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NUCLEAR TECHNOLOGIES

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April 3, 1991

U. S. Nuclear Regulatory Commission
ATTN: Document Control Desk
Washington, D. C. 20555

Gentlemen:

Enclosed are 12 copies each of BAW-10168P-A and BAW-10168-A (non-proprietary version). These are the accepted versions of the topical report entitled "RSG LOCA, B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants." Included in each report are your acceptance letter and Safety Evaluation Report (SER) and NRC questions with BWFC responses that were provided during the review process.

Please note the SER also includes additional approvals. The SER states that Revision 3 of the REFLOD3B Topical Report (BAW-10171P) and the existing FOAM2 code (BAW-10155A) are acceptable for LOCA licensing analyses of RSG plants. This acceptability is subject to the conditions and restrictions delineated in the SER.

B&W requested the report be withheld from public disclosure and provided an affidavit supporting that request with the July 13, 1987 submittal of the draft topical report. Since the accepted version of BAW-10168P does not include any new information beyond that previously submitted, B&W does not intend to submit another affidavit defending the proprietary nature of the report. It is requested, however, that the NRC approved version of BAW-10168P be treated as proprietary for the reasons in the original submittal noted above.

Very truly yours,

J. H. Taylor
Manager, Licensing Services

cc: R. B. Borsum
T. L. Baldwin
Valeria Wilson, NRC
M. A. Caruso, NRC
F. R. Orr, NRC

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BAW-10168-A

Topical Report
March 1991

- RSG LOCA -

# B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants

## Volume I - Large Break

B&W Fuel Company
B&W Nuclear Technologies
P. O. Box 10935
Lynchburg, Virginia 24506

UNITED STATES
NUCLEAR REGULATORY COMMISSION
WASHINGTON, D. C. 20555

January 22, 1991

Mr. J. H. Taylor, Manager
Licensing Services
Babcock & Wilcox Nuclear Technologies
3315 Old Forest Road
P. O. Box 10935
Lynchburg, Virginia 24506-0935

Dear Mr. Taylor:

SUBJECT: ACCEPTANCE FOR REFERENCING OF LICENSING TOPICAL REPORT, BAW-10168P, REVISION 1, "RSG LOCA - B&W LOSS-OF-COOLANT ACCIDENT EVALUATION MODEL FOR RECIRCULATING STEAM GENERATOR PLANTS"

We have completed our review of the subject topical report that was submitted by B&W Fuel Company (BWFC), a subsidiary of B&W Nuclear Technologies, by letters of July 25, 1988, and October 6, 1989. We find the report to be acceptable for referencing in license applications to the extent specified and under the limitations delineated in the report and the associated U. S. Nuclear Regulatory Commission (NRC) evaluation, which is enclosed. The evaluation defines the basis for acceptance of the report.

We will not repeat our review of the matters described in the report and found acceptable when the report appears as a reference in the license applications, except to ensure that the material presented applies to the specific plant involved. Our acceptance applies only to the matters described in the report.

In accordance with procedures established in NUREG-0390, the staff requests that BWFC publish the accepted version of this report, proprietary and non-proprietary, within three months of receipt of this letter. The accepted versions shall incorporate this letter and the enclosed evaluation between the title page and the abstract. The accepted versions shall include an "-A" (designating "accepted") following the report identification symbol.

If our criteria or regulations change such that our conclusions as to the acceptability of the reports are invalidated, BWFC or the applicants referencing the topical report or both will be expected to revise and resubmit their documentation, or submit justification for the continued effective applicability of the topical report without revision of their respective documentation.

Sincerely,

for Ashok C. Thadani, Director
Division of Systems Technology
Office of Nuclear Reactor Regulation

UNITED STATES
NUCLEAR REGULATORY COMMISSION
WASHINGTON, D. C. 20555

ENCLOSURE 1

SAFETY EVALUATION OF BAW-10168P, REVISION 1
"RSG LOCA - B&W LOSS-OF-COOLANT ACCIDENT EVALUATION MODEL
FOR RECIRCULATING STEAM GENERATOR PLANTS"

## 1.0 INTRODUCTION

By letter of July 25, 1988 (Ref. 1), the Babcock & Wilcox Fuel Company (BWFC), a subsidiary of the B&W Nuclear Technologies, submitted topical report BAW-10168P, "RSG LOCA - B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generators," for staff review. In a letter of October 6, 1989 (Ref. 2), BWFC submitted Revision 1 to BAW-10168P (Ref. 3). This evaluation model (EM) for loss-of-coolant accidents (LOCAs) is a part of the BWFC reload methodology for the pressurized water reactors (PWRs) with recirculating steam generators (RSGs). The LOCA EM consists of two parts: Part I is for large-break LOCAs, and Part II for small-break LOCAs.

The large-break LOCA methodology uses a series of computer codes to calculate various phenomena during different phases of a large-break LOCA, namely, the blowdown, refill, and reflood phases. The blowdown portion is calculated using RELAP5/MOD2-B&W (Ref. 4) for the system transient thermal hydraulic calculation. For the phases of the accident from the blowdown through the end of refill, the FRAP-T6-B&W code (Ref. 5) is used in conjunction with the results from RELAP5/MOD2-B&W to perform the thermal mechanical analysis and cladding temperature calculation of the hot rod. Then the REFLOD3B code (Ref. 6) is used to calculate the length of the refill period and the core reflood rates during the reflood phase. The resulting core reflood rates are used in the BEACH code (Ref. 7) to calculate the hot rod cladding temperature and oxidation response during reflood. The peak cladding temperature and oxidation responses

of the entire LOCA transient are combinations of the results from FRAP-16-B&W and BEACH.

The small-break LOCA methodology uses the RELAP5/MOD2-B&W to calculate the thermal-hydraulic response of the reactor system for the entire transient. If RELAP5/MOD2-B&W predicts no core uncovery, a thermal analysis is not required and compliance with 10 CFR 50.46 is ensured. If core uncovery is predicted by RELAP5/MOD2-B&W, then the FOAM2 code (Ref. 8) is used to calculate the core mixture height, and FRAP-T6-B&W is used to calculate the hot rod thermal response to determine the peak cladding temperature (PCT) during the accident.

The staff reviewed separately all of the codes used in the LOCA calculations and found them to be acceptable. However, the staff previously accepted the FOAM2 code for application to the B&W-designed reactors. In a letter of April 2, 1987 (Ref. 9), BWFC stated its intention to use FOAM2 for the reloads of the plants designed by Westinghouse. In a letter of August 9, 1989 (Ref. 10), BWFC provided justifications to support its application to the LOCA conditions anticipated in the Westinghouse plants. In addition, by letter of August 17, 1989 (Ref. 11), BWFC submitted Revision 3 to BAW-10171P, which added two minor features into the REFLOD3B code. This report includes the staff's reviews of the revisions to REFLOD3B and extended application of FOAM2 because they are a part of the LOCA evaluation model.

## 2.0 STAFF EVALUATION

The staff performed its evaluation of the LOCA evaluation models and the revisions to the REFLOD3B code with the technical assistance from the Idaho National Engineering Laboratory (INEL). The evaluation and findings are described in detail in the INEL technical evaluation report (TER) which is enclosed as a part of this report.

### 2.1 Application of FOAM2 to Westinghouse Plants

The FOAM2 code calculates the core level swell during a small-break LOCA. The NRC has previously reviewed and accepted this code with the condition that

justifications be provided for applications to the situations with the core flow rates exceeding 50 lbm/sec and 200 lbm/sec for the small- and large-break LOCAs, respectively, and pressures of less than 150 psig. However, the core conditions during small-break LOCAs at the Westinghouse-designed plants are anticipated to be greater than 50 lbm/sec and less than 150 psig. Therefore, in a letter of August 9, 1989 (Ref. 10), BWFC provided analyses to support the use of FOAM2 for licensing Westinghouse-designed PWR reload analyses with the extension of the FOAM2 applicability ranges to a lower system pressure of 14.7 psia and a higher core flow rate of 1 percent of normal core inlet flow, about 400 lbm/sec. The staff performed the following evaluation and concludes that BWFC's requests are acceptable.

FOAM2 consists of two principal equations: an equation to model the formation and rise of bubbles in the core, and an equation to calculate the core enthalpy distribution and the swell of the water level. Both equations are commonly used in the industry. In addition, the test data used to benchmark FOAM2 are typical level swell data that covered a range of conditions that apply to all light water reactors (LWRs). Therefore, the results of comparisons between test data and FOAM2 apply to all LWRs, including the B&W- and the Westinghouse-designed reactors. Because the designs of the reactor vessel, the core geometries, and the fuel assemblies are similar and because the thermal hydraulic designs are comparable for both types of reactors, the staff concludes that FOAM2 is acceptable for both B&W and Westinghouse reactors.

The core mixture level calculation is generally performed when the reactor coolant system is in a boiling pot mode. In the FOAM2 program, the coolant behavior inside the core is assumed to be in a steady state condition. The use of the steady-state energy balance equations to calculate the axial enthalpy distributions restricts the FOAM2 application to slow core flows so that quasi-steady state conditions can be assured. The proposed flow rate limit of 1 percent of normal core inlet flow corresponds to a core inlet velocity of less than 0.1 feet per second, which is slow enough to ensure that the system is in a quasi-steady state condition. BWFC has applied the quasi-steady state test data to benchmark FOAM2 and has demonstrated the acceptability of FOAM2 by showing conservatism in the data comparison. The staff

concludes that use of FOAM2 is acceptable with the core flow of less than 1 percent of the normal flow inlet rate.

The restriction on the FOAM2 application to pressures below 150 psig was imposed because no experimental data with pressure less than 150 psig were presented to verify the validity of the Wilson bubble rise model used in FOAM2 for the calculation of two phase slip. To support lowering the pressure limit to 14.7 psia, BWFC provided comparisons between the low-pressure steam water test data and the FOAM2 void fraction calculations based on the Wilson bubble rise correlation. For these comparisons, BWFC provided five sets of test data at pressures of 14.5, 29, 43.5, 58 and 88 psia. The staff reviewed the data comparisons and found that the FOAM2 results are conservative because the code underpredicts 85 percent of the void fraction data measured over a pressure range of 15.5 to 88 psia and because deviations between the FOAM2 predictions and the remaining 15-percent test data are within the uncertainty band of the data. Therefore, the staff concludes that the proposed pressures limit of 14.7 psia for use of FOAM2 is acceptable.

## 2.2 REFLOD3B Revisions

In the letter of August 17, 1989, BWFC proposed 2 revisions to REFLOD3B: (1) the addition of the "GSTOP" logic to allow the user to close the leak path of the reactor vessel (RV) side to reduce computation time; and (2) the addition of an option to allow only saturated steam to be vented through the vent valve path. INEL reviewed these modifications as documented in Section 3 of the enclosed TER. BWFC indicated that the "GSTOP" logic will be used only if the RV side leak flow is nearly zero after the accumulators empty. When GSTOP is used to close the leak path, the pressure in the downcomer steam region is set equal to the containment pressure and the rates of change of mass and energy in this region are set to zero. In effect, these changes move the containment boundary to the downcomer steam region. The staff finds that this is a reasonable approach to reduce the time required to run REFLOD3B when the code would otherwise be exceedingly slowed by the small differential pressure between the primary system and the containment causing small flow out the leak path. However, in the application report, the licensee should document the use

of this option in licensing calculations. In addition the application report should include justification that moving the containment boundary condition to the downcomer steam region does not affect the PCT by more than 50°F.

The vessel vent valve model is an option to simulate the vessel spray nozzles and the leakage path from the upper head to the upper downcomer in the recirculating steam generator (RSG) plants. The proposed modification to allow only saturated steam to be vented through the vent valve is so that only saturated steam is passed from the upper head to the downcomer. Because the flow is expected to be steam rather than a steam-water mixture, the staff concludes that the saturated steam option for use with the vent valve model is appropriate.

## 2.3 LOCA Evaluation Model

The NRC has reviewed and accepted separately all the computer codes used in the LOCA EMs for licensing LOCA analyses. Therefore, the main objectives of the staff review of the LOCA EMs are to determine if the combinations of these codes and the methods of analyses result in evaluation methodologies that comply with the requirements set forth in Appendix K to 10 CFR 50, and are appropriate for analyzing the large-and small-break LOCAs to show compliance of the plants with the acceptance criteria of 10 CFR 50.46.

The staff reviewed the LOCA EMs with technical assistance from INEL, whose extensive review effort is documented in the INEL TER. For each requirement of Appendix K, a comparison is made with the evaluation model to ensure that the intent of the requirement is met. The review also includes (1) the benchmark analyses of the evaluation models against appropriate test data to show the adequacy of the various models of the methodologies, and (2) the sensitivity studies performed with a model that represents a Westinghouse four-loop plant to ensure that the most limiting break and plant conditions are determined, and that the appropriate plant models and time steps are used in the analyses. In addition, for each of the acceptance criteria of 10 CFR 50.46, the method that is used to show compliance is reviewed to determine its adequacy.

The staff has reviewed the INEL TER and concurred with its findings. The staff concluded that the LOCA evaluation models in Parts I and II, respectively, of BAW-10168P are acceptable for analyses of large- and small-break LOCAs at the RSG plants. This acceptance is subject to the conditions and restrictions delineated in Section 4, "Conclusions," of the INEL TER. Item 8 of the restrictions requires the LOCA EM user to review the LBLOCA analyses to ensure that appropriate heat transfer regimes are applied and that nonconservative results are not calculated due to data transfer procedures. This condition is intended to ensure that no change in heat transfer regime would arise from the interpolation of the discrete core thermal-hydraulic conditions during data transfer from one code to another with different core axial noding details. Therefore, if different core axial noding details are used in various codes for the analysis, the EM user should examine the code outputs to detect any discrepancy in heat transfer regimes as a result of data transfer, and modify the core nodings if necessary.

## 3.0 CONCLUSION

The staff has reviewed (1) the LOCA evaluation models described in topical report BAW-10168P, (2) the modifications to the REFLOD3B code described in BAW-10171P, Revision 3, and (3) the request to extend the acceptance of the FOAM2 code for application to the Westinghouse-designed PWR plants. With technical assistance from INEL, the staff finds that the LOCA EMs including the REFLOD3B modifications and the existing FOAM2 code are acceptable for LOCA licensing analyses of the RSG plants with the conditions and restrictions delineated in Section 4 of the INEL TER.

## 4.0 REFERENCES

1. Letter from J. H. Taylor (BWFC) to J. A. Norberg (NRC), "ECCS Methodology for Licensing Catawba and McGuire Reloads," JHT/88-128, July 25, 1988.

2. Letter from J. H. Taylor (BWFC) to J. A. Norberg (NRC), "RSG LOCA Topical Report, BAW-10168P," JHT/89-207, October 6, 1989.

3. BAW-10168P, "RSG LOCA, B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants," July 1988; Revision 1, September 1989.

4. BAW-10164P, "RELAP5/MOD2-B&W: An Advanced Computer Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis," December 1987.

5. BAW-10165P, Revision 1, "FRAP-T6-B&W - A Computer Code for the Transient Analysis of Light Water Reactor Fuel Rods," October 1988.

6. BAW-10171P, Revision 3, "REFLOD3B - Model For Multinode Core Reflooding Analysis," September 1989.

7. BAW-10166P, Revision 2, "BEACH - A Computer Code for Reflood Heat Transfer During LOCA," September 1989.

8. BAW-10155A, "FOAM2 - Computer Program to Calculate Core Swell Level and Mass Flow Rate During Small-Break LOCA," October 1987.

9. Letter from J. H. Taylor (BWFC) to C. Berlinger (NRC), "ECCS Application of the FOAM2 Computer Program for Licensing Catawba and McGuire Reloads," April 2, 1987.

10. Letter from J. Taylor (BWFC) to J. Norberg (NRC), "Application of the FOAM Computer Program (BAW-10155) for Licensing Westinghouse Design PWR Reloads," August 9, 1989.

11. Letter from J. H. Taylor (BWFC) to J. A. Norberg (NRC), "ECCS Methodology for Licensing Catawba and McGuire Reloads," JHT/89-176, August 17, 1989.

EGG-EAST-9389
Revision 1

# TECHNICAL EVALUATION REPORT
# B&W LOSS-OF-COOLANT ACCIDENT EVALUATION MODEL
# FOR
# RECIRCULATING STEAM GENERATOR PLANTS
# BAW-10168P, REVISION 1

C. P. Fineman

January 1991

Idaho National Engineering Laboratory
EG&G Idaho, Inc.
Idaho Falls, Idaho 83415

Prepared for the
U.S. Nuclear Regulatory Commission
Washington D.C. 20555
Under DOE Contract No. DE-AC07-76ID01570
FIN No. D3030

ABSTRACT

A review of the topical report B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants, BAW-10168P, Rev. 1, was completed. The review evaluated the Babcock & Wilcox Fuel Company (BWFC) method of analyzing the performance of the emergency core cooling systems (ECCS) following a large or small break loss-of-coolant accident (LBLOCA or SBLOCA) for Westinghouse and Combustion Engineering design pressurized water reactors (PWRs) with BWFC reload fuel. The review found the BWFC methods were sufficient to show compliance with the acceptance criteria of 10CFR50.46 and meet the requirements of Appendix K to 10CFR50. Therefore, it is recommended the methodology be accepted for use in evaluating ECCS performance during LBLOCAs and SBLOCAs in PWRs with recirculating steam generators on the basis that suggested conditions and requirements are followed. This report also documents the review of Revision 3 of the REFLOD3B topical report, BAW-10171P. The INEL recommends that Revision 3 be accepted for use in licensing applications.

## SUMMARY

This report documents the review and evaluation of the Babcock & Wilcox Fuel Company (BWFC) methodology for analyzing the performance of emergency core cooling systems (ECCS) following a large or small break loss-of-coolant accident (LBLOCA or SBLOCA). The method will be used to analyze Westinghouse and Combustion Engineering designed pressurized water reactors (PWRs) with BWFC reload fuel. The methodology uses several computer codes to perform LOCA licensing analyses. The codes for LBLOCA analyses include RELAP5/MOD2 - B&W for calculating system blowdown response, FRAP-T6-B&W for hot rod analyses through the end of the refill phase, BEACH for hot rod analyses during reflood, and REFLOD3B for the determination of reflood rates. The codes for SBLOCA analyses include RELAP5/MOD2 - B&W for calculating the system thermal-hydraulic response, FRAP-T6-B&W for hot rod analyses if core uncovery is calculated, and FOAM2 for the analysis of mixture level and steaming rates if core uncovery is calculated. The methodology is used to perform LOCA analyses to show compliance with the acceptance criteria of 10CFR50.46 using methods that conform to Appendix K of 10CFR50. Babcock & Wilcox Fuel Company submitted the methodology to the Office of Nuclear Reactor Regulation for approval. The Office of Nuclear Reactor Regulation requested assistance from the Idaho National Engineering Laboratory (INEL) in reviewing the method.

The INEL reviewed the method using the information provided by BWFC in the report B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants, BAW-10168P, Rev. 1, and in BWFC's responses to NRC questions. The review found the BWFC methods were sufficient to show compliance with the acceptance criteria of 10CFR50.46 and meet the requirements of Appendix K to 10CFR50. Therefore, it is recommended the methodology be accepted for use in evaluating ECCS performance during LBLOCAs and SBLOCAs in PWRs with recirculating steam generators on the basis that suggested conditions and requirements are followed.

This report also documents the review of Revision 3 of the REFLOD3B topical report, BAW-10171P. The INEL recommends that Revision 3 be accepted for use in licensing applications.

# PREFACE

This report was prepared for the U.S. Nuclear Regulatory Commission, Office of Nuclear Reactor Regulation, by EG&G Idaho, Inc., Energy and Systems Technology Group.

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# TECHNICAL EVALUATION REPORT
# BSW LOSS-OF-COOLANT ACCIDENT EVALUATION MODEL
# FOR
# RECIRCULATING STEAM GENERATOR PLANTS
# BAW-10168P, REVISION 1

## I. INTRODUCTION

Babcock & Wilcox Fuel Company (BWFC) developed a methodology for performing evaluation model (EM) analyses of pressurized water reactors (PWRs) with recirculating steam generators (RSGs) and BWFC reload fuel.[1] The models are designed to show conformance of the emergency core cooling systems (ECCS) to 10CFR50.46 using methods that meet 10CFR50, Appendix K, requirements. The methodology was submitted to the Nuclear Regulatory Commission (NRC) by BWFC for review and acceptance as a method to analyze PWR large and small break loss-of-coolant accidents (LBLOCAs and SBLOCAs) in a manner that conforms to NRC requirements contained in 10CFR50, Appendix K, and other pertinent NRC regulations.

The Office of Nuclear Reactor Regulation (NRR) is responsible for the evaluation and review of computer codes and their proposed applications. The Office of Nuclear Reactor Regulation requested the Idaho National Engineering Laboratory (INEL) to provide assistance in the review of the BWFC methodology. Specifically, the request for assistance included evaluating the method for compliance with the requirements contained in 10CFR50, Appendix K, and how the method would be used to meet the acceptance criteria of 10CFR50.46.

Related to the above reviews, NRR also requested INEL review and evaluate BWFC's responses to NRC questions regarding LOCA modeling applications. The NRC questions were transmitted to BWFC in References 2 and 3. Babcock & Wilcox Fuel Company's responses to these questions are contained in References 4 to 13.

Babcock & Wilcox Fuel Company also submitted Revision 3 of the REFLOD3B topical report, BAW-10171P,[14] for review. Because the changes

to the [illegible] were minor, the review of the changes was incorporated into the [illegible] review.

This technical evaluation report documents the results of the BWFC methodology review for licensing analyses of PWRs with recirculating steam generators and the REFLOOD3B modifications. Section 2 describes BWFC's methodology as it will be applied to LBLOCA and SBLOCA analyses of PWRs with recirculating steam generators. This section also discusses how the method would be used to show conformance to the acceptance criteria of 10CFR50.46 and meets the requirements of Appendix K to 10CFR50. The plant nodalizations BWFC intends to use in the licensing calculations are also discussed. The review of Revision 3 of the REFLOOD3B topical report is discussed in Section 3. Section 4 summarizes the conclusions reached from this review and the references are listed in Section 5.

## 2. METHOD DESCRIPTION AND REVIEW

This section describes the methods BWFC will use in the LBLOCA and SBLOCA analyses of PWRs with RSGs. The methods are reviewed for compliance to the requirements of 10CFR50, Appendix K. The nodalizations for the code input models are reviewed as well as the results of sensitivity studies used to develop the model nodalizations. Analyses of large and small break experiments that applied the complete evaluation model are discussed. The LBLOCA method is discussed first followed by the SBLOCA method.

### 2.1 Large Break Loss-of-Coolant Accidents

#### 2.1.1 LBLOCA Method Description

The LBLOCA methodology developed by BWFC uses a series of computer codes to calculate the different phases of a large break accident, blowdown, refill, and reflood. The blowdown portion of the accident is calculated using RELAP5/MOD2 - B&W.[15] FRAP-T6-B&W[16] uses the output from RELAP5/MOD2 - B&W to determine the hot rod cladding temperature and oxidation response through the end of the refill phase of the accident. REFLOD3B is used to calculate the length of the refill period and the core reflood rates during the reflood portion of the accident. Using the core reflood rates from REFLOD3B, BEACH[17] calculates the hot rod cladding temperature and oxidation response during reflood. The peak cladding temperature (PCT) and oxidation for the accident are determined by combining FRAP-T6-B&W results for the blowdown and refill phases and BEACH results for the reflood phase. The codes used to perform the analyses were reviewed separately from the LOCA EM methodology; NRC approval of each code is given in References 18 to 21. The INEL recommends approval of REFLOD3B, Rev. 3, in Section 3 of this report. Approval of the LOCA EM methodology is contingent upon use of the codes consistent with any conditions of approval placed on them as a result of the individual code reviews.

#### 2.1.2 Compliance With Appendix K to 10 CFR50

The required and acceptable features of a LOCA EM analysis are described in Appendix K to 10CFR50. A comparison of the BWFC LBLOCA

methodology with Appendix K requirements is shown in Table 2.1.2-1. Unless specifically noted in the table or notes to the table, INEL found that BWFC's methodology met the requirements of Appendix K.

In addition to the information in Table 2.1.2-1, BWFC provided Tables 9-1 and 9-2 in Volume 1, Reference 1, that listed the code options and input data that must be specified for LOCA EM analyses. The INEL reviewed Tables 9-1 and 9-2 and was in agreement with BWFC regarding the required use of the models and input listed there. The following information concerns some of the specific entries in those tables.

Table 9-1 noted that certain Licensing Audit Code models from the original FRAP-T6[22] code are not used in the application of FRAP-T6-B&W to licensing analyses. Babcock & Wilcox Fuel Company provided information in response to question 3, Reference 9, that other means are provided through added code models or code input to FRAP-T6-B&W to meet Appendix K.

Table 9-2 required $C_{sup} \geq 1.025$ be used in REFLOD3B. $C_{sup}$ is the user input parameter that determines how much liquid is entrained into the upper plenum region to prevent the upper plenum from becoming superheated. However, the REFLOD3B safety evaluation report (SER) required $C_{sup} \geq 1.05$. To justify this difference, BWFC noted (question 4, Reference 9) $C_{sup}$ was used in REFLOD3B as discussed above. If the upper plenum became superheated, calculational instabilities could occur because REFLOD3B is an equilibrium code. Therefore, BWFC stated that $C_{sup}$ should be greater than 1.0 but only to the extent necessary to keep the upper plenum two-phase. Originally BWFC thought a value of 1.05 would be necessary to prevent upper plenum superheating. Later, it was determined that a value of 1.025 was sufficient. Babcock & Wilcox Fuel Company noted that $C_{sup} = 1.05$ would also work but represents an unnecessarily large adjustment on the REFLOD3B solution. Also, although the EM allows the user to put in any value greater than 1.025 BWFC noted the guidance provided REFLOD3B users is to set $C_{sup} = 1.025$ and only increase it if necessary to prevent upper plenum superheating. That is, $C_{sup}$ should be kept as close to 1.0 as possible (currently 1.025) to prevent superheating of the upper plenum. Based on the above, INEL recommends that $C_{sup} \geq 1.025$ be accepted.

Table 2.1.2-1 COMPARISON OF APPENDIX K REQUIREMENTS AND BWFC'S LBLOCA EVALUATION MODEL

| Appendix K Requirement | BWFC Evaluation Model |
|---|---|
| I.A - Use 1.02 times the licensed power level and the power distribution and peaking factors that result in the most severe consequences. | The reactor is assumed to have been operating continuously at 1.02 times the rated power level. A variety of power shapes and peaking factors will be analyzed. |
| I.A.1 - The initial stored energy in the fuel shall be the highest stored energy as a function of burnup and temperature. | The initial stored energy in the fuel will be calculated with the NRC approved steady state fuel rod models TACO2[23,24] or TACO3.[25,26] |
| I.A.2 - Fission heat shall be calculated using reactor kinetics. Temperature and void coefficients shall be given their minimum plausible values. | During blowdown, fission heat will be calculated using the point kinetics model in RELAP5/MOD2-B&W. Conservative temperature and void coefficients are used (see Note 1). During the reflood period, fission heat is not calculated directly. Rather, the amount of fission energy is estimated by holding the ratio of fission power to decay heat at the end of blowdown constant. (See Note 2.) |
| I.A.3 - Decay heat from actinides, including plutonium and neptunium, shall be calculated. | Power from the decay of actinides based on the 1979 ANS standard is included. (See Note 3.) |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.A.4 - Fission product decay heat shall be based on 1.2 times the 1971 ANS decay heat curve.[27] Direct moderator heating may be included but the value of the gamma energy deposited in the fuel shall be justified. | Fission product decay heat is calculated using 1.2 times the 1971 ANS decay heat curve. Direct moderator heating is modeled assuming 95%/5% of the gamma energy is deposited in the fuel/coolant. (See Note 4.) |
| I.A.5 - The metal-water reaction rate shall be calculated using the Baker-Just equation.[28] The reaction should not be steam limited and when cladding rupture occurs the Baker-Just equation will also be used to calculate the reaction rate for the inside of the cladding. Nodes in the model should be no less than 1.5 in. each way from the rupture location. | The metal-water reaction rate is calculated using the Baker-Just equation without steam limiting. The equation is applied to both cladding surfaces if rupture is calculated. The axial dimension of the ruptured location is considered to be at least 3 in. |
| I.A.6 - Heat transfer from reactor internals should be included. | Appropriate metal heat structures are included in the system models. This includes modeling all metal in contact with reactor coolant water. |
| I.A.7 - Primary to secondary heat transfer should be accounted for in evaluation models. | Primary to secondary heat transfer is calculated during all phases of the LOCA. During reflood, the SG heat transfer coefficient will be high enough so all primary fluid is vaporized and heated to the secondary saturation temperature. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.B - The model should provide for predicting cladding swelling from cladding temperature and pressure differences between the inside and outside of the fuel as a function of time. The swelling and rupture models should be based on applicable data. The gap conductance model and other thermal parameters should be functions of temperature. | The hot rod analysis includes prerupture cladding swell effects. Cladding rupture models that include temperature and pressure difference effects are based on NUREG-0630.[29] The gap conductance mode. derived from GAPCON-THERMAL-2[30] is temperature dependent. The init al rod pressure is taken from the fuel code or a bounding pressure is used (see Note 5). For the fuel and cladding temperature dependent material properties, see Note 5. |
| I.C.1.a - A break spectrum study should be performed and include breaks up to double-ended breaks of the largest pipe in the primary coolant system. Include split breaks with area equal to the pipe. | A break spectrum study including split and double-ended ruptures from 1.0 $ft^2$ up to twice the area of the largest pipe in the primary system will be completed. |
| I.C.1.b - The Moody model[31] will be used to calculate two-phase break flow. At least three discharge coefficients ranging from 0.6 to 1.0 should be applied to the break area. The range of coefficients should be expanded if the results indicate the PCT would be found with a lower coefficient. | Two-phase break flow will be calculated using the Moody Model. The required range of discharge coefficients will be used and applied to both double-ended and split breaks. (See Note 6.) |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.1.c - For cold leg breaks, all ECC injected into the inlet lines or reactor vessel during the bypass period shall be subtracted from the vessel inventory. Bypass shall continue until the mechanisms responsible for bypass are calculated not to be effective. | For cold leg breaks, the end of bypass is calculated using a CCFL correlation (see Note 7). ECC bypass is modeled by removing from the inlet annulus node the smaller of 110% of the ECC flow or 99% of all liquid residing in the inlet annulus node. |
| I.C.1.d - Noding near the break and ECC injection points should be detailed enough for reliable analysis. | Noding near the break isolates the break nodes from both the vessel and the pump. A single node is used for the intact loop cold leg from the ECC injection point to the vessel. This is sufficient based on studies discussed in Sections 2.1.3.2 and 2.1.3.5 of this report. |
| C.2 - Pressure drops should be calculated using models that include a realistic variation of friction factor with Reynolds number and realistic two-phase friction multipliers. Acceptable two-phase multiplier models include the modified Baroczy[32] or a combination of the Thom[33] and Martinelli-Nelson[34] models. | During blowdown, RELAP5/MOD2 - B&W uses the Colebrook correlation[35] to calculate the friction factor and it varies the friction factor with Reynolds number. The HTFS modified Baroczy correlation[36] is used to determine two-phase multipliers. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| C.3 - The momentum equation should take into account temporal change of momentum, momentum convection, area change momentum flux, momentum change due to compressibility, pressure loss from wall friction, pressure loss from area change, and gravitational acceleration. | The momentum equations in RELAP5/MOD2-B&W include the appropriate momentum flux terms as defined by Appendix K. |
| C.4.a,b,c,d - Critical heat flux correlations developed from appropriate steady state or transient data are acceptable. Correlations should be used within their range of applicability as specified by the authors. Acceptable steady state correlations include W-3,[37] B&W-2,[38] Hench-Levy,[39] Macbeth,[40] Barnett,[41] and Hughes.[42] Acceptable transient correlations include the GE correlation. | The CHF correlations to be applied in the BWFC evaluation model (RELAP5/MOD2 - B&W, FRAP-T6-B&W, and BEACH) include those listed in Appendix K (Barnett, and modified Barnett (Hughes)) or previously approved by the NRC in the THETA1-B code[43,44] (Griffith and Macbeth).[45,46] The BWCMV correlation[47] was recently approved by the NRC.[48] A minimum CHF value of 90,000 Btu/h-ft$^2$ is applied to both high and low flow regimes. |
| I.C.4.e - Once CHF has been calculated, return to nucleate boiling should be locked out until the reflood portion of the LOCA. | Once CHF has been calculated, return to pre-CHF correlations is not allowed during the remainder of the blowdown phase of the LOCA. See Note 8. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.5.a,b - Transition and film boiling correlations should be compared to applicable steady state and transient data to demonstrate the heat transfer coefficient calculated is less than or equal to the mean value of the applicable data. Acceptable film boiling correlations include the Groeneveld,[49] Dougall-Rohsenow,[50] and Westinghouse[51] correlations. The McDonough, Milich, and King transition boiling correlation[52] is acceptable. Transition boiling cannot be reapplied during blowdown once cladding superheat exceeds 300°F. | The McDonough, Milich, and King transition boiling correlation listed in Appendix K is used. Film boiling is calculated using the Condie-Bengston correlation.[53] (See Note 9.) Once the fuel cladding temperature exceeds the saturation temperature by 300°F, transition boiling is not reapplied for the remainder of the blowdown phase of the transient. |
| I.C.6 - Pump modeling should be based on a dynamic model that includes momentum transfer to the fluid with variable pump speed as a function of time. Pump resistance should be justified. Two-phase pump performance should be verified. | The pump model is based on homologous relationships and includes single- and two-phase performance curves. Plant-specific pump performance tables are given as model input using best-estimate values. Two-phase degradation is modeled conservatively. All data is based on measured data for scaled pumps, vendor homologous curves, or pumps with similar specific speeds. See Note 10. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.7.a,b - The flow through the hot region shall be calculated as a function of time and the hot region shall not be greater than one fuel bundle. Core flow in the average and hot channels should include the effects of cross flow and flow blockage. The flow should be smoothed to remove oscillations with a period less than 0.1 s. The core flow from the blowdown analysis shall be used in the hot channel heatup analysis. | A rupture model is included to account for these effects; however, flow blockage due to prerupture cladding strain is not modeled (see Note 11). The hot region is assumed to be one fuel bundle and cross flow is considered between the average and hot core regions. Flow is smoothed to remove oscillations with periods less than 0.1 s and the smoothed flow is used in the heatup calculations. |
| I.D.1 - Single failure of the ECCS should be chosen to maximize the calculated PCT. | The single failure is taken to be one diesel generator and associated ECC systems unless it is more conservative to assume all ECC systems are available. (See Note 12.) |
| I.D.2 - Conservative containment pressures should be used and should include the effects of installed pressure reduction systems and processes. | Containment pressure will be provided and justified on a plant-specific basis. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.D.3 - Refill and reflood rates and carryover fraction should be based on acceptable data, such as FLECHT data.[54-57] The pump rotor should be modeled in the condition (locked or free spinning) leading to the highest calculated PCT. The effects on the reflood rate of the accumulator cover gas should be accounted for in the analysis. | The refill and reflood rates are calculated using REFLOD3B. The carryover rate model, CRFCKN, was developed from FLECHT data. The pump rotor is locked with a plant-specific value for the pump resistance. Accumulator cover gas is neglected because the effects are minimal or it is conservative to neglect it. (See Note 13.) |
| I.D.4 - Interaction between steam and the ECC water shall be taken into account. | REFLOD3B accounts for this by increasing the injection section pressure difference (see Note 14.) |
| I.D.5 - Refill and reflood heat transfer for reflood rates greater than 1 inch/s shall be based on FLECHT data (Reference 54) for unblocked cores. For reflood rates less than 1 inch/s, heat transfer shall be based on steam cooling only and shall account for flow blockage due to cladding swell or rupture. | During refill, the rods undergo an adiabatic heatup. During reflood, the FLECHT (References 54 and 57) and ANC[58] correlations in REFLOD3B are used to calculate core heat transfer. (See Note 15.) BEACH is used to calculate the reflood PCT and was benchmarked against FLECHT data. For all reflood rates, BEACH heat transfer above the quench front is based only on steam cooling. Cladding rupture is accounted for by increasing the heat transfer area and adding a loss factor to account for rupture. See Note 16. |

Table 2.1.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| II.1.a - A detailed description of the evaluation model shall be provided. | BWFC provided model documentation for each code used in their evaluation model. The documentation is listed in Note 17. |
| II.1.b - Changes to the evaluation model (transient cladding temperature differences of greater than 50°F) shall be documented. | Changes that result in a deviation of greater than 50°F in the cladding temperature transient will be documented. |
| II.1.c - A listing of each computer program shall be furnished to the NRC. | BWFC will provide the computer program listing to the NRC upon request. |
| II.2 - Solution convergence shall be demonstrated by noding and time step studies. | Sensitivity studies for nodalization and time steps were presented. See Section 2.1.3. |
| II.3 - Sensitivity studies shall be performed for each evaluation model to evaluate the effects of noding, predominate phenomena, assuming pump operation or a locked rotor, and values of parameters over applicable ranges. | Appropriate sensitivity studies were done. See Section 2.1.3. Additional studies that must be done for a specific plant category will be provided as part of the category specific submittal. |
| II.4 - Comparisons to data to be made where applicable. | Code/data benchmarks were provided in each code topical report. |
| II.5 - Model acceptability is based on adequacy of the calculational methods and margin of conservatism. | |

Notes to Table 2.1.2-1:

1. Data to be used for the fuel temperature and moderator void reactivity coefficients will be provided and justified with each application of the Evaluation Model (response to question 3, Reference 4).

2. The method of calculating fission heat during reflood was discussed by BWFC in their response to question 4, Reference 4. Babcock & Wilcox Fuel Company chose to keep the fission power to total power ratio constant at the value determined at the end-of-blowdown. Babcock & Wilcox Fuel Company noted this method will result in a conservative calculation of the fission energy above the quench front because an actual calculation of the fission energy would show it to be a decreasing fraction of the decay heat. Therefore, the effect of the method on the calculation of the PCT is conservative. Below the quench front, BWFC noted reactivity feedback effects result in the fission energy being underpredicted by this method until approximately 80 s after blowdown. They stated this underprediction is small, decreases with time, is partially accounted for by the fact that REFLOD3B assumes the coolant entering the core is saturated (even when it may be subcooled), and the underprediction occurs in the portion of the core that is covered with liquid and the cladding temperatures have returned to near saturation. Based on these consideration, this method of calculating reflood fission heat is considered adequate by the INEL.

3. The actinide decay heat is based on the 1979 ANS standard. The actinide decay heat curve only includes neptunium and isotopes of uranium. While Appendix K requires that plutonium also be considered, an adequate decay heat calculation is made without including plutonium because the half-life of plutonium is $2.4 \times 10^4$ years. Therefore, decay of plutonium does not add significantly to the decay heat calculation and this approach is considered adequate by the INEL.

4. In BWFC's response to question 8, Reference 9, they showed the built-in RELAP5/MOD2 - B&W decay heat curves were adequate to represent the 1971 ANS decay heat curve.

Babcock & Wilcox Fuel Company stated the 95%/5% fuel and moderator split for the gamma energy was based on the Catawba FSAR. In BWFC's response to a question on the applicability of the value for Catawba to other plants and BWFC reload fuel (question 6, Reference 4), BWFC stated that the fuel mass for BWFC assemblies differed from the mass for other fuel vendors by less than 3%. In addition, the fluid conditions in the core are similar from plant to plant, varying from liquid to steam. Babcock & Wilcox Fuel Company stated liquid and steam densities are also expected to be similar from plant to plant. Given these considerations, BWFC has adequately justified the applicability of the 95%/5% fuel and moderator gamma energy split.

5. For the initial rod pressure, the approach used will be justified in the plant-specific submittals. Babcock & Wilcox Fuel Company stated in Reference 4, response to question 11, that the material properties are determined on a plant-specific basis using LOCA EM specified criteria. The criteria include comparing the data to accepted reference data and documenting the bases for the selection of the property values used in the calculation. Therefore, the property values selected are subject to the quality assurance process and auditable against the LOCA EM criteria. This is adequate.

6. Subcooled break discharge is calculated using the extended Henry-Fauske model. A smooth transition to the Moody model is ensured by making the last point in the extended Henry-Fauske critical flow table equal to the first point in the Moody critical flow table. An underrelaxation scheme is also used for qualities from 0 to 0.001%. This approach was used in the benchmark calculations for Semiscale Tests S-04-6[59] (LBLOCA) and S-LH-1[60] (SBLOCA) with reasonable results. In a conference call with BWFC on August 8, 1989, BWFC stated the critical flow models will be run using static pressure and enthalpy as input to the models. This is consistent with the recommendations of the RELAP5/MOD2 - B&W safety evaluation report. Therefore, the critical flow modeling is considered adequate.

7. In the BWFC LOCA EM, the end-of-bypass is based on a countercurrent flow limitation (CCFL) correlation. The correlation was described in

BWFC's response to question 1, Reference 12. Bypass ends when the correlation allows complete ECC penetration into the downcomer; that is, bypass ends when resistance to ECC penetration by steam flow up the downcomer is completely removed. The method is implemented in the LOCA EM by having RELAP5/MOD2 - B&W calculate bypass until the end-of-blowdown is reached. The end-of-blowdown is defined to be the first time reverse flow (containment to RCS) at the vessel side break is calculated. The CCFL correlation is then applied to determine when conditions in the downcomer would have allowed ECC penetration in the RELAP5/MOD2 - B&W analysis, as discussed above. If this time is less than the end-of-blowdown, the excess bypass is determined and the mass added to the vessel lower plenum inventory at the start of the REFLOD3B calculation. To account for the free fall of liquid from the cold leg nozzles to the elevation of the bottom of the core, the ECCS is turned off in the REFLOD3B calculation for the free fall time. If the CCFL correlation does not calculate ECC penetration into the downcomer before the end-of-blowdown, end-of-bypass is defined to be the same time as the end-of-blowdown. This approach is considered acceptable by the INEL. The end-of-bypass is determined directly from a CCFL correlation based on dimensionless parameters and derived from large-scale data. Should the CCFL correlation not calculate that conditions in the downcomer allow an end-of-bypass before the end-of-blowdown, INEL agrees with BWFC (Reference 4, question 13) that, with the calculation of reverse flow at the break, the factors responsible for forcing the ECC fluid from the RCS and out the break would no longer be effective.

8. The RELAP5/MOD2 - B&W report, BAW-10164P, indicates there is an exception to the lockout logic when the wall temperature is less than the saturation temperature. In this case single-phase liquid convection or two-phase condensation is allowed by the code. This exception to the nucleate and transition boiling lockouts is considered acceptable during blowdown because, with the wall temperature less than the saturation temperature, the use of the single-phase liquid or two-phase condensation correlations allows the fluid to heat the rod.

9. Approval of the Condie-Bengston film boiling correlation for use in LOCA EM applications was given in the review of RELAP5/MOD2 - B&W.

10. Effect of pump trip during a LBLOCA will be evaluated on the first application of the LOCA EM model for each plant type.

11. Babcock & Wilcox Fuel Company stated in response to question 9, Reference 4, and question 6, Reference 10, that flow blockage due to prerupture cladding strain is not modeled in RELAP5/MOD2 - B&W and BEACH. First, BWFC argued, based on the data of Haidy,[61] the prerupture flow blockage is small (less than 20%).

They also stated prerupture swell results in a smooth area change that will not significantly add to the flow resistance, any swelling would be located near the high power elevations, and any flow diversion that might occur would be quickly recovered. To support their argument that flow diversion effects for cladding swell of 20% or less are minimal, BWFC analyzed the effects of 20% prerupture swell assuming a maximum rupture strain of 90%, i.e., prerupture strain of 18%, using a network resistance method. The analysis included the effects of prerupture swell form loss, prerupture swell flow resistance, and crossflow resistance. Total flow reduction over nominal assembly flow ranged from 2.8 to 10% at the prerupture location. For the 2.8% case, BWFC concluded the flow diversion effects of prerupture cladding swell are small and can be neglected without compromising the overall analysis. For the 10% case, BWFC provided information to show heat transfer effects for the prerupture swelled condition increase by at least 18%. For the 10% flow reduction case, the analysis also showed the flow diversion at the rupture location returns to the hot assembly just upstream of the rupture location. Therefore, BWFC concluded neglect of flow diversion due to prerupture cladding swell for the 10% flow reduction case is also adequate.

The INEL reviewed additional data on zircaloy cladding behavior during ballooning and rupture in References 62 and 63. This data supports BWFC's argument with respect to cladding behavior. The data showed prerupture cladding swell is limited because, once cladding swell

exceeded 10 to 20%, the cladding rapidly expanded to the point of rupture. Reference 64 discussed test results that show heat transfer effects overcompensate for flow diversion effects up to blockages of at least 62% during reflood. Therefore, INEL recommends BWFC's approach concerning prerupture cladding swell be accepted. However, BWFC would need to justify the acceptability of any licensing analyses where cladding swell exceeded 20% but rupture was not calculated.

12. The ECCS single failure applied to the LOCA EM calculation will be justified on a plant-specific basis.

13. The assumption of not including the effects of accumulator nitrogen in the LOCA EM is considered acceptable for the reasons BWFC gave in response to question 17 in Reference 4. However, one concern not directly addressed by BWFC in this response was the increase in system pressure that occurs as a result of accumulator nitrogen entering the system. This pressure increase at reflood pressure conditions could affect low pressure ECCS injection. Data from Semiscale MOD-1 test Series 4[65] showed this pressure increase could be 50 to 100 psi. Babcock & Wilcox Fuel Company addressed this issue in their response to question 9, Reference 10. The information presented by BWFC showed that the pressure increase at the end of accumulator liquid injection for a full-size plant was approximately 1 to 2 psi, and this pressure increase did not have a significant effect on the low pressure ECCS. The reasons for the difference between the plant response and the Semiscale MOD-1 system response presented by BWFC include larger vessel to break resistances for the Semiscale broken loop hot and cold legs and core flow oscillations in Semiscale that are not expected in the plant. On reviewing this information, INEL concluded BWFC provided adequate justification for the differences between the plant and Semiscale system responses. The INEL agrees with BWFC's conclusion that the Semiscale response is not expected for the plant. Therefore, the INEL concludes injection of accumulator nitrogen will have a minimal effect on the plant low pressure ECCS injection capability and does not need to be considered directly in the EM analysis.

14. Babcock & Wilcox Fuel Company (question 15, Reference 9) provided information on the measured pressure loss due to steam/water interaction in Combustion Engineering[66,67] (CE) and Westinghouse[68] tests. This information was sufficient to justify the pressure drops used by BWFC to account for the momentum loss due to steam/water interaction and provided in Reference 1 for the accumulator and pumped injection periods.

15. Values used in REFLOD3B calculations for the fuel and cladding conductivities and the gap heat transfer coefficient will be justified in the plant-specific applications.

16. In BEACH, the increase in cladding surface area for the ruptured node is included by multiplying the convective enhancement factor, Fgg, by the ratio of the ruptured to cold clad diameters (see Section 2.1.3.8 of the BEACH code manual, BAW-10166, Reference 17). The use of the diameter ratio assumes the cladding retains a cylindrical shape after rupture. Depending on the actual shape of the ruptured cladding, the surface area increase in the calculation may be more or less than the actual increase. Based on BWFC's response to question 3, Reference 69, the surface area increase is included in only one node (a node is approximately 6 inches long). However, the INEL notes that test data[70] has shown the surface area increase for a rod with ruptured cladding usually covers the entire ruptured grid span (three BEACH nodes, approximately 18 inches) as well as the two adjacent grid spans. Therefore, while there may be situations where the surface area increase for the ruptured node is too large, it is at least partially compensated by the fact that the surface area is not increased in the other two nodes in the same grid span where surface area increases are possible and the adjacent grid spans. The form loss factor calculated from an abrupt area contraction/expansion formula is also added to the channel loss factors. These factors are input by hand at the time of rupture by restarting the code with the additional input information. Based on the above consideration, INEL finds the BEACH models adequate.

17. Documentation for the codes used in the recirculating steam generator LBLOCA evaluation model was provided in the following BWFC code topical reports:

| | | |
|---|---|---|
| a. | RELAP5/MOD2 + B&W | BAW-10164P |
| b. | FRAP-T6-B&W | BAW-10165P |
| c. | REFLOD3B | BAW-10171P |
| d. | BEACH | BAW-10166P |
| e. | Overall EM | BAW-10168P, Rev. 1 |

2.1.3 Sensitivity Studies

Appendix A of Reference 1 contained a number of LBLOCA sensitivity studies. This section describes those studies as the results apply to meeting Appendix K requirements found in Items I.C.1.d, II.2, and II.3. The sensitivity studies were performed with a model that represented a Westinghouse four-loop plant. In response to question 40 in Reference 8, BWFC provided information on the applicability of the sensitivity studies to the other plant types covered by the Evaluation Model. Unless limitations are specifically noted in the review of an individual study, INEL agreed with BWFC's conclusion regarding the applicability of the study to all plant types covered by the Evaluation Model.

2.1.3.1 Time Step Studies. Time step studies were provided for the RELAP5/MOD2 - B&W, FRAP-T6-B&W, and BEACH codes. The RELAP5/MOD2 - B&W and FRAP-T6-B&W studies were presented in Appendix A of Reference 1. The BEACH study was presented in BWFC's response to question 10, Reference 9.

In the RELAP5/MOD2 - B&W and FRAP-T6-B&W studies, the time step in the base case was cut in half and the analysis rerun with the smaller time step. Comparison of the two calculations showed the two runs were essentially the same. This indicates the time steps used in the base case calculations for RELAP5/MOD2 - B&W and FRAP-T6-B&W were sufficiently small to ensure a converged solution. The time steps used in the base case analyses for RELAP5/MOD2 - B&W and FRAP-T6-B&W will be used in LOCA EM analyses (response to question 28, Reference 4). The time steps were:

RELAP5/MOD2 - B&W

| Time Period (s) | Minimum Time Step (s) | Maximum Time Step (s) |
|---|---|---|
| 0.0 - 0.2 | 0.000001 | 0.002 |
| 0.0 - 1.0 | 0.000001 | 0.005 |
| 1.0 - 16.0 | 0.000001 | 0.02 |
| 16.0 - 22.0 | 0.000001 | 0.005 |

FRAP-T6-B&W

| Time Period Description | Time Period (s) | Time Step (s) |
|---|---|---|
| Blowdown period | 0.0 - 20.0 | 0.01 |
| Post blowdown | beyond 20.0 | 0.2 |

Based on the time step study, these time steps are adequate for LOCA EM analyses using RELAP5/MOD2 - B&W and FRAP-T6-B&W.

For BEACH, BWFC intends to use a maximum input time step of 0.01 s. A base case analysis was run with this maximum time step size. To verify the BEACH time step controller results in a converged solution with this maximum time step size, the BEACH time step study used a time step that was one-half the minimum time step calculated by the time step controller during the base case analysis. This was 0.0025 s. When the smaller maximum time step was used, the analysis results were essentially the same as the base case. The PCT was within 1.5°F, the time of PCT was within 10 s, the rupture node was the same, and the time of rupture was within 0.2 s. This indicates use of the time step controller with the base case maximum input time step resulted in a converged solution. Therefore, use of a maximum time step of 0.01 s in BEACH is considered adequate.

A time step study for REFLOD3B was not performed. Babcock & Wilcox Fuel Company stated (response to question 28, Reference 4) that a specific study was not performed for REFLOD3B because the time advancement in REFLOD3B is controlled by the program using accuracy and convergence criteria and time step size or limits are not input by the user. Therefore, INEL concluded time advancement convergence need not be demonstrated for REFLOD3B.

2.1.3.2 Loop Nodalization Studies. Nodalization studies were performed for RELAP5/MOD2 - B&W and REFLOD3B. In the RELAP5/MOD2 - B&W study, the base case plant nodalization, presented in Figure A-1, Reference 1, was used to calculate the system blowdown response. In the sensitivity study, the number of nodes used to model the loops was cut in half, except in the intact loop cold leg were the number of nodes was

increased from one to two (see Figure A-7, Reference 1). In the REFLOD3B study, the number of nodes to model the loops was doubled (see Figures A-12 and A-13, Reference 1). Calculations with both sets of nodalizations were run. Comparison of the results showed little difference between the calculations. In response to question 40, Reference 8, BWFC noted the REFLOD3B nodalization was only valid for plants with a one-to-one correspondence between hot legs and cold legs. Therefore, a separate study would be completed prior to the first application of the Evaluation Model to a CE 2 x 4 plant to confirm the nodalization scheme. This indicates the nodalizations used in the base case runs were sufficient to ensure a converged solution and are adequate for LOCA EM analyses for Westinghouse plants (RELAP5/MOD2 - B&W and REFLOD3B) and CE plants (RELAP5/MOD2 - B&W).

Nodalization studies for BEACH and FRAP-T6-B&w were not completed. In response to question 37 in Reference 8, BWFC discussed the reasons for this. Regarding BEACH, BWFC noted the nodalization is set by the models used to represent fuel assembly grid heat transfer enhancement. This involves a three node grid-to-grid nodalization. For BWFC fuel, with seven grid spacers per assembly, this results in a 20 or 21 axial node model. The 20 node model would be used in the case where a small volume would result at the top of a fuel bundle model if 21 nodes were used. Babcock & Wilcox Fuel Company verified this nodalization during BEACH development and stated this nodalization must be used in order for the BEACH results to be considered valid. Based on this response, the BEACH nodalization is considered adequate. Babcock & Wilcox Fuel Company selected the nodalization for FRAP-T6-B&W to be consistent with the BEACH nodalization. Therefore, the FRAP-T6-B&W model also includes 20 or 21 axial nodes. Based on INEL experience with FRAP-T6, this nodalization is adequate.

2.1.3.3 Primary Coolant Pump Rotor Resistance Study. Appendix K, Item I.D.3, requires that the pumps have locked rotors, if this leads to the highest PCT; otherwise, the rotor should be assumed to run free. To determine whether the pump rotor resistance should be based on a locked or free spinning rotor, BWFC ran REFLOD3B calculations where both cases were modeled. Because the loop resistance was higher with the pump rotor

locked, the locked rotor case gave lower reflood rates than the free spinning case. Therefore, the locked rotor case should be adequate for LOCA EM analyses.

2.1.3.4 Maximum ECCS Study. Babcock & Wilcox Fuel Company performed a study to determine the effects of the maximum pumped ECCS versus the minimum pumped ECCS. The study in Appendix A of BAW-10168P, Rev. 1, showed that there was little change in the core flooding rate between the maximum and the minimum pumped ECCS cases. However, in BWFC's response to question 40, Reference 8, BWFC stated that this study will be performed on a plant-specific basis. Babcock & Wilcox Fuel Company should provide the results of this study to the NRC for review with BWFC's justification for the pumped ECCS flow rates used in the LOCA EM analyses.

2.1.3.5 Break Nodalization Study. The base case nodalization in the RELAP5/MOD2 - B&W broken loop cold leg uses four nodes. To determine whether this was sufficient to permit a reliable analysis of the thermodynamic conditions in this area as required by Appendix K, Item I.C.1.d, BWFC increased the nodalization to six and eight nodes. The study showed the calculated results were nearly identical for all three cases. This indicates the four node model is adequate for EM analyses.

2.1.3.6 Pressurizer Location Study. Two RELAP5/MOD2 - B&W calculations were run, one with the pressurizer in the intact loop and one with the pressurizer in the broken loop. With the pressurizer in the intact loop, the end-of-blowdown (EOB) temperature was slightly higher and there was less mass remaining in the vessel. For these reasons, BWFC will model the pressurizer in the intact loop for LOCA EM analyses. Because it is conservative, modeling the pressurizer in the intact loop is adequate.

2.1.3.7 Pump Degradation Study. Three RELAP5/MOD2 - B&W calculations were run, one with the two-phase pump degradation multipliers from NUREG/CR-4312 (base case) and two sets of bounding multipliers to maximize (case 1) and minimize (case 2) two-phase pump degradation. Babcock & Wilcox Fuel Company stated in response to question 33, Reference 4, that all three sets of curves (base case, case 1 and case 2) were based on data from the Semiscale Mod-1 pump. Further, based on NUREG-1230,[71] BWFC

noted the Semiscale Mod-1 pump showed more two-phase degradation than pumps closer in design to full scale PWR pumps. Therefore, BWFC concluded the curves used in the pump degradation study result in more degradation than would actually be expected during a LOCA in a PWR. This fact, taken in conjunction with the fact that all PWR pumps have similar specific speeds and operating characteristics, lead BWFC to conclude the curves used in the study are conservative and applicable to all types of PWR pumps. The INEL agrees with BWFC's conclusions.

Comparison of the calculated results showed minimal differences between them. Cases 1 and 2 resulted in higher and lower cladding temperatures compared to the base case; however, the difference between case 1 and the base case was small. Babcock & Wilcox Fuel Company chose the multipliers from the base case for inclusion in the LOCA EM model because BWFC believed them to be more representative of actual degradation at high void fractions than the case 1 multipliers. The INEL finds this acceptable.

The pump degradation study was completed with a RELAP5/MOD2 - B&W model representative of a Westinghouse four-loop plant. To confirm this result for other plant types, BWFC stated a base LBLOCA analysis for other plant types will be completed using the base case two-phase pump degradation multipliers. The core flow for this analysis will be reviewed to determine whether it is similar to that calculated in the base case study reported in BAW-10168P, Rev. 1. If the core flows are similar to the Westinghouse four-loop design, the results of the BAW-10168P, Rev. 1, study will be applied; otherwise, the study will be repeated. Based on the study and BWFC's reasoning, the base case multipliers are adequate for LOCA EM analyses of Westinghouse four-loop plants and will be confirmed for other plant types.

2.1.3.8 RELAP5/MOD2 - B&W Core Crossflow Resistance Study. The RELAP5/MOD2 - B&W LOCA EM core nodalization consists of two parallel channels; one channel represents the hot assembly and the other channel represents the rest of the core. A study to determine the effect of the crossflow resistance was reported in BAW-10168P, Rev. 1. This study took the base case crossflow resistance and multiplied and divided it by 100.

This resistance was based on a 17 x 17 fuel bundle design and was provided in BWFC's response to question 27a, Reference 8. All three cases were run, and only slight differences in the transient and EOB conditions were found. Based on this study, the INEL finds the base case crossflow resistance to be used in LOCA EM analyses is adequate for all reloads involving 17 x 17 fuel bundles.

Babcock & Wilcox Fuel Company stated, in Reference 8 in response to question 27a, that the base case crossflow resistance will be used for 17 x 17 bundles. For other bundle designs (14 x 14, 15 x 15, or 16 x 16), the crossflow resistance will be updated. In a call with BWFC on August 7, 1989, BWFC stated the crossflow resistance for all bundle designs will also be updated to reflect plant-to-plant differences in the number of bundles in the average channel. Because the sensitivity study did not consider the effect of blowdown rupture on the crossflow resistance, the results of the study are not considered valid by the INEL if a blowdown rupture is calculated.

2.1.3.9 Core Noding Study. The RELAP5/MOD2 - B&W axial and radial core nodalization was studied using the nodalizations in Figures A-2, A-42, and A-43, Reference 1. The nodalization in Figure A-2 was the base nodalization. This study showed there were minimal differences between the three cases. Based on this result, the base nodalization is adequate for LOCA EM calculations.

2.1.3.10 Radial Fuel Segmentation Study. The number of radial segments used to model the fuel rod in FRAP-T6-B&W was studied. The base model used seven segments, and BWFC used ten segments in the sensitivity study. This change made essentially no difference in the calculated results. Therefore, use of the seven segment model is adequate.

2.1.3.11 Time-In-Life Study. Babcock & Wilcox Fuel Company performed a study to determine the time-in-life core conditions that result in the highest PCT. The study used burnups of 100; 1000; 40,000; and 60,000 MWD/Mtu. These conditions range from the beginning of core life (BOL) to the end of core life (EOL). Study of the core life conditions using the BWFC steady state fuel behavior code TACO2 showed maximum fuel stored

energy occurred at BOL when fuel densification is complete. The results of the time-in-life study showed that, while the system hydraulic behavior during blowdown was not significantly affected by the initial fuel conditions, the PCT was related to the initial stored energy. As a result, the maximum PCT was calculated for the BOL case. Based on this study, BOL core conditions should be used in LOCA EM analyses unless other conditions, such as the calculation of a blowdown rupture for the EOL case, result in a higher peak cladding temperature. To confirm the EOL case will not result in a blowdown rupture, BWFC stated (Reference 8, response to question 40), "If an examination of the fuel performance and a base run for the alternate design cannot assure that the end-of-life fuel pin conditions will not result in a blowdown rupture, a specific calculation of the condition will be performed." The INEL considers this adequate to assure the EOL blowdown rupture will not be overlooked provided: (a) this review is completed for all fuel bundle and plant types and (b) BWFC provides adequate justification in the LOCA EM plant-specific submittal that an EOL blowdown rupture will not occur if a plant-specific EOL calculation is not supplied.

### 2.1.4 PLANT MODELS

In Reference 1, BWFC presented the plant nodalizations to be used in the LOCA EM methodology for REFLOD3B, FRAP-T6-B&W, and BEACH. For RELAP5/MOD2 - B&W, BWFC presented the plant nodalizations to be used in the LOCA EM methodology in Reference 13. The following sections discuss the nodalizations and the bases for their acceptability.

2.1.4.1 RELAP5/MOD2 - B&W Plant Nodalization. The loop nodalization to be used in the RELAP5/MOD2 - B&W analysis of the blowdown phase is shown in Figure 1a, Reference 13. The intact loop represents the two or three unbroken loops combined while the broken loop represents the one loop containing the break. As discussed in Section 2.1.3.2 of this report, BWFC sensitivity studies showed this nodalization was detailed enough to ensure a converged solution. Therefore, it is adequate for LOCA EM analyses.

The vessel nodalization is shown in Figure 1b, Reference 13. The core is represented by six axial nodes and two channels, one representing the hot bundle (volumes 328 to 338) and one representing the remainder of the

core (volumes 316 to 326). A core nodalization study indicated this nodalization gave similar results to more detailed nodalizations (see Section 2.1.3.9 of this report). Therefore, it is considered adequate.

2.1.4.2 REFLOD3B Plant Nodalization. The loop nodalization to be used in the REFLOD3B refill and reflood analyses is shown in Figure A-12, Reference 1. The loop nodalization was shown in Section 2.1.3.2 of this report to have sufficient detail to ensure a converged solution for plants with one hot leg and one cold leg per loop. Thus, this nodalization is adequate for LOCA EM analyses of this plant type. Babcock & Wilcox Fuel Company stated (Reference 8, question 40) the adequacy of the loop nodalization for a 2 x 4 plant would be verified during the first application of the EM to a 2 x 4 plant.

Four volumes are used to represent the vessel, one for each of the following: the uncovered core and upper plenum, covered core, lower plenum and lower downcomer, and upper downcomer. The rods in the core are represented by five axial nodes. The vessel and core thermal-hydraulic nodalization is fixed by the REFLOD3B code. This approach to the vessel and core nodalization is the same as that previously approved by the NRC and is, therefore, considered adequate.

2.1.4.3 BEACH Core and FRAP-T6-B&W Hot Rod Nodalizations. The nodalizations to be used with these codes were discussed in Section 2.1.3.2 of this report. That review found the nodalizations are adequate.

2.1.5 Application of the RSG LBLOCA EM

Babcock & Wilcox Fuel Company provided an example of the application of the LBLOCA EM in response to question 1, Reference 69, for the BEACH code. The application was an extension of the Semiscale Test S-04-6 assessment of RELAP5/MOD2 - B&W that was provided in response to question 12, Reference 72. Test S-04-6 represented a 200% double-ended cold leg break in the Semiscale Mod-1 system that covered all phases of the LBLOCA: blowdown, refill, and reflood. As such, the analysis of this experiment required the use of the all the LOCA EM codes to determine the PCT. As noted in the RELAP5/MOD2 - B&W SER, the code was found to

adequately calculate the system thermal-hydraulic response during the blowdown phase. With respect to the PCT, the calculated peak blowdown temperature was approximately 500°F higher than the data, and the calculated reflood PCT was approximately 475°F higher than the data (see Figure 1-2, Reference 69).

It should be noted that there were higher temperatures recorded in the core during Test 5-04-6 than BWFC used in their comparison. The differences between the higher temperatures and the temperatures used by BWFC are related to the time to CHF early in the blowdown (Reference 65). With a delayed CHF, as was the case for the experimental rod temperatures used by BWFC, lower blowdown and reflood PCTs resulted relative to the rods where early CHF occurred. This was due to greater energy removal prior to CHF. When the LOCA EM analysis temperature is compared to an experimental rod temperature with early CHF, the conservatism of the LOCA EM analysis is reduced. At the 25-inch elevation (see Figure 1-2, Reference 69), the calculated blowdown peak is approximately 100°F higher than the data and the reflood peak is 175°F higher than the data. At the 29-inch elevation (Figure 1-3, Reference 69), the peak blowdown temperature in the LOCA EM analysis is low by 100°F and the calculated peak reflood temperature equals the data.

The INEL questioned BWFC on why the FRAP-T6-B&W temperature at the 29-inch elevation is approximately 250°F lower than the FRAP-T6-B&W temperature at the 25-inch elevation even though the power is the same at both elevations (see Figures 1-2 and 1-3, Reference 69). The INEL also notes that the FRAP-T6-B&W temperature at the 29-inch elevation is approximately 150°F lower during blowdown than the RELAP5/MOD2 - B&W temperature at the 29-inch elevation (see Figure 1-3, Reference 69, and Figure 12.33, Reference 72). In a telephone call with BWFC on August 24, 1990, BWFC stated the difference was due to the heat transfer regimes being applied at the 25-inch and 29-inch elevations. At the 25-inch elevation, the heat transfer mode was film boiling, and at the 29-inch elevation, the heat transfer mode was forced convection vaporization. Therefore, a higher heat transfer coefficient was applied to the 29-inch elevation in the calculation resulting in better cooling and a lower cladding temperature. Babcock & Wilcox Fuel Company stated the different heat transfer regimes

were calculated because of the data transfer from RELAP5/MOD2 - B&W, used to calculate the system blowdown response, to FRAP-T6-B&W, used to calculate the hot rod response. Because more nodes are used in the FRAP-T6-B&W core model than in the RELAP5/MOD2 - B&W core model, the data transfer includes interpolation of the RELAP5/MOD2 - B&W fluid conditions output when the elevation of the FRAP-T6-B&W node center does not match the elevation of the RELAP5/MOD2 - B&W node center. The interpolation resulted in the conditions at the 25- and 29-inch elevations in the FRAP-T6-B&W run being different enough to result in the different heat transfer regimes.

Babcock & Wilcox Fuel Company indicated during the August 24, 1990 telephone call that this difference is not unusual for codes where arbitrary boundaries, such as nodes and heat structures, are established and used to analyze situations that are in reality continuous, such as cores and fuel rods. However, the INEL notes this situation has the potential to result in the calculation of lower and, thus, nonconservative PCTs, especially for plant analyses where comparisons to data cannot be made. Therefore, the INEL recommends that BWFC review LBLOCA EM analyses where the codes involved in the analysis use different core axial noding to determine any discrepancies in heat transfer regimes that may result from the data transfer. If needed to correct these differences, the core noding should be modified. This would especially apply to situations where the RELAP5/MOD2 - B&W analysis calculated a higher temperature than the FRAP-T6-B&W hot rod analysis at the same elevation. Also, INEL notes BWFC passes the appropriate information (fuel temperatures, gap conductance, oxide thickness, and rod dimensions) at the FRAP-T6-B&W/BEACH interface.

The LOCA EM analysis of Test S-04-6 showed that the rod quenched earlier than in the experiment (see Figure 1-2). Babcock & Wilcox Fuel Company attributed this to boiling in the downcomer that occurred in the experiment (see Figure 25-3, Reference 65) that could not be calculated by the LOCA EM analysis. Because of the lower downcomer level, the downcomer driving head and the core inventory deceased in the experiment. This caused a second heatup in the experiment that was not calculated in the LOCA EM analysis. Babcock & Wilcox Fuel Company stated that downcomer boiling could not be calculated by REFLOD3B, but the REFLOD3B results are more typical of a full-scale plant. Reference 65 describes in detail the

boiling phenomenon and agrees with BWFC's assessment regarding the cause of the second heatup and the fact that downcomer boiling is not likely to occur in a full-scale plant.

Based on the application of the LBLOCA EM to Semiscale Test S-07[illegible], the overall LBLOCA methodology, if it includes the review concerning the data transfer from RELAP5/MOD2 - B&W to FRAP-T6-B&W recommended above, is considered adequate to calculate a conservative PCT.

2.1.6 LBLOCA Compliance with 10CFR50.46

10CFR50.46 states the five acceptance criteria for emergency core cooling systems for light water reactors. In their recirculating steam generator topical report, BWFC discussed the methods by which they would show compliance with the five criteria stated in 10CFR50.46 for LBLOCAs. Each criterion and BWFC's method of showing compliance is discussed below.

1. The peak cladding temperature shall not exceed 2200°F.

   Babcock & Wilcox Fuel Company will calculate the PCT with the evaluation model described in Section 2.1.2 of this report. As noted in Section 2.1.2 of this report, the EM model met Appendix K requirements and, therefore, using this model to show the PCT does not exceed 2200°F is considered adequate.

2. The percentage of local cladding oxidation shall not exceed 17%.

   The local cladding oxidation is calculated as long as the cladding temperature remains above 1000°F and the REFLOD3B quench height correlation, a modification of the Lee quench height correlation,[73] shows the cladding location has not quenched. (See response to question 2, Reference 13.) The oxide thickness at each location is calculated on a mass basis. With respect to the temperature cutoff for cladding oxidation, BWFC noted, and INEL confirmed, the oxidation of 0.1% of the cladding would take over 8000 s at 1000°F. This is beyond the scope of core uncovery in LBLOCAs. The INEL reviewed the use of the

REFLOD3B quench height correlation to terminate the oxidation calculation. Babcock & Wilcox Fuel Company presented enough information to establish the REFLOD3B correlation is adequate to calculate the quench height in the average core and that the average core quench height is representative of the quench height in the hot channel. Based on the review of the information provided by BWFC, the INEL finds the above approach adequate to ensure the local cladding oxidation does not exceed 17%.

3. The maximum core wide oxidation should be less that 1%.

The core wide oxidation for each power shape is calculated by extending the hot channel analysis of that power shape. The amount of oxidation at any location in the core is calculated by multiplying the amount of oxidation in the hot channel, at the same elevation, by a ratio of adjusted powers (local adjusted power divided by hot channel adjusted power). Adjusted power is the power in excess of the minimum power for which any oxidation increase occurs, i.e., $P_{adjusted} = P_{local} - P_{min}$ with power in kW/ft. $P_{min}$ is the minimum power at which oxidation occurs on the hot rod. An integration over the whole core gives the core wide oxidation. The integration is performed by determining 20 or 21 curves of local oxidation versus local power level as discussed above, one for each node in the hot channel model. Based on the local power and elevation, the local oxidation for the assembly is determined. Finally, adding the local oxidation for each elevation in the assembly and all assemblies in the core gives the core wide oxidation level.

Babcock & Wilcox Fuel Company stated this method is conservative because the core power distribution is a representative power distribution that has been pushed to the peak power allowed by plant Technical Specifications. Also, the use of a power ratio to determine the oxidation for lower power zones is conservative. It is conservative because the oxidation rate increases exponentially with temperature and temperature and power are proportional. Therefore, a decrease in power would be

expected to produce a greater than proportional decrease in oxidation as is used in the LOCA EM. Based on these considerations, the INEL finds this method adequate to determine core wide oxidation in the LOCA EM.

4. The calculated changes in the core geometry shall be such that the core remains amenable to cooling.

Babcock & Wilcox Fuel Company stated that the hot rod analysis will show the PCT is less than 2200°F and the analysis will include the effects of fuel rod rupture models based on NUREG-0630. If the PCT for the hottest rod in the core is less than 2200°F with the effects of rod rupture accounted for in the analysis, then this indicates the core remained amenable to cooling at the hottest location. Because the calculated cladding temperatures at all other locations are less than the PCT, this indicates the combination of local power, coolant flow, and core geometry for these rods and bundles also remained in a geometry amenable to cooling. This method is adequate for LOCA EM analyses and the effect of PCT on core geometry. However, core geometry concerns for a combined seismic and LOCA loads event also need to be addressed in the plant-specific submittal.

5. The analysis results shall show that low core temperatures can be maintained following the initial, successful operation of the ECCS and decay heat can be removed for an extended time period.

The LOCA analysis will continue until the cladding temperatures at all locations in the core are decreasing and the core level is rising. This indicates the path to long term cooling is established because the fluid in the core will continue to rise and all locations quench without further upset. Procedures were established and approved by the NRC for the smooth transition to long term cooling. One concern is the possible concentration of boric acid in the core due to boiling of the borated ECC water in the core. To prevent this, a core throughput flow is assured by the operator. The final computation of the ECCS evaluation

model is to demonstrate the operator will take this action in a timely manner, thus, assuring effective establishment of long term cooling.

The INEL considers the description of BWFC's method of assuring long term cooling too brief to be adequately reviewed. Reference to approved procedures assumes that no changes are made to the plant operating requirements that were the basis for the NRC approval of the procedures. Unless justification can be provided that the current licensing base and procedures are still applicable to BWFC reload fuel, BWFC will need to justify the details of the long term cooling methodology on the first application of the method to a RSG plant. Information needed to justify the methodology includes but is not limited to the following items:

1. Justification of the mixing volume assumed in the core and lower plenum for the boric acid precipitation calculation. This would include justifying the assumed thermal-hydraulic conditions in the pump suction loop seal and the effect of the added resistance any water in the loop seal would have on the steam flowing to the break and the corresponding level depression in the core.

2. Justification that the water in the lower plenum does not subcool during the long term cooling analysis and cause boric acid precipitation due to the lower water temperature and solubility limit.

3. The statement was made in BWFC's response to question 22, Reference 8, that the boric acid concentration in the downcomer was allowed to deplete once the ECCS switched to sump recirculation because the boric acid in the ECC was assumed to remain in the core and not pass out into containment. The rate at which the boric acid concentration in the downcomer was allowed to deplete should be supported and justified by a suitable analysis of downcomer mixing.

4. When the switch to combined hot leg and cold leg injection is made, experimental data indicates the mixing region boric acid concentration does not begin to deplete immediately after the hot leg injection rate exceeds the core boiloff rate. The BWFC should identify how much the hot leg injection needs to exceed the core boiloff rate in order to begin depleting the the boric acid in the mixing region and justify the value given.

5. Justify the containment pressure and the containment sump temperature used in the long term cooling analysis.

## 2.2 Small Break Loss-of-Coolant Accidents

### 2.2.1 SBLOCA Method Description

The SBLOCA methodology developed by BWFC uses several computer codes to calculate the system response to a small break accident. The thermal-hydraulic response of the reactor coolant system is calculated using RELAP5/MOD2 - B&W. If RELAP5/MOD2 - B&W predicts no core uncovery, a thermal analysis is not required and compliance to 10CFR50.46 is assured. If RELAP5/MOD2 - B&W predicts uncovery, then FOAM2[74] is used to calculate the core mixture height, and the hot rod thermal response is calculated using the FOAM2 mixture height and FRAP-T6-B&W to determine PCT.

In response to questions 65 and 14, References 4 and 9, BWFC clarified the criterion used to determine whether core uncovery is calculated by RELAP5/MOD2 - B&W. They noted the FOAM2 level is estimated from the RELAP5/MOD2 - B&W collapsed liquid level by multiplying it by 1.3. When the estimated mixture level from this calculation drops below the top of the core, then repeated FOAM2 calculations are performed to track the core mixture level versus time. The 1.3 factor is based on test FOAM2 runs conducted at a 2% power level (decay heat level at about 2000 s) and 1100 psia system pressure. It neglects steam production due to primary metal heat and flashing. Therefore, BWFC concluded that as long as the power is above 2% and the system pressure is below 1100 psia, the estimated mixture level should be conservative relative to the FOAM2 calculated

mixture level. The INEL finds this approach adequate to define the calculation of core uncovery versus no core uncovery in the SBLOCA EM for the range of parameters considered. Babcock & Wilcox Fuel Company noted that the conditions used to determine the 1.3 factor were those where core uncovery is most likely to occur in RSG SBLOCAs, but the possibility of core uncovery at a pressure above 1100 psia or power below 2% could not be totally eliminated. Under these conditions, the need to determine the core mixture height with FOAM2 will be evaluated on a case specific basis. If it is not clear that the core has remained covered, BWFC stated a FOAM2 calculation will be made to accurately determine the core mixture height. In either case, the basis for the evaluation or the FOAM2 run will be documented in the BWFC calculation file and reported with the results of the evaluation. The INEL finds this approach adequate to handle cases where the conditions leading to core uncovery may be outside those used to determine the 1.3 factor.

In SBLOCA EM analyses where core uncovery occurs, the collapsed liquid level in the core, what BWFC calls the quiet water level in Reference 1, is passed from RELAP5/MOD2 - B&W to FOAM2. As noted above, FOAM2 uses this level to calculate the two-phase mixture level used in the hot rod analysis. The inventory passed from RELAP5/MOD2 - B&W to FOAM2 is defined by BWFC to include only the liquid that lies between the bottom of the lower core plate to the top of the upper core plate (see response to question 13, Reference 11). The INEL considers this definition of the quiet water level adequate to ensure that an appropriate amount of liquid is passed to FOAM2.

In their response to question 7, Reference 11, BWFC stated the pressure in the broken loop steam generator secondary (representing one steam generator) would be controlled to a pressure 100 psi higher than the intact loop steam generator secondary (representing three steam generators). This was being done to enhance the probability that, for smaller breaks where break flow does not dominate system flow, one loop seal rather than four loop seals will clear. Babcock & Wilcox Fuel Company discussed the conservatism of this approach occurs during the core boiloff uncovery period. The difference in head required to move steam from the core to the break with one versus four loop seals cleared can cause a

difference in core collapsed liquid level of up to two feet; therefore, there is the potential for a significant effect on PCT. The INEL considers the above approach an appropriate means of introducing conservatism into SBLOCA EM analyses for those break sizes where clearing one loop seal or clearing four loop seals would make a difference in the calculated PCT.

The plant models used by BWFC for EM analyses use dual flow path junction models for the vessel connections to the hot and cold legs (question 16, Reference 11). This modeling approach is not recommended by the RELAP5/MOD2 code developers because of the possibility of unphysical recirculation flows. However, BWFC stated in response to the above question that this approach will be used by BWFC in RELAP5/MOD2 - B&W plant analyses. Therefore, BWFC committed to plotting all junctions within dual flow path groupings and examining the calculated results for unphysical flow patterns. If any unphysical results are found, then corrective measures will be taken, the NRC informed of any deviations from the EM required to correct the problem, justification for the deviation provided, and NRC approval for the deviation sought. The INEL considers the above approach adequate to ensure unphysical recirculation occurring from the use of the dual flow path model will be identified and acceptable solutions found. However, the use of two junctions at the hot and cold leg connections also decouples the interaction of the steam and liquid. Therefore, BWFC should also review the results to ensure liquid draining is calculated correctly in cases where the steam flow is sufficient to impede or prevent liquid drainback. While BWFC will use the dual flow path model mostly for SBLOCA analyses, they may also use it for LBLOCA analyses. Therefore, the INEL recommends the review of the flow path groupings for unphysical results be completed whenever the dual flow path model is used (i.e., for both LBLOCA and SBLOCA analyses).

During a telephone conversation with BWFC on September 27, 1990, it became clear that the SBLOCA EM as currently proposed models the break in one side of the pipe. Because a break at the bottom of the pipe may be more limiting with respect to delivery of ECC to the reactor coolant system and vessel, BWFC should develop a model that allows representing the break at the bottom of the pipe. Other break locations that need to be considered and models developed by BWFC include a break at the top of the

pipe, which maximizes loop seal effects, and a severed injection line, which could result in the maximum ECCS degradation. These models should be developed prior to or with the first application of the SBLOCA EM.

The codes used to perform the calculations were reviewed separately from the LOCA EM methodology. Approval of the methodology is contingent upon the application of the codes in a way that is consistent with any restrictions placed on them as a result of the individual code reviews.

2.2.2 Compliance With Appendix K to 10CFR50

The required and acceptable features of a LOCA EM analysis are described in Appendix K to 10CFR50. Compliance of the BWFC methodology with Appendix K is shown in Table 2.2.2-1 which compares the Appendix K requirements and BWFC's evaluation model. Unless specifically discussed in the table or notes to the table, INEL found that BWFC's methodology met the requirements of Appendix K.

In addition to the information in Table 2.2.2-1, BWFC's SBLOCA EM referenced Tables 9-1 and 9-2 in Volume 1, Reference 1, that listed the code options and input data that must be specified for LOCA EM analyses. The INEL reviewed Tables 9-1 and 9-2 and was in agreement with BWFC regarding the required use of the models and input listed there. The following information concerns one specific entry in those tables.

Table 9-2 provided the discharge coefficients BWFC proposed for use in their SBLOCA EM analyses. The approach taken by BWFC was to change the discharge coefficient based on the break void fraction. The discharge coefficients to be used and the justification for them were discussed in response to question 5, Reference 9. This approach is not in strict agreement with Appendix K, Section I.C.d, regarding the use of the Moody critical flow model to calculate two-phase break flow. In general, the INEL considers BWFC's qualitative evaluation of this approach sufficient to justify it will result in a conservative evaluation of the SBLOCA PCT. However, BWFC should provide analyses that quantitatively justify the conservatism of the proposed approach. This should be done prior to or with the first application of the BWFC RSG LOCA EM for SBLOCAs.

Table 2.2.2-1 COMPARISON OF APPENDIX K REQUIREMENTS AND BWFC'S SBLOCA EVALUATION MODEL

| Appendix K Requirement | BWFC Evaluation Model |
|---|---|
| I.A - Use 1.02 times the licensed power level and the power distribution and peaking factors that result in the most severe consequences. | The reactor is assumed to be operating at 1.02 times the highest licensed power level. An outlet skewed power profile is used in all the SBLOCA EM codes to calculate the PCT (see Note 1). |
| I.A.1 - The initial stored energy in the fuel shall be the highest stored energy as a function of burnup and temperature. | The initial stored energy in the fuel will be calculated from the NRC approved steady state fuel rod model TACO2 or TACO3. |
| I.A.2 - Fission heat shall be calculated using reactor kinetics. Temperature and void coefficients shall be given their minimum plausible values. | Fission heat will be calculated using the point kinetics model in RELAP5/MOD2-B&W. Control rod insertion is assumed and is conservatively calculated (see Note 2). Conservative temperature and void coefficients are used (see Note 3). |
| I.A.3 - Decay heat from actinides, including plutonium and neptunium, shall be calculated. | Power from the decay of actinides is included based on the 1979 ANS standard. (See Note 4.) |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.A.4 - Fission product decay heat shall be based on 1.2 times the 1971 ANS decay heat curve. Direct moderator heating may be included but the value of the gamma energy deposited in fuel shall be justified by a suitable calculation. | Fission product decay heat is calculated using 1.2 times the 1971 ANS decay heat curve. Direct moderator heating is modeled assuming 95%/5% of the gamma energy is deposited in the fuel/coolant. (See Note 5.) |
| I.A.5 - The metal-water reaction rate shall be calculated using the Baker-Just equation. The reaction should not be steam limited and when cladding rupture occurs the Baker-Just equation will also be used to calculate the reaction rate for the inside of the cladding. Nodes in the model should be no less than 1.5 in. each way from the rupture location. | The metal-water reaction rate is calculated using the Baker-Just equation without steam limiting. The equation is applied to both cladding surfaces in the FRAP-T6-B&W analysis if rupture is calculated. The axial dimension of the ruptured location is considered to be approximately 7 in. |
| I.A.6 - Heat transfer from reactor internals should be included. | Appropriate metal heat structures are included in the system model. |
| I.A.7 - Primary to secondary heat transfer should be accounted for in evaluation models. | Primary to secondary heat transfer is calculated during all phases of the LOCA. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.B - The model should provide for predicting cladding swelling from cladding temperature and pressure differences between the inside and outside of the fuel as a function of time. The swelling and rupture models should be based on applicable data. The gap conductance model should be a function of time. | During the flow controlled part of the accident, cladding rupture models that include the effects of temperature and pressure differences are provided in the evaluation model. The models are based on NUREG-0630. The gap conductance model is based on GAPCON-THERMAL-2. During the quiescent portion of the accident, rupture is only allowed above the core mixture level. See Note 6. |
| I.C.1.a - A break spectrum study should be performed and include breaks up to double-ended breaks of the largest pipe in the primary coolant system. Include split breaks with area equal to the pipe. | For the SBLOCA break spectrum study, breaks 1.0 $ft^2$ and less will be investigated. Break location as well as break size will be studied. (See Note 7.) |
| I.C.1.b - Two-phase break flow will be calculated using the Moody model. At least three discharge coefficients ranging from 0.6 to 1.0 should be applied to the break area. The range of coefficients should be expanded if the results indicate the PCT is found at an even lower value of discharge coefficient. | Two-phase break flow will be calculated using the Moody Model. A range of discharge coefficients will not be studied because this is covered by analyzing a spectrum of break sizes. The discharge coefficient only affects the apparent break size. See Note 8. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.1.c - For cold leg breaks, all ECC injected into the inlet lines or reactor vessel during the bypass period shall be subtracted from the vessel inventory. Bypass shall continue until the mechanisms responsible for bypass are calculated not to be effective. | This provision in Appendix K is large break specific and need not be addressed by the SBLOCA EM model. The INEL agrees with this interpretation of the Appendix K ECC bypass requirement. |
| I.C.1.d - Noding near the break and ECC injection points should be detailed enough for reliable analysis. | The noding near the break isolates the break nodes from both the vessel and the pump. A single node is used for the intact loop cold leg from the ECC injection point to the vessel. This is sufficient based on studies discussed in Section 2.2.3 of this report. |
| C.2 - Pressure drops should be calculated using models that include a realistic variation of friction factor with Reynolds number and realistic two-phase friction multipliers. Acceptable two-phase multiplier models include the modified Baroczy or a combination of the Thom and Martinelli-Nelson models. | Realistic models are used to calculate the friction factor and two-phase friction multipliers. The Colebrook correlation is used to calculate the friction factor. This correlation varies the friction factor with Reynolds number. The HTFS modified Baroczy correlation is used to calculate the two-phase multiplier. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| C.3 - The momentum equation should take into account temporal change of momentum, momentum convection, area change momentum flux, momentum change due to compressibility, pressure loss from wall friction, pressure loss from area change, and gravitational acceleration. | The liquid and vapor momentum equations in RELAP5/MOD2-B&W include the appropriate momentum flux terms defined by Appendix K. |
| C.4.a,b,c,d - Critical heat flux correlations developed from appropriate steady state or transient data are acceptable. Correlations should be used within their range of applicability as specified by the authors. Acceptable steady state correlations include W-3, B&W-2, Hench-Levy, Macbeth, Barnett, and Hughes. Acceptable transient correlations include the GE correlation. | For the flow controlled portion of the accident, RELAP5/MOD2 - B&W calculates CHF with correlations listed in Appendix K (Barnett, modified Barnett (Hughes)) or approved by the NRC in the THETA1-B code (Griffith and Macbeth). The BWCMV correlation was recently approved by the NRC. During the quiescent portion of the accident, FRAP-T6-B&W evaluates CHF if core uncovery is calculated. In addition to the above correlations, FRAP-T6-B&W uses the W-3 correlation from Appendix K. The minimum CHF is 90,000 Btu/h-ft$^2$. |
| I.C.4.e - Once CHF has been calculated, return to nucleate boiling should be locked out until the reflood portion of the LOCA. | During the quiescent portion of the transient, return to nucleate boiling is allowed upon refilling the core even if CHF was calculated. See Note 9. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.5.a,b - Transition and film boiling correlations should be compared to applicable steady state and transient data to demonstrate the heat transfer coefficient calculated is less than or equal to the mean value of the applicable data. Acceptable film boiling correlations include the Groeneveld, Dougall-Rohsenow, and Westinghouse correlations. The McDonough, Milich, and King transition boiling correlation is acceptable. Transition boiling cannot be reapplied for the remainder of the blowdown once the cladding superheat exceeds 300°F. | For the flow controlled portion of the accident, the McDonough, Milich, and King transition boiling correlation listed in Appendix K is used. Film boiling is calculated using the Condie-Bengston correlation (see Note 10). For the quiescent portion of the accident, heat transfer above the mixture level is calculated in FRAP-T6-B&W using the same correlations listed above. See Note 11. |
| I.C.6 - Pump modeling should be based on a dynamic model that includes momentum transfer to the fluid with variable pump speed as a function of time. Pump resistance should be justified. Two-phase pump performance should be verified. | The pump model is based on homologous relationships and includes single- and two-phase performance characteristics. Plant-specific pump performance tables are given as model input using best-estimate values. Two-phase degradation is modeled conservatively. This data is based on measured data for scaled pumps, vendor homologous curves, or pumps with similar specific speeds. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.C.7.a,b - The flow through the hot region shall be calculated as a function of time and the hot region shall not be greater than one fuel bundle. Core flow in the hot region should include cross flow and flow blockage. The flow should be smoothed to remove oscillations with a period less than 0.1 s. The core flow from the blowdown analysis shall be used in the hot channel heatup analysis. | The system model includes a separate channel representing the hot assembly with the rest of the core as the average channel. Flow diversion effects are not calculated in the BWFC SBLOCA EM model. However, the effect of rupture on the heat transfer calculation is included in the FRAP-T6-B&W evaluation of the hot rod. During the quiescent portion of the accident, cladding rupture is allowed but only above the mixture level. See Note 12. Note: Flow smoothing is not included in the BWFC model, but this requirement is for LBLOCAs. |
| I.D.1 - Single failure of the ECCS should be chosen to maximize the calculated PCT. | The SBLOCA single failure is the loss of one train of pumped ECCS injection. |
| I.D.2 - Conservative containment pressures should be used and should include the effects of installed pressure reduction systems and processes. | Containment pressure will be based on the highest previously reported containment pressure from any event evaluated in the FSAR. See Note 13. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.D.3 - Refill and reflood rates should be based on acceptable data, such as FLECHT data. The pump rotor should be modeled in the condition (locked or free spinning) leading to the highest calculated PCT. The effects on the reflood rate of the accumulator cover gas should be accounted for in the analysis. | The system response during a SBLOCA is analyzed with RELAP5/MOD2-B&W. The carryover rate fraction and pump rotor requirements are considered LBLOCA reflood specific by BWFC and INEL agrees. The pumps are assumed to be on or off depending on the case analyzed. Accumulator cover gas and the effects of all noncondensible gases are neglected in SBLOCA EM analyses. See Note 14. |
| I.D.4 - Interaction between steam and the ECC water shall be taken into account. | SBLOCAs are analyzed using the nonequilibrium control volume option in RELAP5/MOD2 - B&W. Thus, the steam/ECC water interaction is calculated directly by the code. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| I.D.5 - Refill and reflood heat transfer for reflood rates greater than 1 inch/s shall be based on FLECHT data for unblocked cores. For reflood rates less than 1 inch/s, heat transfer shall be based on steam cooling only and shall account for flow blockage due to cladding swell or rupture. | If core uncovery is calculated, the rods are cooled by steam cooling above the mixture level. As the core recovers, the rods are cooled by film boiling until nucleate boiling can be established, i.e., transition boiling is not calculated. Note: The INEL considers the requirements of Appendix K (discussing reflood rates greater than or less than 1 in/s) to be applicable to LBLOCAs. The BWFC SBLOCA EM model is considered adequate to meet the intent of Appendix K for SBLOCAs. |
| II.1.a - A detailed description of the evaluation model provided. | BWFC provided model documentation for each code used in their evaluation model. The documentation is listed in Note 15. |
| II.1.b - Changes to the evaluation model (transient cladding temperature differences of greater than 50°F) shall be documented. | Changes that result in a deviation of greater than 50°F in the cladding temperature transient will be documented. |
| II.1.c - A listing of each computer program shall be furnished to the NRC. | BWFC will provide the computer program listing to the NRC upon request. |

Table 2.2.2-1 (continued)

| Appendix K Requirements | BWFC Evaluation Model |
|---|---|
| II.2 - Solution convergence shall be demonstrated by noding and time step studies. | Sensitivity studies for nodalization and time steps were presented. See Section 2.2.3. |
| II.3 - Sensitivity studies shall be performed for each evaluation model to evaluate the effects of noding, predominate phenomena, assuming pump operation or a locked rotor, and values of parameters over applicable ranges. | Appropriate sensitivity studies were done. See Section 2.2.3. Additional studies that must be done for a specific plant category will be provided as part of the category specific submittal. |
| II.4 - Comparisons to data to be made where applicable. | Code/data benchmarks were provided in each code topical report. |
| II.5 - Model acceptability is based on the adequacy of calculational methods and margin of conservatism. | |

Notes to Table 2.2.2-1:

1. The specific outlet skewed power profile to be used in the SBLOCA EM model was provided by BWFC in Reference 4, question 46. The power profile uses an axial peak of 1.4 at the 10 ft. core elevation. This power shape was taken from the Catawba and McGuire FSARs. Boiloff evaluations will use this power shape with the average core radial peak for inventory loss and steam flow (RELAP5/MOD2 - B&W and FOAM) and the hot channel calculations will use this shape with a radial peak sufficient to match the plant technical specification limit on total peaking at the 10 ft. elevation. This is considered adequate.

2. Babcock & Wilcox Fuel Company stated (response to question 47, Reference 4) that rod worth and insertion times are plant-specific and validated for each fuel reload. The worth of the control rods over the fuel cycle rod is calculated and compared to those in the safety analysis report to assure the calculated values are larger than those used in the analysis. Insertion times are verified by an actual test that is performed at the plant prior to return to power after each refueling. For SBLOCA analyses, these values should be justified in the plant-specific submittals.

3. Babcock & Wilcox Fuel Company stated (response to question 47, Reference 4) that moderator void and Doppler reactivity coefficients are plant-specific and validated for each fuel reload. These parameter are the same as those used for the LBLOCA evaluation for the plant and fuel being licensed. The LBLOCA data will be justified on a plant-specific basis with each application of the LOCA EM model.

4. See note 3, Table 2.1.2-1.

5. See note 4, Table 2.1.2-1.

6. Based on BWFC's response to question 49, Reference 4, cladding swell and rupture for SBLOCAs is calculated using FRAP-T6-B&W. When rupture is calculated the cladding will be expanded, the fuel gap coefficient decreased, the cladding surface area expanded according to the rupture

strain, the cladding and oxide layers thinned, and the metal water reaction will be calculated on both cladding surfaces.

7. In response to question 52, Reference 4, BWFC stated that the SBLOCA spectrum depends on the particulars of plant design and, therefore, must be defined on a plant-specific basis. Guidelines used to determine the break sizes to be analyzed were discussed. These included examining the interaction of break size, break location, steam generator effects, ECCS performance, and design. At least one break size should be analyzed which depends on each of the major ECC systems at the time of minimum core inventory and PCT. This would include a larger break cooled by HPI, accumulators, and low pressure injection (LPI); a smaller break that does not depressurize to the LPI pressure; and a very small break cooled by only the HPI system. The effects of the steam generators and their importance to system energy removal will also be considered. This approach is considered adequate to define the SBLOCA break spectrum. However, as discussed in Section 2.2.1 of this report, INEL also considers break orientation (top, bottom, etc.) an important parameter for study.

8. For SBLOCAs, BWFC intends to investigate the break spectrum by varying the break size as discussed in Note 7. For each break size, BWFC has proposed using a specific set of break discharge coefficients that depend on the break void fraction. The specific values are given in BWFC's response to question 5, Reference 9. Although the INEL considers BWFC's qualitative evaluation of this approach sufficient to justify it will result in a conservative evaluation of the SBLOCA PCT, BWFC should provide analyses that quantitatively justify the conservatism of the proposed approach. This should be done prior to or with the first application of the BWFC RSG LOCA EM for SBLOCAs.

9. The BWFC RSG LOCA EM does not require lockout of return to nucleate boiling for SBLOCAs. This requirement has been interpreted by the NRC to be LBLOCA specific and not applicable to SBLOCAs.

10. Use of the Condie-Bengston film boiling correlation for evaluation model use was approved in the review of RELAP5/MOD2 - B&W.

11. The BWFC SBLOCA EM model does not require a lockout of transition boiling once the cladding superheat exceeds 300°F. This is not a concern because NRC has interpreted this as applying to only LBLOCAs.

12. Babcock & Wilcox Fuel Company discussed their position on the need to model flow diversion effects in the SBLOCA EM in response to question 53, Reference 5, and question 55, Reference 4. Babcock & Wilcox Fuel Company stated the effects of flow blockage and diversion due to cladding swell and rupture are not calculated in the SBLOCA EM model. However, BWFC noted the model uses average channel steaming rates in the hot rod analysis; this is conservative because, with the higher power associated with the hot channel, hot channel steam flow rates would be higher than those in the average core. Test data from FLECHT-SEASET 21 rod flow blockage tests[75] conducted at low Reynolds numbers was cited to justify BWFC's position. The measured heat transfer coefficients for the tests with blockage were compared to measured heat transfer coefficients for the tests without blockage. The effect of blockage on the heat transfer coefficient was a positive one. In a call on November 16, 1990, BWFC noted that even though the RELAP5/MOD2 -B&W includes a separate hot channel, the SBLOCA EM still uses the average channel steam flow rate in the hot rod analysis. The INEL reviewed the data presented by BWFC and concluded the data was sufficient to support BWFC's position; however, to verify the conservatism in the BWFC SBLOCA EM model, the EM analysis of Semiscale Test S-LH-1 was also reviewed (see question 11, Reference 13). This analysis provided an example of the application of the BWFC SBLOCA EM. This analysis showed the SBLOCA EM was able to overpredict the measured PCT by approximately 800°F. This supports the above conclusion regarding the adequacy of BWFC's approach to calculating the SBLOCA thermal-hydraulic core response.

13. Babcock & Wilcox Fuel Company stated in response to question 62, Reference 8, that the containment pressure does not affect the calculated system response except for the largest SBLOCAs that depressurize to near the containment pressure. In SBLOCA EM analyses, the containment pressure will be held constant at the highest pressure calculated for any event evaluated in the FSAR (this may not even be

a LOCA event). Babcock & Wilcox Fuel Company stated this pressure would result in an underprediction of the low pressure ECCS, which is the most important concern. Babcock & Wilcox Fuel Company noted the use of a lower containment pressure could affect the SBLOCA analysis by extending the blowdown to the point that the primary system inventory is decreased. They noted this occurs when the system inventories are controlled by flow and entrainment processes in the cold legs and downcomer, which occurs in a LBLOCA, and is one of the criterion for determining the division between large and small breaks. Therefore, BWFC concluded the containment pressure selected for a SBLOCA should not be skewed for the purpose of enhancing liquid entrainment. The INEL agrees with the BWFC approach with the following condition. If the PCT in a SBLOCA evaluation occurs after the primary pressure reaches the containment pressure, BWFC should justify that conditions at the break and in the cold leg are such that extended primary system blowdown and/or liquid entrainment could not significantly change the system inventory and PCT or repeat the analysis using the LBLOCA methodology.

14. Babcock & Wilcox Fuel Company discussed their position on the inclusion of noncondensible gases in SBLOCA analyses in the response to question 4, Reference 76. This response discussed compliance of RELAP5/MOD2 - B&W to NUREG-0737, Item II.K.3.30.[77] Acceptance of this response by the INEL and NRC is documented in the Safety Evaluation Report on RELAP5/MOD2 - B&W, Reference 18.

15. The documentation for the codes used in the recirculating steam generator SBLOCA evaluation model was provided in the following BWFC code topical reports:

| | | |
|---|---|---|
| a. | RELAP5/MOD2 - B&W | BAW-10164P |
| b. | FRAP-T6-B&W | BAW-10165P |
| c. | FOAM2 | BAW-10155P |
| d. | Overall EM | BAW-10168P, Rev. 1 |

### 2.2.3 Sensitivity Studies

Babcock & Wilcox Fuel Company's response to question 11, Reference 13, contained a number of SBLOCA sensitivity studies. This section describes those studies as the results apply to meeting Appendix K requirements found in Items I.C.1.d, II.2, and II.3. The sensitivity studies were performed with a model used to analyze Semiscale Test S-LH-1.

2.2.3.1 Time Step Studies. A time step study was provided for RELAP5/MOD2 - B&W. The RELAP5/MOD2 - B&W study was presented in BWFC's response to question 11, Reference 13.

In the RELAP5/MOD2 - B&W study, the time step in the base case, which was the SBLOCA EM case in Reference 13, was cut in half and the analysis rerun with the smaller time step. Comparison of the two calculations showed the two runs were essentially the same. One difference noted by BWFC was that the larger time step used in the SBLOCA EM analysis resulted in two or three surges of liquid into the vessel followed by liquid expulsion right after the minimum core inventory was achieved. Babcock & Wilcox Fuel Company stated they did not expect this behavior in a full scale plant. However, if this behavior is noted in an EM plant analysis, BWFC stated a time step study will be conducted to assure that the vessel liquid inventory is not reduced by the use of a smaller time step. Based on the overall results of the time step study and BWFC's commitment regarding the time step study for the full size plant, the INEL concludes the time steps used in the SBLOCA EM calculation for Test S-LH-1 are adequate for use in SBLOCA EM plant analyses. Therefore, those time steps should be used in SBLOCA EM plant analyses unless adequate justification for the use of other time steps is provided.

Information on the time steps to be used with FRAP-T6-B&W in SBLOCA analyses was not provided by BWFC. The following time steps for FRAP-T6-B&W were justified by BWFC for LBLOCAs (see Section 2.1.3.1 of this report):

| Time Period Description | Time Period (s) | Time Step (s) |
|---|---|---|
| Blowdown period | 0.0 - 20.0 | 0.01 |
| Post blowdown | beyond 20.0 | 0.2 |

Because SBLOCAs are slower developing transients, the FRAP-T6-B&W time steps used for the post-blowdown period of the LBLOCA should be sufficient for SBLOCA analyses. Therefore, unless BWFC justifies other time step controls in the plant-specific submittals, a time step of 0.2 s should be used to analyze core uncovery PCTs in SBLOCAs.

2.2.3.2 Loop Nodalization Studies. A nodalization study was performed for PELAP5/MOD2 - B&W. The results were provided in BWFC's response to question 11, Reference 13, on the analysis of Semiscale Test S-LH-1. In this study, a model that did not represent the BWFC SBLOCA EM nodalization (see Figure 11-2, Reference 13) was converted to the SBLOCA EM nodalization as closely as practicable (see Figure 11-2a, Reference 13). Specific changes to the original model to convert it to the EM nodalization include: (a) reducing the number of nodes in the pressurizer surge line, hot legs, pump suctions, and vessel downcomer; (b) increasing the number of nodes in the steam generator U-tubes and core; and (c) adding dual flow path modeling at the junctions representing the hot leg connections to the vessel. Figure 11-2a differs from the EM nodalization mainly in the area of the cold legs and upper downcomer where specific models were developed to analyze Test S-LH-1 and in the core where a single channel rather than a dual channel core model was used. Analyses completed with the two nodalizations allow the effects of nodalization on the calculated results for Semiscale Test S-LH-1 to be identified.

The results of the study showed the nodalization changes had little effect on the calculated results. Only minor changes were noted for the two nodalizations. Based on these results, the SBLOCA EM nodalization is adequate.

2.2.3.3 Break Nodalization Study. Although a break nodalization study specific to SBLOCAs was not provided, the SBLOCA EM analysis of Semiscale Test S-LH-1 used a nodalization in the broken loop cold leg

consistent with the SBLOCA EM nodalization (four nodes). Based on the adequate analysis of Test S-LH-1 with this nodalization, the four node model is adequate for SBLOCA EM analyses.

2.2.3.4 Core Noding Study. The RELAP5/MOD2 - B&W axial core nodalization was studied using the Semiscale Test S-LH-1 analyses. The base model had 6 core nodes and the EM model had 20 core nodes (see Figures 11-2 and 11-2a, Reference 13). This study showed there were minimal differences between the two analyses. Based on this result, the 20 node core is adequate for SBLOCA EM calculations.

2.2.3.5 Radial Fuel Segmentation Study. The number of radial segments used to model the fuel rod in FRAP-T6-B&W was not studied specifically for SBLOCAs. However, because SBLOCAs develop more slowly, the LBLOCA sensitivity study reported in Section 2.1.3.10 of this report is also considered to apply to SBLOCAs. Based on that study, use of the seven segment model in SBLOCA EM calculations is adequate.

2.2.3.6 Loop Seal Clearing Study. The RELAP5/MOD2 - B&W TER requested the loop seal clearing model in the code be verified by requiring BWFC to perform a loop seal nodalization sensitivity study as part of the review of the RSG LOCA EM. This study was to show that the loop seal nodalization chosen by B&W for use in LOCA licensing calculations results in the highest PCT and that liquid is retained in the loop seal when steam flow conditions would result in liquid retention. The request provided BWFC with the option to demonstrate RELAP5/MOD2 - B&W correctly calculates loop seal phenomena, including cases where liquid was retained in the loop seal for low steam flow conditions. The INEL does not consider the information provided by BWFC in the LOCA EM review (response to question 7, Reference 11) adequate to resolve this issue. Because loop seal effects are important for SBLOCAs, especially for Westinghouse plants with deep loop seals, BWFC should complete a study to verify the adequacy of the loop seal clearing model in RELAP5/MOD2 - B&W prior to or with the first application of the SBLOCA EM. The study should show for a range of pressures and steam flows consistent with the pressure and decay heat in the system at the time of loop seal clearing that the models in

RELAP5/MOD2 - B&W correctly calculate loop seal clearing. That is, liquid is retained in the loop seal when steam flow conditions would result in liquid retention. The models in RELAP5/MOD2 - B&W should also be compared to entrainment data to verify the code calculates entrainment correctly. A suggested reference for flow regime development in large diameter pipes is work by Kataoka and Ishii, Prediction of Pool Void Fraction by New Drift Flux Correlation, NUREG/CR-4657, ANL-86-29, June 1986.

2.2.4 PLANT MODELS

In References 1 and 13, BWFC presented the plant nodalizations to be used in the LOCA EM methodology for RELAP5/MOD2 - B&W and FRAP-T6-B&W. The FOAM2 code was reviewed separately. The following sections discuss the nodalizations and the bases for their acceptability.

2.2.4.1 RELAP5/MOD2 - B&W Plant Nodalization. The loop nodalization to be used in the RELAP5/MOD2 - B&W system analysis is shown in Figure 2a, Reference 13. The intact loop represents the two or three unbroken loops combined while the broken loop represents the one loop containing the break. As discussed in Section 2.2.3.2 of this report, BWFC sensitivity studies showed this nodalization was detailed enough to ensure a converged solution. Therefore, it is adequate for LOCA EM analyses.

The vessel nodalization is shown in Figure 2b, Reference 13. The core is represented by 20 axial nodes and two channels (one channel represents the single hot assembly and the other the average core). A dual channel core was not used in BWFC's analysis of Test S-LH-1. However, based on the adequate representation of the Semiscale system response to Test S-LH-1 (see question 11, Reference 13) with a single channel core model using 20 axial nodes and Appendix K requirements for a including a hot channel, the dual channel, 20 axial node core model is considered adequate.

The core crossflow resistance input to RELAP5/MOD2 - B&W by BWFC for small breaks is the same as that used for large breaks (see Section 2.1.3.8 of this report). This is considered adequate as the crossflow resistance should not depend on the break size.

2.2.4.2 FRAP-T6-B&W Hot Rod Nodalization. The information supplied by BWFC did not clearly identify a specific FRAP-T6-B&W nodalization for SBLOCAs. However, INEL considers the LBLOCA nodalization to also be adequate for SBLOCAs. Therefore, the FRAP-T6-B&W nodalization for LBLOCAs should be used for SBLOCA analyses unless another nodalization is justified. The nodalization to be used with this code for LBLOCAs was discussed in Section 2.1.3.2 of this report.

2.2.5 Application of the RSG SBLOCA EM

Babcock & Wilcox Fuel Company provided an example of the application of the SBLOCA EM in response to question 11, Reference 13. The application was an analysis of Semiscale Test S-LH-1. Test S-LH-1 was a 5% small break experiment with core uncovery during the loop seal clearing process and the core boiloff period. Thus, the analysis of this experiment required the use of all the SBLOCA EM codes to determine the PCT. Based on an earlier assessment of Test S-LH-1 with a slightly different nodalization (see question 5, Reference 78), the INEL concluded in the RELAP5/MOD2 - B&W TER that RELAP5/MOD2 - B&W adequately calculated the system thermal-hydraulic response during Test S-LH-1. The RELAP5/MOD2 - B&W analysis of Test S-LH-1 presented by BWFC in response to question 11, Reference 13, was similar to the earlier analysis with respect to the system thermal-hydraulic behavior. With respect to the PCT, the calculated PCT was approximately 800°F higher than the measured PCT during the core boiloff period (see Figure 11-19, Reference 13). In their response to question 11, BWFC noted most of the conservatism in the calculated PCT results from the definition of quiet water level passed from RELAP5/MOD2 - B&W to FOAM2 for use in the calculation of the two-phase level when core uncovery is calculated (see Section 2.2.1 of this report). Based on the application of the SBLOCA EM to Semiscale Test S-LH-1, the overall methodology is adequate to calculate a conservative PCT.

2.2.6 SBLOCA Compliance with 10CFR50.46

10CFR50.46 states the five acceptance criteria for emergency core cooling systems for light water reactors. In their recirculating steam

generator t pical report, BWFC discussed the methods by which they would show compliance with the five criteria stated in 10CFR50.46. Each criterion and BWFC's method of showing compliance is discussed below.

1. The peak cladding temperature shall not exceed 2200°F.

   Babcock & Wilcox Fuel Company will calculate the PCT with the evaluation model described in Section 2.2.2. As noted in Section 2.2.2, this model met Appendix K requirements and, therefore, using this model to show the PCT does not exceed 2200°F is adequate for LOCA EM analyses.

2. The percentage of cladding oxidation shall not exceed 17%.

   The oxide thickness at each location where a temperature is determined is computed and divided by the cladding thickness at each location. The cladding thickness is defined to be the as calculated strained thickness, circumferentially averaged. The 10CFR50.46 criterion is met if the highest fraction calculated is less than 0.17. This method is adequate to satisfy the acceptance criterion of 10CFR50.46.

3. The maximum core wide oxidation should be less that 1%.

   Babcock & Wilcox Fuel Company discussed the method of complying with this requirement in response to question 56, Reference 4. Babcock & Wilcox Fuel Company stated the whole core oxidation evaluation will be performed for any SBLOCA that results in a peak local oxidation rate of more than 5%. This criterion is based on the results of the LBLOCA analysis in Reference 1. This study showed the core wide oxidation was 0.46% with a local oxidation peak of 4.7%. Babcock & Wilcox Fuel Company noted the criterion is conservative for two reasons. First, the LBLOCA study generated 0.46% core wide oxidation with a peak local oxidation of 4.7%. This indicates local oxidation would need to reach 6.0% before core wide oxidation would reach 1%. Second, more of the core is exposed to temperature excursions for a

LBLOCA than for a SBLOCA because for the latter most of the core is covered by mixture. Compliance to this criterion will be shown by demonstrating the degree of oxidation is bounded by the LBLOCAs or directly using the techniques discussed in Section 2.1.6, item 3. The INEL considers BWFC's approach adequate to meet this criterion.

4. The calculated changes in the core geometry shall be such that the core remains amenable to cooling.

Babcock & Wilcox Fuel Company stated that the hot rod analysis will show the PCT is less than 2200°F and the analysis will include the effects of fuel rod rupture models based on NUREG-0630 If the PCT for the hottest rod in the core is less than 2200°F with the effects of rod rupture accounted for in the analysis, then this indicates the core remained amenable to cooling at the hottest location. Therefore because the calculated cladding temperatures at all other locations are less than the PCT, this indicates the combination of local power, coolant flow, and core geometry for these rods and bundles also remained in a geometry amenable to cooling. This method is adequate for LOCA EM analyses and the effect of PCT on core geometry. However, core geometry concerns for a combined seismic and LOCA loads event also need to be addressed in the plant-specific submittal.

5. The analysis results shall show that low core temperatures can be maintained following the initial, successful operation of the ECCS and decay heat must be removed for an extended period of time.

The LOCA analysis will continue until the cladding temperatures at all locations have decreased to the coolant saturation temperature, and the core is covered. This indicates the path to long term cooling is established. The core temperature is maintained within a few degrees of the coolant saturation temperature through a continuous flow of ECCS water. Procedures

were established and approved by the NRC for the smooth transition to long term cooling. One concern is the possible concentration of boric acid in the core due to boiling of the borated ECC water in the core. To prevent this, a core throughput flow is assured by the operator. Babcock & Wilcox Fuel Company stated the timing of this operator action for SBLOCAs is bounded by the timing for LBLOCAs, and the LBLOCA analysis will be used to satisfy this requirement for SBLOCAs.

The INEL considers the description of BWFC's method of assuring long term cooling too brief to be adequately reviewed. Reference to approved procedures assumes that no changes are made to the plant operating requirements that were the basis for the NRC approval of the procedures. Unless justification can be provided that the current licensing base and procedures are still applicable to BWFC reload fuel, BWFC will need to justify the applicability of the long term cooling methodology on the first application of the method to a RSG plant. Information needed to justify the methodology includes but is not limited to sufficient information to justify the LBLOCA long term cooling analysis, in all aspects of the analysis, is bounding for SBLOCAs with respect to boric acid precipitation.

If BWFC performs a plant-specific SBLOCA long term cooling analysis, information needed to justify the methodology includes but is not limited to the following items:

1. Justification of the mixing volume assumed in the core and lower plenum for the boric acid precipitation calculation. This would include justifying the assumed thermal-hydraulic conditions in the pump suction loop seal and the effect of the added resistance any water in the loop seal would have on the steam flowing to the break and the corresponding level depression in the core.

2. Justification that the water in the lower plenum does not subcool during the long term cooling analysis and cause boric acid precipitation due to the lower water temperature and solubility limit.

3. Clarify what procedures and safeguards are in place to prevent a depressurization of the primary system (such as the accidental or intentional opening of the PORV by an operator) that would cause boric acid precipitation.

4. If the plant has only one residual heat removal (RHR) system, clarify how the operator provides long term cooling and prevents boric acid precipitation.

5. Clarify how the operator decides whether to refill the primary system and restore single-phase natural circulation to provide core flushing and dispersal of boric acid throughout the primary system or use alternate methods of boric acid dispersal. Clarify what instrumentation is available to the operator to determine primary system pressure and temperature and the procedures the operator uses to determine which method to use for long term cooling.

## 3. REFLOD3B, REVISION 3

In Reference 14, BWFC submitted for review Revision 3 of the REFLOD3B code topical report, BAW-10171P. Revision 3 made two changes over the approved version of the report: (a) addition of a GSTOP to close the vessel side leak and (b) addition of an option to allow only saturated steam flow through the vent valve model. Review of these changes was included in the RSG LOCA EM review. The changes and the INEL recommendations concerning them are discussed below.

### 3.1 Vessel Side Leak GSTOP

This GSTOP can be used to close the vessel side leak path after the accumulator empties. According to the REFLOD3B, Revision 3, topical report this will be done only if the vessel side leak flow is nearly zero and the numerical solution is having difficulty (excessive computer run time) achieving the zero flow condition. When this GSTOP becomes active, the pressure in the downcomer steam region, R4, is set equal to the containment pressure and the rate of change of mass and energy in R4 are set to zero. By closing the leak path, the above changes move the containment pressure boundary condition to region R4.

The INEL finds the above change a reasonable approach to solving run time difficulties when the differential pressure between the primary system and containment is small enough that the flow out the leak is small and causing run time problems. Therefore, it is recommended this change be accepted. However, whenever this option is applied, BWFC should document its use in the applications report and justify the move in the containment boundary condition to region R4 does not affect the PCT by more than 50°F.

### 3.2 Vent Valve Steam Flow Only Option

One option available to the REFLOD3B user is to represent leakage paths from the upper plenum and upper head to the downcomer in RSG plants with the vessel vent valve model. In this situation, only steam would pass

through the vent valve. Babcock & Wilcox Fuel Company added the vent valve steam flow only option so that when the vent valve model was used to represent leakage paths from the upper head to downcomer in RSG plants, only saturated steam would flow through the vent valve.

The INEL agrees with BWFC that the steam flow only model is appropriate for use in this situation. Revision 3 of the REFLOD3B code topical was reviewed, and BWFC appears to have made the appropriate changes to implement the model. Therefore, it is recommended this change be accepted.

## 4. CONCLUSIONS

The RSG LOCA EM submittal by BWFC was reviewed to determine the LOCA EM's compliance with 10CFR50.46 and Appendix K. Based on this review, it is recommended the LOCA EM be approved for use in LOCA licensing analyses with the following comments and restrictions:

1. The RSG LOCA EM should be applied consistent with any restrictions placed on the individual codes used in the EM as a result of the individual code reviews.

2. The following items need to be addressed by BWFC in the plant-specific submittals:

   a. Clarify whether the initial rod pressure is taken from the fuel code or a bounding pressure is used. The approach used will be justified in the plant-specific submittals.

   b. Data to be used in EM analyses for the fuel temperature and moderator void reactivity coefficients will be provided and justified with each application of the LBLOCA and SBLOCA EM.

   c. Effect of pump trip will be evaluated on the first application of the LBLOCA EM model for each plant type.

   d. The containment pressure for the LBLOCA analyses will be provided and justified on a plant-specific basis.

   e. The ECCS system single failure applied to the LBLOCA EM calculation will be justified on a plant-specific basis.

   f. The values used in the REFLOD3B calculations for the fuel and cladding conductivities and the gap heat transfer coefficient used will be justified in the plant-specific applications.

   g. The effects of the maximum pumped ECCS versus the minimum pumped ECCS will studied on a plant-specific basis. This study will be

provided to the NRC for review with BWFC's justification for the pumped ECCS flow rates used in the LBLOCA EM analyses.

h. Justification should be provided in the LBLOCA EM plant-specific submittal that an EOL blowdown rupture will not occur if an EOL calculation is not supplied. Also, the core crossflow resistance study is not considered valid for blowdown ruptures.

i. Babcock & Wilcox Fuel Company noted the REFLOD3B nodalization discussed in Appendix A of BAW-10168P, Rev. 1, was only valid for plants with a one-to-one correspondence between hot legs and cold legs. Therefore, a separate study will be completed prior to the first application of the LBLOCA EM to a CE 2 x 4 plant to confirm the nodalization scheme.

j. For SBLOCAs, BWFC noted the possibility of core uncovery at a pressure above 1100 psia or power below 2% cannot be totally eliminated. Under these conditions, the evaluation of the need to determine the core mixture height with FOAM2 will be made on a case specific basis. If it is not obvious that the core has remained covered from the RELAP5/MOD2-B&W analysis using the 1.3 factor discussed in Section 2.2.1 of this report, then a FOAM2 calculation will be made to accurately determine the core mixture height. In either case, the basis for the evaluation or the FOAM2 run will be reported with the results of the evaluation.

k. For SBLOCAs, rod worth and insertion times are plant-specific and validated for each fuel reload. The values used in SBLOCA analyses will be justified in the plant-specific submittals.

l. Unless justification can be provided that the current licensing base and procedures are still applicable to BWFC reload fuel, BWFC will need to justify the details of the long term cooling methodology on the first application of the method to a RSG plant. If a plant-specific long term cooling analysis needs to be performed by BWFC, the analysis methods and results will be justified (see Sections 2.1.6 and 2.2.6 of this report).

m. In response to question 4, Reference 4, BWFC credited control rod insertion after the end-of-blowdown in justifying their approach to calculating fission power during the refill and reflood portions of a LBLOCA. Because the NRC Standard Review Plan[79] requires an analysis justifying control rod insertion at the time it is credited in the analysis, BWFC should justify on a plant-specific basis the control rod insertion credited in the RSG LOCA EM. This should include justification that the core and upper vessel geometry will permit control rod insertion at the time credited in the analysis.

n. In the RELAP5/MOD2 - B&W SBLOCA time step study, the larger time step resulted in two or three surges of liquid into the vessel followed by liquid expulsion right after the time of minimum core inventory. Although BWFC does not expect this behavior in a full scale plant, if this behavior is noted in an EM plant analysis, BWFC stated a time step study will be conducted to assure that the vessel liquid inventory is not reduced with a smaller time step. Based on the above, the INEL concludes the time steps used in the Test S-LH-1 SBLOCA EM calculation are adequate for use in SBLOCA EM plant analyses. Therefore, those time steps should be used unless other time steps are justified.

o. The plant models used by BWFC for SBLOCA EM analyses use dual flow path junction models for the vessel connections to the hot and cold legs. This modeling approach may also be used for LBLOCA EM analyses. This type of modeling is not recommended by the RELAP5/MOD2 code developers because of the possibility of unphysical recirculation flows. However, BWFC stated in response to question 16, Reference 11, that this approach will be used in RELAP5/MOD2 - B&W plant analyses. Therefore, BWFC committed to plotting all junctions within dual flow path groupings and examining the calculated results for unphysical flow patterns. If any unphysical results are found, corrective measures will be taken, justification for any EM deviation provided, and NRC approval for the deviation sought. The INEL considers the above

approach adequate to ensure unphysical recirculation occurring from the use of the dual flow path model will be identified and acceptable solutions found. However, the use of two junctions at the hot and cold leg connections also decouples the interaction of the steam and liquid. Therefore, BWFC should also review the results to ensure liquid draining is calculated correctly in cases where the steam flow is sufficient to impede or prevent liquid drainback. Finally, the INEL recommends: (a) the review be completed for LBLOCA analyses when dual flow path junction models are used and (b) the results of the review be presented in the applications report.

p. The proposed method to meet Criterion 4 of 10CFR50.46 is adequate for LOCA EM analyses and the effect of PCT on core geometry. However, core geometry concerns for a combined seismic and LOCA loads event also need to be addressed in the plant-specific submittal.

3. The REFLOD3B SER required the parameter $C_{sup} \geq 1.05$ in REFLOD3B analysis. The BWFC RSG LOCA EM required the use of $C_{sup} \geq 1.025$. Babcock & Wilcox Fuel Company provided sufficient information regarding this difference so that the INEL recommends that $C_{sup} \geq 1.025$ be accepted.

4. Table 9-2, Reference 1, provided the discharge coefficients BWFC proposed for use in their SBLOCA EM analyses. The approach taken was to change the discharge coefficient based on the break void fraction. This approach is not in strict agreement with Appendix K, Section I.C.d, regarding the use of the Moody critical flow model to calculate two-phase break flow. However, INEL considers BWFC's qualitative evaluation of this approach sufficient to justify the SBLOCA PCT will be conservatively evaluated. But, BWFC should provide analyses that quantitatively justify the conservatism of the proposed approach. This should be done prior to or with the first application of the BWFC RSG LOCA EM for SBLOCAs.

5. If the PCT in a SBLOCA analysis occurs after the primary and containment pressures equalize, BWFC should justify that break and cold leg conditions are such that extended primary system blowdown and/or liquid entrainment could not significantly change the system inventory and PCT or repeat the analysis using the LBLOCA methodology.

6. Babcock & Wilcox Fuel Company's EM does not model flow blockage due to prerupture cladding strain in RELAP5/MOD2 - B&W and BEACH. Babcock & Wilcox Fuel Company provided information to justify their position. Based on the INEL's review of BWFC's responses and additional data on zircaloy cladding behavior during ballooning and rupture, INEL recommends BWFC's approach concerning prerupture cladding swell be accepted. However, BWFC would need to justify the acceptability of any licensing analyses where cladding swell exceeded 20% but rupture was not calculated.

7. Babcock & Wilcox Fuel Company stated, in Reference 8 in response to question 27a, that the base case bundle crossflow resistance will be used for 17 x 17 bundles. For other bundle designs (14 x 14, 15 x 15, or 16 x 16), the crossflow resistance will be updated. The response provided to question 27a, seemed to indicate the crossflow resistance would not be updated to account for plant to plant differences in the number of fuel assemblies in the average core channel. However, in a call with BWFC on August 7, 1989, BWFC stated the crossflow resistance for all bundle designs will also be updated to reflect plant-to-plant differences in the number of bundles in the average channel.

8. Section 2.1.5 of this report discussed reasons why the FRAP-T6-B&W analysis of Semiscale LBLOCA experiment S-04-6 had temperatures approximately 250°F different at two elevations that had the same power. As discussed in that section, BWFC stated the temperature differences were the result of the data transfer from RELAP5/MOD2 - B&W to FRAP-T6-B&W and the interpolation of the core conditions passed between the codes with different core axial noding. This situation has the potential to result in the calculation of lower and, thus, nonconservative PCTs. Therefore, the INEL recommends that BWFC review LBLOCA EM analyses where the codes involved in the

analysis use different core axial noding to determine any discrepancies in heat transfer regimes that may result from the data transfer. If needed to correct these differences, the core noding should be modified. This would especially apply to situations where the RELAP5/MOD2 - B&W analysis calculated a higher temperature than the FRAP-T6-B&W hot rod analysis at the same elevation. Also, INEL notes BWFC passes the appropriate information (fuel temperatures, gap conductance, oxide thickness, and rod dimensions) at the FRAP-T6-B&W/BEACH interface.

9. Information on the time steps to be used with FRAP-T6-B&W in SBLOCA analyses was not provided by BWFC. A time step of 0.2 s for FRAP-T6-B&W was justified by BWFC for LBLOCAs during the post-blowdown period. Because SBLOCAs [illegible] developing transients, the time steps used for the post [illegible] period of the LBLOCA should be sufficient for SBLOCA analyses. Therefore, unless other other time step controls are justified in the plant-specific submittals, a time step of 0.2 s should be used to analyze core uncovery PCTs in SBLOCAs.

10. The information supplied by BWFC did not clearly identify a specific FRAP-T6-B&W nodalization for SBLOCAs. Because the INEL considers the LBLOCA nodalization to also be adequate for SBLOCAs, the FRAP-T6-B&W nodalization for LBLOCAs should be used be SBLOCA analyses unless another nodalization is justified in the plant-specific submittal.

11. Prior to or with the first application of the SBLOCA EM, BWFC should complete a study to verify the adequacy of the loop seal clearing model in RELAP5/MOD2 - B&W. The study should show for a range of pressures and steam flows consistent with the pressure and decay heat in the system at the time of loop seal clearing that the code models correctly calculate liquid retention in the loop seal when steam flow conditions would result in liquid retention. The RELAP5/MOD2 - B&W models should also be compared to entrainment data to verify the code calculates entrainment correctly. A suggested reference is work by Kataoka and Ishii, Prediction of Pool Void Fraction by New Drift Flux Correlation, NUREG/CR-4657, ANL-86-29, June 1986.

12. Prior to or with the first application of the SBLOCA EM, the SBLOCA EM should be modified so that the break can be modeled at the bottom and top of the pipe and at a severed ECCS injection line.

13. The INEL recommends the two changes documented in Revision 3 of the REFLOD3B topical report be accepted. However, whenever the GSTOP to close the vessel side leak path option is used, BWFC should document its use in the applications report and justify the move in the containment boundary condition to region R4 does not affect the PCT by more than 50°F.

## 5. REFERENCES


1. B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants, BAW-10168P, Rev. 1, Babcock & Wilcox Nuclear Power Group, September 1989.

2. W. Hodges, NRC, ltr to J. H. Taylor, B&W, "Request for Additional Information on BAW-10168P, RSG LOCA," December 1, 1988.

3. R. Jones, NRC, ltr to J. H. Taylor, B&W, "Request for Additional Information on BAW-10168P, Revision 1, RSG LOCA," January 19, 1989.

4. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RSG LOCA Topical Report BAW-10168P," JHT/89-66, March 31, 1989.

5. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RSG LOCA Topical Report BAW-10168P," JHT/89-94, May 11, 1989.

6. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "LOCA Evaluation Model Topical Report BAW-10168P," JHT/89-150, July 20, 1989.

7. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RSG LOCA Topical Report, BAW-10168P," JHT/89-172, August 11, 1989.

8. J. H. Taylor, B&W, ltr to V. H. Wilson, NRC, "LOCA Evaluation Model Topical Report BAW-10168P," JHT/89-256, September 22, 1989.

9. J. H. Taylor, B&W, ltr to V. H. Wilson, NRC, JHT/90-37, March 12, 1990.

10. J. H. Taylor, B&W, ltr to V. H. Wilson, NRC, JHT/90-97, June 25, 1990.

11. J. H. Taylor, B&W, ltr to V. H. Wilson, NRC, JHT/90-131, September 5, 1990.

12. J. H. Taylor, B&W, ltr to V. H. Wilson, NRC, JHT/90-133, September 5, 1990.

13. J. H. Taylor, B&W, ltr to USNRC Document Control Desk, JHT/90-168, October 29, 1990.

14. C. K. Nithianandan, RFLOD3B, Model for Multinode Core Reflooding Analysis, BAW-10171P, Rev. 3, Babcock & Wilcox Nuclear Power Group, September 1989.

15. J. A. Klingenfus, et al., RELAP5/MOD2 - B&W: An Advanced Computer Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis, BAW-10164P, Rev. 1, Babcock & Wilcox Nuclear Power Group, October 1988.

16. J. H. Jones, FRAP-T6-B&W -- A Computer Code for the Transient Analysis of Light Water Reactor Fuel Rods, BAW-10165P, Rev. 1, October 1988.

17. N. H. Shah, BEACH: Best Estimate Analysis Core Heat Transfer, A Computer Program for Reflood Heat Transfer During LOCA, BAW-10166P, Rev. 2, Babcock & Wilcox Nuclear Power Group, September 1989.

18. A. Thadani, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10164, Revision 1, RELAP5/MOD2 - B&W, An Advanced Computer Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis," April 18, 1990.

19. A. Thadani, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10165P, Revision 1, FRAP-T6-B&W - A Computer Code for the Transient Analysis of Light Water Reactor Fuel Rods," May 19, 1989.

20. A. Thadani, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10171P, Revision 2, REFLOD3B - Model for Multinode Core Reflooding Analysis," May 19, 1989.

21. A. Thadani, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10166P, Revision 2, BEACH - Best Estimate Analysis Core Heat Transfer, A Computer Program for Reflood Heat Transfer During LOCA" August 13, 1990.

22. L. J. Siefken, et al., FRAP-T6: A Computer Code for the Transient Analysis of Oxide Fuel Rods, NUREG/CR-2148, EGG-2104, May 1981.

23. Y. H. Hsii, et al., TACO2 Fuel Pin Performance Analysis, BAW-10141A, Babcock & Wilcox.

24. C. O. Thomas, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10141(P), TACO-2 Fuel Pin Performance Analysis," April 13, 1983.

25. J. H. Taylor, B&W, ltr to C. Berlinger, NRC, "TACO3 - Fuel Pin Thermal Analysis Computer Code," JHT/87-75, April 10, 1987.

26. A. Thadani, NRC, ltr to J. H. Taylor, B&W, "Babcock & Wilcox Topical Report BAW-10162P, TACO3 - Fuel Pin Thermal Analysis Computer Code," August 14, 1989.

27. ANSI/ANS 5.1-1971 Draft, "Proposed ANS Standard Decay Energy Release Rates Following Shutdown of Uranium Fueled Thermal Reactors," American Nuclear Society, 1971.

28. L. Baker and L. C. Just, Studies of Metal-Water Reactions at High Temperatures, III. Experimental and Theoretical Studies of the Zirconium-Water Reaction, ANL-6548, May 1962.

29. D. A. Powers and R. O. Meyer, Cladding Swelling Models for LOCA Analysis, NUREG-0630, April 1980.

30. C. E. Beyer, et al., GAPCON-THERMAL-2: A Computer Program for Calculating the Thermal Behavior of an Oxide Fuel Rod, BNWL-1898, November 1975.

31. F. J. Moody, "Maximum Flow Rate of a Single-Component, Two-Phase Mixture," ASME Paper 64-HT-35, 1964.

32. C. J. Baroczy, "A Systematic Correlation for Two-Phase Pressure Drop," Chem. Enging. Prog. Symp. Series, Vol. 62, No. 64, 1965.

33. J. R. S. Thom, "Prediction of Pressure Drop During Forced Circulation Boiling of Water," Int. J. of Heat and Mass Transfer, 1, 1964, pp. 709-724.

34. R. C. Martinelli and D. B. Nelson, "Prediction of Pressure Drop During Forced Circulation Boiling of Water," Transactions of ASME, 1948, pp. 695-702.

35. R. W. Lockhart and R. C. Martinelli, "Proposed Correlation of Data for Isothermal Two-Phase, Two-Component Flow in Pipes," Chemical Engineering Progress, 45, 1, 1949, pp. 39-48.

36. K. T. Chaxton, J. G. Collier, and A. J. War, H.T.F.S. Correlation for Two-Phase Pressure Drop and Void Fraction in Tubes, AERE-R7162, 1972.

37. L. S. Tong, "Prediction of Departure From Nucleate Boiling for an Axially Non-uniform Heat Flux Distribution," Journal of Nuclear Energy, 21, 1967, pp. 241-248.

38. J. S. Gellerstedt, et al., "Correlation of Critical Heat Flux in a Bundle Cooled by Pressurized Water," Two-Phase Flow and Heat Transfer in Rod Bundles, ASME, New York, 1969.

39. J. M. Healzer, et al., Design Basis for Critical Heat Flux Condition in Boiling Water Reactors, APED-5186, General Electric Co. Private Report, July 1966.

40. R. V. MacBeth, "An Appraisal of Forced Convection Burnout Data," Proceedings of the Institute of Mechanical Engineers, 1965-1966.

41. P. G. Barnett, A Correlation of Burnout Data for Uniformly Heated Annuli and Its Use for Predicting Burnout in Uniformly Heated Rod Bundles, AEEW-R463, September 1966.

42. E. D. Hughes, A Correlation of Rod Bundle Critical Heat Flux for Water in the Pressure Range 150 to 725 psia, IN-1412, July 1970.

43. Babcock & Wilcox Revisions to THETA1-B, A Computer Program for Nuclear Reactor Core Thermal Analysis (IN-1445) - Revision 3, BAW-10094, Rev. 3, February 1981.

44. C. O. Thomas, NRC, ltr to J. H. Taylor, B&W, "Acceptance for Referencing of Licensing Topical Report BAW-10094, Revision 3, Babcock & Wilcox Revisions to THETA1-B, A Computer Program for Nuclear Reactor Core Thermal Analysis (IN-1445)," May 1985.

45. C. T. Avedisian, Critical Heat Flux in Countercurrent Flow, M. S. Thesis Massachusetts Institute of Technology, 1974.

46. R. V. Macbeth, Burn-out Analysis Part 4: Application of a Local Conditions Hypothesis to World Data for Uniformly Heated Round Tubes and Rectangular Channels, AEEW-R267, August 1963.

47. D. A. Farnsworth and G. A. Meyer, BWCMV Correlation of Critical Heat Flux in Mixing Vane Grid Fuel Assemblies, BAW-10159P, Babcock & Wilcox, May 1986.

48. A. Thadani, NRC, ltr to J. H. Taylor, "Acceptance for Referencing of Licensing Topical Report BAW-10159P, BWCMV Correlation of CHF in Mixing Vane Grid Fuel Assemblies, May 1986," February 17, 1989.

49. D. C. Groeneveld, An Investigation of Heat Transfer in the Liquid Deficient Regime, AECL-3281, revised December 1969.

50. R. S. Dougall and W. M. Rohsenow, Film Boiling on the Inside of Vertical Tubes with Upward Flow of the Fluid at Low Qualities, MIT Report No. 9079-26, September 1963.

51. Proprietary Redirect/Rebuttal Testimony of Westinghouse Electric Corp., USNRC Docket RM-50-1, page 25-1, October 26, 1972.

52. J. B. McDonough, W. Milich, and E. C. King, Partial Film Boiling with Water at 2000 psia in a Round Tube, Technical Report 62, MSA Research Corporation, 1958.

53. G. L. Yoder, Rod Bundle Film Boiling and Steam Cooling Data Base and Correlation Evaluation, NUREG/CR-4394, ORNL/TM-9628, August 1986.

54. PWR FLECHT (Full Length Emergency Core Heat Transfer) Final Report, WCAP-7665, Westinghouse Electric Corp., April 1971.

55. PWR Full Length Emergency Core Heat Transfer (FLECHT) Group I Test Report, WCAP-7435, Westinghouse Electric Corp., January 1970.

56. PWR FLECHT (Full Length Emergency Core Heat Transfer) Group II Test Report, WCAP-7544, Westinghouse Electric Corp., September 1970.

57. PWR FLECHT Final Report Supplement, WCAP-7931, Westinghouse Electric Corp., October 1972.

58. RELAP4/MOD5: A Computer Program for Transient Thermal-Hydraulic Analysis of Nuclear Reactors and Related Systems, Volume 1, ANCR-NUREG-1335, Aerojet Nuclear Company, September 1976.

59. H. S. Crapo, et al., Experimental Data Report for Semiscale MOD-1 Tests S-04-5 and S-04-6 (Baseline ECC Tests), TREE-NUREG-1045, January 1977.

60. G. G. Loomis and J. E. Streit, Results of Semiscale Mod-2C Small Break (5%) Loss-of-Coolant Accident Experiments S-LH-1 and S-LH-2, NUREG/CR-4438, EGG-2424, November 1985.

61. D. G. Hardy, "High-Temperature Rupture Behavior of Zircaloy Tubing," CONF-730304, USAEC/TIC, Water-Reactor Safety, March 1973.

62. H. M. Chung, et al., "III. Mechanical Properties of Zircaloy Containing Oxygen," Light-Water-Reactor Safety Research Program: Quarterly Progress Report, January - March 1976, ANL-76-49, pp. 34-59.

63. F. J. Erbacher and S. Leistikow, "Zircaloy Fuel Cladding Behavior in a Loss-of-Coolant Accident: A Review," ASTM STP 939, 1987, pp. 451-488.

64. F. J. Erbacher, "Interaction Between Fuel Clad Ballooning and Thermal-Hydraulics in a LOCA," KfK 3880/1, pp. 299-310, December 1984.

65. J. M. Cozzuol, Thermal-hydraulic Analysis of Semiscale Mod-1 Integral Blowdown-Reflood Tests (Baseline ECC Test Series), TREE-NUREG-1077, March 1977.

66. J. R. Brodrick, et al., 1/5 Scale Intact Loop Post-LOCA Steam Relief Tests, CENPD-63, Rev. 1, March 1973.

67. W. E. Burchill, et al., Steam Water Mixing Test Program Task D: Formal Report for the Task B and Final Report for the Steam Relief Phases of the Test Program, AEC-COO-2244-1, CENPD-101, October 1973.

68. G. P. Lilly, et al., Mixing of Emergency Core Cooling Water With Steam, EPRI-294-2, January 1975.

69. J. H. Taylor, B&W, ltr to V. Wilson, NRC, JHT/90-38, March 12, 1990.

70. K. Wieher and U. Harten, Datenbericht REBEKA-6, KfK 3986, March 1986.

71. Compendium of ECCS Research for Realistic LOCA Analysis, NUREG-1230, U.S. Nuclear Regulatory Commission, Office of Nuclear Reactor Research, December 1988.

72. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RELAP5/MOD2 - B&W Topical Report BAW-10164P," JHT/88-215, November 23, 1988.

73. N. Lee, et al., PWR FLECHT-SEASET Unblocked Bundle, Forced and Gravity Reflood Task Data Evaluation and Analysis Report, NUREG/CR-2256, EPRI NP-2013, WCAP-9891, November 1981.

74. N. H. Shah, et al., FOAM2 - Computer Program to Calculate Core Swell Level and Mass Flow Rate During Small-Break LOCA, BAW-10155, Babcock & Wilcox.

75. PWR FLECHT SEASET 21-Rod Bundle Flow Blockage Task Data and Analysis Report, NUREG/CR-2444, EPRI NP-2014, WCAP-9992, September 1982.

76. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RELAP5/MOD2 - B&W Topical Report BAW-10164P," JHT/89-149, July 20, 1989.

77. USNRC, Division of Licensing, Clarification of TMI Action Plan Requirements, NUREG-0737, November 1980.

78. J. H. Taylor, B&W, ltr to J. A. Norberg, NRC, "RELAP5/MOD2 - B&W Topical Report, BAW-10164P," JHT/89-95, May 11, 1989.

79. USNRC, Division of Licensing, Standard Review Plan, NUREG-0800, July 1981.

B&W Fuel Company
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- RSG LOCA -

# B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants

## Volume I - Large Break

Key Words: Large Break, LOCA, Transient, Water Reactors, Evaluation Model

## ABSTRACT

This document presents the generic large and small break models used by Babcock & Wilcox for evaluating the performance of the emergency core cooling systems (ECCS) following a loss-of-coolant accident (LOCA) for Westinghouse and Combustion Engineering designed Pressurized Water Reactors (PWR) with B&W supplied fuel. The large break model is discussed in Volume I and the small break model in Volume II. The models have been developed and compared with the required and acceptable features contained in Appendix K of the Code of Federal Regulations, 10CFR50. The evaluation models are shown to conform to Appendix K requirements.

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## Topical Revision Record

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# 1. INTRODUCTION

This report describes the features of the emergency core cooling system (ECCS) evaluation model used by B&W for application to Westinghouse (W) and Combustion Engineering (CE) designed PWR's with B&W reload fuel. The evaluation model is applicable to the pressurized water reactors (PWR) categorized in Table 1.1. There are no significant system design differences for the nuclear steam supply system (NSSS) and the ECCS within each category as these design features are the basis for the grouping.

Specific design information for each plant category is considered input to the evaluation model and is generated using the assumptions and techniques described herein.

The "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Nuclear Power Reactors" (10CFR50.46) were issued by the NRC in January 1974. Appendix K of 10CFR50.46 defines the required and acceptable features of models to be used to evaluate the performance of the ECC systems. The information presented in this document defines the B&W evaluation model and shows that the model conforms to Appendix K requirements.

The topical report is divided into two volumes. The first volume presents the large-break evaluation model. The second volume presents the small-break evaluation model. Each of the volumes contains the following seven sections which define the respective evaluation models:

1. Definition of model versus input (Section 3).

2. Features of the evaluation model and statements of conformity to Appendix K (Section 4).

3. The calculational technique used to evaluate the maximum local cladding oxidation (Section 5).

4. The calculational technique to evaluate the maximum hydrogen generation (Section 6).

5. The technique used to evaluate conformance to the coolable geometry criterion (Section 7).

6. The technique for establishing conformance to the long-term cooling criteria (Section 8).

7. Required documentation necessary to meet 10CFR50.46 (Section 9).

Table 1-1. Applicable PWR Plant Categories.

| Reactor Supplier | RCS Geometry | Fuel Design | Containment Type | ECCS Type |
|---|---|---|---|---|
| W | 3 loop | 15 x 15 | H | Conventional |
| W | 3 loop | 17 x 17 | H | Conventional |
| W | 4 loop | 15 x 15 | H | Conventional |
| W | 4 loop | 15 x 15 | L | Conventional |
| W | 4 loop | 17 x 17 | H | Conventional |
| W | 4 loop | 17 x 17 | L | Conventional |
| W | 4 loop | 17 x 17 | L | UHI valved out |
| CE | 3 loop | 14 x 14 | H | Conventional |
| CE | 4 loop | 16 x 16 | H | Conventional |

Containment type explanation

H = High pressure design

L = Low pressure design with ice condenser

## 2. COMPLIANCE TO 10CFR50.46

The "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Power Reactors" (10CFR50.46), issued by the Nuclear Regulatory Commission in January 1974, include five criteria that must be met before an emergency core cooling system (ECCS) is acceptable. Conformance to these criteria is established in the following manner:

1. The peak cladding temperature shall not exceed 2200F.

   The peak cladding temperature is calculated with the evaluation model described herein and shown not to exceed 2200F.

2. The percentage of local cladding oxidation shall not exceed 17%.

   In the analysis performed to satisfy condition 1, the total oxide thickness (combining both inside and outside oxide layers) at the location of maximum local oxidation is calculated and shown to be less than 0.17 times the total cladding thickness.

3. The maximum hydrogen generated during the transient shall not exceed that which would be generated by the oxidation of 1% of the reactor cladding.

   Using a conservative approach, the cladding temperature response (and thereby the degree of oxidation) is determined as a function of local power. This function is then integrated over the core power distribution to obtain total oxidation. Division of the integrated value by the initial amount of zirconium gives the fraction of oxidized cladding which will be shown to be

less than one percent upon application of this methodology to a specific plant or category of plants.

4. Calculated changes in the core geometry shall be such that the core remains amenable to cooling.

   The changes in geometry that were calculated during the analysis for peak cladding temperature are examined to ensure that no gross core blockage or deformation occurs.

5. The mode of long-term cooling shall be established.

   The analysis of cladding temperature is continued until the cladding peak temperature in the core is decreasing and the fluid level in the core is rising. At this time the path to long-term cooling is established and the core will continue to cool down without interruption or further incident. Cooling for the long-term is established by pumped injection with no interruptions or decreases in coolant flow thereby maintaining the core temperature at an acceptably low value while removing decay heat.

Appendix K sets forth certain required and acceptable features of the evaluation model that must be used to show compliance to the five acceptance criteria of 10CFR50.46. Compliance of the B&W evaluation model to Appendix K is shown in Section 4.

# 3. DEFINITION OF LBLOCA MODEL VERSUS INPUT

The information presented in this document defines the B&W ECCS evaluation model for application to non-B&W plants with B&W-supplied reload fuel. In particular, this document describes the techniques and assumptions used in the evaluation of the consequences of a loss-of-coolant accident. These techniques and assumptions constitute the model. The required features of the ECCS evaluation model are set forth in Appendix K to 10CFR50. The evaluation model for large breaks is given in Chapter 4 along with statements of its conformance to Appendix K. The evaluation model will be changed only if the NRC issues rule changes or if improved analytical techniques become available.

Specific category-related data (such as system design, power level, etc.) are considered inputs to the model. These numbers are developed using the techniques and assumptions described in the evaluation model. This report covers the model only. All other information needed to perform the necessary analyses is considered input. Input may change throughout the design life of a nuclear steam supply system requiring a reapplication of the evaluation model while not affecting the evaluation model per se.

# 4. LARGE BREAK LOCA EVALUATION MODEL

## 4.1. Model Applicability

The large break evaluation model described in this section is applicable to all general plant categories presented in Table 1.1. Any item that is applicable only to certain plant categories due to differences in design, such as RCS geometry, containment pressure suppression, ECCS design, or fuel design, will be specifically identified. There are no significant design differences among the plants within each category.

## 4.2. Transient Description and Computer Code Interfaces

Large break loss-of-coolant accidents can be treated analytically in three major phases: blowdown, refill, and reflood. The blowdown phase can be characterized as the rapid depressurization of the reactor coolant system to a condition nearly in equilibrium with its immediate surroundings. Core flow is variable and dependent on the nature, size, and location of the break. Departure from nucleate boiling (DNB) is generally calculated to occur very quickly, and core cooling is by a film boiling process. Since film boiling amounts to only a small fraction of the steady-state cooling, the cladding temperature increases by 600 F to 800 F. During the very last phases of blowdown, cooling is by convection to steam, and the cladding temperature will again begin to rise.

Following blowdown, a period of time is required for the ECCS to refill the bottom of the reactor vessel, before final cooling can be established. During this period, core cooling is quite small, and the cladding experiences a near-adiabatic heatup. This period is designated as the refill phase. When the ECCS water reaches the bottom of the core, the reflood phase begins. Core cooling is by steam generated below the rising water level. The cladding temperature excursion is generally terminated before the

a particular elevation is covered by water since the steam-water mixture is sufficient to remove the relatively low decay heat power being generated at this time. The core is eventually covered by a two-phase mixture, and the path to long-term cooling is established through pumped injection to supply makeup.

Figure 4-1 illustrates the interrelation of the computer codes used for large break LOCA analysis. The RELAP5/MOD2-B&W[1] code calculates system thermal-hydraulics and core power generation during blowdown. The REFLOD3B[2] code determines the length of the refill period and the core flooding rate during reflood. FRAP-T6-B&W[4] determines the clad temperature response during the blowdown and refill period using the output from RELAP5/MOD2-B&W. Finally, BEACH[3] determines the clad temperature during the reflood period with input from FRAP-T6 and REFLOD3B.

Large break LOCA encompasses a wide range of break sizes, types, and locations. Breaks with cross-sectional areas that vary from 1.0 $ft^2$ up to and including twice the cross-sectional area of the largest reactor coolant pipe are considered large breaks. Both instantaneous double-ended guillotine and longitudinal split type breaks are considered within the range of large breaks. For the longitudinal splits, breaks up to twice the cross-sectional area of the pipe are considered. Breaks can be located anywhere in the primary system piping (hot leg and cold legs).

## 4.3. Features of Model

This evaluation model description includes guidelines for the use and interfacing of the computer codes used for large break analysis. The organization of the following material is on a subject basis and applies to all codes, as appropriate. Where specific details are employed in only one code, the situation is clearly identified.

4.3.1. System Noding

The system noding used for the blowdown analysis (RELAP5/MOD2-B&W) is shown in Figures 4-2 and 4-3. All the nodes are modeled as nonhomogeneous and nonequilibrium volumes except for the core and core boundary nodes, which are modeled as homogeneous-equilibrium volumes. The loop noding scheme shown in Figure 4-2 has one loop representing the loop containing the break and a second loop representing the three combined intact loops and the pressurizer. As shown in Appendix A, this model produces convergent results when compared to other nodi[illegible] models.

The piping in the vicinity of the rupture is modeled by at least four nodes. The noding scheme shown in Figure 4-2 is used for pump discharge breaks. For other break locations, the break noding detail as shown for the cold leg is shifted to the subject break location. For pump discharge split breaks, the entire break flow is from node 275. For double-ended breaks the flow is from nodes 270 and 275, and the connecting junction is closed just after accident initiation. The noding scheme is selected to produce reliable thermodynamic properties at the break. The noding scheme isolates the leak node from the pump, so that large pressure differences caused by acceleration of the fluid would occur downstream of the pump and thus allows the leak to respond in a realistic manner. Also, the leak node must be isolated from the vessel so that the acceleration pressure drop will be for the pipe only. These conclusions are effective regardless of the plant being studied. The leak nodalization is supported by previous break noding studies with similar models (Section 5.3 of topical report BAW-10091[11]) and by a study presented in Appendix A.

The steam generators are modeled such that the broken loop represents one steam generator and the intact loop simulates three combined generators. The feedwater, tube bundle, and steam separation regions are all modeled.

The reactor vessel (RV) is divided into 30 volumes as shown in Figure 4-3. The distinct noding regions within the RV include the downcomer (volumes 300 through 308), lower plenum and lower head (volumes 310 through 314), active core, core bypass (volumes 340 through 344), core baffle (volumes 346 through 350), and upper plenum (volumes 352 and 356) and upper head (volumes 358 through 360). The core is divided into two radial regions; one for the hot fuel assembly (volumes 328 through 338) and one for the remainder of the core (volumes 316 through 326). Axially, the core is modeled by nine control volumes and seven junctions in each of the radial regions. The six-axial region core is chosen because analysis of the different power shapes studied in the plant specific LOCA limits analysis requires computation of fluid conditions at the 2, 4, 6, 8, and 10 foot elevations. For the evaluation model, a symmetrical chopped cosine 6 foot peak power shape was chosen. The selected representation of the core has been verified specifically for Westinghouse 17x17 4-loop designs even though in general it is independent of fuel and plant design. Deviations in fuel hydraulic behavior would be accounted for in the hydraulic inputs and, short of major design alterations, not affect the core noding being used in RELAP5/MOD2-B&W. Plant deviations across the categories could affect the vessel noding arrangement but, except for possible flow variations between the bypass and the average core, no alteration in core noding should be required. To the extent that differing vessel arrangements are required for other plants, modifications will be submitted for approval.

The noding arrangement presented in this section is applied to all large break blowdown analyses except where details in specific areas are altered to satisfy the needs of specific accidents or initial system configurations, for example a change in break location. When such changes occur, details will be explained in the specific application.

4.3.2. Sources of Heat

The sources listed below are used in the analysis. These sources are time-dependent and their representation in the computer codes is dependent on the phase of the accident being modeled.

4.3.2.1. Initial Power

It is assumed that the reactor has been operating continuously at a power level of 1.02 times the rated power level to allow for such uncertainties as instrumentation error.

4.3.2.2. Core Peaking Factors

One purpose in performing the LOCA evaluation is to determine the maximum allowable local heating rates for which calculated peak cladding temperatures (and other parameters) will remain below the peak temperature criterion of 10CFR50.46. These maximum heating rates are then incorporated into the plant Technical Specifications as the LOCA limits. To accomplish this the following procedure is used:

1. A spectrum of breaks is analyzed to establish the worst break using a symmetric power shape and estimated peaking factors.

2. Using the worst break established in step "a", the maximum allowable local heating rate is determined for five axial levels within the core. Five evaluations are performed with the location of the peak power, axial peaking, changed from 2 feet to 10 feet above the bottom of the core in 2 foot increments. For each axial power profile the peak local heating is iteratively increased in a series of runs until the maximum local heating rate which will not violate the criteria of 10CFR50.46 has been determined to the accuracy desired.

4.3.2.3. Core Stored Energy

The initial temperature distribution in the fuel is that which yields the highest calculated temperature during LOCA. This is accomplished by (1) evaluating LOCA significant burnup dependant parameters on a best estimate basis, and (2) combining these mechanistically in a burnup sensitivity study to identify the most conservative combination. This worst combination of parameters is then used in the spectrum and LOCA limits evaluations. The parameters which are varied during the burnup study are: the initial average fuel temperature as determined by an NRC-approved steady-state fuel pin model (TACO2[5] at present or TACO3[15] upon NRC approval), the initial internal fuel pin pressure as determined by the same code as the initial fuel temperature, the moderator coefficient, the initial fuel pin oxide thicknesses, and the core peaking factors as controlled by the Technical Specifications.

The RELAP5/MOD2-B&W runs are initialized by using the initial average fuel temperature from the approved steady-state fuel code. FRAP-T6-B&W is initialized with an iterative process to assure that the fuel stored energy matches or slightly exceeds the prediction of the steady-state code. Calculated results and input parameters from the fuel pin code (such as cladding and fuel dimensions, surface roughness, Poisson's ratio, radial power shape within the fuel, gas composition, gas pressure, etc.) are input FRAP-T6-B&W. If the average fuel temperatures from FRAP-T6-B&W are more than 20 F below those predicted by the fuel thermal code, then the multiplier on the as-calculated gas gap heat transfer coefficient is varied slightly. This procedure is carried out until the stored energy calculated by FRAP-T6-B&W is greater than or equal to the steady-state fuel thermal analysis value.

4.3.2.4. Fission Heat

During blowdown, fission heat is calculated using the point kinetics model of the RELAP5/MOD2-B&W code (Section 2.3.1 of BAW-10164[1]). The only reactivity effects modeled during blowdown for large break LOCA are the fuel temperature coefficient and the void-moderator coefficient. The effectiveness of the void-moderator coefficient in shutting down the core is so large as to make the modeling of other negative reactivities irrelevant. The values of the reactivity coefficients are chosen to be conservative for the time in plant life that yields the highest peak cladding temperature. The spatial weighting of the reactivity factors is by the flux square method.

For the reflood phase, the void-moderator coefficient will become ineffective and both safety rod insertion and borated water injection are used to maintain the reactor subcritical. No explicit calculation of fission power is made. Rather, the fraction of the fission power to the total power at the end-of-blowdown is kept constant. Thus, fission power drops during reflood in direct proportion to the decay heat curve.

4.3.2.5. Decay of Actinides

The power from the decay of actinides is calculated using the 1979 ANS standard.

4.3.2.6. Fission Product Decay

The decay heat curve described in the proposed ANS standard, "Decay Energy Release Rates Following Shutdown of Uranium Fueled Thermal Reactors" (approved by subcommittee ANS-5 in October 1971), for infinite operation times a factor of 1.2 is used for evaluating fission product heating. Figure 4-5 shows the values to be used.

4.3.2.7. Heat Distribution

During the blowdown phase of the analysis a power distribution factor of 0.95 is used to evaluate the power for the hot channel analysis using RELAP5/MOD2-B&W. This factor is based on the consideration that 5.0 percent of the fission energy is deposited directly to the coolant rather than in the fuel or cladding. Thus, this heat is not transmitted through the cladding in the removal process. During the reflood phase of the accident, a distribution factor of 0.95 will be used for the hot channel. This factor is in agreement with previous licensing calculations[14] and provides a conservative representation of gamma-flatting for LOCA applications.

The power at the peak in the core (PLP) is evaluated by the following formula:

$$PLP = \frac{1.02 \times kW \times P_f \times F \times P(t)}{\text{total pin feet in core}},$$

where

PLP = (peak linear power) linear heating rate, kW/ft,
kW = rated power, kW,
$P_f$ = total peaking factor,
F = appropriate power distribution factor, and
P(t) = normalized transient power.

4.3.2.8. Metal-Water Reaction

The rate of energy release from metal-water reaction is calculated using the Baker-Just equation without steam limiting. In the RELAP/MOD2-B&W, BEACH and FRAP-T6-B&W codes, the reaction is applied on the outside of the cladding and to the inner cladding surface if rupture has occurred. Continuous thinning is modeled for the oxide layers in FRAP-T6-B&W; however, oxide

thinning is only considered in the plastic region in RELAP5/MOD2-B&W and BEACH. For this purpose, the axial dimension of the rupture location is considered to be at least 3 inches. The calculations for metal-water reaction are outlined in Section 2.3.2.4, 2.1.2 and 2.2 of BAW-10164[1] (RELAP5/MOD2-B&W), BAW-10165[4] (FRAP-T6-B&W) and BAW-10166[3] (BEACH), respectively.

The initial oxide thicknesses on the outside and inside cladding surfaces are consistent with the time-in-life that maximizes cladding temperature (see Section A.3.9). The reference weight gain oxidation curves for Zircaloy-4 exposed to either a water or steam environment, given in Figure 4-6, are used to develop the outside oxide layer thickness. The data, which are the heavier lines in the figure, are based on the curves published by Bettis in WAPD-MRP-107[6]. The lines of intermediate weight are an interpolation of the experimental data based on WCAP-3269-56[7]. The 680 F oxidation curve was shifted to the left to form a transition point at 110 days compared to the normally accepted transition point of 120 days. This shift reflects the results obtained by B&W on Zircaloy-4 cladding material tested in a reactor system water environment. A relationship between burnup and effective reactor full power days can be established from the steady-state fuels code. Using this relationship, the outside oxide layer weight gain can be determined. The conversion factor, 390 $mg/dm^2$ = 1 mil, is used to determine thickness from weight gain.

Figure 4-7 shows the weight gain oxidation curves for the inside surface of the cladding to be used during hot functional testing of the reactor prior to going critical. These curves, based on the data in WAPD-MRP-107[6], are used to account for the moisture fabricated into the fuel pellets. Once the reactor goes critical, the curve shown in Figure 4-8 is used to represent the inside oxide buildup resulting from the oxygen generated by fissioning of the $UO_2$. This curve is based on data reported in

BAW-1716[8] and BAW-1874[13]. The results of Figure 4-7 added to the data reported in Figure 4-8 give the total inside oxide thickness.

4.3.2.9. Primary Metal Heating

Heat transfer from piping, vessel walls, and non-fuel internal structures is taken into account. Appropriate metal slabs are simulated in each control volume of the RELAP5/MOD2-B&W and REFLOD3B models using lumped parameter methods. The general heat slab model for RELAP5/MOD2-B&W is explained in Section 2.2 of BAW-10164[1]. The primary metal heating model for REFLOD3B is described in Section 2.9 of BAW-10171[2]. A previous sensitivity study on the effect of increasing the primary heat addition (Section 5.7 of topical report BAW-10091[11]) showed minimal effect for this heat source in large break applications.

4.3.2.10. Primary-to-Secondary Heat Transfer

Heat transfer between the primary system and the secondary side of the steam generators is calculated during all phases of the LOCA. During blowdown, the steam generator tubes are modeled as a boundary across which heat is transferred using the models and heat transfer correlations described in Section 2.2 of BAW-10164 (RELAP/MOD2-B&W). During the reflood phase of the transient, the heat is transferred using the model described in Section 2.10 of BAW-10171 (REFLOD3B). A conservative high steam generator heat transfer coefficient is input in REFLOD3B so that all incoming primary side fluid is vaporized and superheated to the secondary saturation temperature.

### 4.3.3. Swelling, Rupture, and Thermal Properties

#### 4.3.3.1. Swelling

Clad thermal expansion, elastic swelling, and plastic deformation are considered prior to rupture in the analysis. The specifics of the models are detailed in the RELAP5/MOD2-B&W, BEACH and FRAP-T6-B&W topical reports (Section 2.3.2 in BAW-10164, Section 2.2 in BAW-10166 and Section 2.4 in BAW-10165). Appendix K requires that clad swelling and rupture calculations be based on applicable data in such a way that the degree of swelling and the incidence of rupture are not underpredicted. In order to establish an industry data base, the NRC has sponsored research programs on cladding behavior during and after a LOCA. NUREG-0630[9] is based on this research and contains models for clad rupture, strain, and blockage during and following a LOCA.

The rupture model used is an implementation of the NUREG-0630 correlations:

1. Rupture temperature is determined by linear interpolation between the NUREG-0630 fast and slow ramp rate rupture temperature versus stress curves, Figure 4-9. The ramp rate for a given location is taken as the instantaneous normalized heating rate for elastic strains. A plastic-weighted, time-averaged heating rate is used after the initiation of plastic deformation at that location.

2. Cladding strain at rupture is also an interpolation between the NUREG-0630 fast and slow ramp rate strain curves, Figure 4-10, based on the plastic weighted time averaged ramp rate after initiation of plastic strain.

3. The coolant flow blockage is again an interpolation of the NUREG-0630 curves, Figure 4-11, based on ramp rate.

4.3.3.2. Rupture

The effects of rupture are modeled in RELAP5/MOD2-B&W, BEACH and FRAP-T6-B&W. These codes compute transient pin internal pressure and utilize the resultant cladding hoop stress to determine the occurrence of rupture. Once a rupture has occurred the effects of the cladding strain are imposed on the solution and the problem evaluation continues.

The initial internal pin pressure is either (1) that which occurs at the burnup which yields the highest peak cladding temperature according to a time-in-life sensitivity study, see Section A.3.9, or (2) a pressure that is higher than the highest pressure expected during the licensed life of the fuel being evaluated. If the bounding pressure is used, the remaining burnup-dependent fuel parameters are taken at the burnup for which their values, independent of the pin pressure, would produce the highest peak cladding temperature.

The pin pressure calculated at any time is used to determine the hoop stress on the pin. The rupture model determines the rupture temperature as a function of the hoop stress using the data from Figure 4-9. The calculated cladding temperature is compared continuously to the rupture temperature and, if that temperature is exceeded, then rupture occurs. Once rupture has occurred in an axial channel (either a set of flow paths in RELAP5/MOD2-B&W or a portion of the pin in FRAP-T6-B&W or BEACH), it cannot occur at any other location in that same channel. The details of the models are shown in Section 2.3.2 of BAW-10164 (RELAP5/MOD2-B&W), Section 2.4 of BAW-10165 (FRAP-T6-B&W) and Section 2.2 of BAW-10166 (BEACH).

If rupture is predicted to occur, $T_{clad} \geq T_{rup}$, the internal pressure of the ruptured pin is set equal to the coolant pressure thereby removing the pressure-induced stress in the cladding. The final cladding deformation and flow blockage for the ruptured

pin become a function of the plastic strain at the time of rupture. Using the rupture temperature, the strain at rupture and the flow blockage are determined from the data in Figures 4-10 and 4-11.

The blockage model in BEACH calculates the enhanced heat transfer at the rupture location as described in Section 2.1.3.8 of the BEACH topical report BAW-10166. When BEACH predicts rupture, the code is restarted at the time of rupture by adding the blockage parameters to the rupture node. These blockage parameters are calculated using the bundle average blockage predicted by BEACH. The increase in the rupture node heat transfer due to the increase in clad surface area is accounted for by multiplying the convective enhancement factor, $F_{gg}$, in Section 2.1.3.7 of the BEACH topical report BAW-10166 by the ratio of the rupture to cold clad diameters. The code automatically adds the blockage form losses to the appropriate junctions.

During an LBLOCA in PWRs with recirculating steam generators, the fuel pin failure generally occurs during the reflood phase of the transient. If rupture occurs during the blowdown phase, RELAP5/MOD2-B&W will generally calculate the rupture within the hot assembly earlier than would FRAP-T6-B&W. In this case the timing of the rupture in RELAP5/MOD2-B&W will not be adjusted to coincide with FRAP-T6-B&W and the resultant post-rupture flow decrease is treated as a conservatism. Should FRAP-T6-B&W predict rupture prior to RELAP5/MOD2-B&W, then the time of rupture in RELAP5/MOD2-B&W will be forced to coincide with FRAP-T6-B&W. The timing of rupture and the cladding strain effects on heat transfer and oxidation rate are taken from the FRAP-T6-B&W prediction. If rupture is predicted during blowdown or refill, the FRAP-T6-B&W clad swelling and rupture conditions and the oxide layer thickness at the end of adiabatic heatup will be imposed on BEACH.

4.3.3.3. Gap Models

The gap models used to determine the mechanical response of the gap between the fuel and cladding include fuel radial expansion and clad swelling and rupture. The clad swelling and rupture is based on NUREG-0630[9] data. The gap conductance model is based on the GAPCON-THERMAL-2[12] code. The details of these models are documented in Section 2.3.2 of the RELAP5/MOD2-B&W topical report, Section 2.2 of the BEACH topical report, and Sections 2.1.3 and 2.4 of the FRAP-T6-B&W topical report.

4.3.3.4. Other Thermal Parameters

The thermal and mechanical properties of the fuel and clad are based on current nuclear industry materials data. The material properties as used in the LOCA analysis models reflect temperature-dependent behavior during the transient. The conductivity of the gas in the fuel pin gap is calculated using kinetic theory which includes the effect of temperature. The details of these calculations are presented in Section 2.3.2 and Appendix E of the RELAP5/MOD2-B&W topical report and Section 2.4 and Appendix D of the FRAP-T6-B&W topical report. The thermal and mechanical properties used in BEACH are the same as those described in RELAP5/MOD2-B&W.

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## 4.3.4. Blowdown Phenomena

### 4.3.4.1. Discharge Model

The discharge of subcooled fluid through the break is determined using the Extended Henry-Fauske model. Once the break node becomes saturated, the discharge is based on the Moody model using Moody slip. To avoid a step change in the flow between the models, a 0.001 quality criterion (e = 0.001 in the equation on page 2.1-109 of BAW-10164) is used to transition between the Extended Henry-Fauske and the Moody models. The Murdock-Bauman model is used for superheated discharge. The break flow is the minimum of the choked flow and the flow calculated by the momentum equations. Section 2.1.4 of the RELAP5/MOD2-B&W topical report describes the discharge models and the assumptions regarding their use.

A range of discharge coefficients is evaluated in the spectrum analysis. The coefficients are applied to both double-ended and split type breaks. Section A.3.8 provides a LOCA spectrum of breaks study extending for a $C_D = 1.0$ to $C_D = 0.4$.

### 4.3.4.2. End-of-Bypass

For cold leg breaks the end-of-bypass time is considered to be the same as the end-of-blowdown time. Bypassed ECC flows are modeled in accordance with Appendix K (Section I.C.1.c) and the dictates of the break location. Unbroken loop ECC flows are deposited into volume 170 (Figure 4-2) and mixed with system fluid residing in that volume. The ECC liquid is then removed from the system by withdrawing from node 302 (Figure 4-2) the minimum of up to 110 percent of the ECC flow or 99 percent of the liquid residing in volume 302. In general the minimum will be the ECC flow. This calculation is initiated at the start of ECC injection and continues until the end-of-blowdown. For breaks in the pump discharge piping the broken loop ECC is deposited directly into the containment, while for pump suction breaks, the

broken loop ECC is deposited in the broken loop pump discharge piping, mixed with existing fluid in that region, and treated as part of the primary system fluid. Using this bypass methodology, any liquid remaining in the inlet lines and RV at the end-of-blowdown is considered in the reflooding calculation.

4.3.4.3. Momentum Flux

Flow between adjoining nodes is determined by the phasic momentum equations in RELAP5/MOD2-B&W and by a mixture momentum equation in REFLOD3B. Included in the formulation of the momentum equation are terms that account for (1) time rate of change of momentum, (2) momentum convection, (3) momentum change due to compressibility, (4) momentum flux resulting from area change, (5) pressure loss resulting from wall friction, (6) pressure loss due to area change, and (7) gravitational acceleration. The derivation of the governing fluid flow equations used in RELAP5/MOD2-B&W is given in Section 2.1 of BAW-10164. The derivation and application of the momentum equation used in REFLOD3B is given in Section 2.2.1 and Appendix C of BAW-10171. The momentum equation used in the BEACH code is the same as that of RELAP5/MOD2-B&W.

4.3.4.4. Frictional Pressure Drop

The RELAP5/MOD2-B&W, REFLOD3B, and BEACH computer codes consider friction and form losses. A description of the RELAP5/MOD2-B&W calculation is given in Section 2.1.3 of BAW-10164; REFLOD3B is given in Section 2.0 of BAW-10171; and BEACH is presented in Section 2.1.3 of BAW-10166. The friction and form losses are calculated for the system using best estimate techniques and normalized to available plant or experimental data. The RELAP5/MOD2-B&W code accepts surface roughness and form loss factors as input. Frictional losses are initialized to the calculated system distribution through adjustments in the surface roughness terms. The frictional terms used in the REFLOD3B code

are taken from the initialized RELAP5/MOD2-B&W run by combining terms where a REFLOD3B path encompasses more than one RELAP5/MOD2-B&W path. The BEACH code models a single fuel pin or a fuel assembly and makes a smooth pipe approximation for the frictional loss. The form loss for the grids in BEACH is derived from fuel bundle pressure drop measurements conducted on prototype assemblies. This form loss corresponds to that used in the RELAP5/MOD2-B&W model of the core.

4.3.4.5. Pump Model

The RC system centrifugal pump model employed by RELAP5/MOD2-B&W is described in Section 2.1.5.2 of topical report BAW-10164. The model is based on homologous relationships and includes default Westinghouse single- and two-phase performance characteristics. The RELAP5/MOD2-B&W plant specific pump performance tables are given as input for the model to determine dynamic pump conditions during the transient.

4.3.4.6. Core Flow

The hot region of the core is taken as one fuel assembly represented by control volumes 328 through 338 in Figure 4-3. The remainder of the core is represented by control volumes 316 through 326 in Figure 4-3. Calculations of core flow take into account cladding rupture as outlined in Section 2.3.2 of the RELAP5/MOD2-B&W topical report. Crossflow is considered between core volumes. The crossflow is simulated using the crossflow junction described in Section 2.1.4.3 of BAW-10164.

Filtered RELAP5/MOD2-B&W hot channel core flow is used in the heatup calculations. This output is obtained by passing the mass flow rate through the equation for a low-pass filter:

$$\frac{dz}{dt} = -P(Z - W) \ ,$$

where

W = flow rate,

Z = filtered flow rate, and

P = break frequency.

The break frequency is set to remove perturbations with periods less than 0.1 seconds.

4.3.4.7. Pre-CHF Core Heat Transfer

Pre-CHF (critical heat flux) heat transfer is by convection in single phase liquid, nucleate boiling, and forced convection vaporization. For the single-phase liquid convection regime, the maximum of the Dittus-Boelter or Rohsenow-Choi correlations is used. Heat transfer for nucleate boiling and forced convection vaporization regimes is by a combination of the Chen, Thom, and Schrock-Grossman correlations. Switching logic and details for the correlations are found in Sections 2.3.3 and 2.3.3.2 of the RELAP5/MOD2-B&W topical report and Sections 2.1.4.1 and 2.1.4.2 of the FRAP-T6-B&W topical report.

4.3.4.8. Core CHF Models

The core CHF model used for the high flow regime is a combination of the BWCMV, Barnett, and modified Barnett correlations. In the low flow regime, a combination of MacBeth and Griffith correlations are used. For all regimes, a minimum CHF value of 90000 BTU/hr-ft$^2$ is used. Switching logic and descriptions of the correlations are found in Sections 2.3.3 and 2.3.3.3 of the RELAP5/MOD2-B&W topical report and Sections 2.1.4.1 and 2.1.4.3 of the FRAP-T6-B&W topical reports.

4.3.4.9. Core Post-CHF Heat Transfer

The post-CHF heat transfer regimes in the core model include transition boiling, film boiling, and single-phase steam heat transfer. Once CHF has been exceeded, no return to pre-CHF correlations is allowed in RELAP5/MOD2-B&W or FRAP-T6-B&W. The transition boiling correlation of McDonough, Milich, and King is used to connect the regimes of nucleate and film boiling. The maximum of the transition boiling and film boiling correlations is used only as long as the fuel pin cladding temperature remains within 300 F of the saturated fluid temperature. If the transition boiling correlation predicts a heat flux which would exceed that calculated by pre-CHF correlations, than a temporary switch to film boiling is used. Once the 300 F margin has been exceeded, the transition boiling correlation is not reapplied for the remainder of the blowdown phase of the transient. The maximum of the Condie-Bengston and Rohsenow-Choi correlations is used in the film boiling regime. The single-phase heat transfer to steam correlation is the sum of a convective term and a radiation term. The convection heat transfer is the maximum of the McEligot or Rohsenow-Choi correlations. The radiation heat transfer is from the Sun correlation. Sections 2.3.3.4 and 2.1.4.4 of the RELAP5/MOD2-B&W and FRAP-T6-B&W topical reports explain the calculations performed for post-CHF heat transfer. Section 2.1.4 of the FRAP-T6-B&W topical report further explains the post-CHF heat transfer logic.

4.3.5. Single Failure

The ECCS system is made up of redundant trains of pumped injection systems providing high and low pressure injection capability. One of these trains will be assumed as nonfunctional, if that assumption is conservative. The operation of an injection system during large break LOCA can cause two effects. First, the availability of more ECCS water can increase the water level in the downcomer faster and create higher initial

core flooding rates. Second, higher ECCS injection rates can cause more subcooled water to spill through the break reducing the containment and the RCS system pressure. A reduction in RCS system pressure during reflood has a reducing effect on core reflood rate. Thus, assuming a failure of one train of ECCS can be either conservative or nonconservative depending on the design of the NSSS. If the downcomer can be kept full of water throughout most of the transient by one ECCS train, then the effect of adding a second train is to increase the spillage of cold water through the break with an attendant reduction of core flooding rate. Conversely, if a single ECCS train cannot maintain the downcomer full, a second ECCS system will, thereby increasing the reflooding rate. For current ECCS designs, a single ECCS train usually maintains a full downcomer.

For each design analyzed, a sensitivity study will be conducted to determine if a single failure of the ECCS system would be conservative. If so, a single diesel and all of the ECCS powered by it will be assumed inoperative. If the failure would be non-conservative, all ECCS systems will be considered operative.

4.3.6. Post-Blowdown Phenomena

4.3.6.1. Containment Pressure

The evaluation of a postulated LOCA assumes a conservative (minimum) containment backpressure during reflood. The selection of the actual pressure for the containment will be done on a plant specific basis and justified in the reports that apply this evaluation model. It is likely that the backpressure used for fuel reload licensing will be the same as that of the previous licensing calculations.

4.3.6.2. Reflood Rate Calculations

The evaluation of the LOCA during the refill and reflood phases is done using the REFLOD3B code. The REFLOD3B topical report (BAW-10171) includes a description of the model and the calculational assumptions.

The fuel pin model used in REFLOD3B is described in Section 2.6 of the topical report. All sensible heat (stored energy plus decay heat) in the fuel is placed at the radius corresponding to the average fuel temperature. A constant fuel conductivity and gap conductance are used in the model. The variables used will be chosen conservatively to maximize heat removal from the fuel.

The cladding surface heat transfer coefficients for REFLOD3B are calculated using correlations developed from FLECHT data. The correlations are described in Appendix F of BAW-10171.

4.3.6.3. Accumulator Tank Cover Gas

The effect of the gas discharged after the accumulator water is discharged is a slight increase in downcomer pressure and a momentary increase in the flooding rate. Hence, the effect of the core flood tank cover gas will be neglected.

The effect of dissolved gases in the injected accumulator water on the downcomer density is also negligible. Of the gas initially dissolved in the accumulator water, the amount above the solubility limit of water at the downcomer condition will separate from the solution upon injection, remain in the upper region of the downcomer, and be passed out of the downcomer through the break. The gas remaining in solution has no effect on the downcomer so long as it remains in solution. Should this gas separate from the solution in the lower region of the downcomer it will have a small, negligible impact on the fluid

density. Solubility curves can be used to yield

$$V_s = K(T)P_g,$$

where

$P_g$ = partial pressure of nitrogen,

$V_s$ = dissolved gas volume in water at standard pressure and temperature per unit mass of water, and

$K(T)$ = rate constant depending on temperature T.

Assuming ar ideal gas law, the volume ratio between two different states is given by

$$\frac{V_2}{V_1} = \frac{P_1 T_2}{P_2 T_1}.$$

Thus, the volume of the dissolved gas in the downcomer at the downcomer conditions is

$$V_g = \frac{K(T)P_s T_g}{T_s},$$

where

$P_s$ = 14.7 at standard pressure, psia,

$T_s$ = 530 at standard pressure, R, and

$T_g$ = downcomer temperature, R.

Thus, the reduced downcomer density (if this gas comes out of solution) is

$$\rho = \left[\frac{62.4}{1 + \frac{K(T)(14.7)T_g}{530}}\right], \text{ lbm/ft}^3.$$

Assuming that the nitrogen gas is in thermal equilibrium with the downcomer water, $T_g = T_{H2O}$, in the range $T_{H2O}$ = 70 to 280 F, K(T) is approximately constant and equal to 0.001. Using a value of $T_g$ = 280 F results in the maximum density decrease, which is given by

$$\frac{\Delta\rho}{\rho} = \frac{K(T)(14.7)(280 + 460)}{530} = 0.0205.$$

This is a negligible decrease in downcomer density. Thus, the effect of dissolved gases in the accumulator water need not be considered in REFLOD3B calculations.

4.3.6.4. Steam Venting and Steam-Water Interaction

As part of the REFLOD3B input, flow resistance factors (k-factors) for friction and form losses in the loop at steady-state flow are provided. For transient flow, the friction resistance factor is adjusted by the ratio of the friction factor for the transient flow to that of the steady-state flow. The friction resistance factors are modified by Martinelli-Nelson two-phase multipliers which are a function of upstream quality and pressure and are given in Appendix B of BAW-10171. These values are curve fitted using a third degree polynomial surface fit to determine values between stored entries. The form resistance factor remains constant throughout the transient.

To account for the momentum loss due to the steam-water interaction near the point of ECC injection in the cold legs, a constant pressure drop of 0.85 psi during the accumulator injection period and 0.5 psi during the pump injection period will be used.

The pump rotors are assumed to be locked during the reflood phase of the accident (see Section A.2.4). The resistance values used are determined from vendor-supplied zone maps using a zero speed constraint. The values will reflect the direction of flow.

The leak flow calculation during reflood is discussed in Section 2.8 of the REFLOD3B topical report. A variable containment backpressure is input into REFLOD3B. The values input will be conservative for the PWR under consideration.

The steam flow through the unbroken loop can possibly entrain ECC water. The accumulators will easily condense the steam during the injection period. After the accumulators are empty, only one train of ECCS will be injecting. However, by this time, the downcomer liquid level has reached its maximum height (bottom of the cold leg nozzle), and all excess ECC water is assumed to spill out through the break for cold leg breaks. The spillage will overcompensate for any possible entrainment during this period.

The steam flow from the upper plenum and upper head to the downcomer (such as vessel spray nozzles and leakage paths around the hot leg) is simulated using the vent valve model with the steam flow only option (IVQUAL = 2 as discussed in Section 2.5.3.2 of the REFLOD3B topical report BAW 10171P, Revision 3).

4.3.6.5. Heat Transfer

During the refill period (the time between the end-of-blowdown and the beginning of reflood), the core is considered to undergo an adiabatic heatup. This assumption is conservative for thermal analysis. The ECC water transport time delay, that accounts for the water injected at the top of the downcomer to fall to the lower plenum, is neglected based on the results from the downcomer separate effect test (Test No. 5) conducted in the full scale Upper Plenum Task Facility (UPTF).[16,17]

Using the flooding rates from REFLOD3B and the fuel pin conditions at the time of core recovery from FRAP-T6-B&W, the BEACH code calculates the fuel pin heatup during the reflood period. The BEACH code is based on the RELAP5/MOD2 reflood heat transfer model. The code is described in topical report BAW-10166.[3] The topical report includes benchmark comparisons to FLECHT-SEASET, CCTF, G-2 and REBEKA-6 tests.

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## 4.4. Compliance of Model

This section shows that the features of the large break evaluation model described in Section 4.3 conform to the required features of the evaluation model set forth in Appendix K. To accomplish this, the organization of this section parallels that of Appendix K, and references are made to the appropriate parts of Section 4.3.

### I. Required and Acceptable Features

#### A. Heat Sources During LOCA

As required, the initial power is considered as 1.02 times the rated power (Section 4.3.2.1). The method by which a series of power shapes will be analyzed effectively encompasses the requirements of this paragraph (Section 4.3.2.2).

#### A.1. Initial Stored Energy of Fuel

The initial fuel temperatures are consistent with an approved steady-state evaluation code (Section 4.3.2.3). The gap model and other variables of concern are determined as a function of burnup and temperature (Sections 4.3.3.3 and 4.3.3.4).

#### A.2. Fission Heat

Section 4.3.2.4 of the model directly incorporates all of the requirements of this section of Appendix K.

#### A.3. Decay of Actinides

The level of actinide heating is higher than that proposed by the ANS standard (Section 4.3.2.5).

A.4. Fission Product Decay

The ANS curve is multiplied by 1.2 (Section 4.3.2.6).

A.5. Metal-Water Reaction Rate

Section 4.3.2.8 of the model directly incorporates all of the requirements of this section of Appendix K.

A.6. Reactor Internals Heat Transfer

Section 4.3.2.9 of the model directly incorporates all of the requirements of this section of Appendix K.

A.7. Pressurized Water Reactor Primary-to-Secondary Heat Transfer

Section 4.3.2.10 of the model directly incorporates all of the requirements of this section of Appendix K.

B. Swelling and Rupture of Cladding and Fuel Rod Thermal Parameters

Sections 4.3.3.1 to 4.3.3.4 of the model directly incorporate all of the requirements of this section of Appendix K.

C. Blowdown Phenomena

C.1. Break Characteristics and Flow

C.1.a. Spectrum of Breaks

A spectrum of breaks will be analyzed (Sections 4.2, 4.3.2.2 and 4.3.4.1).

C.1.b. Discharge Model

The discharge model conforms to the conditions of this paragraph (Section 4.3.4.1). The required series of discharge coefficients will be applied to double-ended and split ruptures (Section 4.3.4.1). The possible worst-case smaller break is allowed for by analyzing a series of breaks.

C.1.c. End-of-Blowdown

The time when the vessel side break flow is negative.

C.1.d. Noding Near Break and ECCS Injection Points

The areas have been investigated for spatial convergence (Section 4.3.1).

C.2. Frictional Pressure Drops

Section 4.3.4.4 of the model directly incorporates all of the requirements of this section of Appendix K.

C.3. Momentum Equation

All terms of the momentum equation are included in the RELAP5/MOD2-B&W and REFLOD3B codes (Section 4.3.4.3).

C.4. Critical Heat Flux

The correlations employed are acceptable as set forth in Section C.4.c (Section 4.3.4.8). A conservative approach is used with respect to the condition of return to nucleate boiling following calculation of CHF (Section 4.3.4.9).

C.5. Post-CHF Heat Transfer Coefficient

The correlations and method of application used for post-CHF heat transfer are acceptable (Section 4.3.4.9).

C.6. Pump Modeling

The pump model conforms to the requirements of this paragraph in that it is dynamic, obtained from experimental data, and contains allowance for two-phase degradation (Section 4.3.4.5).

C.7. Core Flow Distribution During Blowdown

The hot region is taken as one fuel assembly for hydrodynamic modeling (Section 4.3.1). Crossflow and possible bundle blockage are considered (Section 4.3.4.6). The hot region flow is smoothed by an appropriate filter (Section 4.3.4.6). The enthalpy of the hot region is determined with flow diversion effects included (Section 4.3.1).

D. Post-Blowdown Phenomena -- Heat Removal by ECCS

D.1. Single Failure Criterion

The selected treatment of this criteria will maximize the calculated cladding temperature based on appropriate sensitivity studies (Section 4.3.5).

D.2. Containment Pressure

A conservative containment pressure will be used to underpredict the pressure response (Section 4.3.6.1).

D.3. Calculation of Reflood Rate for PWRs

Reflood rates are calculated by a dynamic code that fully accounts for the transient properties of the system and the fluid. The pump rotor is modeled in its most resistive condition (Section 4.3.6.4). The carryover rate fraction has been developed from the FLECHT-SEASET experiments (Section 4.3.6.2). The effects of core cover gas have been considered.

D.4. Steam Interaction with ECC Water in PWRs

Steam interaction has been considered by increasing the resistance to flow for ECC water entering the reactor vessel (Section 4.3.6.4).

D.5. Refill and Reflood Heat Transfer for PWRs

During refill the heatup is assumed to be adiabatic (Section 4.3.6.5.). The correlations used for hot channel reflood heat transfer have been compared with the FLECHT-SEASET data (see Appendix C of BAW-10166).

FIGURE 4-1. LARGE BREAK ANALYSIS CODE INTERFACE.

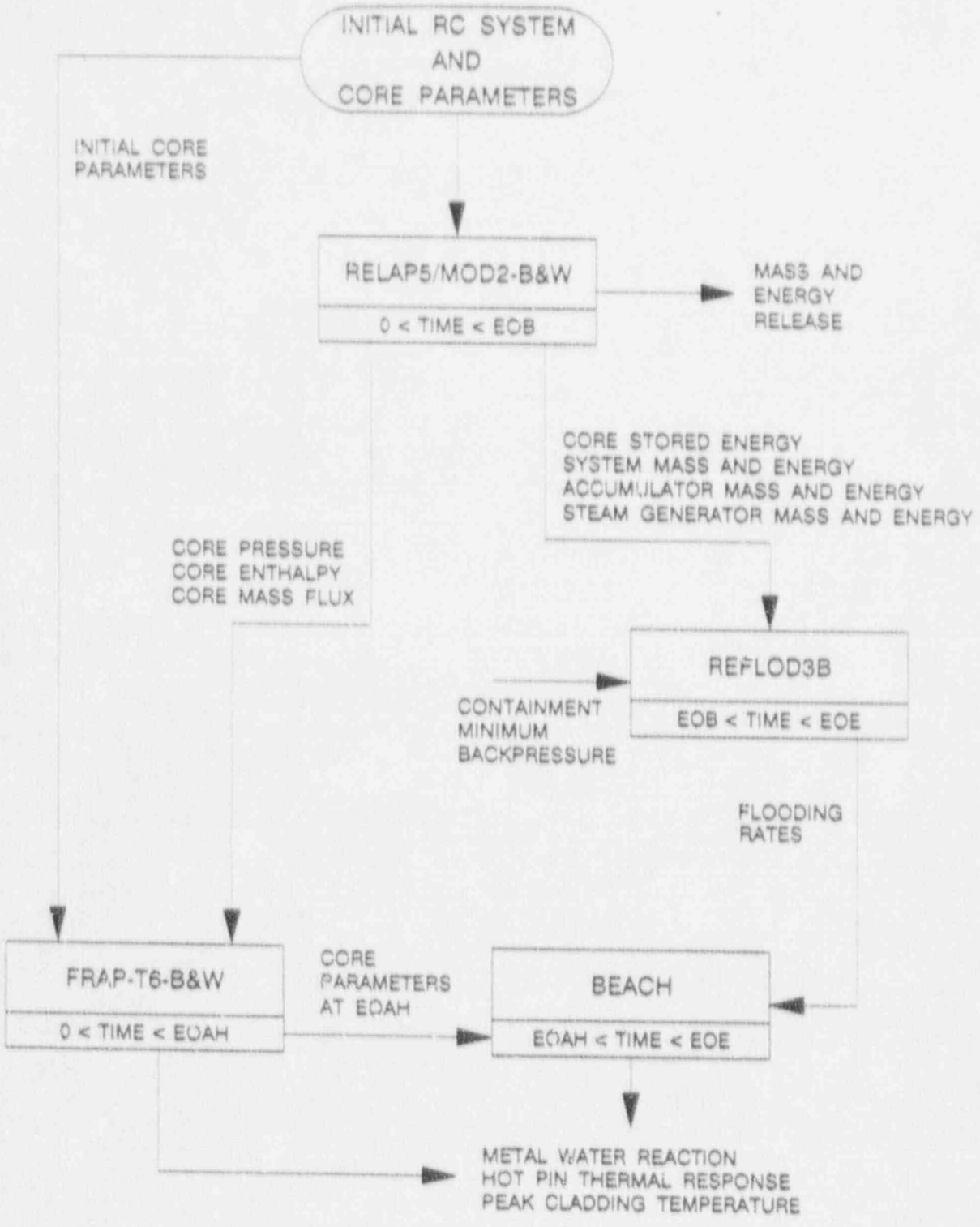

Figure 4-2. RELAP5/MOD2-B&W Noding Diagram -- Loop.

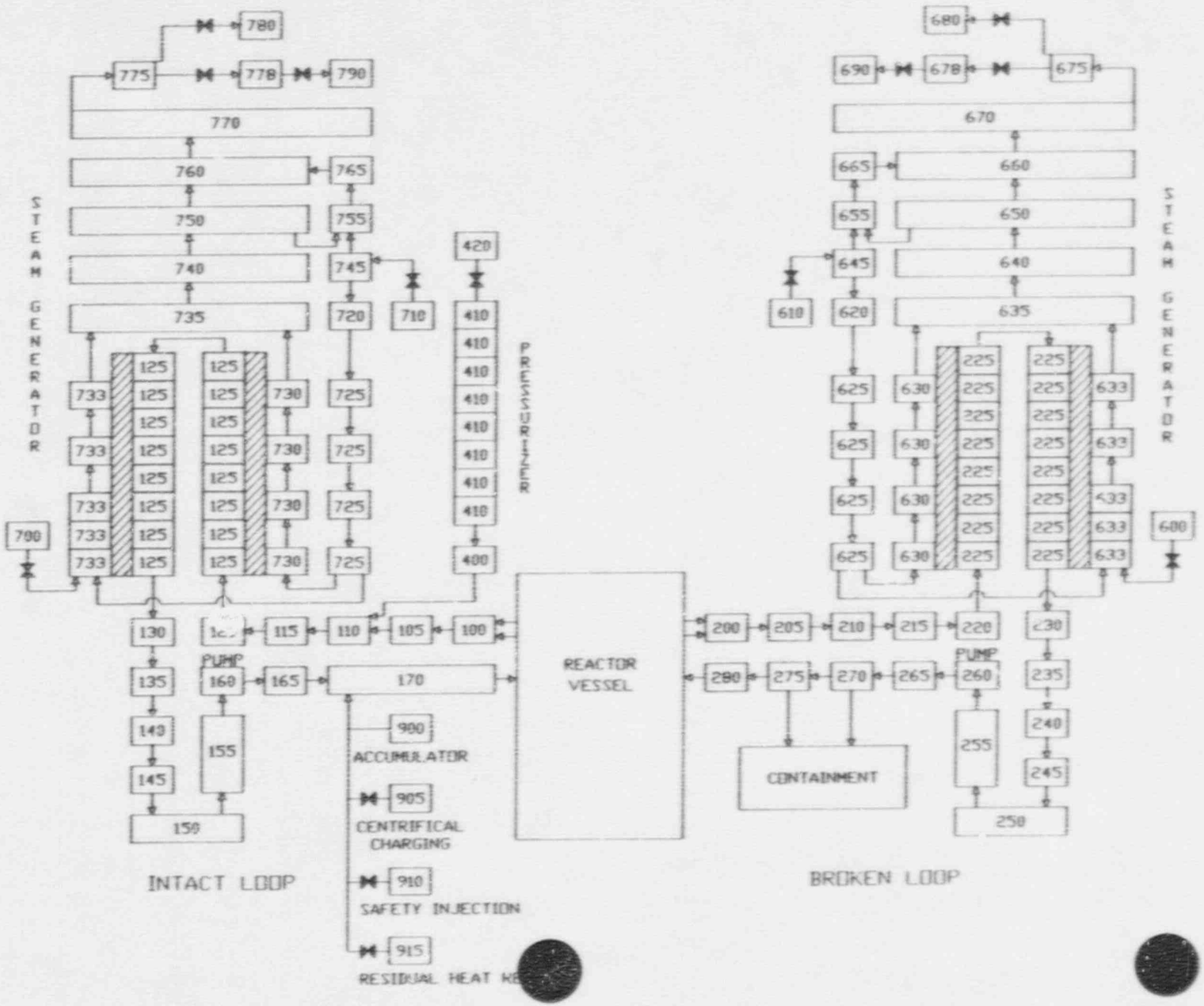

Figure 4-3. RELAP5/MOD2-B&W Noding Diagram -- Reactor Vessel.

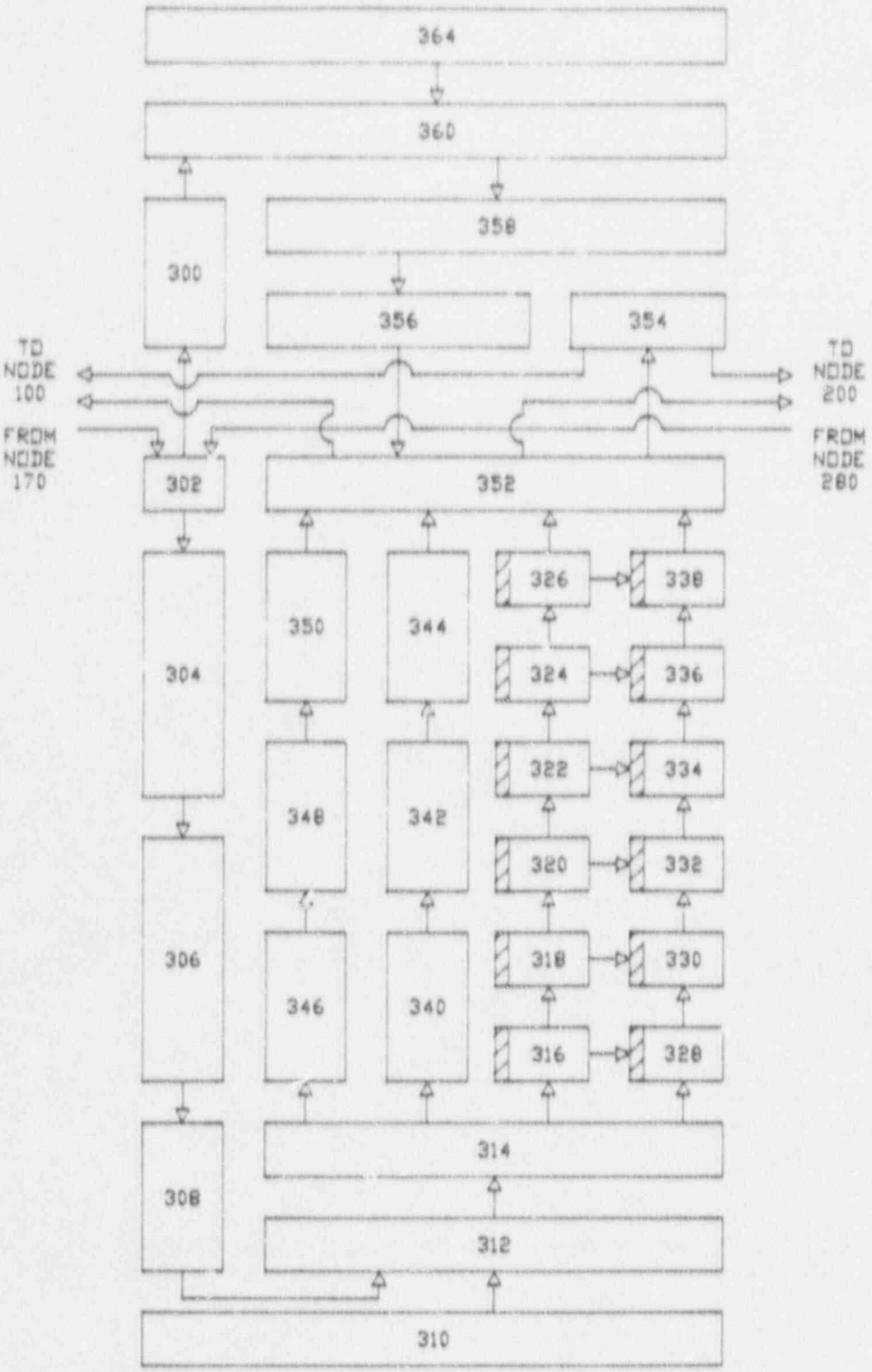

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Figure 4.5. Decay Heat Curve.

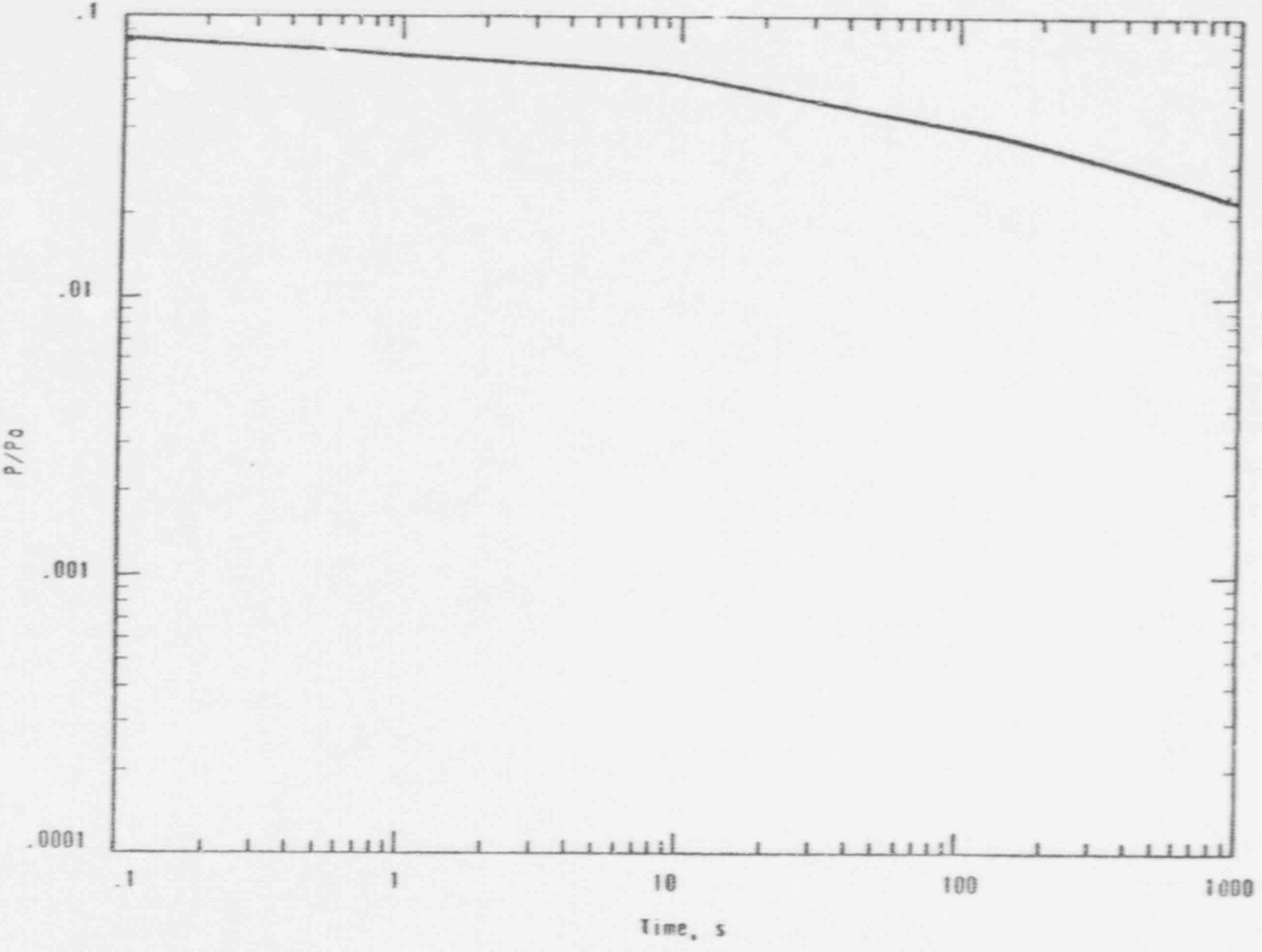


Figure 4.6. Outside Weight Gain Oxidation Curves for Zircaloy - 4.

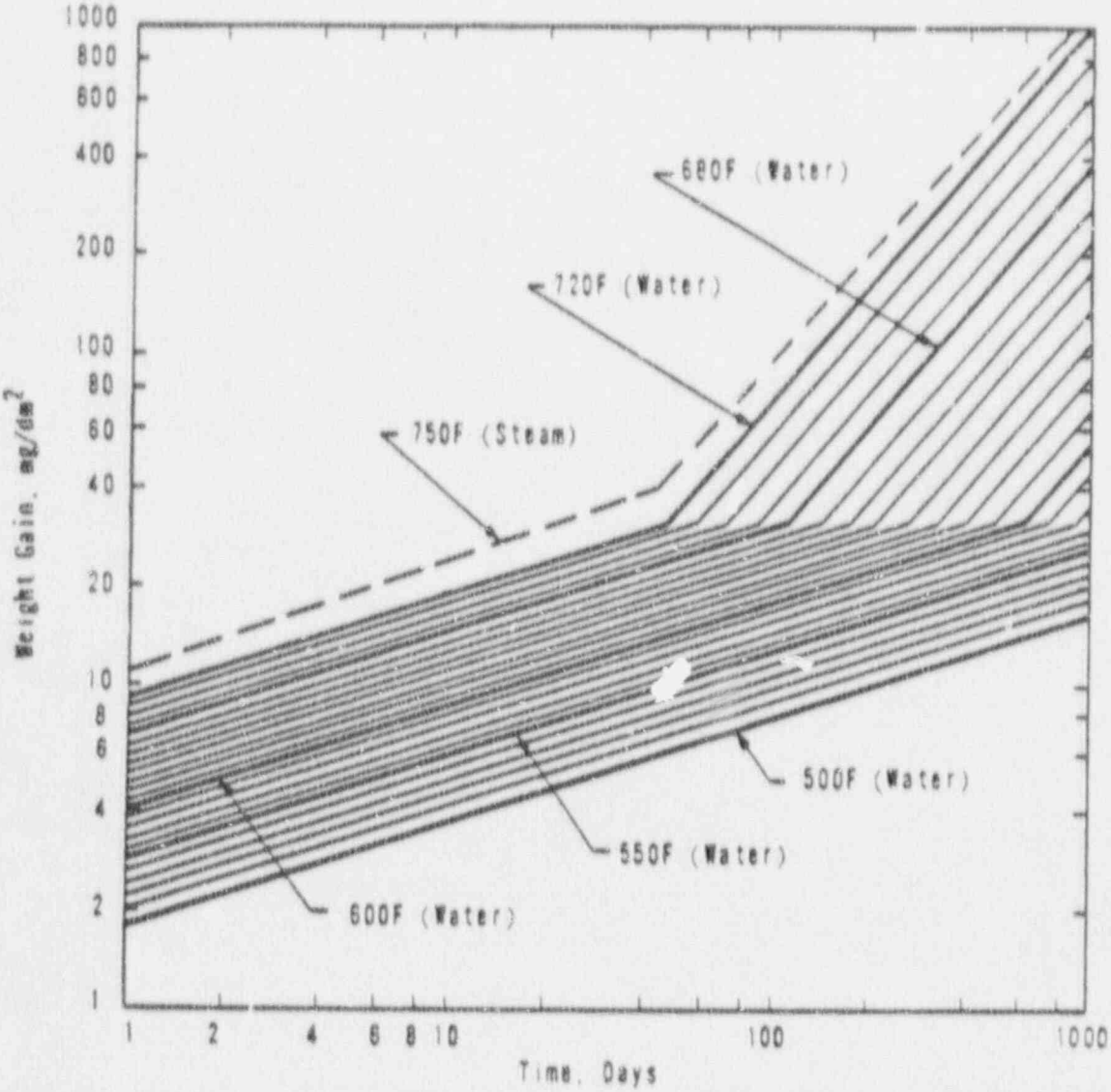


Figure 4.7. Inside Weight Gain Oxidation for Zircaloy - 4 in Water and Steam.

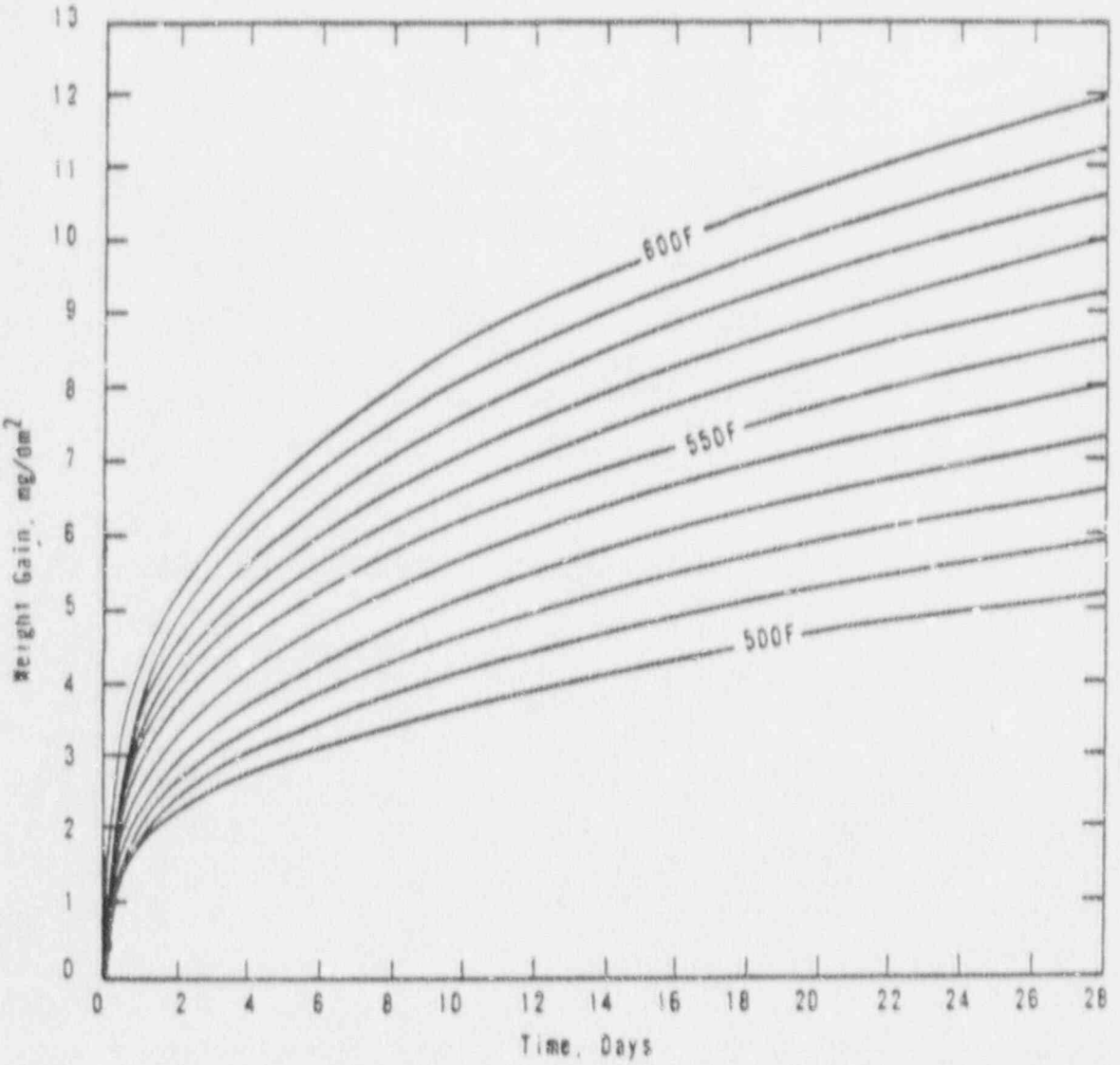


FIGURE 4-8. INSIDE WEIGHT GAIN OXIDATION FOR ZIRCALOY-4 RESULTING FROM FISSIONING.

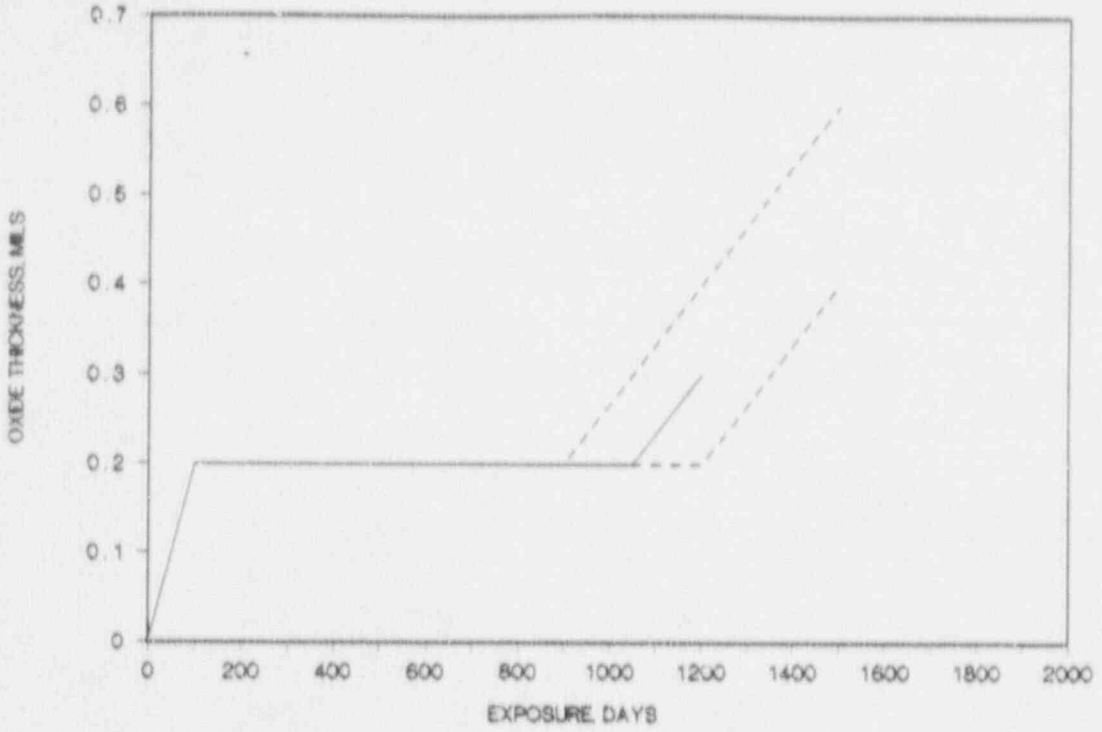



FIGURE 4-9. RUPTURE TEMPERATURE AS A FUNCTION OF ENGINEERING HOOP STRESS AND RAMP RATE.

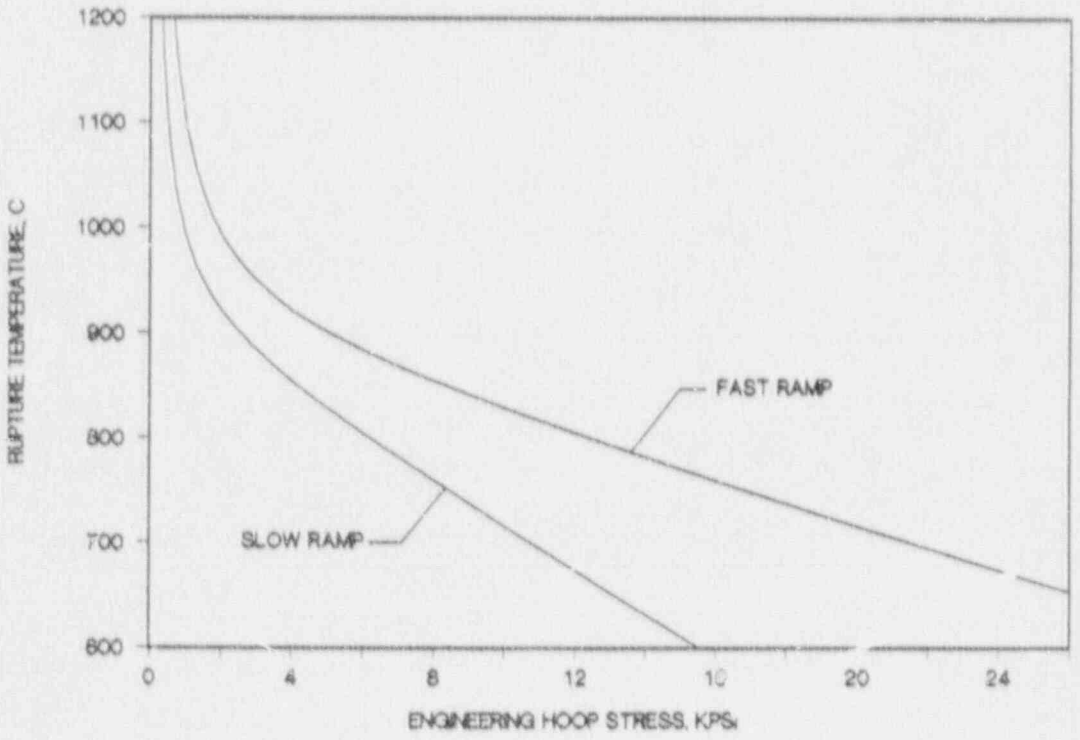


FIGURE 4-10. CIRCUMFERENTIAL BURST STRAIN AS A FUNCTION OF RUPTURE TEMPERATURE.

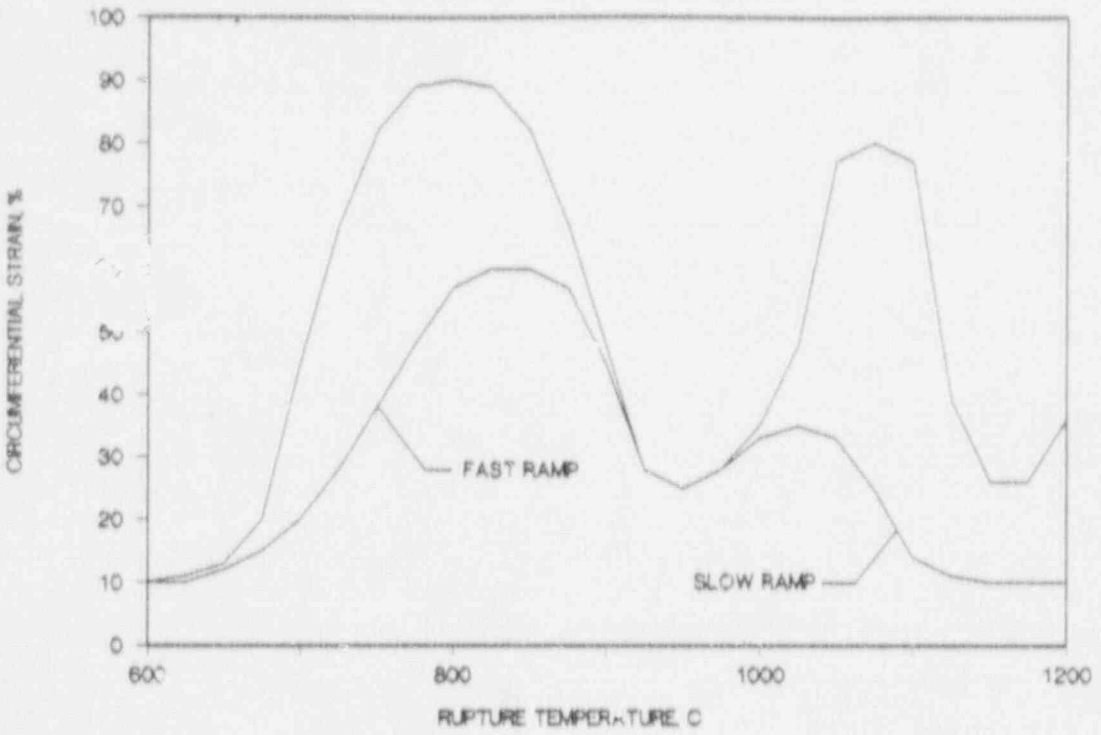
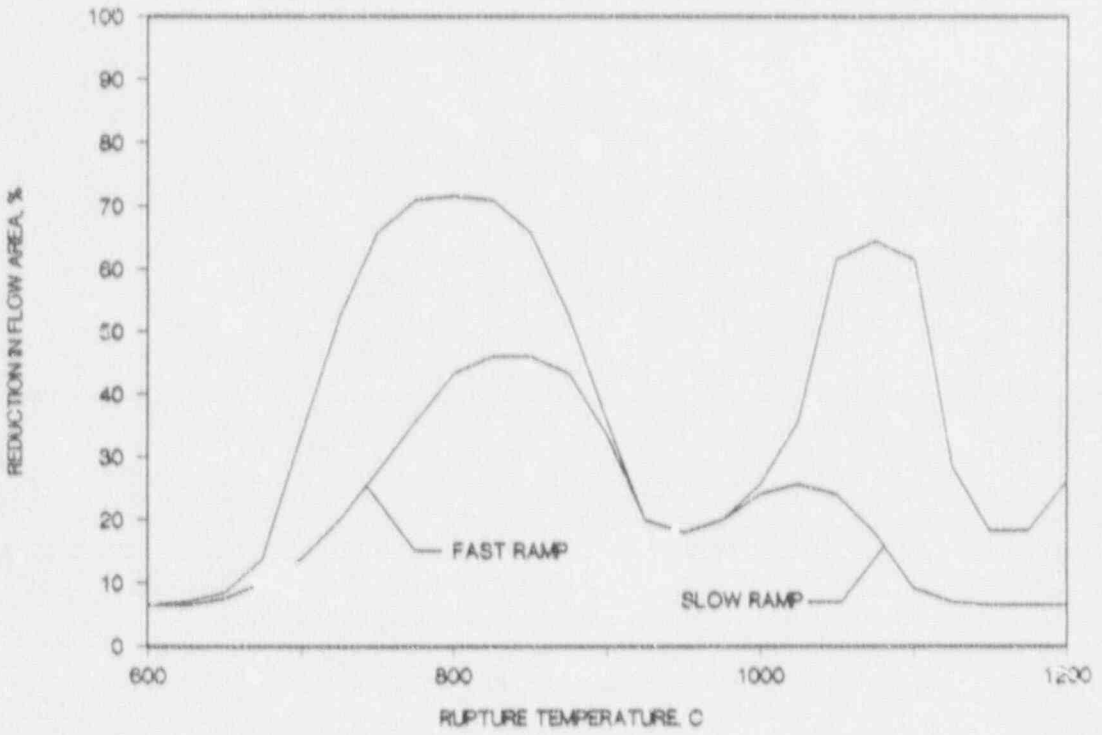



FIGURE 4-11. REDUCTION IN FLOW AREA AS A FUNCTION OF RUPTURE TEMPERATURE.



# 5. LOCAL CLADDING OXIDATION

Criterion 3 of 10CFR50.46 requires that the maximum local degree of cladding oxidation not exceed 17 percent. Compliance to this criterion is obtained by evaluating the results of the calculation for peak cladding temperature. In the calculation, local cladding oxidation is computed as long as the cladding temperature remains above 1500 F and the REFLOD3B analysis shows that the mixture covered region of the core is below the top of the cladding. This region is conservatively taken to be twenty percent above the REFLOD3B predicted core collapsed liquid level. The amount of oxide thickness at each location is computed on a mass basis.

# 6. MAXIMUM HYDROGEN GENERATION

Criterion 3 of 10CFR50.46 states that the maximum amount of cladding oxidation during a LOCA shall be limited to 1 percent of the whole core cladding. The demonstration of compliance to this criterion is accomplished by calculating the amount of core-wide oxidation that occurs for each of the power shapes used in the LOCA limits study to be less than 1 percent.

The determination of whole core oxidation for each power shape is done by an extension of the hot channel analysis performed for that shape. The amount of metal-water reaction at any position in the core is determined by a ratio of adjusted power times the metal-water reaction, at the same elevation, which occurred during the hot channel analysis. In equation form:

$$O_X\,(x,y,z) = O_X\,(z)_{hc} \cdot \frac{P_{adj}(x,y,z)}{P_{adj}(z)_{hc}} \quad ,$$

where

$O_X(x,y,z)$ = the increase in oxidation at location (x,y,z) in the core,

$O_X(z)_{hc}$ = the increase in oxidation which occurred at elevation z in the hot channel calculation,

$P_{adj}(x,y,z)$ = the adjusted power at location (x,y,z) in the core, and

$P_{adj}(z)_{hc}$ = the adjusted power at elevation z in the hot channel calculation.

The adjusted power is the power in excess of the minimum power for which any oxidation increase occurs. For the evaluations presented herein, the minimum power at which oxidation is

observed is 3 kw/ft. Therefore,

$$P_{adj}(x,y,z) = P(x,y,z) - 3 ,$$

where

P = local power in kw/ft.

An integration over the core will then give the core wide oxidation increase or the amount of hydrogen generation.

Conservatism in the evaluation comes from three sources: (1) the amount of oxidation in the base run is computed using the Baker-Just correlation which is recognized to be about fifty percent conservative; (2) the core power distribution is a reasonable and representative distribution which has been pushed to the peak powers allowed by plant Technical Specifications; and (3) the use of a power ratio to determine the degree of oxidation for lower power zones is conservative. Since the oxidation rate increases exponentially with temperature, a decrease in power can be expected to produce a greater than proportional decrease in oxidation.

To demonstrate the expected range of results of the technique it was applied to the most severe large break LOCA transient (Section A.3.10). The amount of core wide metal-water reaction that is predicted for this case is 0.43 percent, a value well within the 1 percent criterion of 10CFR50.46. Of course, during an actual application, the technique would be applied to a range of power shapes of differing axial peak locations in order to demonstrate compliance to 10CFR50.46 for all power distributions.

# 7. COOLABLE GEOMETRY

Criterion 4 of 10CFR50.46 states, "Calculated changes in core geometry shall be such that the core remains amenable to cooling." The analysis for predicting the peak cladding temperature uses the hottest fuel pin in the core. This analysis includes the effect of clad swelling and flow blockage based on NUREG-0630 and shows that the peak temperature remains below 2200F. The combination of core geometry, coolant flow, and local power at all other locations in the core produce transient cladding temperatures which peak below that of the fuel pin analyzed in the peak cladding temperature calculation. Therefore, these pins and assemblies also remain in a geometry "amenable to cooling."

# 8. LONG-TERM COOLING

Criterion 5 of 10CFR50.46 states, "A low core temperature must be maintained following the calculated successful initial operation of the ECCS and that decay heat must be removed for an extended period of time." This section describes the assumptions used to meet this condition.

## 8.1. Establishment of Long-Term Cooling

The analysis of a LOCA is continued until the cladding temperature at all locations in the core is decreasing, and the fluid level in the core is rising. At this time the path to long-term cooling is established. The fluid within the core will continue to rise and the cladding at all elevations quench without further excursions. Once quenched, the core is maintained within a few degrees of the coolant temperature thorough a continuous flow of water maintained by the ECCS.

Procedures have been established and approved by the NRC for the smooth transition to long-term cooling during which water is recirculated from the reactor building sump through a heat exchanger to the reactor vessel. For hot leg breaks, the establishment of recirculation is the final requirement for long term stable cooling. For cold leg breaks, however, coolant supplied to the vessel may not flow to the core. In the most severe case, the core continues to boil for an extended period and only sufficient water to make up for the boiloff is actually passed to the core. Boiling, without throughput of water, will concentrate boric acid. To prevent the crystallization of boric acid within the core, a throughput flow is assured by operator action. The final computation of the ECCS Evaluation Model is to demonstrate that this action is timely, assuring the effective establishment of long-term cooling.

## 8.2. Boric Acid Concentration

Since all ECC systems inject borated water, salts could build up, precipitate, and block core channels during long-term cooling. To prevent this, operator action is taken to establish a flow of water through the core regardless of the type or location of the break. A simple concentration calculation that assumes boiling at the decay heat rate and no core throughput (water passed through the core) is made from the initiation of the event to the time of operator action. The concentration calculated must be shown to be below the saturation limit of boric acid for the core conditions. The rate of concentration at the time of operator action is shown to be less than the loss of boric acid caused by the throughput flow. This assures that the concentration will thereafter decrease.

# 9. REQUIRED DOCUMENTATION

This section verifies compliance with the documentation requirements (Part II) of the Appendix to 10CFR50 and is arranged according to the section division of Part II.

II. Required Documentation

1.a. The computer codes that form the basis for the ECCS evaluation model are described in the following B&W topical reports:

| | |
|---|---|
| RELAP5/MOD2-B&W | BAW-10164[1] |
| FRAP-T6-B&W | BAW-10165[4] |
| REFLOD3B | BAW-10171[2] |
| BEACH | BAW-10166[3] |
| FOAM2 | BAW-10155[10] |

These topical reports include the derivations of the equations used in the codes, starting with fundamental physical laws and including all approximations. Any assumptions made in the derivation of the solution technique used by the code and the value of all code specified parameters are disclosed in these reports. The reports also describe the general application of the codes to applicable problems including potential code interfaces and input parameter selection.

The computer code options used in the Evaluation Model are summarized in Table 9-1 of Volume I. At several locations throughout all of the computer codes, correlations have been programmed to include user input multipliers. These constants shall all have a value of one unless specifically stated to the contrary. Programmed constants that are part of the correlations or part of B&W's implementation of the correlations will have the value as published in the individual code topical reports.

1.b. Any changes in the ECCS evaluation model that result in a deviation of more than 50 F in the calculated cladding temperature transient will be documented by appropriate amendments to the evaluation model description.

1.c. B&W computer codes, including their source coding, are controlled through a systematic process that has been audited and approved by the NRC. Each code is tracked by a unique name, version number, and revision level. Authorization to change an approved version of a code involves a multiple review and approval process. The source listing of the current or past approved versions of the ECCS codes can be made available to the NRC, at B&W's offices in Lynchburg, Virginia, upon their request.

Inputs used during an evaluation can be categorized as follows:

Generic: User supplied values or constants whose values are controlled by the evaluation model. The materials properties are a good example of this type of input.

Prescribed: Input for which a determining procedure is specified in the evaluation model without the specification of a value. The use of hot fluid volumes within the RELAP5/MOD2 model is a good example of this type of input.

Plant: Input which is taken from documentation for the individual plant or plants to be covered by the evaluation. Plant geometry inputs are examples of this type of input.

Case: Input which will vary depending on the accident being evaluated. Break area is a good example of this type of input.

The B&W evaluation model controls these different types of inputs in differing ways. Generic and prescribed inputs are controlled in the same fashion as code options, and are documented in context within the evaluation model report. A summary of the generic and prescribed inputs is given in Table 9.2 of Volume I.

Plant input is controlled by B&W internal calculational procedures and not by the evaluation model. These procedures are written to adhere to ANSI quality assurance standards. For the most part these procedures require that the input come from controlled design documentation, that it be referenceable, that its use be documented, and that an independent review be conducted to assure that this has been done. The documents attesting to this for any given evaluation are controlled documents, maintained at B&W's offices in Lynchburg, Virginia, and are available for audit upon request.

Case input is similar to assumptions. It is not controlled by either the evaluation model or B&W procedures other than that it must be documented along with the plant input in controlled stored records of the calculation. Case input is also available for audit upon request.

2. Convergence of solution techniques is demonstrated in Appendix A of this topical report. System modeling and noding are described in Section 4.3.1 of this report. Time step selection is presented in Appendix A of this report.

3. Appropriate sensitivity studies using the EM methodology

described in Revision 0 of this report are presented in Appendix A of this topical report. The impact of Revision 1 of this report on the sensitivity studies is discussed in Appendix B. Additional sensitivity studies, which must be performed for each specific plant category, will be documented as part of the specific analysis presented for that category.

4. Predictions and experimental data are being compared continually to the evaluation model and portions of it. Code topical reports contain comparison to applicable experimental data as appropriate.

5. The B&W topical reports listed in Section 9.II.1.a which, along with this report, describe the ECCS evaluation model, provide the technical basis for the adequacy of the computational methods as well as compliance with 10CFR50. The evaluation model description provides sufficient flexibility that it is applicable to all the plant categories presented in Table 1-1.

Table 9-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model

| OPTION | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Flow Film Boiling Lock-In | No lock-in until $T_w - T_s \geq 300$ F |
| Fine Mesh Rupture Option | Not used |
| Critical Flow Model | Subcooled - Ext. Henry-Fauske<br>Two-phase - Moody<br>Superheat - Murdock-Bauman |
| Internal Critical Flow | Flow not limited except for special paths: downcomer to reactor vessel upper head bypass path, pressurizer surgeline, & accumulator lines |
| Friction | Calculated by RELAP5 |
| Homogeneous-Equilibrium | Only in the core region |
| Heat Transfer Model | Core model is used for the active core heat structures & the System model is used elsewhere |
| CHF & Film Boiling Correlation | The correlation combination with BWCMV for high pressure and high flow is used. Condie-Bengston IV is selected for flow film boiling |
| Metal-Water Reaction Model | Baker-Just |
| Rupture Temperature | A plastic weighted, time averaged ramp rate is used |

Table 9-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| FRAP-T6-B&W | |
| Flow Film Boiling Lock-In | No lock-in until $T_w - T_s \geq 300$ F |
| CHF & Film Boiling Correlation | The correlation combination with BWCMV for high pressure and high flow is used. Condie-Bengston IV is selected for flow film boiling |
| Metal-Water Reaction Model | Baker-Just |
| Rupture Temperature | A plastic weighted, time averaged ramp rate is used |
| Licensing Audit Code Models | All of the Licensing Audit Code Models are used except for:<br>Cladding Specific Heat,<br>Elastic Modulus,<br>Fuel Deformation,<br>Operating Power x 1.02, and<br>ANS Decay Power x 1.20 |
| Clad Deformation | No Balloon Option, clad failure occurs on rupture curves |

Table 9-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Con'inuec

| OPTION | SELECTION |
|---|---|
| REFLOD3B | |
| CRF/Core at Transfer | CRFCKN option: CRFCKN correlation for carry out coupled with FLECHT/ANC correlation for core heat transfer |
| Core Bypass | The core bypass model is selected |
| Vent Valve Model | The steam only option of the vent valve model maybe selected by user to simulate downcomer to upper head bypass flow. Vent valve steam condensation efficiency in Equation 2-68.4 of the REFLOD3B topical is set to 1.0 |

Table 9-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| BEACH | |
| Friction | Smooth pipe option |
| Reflood | Reflood option selected |
| Fine Mesh Rupture Option | Selected |
| Concentric or Non-concentric Fuel Pellet Simulation | Use Concentric with TACO2<br>Use Non-concentric with TACO3 |
| Metal-Water Reaction Model | Baker-Just |
| Rupture Temperature | A plastic weighted, time averaged ramp rate is used |
| Non-Equilibrium and Non-Homogeneous | Yes |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

| INPUT | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Fluid Volumes | Hot - from design drawings |
| Attached Piping Volumes | Only the accumulator line volumes are included. The total of other attached piping volumes lies within the accuracy of the system volume calculation and are not included |
| Initial Reactor Coolant System Flows | The system flows are those used in the at power minimum DNB analyses. The hot and cold leg temperatures are set by nominal control system response to that RCS flow |
| Two-Phase Pump Degradation | Default option |
| Initial Inventories for Reactor Coolant System, Secondary System, and ECCS Systems Pressure | Set by nominal operation design levels. The volumes for attached piping except for the pressurizer surgeline are not included in the LOCA model |
| Primary Metal | Structures are lumped together by material properties, thicknesses, and location. Grouping is user controlled |
| Over Power Factor | 2% applied to both average and hot assembly powers |
| Power Flattening | 5% of the heat generated in the hot assembly is deposited directly in coolant. This factor is not applied to the average core |
| Decay Heat | 120% ANS 1971 based on 102% core power. Actinide power is 100% ANS 1979 |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Initial Fuel Temperatures | Adjusted to agree with an NRC approved steady-state fuel performance code (TACO2/TACO3) |
| Rupture Data | NUREG-0630 ramp rate dependant data |
| Time Step Control Option | Option 3, mass error checking, consistent hydrodynamic and heat structure solution time advancement |
| ECCS Fluid Temperatures | Set at nominal year average temperatures per system |
| ECCS Time Delays | Includes provision for signal, diesel start-up, pump start-up, and line filling for all evaluations |
| Containment Pressure | Set from FSAR |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| FRAP-T6-B&W | |
| Power Flattening | 5% of generated heat deposited directly in coolant |
| Over Power Factor | 2% applied |
| Power Distribution | Appropriate for the average fuel pin in the highest powered fuel assembly in the core. Axial peaking is the same as for RELAP5/MOD2-B&W evaluation and the radial peak is the relative hot assembly power |
| Decay Heat | 120% ANS 1971 based on 102% core power. Actinide power is 100% ANS 1979 |
| Rupture Data | NUREG-0630 ramp rate dependant data |
| Initial Fuel Temperatures | Adjusted to agree with an NRC approved steady-state fuel performance code (TACO2/TACO3) |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| REFLOD3B | |
| Unrecoverable Loss Factors | Input separately from friction loss factors |
| Loss Factors Source Fluid Volumes | RELAP5/MOD2-B&W steady state Hot - from RELAP5/MOD2 model |
| ECCS Time Delays | Includes provision for signal, diesel start-up, pump start-up, and line filling for all evaluations |
| Containment Pressure | Set from FSAR |
| Decay Heat | 120% ANS 1971 based on 102% core power. Actinide power is 100% ANS 1979 |
| RC Pump Resistance | Appropriate for locked rotor condition |
| ECCS Fluid Temperatures | Set at nominal year average temperatures per system |
| Primary Metal | Primary metals are included with structures lumped together by material properties, thicknesses, and location. Upper head metal is modelled in the RCS hot legs. Grouping selections are user controlled |
| Cold Leg Steam Water Interaction Pressure Drop | 0.85 psia during accumulator injection and 0.5 during pumped injection (once accumulator injection stops) |
| System Initialization | Core and upper head regions are saturated steam, hot legs and steam generators are superheated steam |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| REFLOD3B | |
| Constants in CRFCKN Option | $C_{SUP} \geq 1.025$ (-), $C_T = 1.0$ (1/s), and $C_H = 1.0988$ (1/ft) |
| ECC Water Transport Time Delay | 0.0 |

Table 9-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| BEACH | |
| Form Loss Factors | From assembly tests |
| Power Flattening | 5% of generated heat outside of fuel pin |
| Rupture data | NUREG-0630 ramp rate dependant data |
| Number of Fine Mesh Points for Conduction | 32 |

## 10. REFERENCES


1. J. A. Klingenfus, et al, "RELAP5/MOD2-B&W -- An Advanced Computer Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis," BAW-10164, Babcock & Wilcox, Lynchburg, Virginia.

2. C. K. Nithianandan. "REFLOD3B -- Model for Multinode Core Reflooding Analysis," BAW-10171, Babcock & Wilcox, Lynchburg, Virginia.

3. N. H. Shah, "BEACH -- A Computer Program for Reflood Heat Transfer During LOCA," BAW-10166, Babcock & Wilcox, Lynchburg, Virginia.

4. J. H. Jones, "FRAP-T6-B&W -- A Computer Code for the Transient Analysis of Light Water Reactor Fuel Rods," BAW-10165, Babcock & Wilcox, Lynchburg, Virginia.

5. Y. H. Hsii, et al, "TACO2 Fuel Pin Performance Analysis," BAW-10141A, Babcock & Wilcox, Lynchburg, Virginia.

6. E. N. Aqua, "Corrosion and Hydriding of Zircaloy Under Heat Transfer Conditions," WCAP-3269-56, Westinghouse, August 1967.

7. Pressurized Water Reactor (PWR) Project -- Technical Progress Report, Oct. 24, 1963 - Jan. 23, 1964, WAPD-MRP-107, Westinghouse (1964), p 88.

8. C. G. Dideon and G. M. Bain, Fuel Performance Under Extended Burnup Operation B&W 15x15 Design, BAW-1716, Babcock & Wilcox, Lynchburg, Virginia.

9. D. A. Powers and R. O. Meyer, "Cladding Swelling Models for LOCA Analysis," NUREG-0630, April 1980.

10. N. H. Shah, et al, "FOAM2 -- Computer Program to Calculate Core Swell Level and Mass Flow Rate During Small-Break LOCA," BAW-10155, Babcock & Wilcox, Lynchburg, Virginia.

11. B. M. Dunn, et al, "B&W's ECCS Evaluation Model Report with Specific Application to 177 FA Plants with Lowered Loop Arrangement," BAW-10091, Babcock & Wilcox, Lynchburg, Virginia, August 1974.

12. C. E. Beyer, et al, "GAPCON-THERMAL-2: A Computer Program for Calculating the Thermal Behavior of an Oxide Fuel Rod," BNWL-1898, November 1975.

13. L. W. Newman, The Hot Cell Examination of Oconee 1 Fuel Rods After Five Cycles of Irradiation, BAW-1874, Babcock & Wilcox, Lynchburg, Virginia.

14. Duke Power Company, Catawba Nuclear Station, Final Safety Analysis Report, Volume 12, Section 15.0, p. 15.0-13.

15. D. A. Wesley and K. J. Firth, "TACO3 - Fuel Pin Thermal Analysis Code," BAW-10162, Babcock & Wilcox, Lynchburg, Virginia.

16. Quick Look Report on "Upper Plenum Test Facility (UPTF) Test No. 5: Downcomer Separate Effect Test," U9 316/87/17, Siemens AG, UB KWU, Erlanger, October 1987.

17. M. G. Izenson and C. J. Crowley, "Scaling of Flashing Transient Behavior to Full Scale - Evaluation of UPTF Test 5A," Technical Memorandum TM-1277, CREARE Inc., Hanover, NH 03755, September 1988.

# APPENDIX A
## Sensitivity Studies

## A.1. Introduction

Numerous sensitivity studies were performed with the evaluation model described in Revision 0 of this report to demonstrate its acceptability to analyze LOCA, to provide a basis for the selection of input parameters, and to satisfy the requirements of Appendix K. The impact of Revision 1 changes on these studies is discussed in Appendix B. In general, the RELAP5/MOD2-B&W and REFLOD3B models were the main subject of these studies. The BEACH code, being-boundary driven with an internal, code-controlled time step and already the subject of extensive benchmarking,[3] was not considered in this report. FRAP-T6-B&W, also being boundary driven, was evaluated relative to model time step and radial fuel noding. The results from RELAP5/MOD2-B&W and REFLOD3B together with the containment pressure time history and the core geometry define the boundary conditions for the remainder of the EM. The containment backpressures, used as input to REFLOD3B, are based on Figure 15.6-49 in the Trojan FSAR.

Sensitivity studies were performed and evaluated based on individual elements of the specific EM application. For example, the RELAP5/MOD2-B&W stand-alone sensitivity studies were performed and evaluated on the basis of the comparison to the base EM results at the end-of-blowdown. The conditions encountered during the blowdown that may significantly alter the other bounding elements of the overall EM were also considered. Spectrum and time-in-life studies were performed. To support those RELAP5/MOD2-B&W calculations, additional individual sensitivity study calculations were performed and the results are presented within this appendix. These studies support the basic assumptions for the EM models.

Many individual cases were analyzed using RELAP5/MOD2-B&W. This report presents the following studies: time step, loop noding arrangement, pressurizer location, leak noding arrangement, pump

degradation model, core crossflow resistance, and core spacial noding detail studies. The base or reference case for these studies was a double area (2A) guillotine break in the cold leg piping between the reactor vessel inlet nozzle and the pump discharge nozzle with a $C_D$ of 0.6. This break geometry and location were chosen based on being the most severe LOCA reported in previous licensing calculations for a Westinghouse 4-loop plant. Although different licensing codes and models could predict the same worst break conditions, it is also possible that variation in the value of the $C_D$ that determines the worst break may occur. (This is one of the purposes of varying $C_D$ in the spectrum analysis.) However, since the relative sensitivities to the parameter variations should be similar for all 2A guillotine, pump discharge breaks regardless of the discharge coefficient, the trends observed and conclusions drawn for the base case studies will apply across the spectrum of discharge coefficients.

## A.2. Initial Sensitivity Studies

Following development of the initial base RELAP5/MOD2-B&W input model shown in Figures A-1 and A-2, the model was executed to determine sensitivity to various parameters. The initial model was subsequently modified. Nonetheless, those studies using the initial model that remain valid are reported in this section. For these studies comparisons of four variables--primary system pressure, leak mass flow rates, and the core hot channel flow rate at the peak power location--were made. End-of-blowdown (EOB) conditions, EOB time, liquid volume remaining in the reactor vessel and intact cold leg downstream of the accumulator injection point, total integrated intact loop accumulator injection, and total integrated leak mass and energy removal, are also summarized in tables. Similar work, using input from the initial base RELAP5/MOD2-B&W model, was performed on the initial REFLOD3B model and the results of these studies are also presented in Section A.2.

### A.2.1. RELAP5/MOD2-B&W Time Step Study

The first study performed was the reduced time step case. In RELAP5/MOD2-B&W, the user specifies a time step that may be modified by the RELAP5/MOD2-B&W time step control due to convergence or Courant limitations. The base case generally used the maximum time step as specified by the user. To verify that the code convergence criteria were adequate, a case was executed with a time step that was one-half that which the base case used. The comparison of the results from the base and reduced time step cases are shown in Figures A-3 through A-6 and in Table A-1. The reduced time step results for the system pressure, leak mass flow rates, and hot channel mass flow rates essentially replicate the base case blowdown results except during the final three seconds of the transient. During that time, the accumulator and unchoked leak mass flow rate interact to produce the observed rapid flow rate fluctuations. The impact on transient behavior and end-of-blowdown conditions, however, is minimal. These results support the conclusion that the specified base case time steps with the code internal time step control is adequate for the EM applications.

### A.2.2. RELAP5/MOD2-B&W Loop Noding Study

The second sensitivity study performed with RELAP5/MOD-B&W was a variation in the loop noding. The number of control volumes and junctions used within the reactor coolant loops was reduced as shown in Figure A-7. Comparisons against the base case results are shown in Figures A-8 through A-11 and Table A-2. Again, the results replicated the base case predictions for nearly the entire transient, thus verifying the convergence of base case loop noding arrangement for LBLOCA EM applications.

## A.2.3. REFLOD3B Loop Noding Study

A number of REFLOD3B sensitivity analyses were performed with the same RELAP5/MOD2-B&W EOB input conditions from the initial base case (2A/G break at PD w/ $C_D$ = 0.6). These studies were performed to ensure that the base REFLOD3B model meets the requirements of Appendix K. The first sensitivity study performed was loop nodalization. In addition, primary coolant pump flow resistance and the effect of maximum ECCS injection were examined.

In REFLOD3B, the reactor vessel is represented by a fixed number of volumes (4 volumes) as shown in Figure A-12. This noding scheme considers thermal hydraulic behavior occurring in the RV during the reflooding transient and is adequate to predict the core flooding rate as demonstrated in the benchmark of FLECHT-SEASET experiment 33338 (Appendix I, BAW-10[illegible]). The primary piping is represented by two loops similar to the blowdown model. Since the core flooding rate is primarily controlled by the amount of steam that vents through the loop and out the break, the effect of increasing the number of loop volumes on core flooding rate was evaluated. The number of hot and cold leg pipe volumes was doubled as shown in Figure A-13. Flow resistance through the primary coolant pump was based on a locked rotor assumption. A 0.85 psi pressure drop, to account for losses due to steam-water interaction, was imposed on cold leg pipe junctions. A convergent solution is judged on important parameters such as core flooding rate, core and downcomer water levels and carryout rate fraction (CRF). Comparison plots for these parameters are presented in Figures A-14 through A-17. These plots confirm that there is little difference between the base case and the increased loop noding case. Therefore, it is concluded that the loop noding of the base model is sufficiently detailed.

## A.2.4. REFLOD3B Primary Coolant Pump Rotor Resistance Study

In accordance with Section I.D.3 of Appendix K, the effect of primary coolant pump resistance was determined, and the pump rotor condition that leads to the maximum calculated cladding temperature was assumed in the reflooding analysis. To determine the worst pump rotor condition, the base model was used with a pump rotor resistance based on a free spinning condition. The pump resistance in REFLOD3B is input as a form loss coefficient, $K_{Fj}$, to be used in Equation 2-2 in BAW-10171. The pump homologous data in BAW-10164 was used to derive the loss coefficients for the free spinning rotor. A value of $h/\nu^2$ ($h$ = head/rated head and $\nu$ = volumetric flow/rated volumetric flow) can be obtained from the homologous data. The pump pressure drop is given by

$$\Delta P = \frac{\rho g H}{g_c} = C \frac{h}{\nu^2} ,$$

where

$P$ = pressure, psi,
$\rho$ = density, $lbm/ft^3$,
$g$ = gravitational acceleration, $ft/sec^2$,
$g_c$ = conversion factor, $lbm\text{-}ft/lbf\text{-}sec^2$,
$H$ = head, ft, and
$C$ = constant.

In REFLOD3B the pump pressure drop is calculated by

$$\Delta P = \frac{K_{Fj} W^2}{2\rho g_c A^2} ,$$

where

$W$ = mass flow rate, lbm/sec,
$A$ = flow area, $ft^2$, and
$K_{Fj}$ = loss coefficient.

Combining these two equations, the loss coefficient for the free spinning rotor condition can be determined. The loss coefficients used for the locked rotor (base case) and the free spinning rotor, based on a flow area of 5.241 $ft^2$, are 12.08 and 3.21, respectively. It is obvious that the free spinning rotor case with substantially lower loop flow resistance will increase the steam flow out the break and give rise to a higher flooding rate. A comparison of core flooding rate between the locked rotor and free spinning rotor cases presented in Figure A-18 confirms that the locked rotor assumption used in the base model is conservative.

A.2.5. REFLOD3B Maximum ECCS Injection Study

The Combustion Engineering experimental results have shown that the degree of thermal-hydraulic interaction between steam and ECC water varies with the amount of ECC injection. The base model uses a minimum ECC injection assumption: one RHR pump, two centrifugal charging pumps and two SI pumps. To evaluate the effect of steam-water interaction on core flooding rate, the pumped injection was increased to its maximum capacity consisting of two RHR pumps, two centrifugal charging pumps and two SI pumps. The primary coolant pump resistance was based on the locked rotor condition and a 0.85 psi pressure drop was used to account for the steam-water interaction.

In the REFLOD3B EM the 0.85 psi penalty is imposed on both the broken and unbroken loops, but steam condensation (based on thermal equilibrium) is considered only in the unbroken loop. This is conservative because condensation in the broken loop will

increase steam venting. The condensation in the unbroken loop impacts the downcomer pressure because it reduces steam flow into the downcomer, thereby resulting in a lower flooding rate. The calculated flooding rates are shown in Figure A-19. The figure shows a difference in core flooding rate during the accumulator injection phase because the ECC water is more than that required to quench the steam. After the accumulators are empty, the pumped injection is not sufficient to quench the steam and an insignificant change in core flooding rate is observed. Therefore, the minimum ECCS used in the base model is acceptable.

## A.3. Final Studies

An initial set of sensitivity studies performed with RELAP5/MOD2-B&W were completed prior to making various final base case input modeling assumption changes. The significant changes made to the base model after execution of these initial cases (reported in Section A.2) included the following:

1. No lock-in to flow film boiling before 300 F (Section 4.3.4.9);
2. Downcomer ECCS Bypass zone model as described in Section 4.3.4.2;
3. Revised reactor vessel upper head junction input, the junction area and the junction resistance factors;
4. Moody phase slip at the leak junctions;
5. Use of hot channel filtered flow control variables (Section 4.3.4.6);
6. A detailed 24 axial segment rod model of equal size was used in the RELAP5 hot channel;
7. Switch to Condie-Bengston IV film boiling correlation (Section 4.3.4.9);
8. Switch to Babcock & Wilcox MARK-BW fuel at 100MWD/Mtu burnup initialization; and
9. Utilization of BWCMV CHF correlation (Section 4.3.4.8).

These changes produced slightly different blowdown results. The revised ECCS bypass model produced a smoother, more realistic leak flow during the last several seconds of blowdown. Both the ECCS bypass model and the allowance of slip flow at the leak act to shorten the blowdown period by allowing higher energy removal rates from the system. Such changes, however, do not alter the conclusions that were reached in the initial studies. In fact, convergence of the time step and loop noding improve since the modified model reduces oscillations that occurred during the last several seconds of blowdown. Therefore, the RELAP5/MOD2-B&W cases in Section A.2 were not reevaluated with the final base model. The other initial studies (not reported herein), demonstrating different aspects of transient sensitivity, were recalculated. The new calculations confirmed previous trendings and results. The modified RELAP5/MOD2-B&W EM was used in the individual RELAP5/MOD2-B&W studies as well as in the spectrum and time-in-life studies reported in Section A.3.

Pressurizer location, leak noding arrangement, pump degradation, core crossflow resistance, and core spatial noding sensitivity studies were redone and compared to the revised LBLOCA EM base case. Comparison plots are presented for primary system pressure, leak mass flow rates, and core hot channel filtered flow rate and temperature at the peak power location. Table summaries of end-of-blowdown conditions are also presented for each individual case.

The initial REFLOD3B base EM was also modified from that used in Section A.2. The significant model changes include the following:

1. Quench logic change (Reference 2, Revision 1);
2. Transition and the quasi-steady period core outlet flow modification (Reference 2, Revision 1);
3. Lower core support barrel primary metal put entirely in downcomer region;

4. Reduce primary metal slab, representing baffle plates, surface heat transfer coefficient to a realistic value of 10 BTU/hr-ft$^2$-F; and
5. To account for the blowdown leak $C_D$ value change in the calculation of the reflooding leak discharge coefficient.

These modifications are minor and produce no change in the long term flooding rate from the initial base case. Therefore, there is no need to reevaluate the REFLOD3B studies presented in Section A.2. The modified REFLOD3B EM was used in both the spectrum and time-in-life studies reported in Section A.3.

A.3.1. RELAP5/MOD2-B&W Break Noding Study

This study evaluated the impact of increased spatial noding detail adjacent to the cold leg pump discharge leak. Initial studies, using the RELAP5/MOD2-B&W model in Section A.2, were performed with leak configurations of 4, 6, and 8 nodes, as shown in Figure A-20. These studies confirmed the adequacy of a four-node break configuration. The six-node case was again run using the final RELAP5/MOD2-B&W model and compared against the new base case. Comparison results are shown in Figures A-21 through A-25 and in Table A-3. The system pressure, leak flows, hot channel filtered flows, and hot channel peak power location temperatures were nearly identical for both cases. Since model convergence was demonstrated with this arrangement, there was no need to reexecute the eight-node model. Note the effect of the ECCS bypass model and leak slip on leak flow is to shorten the EOB time (compared to Figures A-3 to A-6).

A.3.2. RELAP5/MOD2-B&W Pressurizer Location Study

The base case includes the pressurizer within the intact loop. For this study, the pressurizer surge line connection was removed from control volume 110 and switched to volume 210 in the broken

loop. Comparisons are shown in Figures A-26 through A-30 and in Table A-4. The new pressurizer location produced timing phase shifts and a slightly earlier blowdown time. The EOB peak clad temperature was lower and there was more liquid remaining in the vessel because of the switch in pressurizer location. Although differences between the two case were minimal, the base case produced a more conservative result and the pressurizer will be modeled in the intact loop.

### A.3.3. RELAP5/MOD2-B&W Pump Degradation Study

Behavior of the reactor coolant pump for guillotine breaks may alter the blowdown transient characteristics by changing the distribution of the system flow between the vessel and pump sides of the break. To evaluate the impact of the pump two-phase degradation model, a sensitivity study on pump degradation was performed. The most highly degraded pump characteristics were achieved by using the M1 curve shown in Figure A-31. The minimum degradation was achieved through use of the M3 modified curve in that same figure. The M1 and M3 modified curves were derived in previous Babcock & Wilcox work as being bounding curves.[11] Both models were incorporated into the base deck and executed in separate calculations. Comparisons against the base case are shown in Figures A-32 through A-36 and in Table A-5. The M1 curve forced more flow through the reactor vessel side of the leak. A higher vessel leak flow lead to slightly lower hot channel flows between 5 and 7 seconds and correspondingly higher cladding temperatures due to lower heat transfer coefficients. The opposite effect was noted for the M3 modified transient with correspondingly lower cladding temperatures. The variation in the results were minimal. While the maximum pump degradation, the M1 curve, produced slightly more severe EOB conditions than the NUREG/R-4312 curve, the NUREG curve was adopted into the model, since it was believed to be more representative of degradation at higher void fractions than the M1 curve.

## A.3.4. RELAP5/MOD2-B&W Core Crossflow Study

Appendix K requires that core crossflow should be considered for cases with parallel axial core channels. The choice of a crossflow resistance may impact hot channel flows and peak cladding temperature. To evaluate the sensitivity of the model to variations in crossflow resistance, two additional calculations were performed. The base model crossflow resistance was varied up and down by [illegible]0. Comparisons against the base case are shown in [illegible]rough A-41 and in Table A-6. Neither core crossflow [illegible] change made a significant impact on the results of the [illegible]ient. EOB conditions were also similar for all three cases. Thus, the base case crossflow resistance is acceptable.

## A.3.5. RELAP5/MOD2-B&W Core Noding Study

The final RELAP5/MOD2-B&W individual sensitivity study evaluated the core spatial noding by increasing both the axial and the radial detail in two separate cases. The first variation increased the axial detail from six to twelve control volumes in both the hot and average channels, while the second variation added a third radial bundle to the base noding. Figures A-42 and A-43 show the revised noding schemes for both of these calculations. In Figure A-43, volumes 328 through 338 represent the hot assembly, volumes 317 through 327 the next hottest eight assemblies, and the third channel the average core. Comparisons against the base case are shown in Figures A-44 through A-48 and in Table A-7. Generally, the results were consistent among the cases. Subtle phase shifts, however, were noted in the core hot channel flows and temperatures. The 2 x 12 case had more liquid inventory remaining at the end of blowdown, even though the transient progressed similarly. It was concluded that the core spatial noding detail was adequate in the base model.

## A.3.6. FRAP-T6-B&W Time Step Study

The initial FRAP-T6-B&W study performed was a reduced time step study. The purpose of the study was to examine the sensitivity of the ruptured and peak clad temperature locations to a change in the calculational time step. FRAP-T6-B&W allows the user to input time steps and the time increments over which the code will use the time steps. The study used the spectrum double area (2A/G) break at the pump discharge with $C_D$ = 0.6 (see Section A.3.8) as a base case. The study consisted of reducing the base case time steps by a factor of two. The results of the study are shown in Figures A-50 and A-51. The results demonstrate that the time steps used in the base FRAP-T6-B&W case are adequate for evaluation model applications.

## A.3.7. FRAP-T6-B&W Radial Fuel Segmentation Study

The purpose of this study was to examine the impact on the ruptured and peak clad temperature nodes due to changes in the number of equally spaced radial calculational nodes in the fuel. This study also used the 2A/G break at the pump discharge with $C_D$ = 0.6 from the spectrum study as its base case. The study increased the number of fuel calculational nodes from seven in the base case to ten. The results, shown in Figures A-52 through A-55, show that the base case contains sufficient fuel spatial detail for evaluation model applications.

## A.3.8. Spectrum Analysis

A limited spectrum of break sizes and locations were analyzed to determine which would produce the highest peak cladding temperature. Postulated large breaks were selected for analysis that have previously been shown to produce the highest peak cladding temperatures for plants of the type being analyzed. Listed below are the breaks analyzed using the large break model.

Double area (8.25 $ft^2$) guillotine break at the pump discharge (2AGPD), $C_D$ = 1.0

Double area (8.25 $ft^2$) guillotine break at the pump discharge (2AGPD), $C_D$ = 0.8

Double area (8.25 $ft^2$) guillotine break at the pump discharge (2AGPD), $C_D$ = 0.6

Double area (8.25 $ft^2$) guillotine break at the pump discharge (2AGPD), $C_D$ = 0.4

Double area (8.25 $ft^2$) split break at the pump discharge (2ASPD), $C_D$ = 1.0

Double area (10.482 $ft^2$) guillotine break at the pump suction (2AGPS), $C_D$ = 1.0

The analysis was performed using the input selection methods outlined in Section 4.0 and the results of the other LOCA sensitivity studies in Appendix A. Also used for the spectrum study were the following general conditions and assumptions:

1. Power Shape and Linear Heat Rate - A power shape that is symmetric about the core midplane was used. The hot pin peak linear heat rate was 13.2 kw/ft.

2. Core Power - A core power of 3411 MWt was used for the entire spectrum. In all cases this power was increased by 2% as required by 10CFR50, Appendix K. A power distribution factor of 0.973 was used during the blowdown phase of the transient and a value of 0.95 was used in the reflood phase (BEACH and FRAP-T6-B&W) of the transient. (This is a deviation from the EM rules which allow a factor of 0.95 for both blowdown and reflood.)

3. Time-in-Life - BOL conditions of initial pin pressure and oxide thickness were used.

4. RC Pump Status - Power to the RC pumps was terminated at the start of the transients, simulating the loss of off-site power.

5. ECC Systems - The loss of one residual heat removal pump was assumed, similar to the minimum ECC injection assumption for the REFLOD3B ECC injection study from Section A.2.5.

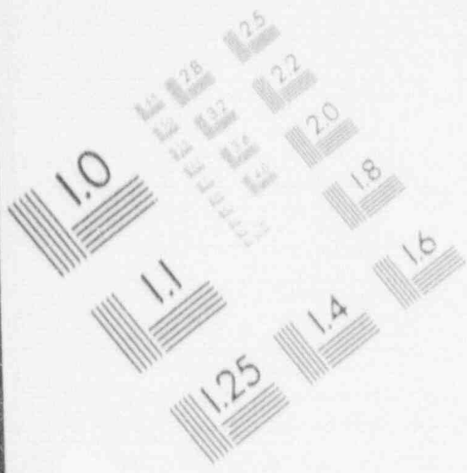
Results from the spectrum study are presented in Figures A-56 through A-71 with additional results summarized in Table A-8.

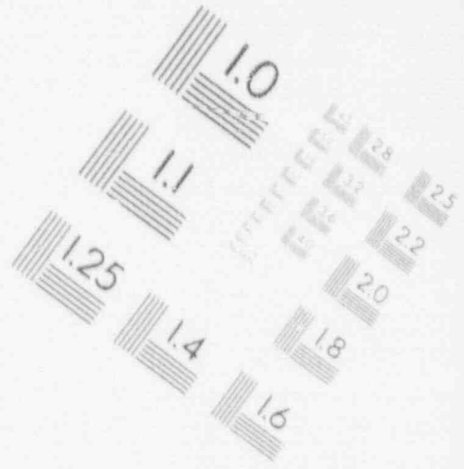
Figures A-56 and A-57 show the mass flow rates of the fluid leaving the RC system during blowdown. For the split break, the rates are those in the paths leading to the break. The pump suction leak flow rates show the effect of the pump resistance between the reactor vessel and the break. Figure A-58 presents the pressure in the RV upper plenum during blowdown. The effect of the change in the discharge coefficient is evident in the rate of depressurization.

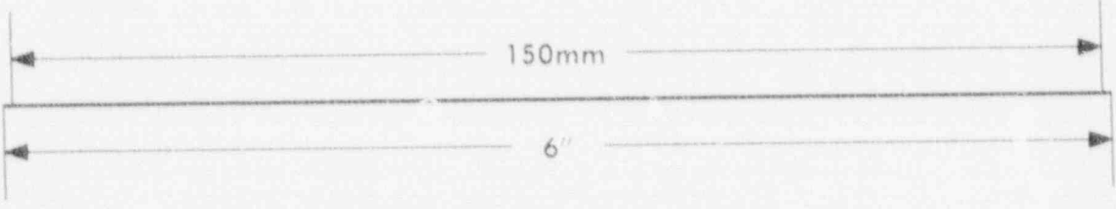
Figures A-59 through A-62 give the hot channel mass flow rates and enthalpies during blowdown for the ruptured and peak clad temperature locations. The ruptured and peak clad temperature locations are as determined by FRAP-T6-B&W. Pump suction break results are not shown as this case was not continued through BEACH and FRAP-T6-B&W calculations. REFLOD3B results showed that the suction break obtained even higher core flooding and filling rates than did the $C_D$ = 0.4 discharge break. By 30 seconds after the EOB, the suction break already obtained a collapsed core liquid level in excess of nine feet, so that case was dismissed as not limiting. Figures A-63 and A-64 show the core flooding

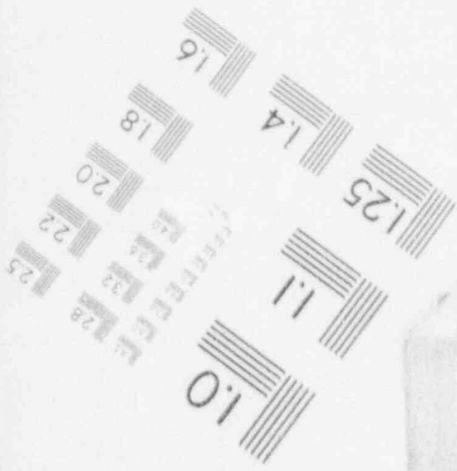
# 1

## IMAGE EVALUATION TEST TARGET (MT-3)





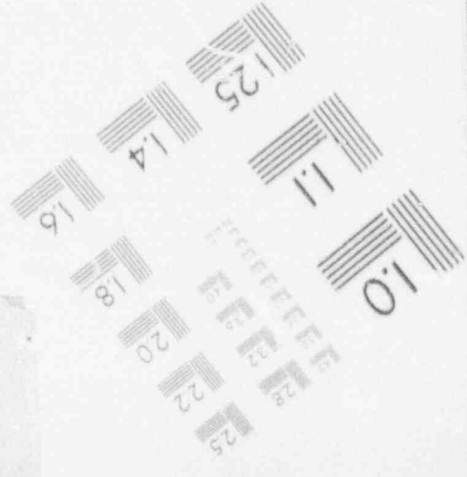



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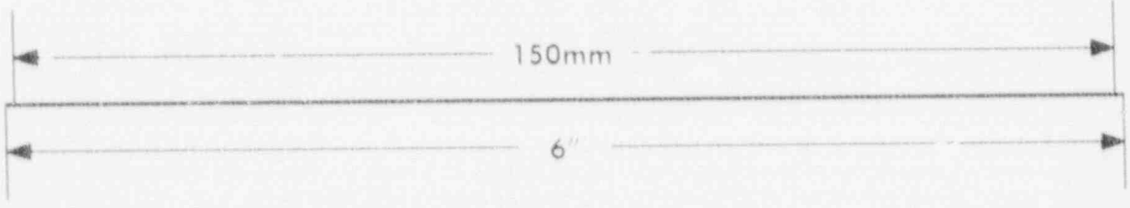
IMAGE EVALUATION
TEST TARGET (MT-3)



rates from REFLOD3B. Figure A-63 is a time segment of Figure A-62.

Figures A-65 through A-69 give the surface heat transfer coefficients for the PCT and ruptured locations. The heat transfer coefficient shown is used in the FRAP-T6-B&W code for the heatup calculation. Figures A-70 and A-71 present the hot channel clad temperatures for the ruptured and peak clad temperature locations.

The maximum clad temperature was predicted for the 2AGPD, $C_D$ = 0.8 transient. The peak temperature is 2036 F which is less than the 2200 F limit for the acceptance criteria of 10CFR50.46. The maximum metal-water reaction is 5.25% which is considerably less than the 10CFR50.46 limit of 17%.

A.3.9. Time-In-Life Study

The steady-state parameters that are used to initialize the hot fuel rod in the LOCA analysis vary with fuel burnup. These variations are caused primarily by the generation of fission gases and by clad creep. In order to study the effect of burnup on the LOCA PCT analysis, the fission gas generation and clad creep behavior of a fuel rod throughout its life were examined with the B&W steady-state fuel code, TACO2[5]. The results of the TACO2 calculation indicate that the maximum fuel pellet stored energy occurs at 100 MWD/Mtu (BOL) when fuel densification is complete. The gap gas pressure is at a minimum because the fuel pellet contracts, providing a larger void volume, and the gap heat transfer coefficient decreases due to the larger gap. As a result, the pellet stored energy is at a maximum because of the smaller gap heat transfer coefficient.

As the burnup increases to 60000 MWD/Mtu (EOL), the production of fission gas results in a higher gas pressure and an alteration in gas composition. The change in gas pressure significantly

affects the clad rupture time but has very little impact on the gas conductivity. The change in gas composition due to the burnup at EOL causes a substantial decrease in gas conductivity. However, because of a smaller gap thickness, the gap heat transfer coefficient is greater than that of the BOL case, thereby resulting in lower pellet stored energy.

The clad rupture time depends primarily on initial pressure and average fuel pellet temperature. There is a tradeoff between these parameters in determining rupture time. In addition, the ruptured node temperature is sensitive to changes in oxide layer thickness due to metal-water reaction on both the inside and outside surfaces of the ruptured node clad. The metal-water reaction rate is inversely proportional to the oxide thickness. At the BOL condition, the oxide layers are thin. This leads to an increase in metal-water reaction rate, causing the PCT of the ruptured node to increase.

In order to determine the burnup condition that will result in the highest PCT during the fuel cycle and to establish the relationship between burnup and PCT, a sensitivity study was performed with the initial hot fuel rod conditions. The study was based on burnups of 100, 1000, 40000 and 60000 MWD/Mtu with a total peaking factor of 2.32 and a 3479.2 MWt initial core power. Power distribution factors were the same as those used in the spectrum study (Section A.3.8). The initial hot pin conditions for the burnup sensitivity study are presented in Table A-9.

Based on the results of the sensitivity studies with the blowdown model, a 2A double-ended guillotine break (DECLG) with $C_D$ = 1.0 at the pump discharge was selected as the base case to examine the impact of burnup on PCT. RELAP5/MOD2-B&W blowdown calculations were made for all cases listed in Table A-9. The primary system pressure, leak flow rate and filtered hot channel mass flow rates (both ruptured and unruptured nodes) are presented in Figures A-72 through A-76. These comparison plots

show that the blowdown behavior is relatively insensitive to the fuel burnup conditions. An examination of the end-of-blowdown conditions indicates that the fuel rod temperatures at the EOB are substantially lower for higher burnups than for the BOL conditions. This could lead to early core quenching for the higher burnups during the reflooding transient. Therefore, it is appropriate to use the reflood heat transfer coefficients from the BOL case for all the burnup conditions. To demonstrate this, core reflooding and BEACH heat transfer calculations were made for the 40000 MWD/Mtu burnup conditions. The approximation, used by B&W for this study, for the post-blowdown heat transfer calculations provides reasonable accuracy and is adequate for the purpose of this study.

The results of the burnup study are summarized in Table A-10. The most limiting PCT is located at 8.0 feet above the bottom of the active fuel and, as shown in the temperature plots (Figures A-82 and A-83), the PCT is proportional to the initial stored energy in the fuel pellet. The initial pin pressure has very little impact because rod burst occurred during the reflood transient when the stored energy had been substantially reduced from its initial value. Had the clad burst occurred during the blowdown, the ruptured node temperature could increase rapidly during the adiabatic heatup period due to increased $\Delta T$ between the fuel pellet and the clad inner surface as a result of reduced gap heat transfer following rupture. The cladding temperature responses for both the ruptured (node 11) and PCT (node 14) nodes and the corresponding surface heat transfer coefficients are shown in Figures A-78 through A-83. The unruptured node cladding temperature turnaround time increases with burnup because of higher gap heat transfer coefficients that increase the heat transfer from the fuel pellet to the cladding. Comparing the results in Figures A-56 through A-71 and Table A-8 between the $C_D$ = 1.0 case and the $C_D$ = 0.8 case, it is concluded that the results and conclusions drawn for the time-in-life study, performed with a discharge coefficient of one, would be equally

valid for a coefficient of 0.8. From the results of this study, the hot fuel rod steady-state condition at the time of maximum densification, BOL (100 MWD/Mtu), is the most limiting and was used for all the large break LOCA analyses.

A.3.10. Most Severe Break Case

Two final model changes were made after completion of the previous studies. The blowdown power distribution factor was changed to 0.95 and the single failure of one train of ECC pumped injection was assumed. As a result of these modifications, the worst break case was reanalyzed.

The results of this analysis are presented in Figures A-84 through A-90 and in Table A-11. As expected, there is little difference between the new analysis and those of the $C_D = 0.8$ spectrum case (Section A.3.8). A slight drop in peak cladding temperature and the maximum local metal-water reaction to under 5 percent, attributable to the change in the blowdown power distribution factor, were noted. The change in the single failure criterion was of no consequence for the following reasons:

1. During blowdown, no pumped ECC injection is activated as a result of the long delay time associated with the loss of off-site power assumption. Consequently, no RELAP5/MOD2-B&W cases are impacted by this modification.

2. Accumulator injection, which starts before the EOB and continues until after the downcomer is filled, dominates the ECC pumped injection flow rate. Therefore, dropping one centrifugal charging pump and SI pump from previous REFLOD3B cases, during this portion (EOB to end-of-accumulator injection) of the

reflood transient, is of no consequence and the previous REFLOD3B cases remain valid.

3. Accumulator injection stops after the downcomer is filled. At that point in the reflood transient, sufficient water is being spilled out the break to offset the loss of one charging pump and one SI pump. Thus, the downcomer will remain full and the previous REFLOD3B cases are valid.

This is confirmed by comparing the flooding rate in Figures A-63 and A-64 ($C_D$ = 0.8 case) with that in Figure A-88.

## A.4. Summary and Conclusions

Numerous individual RELAP5/MOD2-B&W, REFLOD3B, and FRAP-T6-B&W sensitivity studies were performed and reported herein. The studies were undertaken to demonstrate model convergence and to determine the relative sensitivity of the large break LOCA evaluation model to variations in key input assumptions and parameters. Spectrum and time-in-life studies were also performed to establish the most severe break case and burnup, respectively. The studies demonstrate that Babcock & Wilcox's evaluation model is in compliance with the criteria of 10CFR50.46 Appendix K and that it is a valid model for LOCA application to those plants listed in Table 1-1.

Table A-1. Parameter Comparison for the Base and Reduced Time Step Cases.

| Parameter | Base Case | Reduced Time Step |
|---|---|---|
| End-of-blowdown (EOB), s | 24.74 | 24.35 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 385.7 | 306.0 |
| Integrated accumulator injection at EOB, lbm | 41009. | 39110. |
| Integrated mass out the break at EOB, lbm | 521237. | 523793. |
| Integrated energy out the break at EOB, mBTU | 304.867 | 304.061 |

mBTU denotes $10^6$ BTU

Table A-2. Parameter Comparison for the Base and Loop Noding Cases.

| Parameter | Base Case | Loop Noding |
|---|---|---|
| End-of-blowdown (EOB), s | 24.74 | 24.79 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 385.7 | 299.3 |
| Integrated accumulator injection at EOB, lbm | 41009. | 41411. |
| Integrated mass out the break at EOB, lbm | 521237. | 527732. |
| Integrated energy out the break at EOB, mBTU | 304.867 | 305.108 |

Table A-3. Parameter Comparison for the Base and Break Noding Cases.

| Parameter | Base Case | Six Node |
|---|---|---|
| End-of-blowdown (EOB),s | 22.64 | 22.62 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.6 | 280.6 |
| Integrated accumulator injection at EOB, lbm | 36740. | 36862. |
| Integrated mass out the break at EOB, lbm | 524270. | 524484. |
| Integrated energy out the break at EOB, mBTU | 307.317 | 307.515 |

Table A-4. Parameter Comparison for the Base and Pressurizer in the Broken Loop Cases.

| Parameter | Base Case | Pressurizer in Broken Loop |
|---|---|---|
| End-of-blowdown (EOB),s | 22.64 | 22.42 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.6 | 344.6 |
| Integrated accumulator injection at EOB, lbm | 36740. | 37309. |
| Integrated mass out the break at EOB, lbm | 524270. | 521466. |
| Integrated energy out the break at EOB, mBTU | 307.317 | 305.409 |

Table A-5. Parameter Comparison for the Base and Pump Degradation Cases.

| Parameter | Base | M1 Curve | M3 Modified Curve |
|---|---|---|---|
| End-of-blowdown (EOB), s | 22.64 | 22.66 | 22.79 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.6 | 287.6 | 290.3 |
| Integrated accumulator injection at EOB, lbm | 36740. | 37035. | 37322. |
| Integrated mass out the break at EOB, lbm | 524270. | 524428. | 524524. |
| Integrated energy out the break at EOB, mBTU | 307.317 | 307.070 | 308.171 |

Table A-6. Parameter Comparison for the Base and Cross Flow Cases.

| Parameter | Base | 0.01X | 100X |
|---|---|---|---|
| End-of-blowdown (EOB), s | 22.64 | 22.57 | 22.58 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.6 | 267.6 | 270.5 |
| Integrated accumulator injection at EOB, lbm | 36740. | 36306. | 36429. |
| Integrated mass out the break at EOB, lbm | 524270. | 524915. | 524833. |
| Integrated energy out the break at EOB, mBTU | 307.317 | 307.419 | 307.362 |

Table A-7. Parameter Comparison for the Base and Core Noding Cases.

| Parameter | Base | 2 x 12 | 3 x 6 |
|---|---|---|---|
| End-of-blowdown (EOB), s | 22.64 | 22.67 | 22.66 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.6 | 355.9 | 288.2 |
| Integrated accumulator injection at EOB, lbm | 36740. | 36956. | 36896. |
| Integrated mass out the break at EOB, lbm | 524270. | 520441. | 524317. |
| Integrated energy out the break at EOB, mBTU | 307.317 | 306.429 | 307.281 |

Table A-8. Parameter Comparison for the Spectrum Cases.

| Parameter | 2A/G at PD $w/C_D$=1.0 | 2A/G at PD $w/C_D$=0.8 | 2A/G at PD $w/C_D$=0.6 | 2A/G at PD $w/C_D$=0.4 | 2A/S at PD $w/C_D$=1.0 | 2A/G at PS $w/C_D$=1.0 |
|---|---|---|---|---|---|---|
| End-of-blowdown (EOB), s | 18.39 | 19.65 | 22.69 | 27.65 | 17.74 | 24.65 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | 285.629 | 284.702 | 288.608 | 383.913 | 261.976 | 271.310 |
| Integrated accumulator injection at EOB, lbm | 34635. | 34442. | 37104. | 38709. | 33693. | 55988.** |
| Integrated mass out the break at EOB, lbm | 522121. | 521992. | 524503. | 521173. | 522392. | 556988. |
| Integrated energy out the break at EOB, mBTU | 304.859 | 305.290 | 307.340 | 309.269 | 304.485 | 312.746 |
| Rupture: time, s | 47.8 | 51.8 | 72.4 | 126.6 | 50.2 | ---- |
| node* | 11 | 11 | 12 | 14 | 11 | ---- |
| temperature, F | 1615.6 | 1610.7 | 1605.3 | 1582.3 | 1621.2 | ---- |
| PCT: time, s | 188.6 | 195.8 | 218.0 | 274.6 | 194.4 | ---- |
| node* | 14 | 14 | 14 | 15 | 14 | ---- |
| temperature | 2028.9 | 2036.0 | 1923.7 | 1776.3 | 1998.9 | ---- |

A = Pipe area
G = Guillotine Break
S = Split Break
PD = Cold Leg Piping Between RV Inlet and Pump Discharge
PS = Cold Leg Piping Between Pump Suction and RSG Outlet

EOAH = End-of-Adiabatic Heatup
PCT = Peak Clad Temperature
* Figure A-49
** 4 Accumulators

Table A-9. Initial Hot Fuel Rod Conditions.

| | 100 MWD/ Mtu | 1000 MWD/ Mtu | 40000 MWD/ Mtu | 60000 MWD/ Mtu |
|---|---|---|---|---|
| Hot spot (6 foot) average fuel temperature, F | 2299 | 2209 | 1803 | 1911 |
| Gas pressure, psia | 1064 | 1018 | 1623 | 2280 |
| Hot spot inside oxide thickness, in | $9.996 \times 10^{-6}$ | $5.128 \times 10^{-5}$ | $2.236 \times 10^{-4}$ | $4.236 \times 10^{-4}$ |
| Hot spot outside oxide thickness, in | $1.282 \times 10^{-5}$ | $3.077 \times 10^{-5}$ | $1.205 \times 10^{-4}$ | $2.0 \times 10^{-4}$ |
| Gas composition: | | | | |
| $O_2$ | 0.009372 | 0.009415 | 0.008576 | 0.005972 |
| He | 0.955365 | 0.954849 | 0.813001 | 0.166776 |
| $N_2$ | 0.035257 | 0.035419 | 0.032264 | 0.022465 |
| Kr | 0.000001 | 0.00005 | 0.023093 | 0.079756 |
| Xe | 0.000005 | 0.000267 | 0.123066 | 0.425031 |

Table A-10. Parameter Comparison for the Time-in-Life Study.

| Parameter | 100 MWD/ Mtu | 1000 MWD/ Mtu | 40000 MWD/ Mtu | 60000 MWD/ Mtu |
|---|---|---|---|---|
| End-of-Blowdown (EOB), s | 18.39 | 18.35 | 18.19 | 18.20 |
| Rupture: | | | | |
| time, s | 48 | 51 | 57 | 45 |
| node | 11 | 11 | 11 | 11 |
| temperature, F | 1616 | 1600 | 1507 | 1496 |
| PCT: | | | | |
| time, s | 189 | 196 | 243 | 242 |
| node | 14 | 14 | 14 | 14 |
| temperature | 2029 | 1993 | 1898 | 1888 |

Table A-11. Most Severe Break Case - 2A/G at PD with $C_D$ = 0.8.

| Parameter | | Value |
|---|---|---|
| End-of-blowdown (EOB), s | | 19.62 |
| Amount of water in the reactor vessel and cold leg piping at EOB, $ft^3$ | | 282.061 |
| Integrated accumulator injection at EOB, lbm | | 34196. |
| Integrated mass out the break at EOB, lbm | | 521865. |
| Integrated energy out the break at EOB, mBTU | | 305.248 |
| Rupture: | time, s | 55.8 |
| | node | 11 |
| | temperature, F | 1601 |
| PCT: | time, s | 198.6 |
| | node | 14 |
| | temperature, F | 2006 |
| Maximum local cladding oxidation, % | | 4.7 |
| Whole core metal-water reaction, % | | 0.46 |

Figure A-1. Reactor Coolant Loop Noding.

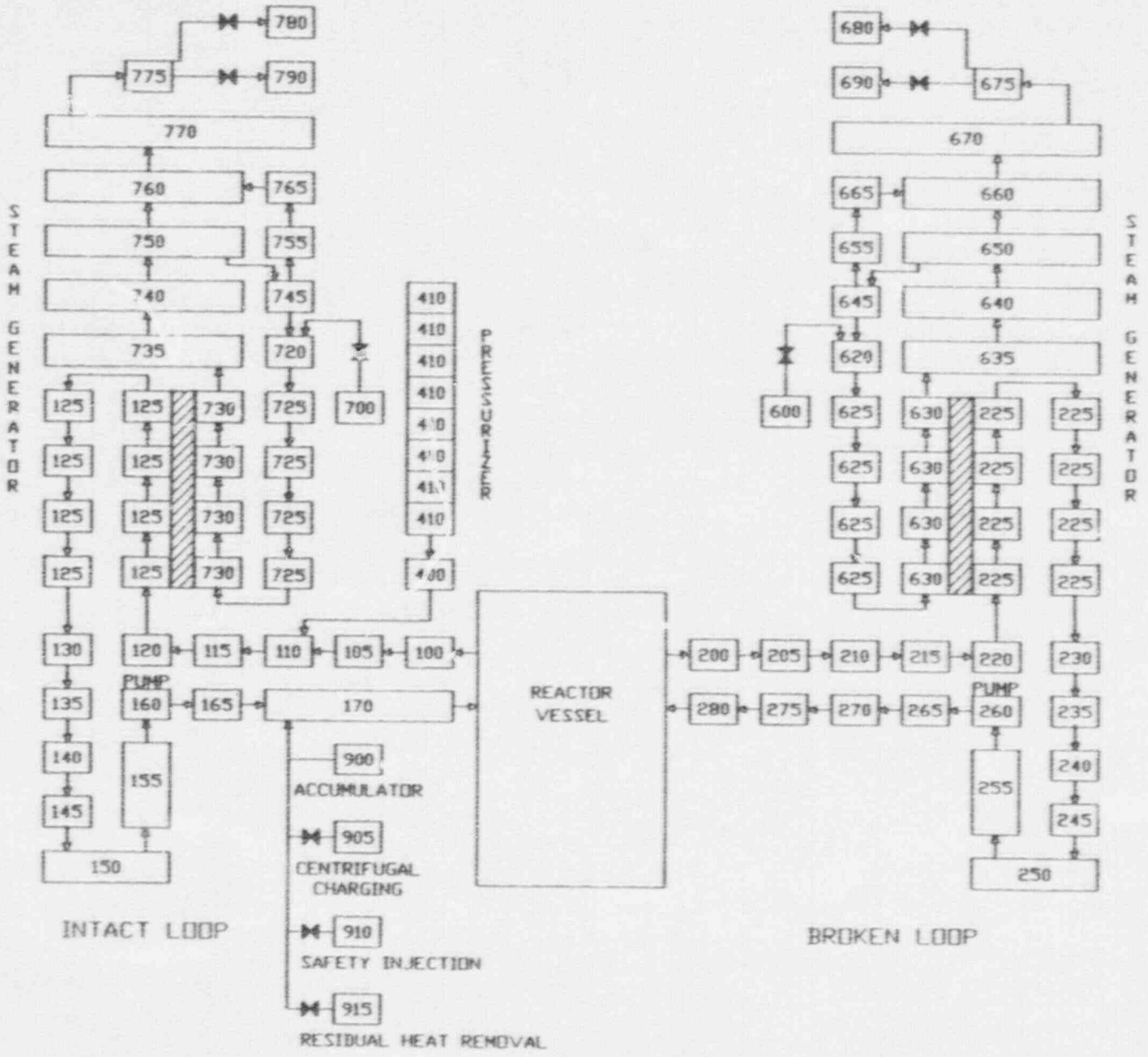


Figure A-2. Reactor Vessel Noding.

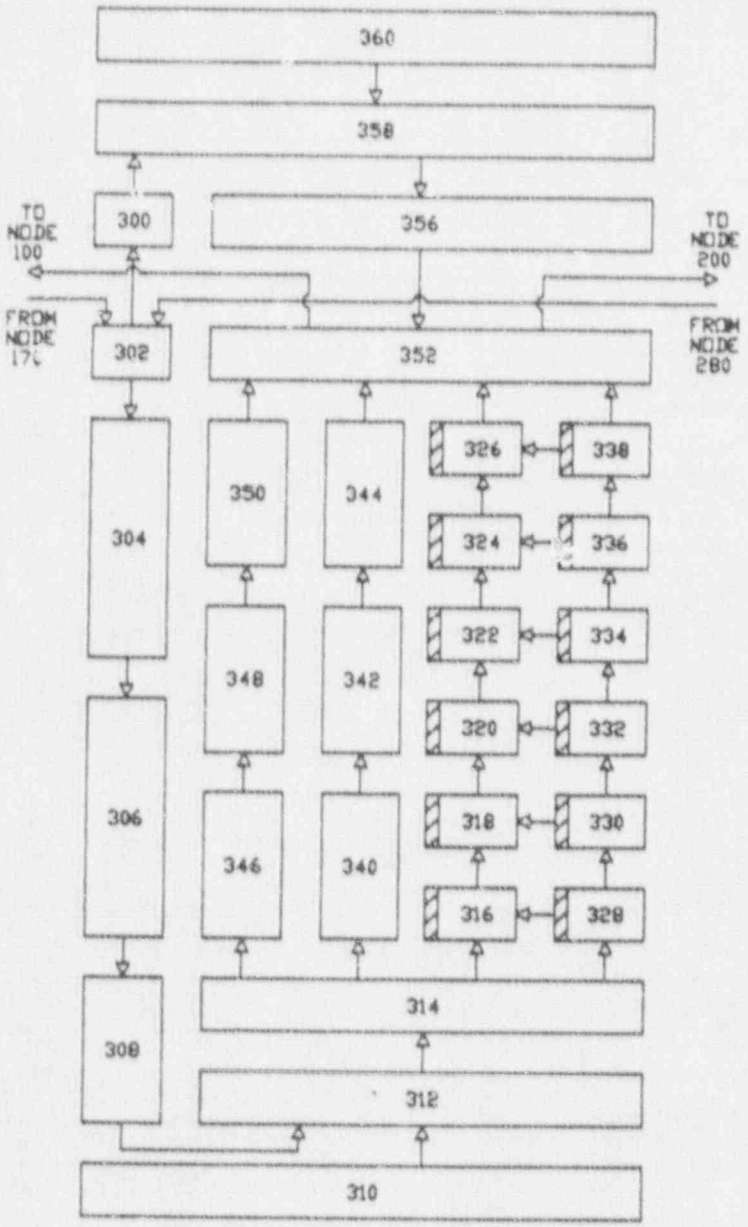


FIGURE A-3. RELAP5 TIME STEP STUDY - LEAK FLOW AT RV SIDE.

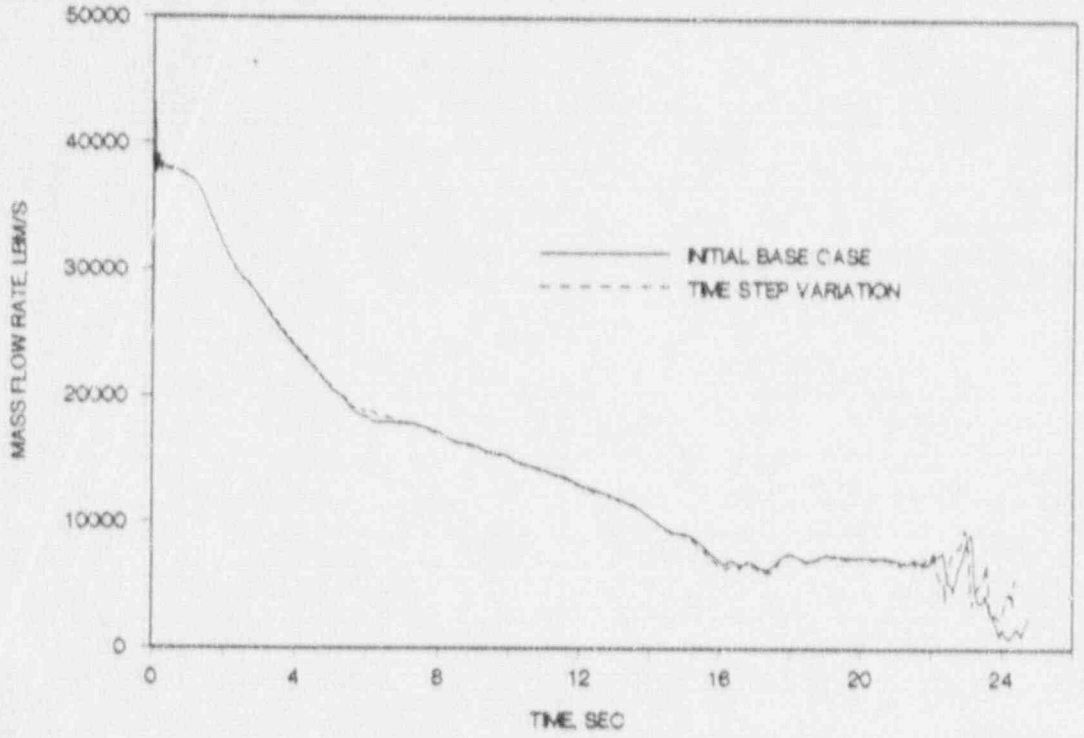



FIGURE A-4. RELAP5 TIME STEP STUDY - LEAK FLOW AT PUMP SIDE.

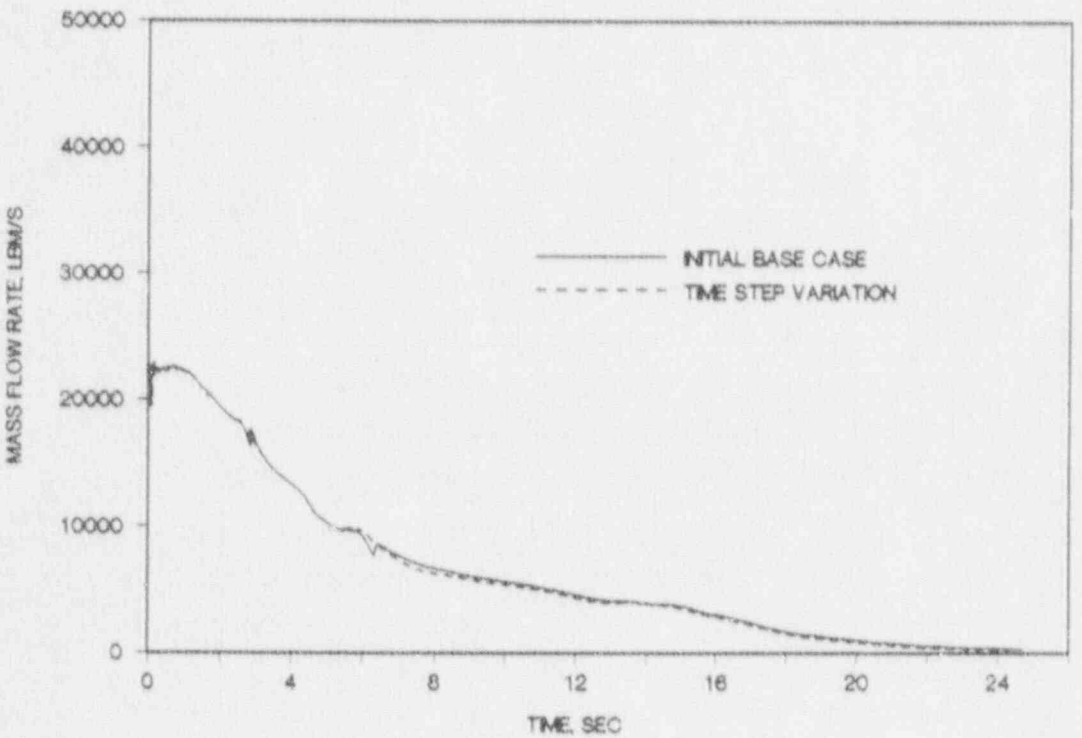


FIGURE A-5. RELAP5 TIME STEP STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

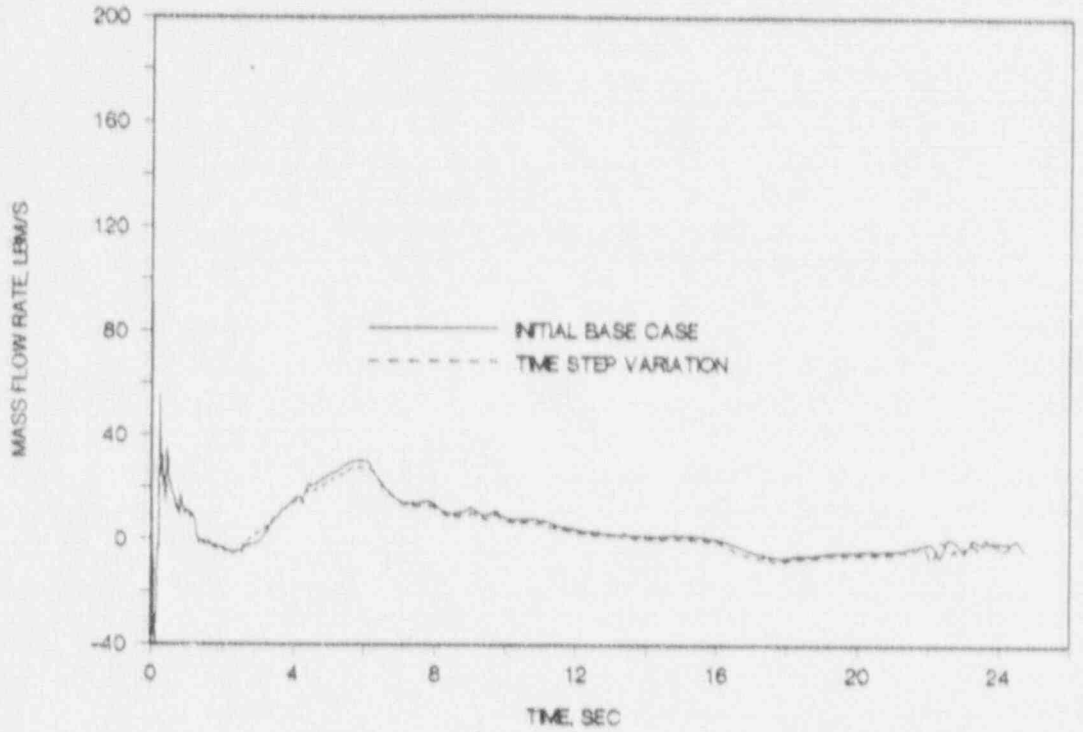



FIGURE A-6. RELAP5 TIME STEP STUDY - SYSTEM PRESSURE IN THE INTACT LOOP HOT LEG.

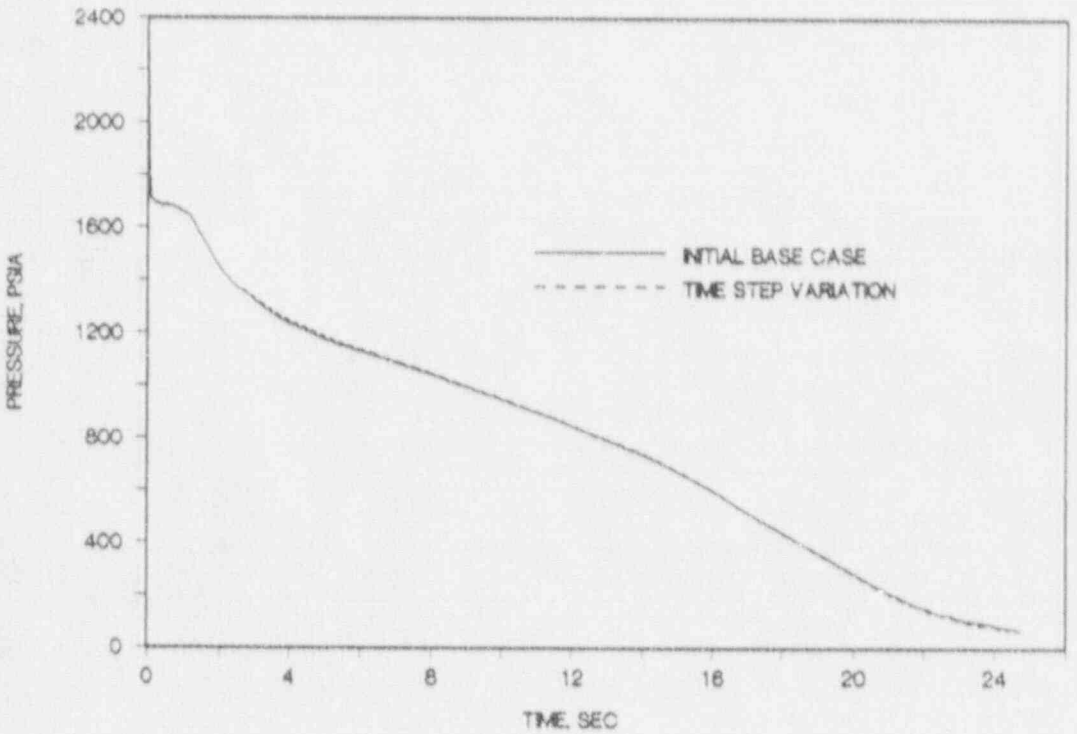


Figure A-7. RELAP5 Loop Noding Study; Reactor Coolant Loop Noding.

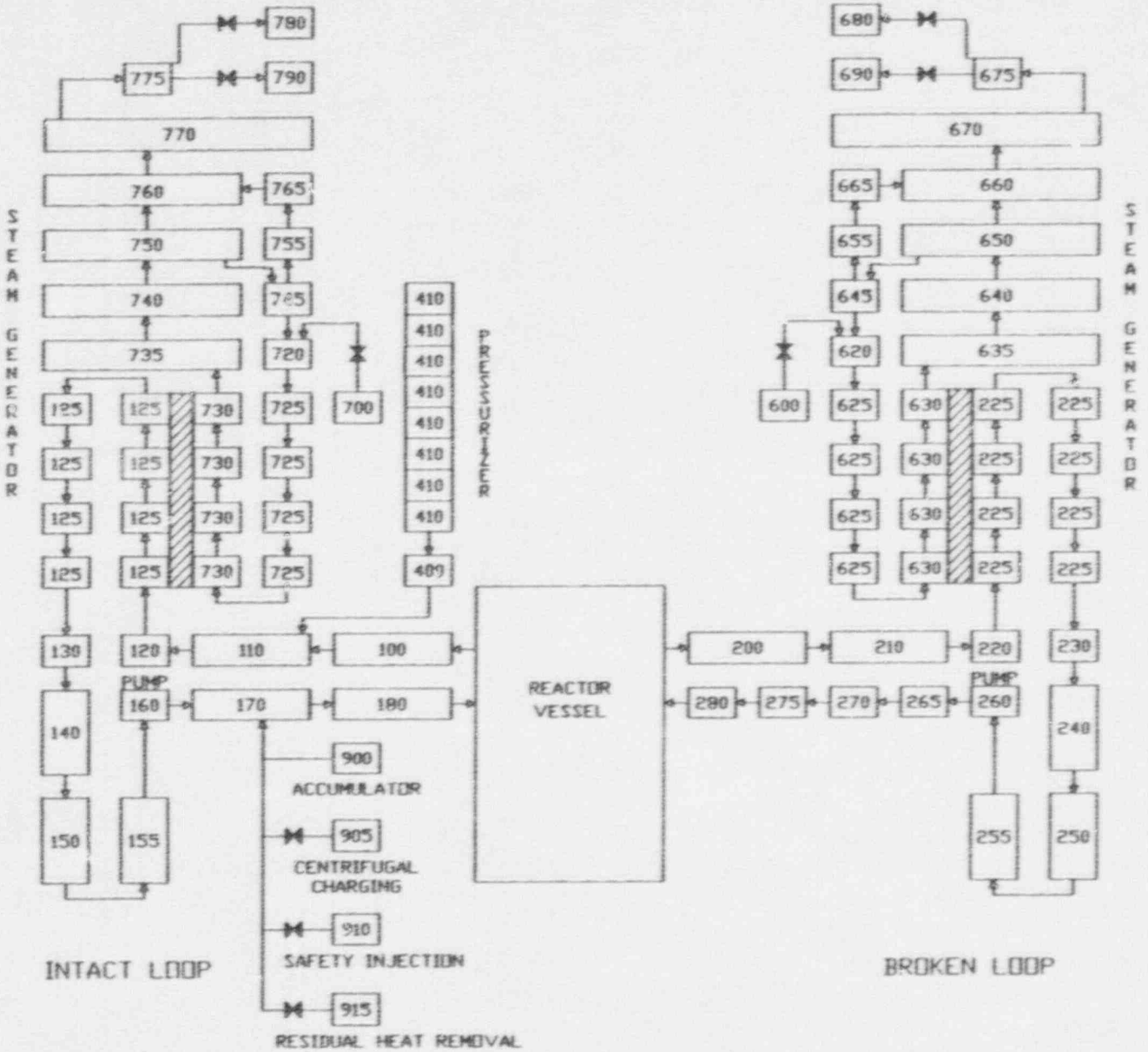


FIGURE A-8. RELAP5 LOOP NODING STUDY - LEAK FLOW AT RV SIDE.

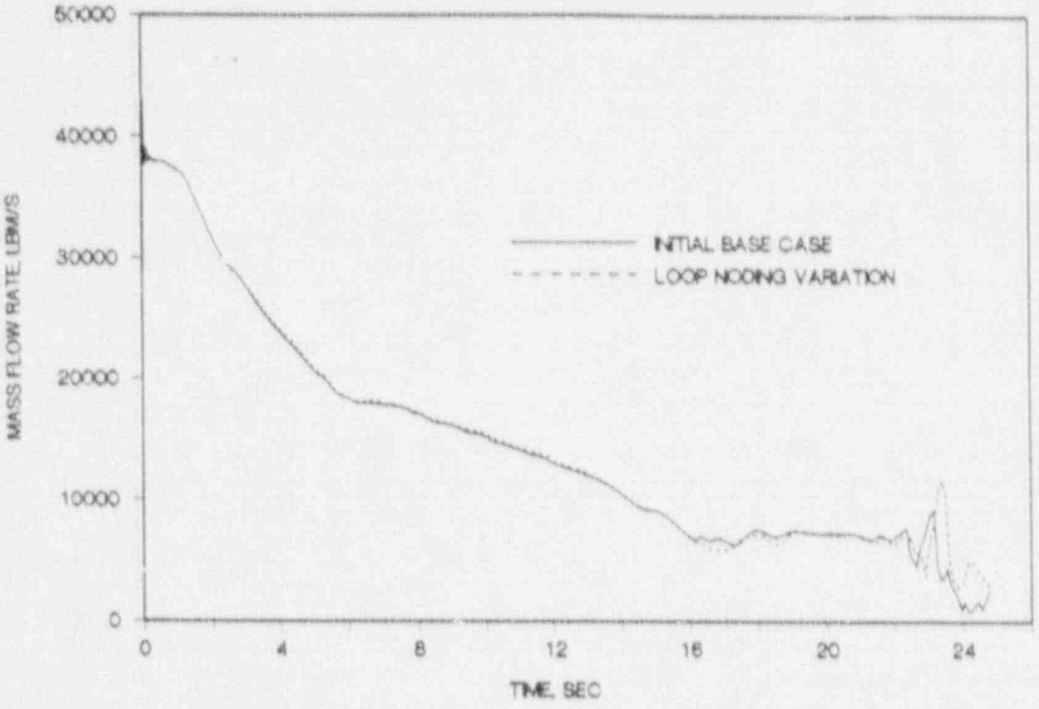



FIGURE A-9. RELAP5 LOOP NODING STUDY - LEAK FLOW AT PUMP SIDE.

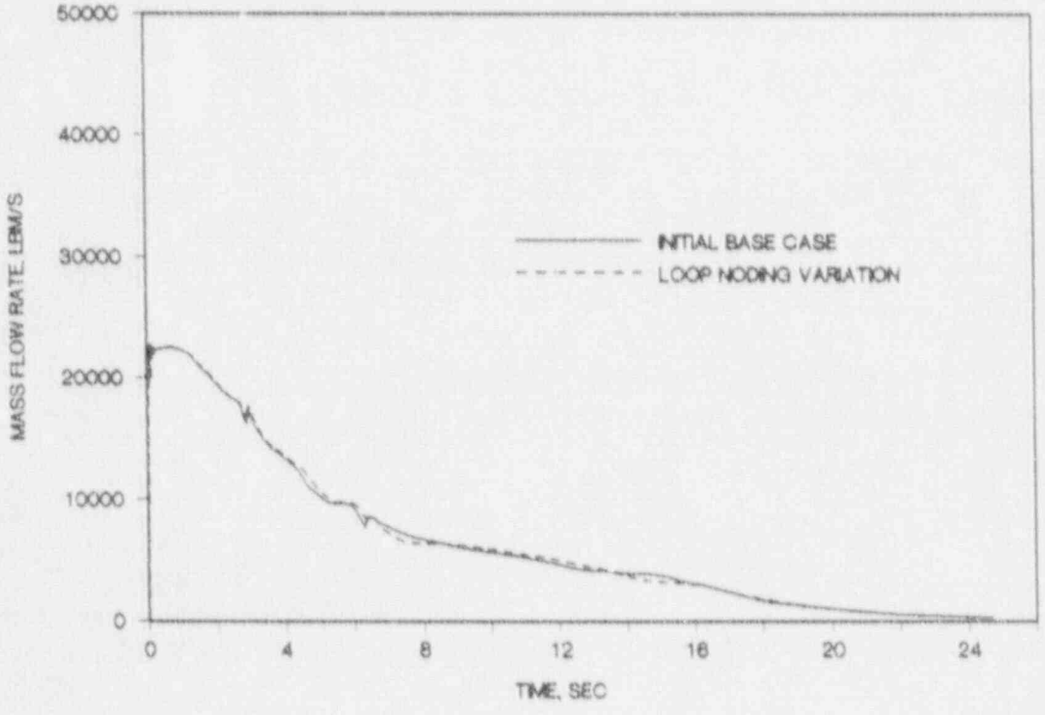


FIGURE A-10. RELAP5 LOOP NODING STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

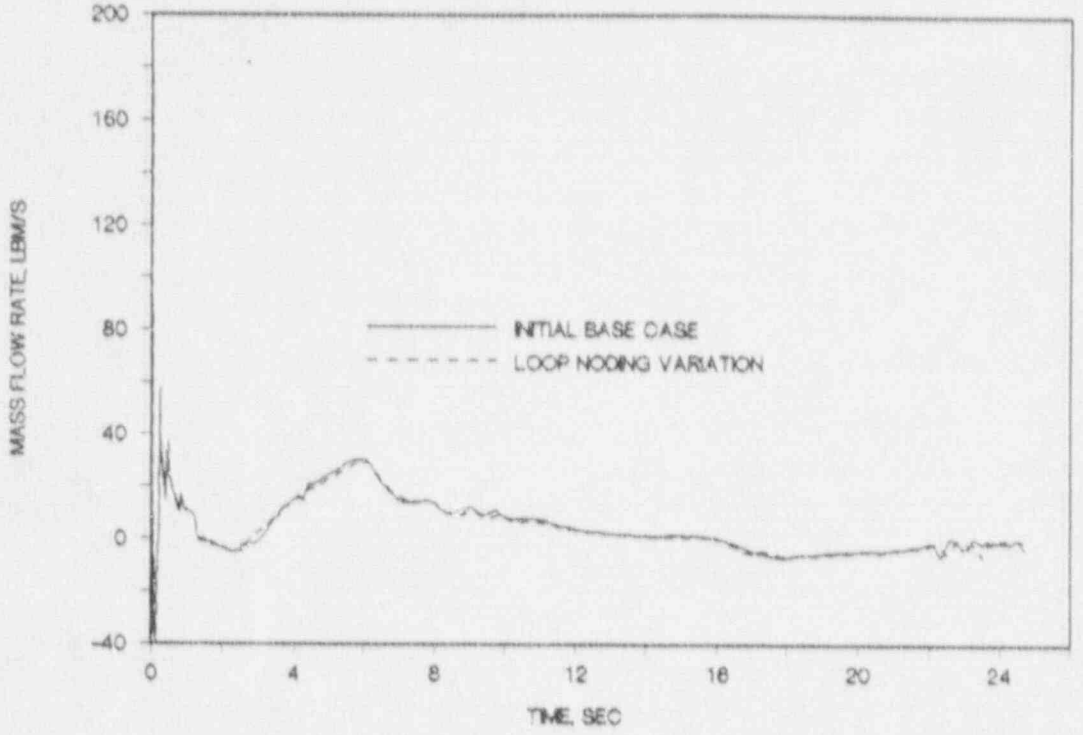



FIGURE A-11. RELAP5 LOOP NODING STUDY - SYSTEM PRESSURE IN THE INTACT LOOP HOT LEG.

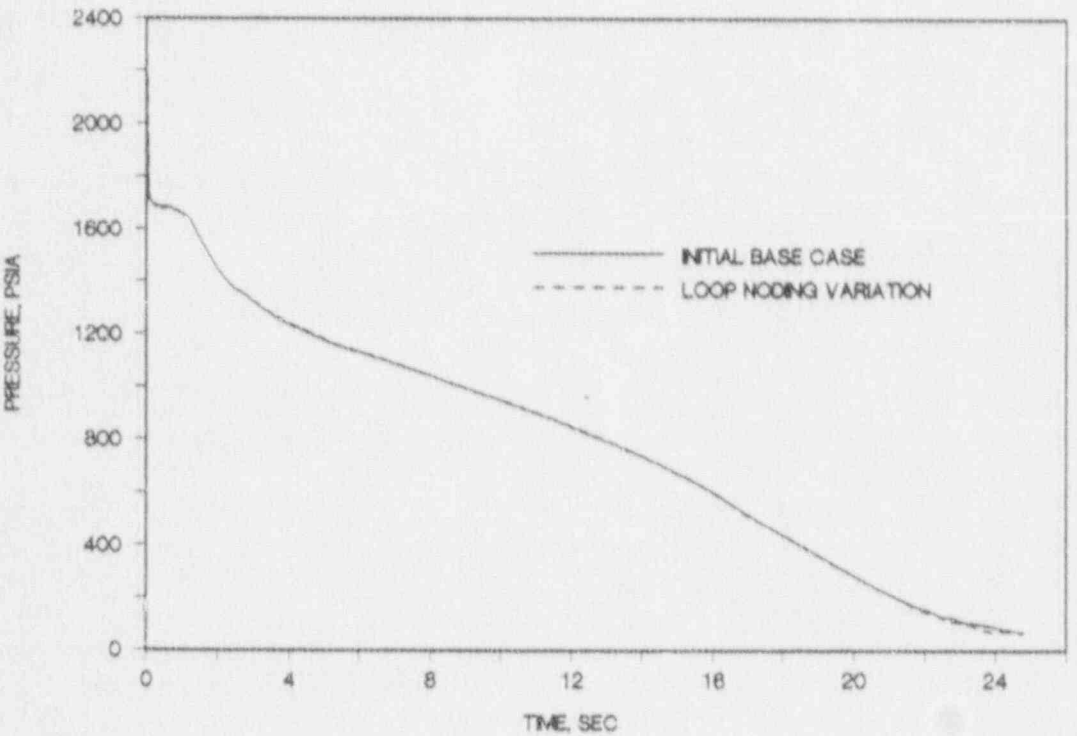


Figure A-12. REFLOOD3B Reactor Coolant System Noding.

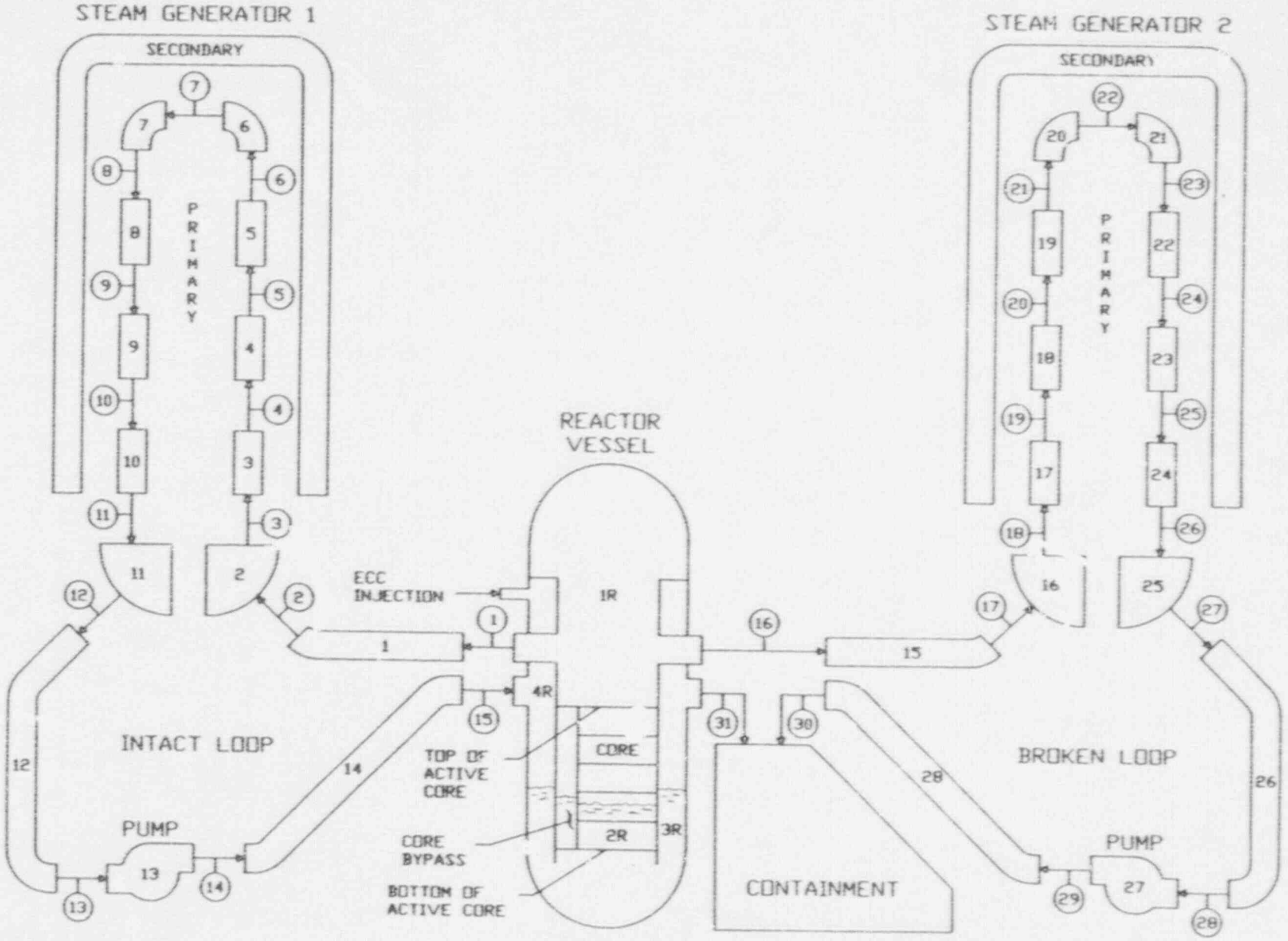


Figure A-13. REFLOD3B Loop Noding Study; Reactor Coolant Loop Noding.

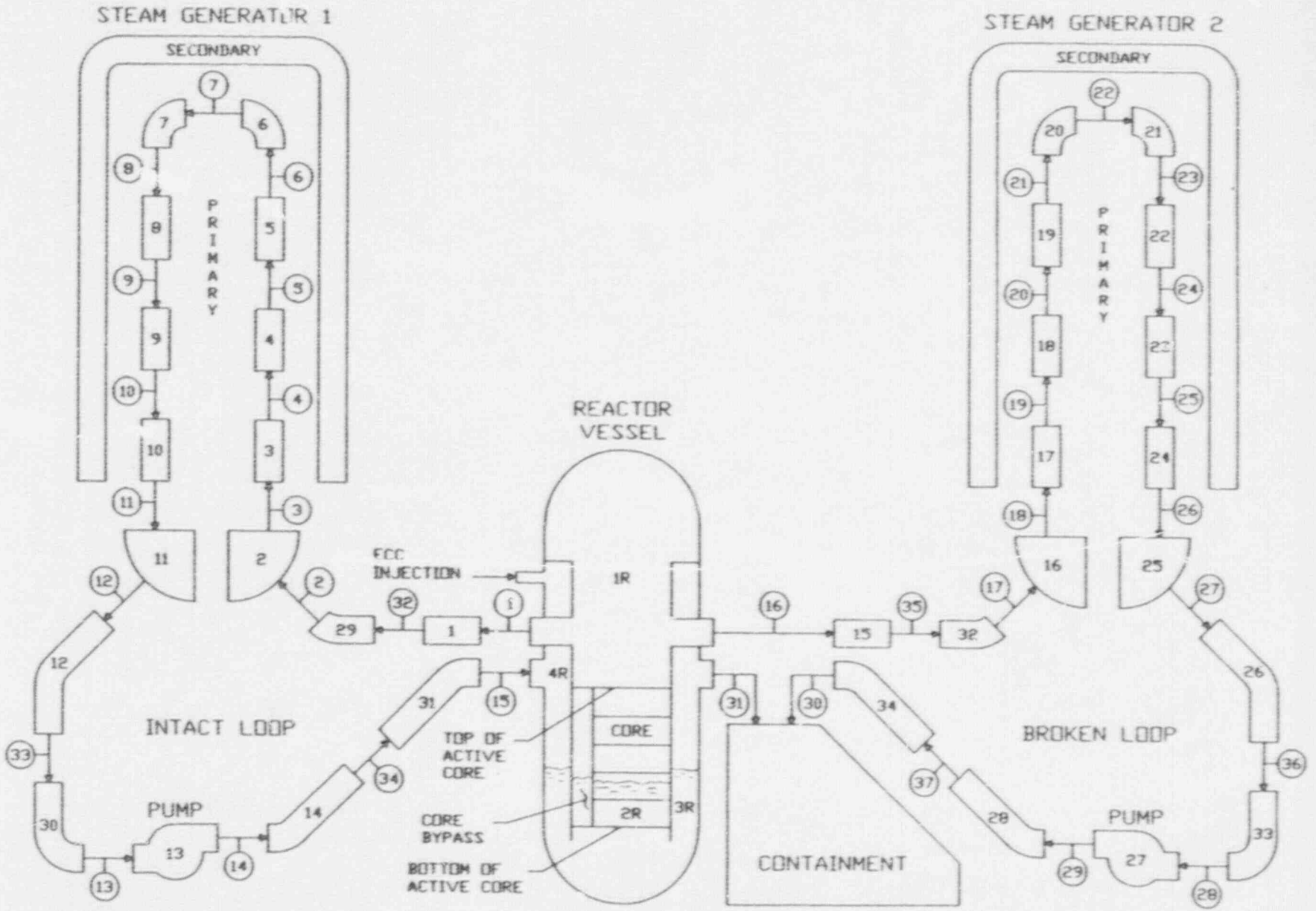


FIGURE A-14. REFLOD3B LOOP NODING STUDY - CORE FLOODING RATE.

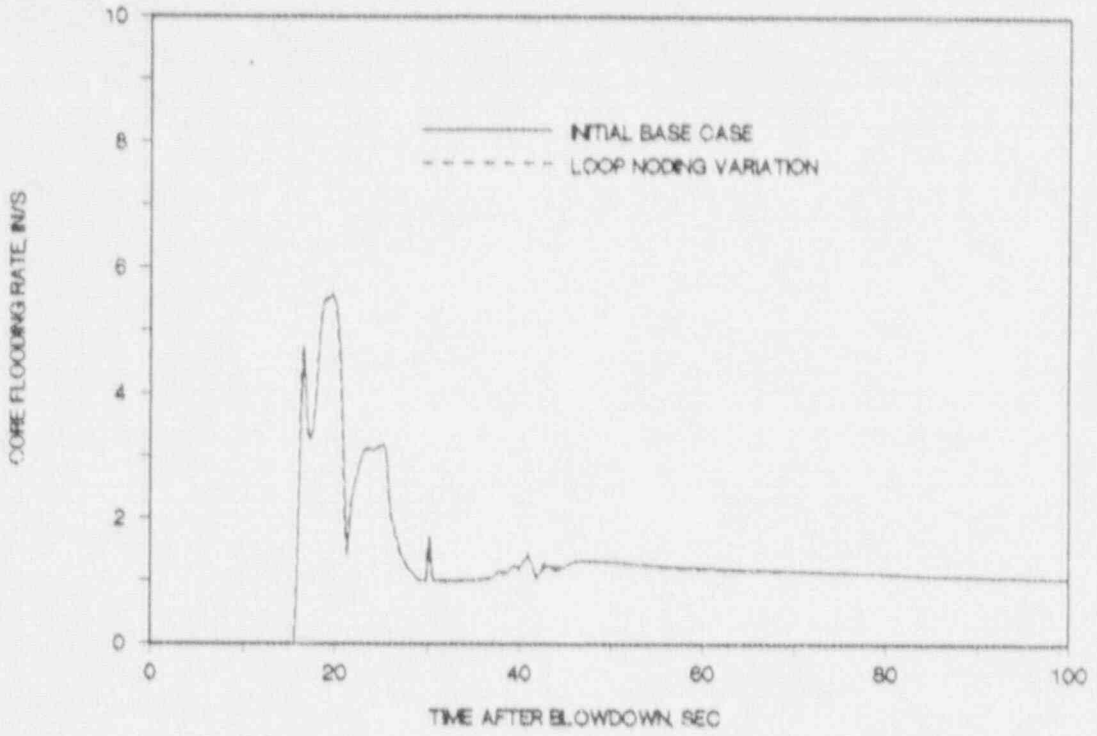



FIGURE A-15. REFLOD3B LOOP NODING STUDY - CORE WATER LEVEL.

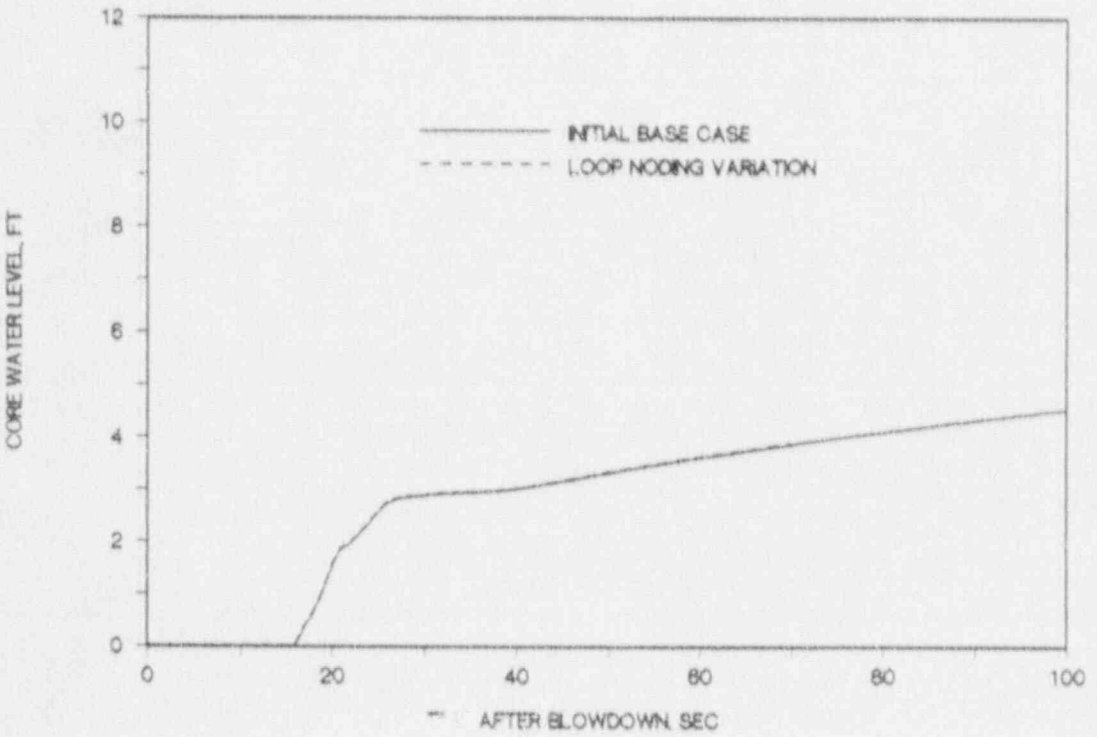


FIGURE A-16. REFLOD3B LOOP NODING STUDY - DOWNCOMER WATER LEVEL.

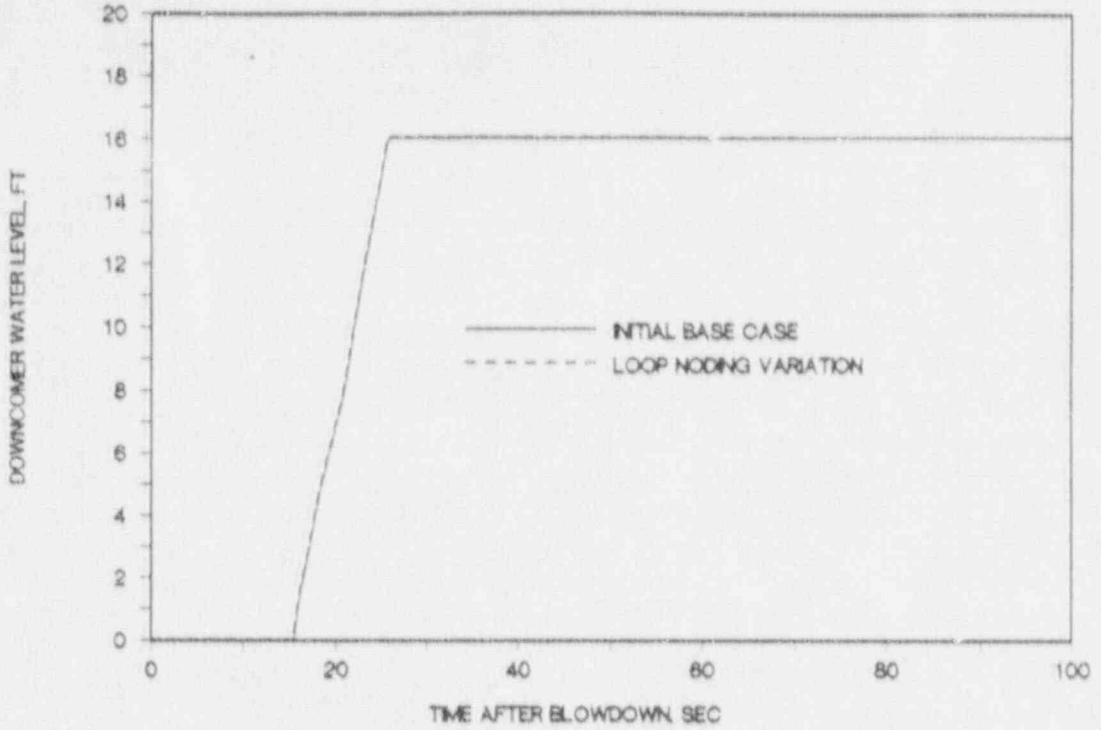



FIGURE A-17. REFLOD3B LOOP NODING STUDY - CRF.

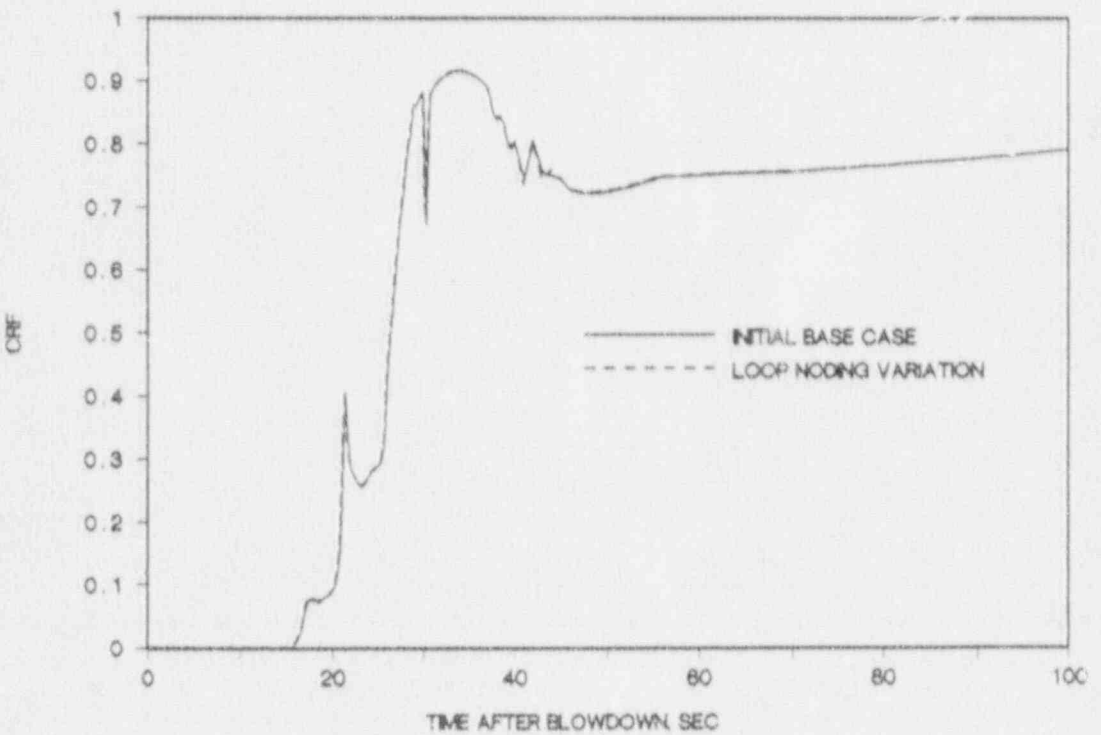


FIGURE A-18. REFLOD3B PUMP ROTOR STUDY - CORE FLOODING RATE.

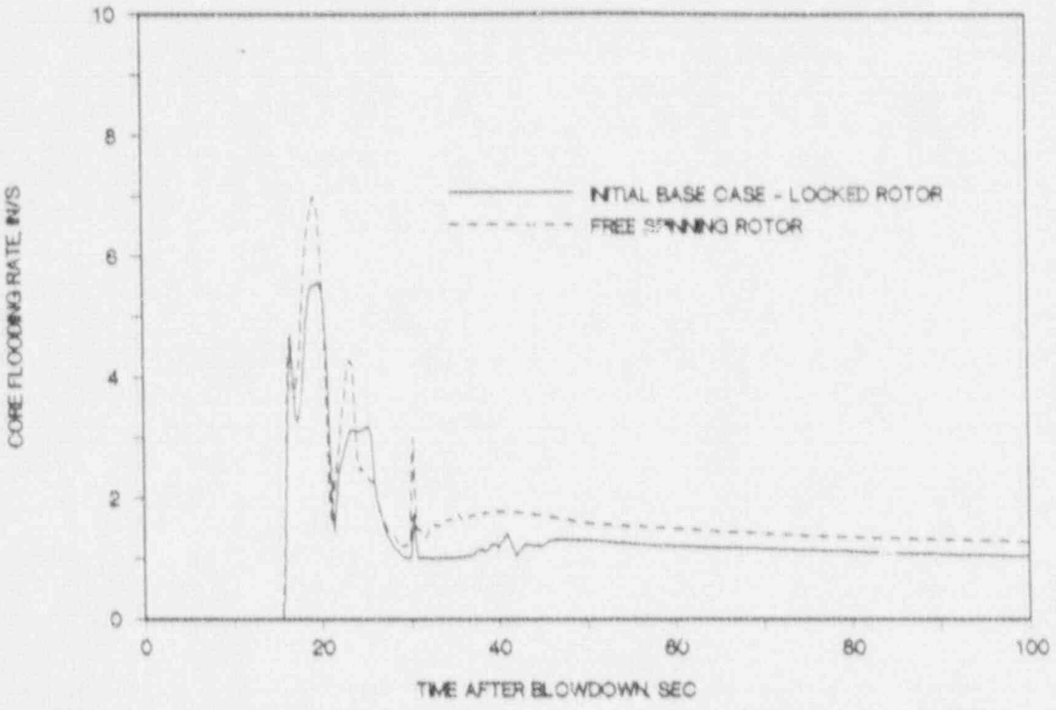



FIGURE A-19. REFLOD3B ECCS STUDY - CORE FLOODING RATE.

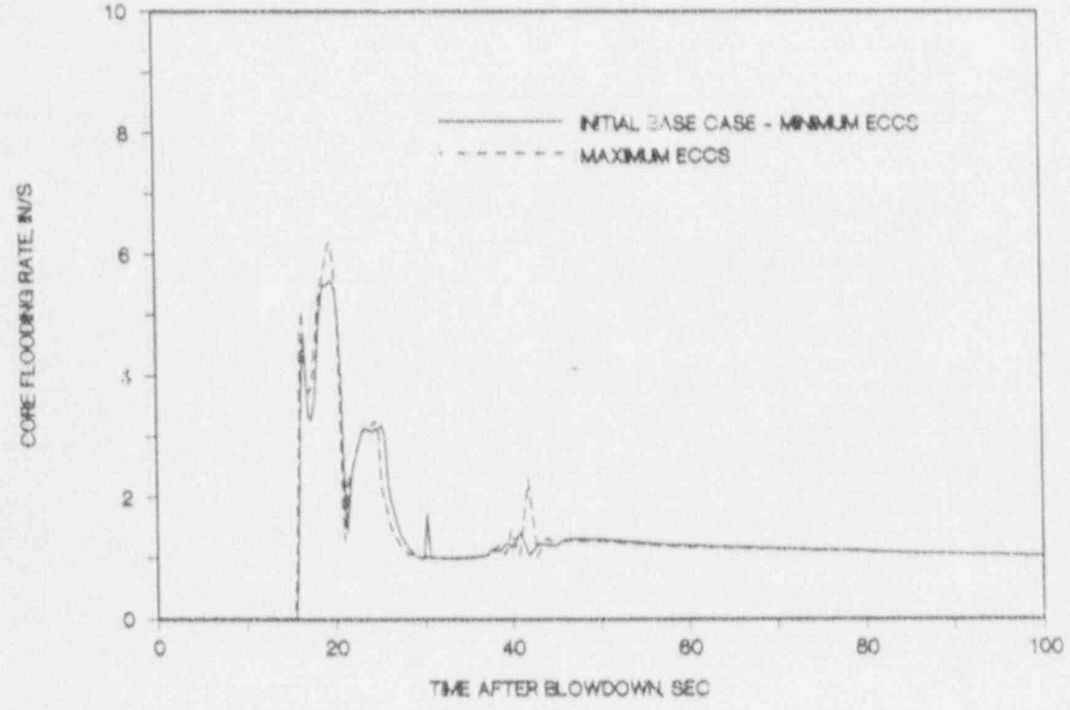


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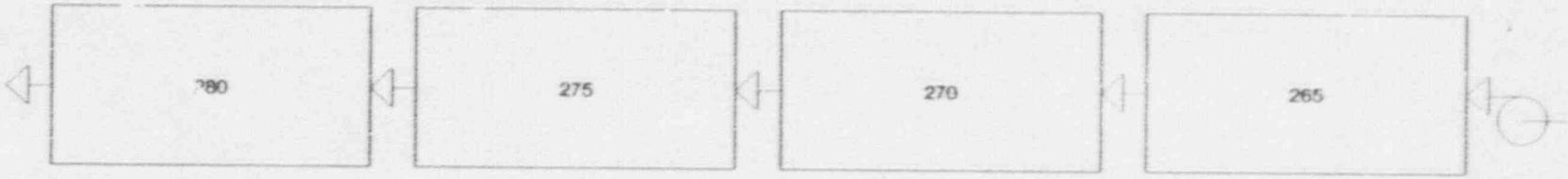
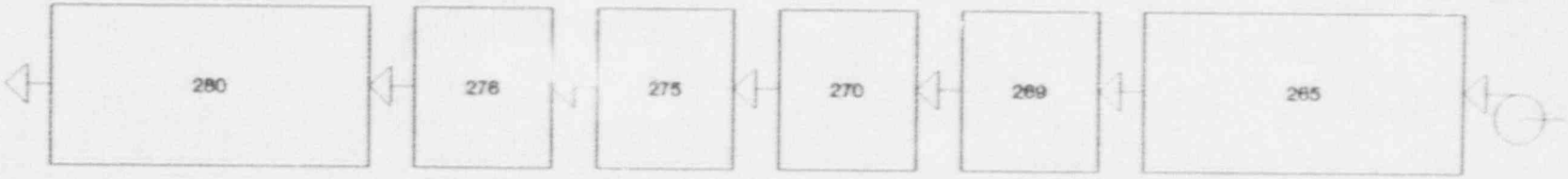
FIGURE A-20. RELAP5 BREAK NODING STUDY; COLD LEG NODING.

BASE CASE




6 NODE COLD LEG




8 NODE COLD LEG

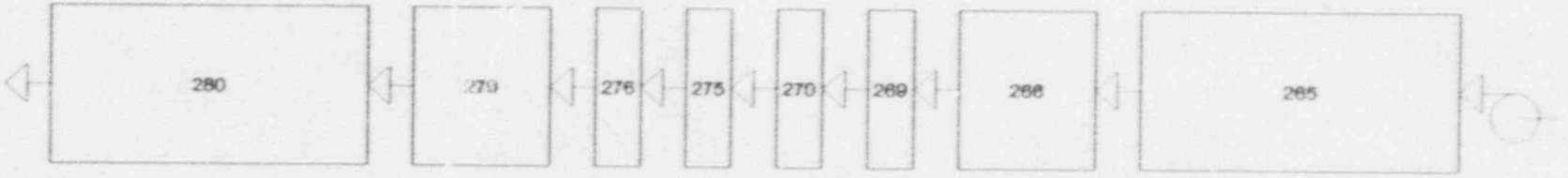


FIGURE A-21. RELAP5 BREAK NODING STUDY - LEAK FLOW AT RV SIDE.

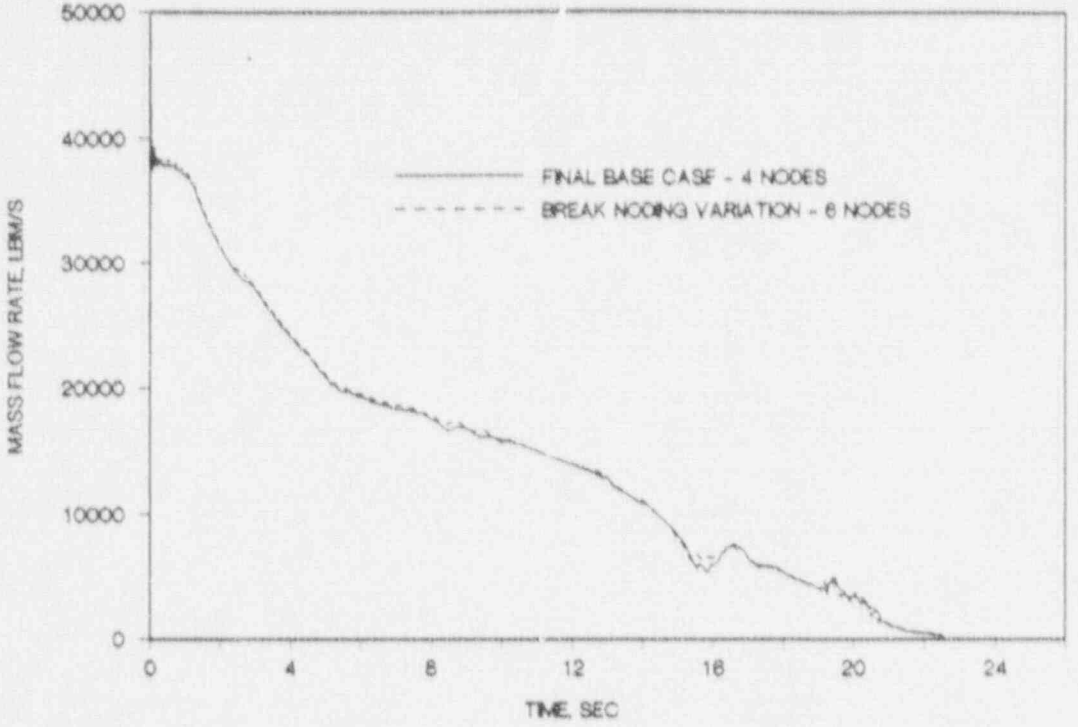



FIGURE A-22. RELAP5 BREAK NODING STUDY - LEAK FLOW AT PUMP SIDE.

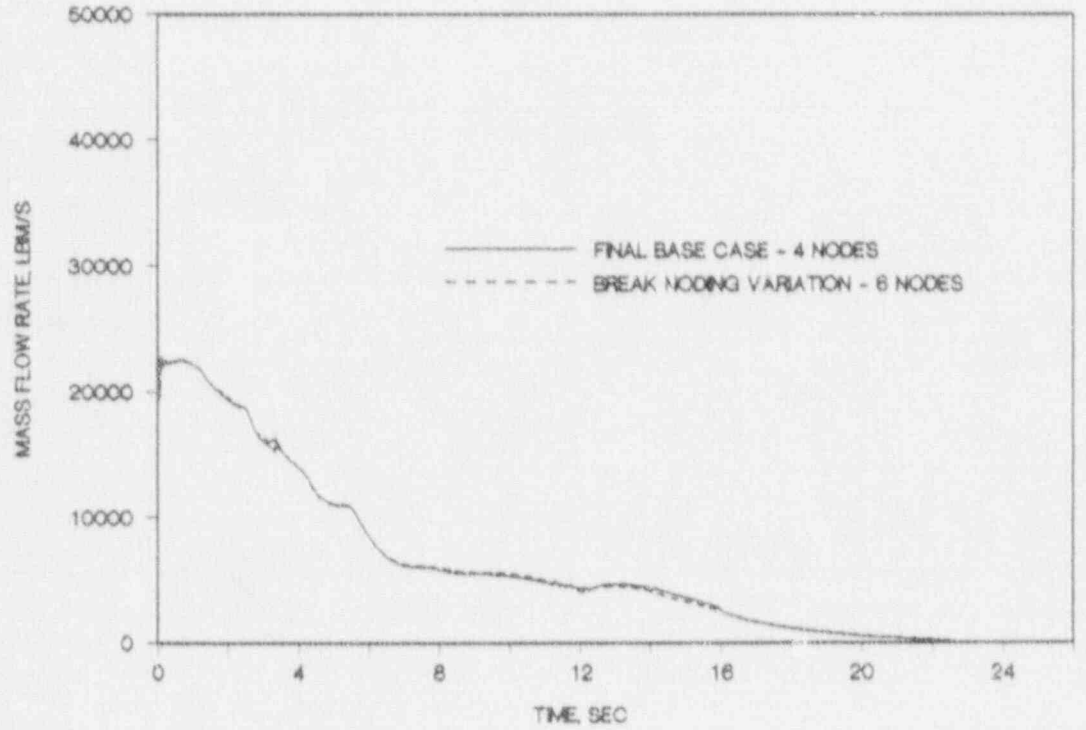


FIGURE A-23. RELAP5 BREAK NODING STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

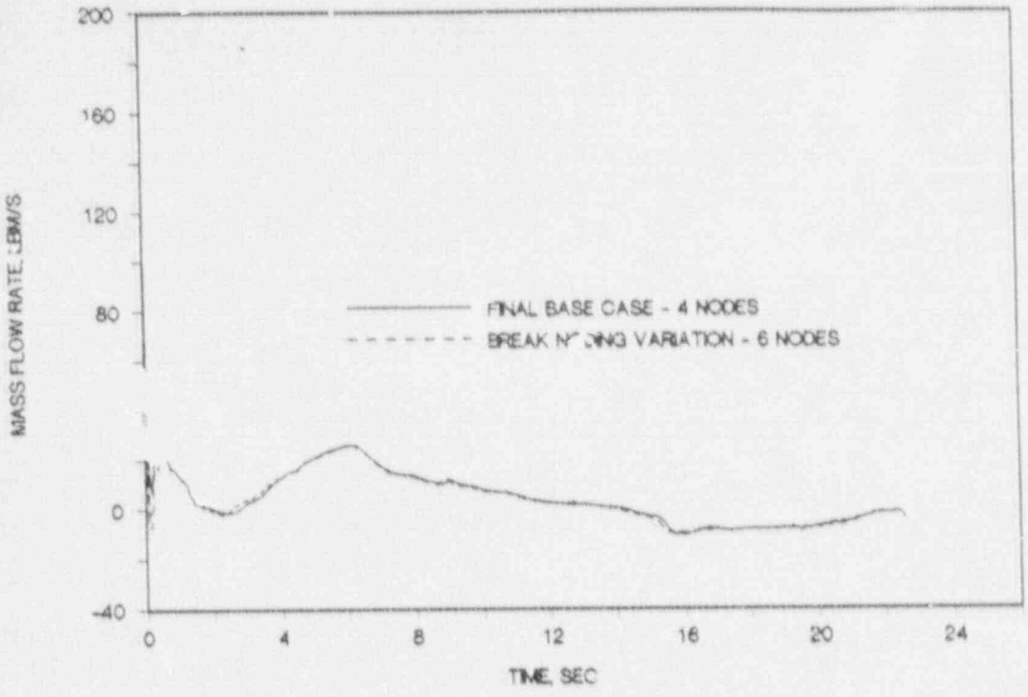



FIGURE A-24. RELAP5 BREAK NODING STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

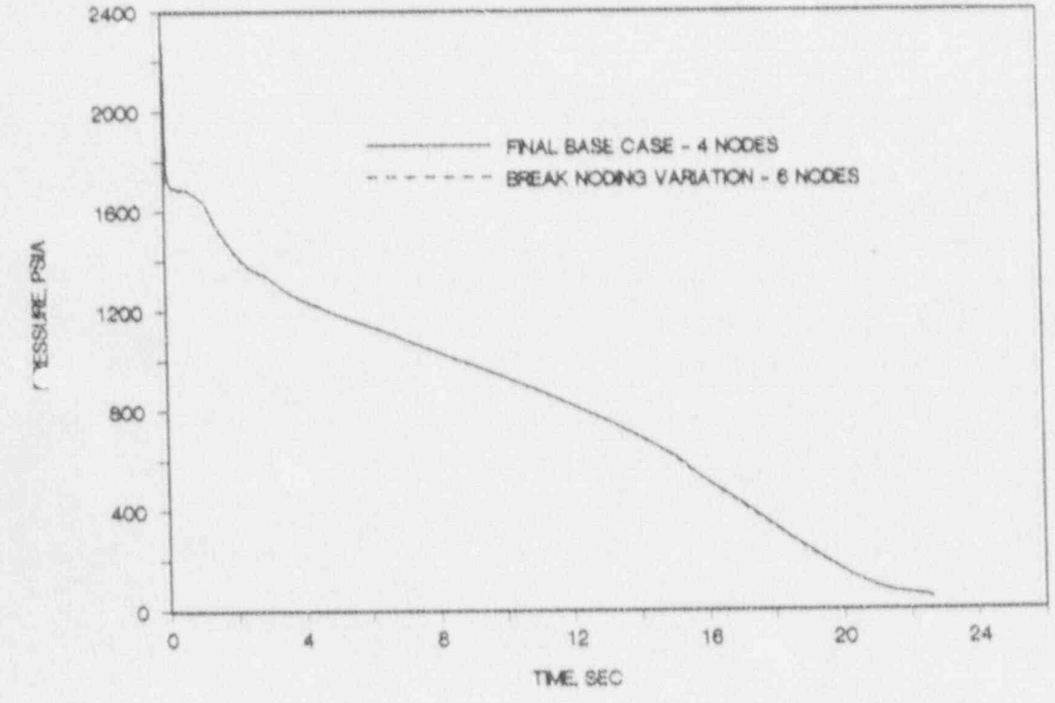


FIGURE A-25. RELAP5 BREAK NODING STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

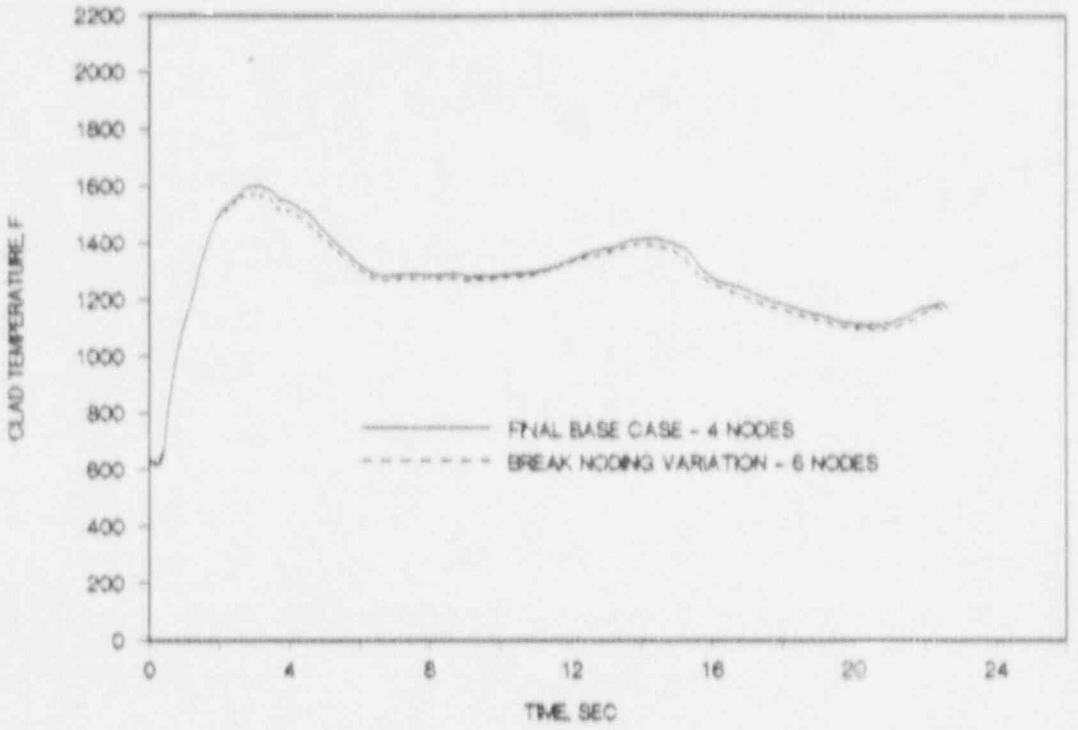



FIGURE A-26. RELAP5 PRESSURIZER LOCATION STUDY - LEAK FLOW AT RV SIDE.

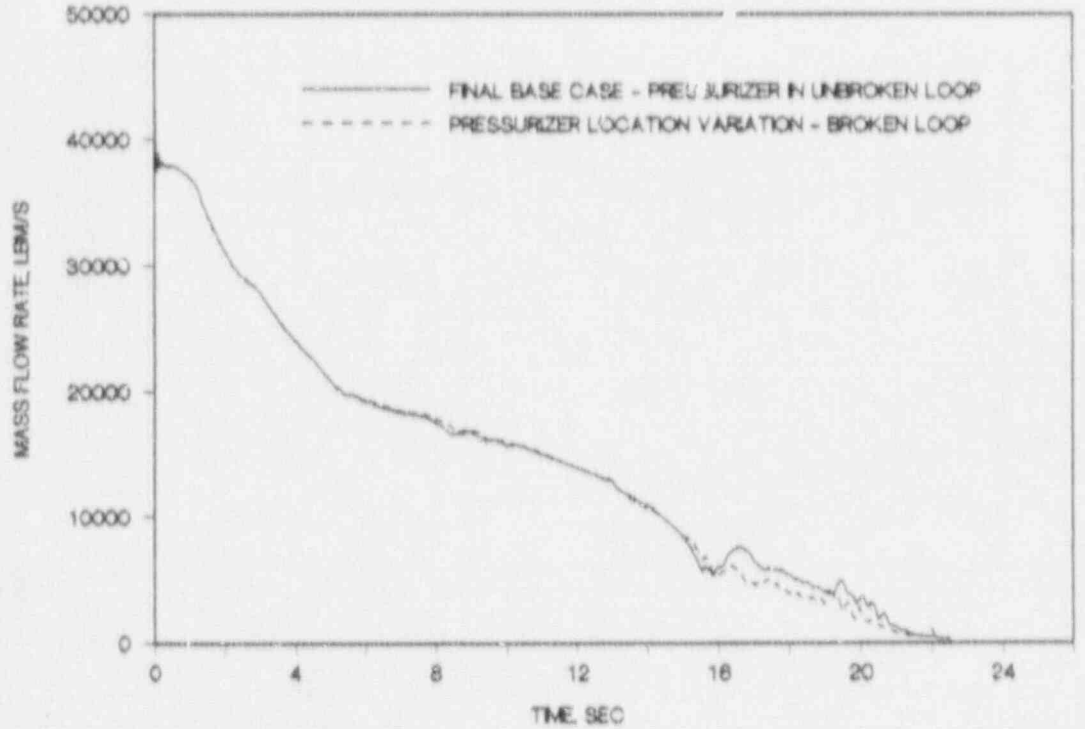


FIGURE A-27. RELAP5 PRESSURIZER LOCATION STUDY - LEAK FLOW AT PUMP SIDE.

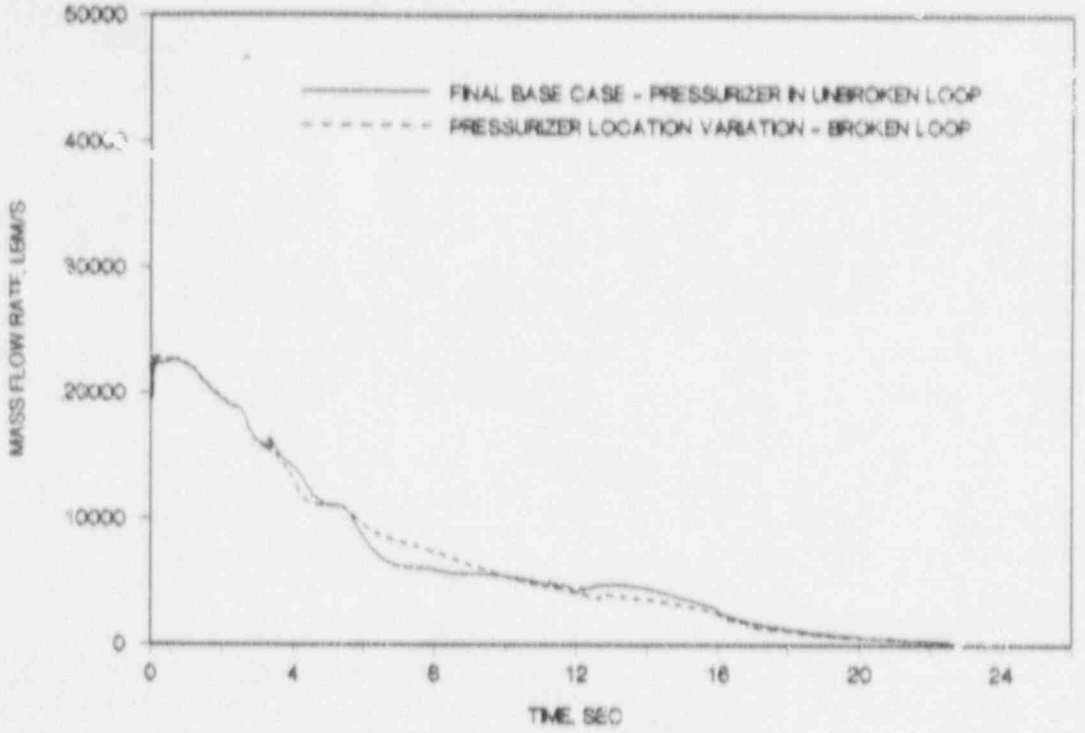



FIGURE A-28. RELAP5 PRESSURIZER LOCATION STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

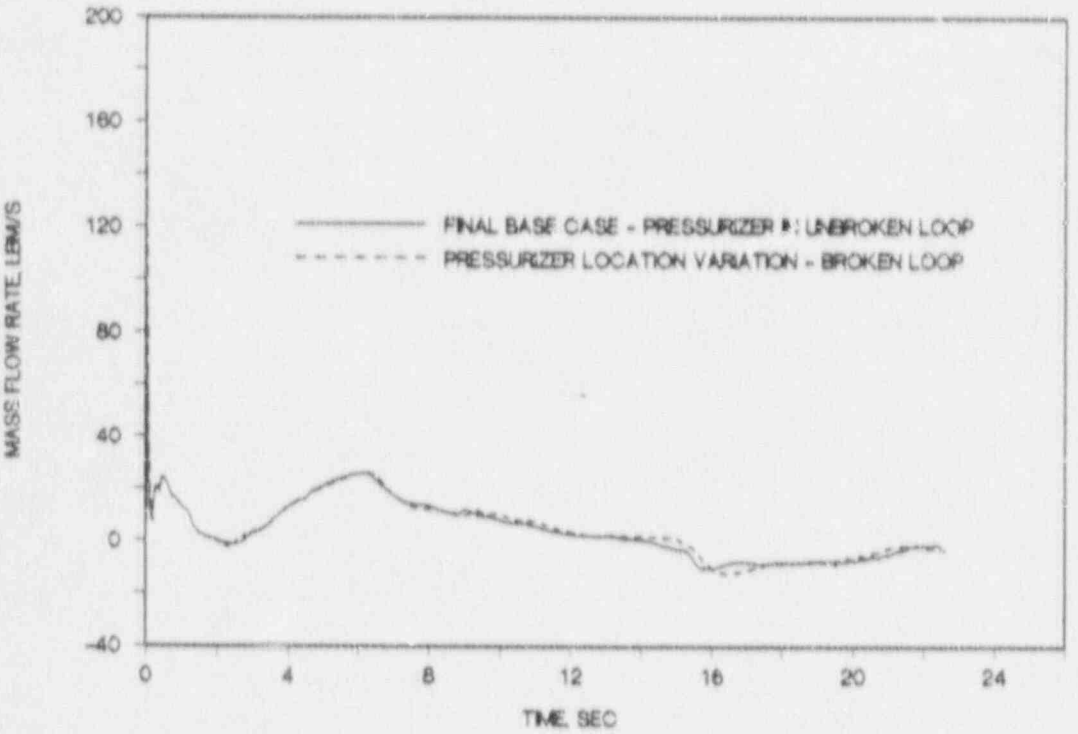


FIGURE A-29. RELAP5 PRESSURIZER LOCATION STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

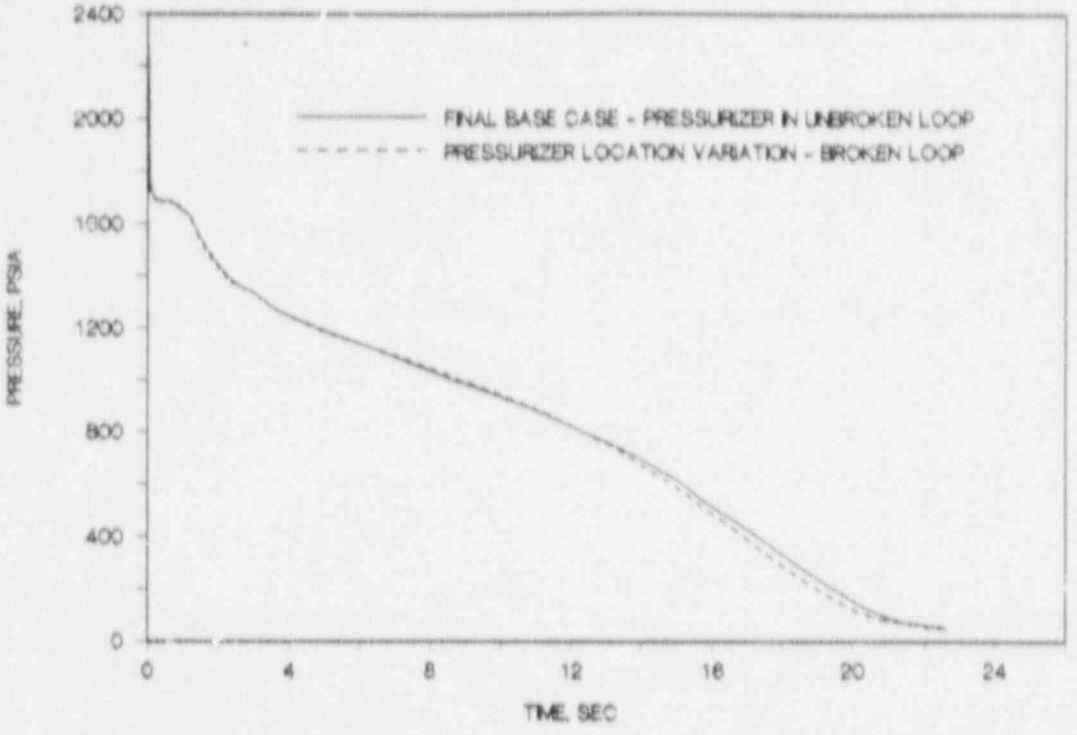



FIGURE A-30. RELAP5 PRESSURIZER LOCATION STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

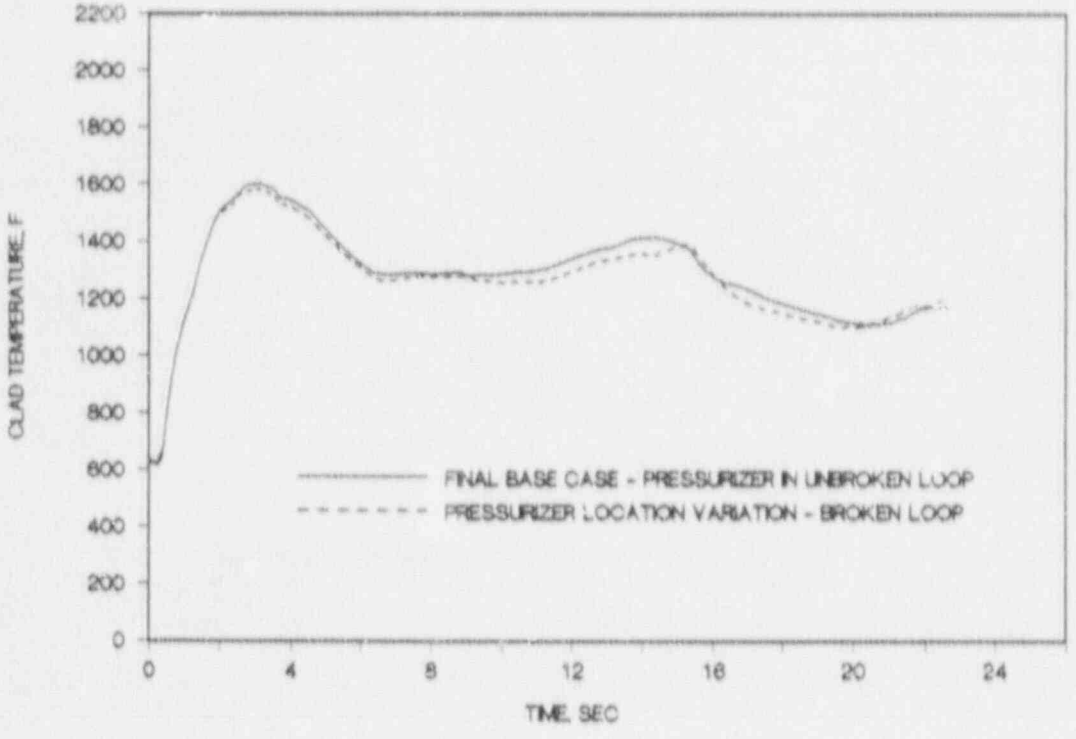


FIGURE A-31. RELAP5 PUMP DEGRADATION STUDY - PUMP HEAD MULTIPLIER CURVES.

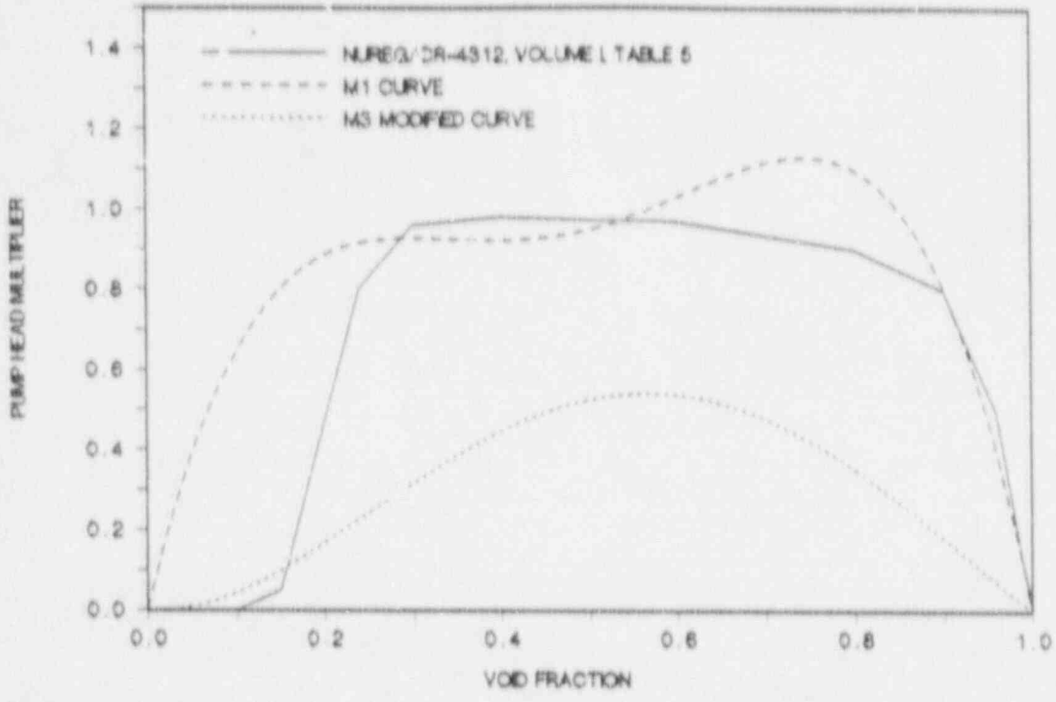



FIGURE A-32. RELAP5 PUMP DEGRADATION STUDY - LEAK FLOW AT RV SIDE.

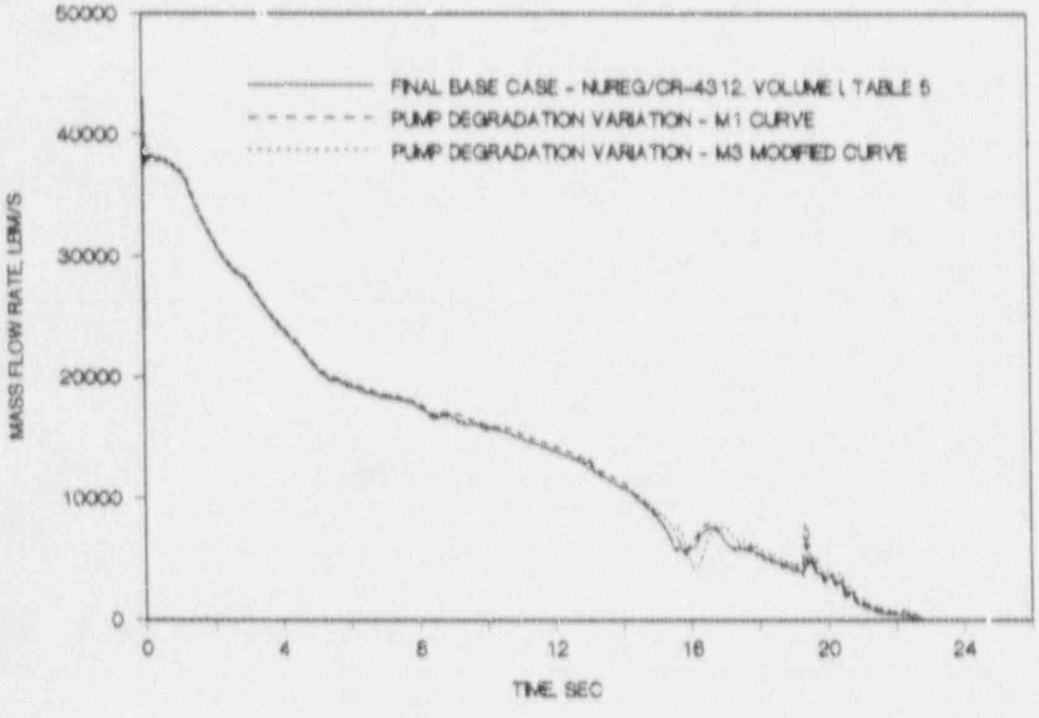


FIGURE A-33. RELAP5 PUMP DEGRADATION STUDY - LEAK FLOW AT PUMP SIDE.

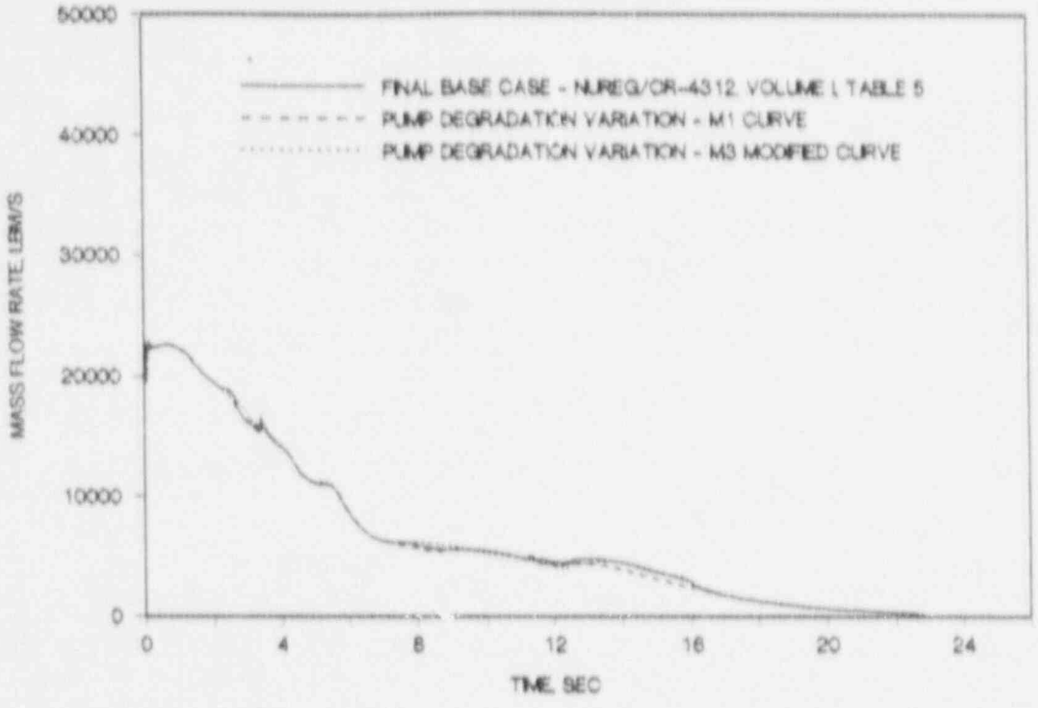



FIGURE A-34. RELAP5 PUMP DEGRADATION STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

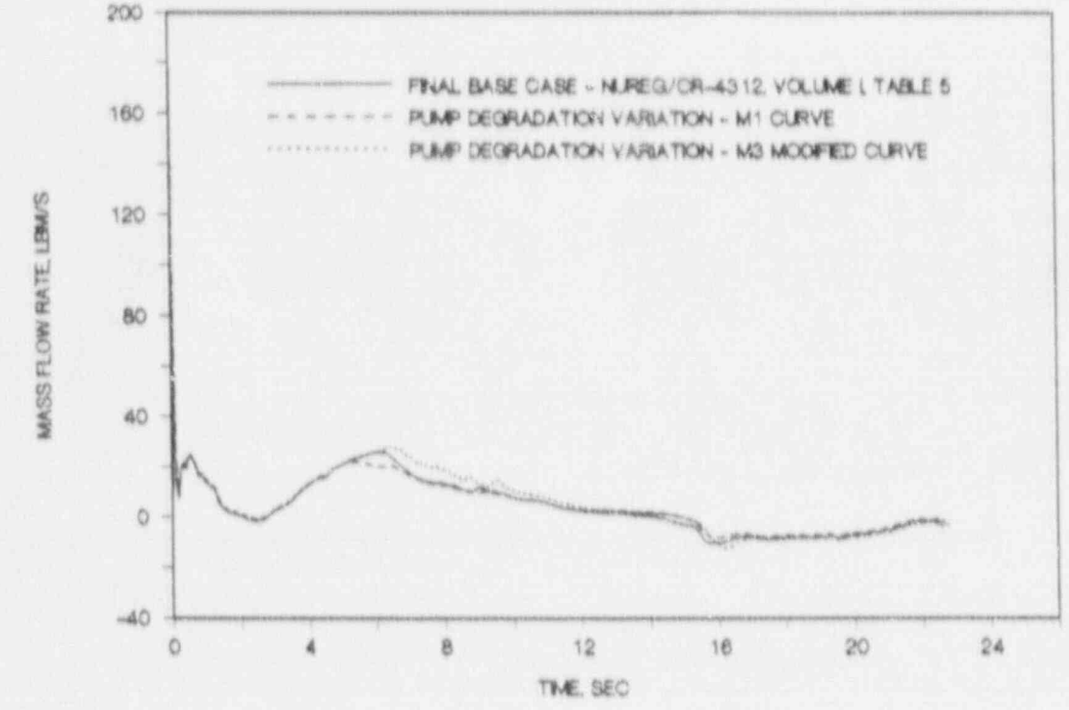


FIGURE A-35. RELAP5 PUMP DEGRADATION STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

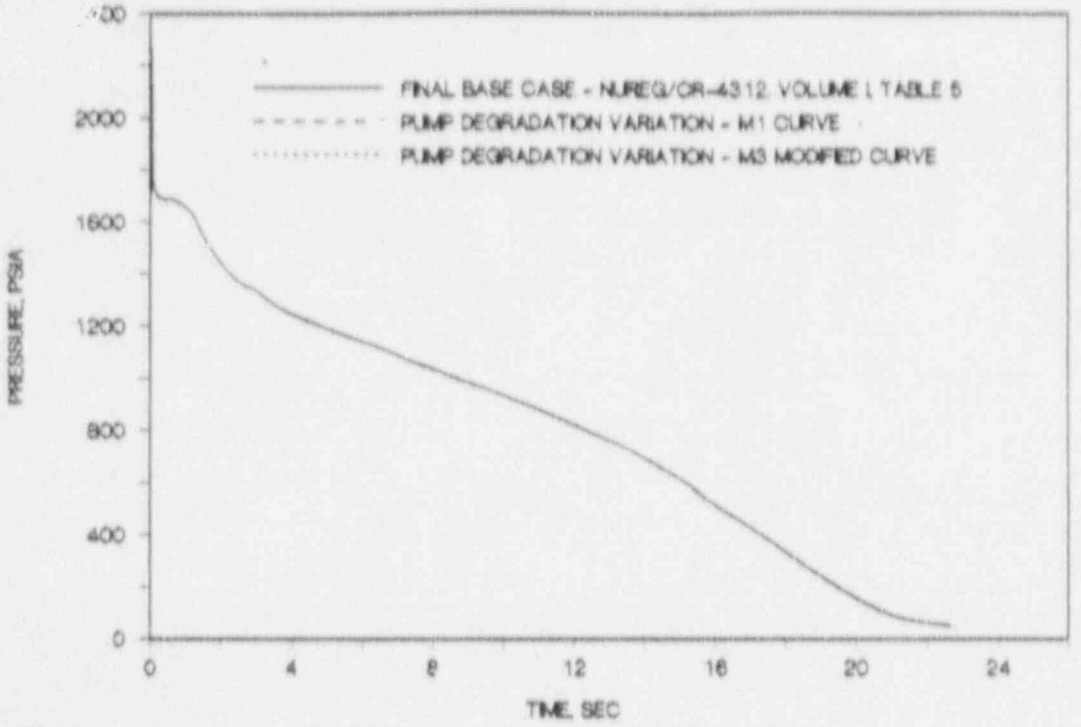



FIGURE A-36. RELAP5 PUMP DEGRADATION STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

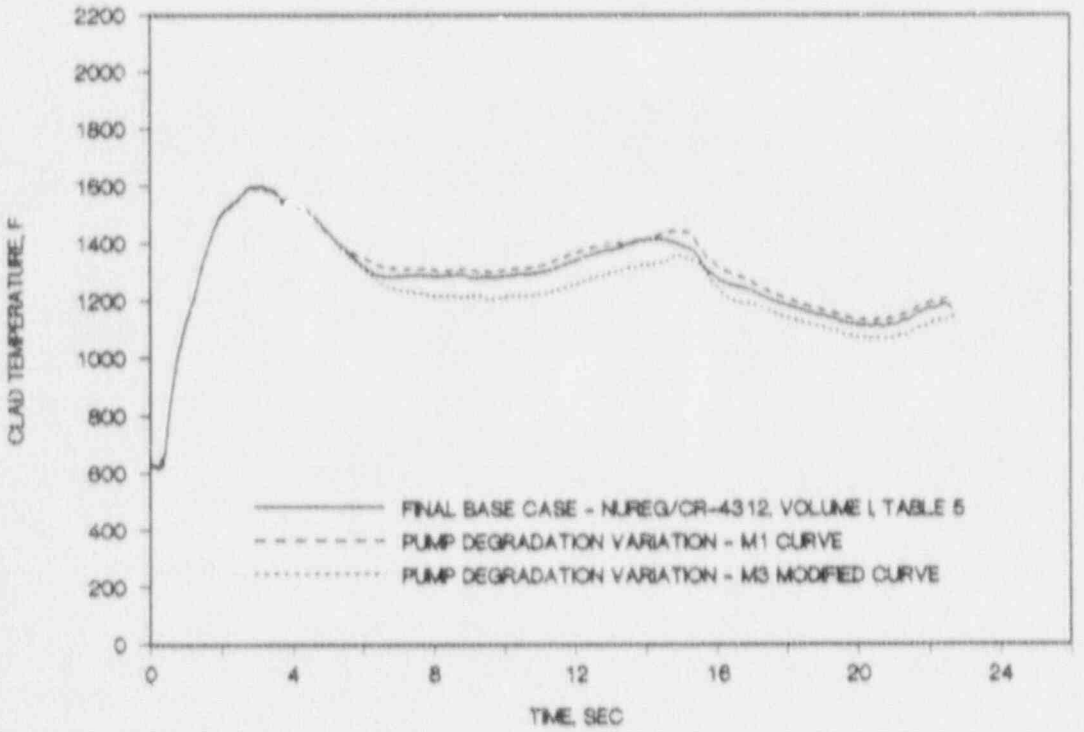


FIGURE A-37. RELAP5 CROSS FLOW STUDY - LEAK FLOW AT RV SIDE.

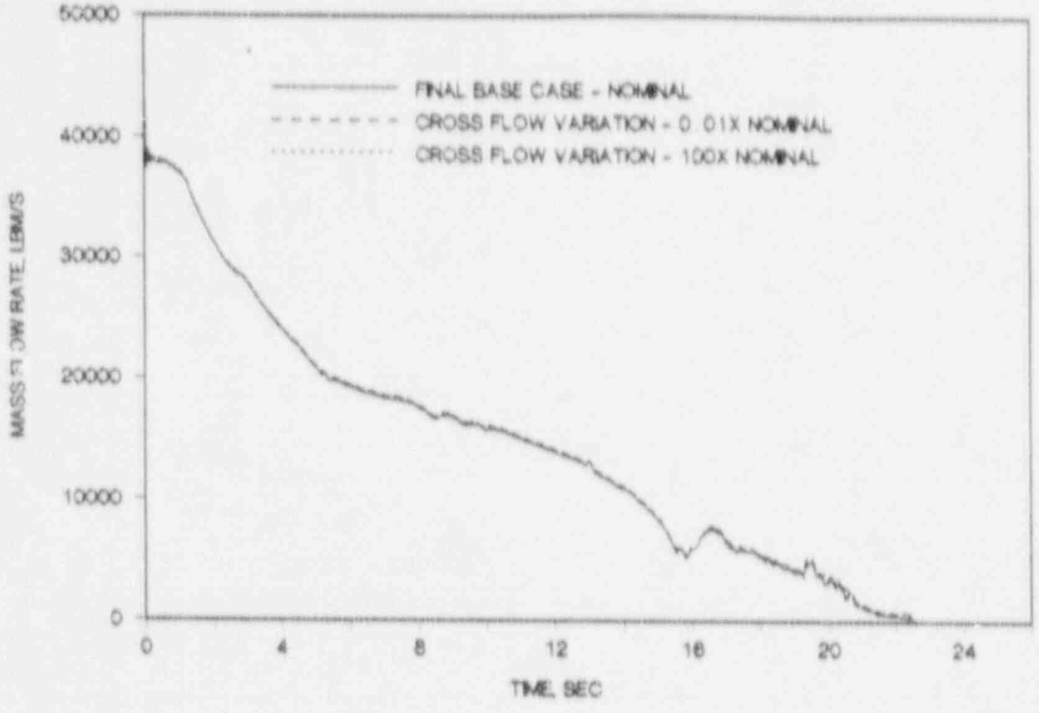



FIGURE A-38. RELAP5 CROSS FLOW STUDY - LEAK FLOW AT PUMP SIDE.

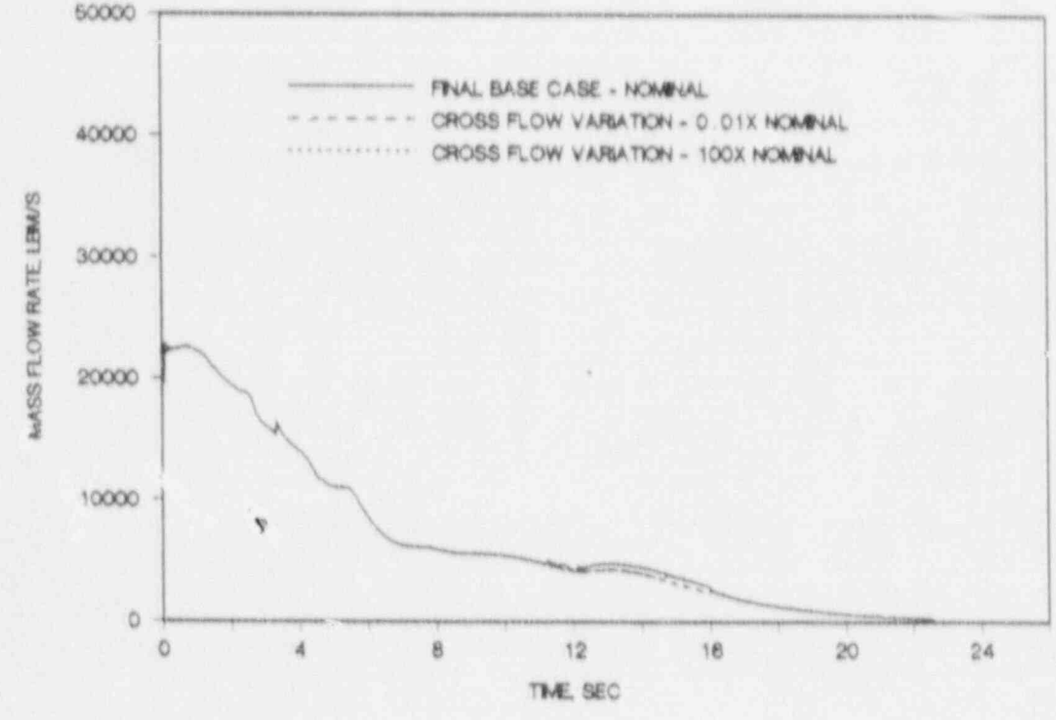


FIGURE A-39. RELAP5 CROSS FLOW STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

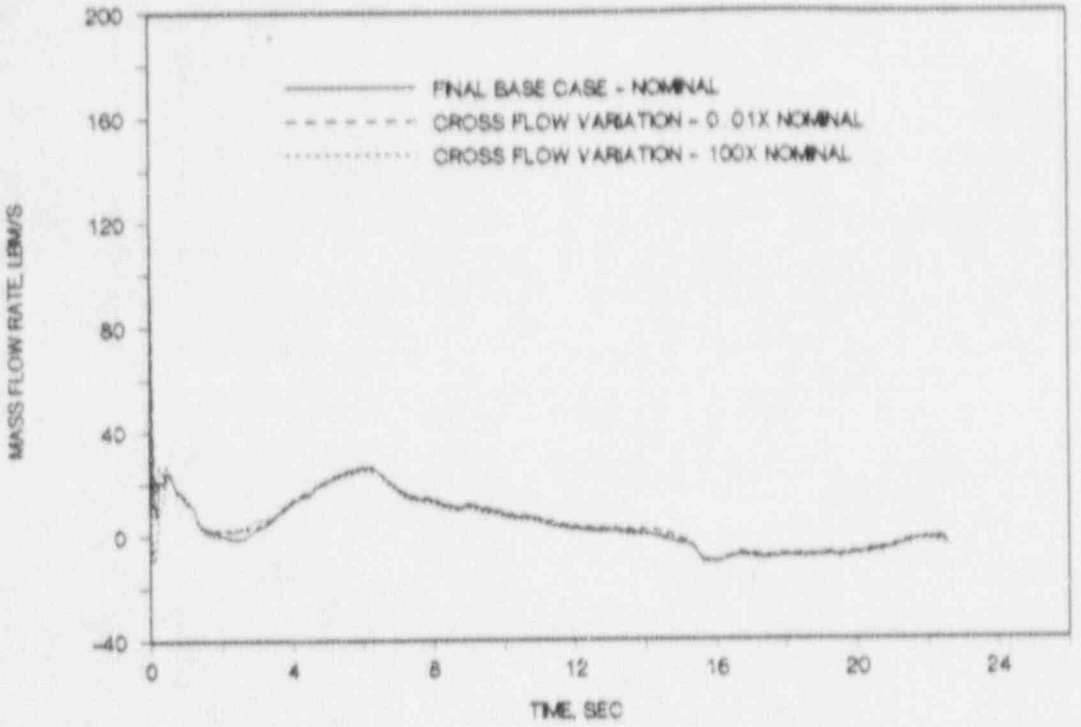



FIGURE A-40. RELAP5 CROSS FLOW STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

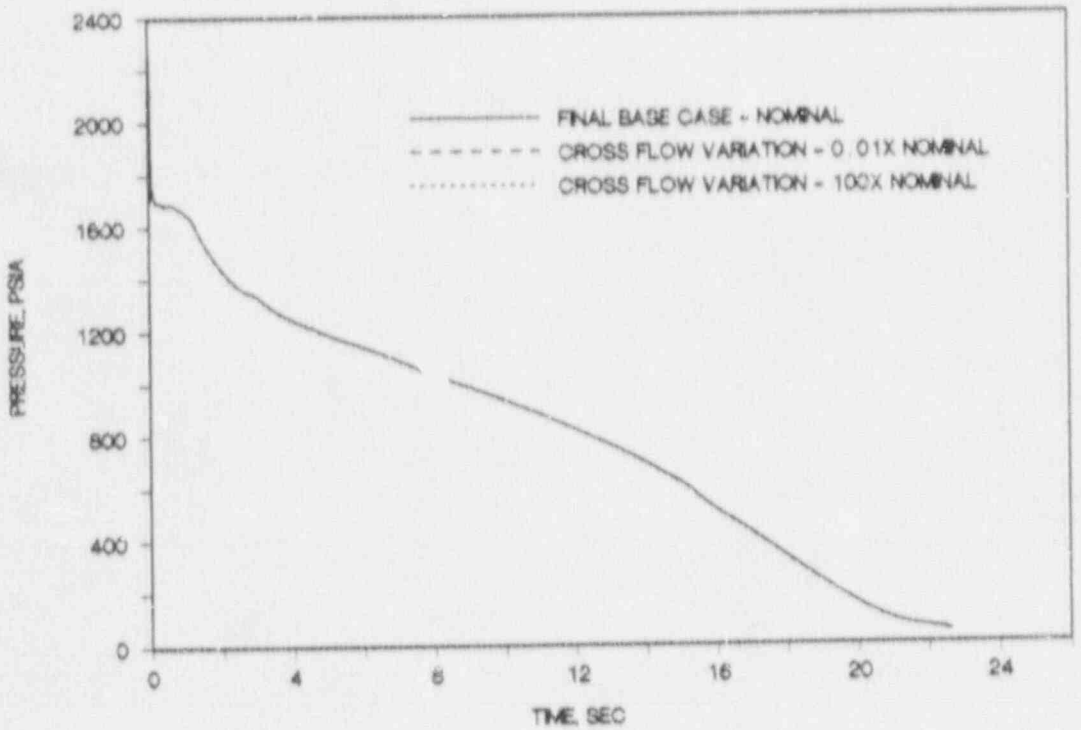


FIGURE A-41. RELAP5 CROSS FLOW STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

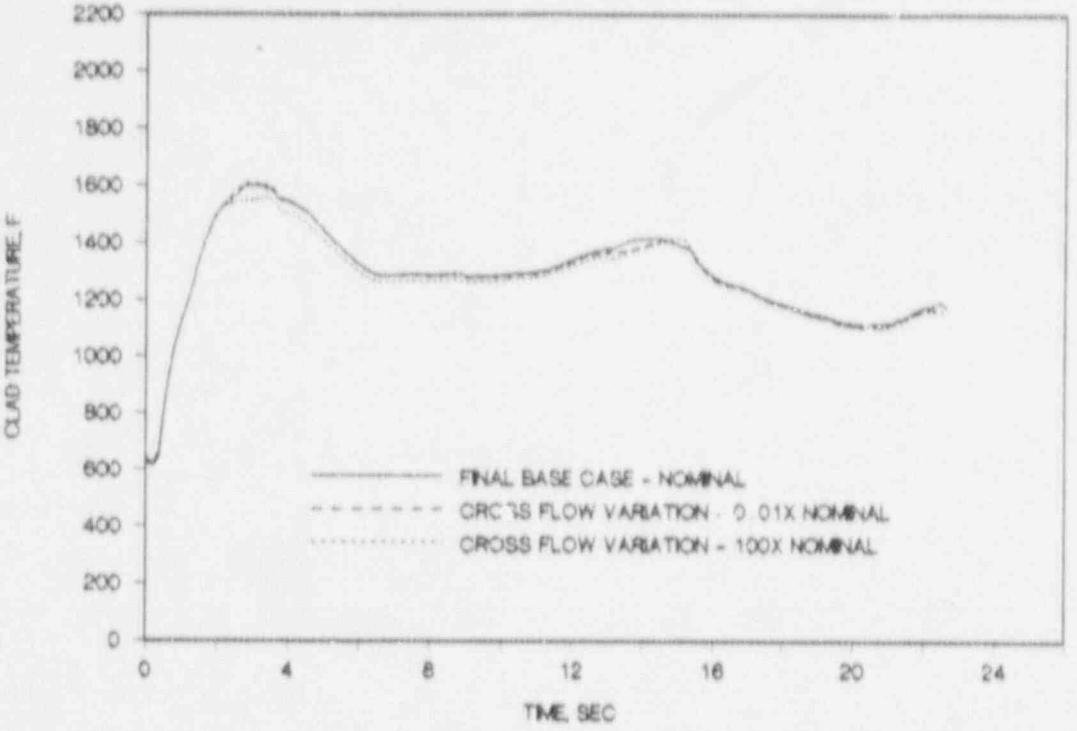


FIGURE A-42. RELAP5 CORE NODING STUDY - 2 x 12 CONFIGURATION.

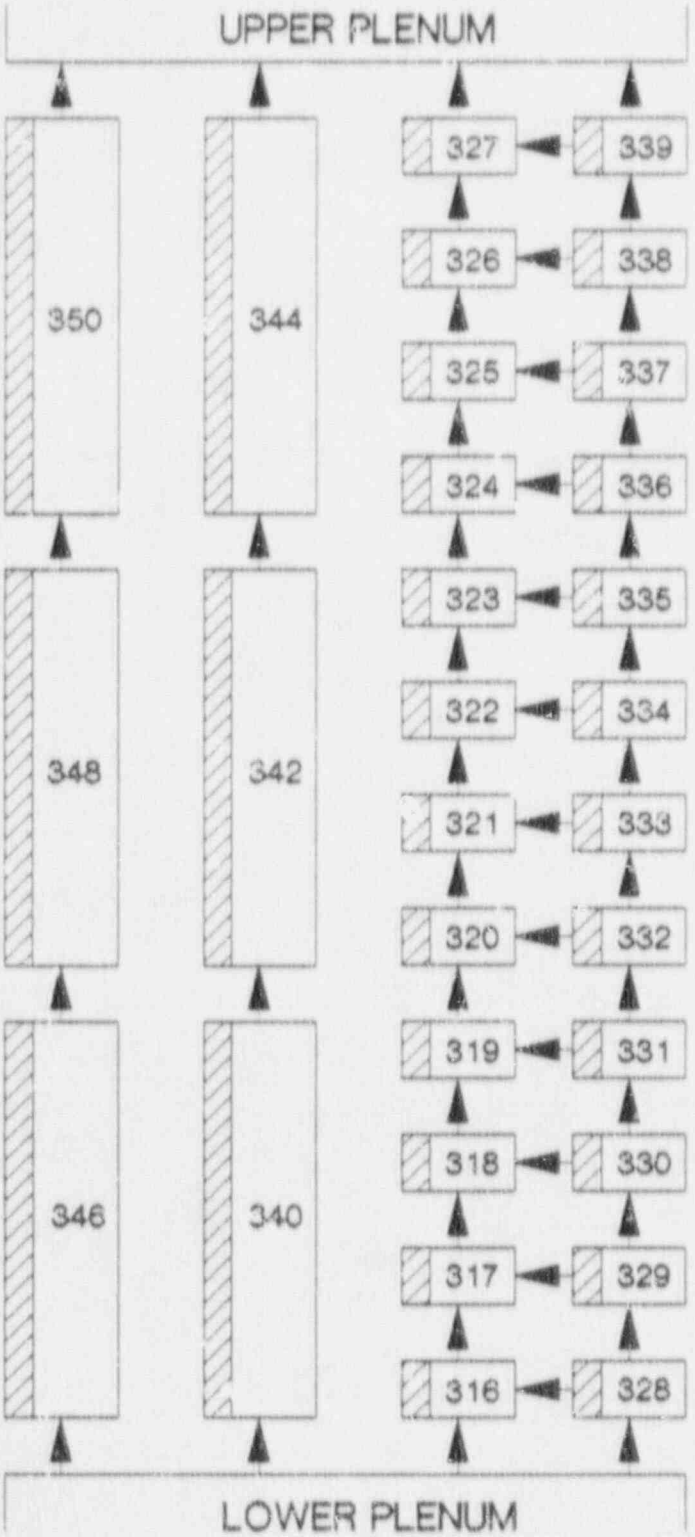


FIGURE A-43. RELAP5 CORE NODING STUDY - 3 x 6 CONFIGURATION.

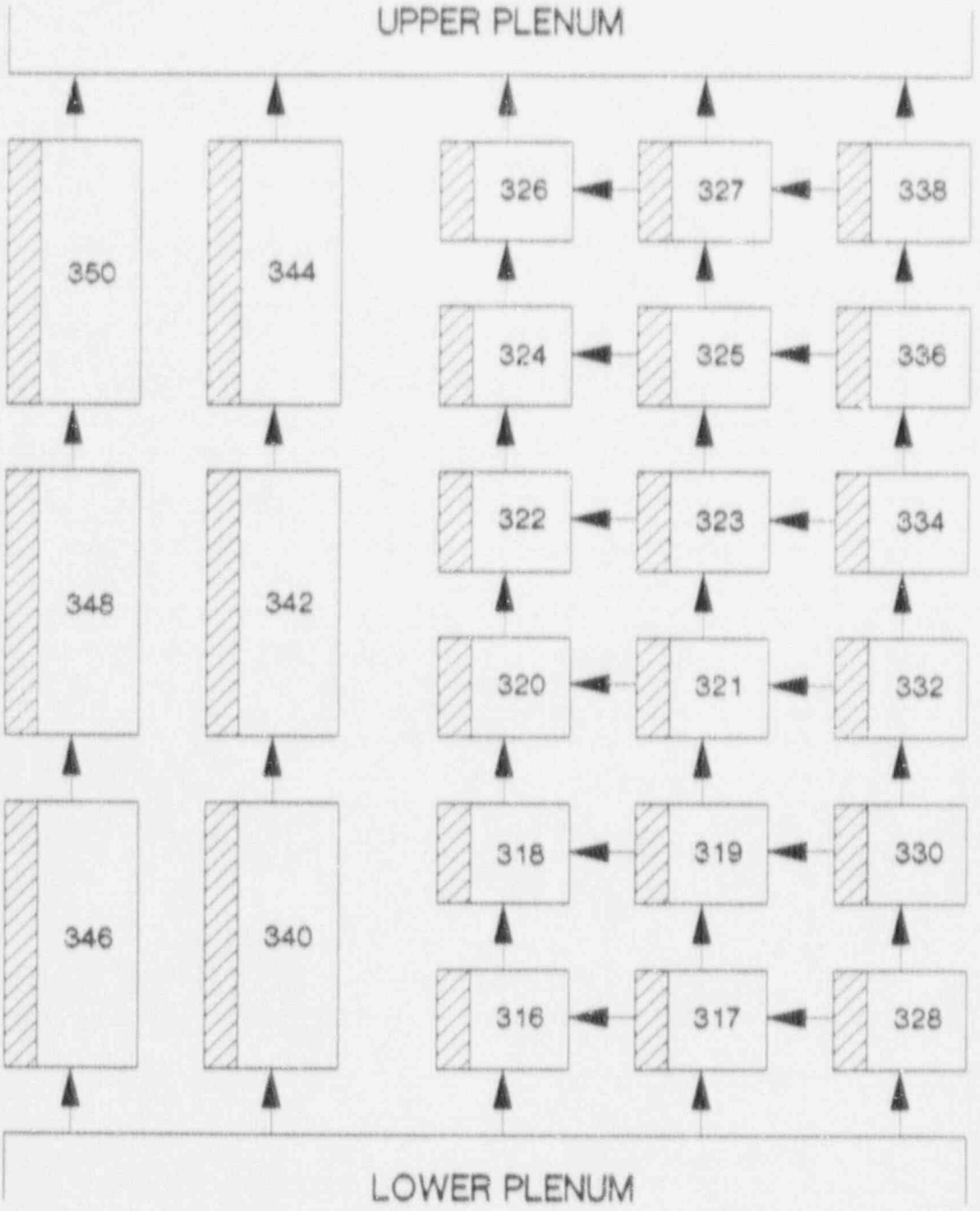


FIGURE A-44. RELAP5 CORE NODING STUDY - LEAK FLOW AT RV SIDE.

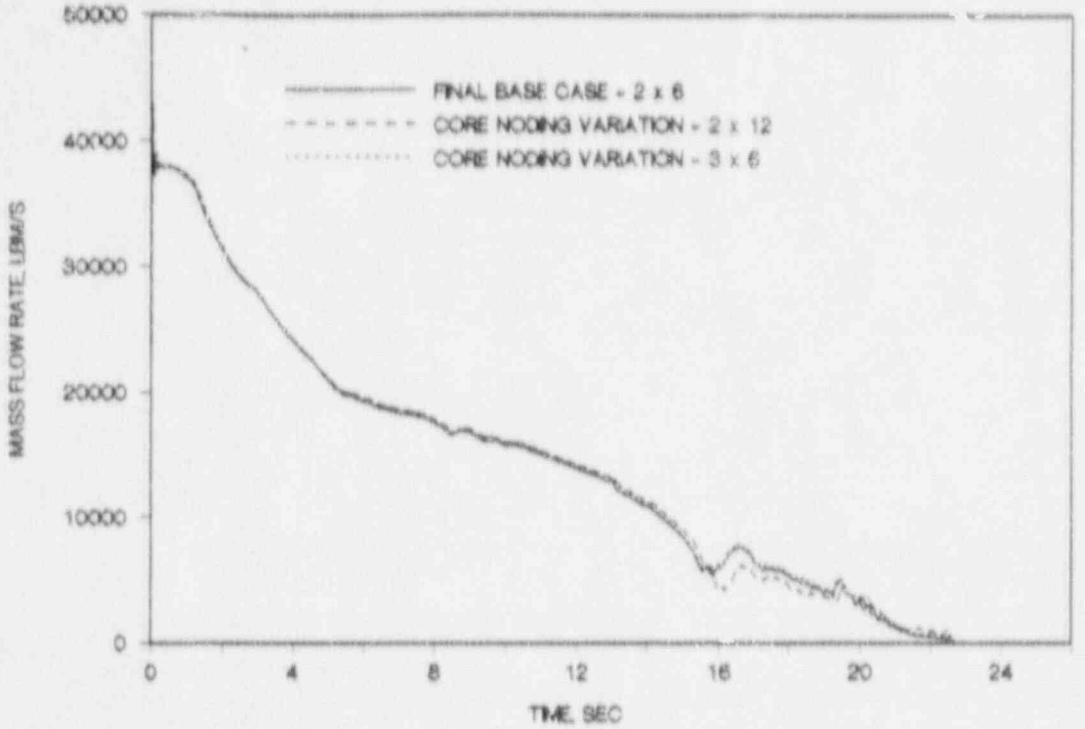



FIGURE A-45. RELAP5 CORE NODING STUDY - LEAK FLOW AT PUMP SIDE.

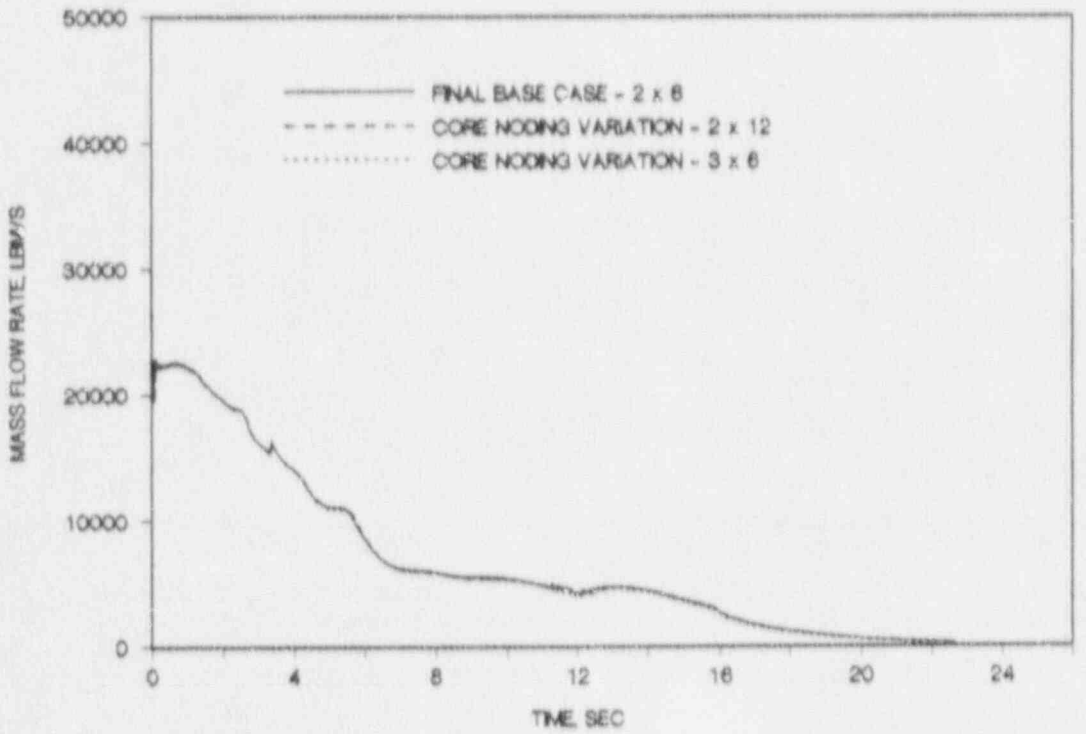


FIGURE A-46. RELAP5 CROSS FLOW STUDY - HOT CHANNEL FLOW AT PEAK POWER ELEVATION.

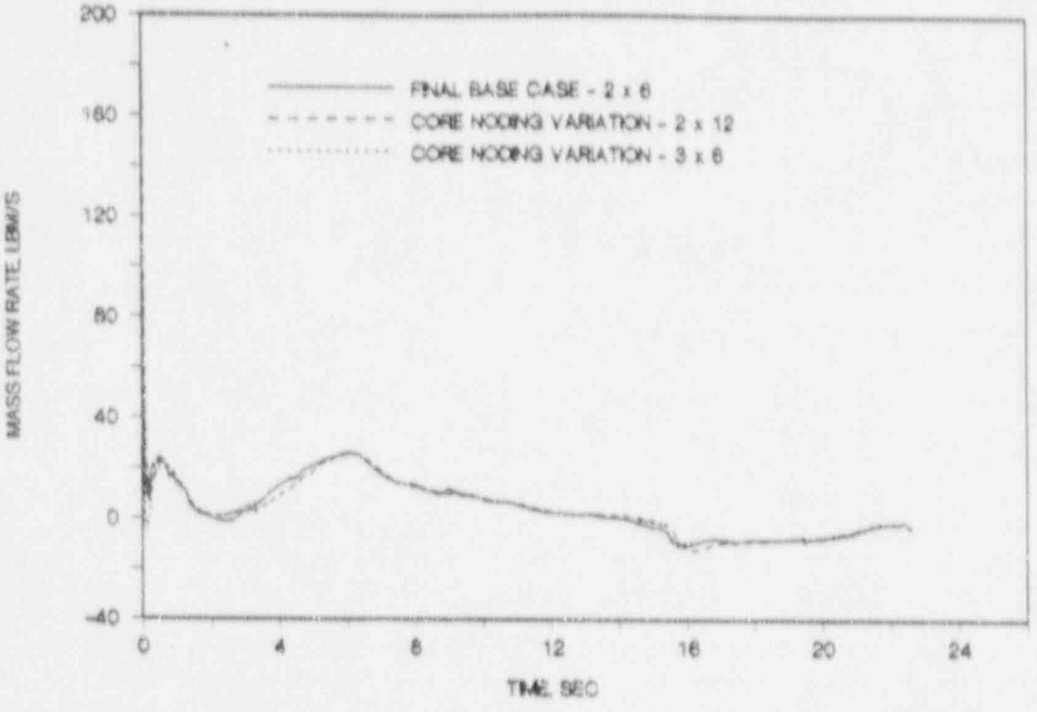



FIGURE A-47. RELAP5 CORE NODING STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

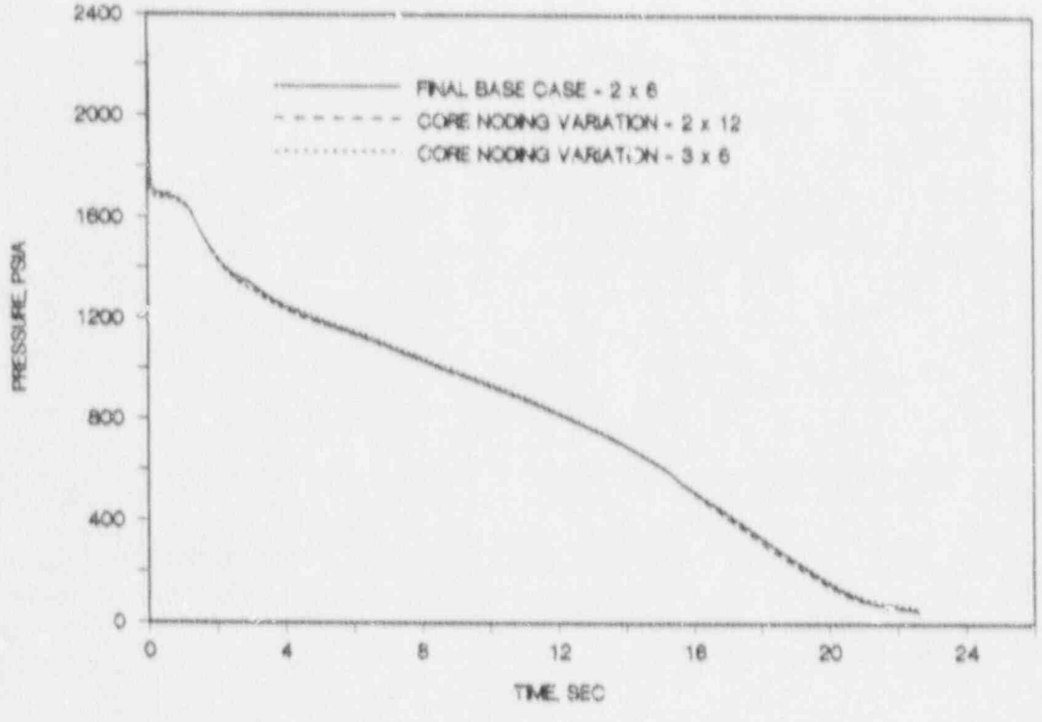


FIGURE A-48. RELAP5 CORE NODING STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK POWER ELEVATION.

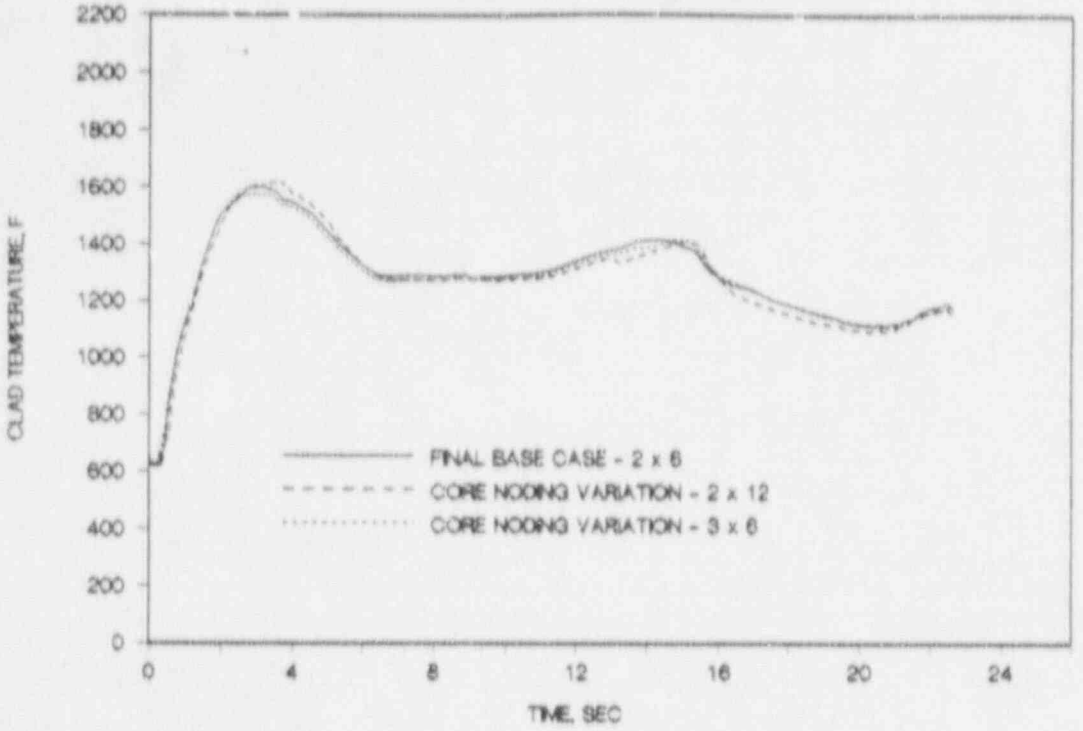
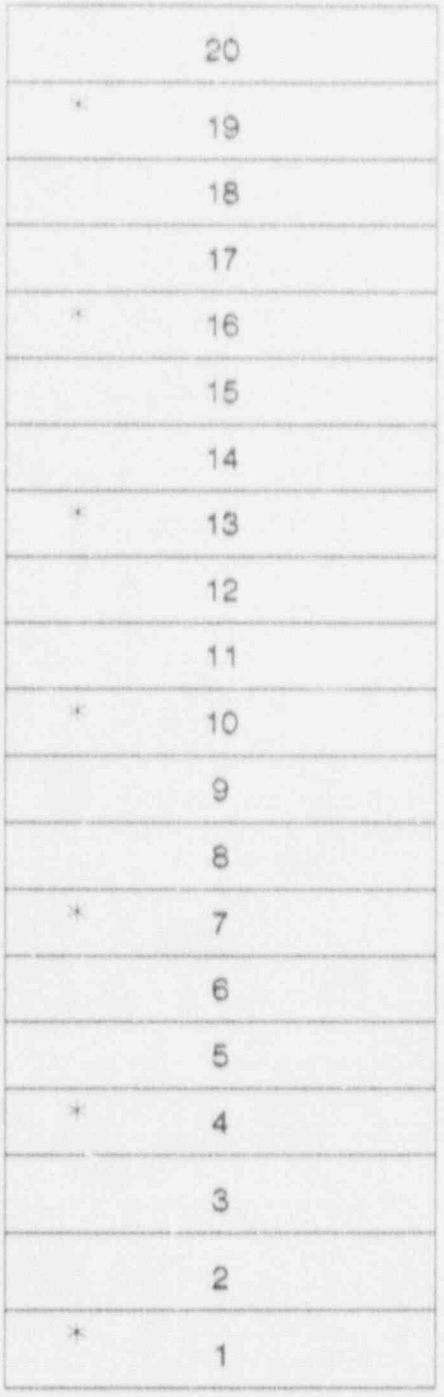


FIGURE A-49. BEACH AND FRAP-T6-B&W NODING DIAGRAM FOR MARK-BW FUEL ASSEMBLY.




| NODE | ELEVATION |
|---|---|
| | 12.00' |
| 20 | |
| | 11.28' |
| * 19 | |
| | 10.56' |
| 18 | |
| | 9.99' |
| 17 | |
| | 9.42' |
| * 16 | |
| | 8.85' |
| 15 | |
| | 8.28' |
| 14 | |
| | 7.71' |
| * 13 | |
| | 7.14' |
| 12 | |
| | 6.57' |
| 11 | |
| | 6.00' |
| * 10 | |
| | 5.43' |
| 9 | |
| | 4.86' |
| 8 | |
| | 4.29' |
| * 7 | |
| | 3.72' |
| 6 | |
| | 3.15' |
| 5 | |
| | 2.58' |
| * 4 | |
| | 2.01' |
| 3 | |
| | 1.34' |
| 2 | |
| | 0.67' |
| * 1 | |
| | 0.00' |

* GRID NODE

FIGURE A-50. FRAP-T6 TIME STEP STUDY - HOT CHANNEL CLAD TEMPERATURE AT RUPTURED LOCATION.

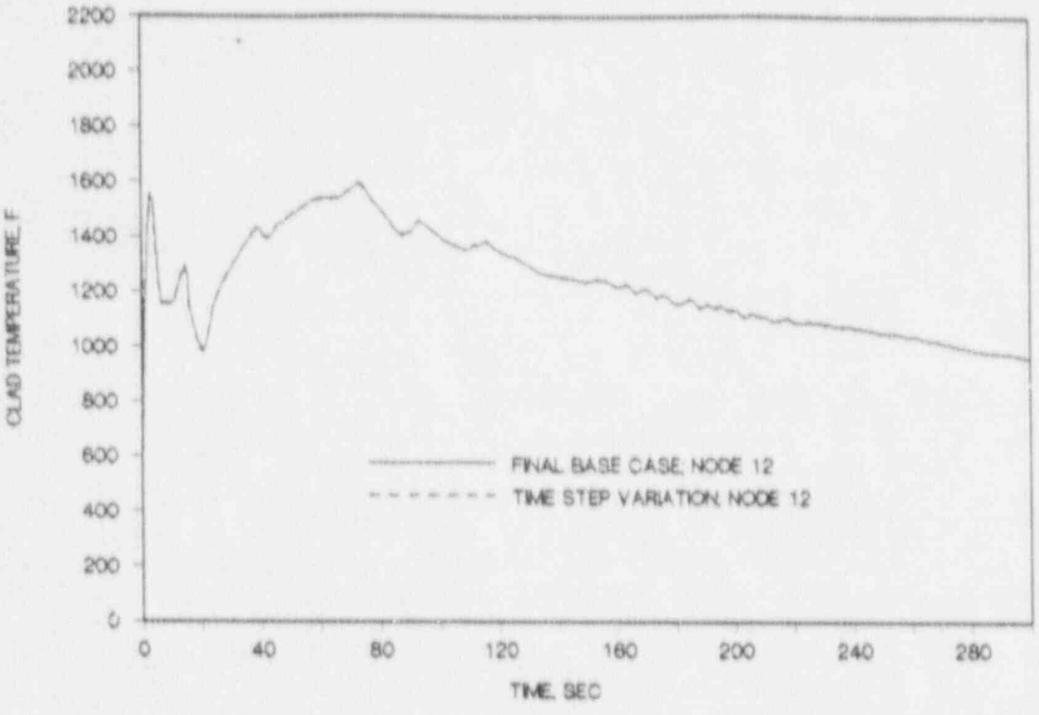



FIGURE A-51. FRAP-T6 TIME STEP STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK CLAD TEMPERATURE LOCATION.

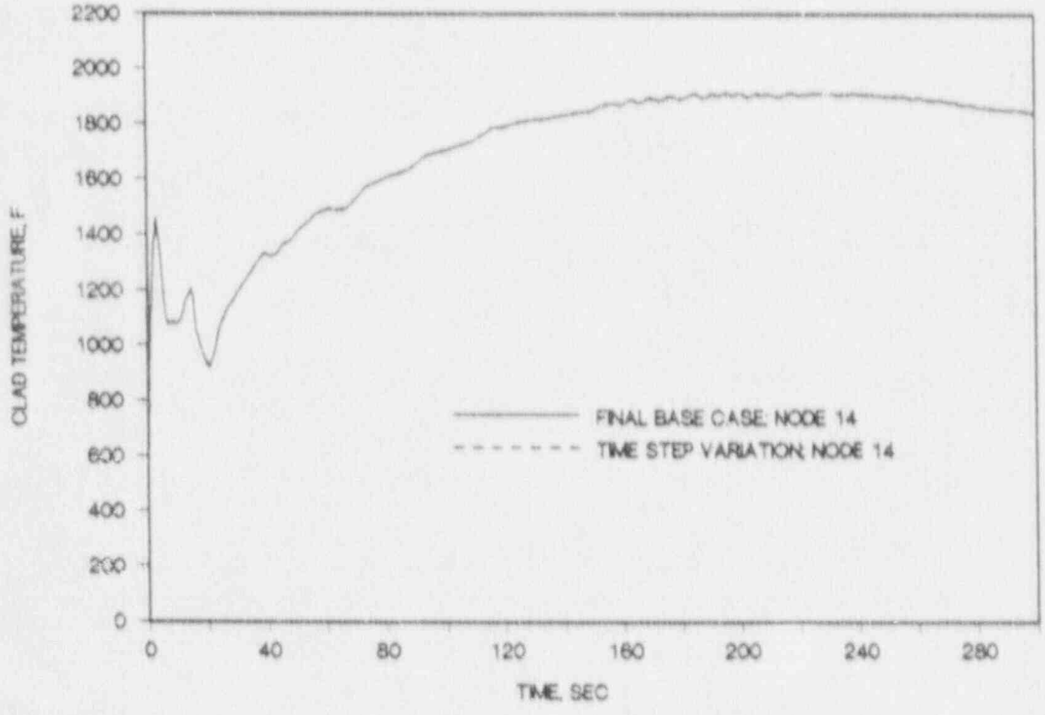


FIGURE A-52. FRAP-T6 RADIAL FUEL SEGMENTATION STUDY - HOT CHANNEL AVERAGE FUEL TEMPERATURE AT RUPTURED LOCATION.

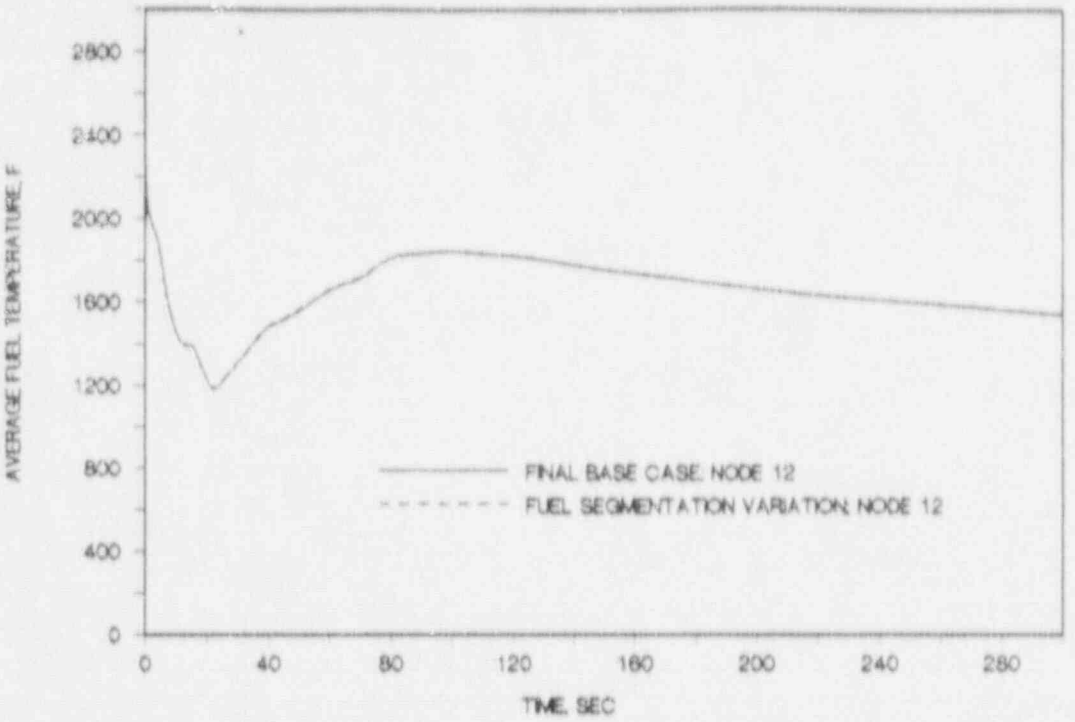



FIGURE A-53. FRAP-T6 RADIAL FUEL SEGMENTATION STUDY - HOT CHANNEL AVERAGE FUEL TEMPERATURE AT PEAK CLAD TEMPERATURE LOCATION.

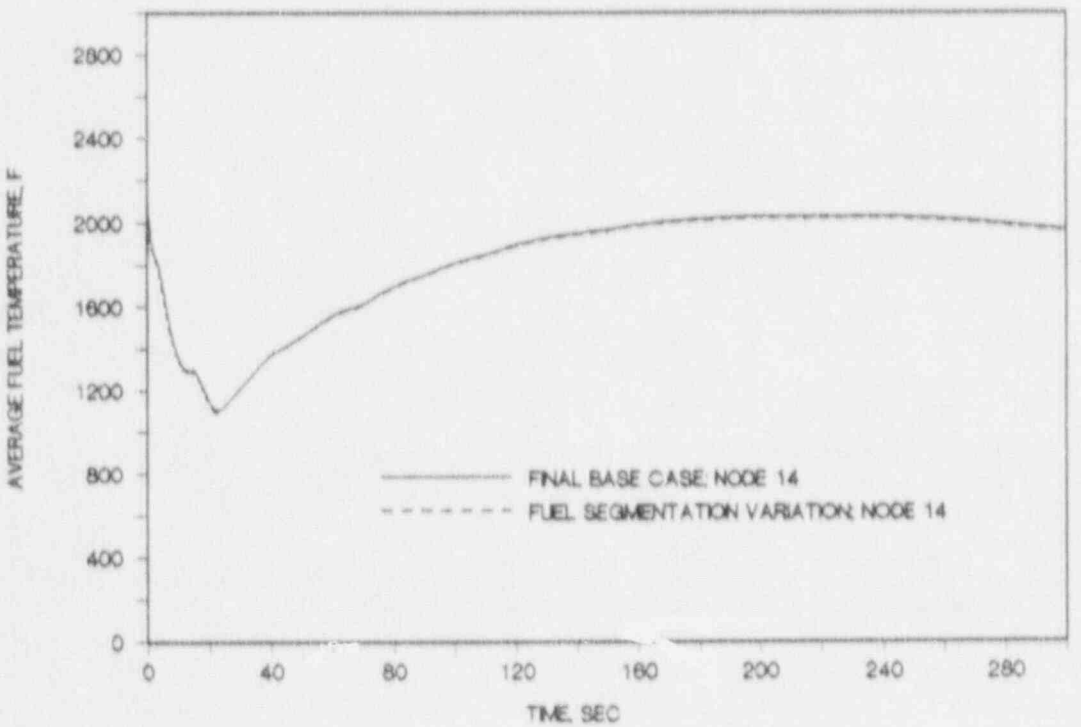


FIGURE A-54. FRAP-T6 RADIAL FUEL SEGMENTATION STUDY - HOT CHANNEL CLAD TEMPERATURE AT RUPTURED LOCATION.

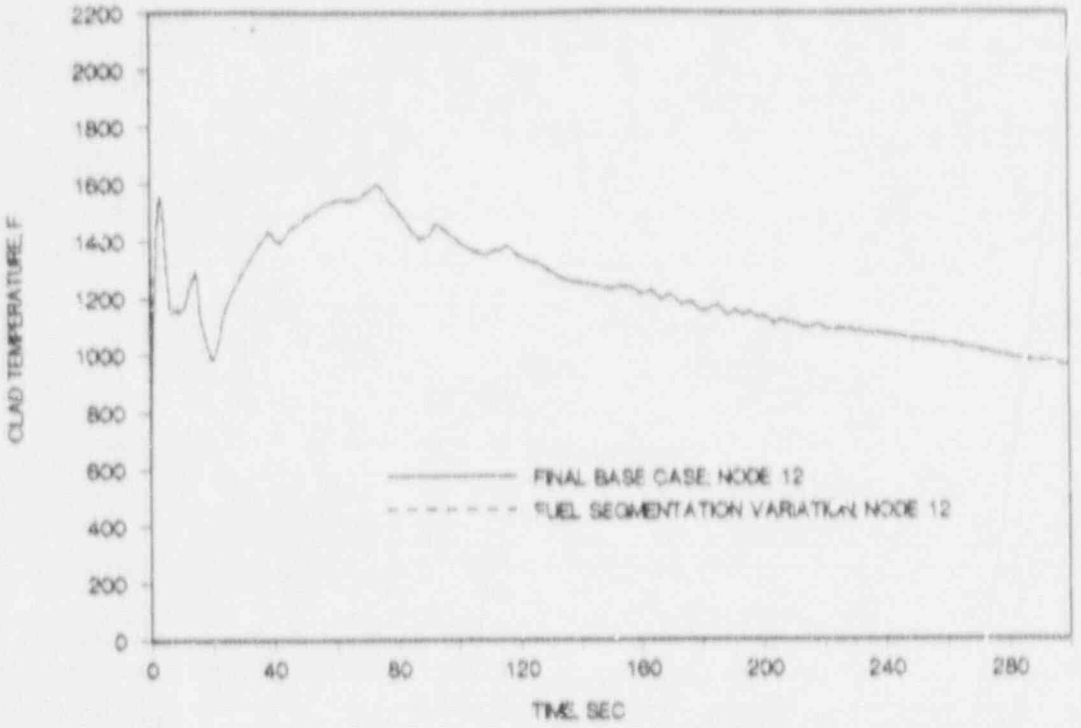



FIGURE A-55. FRAP-T6 RADIAL FUEL SEGMENTATION STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK CLAD TEMPERATURE LOCATION.

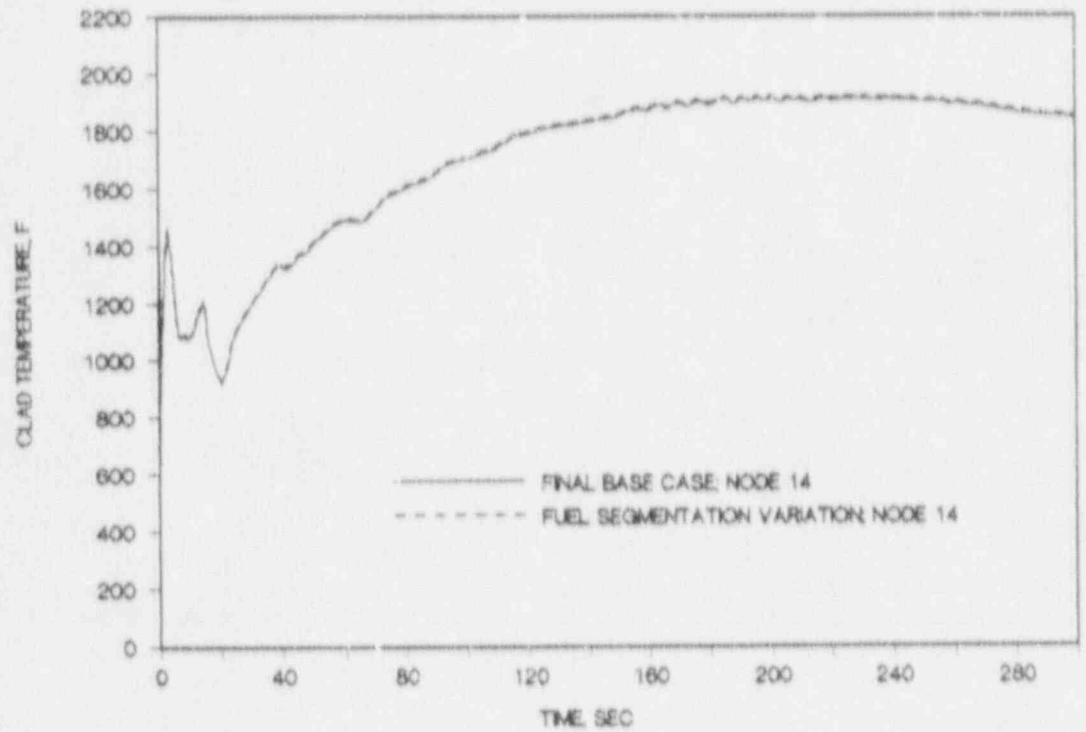


FIGURE A-56. SPECTRUM STUDY - LEAK FLOW AT RV SIDE

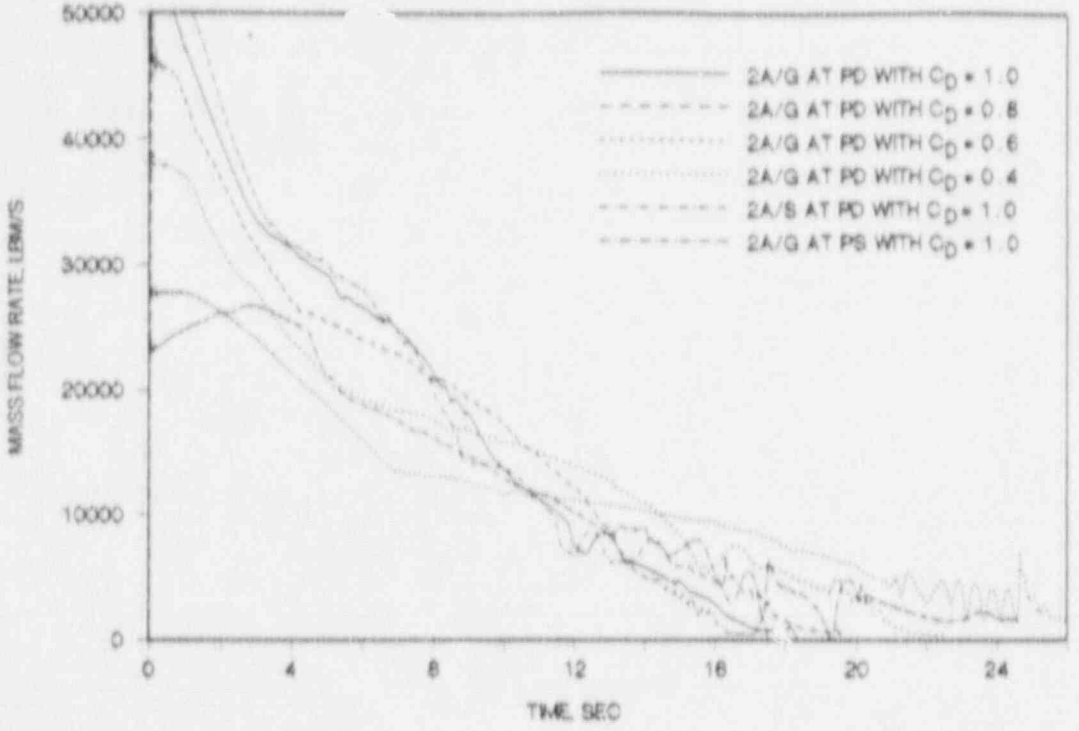



FIGURE A-57. SPECTRUM STUDY - LEAK FLOW AT RSG SIDE

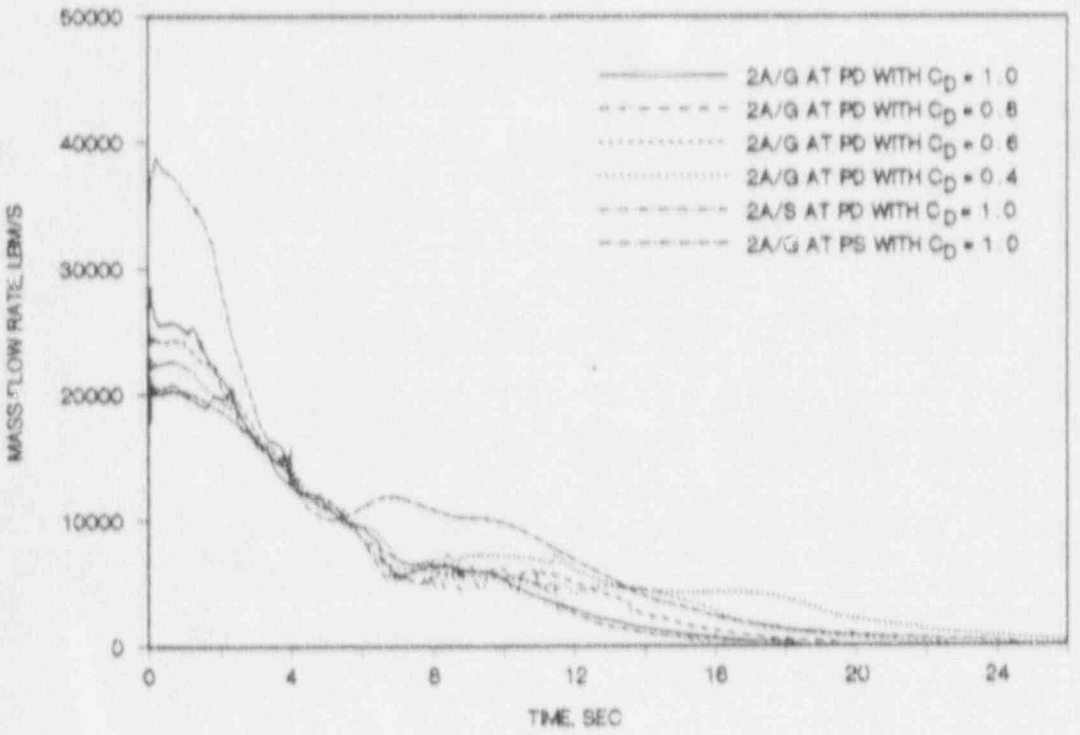


FIGURE A-58. SPECTRUM STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

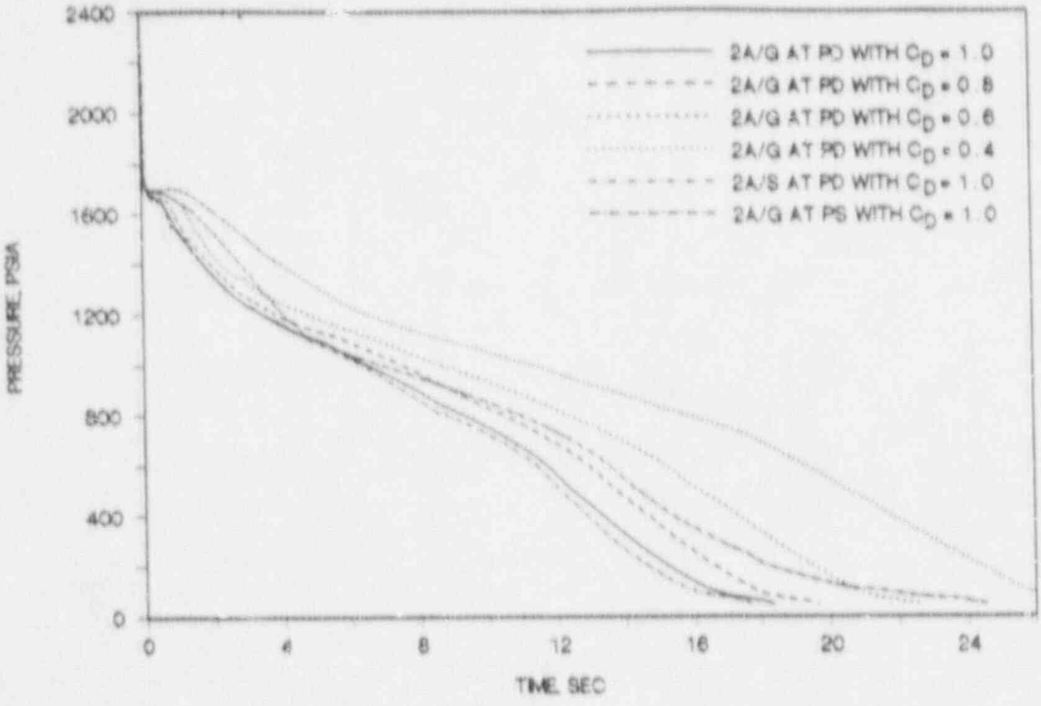



FIGURE A-59. SPECTRUM STUDY - HOT CHANNEL FLOW AT RUPTURED LOCATION.

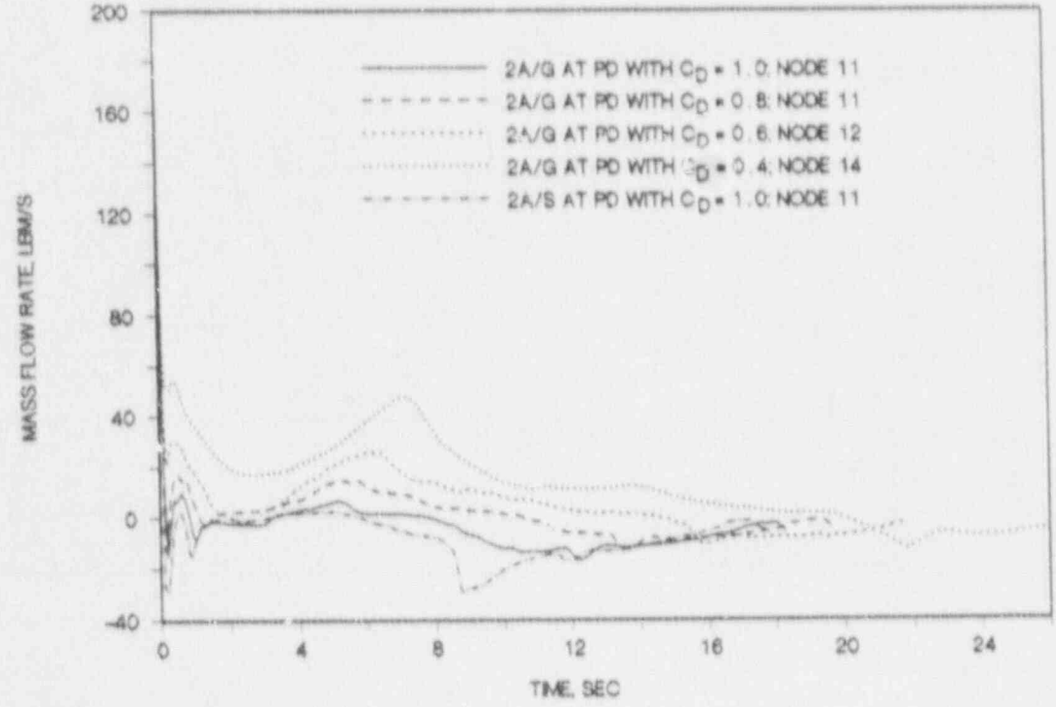


FIGURE A-60. SPECTRUM STUDY - HOT CHANNEL FLOW AT PEAK CLAD TEMPERATURE LOCATION.

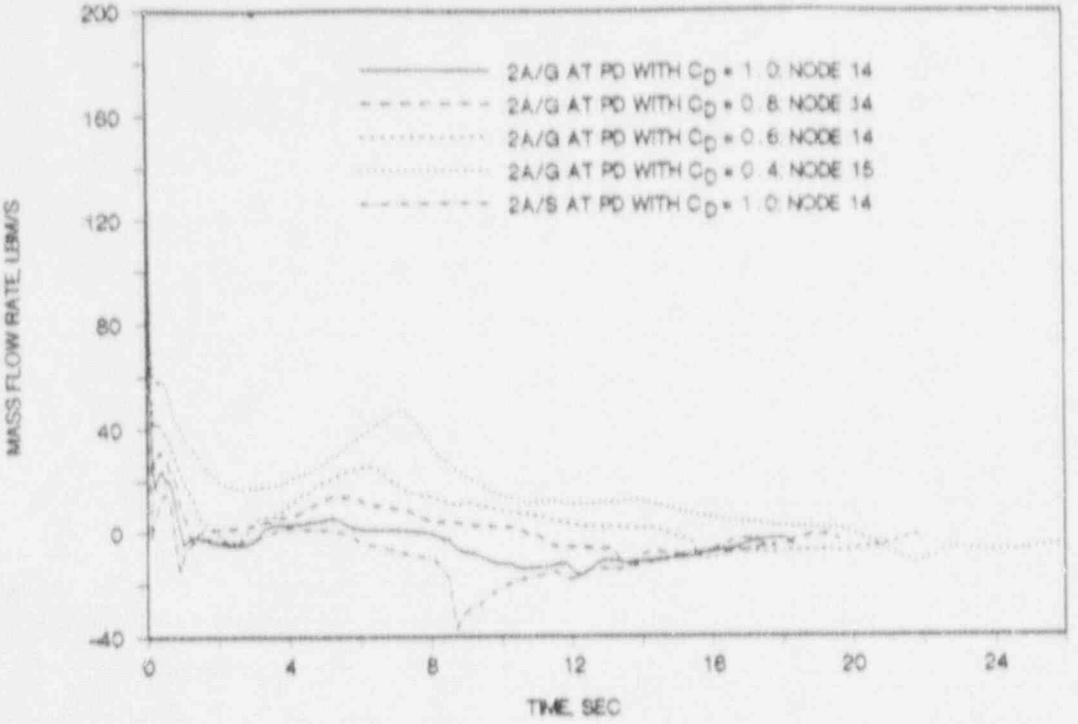



FIGURE A-61. SPECTRUM STUDY - HOT CHANNEL ENTHALPY AT RUPTURED LOCATION.

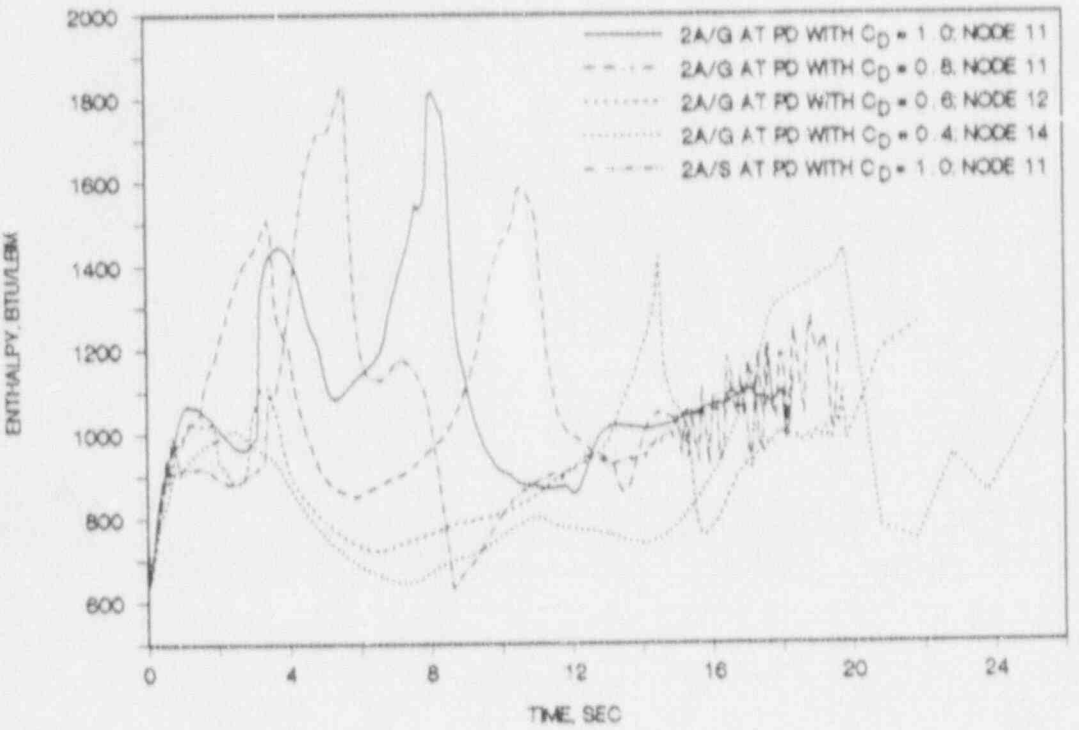


FIGURE A-62. SPECTRUM STUDY - HOT CHANNEL ENTHALPY AT PEAK CLAD TEMPERATURE LOCATION.

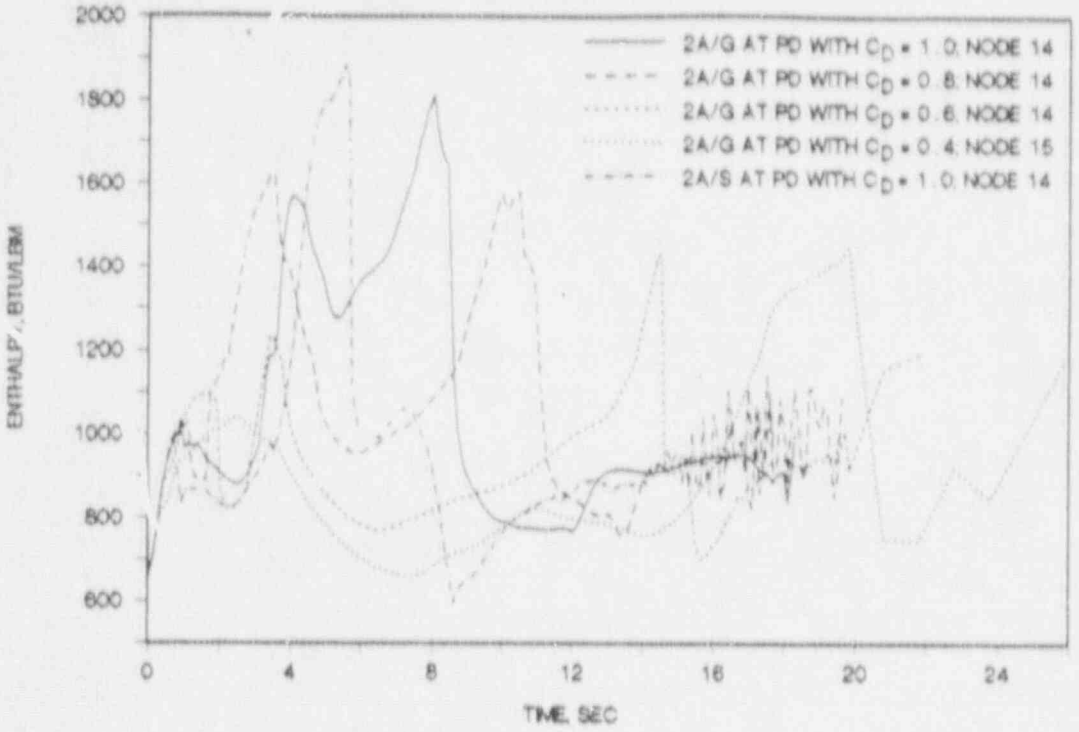



FIGURE A-63. SPECTRUM STUDY - CORE FLOODING RATE.

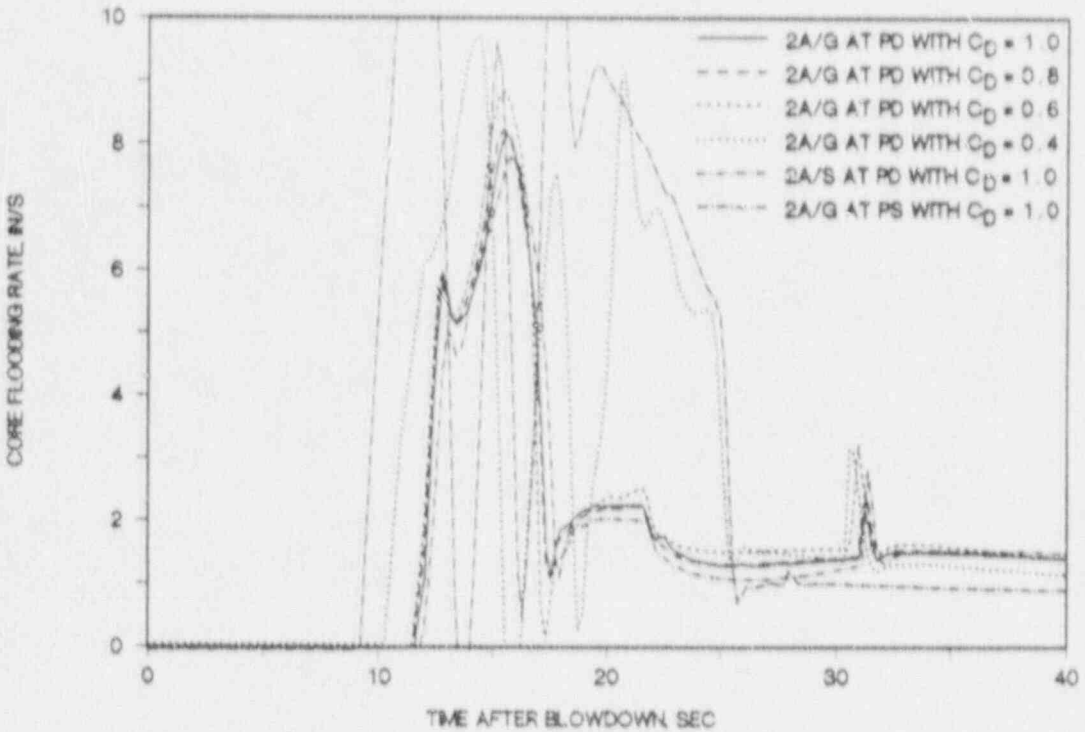


FIGURE A-64. SPECTRUM STUDY - CORE FLOODING RATE.

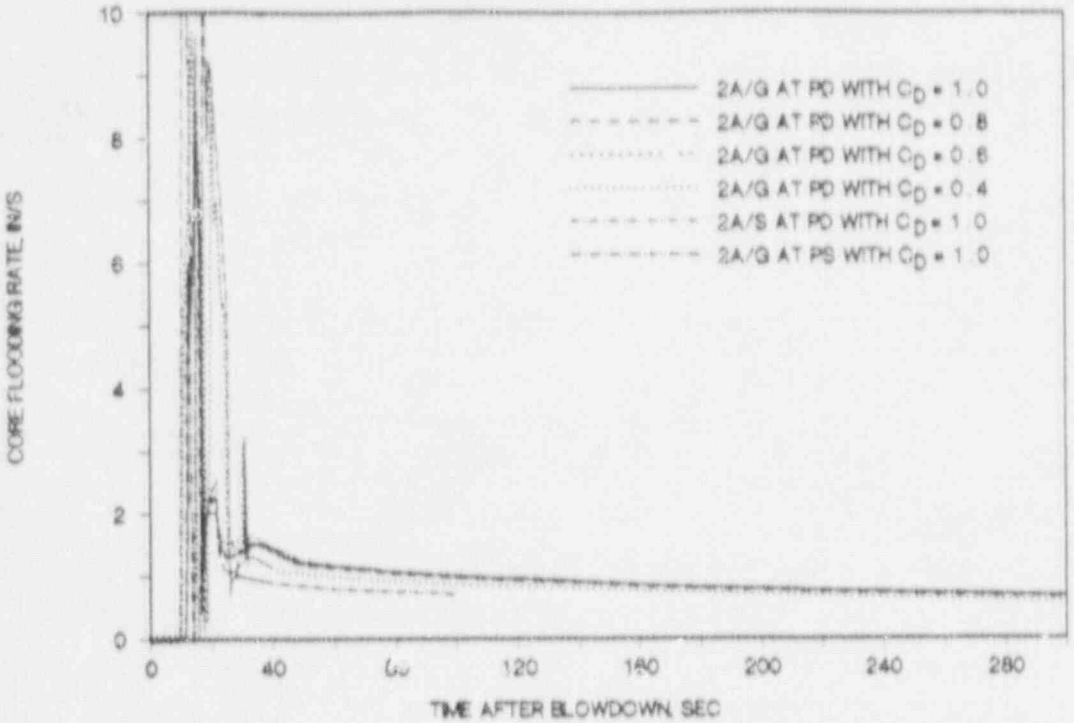



FIGURE A-65. SPECTRUM STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS; 2A/G AT PD WITH $C_D$ = 1.0.

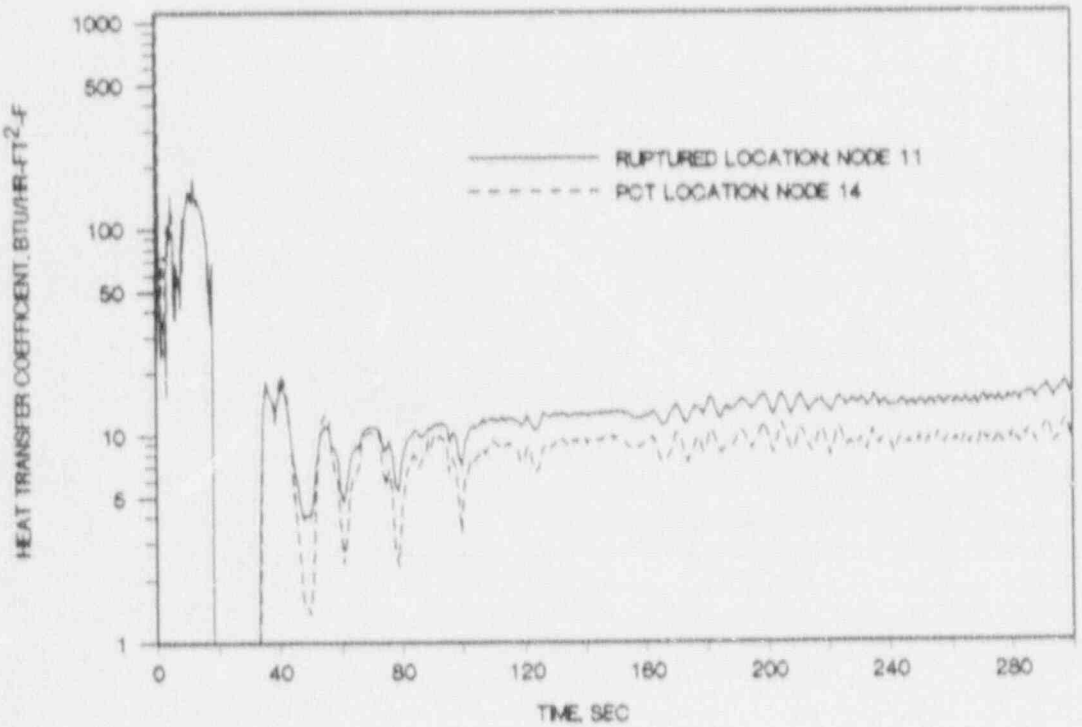


FIGURE A-66. SPECTRUM STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS: 2A/G AT PD WITH $C_D = 0.8$.

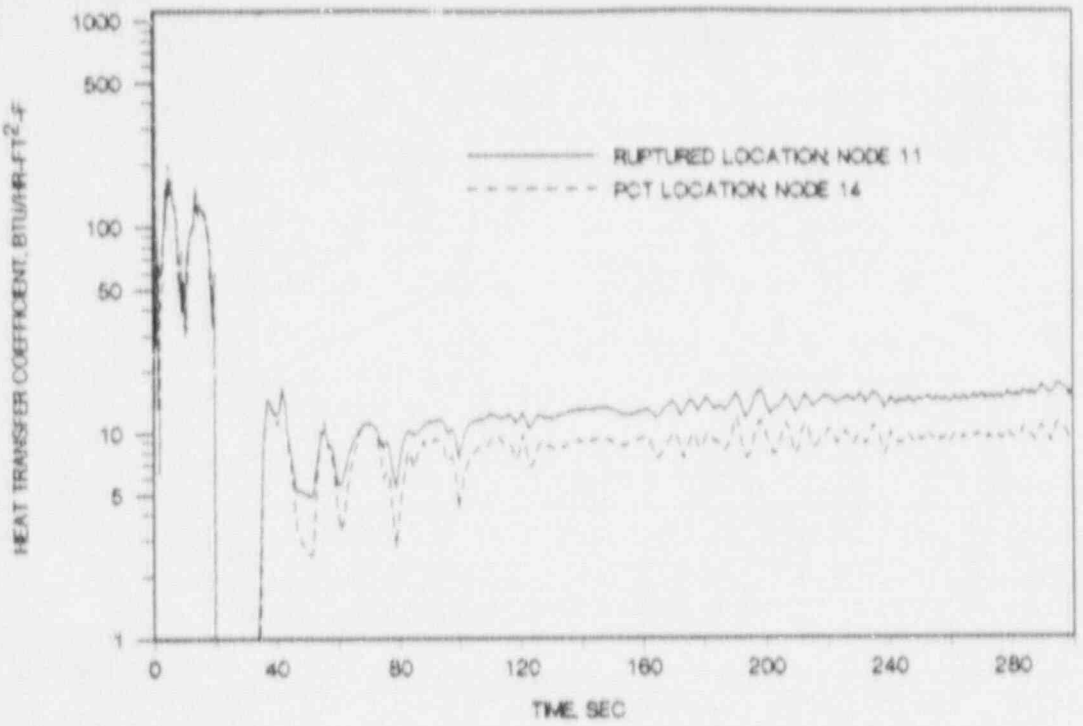



FIGURE A-67. SPECTRUM STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS: 2A/G AT PD WITH $C_D = 0.6$.

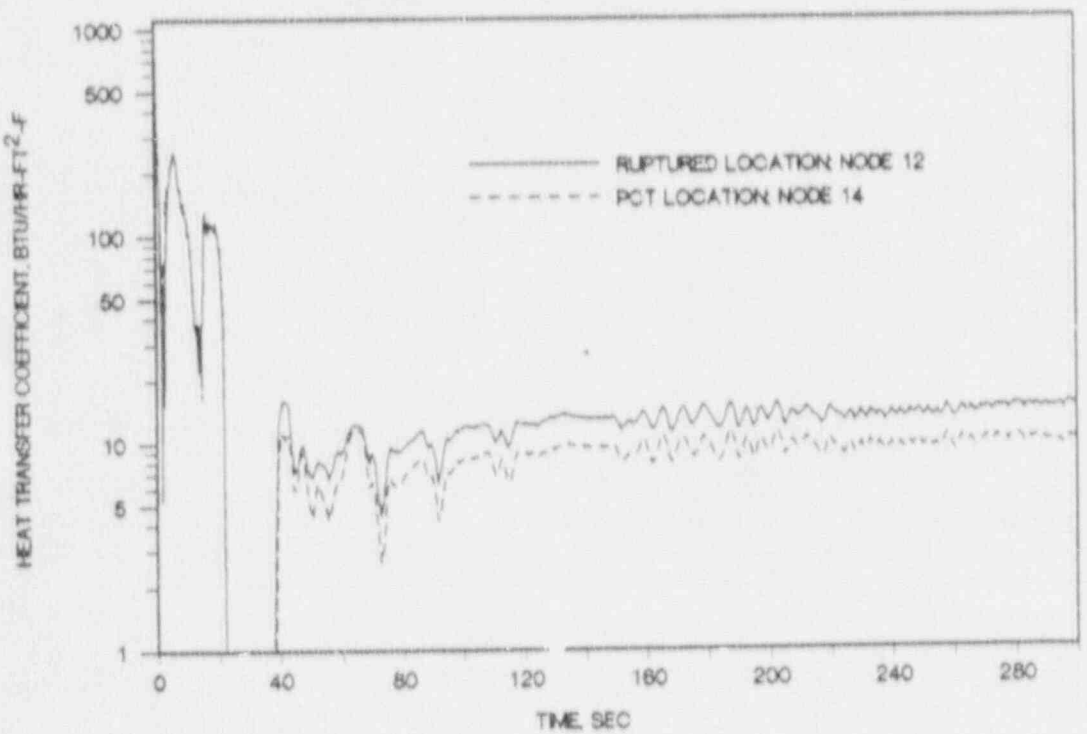


FIGURE A-68. SPECTRUM STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS; 2A/G AT PD WITH $C_D$ = 0.4.

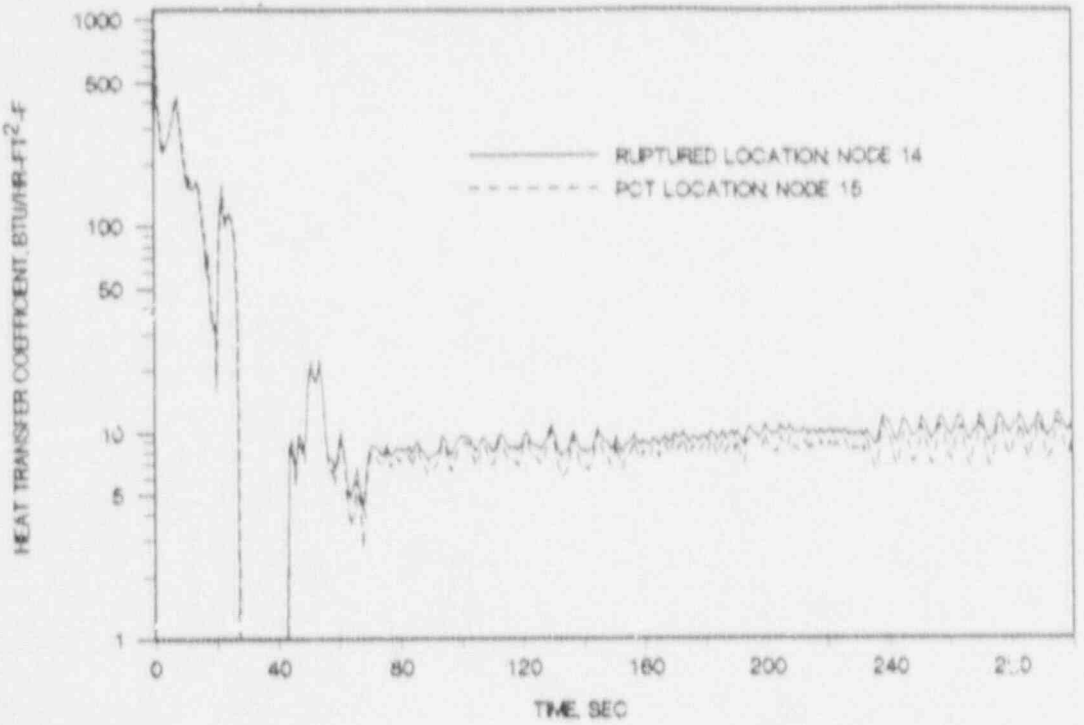



FIGURE A-69. SPECTRUM STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS; 2A/S AT PD WITH $C_D$ = 1.0.

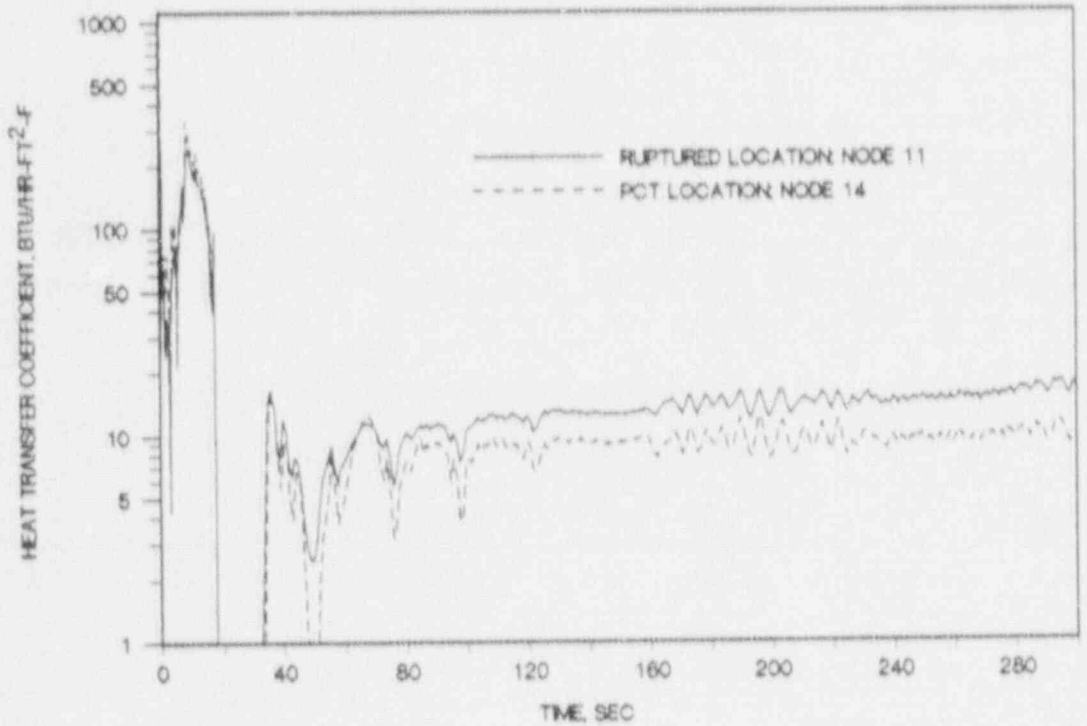


FIGURE A-70. SPECTRUM STUDY - HOT CHANNEL CLAD TEMPERATURE AT RUPTURED LOCATION.

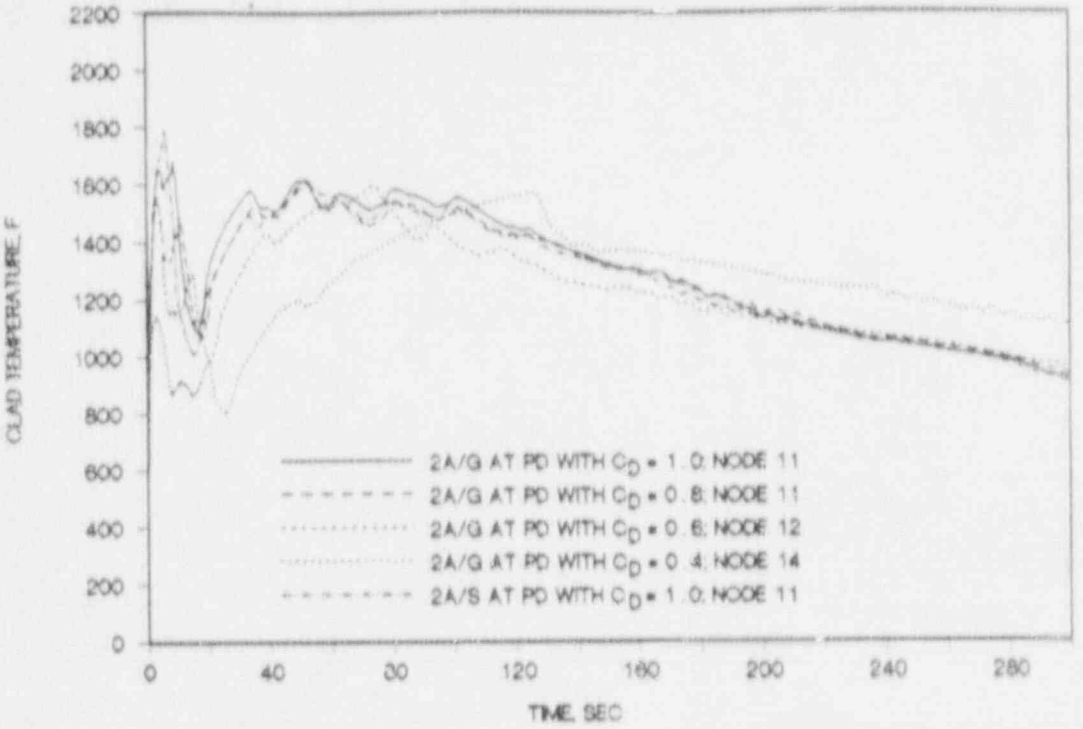



FIGURE A-71. SPECTRUM STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK CLAD TEMPERATURE LOCATION.

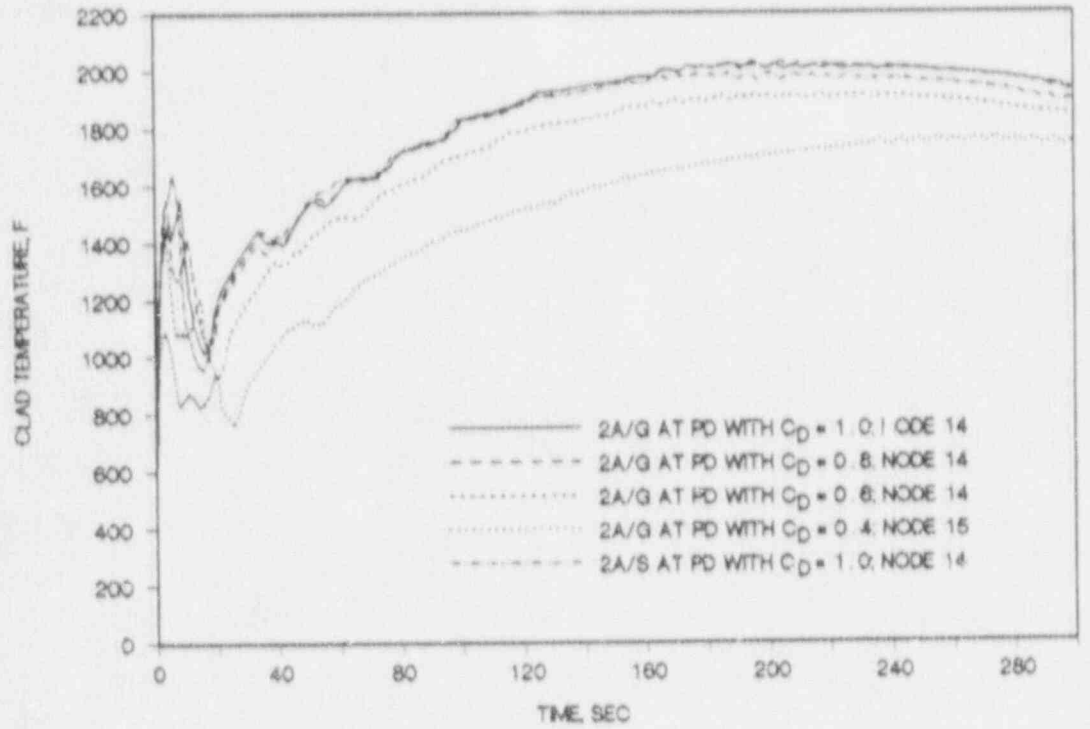


FIGURE A-72. TIME-IN-LIFE STUDY - LEAK FLOW AT RV SIDE

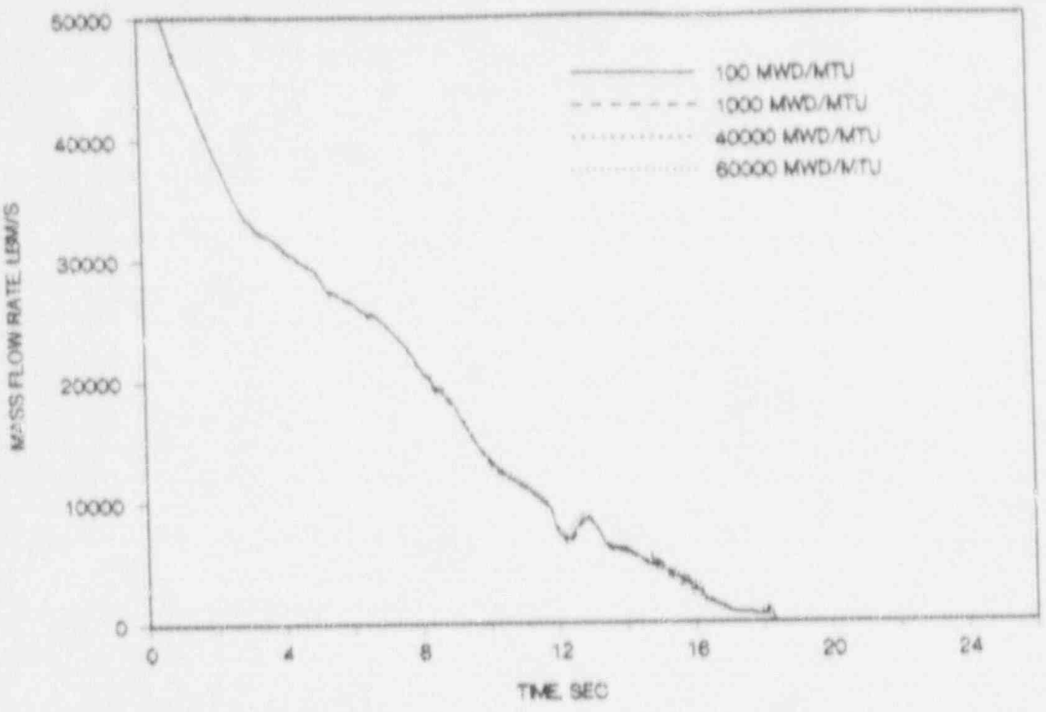



FIGURE A-73. TIME-IN-LIFE STUDY - LEAK FLOW AT PUMP SIDE

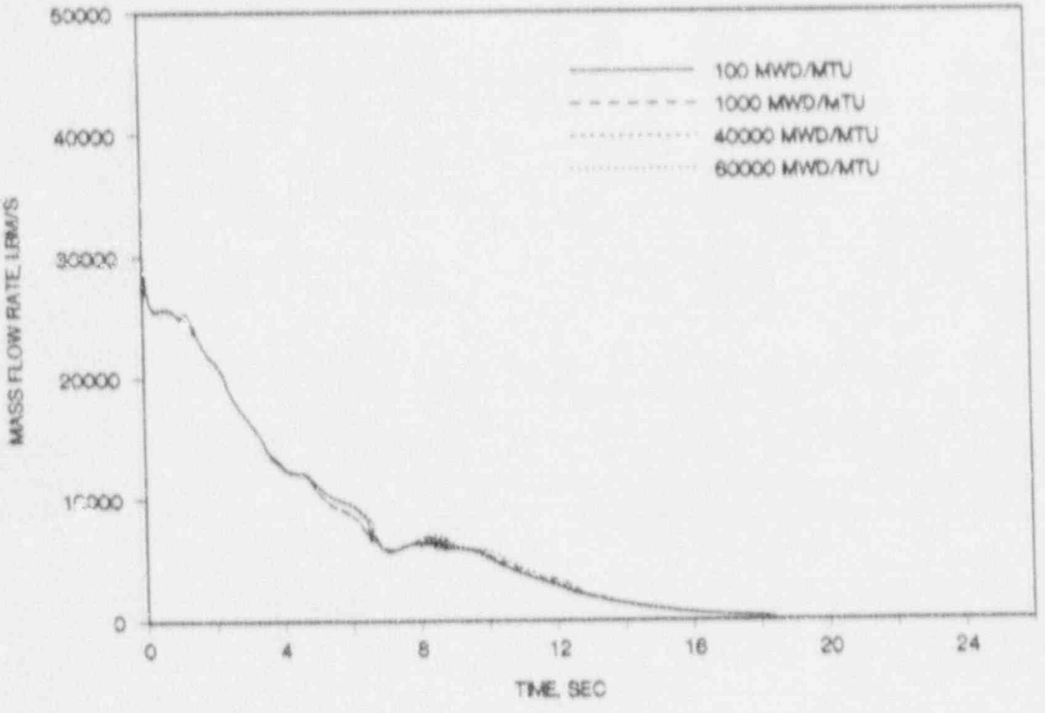


FIGURE A-74. TIME-IN-LIFE STUDY - SYSTEM PRESSURE IN THE UPPER PLENUM.

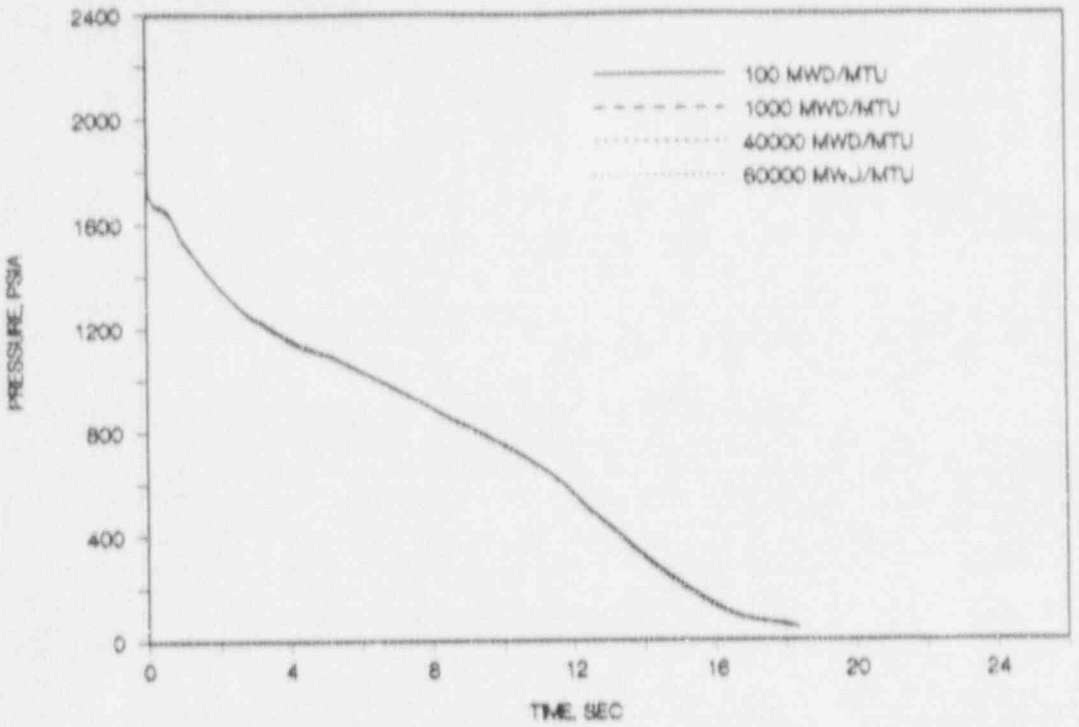



FIGURE A-75. TIME-IN-LIFE STUDY - HOT CHANNEL FLOW AT RUPTURED LOCATION (NODE 11).

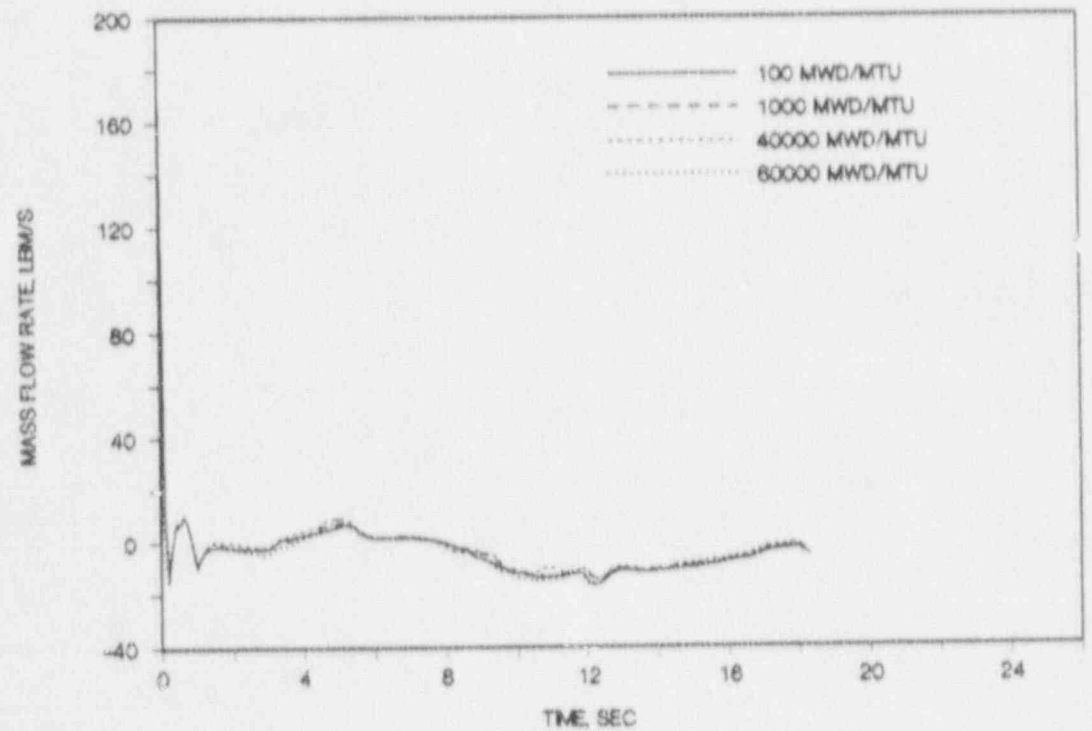


FIGURE A-76. TIME-IN-LIFE STUDY - HOT CHANNEL FLOW AT PEAK CLAD TEMPERATURE LOCATION (NODE 14).

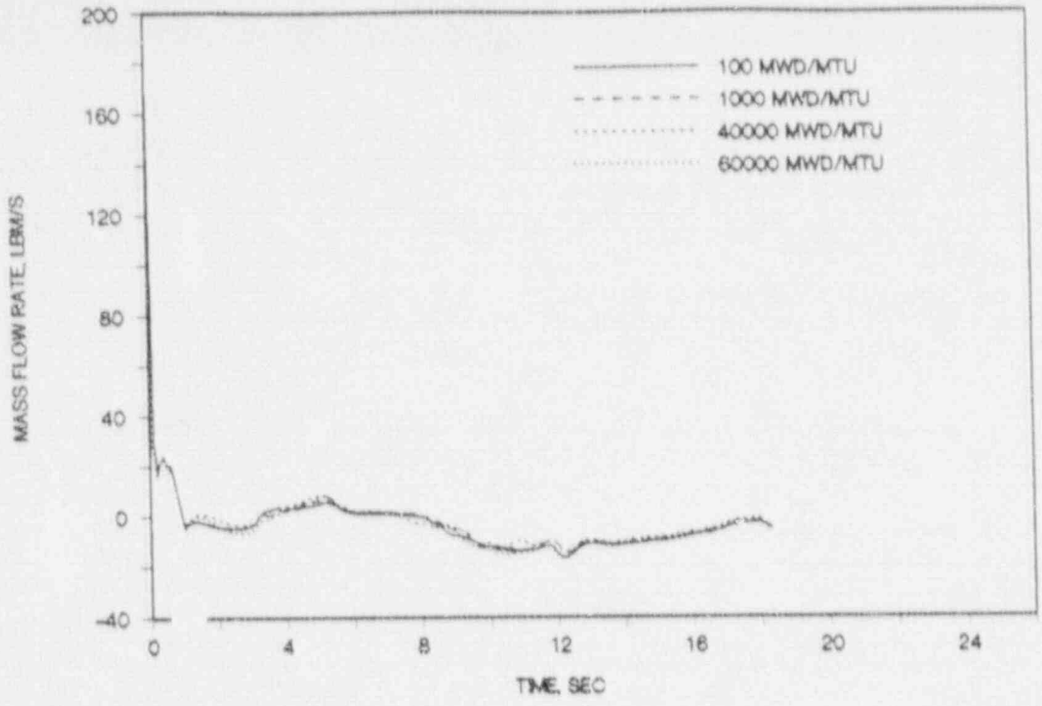



FIGURE A-77. TIME-IN-LIFE STUDY - CORE FLOODING RATE.

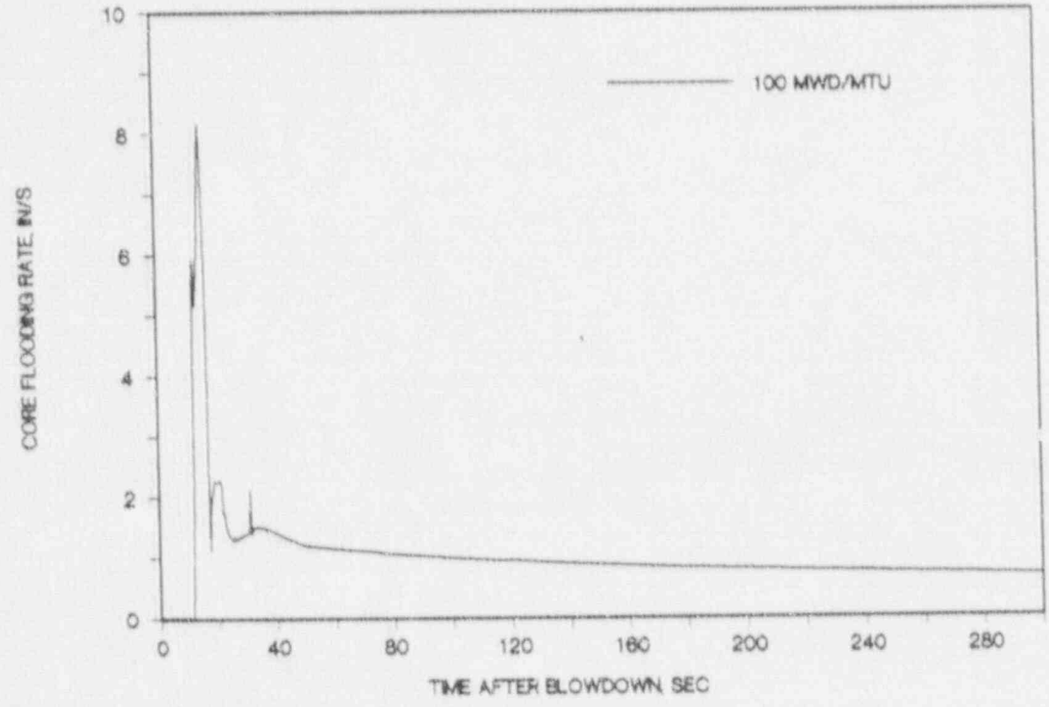


FIGURE A-78. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS FOR 100 MWD/MTU.

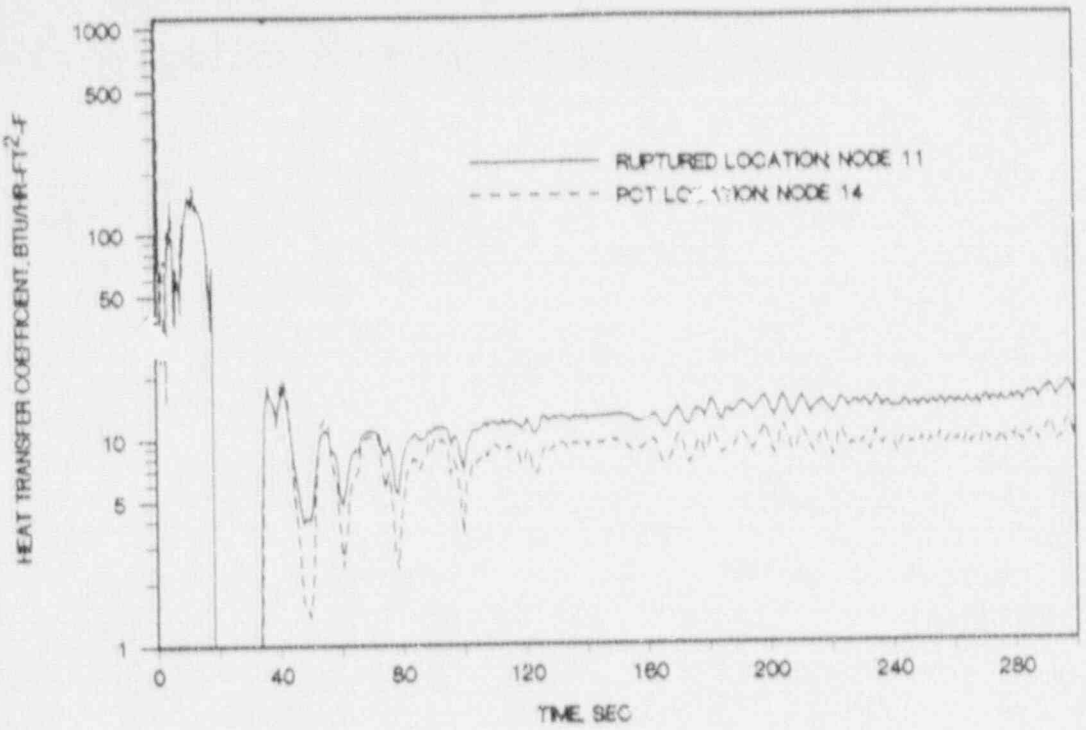



FIGURE A-79. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS FOR 1000 MWD/MTU.

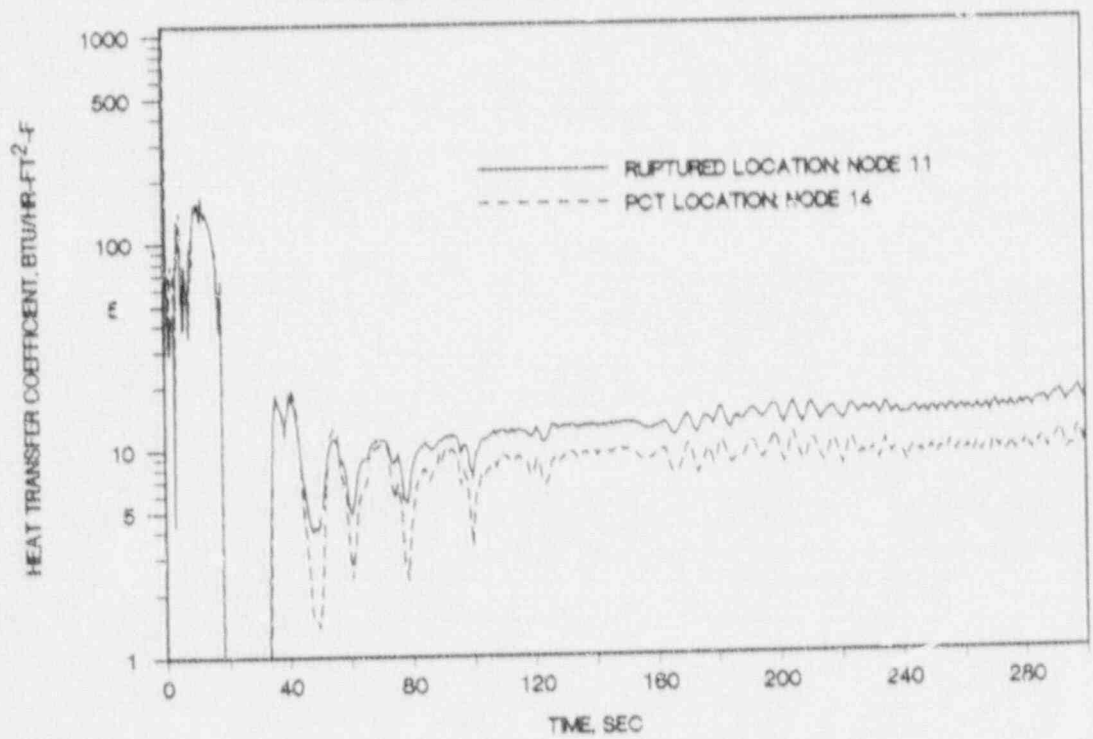


FIGURE A-80. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS FOR 40000 MWD/MTU.

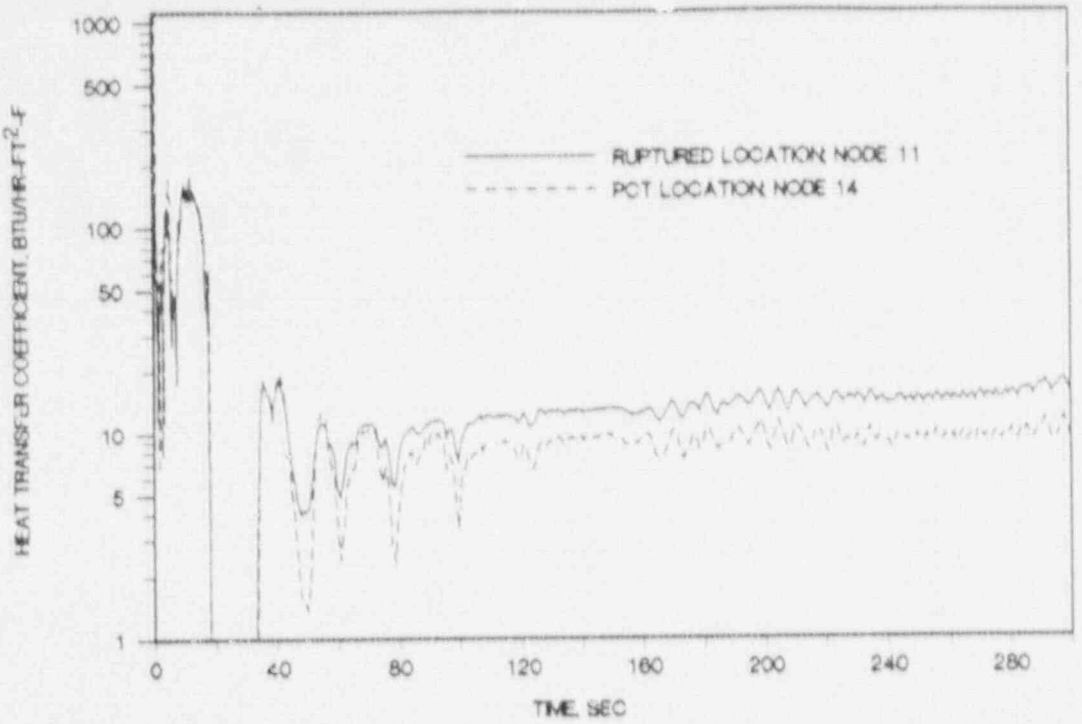



FIGURE A-81. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS FOR 60000 MWD/MTU.

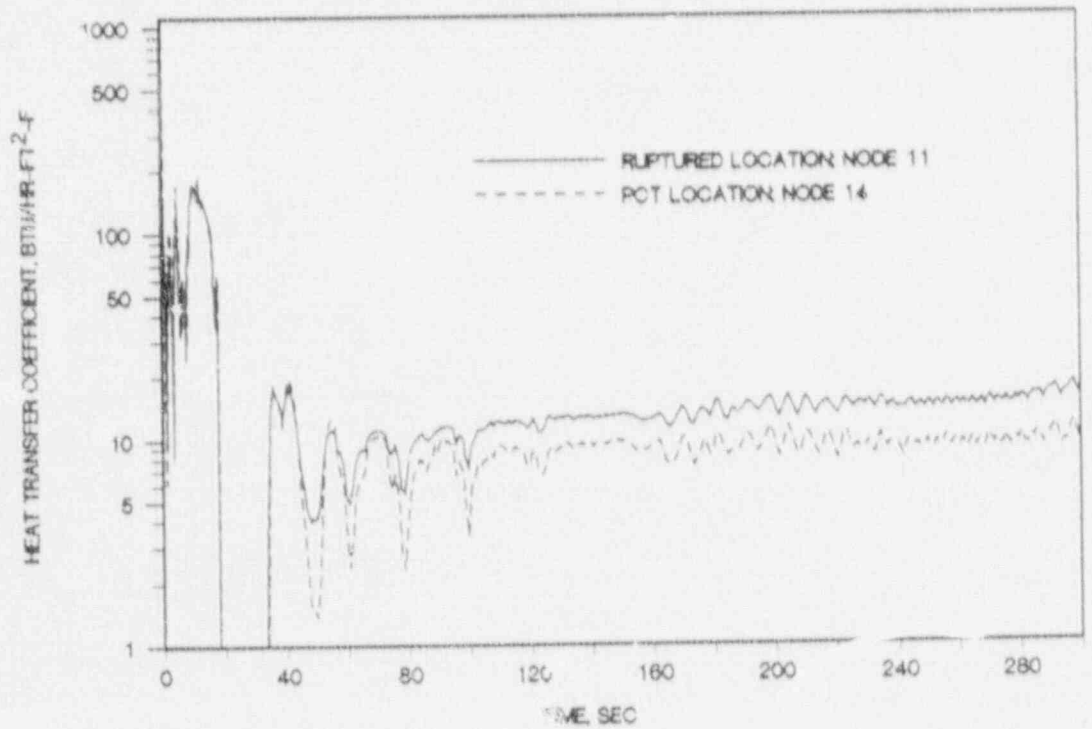


FIGURE A-82. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD TEMPERATURE AT RUPTURED LOCATION (NODE 11).

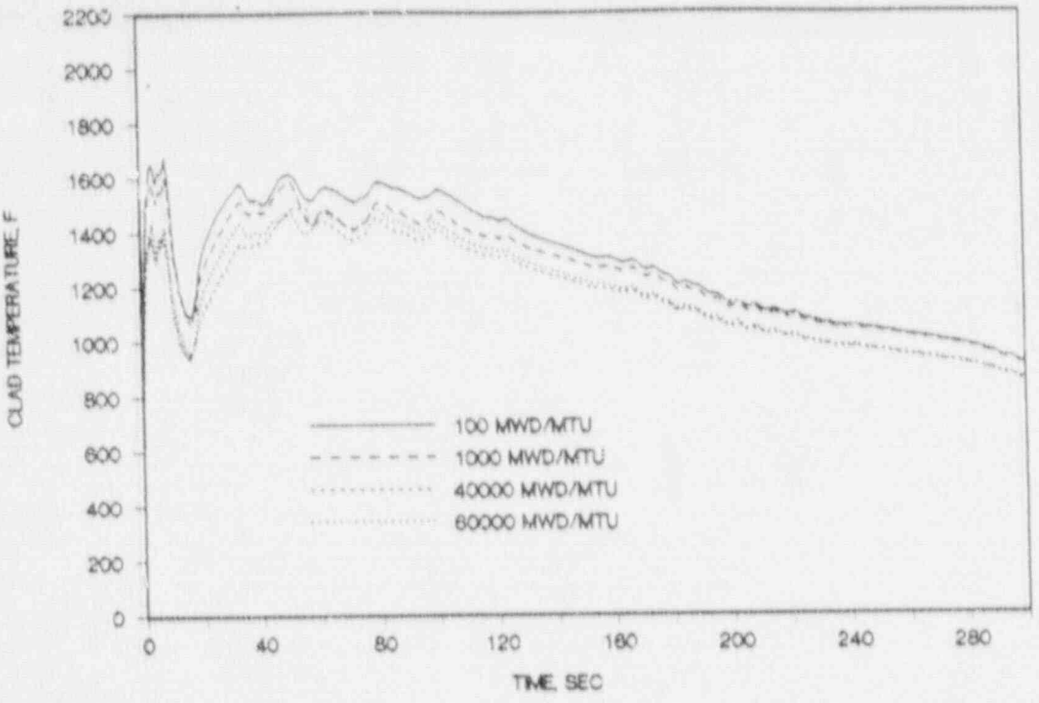



FIGURE A-83. TIME-IN-LIFE STUDY - HOT CHANNEL CLAD TEMPERATURE AT PEAK CLAD TEMPERATURE LOCATION (NODE 14).

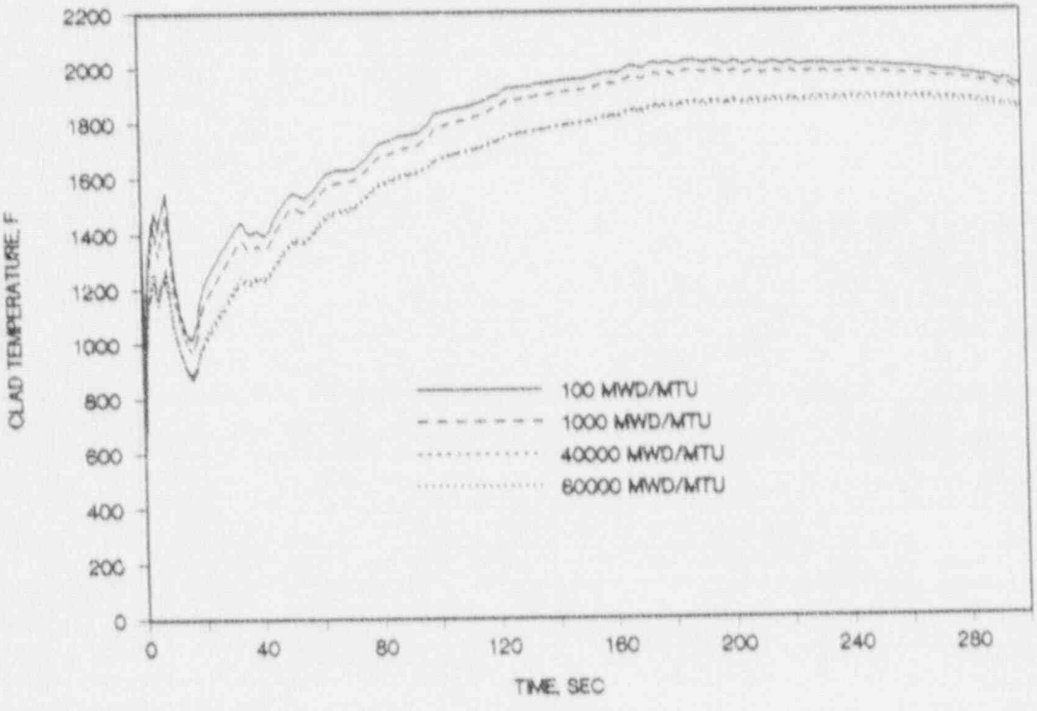


FIGURE A-84. MOST SEVERE BREAK CASE - LEAK FLOWS.

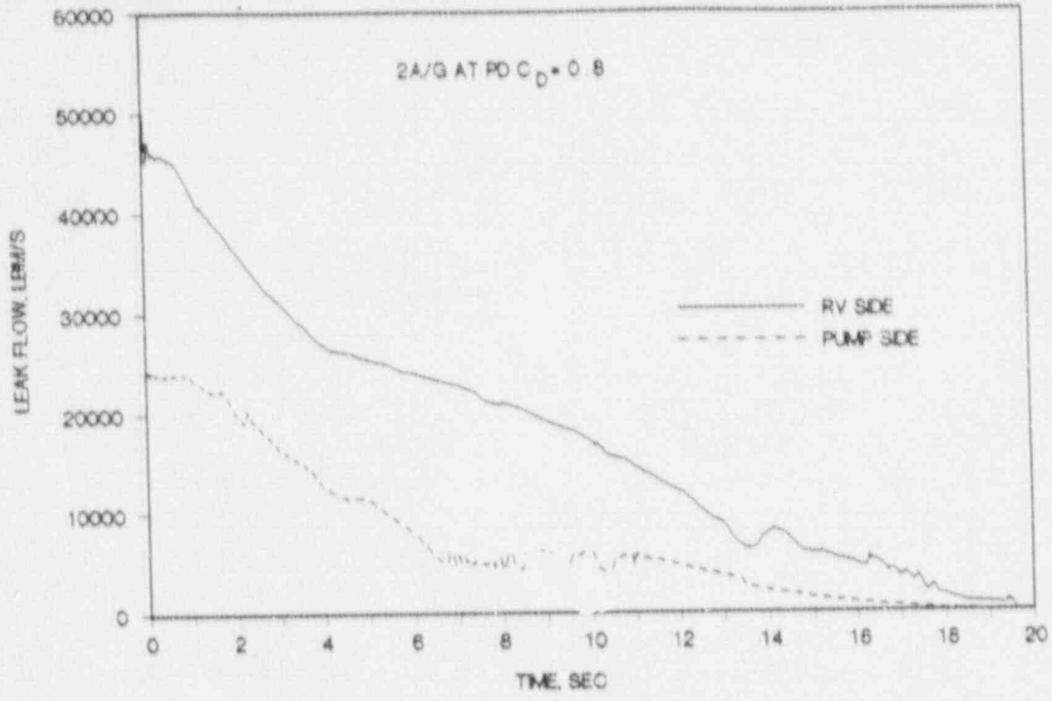



FIGURE A-85. MOST SEVERE BREAK CASE - SYSTEM PRESSURE IN THE UPPER PLENUM.

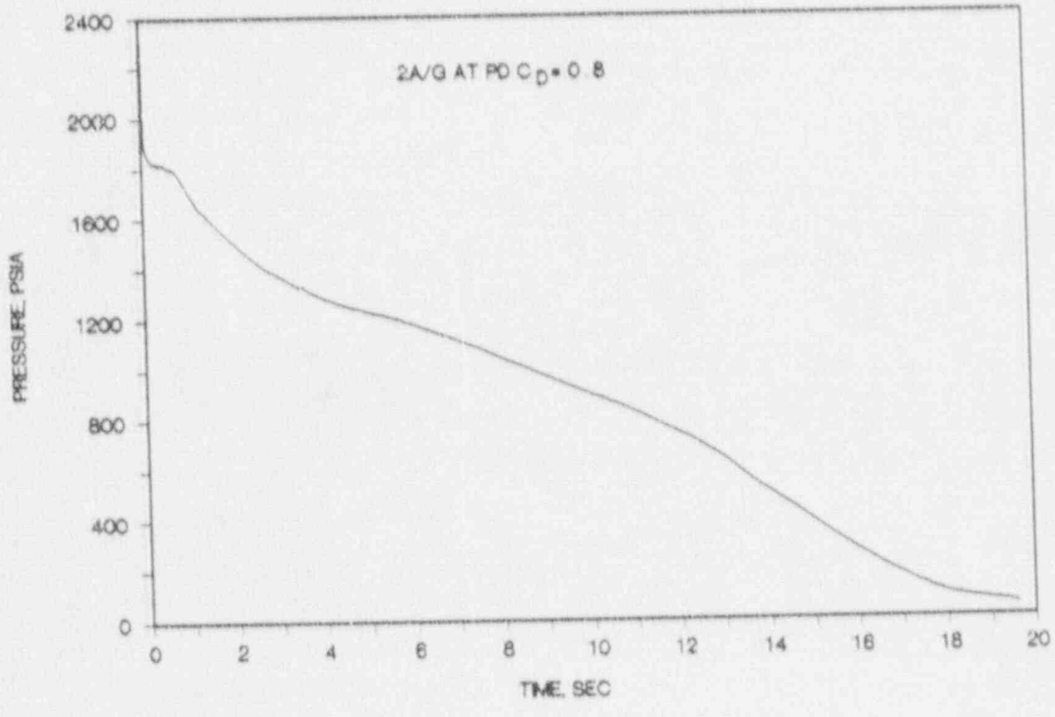


FIGURE A-86. MOST SEVERE BREAK CASE - HOT CHANNEL FLOWS AT RUPTURED AND PCT LOCATIONS.

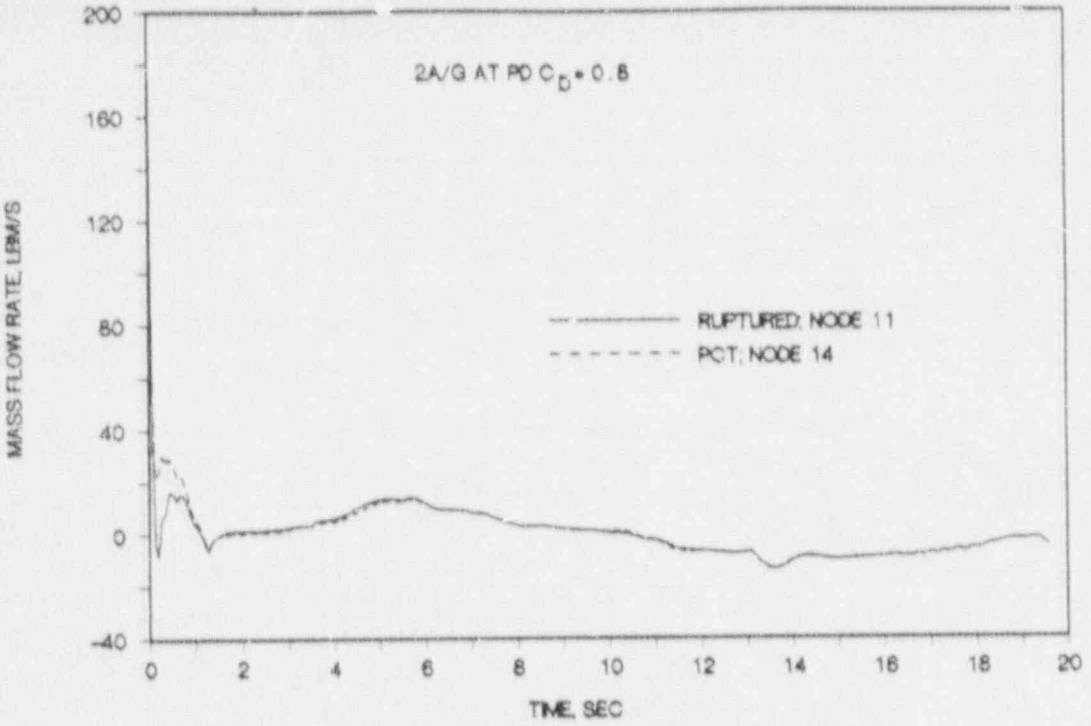



FIGURE A-87. MOST SEVERE BREAK CASE - HOT CHANNEL ENTHALPIES AT RUPTURED AND PCT LOCATIONS.

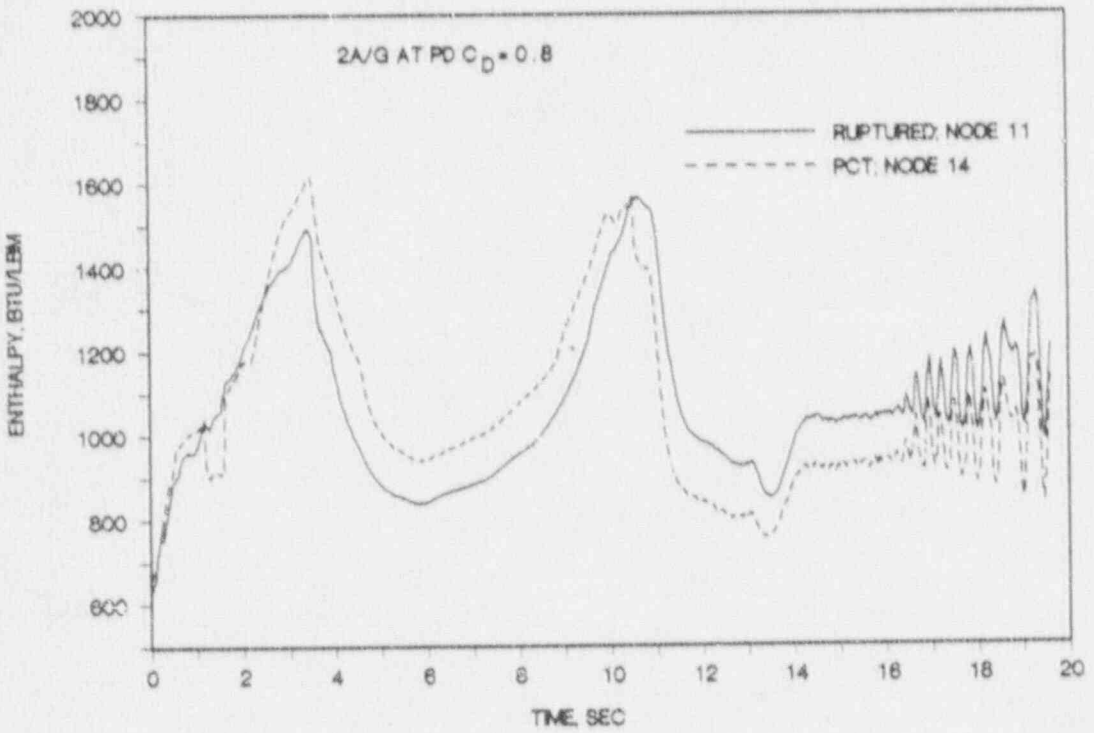


FIGURE A-88. MOST SEVERE BREAK CASE - CORE FLOODING RATE.

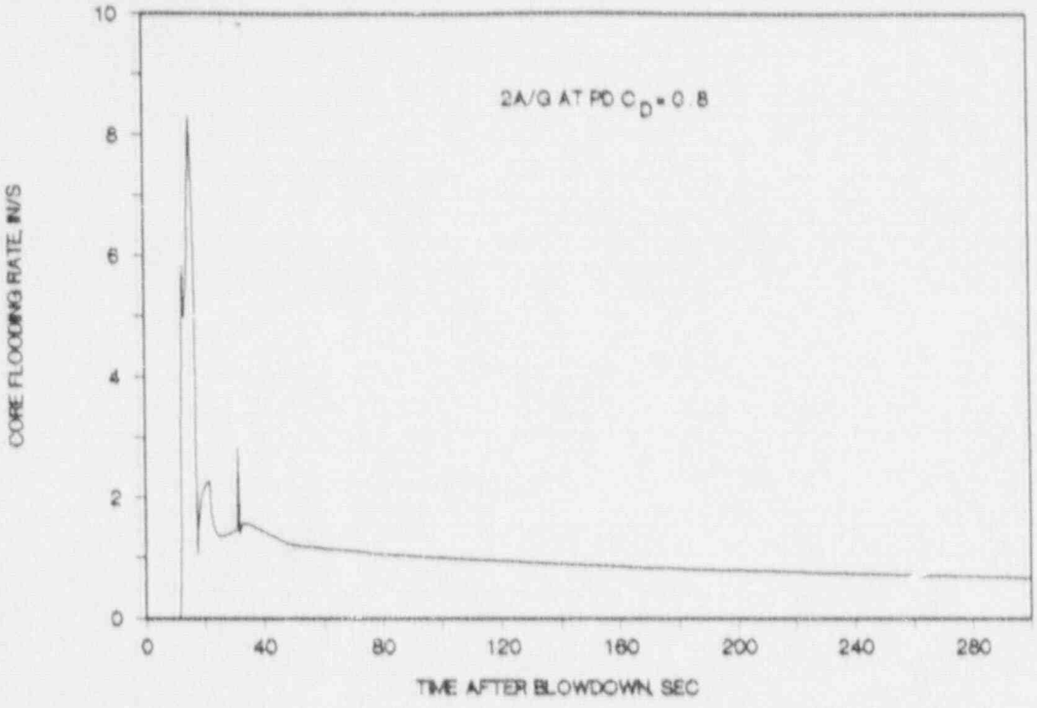



FIGURE A-89. MOST SEVERE BREAK CASE - HOT CHANNEL CLAD SURFACE HEAT TRANSFER COEFFICIENTS AT RUPTURED AND PCT LOCATIONS.

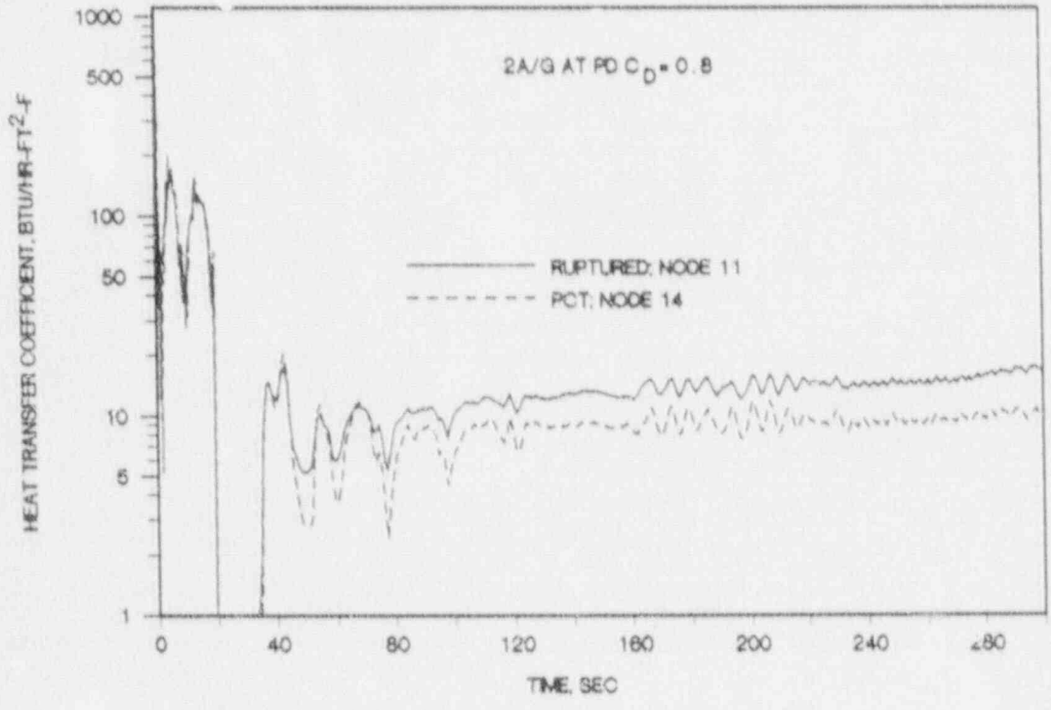
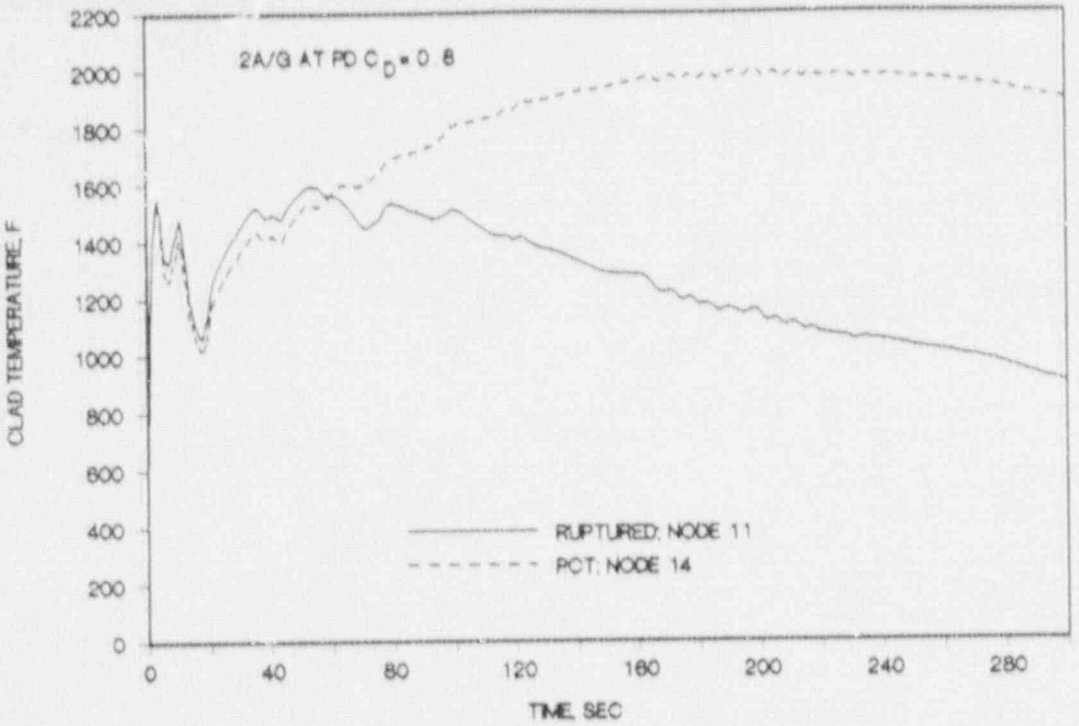


FIGURE A-90. MOST SEVERE BREAK CASE - HOT CHANNEL CLAD TEMPERATURES AT RUPTURED AND PCT LOCATIONS.



# APPENDIX B
## Assessment of Evaluation Model Revision 1 Changes

Note: This appendix was originally added in its entirety in Revision 1 of BAW-10168, September 1989.

## B.1. Introduction

The original version of the BWFC large break LOCA evaluation model was submitted to the NRC in July 1988. Following that submittal, it became evident that the original evaluation model code package produced excessive conservatism in calculating certain aspects of the reflooding heat transfer processes. Consequently, improved methods were developed. The changes incorporated alterations to the BEACH code to: refine the spacer grid effects model, add rupture-blockage effects, include cladding metal-water reaction, and actively calculate clad swelling and rupture. The details of these modifications are given in Revision 2 to the BEACH topical report BAW-10166. These modifications allow BEACH to be used alone during the reflooding period to calculate peak cladding temperatures. The analyses presented and discussed in Appendix A of this report were performed previous to the modeling revisions. The remainder of this appendix delineates the differences in the methods and discusses their effects on calculated results.

## B.2. Revisions to the BWFC LOCA Evaluation Model

The model changes discussed in this appendix affect only large break modeling. Code interrelationships for the original model are shown in Figure B-1. RELAP5/MOD2-B&W is used to calculate system thermal-hydraulics and core power generation during blowdown. REFLOD3B determines the refill time and the core reflooding hydraulics for the remainder of the transient. The BEACH code is used to determine cladding surface heat transfer coefficients (HTC) using core reflooding hydraulics from REFLOD3B as input. FRAP-T6-B&W completes the analysis by using RELAP5 and BEACH inputs to evaluate the hot rod cladding temperature for the entire transient.

Modifications to BEACH

The BEACH code version represented in the original evaluation model was found to contain two limitations. First, the fine drop vaporization model employed to predict the shattering of entrained liquid into small droplets at the spacer grids (and the controlling input parameter for that model) was inaccurate--and overly conservative--in terms of sensitivity to the grid blockage ratio. Second, the modeling of clad rupture effects was limited to those of increased cladding surface area at the rupture site. The additional effects, most notably those beneficial to interphase heat transfer and clad cooling above the rupture, were not represented.

The spacer grid effects model as originally formulated was found to be appropriate to account for the effects of simple spacer grids, as had been used in the FLECHT, FLECHT-SEASET, and other experiments. Applying the model to grids with higher blockage ratios, particularly to the mixing vane grids used in the Mark-BW design, was found to be overly conservative. The model was not capable of representing mixing vanes other than as an increase in the thickness of the grid straps, that is, as a simple increase in the total blockage ratio. In addition, in developing the original model it was decided that the occurrence of rupture and the impact of the resultant channel blockage on interphase heat transfer would not be modeled.

The grid fine-drop vaporization model was upgraded so that the droplet impact Weber number is calculated as a function of axial position within the grid and a new correlation for the fine drop vaporization constant, $C_{max}$, was developed. Second, the procedure for running BEACH was altered such that a blockage effects model, similar to a grid model, could be user-initiated at the location of rupture when rupture occurred. This required stopping BEACH near the time of rupture, executing FRAP-T6 with

pre-rupture inputs from BEACH to determine the time and location of rupture, and restarting BEACH post-rupture with the blockage effects model and appropriate inputs active.

Even after the above modifications the cladding temperatures calculated in FRAP-T6 were higher than those calculated in BEACH. The difference was primarily the result of the selected code interrelationships (Figure B-1). FRAP-T6-B&W does not model axial conduction along the fuel pin. With 200 F to 300 F cladding temperature differences between ruptured and unruptured locations, the effect of axial conduction on the solution can be significant. A change was made to replace the FRAP-T6-B&W code during reflooding with the BEACH code. This required the addition of metal-water reaction and clad strain and rupture models, that were already available in RELAP5/MOD2-B&W (the parent code for BEACH), to the BEACH code.

Additionally, the fine-mesh rezoning in the BEACH conduction solution were revised to retain the maximum user input fine mesh points near the quench front. In the nucleate boiling regime a constant heat transfer coefficient of 3000 $W/m^2$-K (528 Btu/hr-$ft^2$-F) was used for the boiling part. The maximum number of fine mesh points, a user input, was increased from 16 to 32.

The revised code interfaces are shown in Figure 4-1. FRAP-T6-B&W is run only through the refill period, and BEACH, initialized to FRAP-T6, is started at the beginning of reflooding and run to the end of the analysis. If rupture occurs, BEACH is run in two passes. First, BEACH is run to the time of rupture. At this point the cladding surface area at the location of rupture is increased, the blockage model applied, and BEACH is restarted and run to the end of the analysis.

## B.3. Effects of Evaluation Model Revisions

The changes to the evaluation model described in the foregoing section affect the calculated LOCA results only in the hot pin portion of the calculation and only during the reflood phase of the transient. The RELAP5/MOD2-B&W and REFLOD3B computer codes are completely unaffected by the changes, and the FRAP-T6-B&W analysis of the blowdown and refill phases is not impacted. For the most part, the original evaluation model remains intact, and trends and conclusions derived from it continue to be applicable.

### Effects of Spacer Grid and Rupture Modifications

Because the grid model change was to make the model responsive to grid type and to generate a more accurate solution, the effect of the modification depends on the type of grid being modeled. The original grid model was directly tied to spacer grids of conventional design (simple grids). Such grids have blockage factors (fraction of the flow channel area blocked by the grid) of about 30 percent. The revised model is formulated to be the same as the original when applied to simple grids. Thus, results produced by BEACH in benchmarks of experiments that used plain or simple spacer grids are unaffected by the model changes. For grid designs with higher blockage ratios (such as zircaloy grids or mixing vane spacer grids), the blockage factors range from 45 to 60 percent and the blockage is distributed along the direction of flow. (For example, there may be a step to 40 percent blockage followed by a step to 60 percent blockage). The effects of such grids on interphase heat transfer were found to be barely distinguishable from the effects of grids with a 30 percent blockage factor. The revised model accounts for the differences in blockage factor, and the interphase heat transfer, particularly due to fine-drop effects, is substantially increased for the higher blockage factor grids. Depending on the

particular grid design, the amount of interphase heat transfer may increase considerably. Similarly, if a grid does not have a higher blockage factor, the amount of interphase heat transfer will be reduced by the new modeling.

The immediate effect of this change in the calculation is observed in the "grid nodes" of the hot channel simulation. At these locations, the vapor temperature can be reduced by several hundred degrees, and that can be directly reflected in the cladding temperature. However, this observation would apply to any representative grid model, and the local effect is not the one of interest. Because the interphase heat transfer has been improved at the grid, the downstream locations within the span experience increased flow of the steam phase, and the temperature of the steam is reduced. Thus, heat transfer downstream of the spacer grid is improved. This interaction propagates downstream for several feet before it is essentially washed-out. By the time (length) the effects of one grid have diminished, the flow encounters the next grid. The grid spans are typically 1.6 to 1.9 feet. Thus, the effect of this model change on the results on an evaluation (on an assembly with a higher blockage factor grid) is an overall decrease in cladding temperatures. This effect will pertain to the entire length of the fuel pin, but the timing may differ between the upper and lower elevations.

The effect of improving the interphase heat transfer at the location of rupture is nearly identical to that of the grid model. Here, the blockage factor is determined by the amount of swelling predicted by the rupture model. If that swelling is high, there will be a substantial increase in the interphase heat transfer. If the swelling is low, there will be little change. For a representative case of RSG plant evaluations the blockage factors from rupture are on the order of 50 to 60 percent, and the amount of resulting interphase heat transfer improvement is significant. As with the grid model, the effect is on the

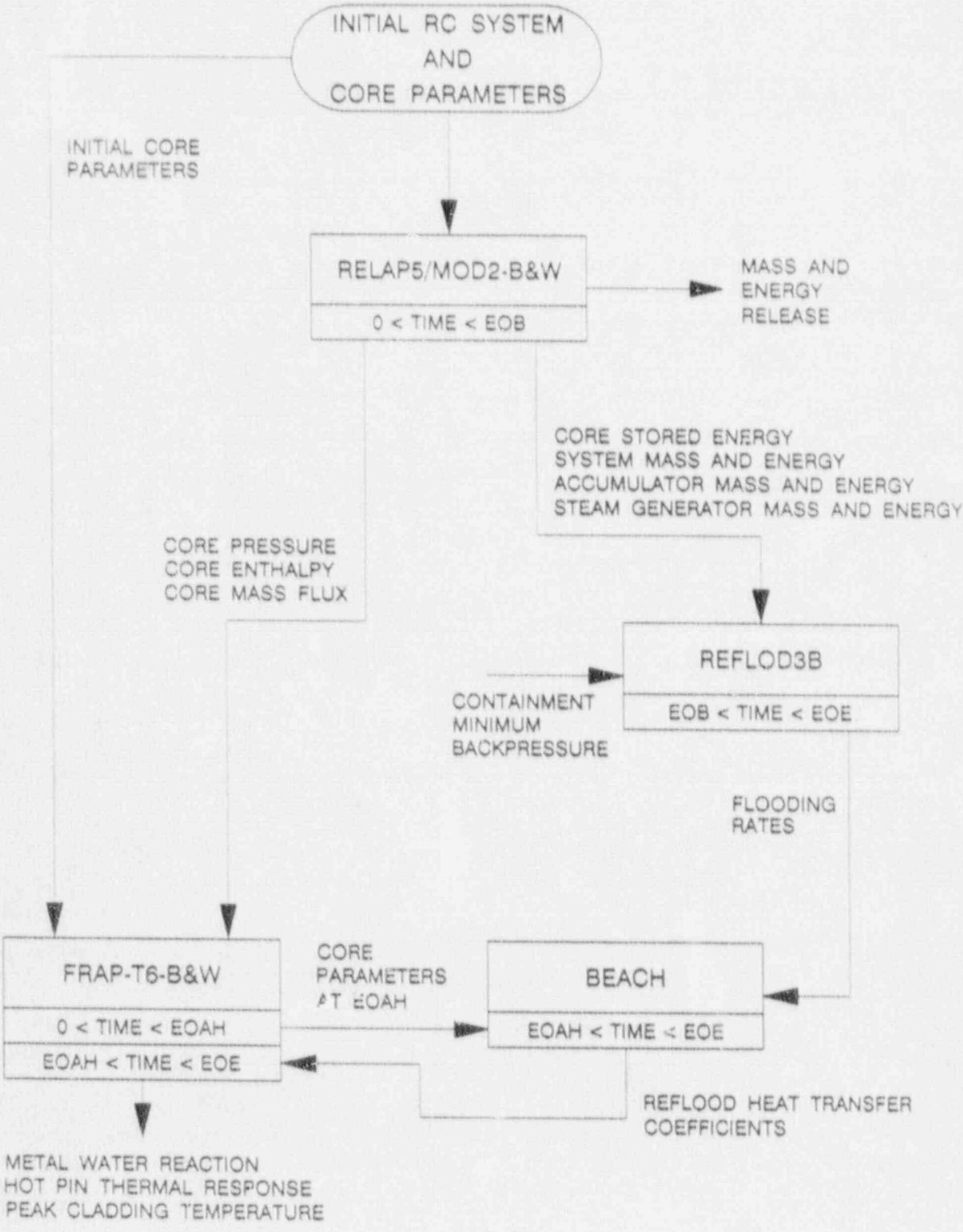
interphase heat transfer and can directly impact the ruptured node cladding temperature. At other locations on the pin, the higher and colder steam flow from the ruptured location will provide greater cooling for nearly two feet downstream. Beyond that, there is no discernible effect.

The effects of incorporating axial conduction into the hot channel calculation are substantial at the ruptured node and in the nodes adjacent to it. Without axial conduction, the ruptured node can be 200 F to 300 F below the adjacent nodes. With axial conduction, sufficient heat is transferred to the ruptured node that it runs only 50 F to 100 F below the adjacent nodes. Those nodes in turn are up to 100 F to 150 F cooler than calculated in the original model. The model also affects the grid nodes and adjacent nodes in the same way, though the effects are less marked, because the temperature gradients between the grid nodes and neighboring nodes are not as severe. The net effect, though, is the same: axial conduction reduces the temperature differences between adjacent heat structures.

The other model changes were refinements to remove spurious oscillations, driven by switching in the code logic. They are found to produce essentially no difference in peak cladding temperatures. Post-peak cooldown for nodes at and above the midplane is smoothed by the use of a 32-point fine-mesh rezoning.

The cumulative effect of the model changes on the calculated LOCA results is an overall reduction in predicted cladding temperatures. Post-rupture temperatures at locations adjacent to the ruptured node are noticeably reduced. With the additional cooling in the rupture node, and in the adjacent nodes through axial conduction, the model has the effect of separating the rupture location and the location of peak cladding temperature by one grid span.

FIGURE B-1. LARGE BREAK ANALYSIS CODE INTERFACE.



# APPENDIX C
## Justification of Appendix A Sensitivity Studies

Note: This appendix was originally added in its entirety in Revision 1 of BAW-10168, September 1989.

## C.1. Introduction

LOCA evaluations require that a substantial number of sensitivity studies be performed with the evaluation model to establish model convergence and conservatism. Such studies, many of which are generic, have been performed for the EM contained herein and are reported in Appendix A. The studies in Appendix A were performed with Revision 0 of the Evaluation Model. As explained in Appendix B most of the studies in Appendix A remain valid under the new evaluation model. This Appendix addresses the studies individually and assesses the applicability of the generic studies after the EM revision. Several of studies in Appendix A are confirmable. These required that the plant, to which the study is being applied, behave in a certain manner for the study to be applicable. The appendix also addresses the degree to which these confirmable studies remain valid. Studies such as break spectrum and worst case ECCS configuration are plant-specific and, because they would be repeated with each application of the evaluation model, are not addressed in this appendix. In addition, new material is provided for the burnup sensitivity study and a generic discussion of break location is presented.

## C.2. Evaluation Model Generic Studies

The generic studies demonstrate results that are characteristic of the evaluation model--the codes and their interfaces--and that are not plant dependent. An example of this is the RELAP5/MOD2-B&W time step study. This study demonstrates that the automatic time step selection in RELAP5/MOD2-B&W produces converged results. This demonstration need not be repeated for plant-specific applications wherein the modeling techniques used are represented by those in the evaluation model studies. The following discussion of the generic sensitivity studies concludes

that the conclusions of the studies remain applicable following the changes to the evaluation model.

RELAP5/MOD2-B&W Time Step Study

This study (Section A.2.1) verified that for light water reactor geometry, the RELAP5 time step controller governs the code solution sufficiently to assure converged results. Alternate system designs within the group to be covered by the evaluation model will not change that result. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

RELAP5/MOD2-B&W Loop Noding Study

This study (Section A.2.2) verified the general noding requirements within the loop for recirculating steam generator plants. In conjunction with the break noding study, the results can be applied to the separate regions of the hot leg, the steam generator, and the cold leg. Alternate system designs within the group to be covered by the evaluation model will not change the noding requirements. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

RELAP5/MOD2-B&W Break Noding Study

This study (Section A.3.1) verified that hydraulic stability is achieved by providing at least one control volume in the pipe between any adjacent component and the break node. The study is applicable to all plants covered by the evaluation model. The differences between Revision 0 and Revision 1 of the EM do not

affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

RELAP5/MOD2-B&W Pressurizer Location Study

Although the assumption placing the pressurizer in one of the intact loops is conservative, this study (Section A.3.2) showed that there is little difference in results when the pressurizer is modeled in the broken loop. The lack of sensitivity to pressurizer location is expected to hold for all designs covered by the evaluation model and this study will be not be repeated. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

RELAP5/MOD2-B&W Core Crossflow Study

This study (Section A.3.4) verified that cross flow in a light water reactor is limited and does not substantially alter the course of a LOCA evaluation. The study is dependent only on the very basic aspects of the fuel design, which are consistent across the range of designs considered by the evaluation model. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

RELAP5/MOD2-B&W Core Noding Study

In conjunction with the core cross flow study, this study (Section A.3.5) verified that the modeling of a light water reactor core in six axial segments with a hot and an average channel provides sufficient spacial detailing for both model convergence and accuracy of results. As the basic core arrangement and fuel design are not altered across the range of designs to be considered, the results of the study are applicable

to all plants considered by the evaluation model. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

REFLOD3B Primary Coolant Pump Rotor Resistance Study

This study (Section A.2.4) showed a considerable reduction in flooding rate under a locked rotor assumption. The study affirms the generally accepted data on loop resistance effects on reflooding rates and is applicable for all plant types covered by the evaluation model. The differences between Revision 0 and Revision 1 of the EM do not affect the REFLOD3B code. Therefore, the study remains applicable.

FRAP-T6-B&W Time Step Study

This study (Section A.3.6) verified that the time step selection for FRAP-T6-B&W provided converged results for the spatial detail modeled in the base runs. Because the spatial detail required for the FRAP-T6-B&W model is not to be altered for the other designs covered by the evaluation model, the study remains valid for all designs. The differences between the evaluation package used for the study and the evaluation model used for this report only shorten the period of the LOCA for which FRAP-T6 is applied, that is to the blowdown and refill phases. As the evaluation model changes do not directly affect the FRAP-T6 code nor the blowdown and refill periods, the study remains applicable.

FRAP-T6-B&W Radial Fuel Segmentation Study

This study (Section A.3.7) verified that the number of solution points selected for radial representation of the fuel pin used by the base FRAP-T6-B&W model was adequate. The study is dependent only on the very basic aspects of the fuel design, which are

consistent across the range of designs considered by the evaluation model. The differences between Revision 0 and Revision 1 of the EM only shorten the period of the LOCA for which the FRAP-T6 code is applied to the blowdown and refill phases. As the model changes do not directly affect the FRAP-T6 code nor the blowdown and refill periods, the study remains applicable.

## C.3. Confirmable Sensitivity Studies

In addition to the generically applicable studies, several of the studies performed for the evaluation model are considered confirmable. These studies remain valid under most but not all circumstances. The following is a discussion of why such sensitivity studies remain valid for Revision 1 of the evaluation model.

### RELAP5/MOD2-B&W Pump Degradation Study

This study (Section A.3.3) established a most severe pump degradation multiplier by altering the pump effects on the core flow. The study can be applied to all plants which experience similar LOCA core flow histories during blowdown. The differences between Revision 0 and Revision 1 of the EM do not affect the RELAP5/MOD2-B&W code nor the blowdown period of the evaluation. Therefore, the study remains applicable.

### REFLOD3B Loop Noding Study

This study (Section A.2.3) verified the noding detail used in the REFLOD3B code. It is applicable to plants with a one-to-one correspondence of hot and cold legs, such as Duke Power's McGuire and Catawba units or the Trojan unit. A separate study is required only for severely altered loop designs, such as the B&W or Combustion Engineering 2-by-4 designs. The differences

between Revision 0 and Revision 1 of the EM do not affect the REFLOD3B code.. Therefore, the study remains applicable.

Time-in-Life Study

This study (Section A.3.9) showed the initial fuel temperature to be the dominant fuel burnup-related parameter affecting the LOCA results. There are three significant fuel parameters that vary with fuel burnup. The initial stored energy of the fuel, given by the volume-average temperature at any specific local power level, changes with burnup. The pattern observed is that the fuel temperature at a specific local power may increase sharply during the first few days of operation, but it will then decrease until relatively high burnups, after which a slow increase may occur. Calculations with TACO3 have shown that the initial rise in temperature takes place virtually at the start of fuel operation. From that point, there remains a long burnup period with decreasing fuel temperatures. The fuel pin internal pressure undergoes a continuous increase with burnup. The third parameter, which is of secondary importance, is the oxide thickness, which increases with fuel burnup.

While the interactions among these parameters in a LOCA analysis are complex--depending on the complexity of the model being used--the sensitivities of the results to each can be generalized: The higher the average fuel temperature at any location on a fuel pin at the start of a LOCA, the higher the resultant cladding temperatures at that location during the LOCA. The higher the internal pin pressure at the initiation of the LOCA, the more likely or earlier the occurrence of rupture will be. This may or may not have a bearing on the peak cladding temperature. The thicker the initial oxide layer of the fuel pin, the lower the cladding temperatures for cladding that has temperatures above about 1800 F. The oxide effect results from metal-water reaction

and is relatively inconsequential for cladding that is not heated to 1800 F.

The effect of rupture, and thus, pin pressure on cladding temperature is model-dependent. For the computer codes and methods (Revision 0 of the EM) with which the sensitivity studies were performed, a rupture after the end-of-blowdown had essentially no effect on the cladding temperature at any location other than at the rupture location. At that location, the effect of rupture was to allow the gap and the cladding surface area to increase, both of which act to decrease the cladding temperature. Thus, the effect of rupture during reflooding was to reduce the temperature increase of the cladding at a location that, clad rupture notwithstanding, would not have been the peak temperature location. A rupture during blowdown was modeled to have one additional effect: The blowdown rupture would cause additional flow resistance to be calculated in the hot bundle. This would cause some flow diversion away from the hot assembly and result in poorer blowdown cooling for both the ruptured location and the rest of the hot pin. The rupture location would, nonetheless, remain relatively cooler than other locations on the pin, but those other locations would have higher cladding temperatures because of the blowdown rupture.

The recent evaluation model changes do not directly affect the occurrence of rupture during blowdown. The changes do, however, extend the calculated effects of rupture during reflood. In the latest model, all of the previously modeled aspects of rupture are included, with additional effects. The blockage of the flow channel by the expanded cladding will interact with the entrained liquid--droplets--in the dispersed flow causing further droplet shattering. This produces an increase in the calculated interphase heat transfer and thereby causes lower vapor temperatures and higher steam flows at the rupture location. Both of these propagate up the bundle channel to promote better

cooling in the adjacent nodes. Additionally, because BEACH models axial conduction in the cladding, once the ruptured node expands to as much as twice the heat transfer area, the resulting cladding temperature gradients permit substantial transfer of heat from the unruptured locations on either side of the rupture to the ruptured location. Both the increased interphase heat transfer and the axial conduction produce additional cooling in nodes beyond the immediate rupture location.

Considering the effects of cladding rupture in the latest model separately, it could be inferred that, because a predicted rupture improves the cooling and reduces the temperature increase during reflood for a portion of the cladding beyond the rupture site, earlier prediction of rupture would result in lower calculated peak cladding temperatures. It further develops that the relationship between pin internal pressure--the hoop stress leading to rupture--and fuel average temperature is such that the pin pressure is lowest at the burnup conditions producing the highest fuel average temperatures, namely at BOL conditions. The coincidence of these conditions leads to the conclusion that: For fuel pin pressures and temperatures not producing rupture during blowdown, the limiting LOCA initial conditions are those at beginning-of-life. If rupture occurs during blowdown, additional study is required to determine the most severe combination(s) of temperature, pin pressure, and oxide thickness. This is, in fact, what was observed in the sensitivity study performed in Appendix A.

The conclusion is independent of fuel and plant designs and applies to any fuel or plant of reasonably similar design that is determined not to encounter a blowdown rupture. During an application of the evaluation model, the conditions of the fuel for the highest burnup to be covered by the evaluation will be examined to determine if a blowdown rupture can be ruled out. If so the evaluation will be conducted at the burnup for which the

highest initial fuel temperatures occur, typically BOL. If the blowdown rupture can not be ruled out further study will be conducted to determine the most severe burnup for the evaluation.

The conclusions of this subsection have been developed using both the Revision 0 and Revision 1 Evaluation Models. Although the study of Appendix A, which directly supports the analysis provided, was performed with Revision 0, the changes of Revision 1 only make the conclusions more obvious. Therefore, these conclusions are valid under the Revision 1 EM.

C.4. Break Location

In general, break spectrum studies are plant-specific in terms of applicability. The break location studies, however, which have typically been included among the break spectrum cases can be considered generic. There are three break locations to consider for the large break LOCA: the hot leg piping, the cold leg piping at the pump suction, and the cold leg piping at the pump discharge--between the pump and the reactor vessel. Both hot leg and pump suction breaks have been consistently shown to result in peak cladding temperatures below those predicted for pump discharge breaks. (This is demonstrated in the Westinghouse RESAR for the 3411 Mwt class and is consistent with the numerous analyses reported by B&W for the B&W-designed PWRs.) The analysis in Appendix A of Reference 1 concluded that the blowdown and reflood cooling for the pump suction break were sufficient to dismiss the suction break as a potential limiting case strictly on the basis of the blowdown and reflood system analyses.

An examination of the consequences of the break location explains these observations. With the break at the pump discharge, one accumulator and approximately 25 percent of the available pumped injection will be bypassed directly to the containment, not providing blowdown cooling and lengthening adiabatic heatup.

Flow through the core during blowdown for the hot leg and the pump suction breaks will be more positive than for the pump discharge break because of the leak position in the loops. For the hot leg break there can be no bypass of the injected ECC water unless that water has already passed through the core. Furthermore, the elevation head driving reflood can rise to as high as the spillover elevation in the steam generator tubes.

For the pump suction break, the core flooding also improves because the break is moved to the other side of the pump. The effect, however, is not as strong as for a hot leg break. Therefore, in conjunction with the better blowdown cooling, the suction break produces a lower peak cladding temperature than does the pump discharge break. While the lower peak temperature alone precludes the suction break from being limiting, there is further reason to concentrate analyses upon breaks in the pump discharge piping. Because of the volume of pumped ECCS flow spilled from the system for the pump discharge break (roughly 25 percent), that condition represents a more limiting ECCS flow than does the pump suction break. For these reasons, and based upon the discussions in the foregoing paragraphs, the spectrum and LOCA limits cases performed for a plant evaluation will be for large breaks in the reactor coolant pump discharge piping.

The conclusions of this subsection although supported by the work of Appendix A on the suction break have for the most part been arrived at through more basic considerations and are supported by the results of two other separate evaluation models. Therefore, these conclusions are independent of the EM changes.

BAW-10168-A

Topical Report
March 1991

- RSG LOCA -

# B&W Loss-of-Coolant Accident Evaluation Model for Recirculating Steam Generator Plants

## Volume II - Small Break

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Key Words: Small Break, LOCA, Transient, Water Reactors, Evaluation Model

ABSTRACT

This document presents the generic large and small break models used by Babcock & Wilcox for evaluating the performance of the emergency core cooling systems (ECCS) following a loss-of-coolant accident (LOCA) for Westinghouse and Combustion Engineering designed Pressurized Water Reactors (PWR) with B&W supplied fuel. The large break model is discussed in Volume I and the small break model in Volume II. The models have been developed and compared with the required and acceptable features contained in Appendix K of the Code of Federal Regulations, 10CFR50. The evaluation models are shown to conform to Appendix K requirements.

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# 1. INTRODUCTION

This report describes the features of the emergency core cooling system (ECCS) evaluation model used by B&W for application to Westinghouse (W) and Combustion Engineering (CE) designed PWR's with B&W reload fuel. The evaluation model is applicable to the pressurized water reactors (PWR) categorized in Table 1.1. There are no significant system design differences for the nuclear steam system (NSS) and the ECCS within each category as these design features are the basis for the grouping.

Specific design information for each plant category is considered input to the evaluation model and is generated using the assumptions and techniques described herein.

The "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Nuclear Power Reactors" (10CFR50.46) was issued by the NRC in January 1974. Appendix K of 10CFR50.46 defines the required and acceptable features of models to be used to evaluate the performance of the ECC systems. The information presented in this document defines the B&W evaluation model and shows that the model conforms to Appendix K requirements.

The topical report is divided into two volumes. The first volume presents the large-break evaluation model. The second volume presents the small-break evaluation model. Each of the volumes contains the following seven sections which define the respective evaluation models:

1. Definition of model versus input (Section 3).

2. Features of the evaluation model and statements of conformity to Appendix K (Section 4).

3. The calculational technique used to evaluate the maximum local cladding oxidation (Section 5).

4. The calculational technique to evaluate the maximum hydrogen generation (Section 6).

5. The technique used to evaluate conformance to the coolable geometry criterion (Section 7).

6. The technique for establishing conformance to the long-term cooling criteria (Section 8).

7. Required documentation necessary to meet 10CFR50.46 (Section 9).

Table 1-1. Applicable PWR Plant Categories.

| Reactor Supplier | RCS Geometry | Fuel Design | Containment Type | ECCS Type |
|---|---|---|---|---|
| W | 3 loop | 15 x 15 | H | Conventional |
| W | 3 loop | 17 x 17 | H | Conventional |
| W | 4 loop | 15 x 15 | H | Conventional |
| W | 4 loop | 15 x 15 | L | Conventional |
| W | 4 loop | 17 x 17 | H | Conventional |
| W | 4 loop | 17 x 17 | L | Conventional |
| W | 4 loop | 17 x 17 | L | UHI valved out |
| CE | 3 loop | 14 x 14 | H | Conventional |
| CE | 4 loop | 16 x 16 | H | Conventional |

Containment type explanation

H = High pressure design

L = Low pressure design with ice condenser

## 2. COMPLIANCE TO 10CFR50.46

The "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Power Reactors" (10CFR50.46), issued by the Nuclear Regulatory Commission in January 1974, include five criteria that must be met before an emergency core cooling system (ECCS) is acceptable. Conformance to these criteria is established in the following manner:

1. The peak cladding temperature shall not exceed 2200 F.

   The peak cladding temperature is calculated with the evaluation model described herein and shown not to exceed 2200 F.

2. The percentage of local cladding oxidation shall not exceed 17%.

   In the analysis performed to satisfy condition 1, the total oxide thickness (combining both inside and outside oxide layers) at the location of maximum local oxidation is calculated and shown to be less than 0.17 times the total cladding thickness.

3. The maximum hydrogen generated during the transient shall not exceed that which would be generated by the oxidation of 1% of the reactor cladding.

   The amount of core wide zirconium oxidation which occurs during small break LOCA is much less than for large break LOCA. This criteria is satisfied by showing that an existing core wide metal water oxidation prediction (typically for a large break) bounds the results for small breaks.

4. Calculated changes in the core geometry shall be such that the core remains amenable to cooling.

   The changes in geometry that were calculated during the analysis for condition 1 are examined to ensure that no gross core blockage or deformation occurs.

5. The mode of long-term cooling shall be established.

   The analysis is continued until the cladding temperatures at all locations in the core are decreasing and the fluid level in the core is rising. At this time the path to long-term cooling is established. Cooling for the long-term is established by pumped injection with no interruptions or decrease in coolant flow thereby maintaining the core temperature at an acceptably low value while removing decay heat.

Appendix K sets forth certain required and acceptable features of the evaluation model that must be used to show compliance to the five acceptance criteria of 10CFR50.46. Compliance of the B&W evaluation model to Appendix K is shown in Section 4.

# 3. DELINEATION OF SBLOCA MODEL VERSUS INPUT

The information presented in this document defines the B&W ECCS evaluation model for application to non-B&W plants with B&W supplied reload fuel. In particular, this document describes the techniques and assumptions used in the evaluation of the consequences of a loss-of-coolant accident. These techniques and assumptions constitute the model. The required features of the ECCS evaluation model are set forth in Appendix K to 10CFR50. The evaluation model for small breaks is given in Chapter 4 along with statements of its conformance to Appendix K. The evaluation model will be changed only if the NRC issues rule changes or if improved analytical techniques become available.

Specific category-related information (such as system design, power level, etc.) is considered input to the model. These numbers are developed using the techniques and assumptions described in the model. This report covers the model only. All other information needed to perform the necessary analyses is considered input. Input may change throughout the design life of a nuclear steam system requiring a reapplication of the evaluation model while not affecting the evaluation model per se.

# 4. SMALL BREAK EVALUATION MODEL

## 4.1. Model Applicability

The small break evaluation model described in this section is applicable to all the general plant categories presented in Table 1.1. Any item that is applicable only to certain plant categories due to differences in design, such as RCS geometry, containment pressure, ECCS design or fuel design will be specifically identified. There are no significant design differences between the plants within each category.

## 4.2. Transient Description and Computer Code Interfaces

A postulated RCS (reactor coolant system) piping break is considered to be a small break when its cross-sectional area is 1.0 $ft^2$ or less. Experience with studies of small breaks has shown that the large break phenomena of ECC bypass and reflood do not apply to breaks of this size or smaller. The brief description of the behavior of small break transients that follows identifies the major phenomena simulated in the evaluation technique.

In general, a small break loss-of-coolant transient begins with a depressurization of the RCS to the hot leg saturation pressure. This is followed by an extended saturated depressurization. If the reactor coolant pumps continue to operate, the resultant forced circulation provides effective and continuous core cooling throughout the transient. The reactor coolant system void fraction will increase (frequently coming quite close to total voiding) until the decay heating rate, the ECCS injection, and the break flow come into equilibrium. Thereafter, the system void fraction will slowly decrease as excess ECCS water is injected, and the decay heating rate continues to decline. Eventually the system will depressurize sufficiently to be put into a long-term cooling configuration.

If the reactor coolant pumps do not continue to operate, the transient evolves in a more complicated fashion. First, beginning at the time at which the reactor coolant pumps begin to coastdown, the system undergoes a transition from forced flow conditions to those of natural circulation. During the saturated blowdown phase steam and water will separate within the reactor coolant system. Core heat transfer is by pool nucleate boiling and is adequate to keep the cladding cool so long as the core is covered by a mixture of steam and water. If the liquid inventory is reduced, such that part of the core is uncovered, that portion of the core above the mixture will be cooled by steam flow. Generally these flow rates are insufficient to maintain the cladding near the saturation temperature and the cladding temperature increases. With continued depressurization of the system and decline in the decay heating rate, the decrease in system inventory is halted and the reactor coolant system refilled. Eventually, the system depressurizes to a condition at which it can be placed in long-term cooling.

The RELAP5/MOD2-B&W[1] code is used to predict the thermal-hydraulic behavior of the reactor coolant system. If RELAP5/MOD2-B&W predicts that the core will be covered with liquid throughout the transient, no thermal analysis is required and compliance with 10CFR50.46 is assured. Otherwise, the FOAM2[2] code is used to determine the mixture height within the core, and the thermal response of the hottest fuel pin is evaluated by FRAP-T6-B&W[3]. Figure 4-1 illustrates the interrelationship of the computer codes.

## 4.3. Features of Model

This section addresses the features of the B&W small break evaluation model for plants with recirculating steam generators. Compliance of the model with 10CFR50, Appendix K, is described in Section 4.4.

The evaluation model described sets forth guidelines for the use of and the interfaces among the codes used. The organization parallels that of Appendix K and applies to all codes as appropriate. When specific details are employed in only one code, the situation is clearly identified.

4.3.1. Heat Sources

The analysis considers the heat sources listed below. These sources are time-dependent, and calculation of the heat sources within the codes is dependent on the phase of the accident.

4.3.1.1. Initial Power

For the purpose of obtaining initial stored energy and decay heat, the reactor is assumed to be operating at 102% of the highest licensed power level for that plant category at the time of the accident to allow for such uncertainties as instrumentation error.

4.3.1.2. Core Peaking Factors

If core uncovering is predicted, the cladding temperature transient is evaluated using an outlet skewed power profile which is consistent with the technical specification limits. This selection creates a conservative simulation by reducing the steam flow available to cool the uncovered portion of the core while simultaneously increasing the heating rate at the locations to be cooled.

4.3.1.3. Core Initial Stored Energy

Small break LOCA results do not depend heavily on the initial enthalpy of the core or its distribution. Coolant levels and cladding temperatures are dictated by core power level, hot channel power, and decay heat. Thus, the initial temperature distribution and other related parameters for the core and hot channel will be taken as that used for large break LOCA as

outlined in Section 4.3.2.3 of Volume I of this report. This specifies the use of an NRC approved steady-state fuel pin model (TACO2[5] at present or TACO3[8] upon NRC approval) as the source of the initial fuel temperatures.

4.3.1.4. Fission Heat

The fission power of the reactor is calculated using the point kinetics model in the RELAP5/MOD2-B&W computer code (described in Section 2.3.1 of BAW-10164). Credit is taken for reactor trip and control rod insertion. The response of the RCS following a small break is characterized by a slow reduction in pressure and system flow. In the upper core and plenum regions, this results in lower forces (DP and flow) opposing control rod insertion than those present during normal operation. Thus, reactor trip initiated on low RCS pressure and subsequent control rod insertion is assumed when it is calculated to occur. Provisions are made to analyze this event conservatively; that is, a minimum tripped rod worth and appropriate delay and insertion times are assumed. The core is assured to be shut down by fully inserted control rods and the use of borated water for recirculation (all of the ECC systems supply borated water).

4.3.1.5. Decay of Actinides

Heat from the radioactive decay of actinides is calculated using the 1979 ANS standard.

4.3.1.6. Fission Product Decay Heat

The ANS standard fission product decay heat curve for infinite irradiation published in October 1971 is used with a factor of 1.2 to determine fission product decay heat during the analysis (Figure 4-5 of Volume I shows the values to be used).

4.3.1.7. Heat Distribution

As the thermal-hydraulic model (RELAP5/MOD2-B&W) for the small break LOCA evaluates only the average core, there is no provision for a hot channel power distribution factor in this model. A power distribution factor of 0.95 is used during the analysis for the hot channel using FRAP-T6-B&W. This factor is based on the consideration that 5.0 percent of the fission energy is deposited directly in the coolant rather than in the fuel or cladding; thus, its heat is not transmitted through the cladding in the removal process.

The peak power in the core is evaluated using the following equation:

$$PLP = \frac{1.02 \times kW \times P_f \times F \times P(t)}{\text{total active pin length}} ,$$

where

PLP = (peak linear power), linear heat rate, kW/ft,
kW = rated power,
$P_f$ = total peaking factor,
F = appropriate power distribution factor, and
P(t) = normalized transient power.

4.3.1.8. Metal-Water Reaction Rate

The rate of energy release from metal-water reaction is calculated by the Baker-Just equation without steam limiting. Details of the model appear in Sections 2.3.2 and 2.1.2 of BAW-10164 (RELAP5/MOD2-B&W) and BAW-10165 (FRAP-T6-B&W), respectively.

The initial oxide thicknesses on the outside and inside cladding surfaces are consistent with the time-in-life that maximizes cladding temperature for large break LOCA. The oxide thicknesses

may be calculated from Figures 4-6, 4-7, and 4-8 of Volume I of this topical report.

#### 4.3.1.9. Primary Metal Heating

Heat transfer from the reactor vessel walls, piping, and non-fuel internal structures is taken into account. Appropriate metal slabs are simulated in each control volume of the RELAP5/MOD2-B&W model using the system heat transfer model. The general heat slab model for RELAP5/MOD2-B&W is explained in Section 2.2 of BAW-10164. A previous sensitivity study on the effect of increasing the primary heat addition (Section 5.7 of topical report BAW-10091[7]) showed minimal effect for this heat source in a small-break application.

#### 4.3.1.10. Primary-to-Secondary Heat Transfer

Primary-to-secondary heat transfer in the steam generator is modeled to allow the secondary side to act as a heat source as well as a heat sink during an analysis using the model in Section 2.2 of BAW-10164.

### 4.3.2. Reactor System Hydrodynamics

#### 4.3.2.1. Noding Scheme for RELAP5/MOD2-B&W

The noding description of the RCS used in the RELAP5/MOD2-B&W computer model for small break analysis is the same as the large break noding except that the core has fewer control volumes. The noding in the core required in large breaks for hot channel flow diversion is not required for the small break model because small breaks have a relatively slow depressurization rate. The noding scheme generally used for the small break blowdown analysis is shown in Figure 4-2. The noding arrangement is based on past experience (topical report BAW-10154[6], Appendices E, F, and I), the model shown in the LOFT small break model[4], and the noding sensitivity studies shown herein in Appendix A, Volume I.

The control volumes are selected to preserve key volumes in the system: (1) volume in the RV below the bottom of the active fuel, (2) volume below the top of the active fuel, (3) reactor vessel volume below the bottom of the hot and cold leg nozzles, (4) loop volume below the RC pump spillover point, (5) cold leg volume on either side of the pump spillover point, and (6) SG tube volume split at the top of the U-bend. As in the large break noding scheme, the small break noding has one loop representing the broken loop and the other representing the three intact loops with a non-equilibrium pressurizer. The noding in the vicinity of the small break is the same as that used for the large break.

4.3.2.2. Flow Model for RELAP5/MOD2-B&W

Flow between control volumes is determined by the phasic momentum equations. Included in the formulation of the momentum equations are terms that account for (1) temporal change of momentum, (2) momentum convection, (3) momentum change due to compressibility, (4) momentum flux resulting from area change, (5) pressure loss resulting from wall friction, (6) pressure loss due to area change, and (7) gravitational acceleration. Two-phase friction effects are also taken into account. The derivation of the governing fluid flow equations used in RELAP5/MOD2-B&W is given in Section 2.1.1 of topical report BAW-10164.

4.3.2.3. Discharge Model

The discharge of subcooled fluid through the break is determined using the Extended Henry-Fauske model. Once the break node becomes saturated, the discharge is based on the Moody model. To avoid a step change in the flow between the use of the two models, a transition region is used (See equation on Page 2.1-109 of BAW-10164). The transition region uses an under-relaxation technique to smooth the flow whenever the upstream void fraction is between a specified range, $\alpha_{lower}$ and $\alpha_{upper}$. The Murdock-Bauman model is used for superheated discharge. Appendix C of

the RELAP5/MOD2-B&W topical report describes the formulations and assumptions regarding the discharge correlations.

The RELPA5/MOD2-B&W EM choked flow model allows input of four discharge coefficients as functions of the leak inlet conditions. Separate coefficients can be used for subcooled, transition to two-phase, two-phase, and for steam flow. The discharge coefficient transition for small breaks is based on a void fraction criteria. The subcooled coefficient is used for void fractions less than $\alpha_{lower}$. The transition discharge coefficient is used for void fractions between $\alpha_{lower}$ and $\alpha_{upper}$. The two-phase discharge coefficient is used when the void fraction is greater than $\alpha_{upper}$ and the leak enthalpy is less than the leak node saturated steam enthalpy. The superheat discharge coefficient is applied whenever the leak inlet enthalpy is greater than or equal to the leak node saturated steam enthalpy. The lower bound for the transition regime, $\alpha_{lower}$, is set to a one percent void fraction. Based on experimental data, the Moody critical flow model is observed to over-predict two-phase leak flows for qualities greater than 10 percent while under-predicting the flow for lower qualities. To account for this effect, the upper bound for the transition regime, $\alpha_{upper}$, is set to 70 percent void fraction which corrosponds to a quality of 10 percent at 1000 psia. In making adjustments to these coefficients, their relative relationships to each other is chosen and maintained as shown in the table given below.

| Regime | Range of Application | Normalized Value |
|---|---|---|
| Subcooled | $\alpha_g < 1\%$ | 1.0 |
| Transition | $\alpha_g \geq 1\%$ & $\alpha_g \leq 70\%$ | 1.0 |
| Two-phase | $\alpha_g > 70\%$ & $H_{mix} < H_{g,sat}$ | 0.7 |
| Superheat | $H_{mix} \geq H_{g,sat}$ | 1.0 |

These relative values may be adjusted uniformly across all regimes through a multiplier which accounts for a break area adjustment and is part of the spectrum approach to identify the worst case.

A spectrum of break areas is studied to ensure that breaks $1.0ft^2$ and less have been adequately investigated. The spectrum includes break location as well as break size variations.

4.3.2.4. Core Flow Model

The relatively high core flow rates early in the transient are calculated by RELAP5/MOD2-B&W. Later, however, if RELAP5/MOD2-B&W calculates core uncovering during the quiescent stage of the transient, the steam flow rate above the core mixture is determined by the FOAM2 code. The steaming rate, determined from average channel conditions, is applied to the thermal analysis of the hot pin. Because a significantly higher swell level and steaming rate would be associated with the hot channel, the use of average conditions implicitly allows for the effects of blockage and flow diversion to neighboring bundles.

4.3.2.5. Phase Separation and Countercurrent Flow

For small break LOCA applications, all system junctions will be modeled as nonhomogeneous. The RELAP5/MOD2-B&W code has three simplified flow regime maps that are used to determine the phase separation within the RCS system components. Vertical flow, horizontal flow, and high mixing for pumps maps are included. These models are described in detail in Section 2.1.3 of the RELAP5/MOD2-B&W topical report BAW-10164.

4.3.2.6. Pump Model

The reactor coolant (RC) pumps are represented by the two-phase dynamic pump model. Two-phase pump head and torque degradation are also accounted for in the pump model. The pump models are described in Section 2.1.5.2 of the RELAP5/MOD2-B&W topical report BAW-10164.

4.3.2.7. Mixture Height Calculation

Should RELAP5/MOD2-B&W predict partial core uncovering, the FOAM2 code is used to determine the mixture height in the core during the quiescent portion of the transient. Steam production due to core average power, primary metal heat, and flashing within the vessel combined with the inner vessel liquid volume are used to calculate the rate of steam separation from the mixture and the corresponding degree of swelling due to entrained steam. The FOAM2 code is discussed in topical report BAW-10155. The amount of swelling in the hot channel is considerably greater than in the average channel. No credit is taken for this in the hot pin thermal analysis. Also, by using average core conditions, the steaming rate above the froth level calculated by FOAM2 already implicitly includes the effects of blockage and crossflow.

4.3.2.8. Single Failure Condition

The single failure assumed for small breaks is that which produces the minimum ECCS injection. The single failure assumed is the loss of one train of pumped ECCS injection. This approach is the most conservative since the containment backpressure has little effect on small breaks. A single failure chosen to minimize the containment backpressure would have no effect on the transient and would possibly increase ECC injection. Since pumped injection is very important for small breaks, limiting such injection produces the worst case.

4.3.3. Heat Transfer and Thermal Analysis

4.3.3.1. Flow Controlled Portion of Transient

Heat transfer is flow controlled while core flow remains greater than about one percent of full flow for the pumps-tripped case and continuously for a pumps operating case. During the flow coastdown for a pumps-tripped small break LOCA, heat transfer is by forced convection to liquid or by nucleate boiling. The cladding stays within a few degrees of the coolant saturation

temperature and the heat flux does not approach the CHF value. Since no temperature excursion occurs, the peak cladding temperature will be the peak initial cladding temperature, and no specific cladding temperature calculation need be made unless an initial condition is required for the calculation of a temperature excursion occurring during the quiescent period.

A small break with the reactor coolant pumps operating is characterized by a continuous flow of coolant through the core. The pump induced core flow evolves from its initial subcooled condition to one of high quality and back to liquid during the course of the transient. The reactor coolant pumps cavitate and degrade in performance during the transient recovering to full volumetric flow as the system void fraction approaches 100 percent. Cladding temperature calculations based on forced steam flow with the high pressure ECCS flow added have been used to bound the temperature excursions for all possible such accidents. This eliminates the need to do case specific cladding temperature evaluations.

The most limiting core cooling situation occurs when the plant is at the peak in void fraction for the specific SBLOCA (for most SBLOCA cases this is essentially 100 percent). Under the condition that the core flow does not evolve to a 100 percent quality before the ECCS injection can absorb core decay heating without superheating, the core cooling can be conservatively bounded by assuming that the core inlet volumetric flow is the same as that at power operation and that this flow is 100 percent saturated steam plus the high pressure ECCS injection. As discussed in the response to Round 1 question 54, this method would predict the peak cladding temperature below the range for metal-water reaction, and well below the limit of 10 CFR 50.46.

The above evaluation will be applied to all pumps running SBLOCA evaluations made under the B&W recirculating steam generator

evaluation model. The criterion for application will be that the ECCS injection is capable of absorbing the plant decay heat without superheating before the core inlet void fraction, as calculated by RELAP5/MOD2-B&W, reaches 100 percent. Only if the criterion is not met, will it be necessary to run FRAP-T6-B&W and use the large break LOCA approach to evaluate the cladding temperature.

4.3.3.2. Quiescent Portion of Transient

The system is considered to be in a quiescent condition once the core flow has decreased to less than about one percent of its initial value. Should the core remain covered during the accident, no thermal analysis using FRAP-T6-B&W is necessary. Numerous experiments, conducted at power levels two to three times higher than those that occur during the quiescent period of a small break LOCA, have demonstrated that pool heat transfer is sufficient to keep the cladding within a few degrees of the coolant saturation temperature.

Should the core become partially uncovered, heat transfer within the portion of the core covered by a steam-water mixture is through pool nucleate boiling. Above the mixture level, heat transfer will be by forced convection to steam and by radiation. FRAP-T6-B&W is used to evaluate the cladding temperature response. The degree of superheat in the steam flow is calculated in FRAP-T6-B&W using the technique described in Section 2.1.1.2 of BAW-10165 using the McEligot correlation as the convective heat transfer coefficient. Upon refill of the core, a return to pool nucleate boiling for the portion of the core being recovered by the steam-water mixture is allowed.

## 4.3.4. Cladding Swelling and Rupture

### 4.3.4.1. Flow Controlled Portion of Transient

The small break evaluation model includes a provision for predicting cladding swelling and rupture. The model is the same as that used for the large break model (Sections 4.3.3.1, 4.3.3.2, and 4.3.3.3 of Volume I of this topical report) and is based on NUREG-0630 data.

Occurrence of rupture is determined during a thermal analysis of the hot pin using FRAP-T6-B&W. The effects of rupture and the attendant minimal flow diversion are conservatively bounded during small break LOCA through the use of average core fluid boundary conditions within the FRAP-T6 analysis. Therefore, the effects of rupture are not fed back into the system analysis.

### 4.3.4.2. Quiescent Portion of Transient

#### 4.3.4.2.1. Below Core Mixture Level

Heat transfer to the mixture is by pool nucleate boiling, which is adequate to maintain the cladding well below rupture temperature, and rupture is not considered or allowed for this region.

#### 4.3.4.2.2. Above Core Mixture Level

The occurrence of rupture above the mixture requires the consideration of the effect on fluid flow and the energy equation and the effect on the pin heat transfer model. The effect on the fluid model is implicitly covered by the use of the average channel steaming rate as the coolant flow for the evaluation of the hot pin. The effects of rupture and the attendant minimal flow diversion are conservatively bounded during small break LOCA through the use of average core fluid boundary conditions within

the FRAP-T6 analysis. Once rupture has been calculated, the heat transfer, heat conduction, and metal-water reaction models are updated for the resultant strain and the availability of interior clad surface for oxidation.

## 4.4. Compliance

This section shows that the features of the small break evaluation model described in Section 4.3 conform to the required features of the evaluation model set forth in Appendix K. To accomplish this, the organization of this section parallels that of Appendix K and references are made to the appropriate parts of Section 4.3.

### I. Required and Acceptable Features

#### A. Heat Sources During LOCA

Sections 4.3.1.1 through 4.3.1.10 of the model directly incorporate all of the requirements of this section of Appendix K.

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B. Swelling and Rupture of Cladding and Fuel Rod Thermal Parameters

The small break model contains provisions for calculating the effects of clad swelling and rupture and changes in fuel rod thermal parameters with time (Sections 4.3.4.1 and 4.3.4.2).

C. Blowdown Phenomena

C.1. Break Characteristics and Flow

C.1.a. Spectrum of Breaks

As required, the small break analysis includes a spectrum of break areas up to 1.0ft$^2$ (Section 4.3.2.3). Breaks larger than this are evaluated with the large break model.

C.1.b. Discharge Model

The discharge model described in Section 4.3.2.3 complies with the rules of Appendix K. Incorporation of a range of discharge coefficients into the analysis is already implicit for small breaks by considering a spectrum of break areas. This results from the fact that the discharge coefficient only affects the apparent break size.

C.1.c. End of Blowdown

The term "End of Blowdown" does not apply to small break LOCA because effectively all event occurrences of significance take place during the depressurization of the LOCA. Similarly the phenomena of bypass and entrainment of liquid by reverse downcomer flow do not occur. The provisions of this section of Appendix K are thus large break specific and not addressed by the small break evaluation model.

C.1.d. Noding Near Break and ECCS Injection Points

Noding in these areas has been investigated (Section 4.3.2.1).

C.2. Frictional Pressure Drops

Section 4.3.2.2 of the model directly incorporates all of the requirements of this section of Appendix K.

C.3. Momentum Equation

The flow model used is based on the RELAP5/MOD2-B&W phasic momentum equations and includes the effects listed in Appendix K (Section 4.3.2.2).

C.4. Critical Heat Flux

The CHF correlations employed are acceptable per Section C.4 (Section 4.3.3.1.). Once CHF is predicted at an axial position, a return to nucleate boiling is delayed as described in Section 4.3.3.2, which is in agreement with the requirements of Appendix K.

C.5. Post-CHF Heat Transfer Correlations

The correlations and method of application used for post-CHF heat transfer are acceptable (Section 4.3.3).

C.6. Pump Modeling

As required, the reactor coolant system circulation pumps are represented by a two-phase, dynamic pump model (Section 4.3.2.6).

C.7. Core Flow Distribution During Blowdown

Flow through the core is smooth because of the nature of the small break accident (Section 4.2). The effects of rupture and blockage during the flow-controlled portion of the accident are determined in the hot pin temperature calculation (Section 4.3.4).

Core average conditions as a function of elevation are provided to FRAP-T6-B&W for the hot pin thermal analysis (Section 4.3.2.7).

## D. Post-Blowdown Phenomena - Heat Removal by ECCS

### D.1. Single Failure Criterion

The single failure chosen minimizes ECCS injection (Section 4.3.2.8).

### D.2. Containment Pressure

In most small breaks, the small discharge area and the slow depressurization result in the leak flow remaining choked until after long-term cooling has been established. In such situations, containment pressure does not influence the transient. However, some of the larger small breaks may unchoke before the core is completely recovered. The containment pressure will affect depressurization somewhat in these cases, but because recovering of the core is slow with small breaks, slight variations in pressure do not appreciably influence the rate of recovering. Containment pressure is not a critical factor in these accidents as long as the value used is reasonable.

### D.3. Calculation of Reflood Rate

The typical reflooding period as envisioned for large breaks does not occur for small breaks. Those requirements which can be logically extended to small breaks are applied; the other requirements are interpreted as large break-specific and not considered further. Appendix K places a requirement on the reactor coolant pumps during reflood. The effect and function of the requirement applies only to large breaks. For small breaks the reactor coolant pumps are modeled as on or off depending on the assumptions of the case. The carryout rate fraction is a large break reflooding parameter not applicable to small breaks wherein the core steam velocities are insufficient to entrain liquid.

The effects of accumulator injection tank cover gas being injected are not considered in the small break model. These effects are inconsequential or beneficial and it is conservative not to model them. Dissolved gases are evaluated in a bounding manner and also shown to have inconsequential impacts on the course of the transients.

The determination of the carryover fraction during reflooding of the core is not necessary in small break analyses because of the slow recovering, the relatively low cladding temperatures reached while uncovered, and the low power level. This results in a small amount of steam being formed with a correspondingly low separation velocity, which is not capable of entraining liquid.

D.4. Steam Interaction with Accumulator Water

The system control volumes for the small break LOCA calculations are modeled as nonequilibrium volumes. The interphase heat and mass transfer are therefore calculated directly by RELAP5/MOD2-B&W as described in Section 2.1.3.4 of the RELAP5/MOD2-B&W topical report BAW-10164.

D.5. Refill and Reflood Heat Transfer

During core recovering, heat transfer above the core mixture is by steam cooling only. As sections of the core are recovered, they are cooled by film boiling until nucleate boiling can be appropriately established (Section 4.3.3.2). This method conforms to the requirements of Appendix K.

FIGURE 4-1. SMALL BREAK ANALYSIS CODE INTERFACE.

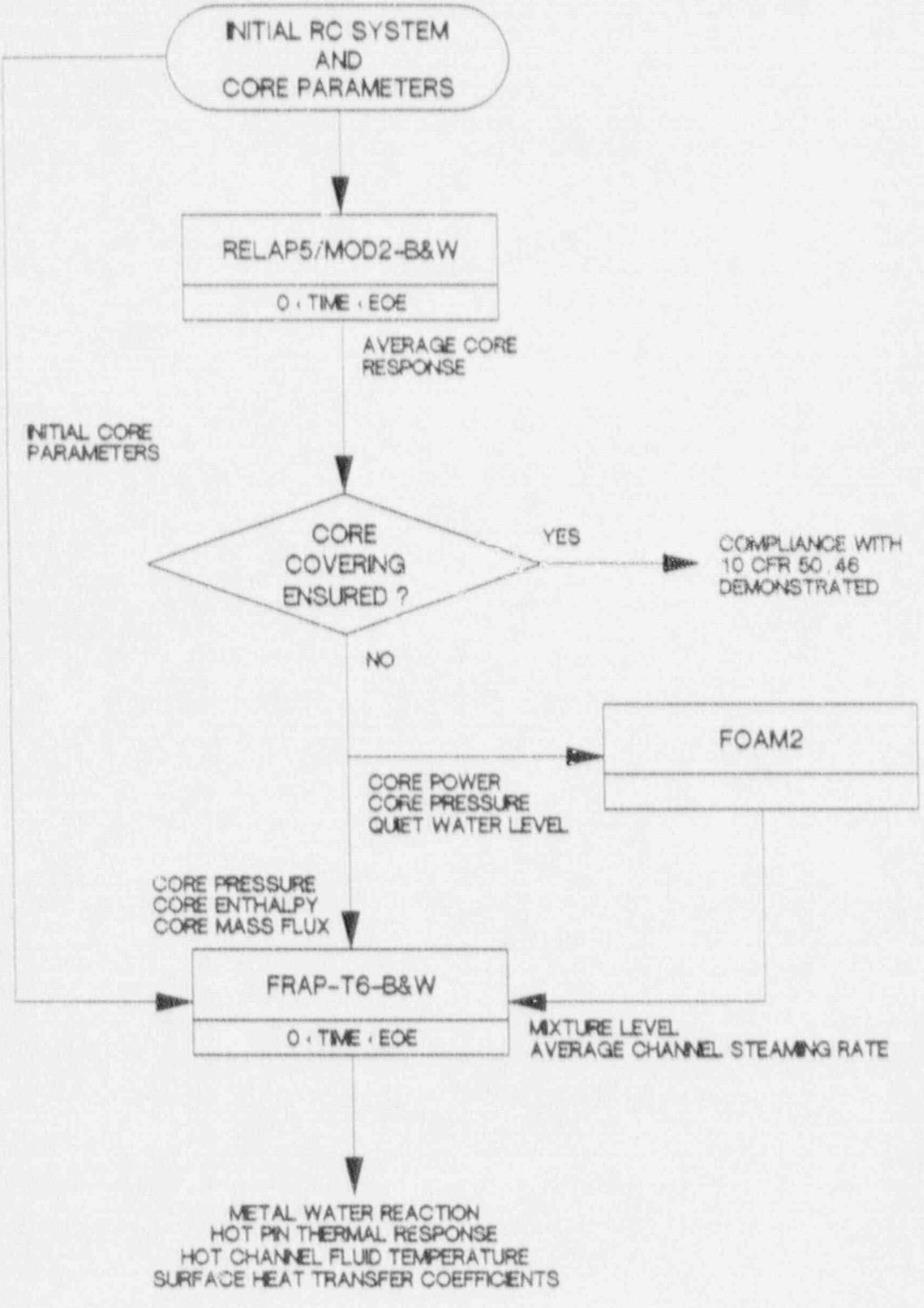
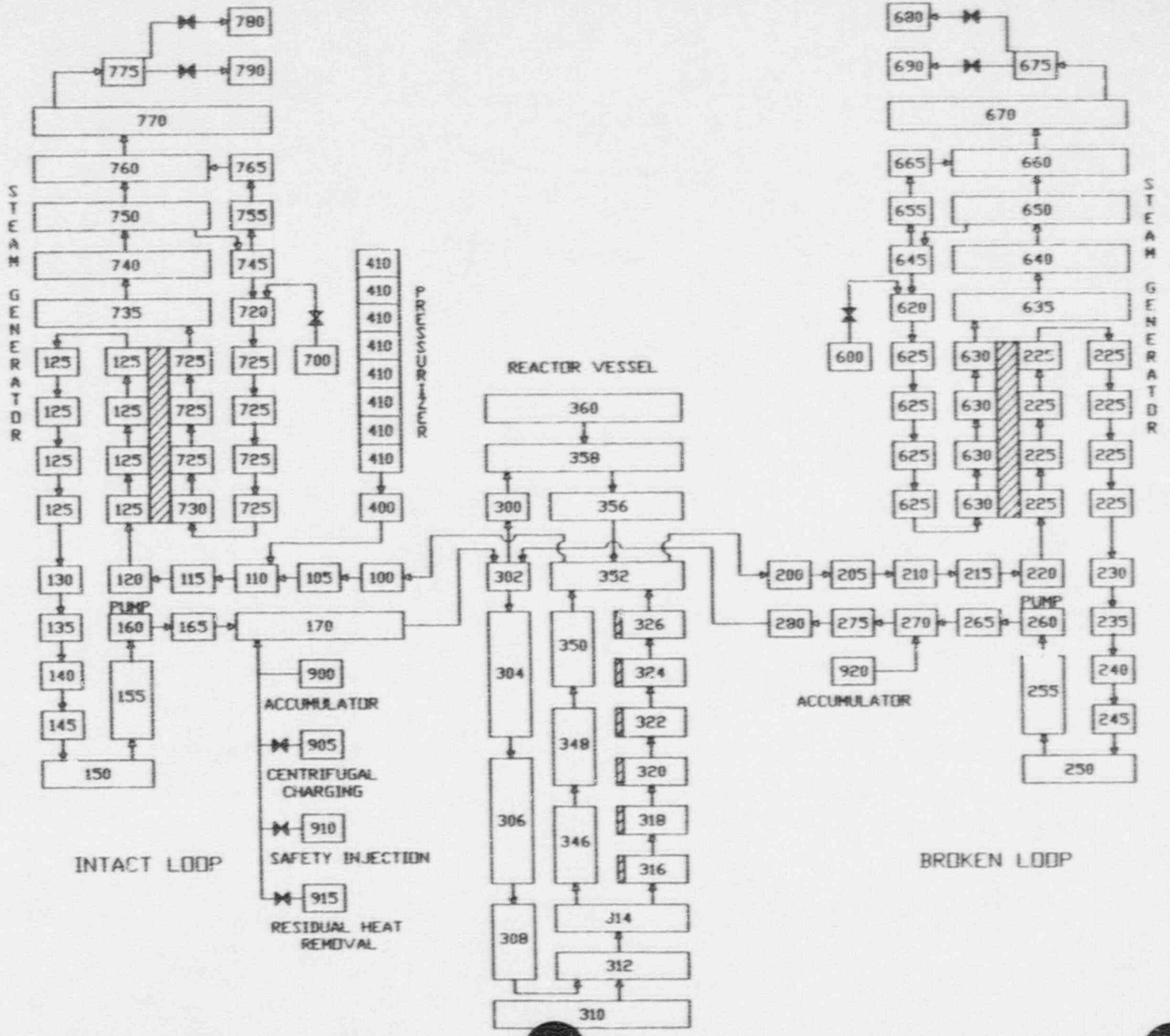


Figure 4-2. RELAP5/MOD2-B&W SBLOCA Model.



# 5. LOCAL CLADDING OXIDATION

Criterion 3 of 10CFR50.46 requires that the maximum local degree of cladding oxidation not exceed 17 percent. Compliance to this criterion is obtained by evaluating the results of the calculation for peak cladding temperature. The amount of oxide thickness at each location for which a temperature is determined is computed and divided by the cladding thickness at each location. For this purpose the cladding thickness is the as-calculated strained thickness circumferentially averaged.

# 6. MAXIMUM HYDROGEN GENERATION

Criterion 3 of 10CFR50.46 states, the maximum amount of cladding oxidation during a LOCA shall be limited to 1 percent of the whole core cladding. For small break LOCA, cladding temperatures are generally below the temperatures at which any substantial metal-water reaction can occur. Therefore, the amount of core wide oxidation during a small break is essentially zero. However, based on the response to Round 1 question 56, a case specific evaluation of the whole core metal-water reaction will be done for any SBLOCA that results in a peak local oxidation of more than 5 percent.

# 7. COOLABLE GEOMETRY

Criterion 4 of 10CFR50.46 states, "Calculated changes in core geometry shall be such that the core remains amenable to cooling." The analysis for predicting the peak cladding temperature uses the hottest fuel pin in the core. This analysis includes the effect of clad swelling and flow blockage based on NUREG-0630 and shows that the peak temperature remains below 2200 F. The combination of core geometry, coolant flow, and local power at all other locations in the core produce transient cladding temperatures that peak below that of the fuel pin analyzed in the peak cladding temperature calculation. Therefore, these pins and assemblies also remain in a geometry "amenable to cooling."

# 8. LONG-TERM COOLING

Criterion 5 of 10CFR50.46 states, "A low core temperature must be maintained following the calculated successful initial operation of the ECCS and that decay heat must be removed for an extended period of time." This section describes the assumptions used to meet this condition.

## 8.1. Establishment of Long-Term Cooling

The analysis of a small break LOCA is continued until the cladding temperatures at all locations in the core have decreased to the coolant saturation temperature and the core is covered. At this time the path to long-term cooling is established. The core temperature is maintained within a few degrees of the coolant temperature thorough a continuous flow of water maintained by the ECCS.

Procedures have been established and approved by the NRC for the smooth transition to long-term cooling during which water is recirculated from the reactor building sump through a heat exchanger to the reactor vessel. Although the procedure for depressurization of the reactor coolant system is more complicated, the safety requirements are the same as those for large breaks, Chapter 8, Volume 1 of this report. To prevent the crystallization of boric acid within the core a throughput flow is assured by operator action. The computation of the timing of this action is bounded by the large break and that analysis is used to satisfy this criterion.

# 9. REQUIRED DOCUMENTATION

This section verifies compliance with the documentation requirements (Part II) of the Appendix to 10CFR50 and is arranged according to the section division of Part II.

## II. Required Documentation

1.a. The computer codes that form the basis for the ECCS evaluation model are described in the following B&W topical reports:

| | |
|---|---|
| RELAP5/MOD2-B&W | BAW-10164[1] |
| FRAP-T6-B&W | BAW-10165[3] |
| FOAM2 | BAW-10155[2] |

These topical reports include the derivations of the equations used in the codes, starting with fundamental physical laws and including all approximations. Any assumptions made in the derivation of the solution technique used by the code and the value of all code specified parameters are disclosed in these reports. The reports also describe the general application of the codes to applicable problems including potential code interfaces and input parameter selection.

The computer code options used in the Evaluation Model are summarized in Table 9-1 of Volume I. At several locations throughout all of the computer codes, correlations have been programmed to include user input multipliers. These constants shall all have a value of one unless specifically stated to the contrary. Programmed constants that are part of the correlations or part of B&W's implementation of the correlations will have the value as published in the individual code topical reports.

1.b. Any changes in the ECCS evaluation model that result in a deviation of more than 50 F in the calculated cladding temperature transient will be documented by appropriate amendments to the evaluation model description.

1.c. B&W computer codes, including their source coding, are controlled through a systematic process that has been audited and approved by the NRC. Each code is tracked by a unique name, version number, and revision level. Authorization to change an approved version of a code involves a multilayered review and approval process. The source listing of the current or past approved versions of the ECCS codes can be made available to the NRC, at B&W's offices in Lynchburg, Virginia, upon their request.

Inputs used during an evaluation can be categorized as follows:

Generic: User supplied values or constants whose values are controlled by the evaluation model. The materials properties are a good example of this type of input.

Prescribed: Input for which a determining procedure is specified in the evaluation model without the specification of a value. The use of hot fluid volumes within the RELAP5/MOD2 model is a good example of this type of input.

Plant: Input which is taken from documentation for the individual plant or plants to be covered by the evaluation. Plant geometry inputs are examples of this type of input.

Case: Input which will vary depending on the accident being evaluated. Break area is a good example of this type of input.

The B&W evaluation model controls these different types of inputs in differing ways. Generic and Prescribed inputs are controlled in the same fashion as code options, and are documented in context within the evaluation model report. A summary of the generic and prescribed inputs is given in Table 9.2 of Volume I.

Plant input is controlled by B&W internal calculational procedures and not by the evaluation model. These procedures are written to adhere to ANSI quality assurance standards. For the most part these procedures require that the input come from controlled design documentation, that it be referenceable, that its use be documented, and that an independent review be conducted to assure that this has been done. The documents attesting to this for any given evaluation are controlled documents, maintained at B&W's offices in Lynchburg, Virginia, and are available for audit upon request.

Case input is similar to assumptions. It is not controlled by either the evaluation model or B&W procedures other than that it must be documented along with the plant input in controlled stored records of the claculation. Case input is also available for audit upon request.

2. Convergence of solution techniques is demonstrated in Appendix A, Volume I of this topical report. System modeling and noding are described in Section 4.3.1 of this report. Time step selection is presented in Appendix A, Volume I of this report.

3. Appropriate sensitivity studies are presented in Appendix A of Volume I The impact of Revision 1 of this report on the sensivity studies is discussed in Appendices B and C, Volume I. Additional sensitivity studies, which must be performed for each specific plant category, will be documented as part of the plant-specific analysis.

4. Predictions and experimental data are being compared continually for the evaluation model and portions of it. Code topical reports contains current comparison to applicable experimental data as appropriate.

5. The B&W topical reports listed in Section 9.II.1.a, describe the ECCS evaluation model and provide the technical basis for the adequacy of the computational methods as well as compliance with 10CFR50. The evaluation model description provides sufficient flexibility that it is applicable to all the plant categories presented in Table 1-1.

# 10. REFERENCES


1. J. A. Klingenfus, et al, "RELAP5/MOD2-B&W -- An Advanced Computer Program for Light Water Reactor LOCA and Non-LOCA Transient Analysis," BAW-10164, Babcock & Wilcox, Lynchburg, Virginia.

2. N. H. Shah, et al, "FOAM2 -- Computer Program to Calculate Core Swell Level and Mass Flow Rate During Small-Break LOCA," BAW-10155, Babcock & Wilcox, Lynchburg, Virginia.

3. J. H. Jones, "FRAP-T6-B&W -- A Computer Code for the Transient Analysis of Light Water Reactor Fuel Rods," BAW-10165, Lynchburg, Virginia.

4. W. H Crush, et al, "Best Estimate Prediction for LOFT Nuclear Experiment L3-2," EGG-LOFT-5089, February 1980.

5. Y. H. Hsii, et al, "TACO2 Fuel Pin Performance Analyis," BAW-10141A, Babcock & Wilcox, Lynchburg, Virginia.

6. N. K. Savani, et al, "B&W's Small Break LOCA ECCS Evaluation Model," BAW-10154A, Babcock & Wilcox, Lynchburg, Virginia.

7. B. M. Dunn, et al, "B&W's Evaluation Model Report with Specific Application to 177 FA Plants with Lowered Loop Arrangement," BAW-10091, Babcock & Wilcox, Lynchburg, Virginia.

8. D. A. Wesley and K. J. Firth, "TACO3 - Fuel Pin Thermal Analysis Code," BAW-10162, Babcock & Wilcox, Lynchburg, Virginia.

# APPENDIX D

## Responses to Round 1 Request for Additional Information

1. Question: Section 4.3.1 stated that, "The selected representation of the core has been verified specifically for Westinghouse 17x17 four-loop design..." What verification is it referring to as the basis for this statement?

Response: Verification as used in section 4.3.1 refers to the sensitivity studies performed to assure that the selected noding and modeling assumptions to be used in the evaluation model combine to create a stable, converged, and physically sensible analytical representation of a Nuclear Power Plant. Section 4.3.1 makes the point that the sensitivity studies performed in support of the vessel noding used inputs characteristic of a Westinghouse 17 x 17 four-loop design. Not withstanding this, the information gained from the studies and much of the sensitivity observed is valid for all categories of plants covered by this evaluation model.

2. Question: Section 4.3.2.3 discussed the procedure for initializing the FRAP-T6-B&W model with the results from TACO2, but only briefly discussed the procedure for initializing the RELAP5/MOD2-B&W model with data from TACO2. Describe how this is accomplished. Clarify how the data from TACO2 are combined for input into both RELAP5/MOD2-B&W and FRAP-T6-B&W codes if fuel rod nodalization is not consistent between them (e.g., different numbers of axial nodes are used in TACO2 and RELAP5/MOD2-B&W). Also, clarify what procedures B&W uses to determine whether the data transfer from TACO2 to these two codes is implemented correctly (e.g., the initialization of RELAP5/MOD2-B&W is consistent with TACO2 output).

Response: The same initialization procedure is applied to the RELAP5 model with data from the approved fuels code (TACO2 or TACO3) outputs. The fuels code outputs provide fuel rod parameters as a function of linear heat rate. The parameters used in the initialization are average fuel pellet temperature, fuel pellet outside diameter, clad inside and outside diameters, gas composition, and gas pressure. Because of different axial noding schemes used in TACO2 - TACO3, RELAP5/MOD2 and FRAP T6, the local linear heat rates (node average) for the RELAP5 and FRAP-T6 models are calculated, and the fuel rod parameters for the corresponding linear heat rates are obtained from the fuels code outputs. This methodology will ensure a consistent data transfer from the fuels code to RELAP5 and FRAP-T6. Following the initialization, the fuel rod parameters from FRAP-T6 and RELAP5/MOD2 are verified against those of the approved fuels code (TACO2 or TACO3), and, if necessary, a multiplier is used to adjust the gap conductance and assure agreement between the codes. Agreement occurs when the average fuel temperature of FRAP-T6 and RELAP5/MOD2 is not

more than 20F below that predicted by the approved fuels code (TACO2 or TACO3).

3. Question: Section 4.3.2.4 stated that the values of the reactivity coefficients for fuel temperature and moderator voiding used in the reactor kinetics model would be chosen to be conservative for the time in plant life that yields the highest PCT. Are these parameters to be justified on a plant specific basis? If not, provide additional information and values of these coefficients to be used in the evaluation model (EM) analysis, and the basis to justify the conservatism and bounding nature for all plants to be analyzed.

Response: For the large break LOCA model the only reactivity effects controlling plant shutdown are the fuel temperature and the moderator voiding effects. The values of the coefficients that describe these effects are burnup and cycle design sensitive. The fuel temperature coefficient, Doppler coefficient, becomes more negative with the build up of plutonium in the fuel. The moderator void coefficient becomes more negative as the boron concentration is reduced during the fuel cycle. The values used in the LOCA evaluation are selected as conservative for the burnup at which the evaluation is performed. Justification of the conservatism is part of the fuel design and the applicability of the values used is verified for each fuel cycle. The values actually used in a LOCA evaluation are published in the report describing the evaluation calculations.

A bounding fuel temperature coefficient value will be selected at the time of the LOCA evaluation. This value will be reported in the report documenting the LOCA evaluation for the specific fuel and plant. On each subsequent refueling validation of the coefficient is done by recalculating the Doppler coefficient and assuring that

the one used in the LOCA evaluation is more negative than the coefficient for the reload.

The moderator void coefficient is developed by computing the effects of voiding in a core with a selected moderator temperature coefficient. So long as the selected temperature coefficient is more positive than the temperature coefficient applicable to the fuel at the burnup being evaluated, the moderator void coefficient will be conservative. A moderator temperature coefficient, bounding for the expected range of application of the LOCA evaluation, is selected when the application computer runs are conducted and reported with the LOCA results. On each subsequent refueling this coefficient is compared to new calculations of the moderator temperature coefficient to assure that the value used in the LOCA evaluation is still bounding.

4. Question: Section 4.3.2.4 stated that no explicit calculation of fission power would be made during the reflood phase of the accident. Rather, the fraction of fission power to the total power at the end of blowdown would be kept constant. With this assumption, fission power would decrease at the same rate as decay heat. Additional information is needed to justify this assumption. Compare the fission heat calculated using the assumption to that calculated using the reactor kinetics model. If the proposed approach does not accurately represent the fission power calculated using the kinetics model or is not conservative, discuss how B&W will modify its EM model to conservatively calculate fission power during the reflood phase of a LBLOCA.

Response: The approach to the evaluation of post blowdown fission power as a constant fraction of decay heat power is not unique to this evaluation model. The B&W evaluation model for B&W-designed NSS's uses this approach and has been approved by the NRC. Although it is not an exact accounting for the fission heat, the approach is reasonable and somewhat conservative as it is applied to the peak cladding temperature calculations.

Figure 4-1 shows fission power and decay heat power normalized to initial power as a function of time for the 0.8 discharge coefficient double-ended cold leg break presented in the evaluation model topical, BAW-10168. Also presented is the ratio of fission power-to-decay heat power. The conditions after 10 seconds relative to reactivity are reasonably steady and the decays of the components can be expected to continue at their relative rates. Two observations from these curves are important. First, the contribution of fission power to total power is very small by the end of blowdown, less than 3% by 19 seconds. Second,

from 10 seconds on, the ratio of fission power-to-decay heat has continued to decline. Fixing this ratio at the end of blowdown is a reasonable approach to providing some accounting for fission power during the post blowdown period.

So long as reactivity insertions do not occur, the rates of decline as illustrated in Figure 4-1 will be preserved and the fission power will continue to be an increasingly smaller fraction of the decay heat. This situation is clearly true through the end of adiabatic heat-up. During reflood, however, positive insertion of reactivity occurs below the quench front. At these times the region of the core above the quench front is highly voided with void fractions approximating those of the third quarter of blowdown (toward the end just before steam only conditions). Local reactivities are similar to those during the later stages of blowdown and power decay rates would continue as they were during blowdown. Thus, for a span of time well past the time of peak cladding temperature at a particular position in the core, the power at that position is conservatively evaluated by holding the fraction of fission power to decay heat constant during the post blowdown period.

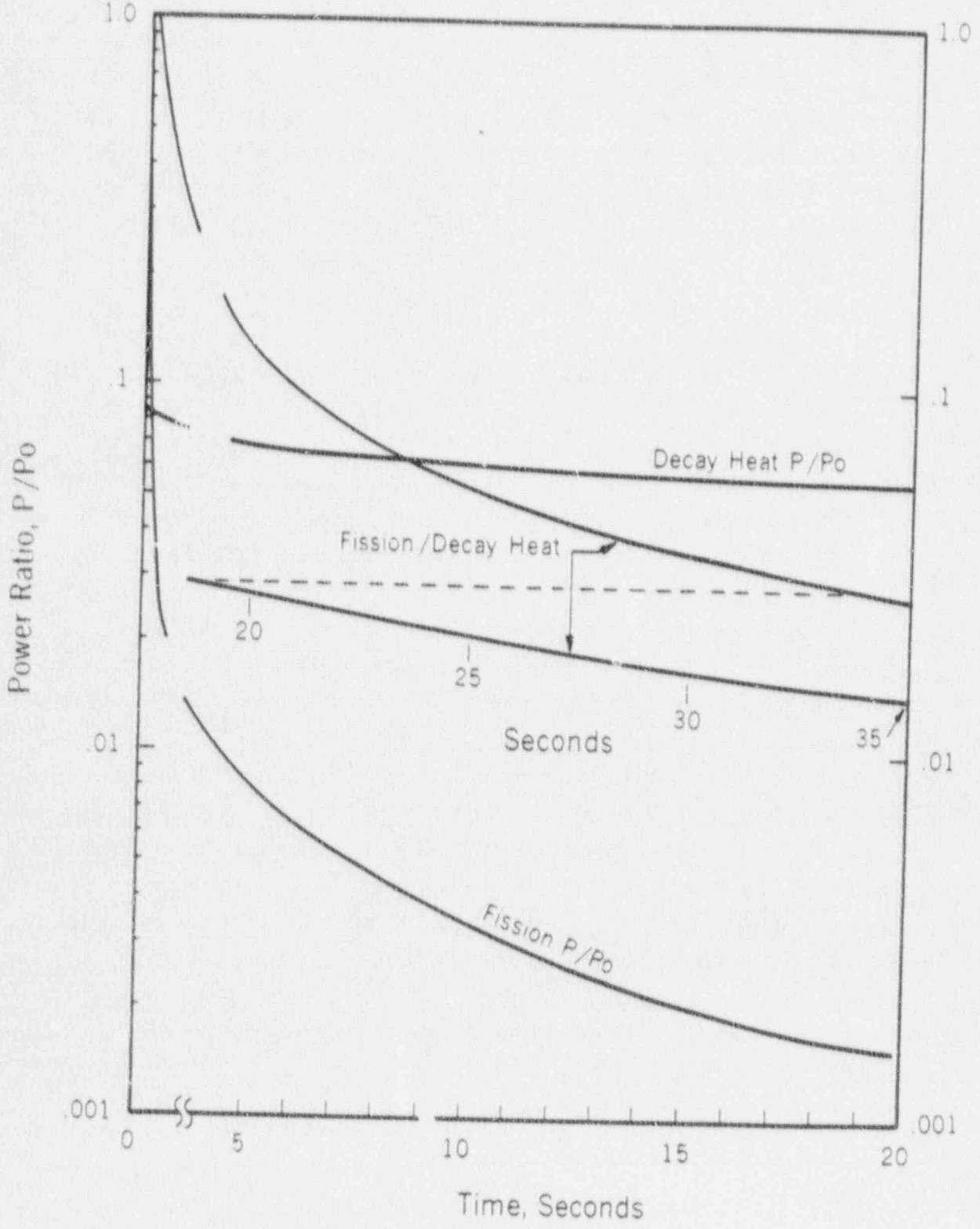
The portion of the core below the quench front experiences the following local reactivity effects: First the moderator void fraction is reduced to near zero causing a substantial positive insertion. Second, the coolant is highly borated water causing a substantial negative insertion. Third, the fuel temperatures are decreasing, causing a positive insertion. Finally, the control rods are now given credit within the evaluation model causing a negative insertion. The net effect of all of this is that the local k-infinity (which may have been .6 to .75 during adiabatic heatup) is

increased to the .9 to .95 range. This alters the fission power upward for the region below the quench front. The contribution of fission power to the region total power may increase by a factor of five or six. Before this can happen, however, the fraction of fission-to-decay heat power will have been substantially reduced. Extending the curves provided indicates that the actual fraction of fission power-to-decay heat would decrease by 50% in 15 to 20 seconds after blowdown and to 25% by 60 to 70 seconds. Thus the power level below the quench front will be somewhat under-predicted for about 80 seconds after the end of blowdown.

The degree of underprediction is not large and continues to decrease with time. The REFLOD3B code contains a conservative provision that more than compensates for the potential increase in power below the quench front: Steam generation below the quench front and liquid entrainment assume that the water is at saturated conditions, with no accounting for the fact that the injection temperature is actually quite subcooled. This is essentially a 10 to 15 percent increase in power below the quench front which will compensate for the potential refilling local power increases.

In conclusion, the approach to the post blowdown treatment of fission power selected by B&W is reasonable. Treating fission power as a constant fraction of decay heat is always conservative for the calculation of cladding temperature up to the time of quench which is well beyond the time of the peak in cladding temperature. For the core power as a whole the approach is less conservative and remains well within reasonable bounds.

Figure 4-1 Decay Heat and Fission Power for LBLOCA



5. Question: Section 4.3.2.5 of Volume I (LBLOCA) and Section 4.3.1.5 of Volume II (SBLOCA) discussed the power generated by the decay of actinides. Clarify whether the curve shown in Figure 4-4 includes the heat from the decay of neptunium and plutonium as required by Appendix K.

Response: Figure 4-4 includes the heating effects from the decay of actinides, including neptunium, plutonium, and isotopes of uranium.

6. Question: Section 4.3.2.7 of Volume I and Section 4.3.1.7 of Volume II stated that the fraction of gamma energy deposited in the fuel would be set at 95% based on the licensing calculations for Catawba in the plant final Safety Analysis Report. Clarify why this value would be applicable to other plants and the B&W fuel reload.

Response: Design differences between the B&W fuel and other fuel are small relative to the fraction of gamma energy deposited in the fuel. The fuel mass varies from one assembly design to the other by less than 3%, therefore having a negligible impact on the gamma energy deposition. The other significant differences related to plant and fuel design are the fluid conditions. During the event, the fluid changes from liquid to steam conditions. The water density for the liquid condition and the water density for the steam condition will also not be significantly different from the B&W fuel design to the Westinghouse fuel design and from Catawba to other plant designs. Therefore the 95% factor in the Catawba Final Safety Analysis Report is applicable to other plants and the B&W fuel reload.

7. Question:

a. Section 4.3.2.8 discussed the calculation of the initial oxide thickness on the outside and inside of the fuel cladding. Are these parameters calculated in TACO2? If not, clarify the source of this parameter in the EM methodology. Also, clarify the basis for the conversion factor of 390 $mg/dm^2$ = 1 mil used for determining the outside oxide thickness from the weight gain.

Response: TACO2 is a quasi-steady state fuel performance code that calculates fuel pin pressure and temperature distribution. It is not used to determine the initial oxide layers for the LOCA evaluations. Oxidation on the outside surface of the cladding is caused by reaction of the Zircaloy clad and oxygen from the primary coolant. It is directly proportional to the clad surface temperature during operation and the fuel assembly's in reactor residence time. The oxygen sources for oxidation of the inside surface are from moisture in the fuel pellet (fabrication) and from fissioning of $UO_2$. Figures 4-6, 4-7, and 4-8 of the evaluation model report, BAW-10168 provide the oxide values which are used in the LOCA evaluations. The basis for these figures is discussed in the response to part (b) of this question. The conversion factor of 390 $mg/dm^2$ equal to 1 mil is a mean value based on the data from page 44 of WCAP-3269-56. (The weight gain of

approximately 380 to 400 mg/dm$^2$ is equivalent to 1 mil).

b. The section contained references to the following documents: WCAP-3269-56, WAPD-107, BAW-1716 and BAW-1874. Provide the basis for using the data in these reports for the initial oxidation thickness calculation.

Response: WCAP-3269-56 and WAPD-MRP-107 contain the data from oxidation experiments conducted by W under simulated PWR operating conditions presented in Figure 4-6 of BAW-10168. WAPD-MRP-107 presents data on the weight gain of Zircaloy during corrosion experiments. WCAP-3269-56 provides data for outside oxidation obtained from experiments with electrically heated Zircaloy tubes emersed in water. The water temperatures and heat fluxes were characteristic of reactor operating conditions. The data for Figure 4-8 of BAW-10168 is from BAW-1716 and BAW-1874 and was obtained from the examination of fuel rods from the Oconee Unit 1 plant with exposures up to 50000 MWD/Mtu. The inside oxidation due to fissioning was obtained from measurements of defueled sections of cladding. Interior oxidation due to moisture in the fill gas, Figure 4-7 of BAW-10168, is taken from WAPD-MRP-107. These data are applicable to all PWR fuel rods using Zircaloy as a cladding material because the oxidation is primarily a function of cladding temperature and duration of power operation. All of the plant types covered by this evaluation model (B&W, W, and CE PWRs) are operate at approximately the same conditions so that exposure times are effective in determining the oxide thicknesses.

References:


7.1 E. N. Adud, WCAP-3269-56, "Corrosion and Hydriding of Zircaloy Under Heat Transfer Corditions," Westinghouse, August 1967.

7.2 WAPD-MPR-107, "Pressurized Water Reactor (PWR) Project-Technical Progress Report, Octobe' 24, 1963 - January 23, 1964,", Westinghouse, 1964, page 88.

7.3 C. G. Dideon and G. M. Bain, BAW-1716, "Fuel Performance Under Extended-Burnup Operation, B&W 15x15 Design," Babcock & Wilcox, September 1983.

7.4 L. W. Newman, BAW-1874, "The Hot Cell Examination of Oconee 1 Fuel Rods After Five Cycles of Irradiation," Babcock & Wilcox, October 1986.

8. Question: Sections 4.3.2.9 and 4.3.2.10 of Volume I and Section 4.3.1.9 of Volume II referenced B&W report BAW-10091 to show that the effects of heat transfer from primary piping, vessels, and internals and secondary to primary heat transfer were minimal for LBLOCA and SBLOCA, respectively. Because this report was for B&W plants, provide additional information or analysis to justify the applicability of the conclusions of the report to the plants listed in Table 1-1, Volume I. Also, what are the criteria used to determine whether or not to model a metal mass?

Response: The LBLOCA and SBLOCA models contain primary metal slabs to properly simulate heat transfer from these metals. The study in BAW-10091 demonstrated that heat transfer from primary metals is not sensitive to conditions of surrounding fluids because the transfer rate quickly becomes conduction-limited. Furthermore, the amount of energy released from the primary metals and the steam generators for LBLOCA is small compared to core decay heat and flashing. Because the thicknesses of metal slabs in B&W, W and CE plants are comparable, the conclusions from the referenced study are applicable to the other designs. The primary metal model used in the evaluations contains all metal within or in contact with the reactor coolant system (RCS) water. Attached small piping, ECCS piping, instrument lines, and metal attached to the RCS metal are considered to have little impact on the LOCA results and are not included in the model.

The steam generators are important for both LBLOCA and SBLOCA as large reservoirs that act as either heat sources or as heat sinks. The energy of the pool of secondary coolant and the auxiliary feedwater has a profound effect on the transients. Within reasonable limits, however, the secondary metal and flow geometries do not substantially impact on the results of either the LBLOCA or the SBLOCA.

For the LBLOCA, the secondary system acts as a heat source after the first several seconds of the transient. Although

unimportant during blowdown, the energy transport can easily vaporize and superheat the venting fluids during reflooding. That, in turn, worsens the steam venting and retards the plant flooding rates. During this period, the primary-to-secondary temperature differential is large and there is ample heat transfer area such that the details of the steam generator do not control the process. The only requirement is for the model to recognize and account for a large reserve of energy within the secondary system. The B&W evaluation model reflood simulation does this by setting the steam generator heat transfer coefficient conservatively high so that all incoming primary side fluid is vaporized and superheated to the secondary saturation temperature. Therefore, dependency on modelling detail and nodalization is removed from the simulation.

Surface heat transfer coefficients for the primary metal and the steam generators are selected to be representative of the heat transfer regimes encountered. Although no attempt is made to incorporate conservatism per se, the calculations of heat transfer can not be made to produce more conservative conditions because of the large energy supply and the heat transfer from the steam generator secondary fluid described above. The selected film coefficients around the loop are: (1) For primary metal (not fuel) within the reactor vessel upstream of the top of the core, a film coefficient of 200 btu/hr-F-ft$^2$ is used for liquid regions and 20 btu/hr-F-ft$^2$ for steam blanked regions. The 200 btu/hr-F-ft$^2$ is reasonable to high for pool boiling in liquid under low flow conditions. The 20 btu/hr-F-ft$^2$ is reasonable for convection to vapor at low flows. (2) The film coefficient for the upperhead is set at 100 btu/hr-F-ft$^2$. The upperhead is a relatively stagnate steam only region and not a significant contributor to the solution. 100 btu/hr-F-ft$^2$ is quite high for a convection to vapor coefficient in this area. (3) A film coefficient of 1000

btu/hr-F-ft$^2$ is applied in the reactor coolant loops, hot and cold legs, and the steam generators for both the thick metal and tube surfaces. These regions experience a relatively high velocity flow of mixed vapor and liquid droplets. The 1000 btu/hr-F-ft$^2$ is high for the flow film boiling regime expected.

Although of substantially lower importance than vapor superheating, the amount of tube plugging modelled should be higher than that applicable to the plants to be covered by the analysis. At 10 to 20 percent plugging a difference of a few percent is not consequential, but differences of 5 percent or more will effect the results. The modelling within the B&W evaluation model for these parameters assures an appropriate and representative secondary inventory, sets a conservatively high reflooding secondary heat transfer coefficient, and employs a degree of tube plugging with selected margin.

For the smaller SBLOCAs, the steam generator acts as a heat sink. If the break flow cannot remove sufficient energy to keep the plant below the secondary pressure, the steam generator will absorb heat through steam condensation to maintain the primary system at a pressure near that of the secondary system. The available heat transfer area in the generator is large and the required heat transfer is small, so that the only substantial requirement on the modelling is to provide for the secondary side as a large reservoir of water. Of substantially lower importance is the action of the steam generators as heat sources for larger SBLOCAs and the flow restriction offered by tube plugging. The primary coolant flow rate through the steam generators is not high at any time that there is a potential for core uncovery, and only small frictional differential pressures result. The alteration of these pressure drops by a few percent because of the effect of tube plugging will not alter the static

heads of liquid within the system. Similarly, because the flow through the steam generators is steam, at any time at which the core may be uncovered, the action of the steam generator as a heat source is to superheat the steam and not to vaporize entrained liquid. This effect should be modelled, but it does not have the importance of either the heat sink effect for SBLOCA or the heat source effect on reflooding for LBLOCA. The modelling within the B&W evaluation model for these parameters, is the same as that for the LBLOCA and assures appropriate and representative treatment.

For both large and small breaks, metal masses for the secondary side are included in the modelling. The steam generator tubes and the tube sheet are modelled with the primary system. Table 8-1 provides representative numbers for the initial, steady-state, energy content of the three major secondary metal structures (the shell, the downcomer shroud, and the steam separators) in contrast to the total energy of both the primary and secondary systems. The total energy of the secondary side metal is only 10% of the total system energy. Of this 9% is associated with the thick SG shell metal and only 1% with thin secondary metals. The thin metal is further isolated from the primary system because it is almost all, 99%, the steam separators and located in the steam dome of the generator. At this location it can act to superheat steam but cannot directly supply energy back to the primary system. Therefore, the modelling of the secondary side metals is not crucial to the prediction of the peak cladding temperatures for either large or small break LOCA. At a minimum, representative values for the shell metal, appropriate for the steam generator designs used by the plant being evaluated, will be included in the secondary side modelling for LOCA calculations.

The text within the evaluation model report that could be taken to mean that the secondary system is not important to the course of the LOCA transients will be modified to reflect the discussions in this response.

Table 8-1 Initial Energies of Secondary and Primary Systems

| System | Initial Energy * | % of Total |
|---|---|---|
| Secondary Shell | 59.0 x $10^6$ Btu | 9 |
| Secondary Downcomer Shroud | 0.1 x $10^6$ Btu | 0 |
| Steam Separators | 7.0 x $10^6$ Btu | 1 |
| Secondary Fluid | 141.3 x $10^6$ Btu | 21 |
| Primary System ** | 467.6 x $10^6$ Btu | 69 |

* For the purposes of this table the reference conditions have been set at 228 F and 20 psia which is saturation temperature and pressure corresponding approximately to the containment conditions during reflood for a ice condenser containment.

** This value includes an approximation of the contribution of fission and decay heating up to the time of peak cladding temperature. The actual value for a LBLOCA could be lower by about 15 x $10^6$ Btu and the value for a SBLOCA higher by about 35 x $10^6$ Btu.

9. Question: Sections 4.3.3.1 and 4.3.3.2 discussed the models used in complying with the Appendix K requirements of not under predicting the cladding swelling and rupture. Reference was made to Section 2.3.2 of BAW-10164P and NUREG-0630.

a. Describe how the core flow blockage due to clad swelling, prior to rupture, is modeled.

Response: Flow blockage due to prerupture cladding strain is not modeled in the evaluation model during blowdown because the added flow resistance is small and has no significant impact on channel flow. Prerupture strain could be as high as 20 percent, but the resulting obstruction has a smooth area change with gradual contraction and expansion. The associated resistance would contribute only a small fraction to the local fuel assembly resistance. The incidence of swelling will be distributed statistically within about a 2 foot span centered about the elevation of highest power. Thus the minor amount of subchannel flow diversion that might occur would be quickly recovered.

The effect of rupture-induced flow blockage on the post blowdown core heat transfer has been shown to be a positive one. As shown in the response to question 13 of the first round of BEACH (BAW-10166) questions, blockage tests run in the FLECHT-SEASET facility show that the overall heat transfer in the vicinity of the rupture is improved while no effect is observed at other locations in the assembly. Although the tests were for a degree of strain representative of post rupture conditions, there is no reason to believe that the effect with prerupture levels of strain would be different except that it would be reduced.

The full effect of prerupture strain is modeled in the hot channel heat-up analysis. This includes gap dimension changes, reconsideration of pellet-clad contact, and clad thinning.

b. Provide the reference for the source of the model and associated data tables used to calculate the unruptured elastic and thermal deformation (Mode 1) described in Section 2.3.2.1 of BAW-10164.

Response: The elastic and thermal deformation model (mode 1) is identical to the model in RELAP5/MOD2 cycle 36.04 except for the clad radius overspecification factor, $U_{cc}$, in Equation 2.3.2-19 of BAW-10164, the RELAP5/MOD2-B&W topical. That factor was introduced to maintain consistent fuel-clad gap and clad dimensions between the RELAP5 and TACO codes. The data for thermal strain and Young's modulus of elasticity in RELAP5/MOD2-B&W are the same as in RELAP5/MOD2 - Cycle 36.04.

10. Question: Section 4.3.3.2 discussed two approaches for determining the initial fuel rod internal pressure. What criteria will B&W use to determine which approach to be used in the EM analysis? Will justification of the chosen approach be provided on a plant specific basis? If the bounding rod pressure approach is taken, how is the bounding pressure selected and what are the bases for selecting this pressure? Also, since fuel pellet conductivity increases with increasing pressure, is the effect of the higher pressure on fuel pellet conductivity considered in the process to determine which approach is taken to select the initial rod internal pressure?

Response: The burnup sensitivity study in section A.3.9 of BAW-10168, showed that the results of LOCA become more severe with increasing initial fuel pin temperature and increasing fuel pin pressure. Furthermore, for the range of burnups considered, the fuel temperature effect was dominant, with the result that the most constraining burnup could be specified as that at which the highest initial fuel temperatures occur. The B&W evaluation model specifies that the combination of burnup dependent fuel parameters used in the LOCA evaluation shall be equal to or more conservative than those that produce the highest peak cladding temperature when realistically combined. From the study in section A.3.9, the minimum combination occurs at 100 megawatt days per metric ton.

The concept of a bounding internal pin pressure is used to develop an envelope for fuel design and for quick validation of the fuel rod design parameters with each reload. One implementation of such a concept would be to set the fuel temperature and oxide thicknesses at their worst case values and use a pin pressure that exceeds the worst case value, say that which can occur at the end of life. So long as the

conservatism in pin pressure is not overly restrictive on the evaluation results, this may be a reasonable way to provide design flexibility. The decision as to whether or not to use a bounding pin pressure will be made based on the results of the evaluation and the potential for constraining plant operation. The nature of the approach and justification for the values selected will be documented in each application of the evaluation model.

11. Question: Section 4.3.3.4 discussed the approach taken by B&W to model the thermal properties for the fuel and cladding, and referenced several sections in the RELAP5/MOD2-B&W and FRAP-T6-B&W code manuals which indicated the user can input the thermal properties for the fuel and cladding or use built-in tables for these properties. Which approach will B&W use in EM calculations with RELAP5/MOD2-B&W and FRAP-T6-B&W: If user input values are used, will the data be justified in the plant specific submittal? If not, provide the data to be used and justify its applicability to all plants to be analyzed.

Response: In order to provide sufficient flexibility to deal with plant-specific conditions, fuel design changes, and refinements to materials properties, the B&W evaluation model controls materials properties by allowing them to be determined on a plant-specific basis while specifying criteria on how the determination shall be made. Properties are to be selected to be consistent with reference values to such an extent that any differences will not cause a substantial deviation in LOCA results (a substantial difference might, but can not always, be expressed as a change in result of peak cladding temperature by the amount reportable according to 10CFR50.46). This agreement is to be established by comparison to accepted reference data sources, and the bases rationale behind the property values used documented with the calculations. Therefore, the selection of any given property value is subject to the independent quality assurance process just as are other plant parameters. By including the values and bases with the documentation of the calculation, both are auditable against the criteria established within the evaluation model.

Specific to the question, the property values used in the calculations contained in the evaluation model topical report (BAW-10168) and that will be used in the application of the EM to the McGuire and Catawba plants have been derived from a number of sources. These include the "Hand Book of Chemistry and Physics", B&W internal sources, national data banks, etc. In order to provide a basis for these values they were individually compared to the defaults available in FRAP-T6-B&W and RELAP5/MOD2-B&W and to the values available in the current revision of MATPRO, NUREG/CR-0497[11.1].

References:

11.1 MATPRO-Version 11(Revision 2) A Handbook of Materials Properties for Use in the Analysis of Light Water Reactor Fuel Rod Behavior, NUREG/CR-0497, TREE-1280, Rev. 2, August 1981.

12. Question: Section 4.3.4.1 of Volume I and Section 4.3.2.3 of Volume II indicated that the break flow model includes a transition phase between the extended Henry-Fauske and Moody models to avoid step changes in break flow. In the transition phase, the break flow is calculated using linear weighting between the extended Henry-Fauske model at zero quality and the Moody model at 5% quality. Clarify the basis for choosing 5% quality to complete the transition to the Moody model. Are the calculated results sensitive to the choice of this value?

Response: The smoothing technique used in RELAP5/MOD2-B&W in the transition phase between the extended Henry-Fauske and the Moody models is different from that described in Section 4.3.4.1 of Volume I and Section 4.3.2.3 of Volume II of the EM topical report BAW-10168. In the transition phase between the two models an underrelaxation of the velocity is used as given by the equation 2.1.4-47 of the RELAP5/MOD2-B&W topical report BAW-10164. This method is consistent with the underrelaxation scheme used with the Ransom-Trapp model (equation 2.1.4-35 in BAW-10164). The transition zone is determined by either the quality, as defined on page 2.1-109 of BAW-10164, or the void fraction. For the analyses reported in the EM topical report, a 5% quality criterion (e = 0.05 in the equation on page 2.1-109 of BAW-10164) was used. The 5% quality for the transition zone worked satisfactorily in the studies reported in the EM topical report BAW-10168. The results should not be sensitive to this value because the break flow regime passes through this transition zone very quickly.

The recommended value for this quality has been reduced to 0.001 for the future analyses because the extended Henry-Fauske and Moody Tables were made continuous as described below. This value was used in the simulation of the

Semiscale MOD1 Test S-04-6 and the Semiscale MOD-C Test S-LH-1 and the results were submitted to the NRC as the response to questions 12 and 17 of the first round of questions on the RELAP5/MOD2-B&W topical report BAW-10164.

The extended Henry-Fauske and Moody tables used in RELAP5/MOD2-B&W were obtained from RELAP4/MOD5[12.1] (Tables VI and VII in the RELAP4/MOD5 code manual Volume 1). From pages 163 and 281 and Figure 34 of the RELAP4/MOD5 code manual Volume 1 the following conclusions are made:

1. A small discontinuity exists between the extended Henry-Fauske and the Moody models at the saturation boundary.

2. In order to make a smooth transition between the two models the first point in the Moody table (Table VI of Reference 12.1) has been placed in the last position of the extended Henry-Fauske table (Table VII of Reference 12.1).

It can be seen form the tables given in Appendix C of the RELAP5/MOD2-B&W topical report BAW-10164 that the first point of the Moody table and the last point of the extended Henry-Fauske table are the same. Therefore, in the future analyses a small quality value of 0.001 will be used to maintain the continuity in the transition between the extended Henry-Fauske and the Moody critical flow models.

References:

12.1 RELAP4/MOD5 : A Computer Program for Transient Thermal-Hydraulic Analysis of Nuclear Reactors and Related Systems, ANCR-NUREG-1335, September 1976.

13. Question: Section 4.3.4.2 indicated the criterion used to determine the end-of-bypass as being the end-of-blowdown. While many of the forces responsible for ECC bypass are minimized at the end-of-blowdown, it is not clear that all of them are reduced to the point where ECC could penetrate the downcomer at this time for all LBLOCAs. Appendix K requires the end-of-bypass definition be justified by a suitable combination of analysis and experimental data. Provide additional information to justify that this definition of the end-of-bypass is applicable to all LBLOCAS and plants to be analyzed.

Response: Using the end-of-blowdown (EOB) as the definiti of end-of-bypass is conservative and applicable to all LBLOCAs and plants to be covered by this evaluation model. The B&W evaluation model for B&W designed NSS applies the same definition at any time that the downcomer velocity-based bypass criteria would produce an end-of-bypass time later than the end-of-blowdown (EOB) time. After blowdown there are no forces in the system, regardless of location, that can cause the expulsion of ECCS fluid out of the break. The ECC water may be held up, either in the cold leg pi, es and/or the upper downcomer, depending on steam flows but it cannot exit the system. If the ECC water is held up in higher regions of the system at EOB it falls into the lower reactor vessel during adiabatic heatup. Furthermore, the conditions in the upper downcomer at the EOB are relatively quiescent and the actual downcomer penetration by part of the ECCS fluid is expected several seconds prior to the EOB.

14. Question: Section 4.3.4.5 of Volume I and Section 4.3.2.6 of Volume II discussed the RELAP5/MOD2-B&W pump model and pump input for LBLOCA and SBLOCA, respectively. For those cases where the reactor coolant pumps (RCPs) are not assumed to trip, pump performance is an area where code input added uncertainty to the calculated PCT. How do you ensure that the pump inputs (homologous curves, two-phase pump head multipliers, etc.) in the plant licensing calculations provide conservative results.

Response: As with the primary system flow distribution and piping resistance network, the reactor coolant pumps are modelled on a best estimate basis within the evaluation model. The two-phase degradation is set in the conservative direction. Variation of the reactor coolant pump performance within a reasonable range of the expected performance does not have a strong effect on the peak cladding temperature for large break LOCA and has no effect on small break LOCA. With the direction of variation that would achieve conservative results for large breaks somewhat uncertain, the most reasonable approach to assure proper results by the evaluation model is to employ best estimate or nominal input values without attempting to create substantial forced skewing toward conservatism.

The performance of the reactor coolant pumps has been shown to have only a slight impact on the results of large break LOCA evaluations. Two phase effects on the pumps make some small differences. In BAW-10091[14.1], the sensitivity of a B&W NSS to a wide range of two phase degradation was shown to be low. The difference between no degradation and bounding degradation was shown to have only a 200 F impact on peak cladding temperature. Reasonable variations of the degree of degradation will only change the cladding temperatures by 20 to 30 F. For the study performed in the

B&W evaluation model for recirculation steam generator plants, BAW-10168, the results are much the same. Here varying the degree of degradation from lower to upper bounds effected the cladding temperature by less than 100 F. Again reasonable variations would be in the 20 to 30 F range.

The effect of tripping the pumps has been studied for B&W plants and other NSS designs with inconclusive results. Therefore, the effect off-site-power availability or reactor coolant pump trip will be evaluated by a sensitivity study for each plant type covered by the evaluation model at the time of its first application to that plant type.

For small break LOCA, all pressurized water reactors control the reactor coolant pump status by automatic pump trip mechanisms or operator procedures. The reactor coolant pumps are tripped early in the accident and costdown to become inactive long before the critical stages of the LOCA. The effect of pump parameters and variations of parameters is thus inconsequential to the results of small break LOCA evaluations.

The inputs for the reactor coolant pumps are measured values for scaled pumps and obtained from pump vendor homologous curves or zone maps. If such information is not available, information from similar pumps of similar specific speeds is used. This is a standard approach to pump evaluation within the industry and provides best estimate parameters for the pump inputs.

References:

14.1 B. M. Dunn, L. R. Cartin, et. all, B&W's ECCS Evaluation Model Report with Specific Application to 177 FA Class Plants with Lowered Loop Arrangements, BAW-10091, August 1974.

15. Question: Section 4.3.4.8 discussed the CHF correlations to be used in the B&W core model. For the high flow regime, the BWCMV, Barnett, and modified Barnett correlations were listed as those to be used in EM calculations. Clarify whether other correlations such as B&W-2 and BWC will be used in EM calculations.

Response: As stated in Section 4.3.4.8, the CHF correlations that will be used for EM calculations are as follows.

| | |
|---|---|
| High Flow Regime: | A combination of BWCMV, Barnett and modified Barnett. |
| Low Flow Regime: | A combination of MacBeth and Griffith correlations. |

For all regimes a minimum CHF value of 90,000 BTU/hr-ft$^2$ will be used. The user input multipliers for all these correlations are 1.0. There are no plans at present to use either B&W-2 or BWC as CHF Correlations in the evaluation model.

16. Question:

a. Section 4.3.6.2 discussed the EM methodology approach to calculating thermal-hydraulic phenomena during the reflood phase by referencing the REFLOD3B code manual which has several modeling options available for calculating carryout rate fraction and core heat transfer coefficients. Clarify which options will be used to calculate these phenomena for EM analyses and justify how they meet Appendix K requirements.

Response: For EM applications during the r[illegible] period the core heat transfer is calculated [illegible] CRFCKN option as described in the REFLOD3B code [illegible] report BAW-10171.

When this option is selected the core heat transfer calculated using the built-in FLECHT and [illegible] correlations described in the Appendix F-1 of the code topical report. These correlations were developed based on the FLECHT tests (References 11 and 17 in BAW-10171).

The carryout rate fraction is calculated using the CRFCKN correlation as described in the Section F-3 of the code topical report. The CRFCKN correlation was developed using selected FLECHT-SEASET (References 19 and 20 in BAW-10171) cosine power tests and FLECHT cosine and skewed power tests (References 21 and 22 of BAW-10171). During the early reflooding period, a smoothing technique, as explained in the Section 2.7 of the code topical report was used to avoid non-physical oscillations in the thermal-hydraulic system behavior.

The applicability of the core heat transfer and the carryout rate fraction correlations are further demonstrated by the simulation of the FLECHT-SEASET gravity feed test 33338.

b. Section 4.3.6.2 stated that the values of the fuel conductivity and gap conductance, which are user input and held constant during the refill and reflood phases, will be chosen conservatively to maximize heat removal from the fuel. Clarify if the values to be used in EM calculations will be justified on a plant specific basis. If not, provide additional information and basis to justify the conservatism in the values used for these parameters in the EM analysis on a generic basis.

Response: The fuel-to-cladding heat transfer is calculated using a lumped parameter equation (Eq. 2-76 in the code topical report). The values of fuel and cladding conductivities and of the gap heat transfer coefficient are selected such that the overall conductance between the fuel and the cladding is on the conservative side within the expected thermal conditions of the average fuel rod during reflood. Bounding values are generally selected to cover all the plants, but they will be justified for the plant-specific applications. It is to be noted that added conservatism, all of the core heat transfer is used to generate steam from the saturated liquid conditions, is inherent in REFLOD3B.

17. Question: Section 4.3.6.3 indicated that the effects of a accumulator gas discharge after the accumulator water was depleted could be neglected in LBLOCA analyses because the only effect was a momentary increase in the downcomer pressure and core flooding rate. However, studies based on Semiscale data (NUREG/CR-2509, "Summary of the Semiscale Program (1965 - 1986)," July 1987) indicate other effects, including a long term increase in the system pressure. This pressure increase has opposing affects on the reflood period, i.e., higher flooding rates but reduced low pressure injection rates. The gas discharge could also affect the injection section pressure difference. Provide additional information to justify neglecting the discharge of the accumulator gas or discuss how the effects of accumulator gas discharge will be included in the analyses.

Response: The injection of accumulator nitrogen into the intact loop cold leg causes degradation of condensation of steam on the highly subcooled ECCS water. This non-condensed steam and the nitrogen has to flow out of the system through the vessel side break, and it causes the system pressure to increase. The sudden decrease in condensation will increase the downcomer pressure, producing higher core flooding rates. The higher core flooding rates increase the core steam generation rate, also causing an increase in the system pressure. Therefore, the net effect of injecting the accumulator nitrogen is to increase the system pressure. This conclusion is consistent with the semiscale Mod-1 test results given in NUREG/CR-4945 (EGG-2509, the NUREG number given in the question is incorrect).

As stated in the question and also by Loomis in NUREG/CR-4945, the increase in system pressure can have opposing effects: an increase in flooding rate and decrease in the

ECCS flow rate due to the increase in the system pressure. However, the pump injected flow rate is not very sensitive to the changes in the back-pressure near the reflooding pressure conditions. An earlier B&W study[17.2], using the REFIOD3 code, showed that the net effect of a reduction in condensation was to increase core flooding rate. Since the primary effects of the accumulator nitrogen injection are a reduction in condensation and an increase in the system pressure, the net effect will be an increase in flooding rate. Therefore, the result of neglecting the accumulator nitrogen injection is conservative for LOCA EM application.

References:


17.1 G. G. Loonis, Summary of the Semiscale Program (1964-1986), NUREG/CR-4945, EGG-2509, July 1987.

17.2 J. J. Carbajo and N. H. Shah, "Effect of ECCS Steam Condensation on Reflooding Rates," Trans. Am. Nucl. Soc., 35, pp 325-326, 1980.

18. Question: Section 4.3.6.4 stated that steam/water interaction during the reflood phase of the accident would be accounted for by a differential pressure of 0.85 psi in the cold legs of the REFLOD3B model. Provide the basis for this value and justify its applicability to all plants to be analyzed. Will the option in RELFOD3B that allows the user to change this differential pressure after the accumulator empties be used in licensing calculations? If so, provide the value, the basis for its selection, and justify its applicability to all plants to be analyzed.

Response: As stated in Section 4.3.6.4, the momentum loss due to steam water interaction in the cold legs during the reflooding period is accounted for by inputting a differential pressure of 0.85 psia for the junction near the injection location. In the sensitivity studies reported in the EM topical report, the same value was used both during the accumulator injection period and during the pump injection period. However, from the assessment of the Westinghouse 1/14 scale, CE 1/5 and 1/3 scale 90 degree angle injection tests[18.1-3] the following conclusions are made.

1. 0.85 psia is appropriate value during the accumulator injection period.

2. During the pump injection period a reasonable value for the momentum loss is 0.5 psia.

Therefore, in the future analyses of recirculating steam generator plants with 90 degree injection angle the values given above will be used for the momentum loss instead of a constant value of 0.85 psia. This change will be made in the next revision to the EM topical report.

This change will have no impact to the conclusions made from the analyses made earlier. The flooding rate will increase slightly after the accumulators are emptied due to the decrease in the value for the momentum loss near the ECC injection location. However, the parameter effects evaluated from the sensitivity studies has minimal dependance on the value for the ECC momentum loss. Therefore, the conclusions made from the earlier studies will be valid for future applications.

References:


18.1 C. P. Lilly, et al., Mixing of Emergency Core Cooling Water with Steam, EPRI-294-2, January, 1975.

18.2 J. R. Brodrick, et al., 1/5 Scale Intact Loop Post-LOCA Steam Releaf Tests, CENPD-63, Rev.1, March 1973.

18.3 W. E. Burchill, et al., Steam Water Mixing Test Program Task D: Formal Report for the Task B and Final Report for the Steam Relief Phases of the Test Program, AEC-C00-2244-1, CENPD-101, October 1973.

19. Question: With regard to the use of the BEACH code to calculate reflood heat transfer coefficients (Section 4.3.6.5), clarify whether the code logic and models only allow steam cooling and take into account flow blockage due to rod swell or rupture for flooding rates of less than 1.0 in/s as required by Appendix K.

Response: The BEACH code meets the requirements of 10 CFR 50.46 Appendix K for reflood heat transfer at flooding rates of less than one inch per second. Direct heat transfer above the quench zone, as described in sections 2.2.2.8, 2.2.2.9, and 2.2.2.10 of the BEACH topical BAW-10166, occurs only between the cladding and the vapor. Radiation heat transfer is allowed from the cladding to vapor for single phase flow and from the cladding to both vapor and suspended liquid for two phase flow. Interphase heat transfer between vapor and the liquid is allowed under the conditions of two phase flow.

These limitations are applied until the cladding approaches the quench temperature. As quenching occurs and afterward the heat transfer package realistically models direct heat flow to liquid even with the flooding rate less than one inch per second. It is B&W's position that the provision of steam only cooling contained in Appendix K is to be applied at the location of the peak cladding temperature and not to the determination core boundary conditions used to specify the fluid state at the peak. Thus, the use of direct liquid cooling in the lower regions of the core after those regions have peaked in temperature and cooled substantially does not violate Appendix K requirements.

Accounting for flow blockage due to rod swelling and rupture for flooding rates less than one inch per second is made by not including the positive effects of rupture on the heat

transfer at or near the location of rupture. The effects of rupture on the heat transfer in the region of rupture can be seen in one of the additional benchmarks of the BEACH code supplied in the answer to NRC question 13 on the BEACH code topical report, BAW-10166. From this case it is obvious that the heat transfer in the region of the ruptured cladding is improved by the occurrence of rupture and remains essentially unchanged away from the region of the ruptures. It is also obvious that the applying BEACH without direct inclusion of rupture effects conservatively evaluates the resultant heat transfer for the fuel pin.

The formulations within the BEACH code not only adhere to the requirements but combine to produce a model that performs in excellent agreement with the available reflood heat transfer data. The set of benchmarks for the BEACH code includes a wide range of fluid conditions, symmetric and asymmetric axial power shapes, two experimental facilities, variation in fuel assembly power, variation in radial power effects, and sensitivity to the occurrence of rupture. In all cases the code results achieved are characterized as best estimate to conservative. Reference should be made to the BEACH topical Appendix C and to the response to NRC question 13 which contain comparisons to the data for each of the benchmarks. Recognition that the location of the peak cladding temperature lies in the region of a rupture, for any peak temperature approaching the limit of 10 CFR 50.46, provides assurance that the code as used conservatively predicts heat transfer at the peak. Therefore the code both meets the requirements posed by 10 CFR 50.46 and does so in a clearly conservative way.

20. Question: In discussing the approach to calculating the maximum local cladding oxidation to meet Criterion 3 of 10 CFR 50.46, Section 5 stated "For the ruptured location no gap in the cladding is considered. The consideration of such a gap for the ruptured location would be likely to increase the cladding thickness due to the necessity of maintaining a cladding mass balance." Clarify what is meant by "a gap in the cladding" and how the consideration of this gap and the associated cladding mass balance would add to the cladding thickness.

Response: The statements in section 5 in regard to the geometry of the cladding at and after rupture and the existence of a gap in the cladding are unclear. B&W will use the differential strain to establish the post rupture cladding dimensions. Section 5 will be revised to delete any discussion of gaps in the cladding and resultant conservatism.

21. Question:

a. Section 6 stated that the 10 CFR 50.46 criterion on core wide oxidation was met using a formula that extrapolated the results of the hot rod calculation to the whole core. In the formula, the oxidation in the average core at an elevation was set equal to the oxidation of the hot rod times the ratio of the average core "adjusted" power to the hot rod "adjusted" power, all at the same elevation. Since the "adjusted" power is calculated by subtracting the minimum power required for oxidation to occur from the local power, the ratio of the "adjusted" powers is lower than the ratio of the "unadjusted" powers. Provide additional information to justify use of the adjusted power ratio.

Response: The assumptions upon which the technique for power adjusted oxidation is based are:

1) Below a certain local power the cladding temperature will not increase sufficiently nor remain high long enough to incur significant local oxidation.

2) The degree of oxidation that occurs at powers above the threshold can be bounded by a straight line set to intersect zero oxidation at the threshold power and the oxidation observed in the peak temperature calculation at the same local power.

For each elevation within the core a curve of oxidation versus local power is constructed that gives zero oxidation for local powers between zero and the threshold power and gives a linear increase to the hot

channel oxidation for powers above the threshold. The amount of oxidation at any location in the core can be obtained from this curve when the local power and elevation of that location are known. Totalling the local oxidation over the core gives the whole core oxidation.

The information used to construct and confirm this approach is taken from the hot channel FRAP-T6-B&W evaluation for the LOCA case for which the core-wide oxidation is being determined. This run, by nature, calculates the hottest, most oxidized axial channel in the core. The model consists of 21 axial nodes each approximately 0.57 feet high. Each node is checked for oxidation, and the threshold is set at a power below that of the lowest power node that produced significant oxidation increase. In the technique this value is not elevation dependent and is generally set by one of the uppermost nodes. The other point through which the curves are drawn comes directly from the results of the hot channel analysis.

That a linear approach is valid comes from the nature of the equation governing the oxidation rate. The temperature history relationship between two locations within the core that are at the same elevation but differ in radial position can be reasonably determined by power ratio. The equation for oxidation rate is exponential with temperature and thus oxidation rate is by extension exponential with power. Therefore the actual function of local oxidation versus local power is a curve that departs from a zero value at the threshold power. The straight line approximation used here represents a chord to that curve and will always overpredict the amount of oxidation.

This approach to the calculation of local oxidation simplifies the determination of corewide oxidation but adds conservatism. A reasonable estimate of the amount of conservatism inherent in this approach is about 50 percent.

b. It is stated that an integration over the core will give the core wide oxidation increase or the amount of hydrogen generation. Clarify how the integration is performed.

Response: As outlined in response to the first part of this question, a function of local oxidation versus local power is determined for each of 21 elevations within the core. From core power maps, the radial peaking for each fuel assembly within the core can be determined. In general, however, radial power maps will not show sufficient radial peaking to place the plant at its LOCA limited peaking, and a way must be found to push such shapes. The most conservative approach is obvious and beyond reason. Here the fraction of the core required to meet plant power is all considered to be at the maximum local power allowed by LOCA and the rest of the core is considered to be at zero power. Clearly, the distribution cannot become worse, and, just as clearly, the distribution is erroneous. The approach selected for the evaluation model is to start with a realistic power distribution and to gradually force power from the lower power zones into the higher power zones until the radial power profile combines with the axial profile for the case to give the LOCA limited power. This gives a new radial power map that, combined with the axial profile of the hot channel analysis, will integrate to the whole core

power while at the same time peaking the local power to provide a conservative degree of oxidation.

The final step is merely to assign 21 local degrees of oxidation to each fuel assembly from the 21 curves of local oxidation versus local power curves. The oxidation for each assembly in the core can be determined by adding the local values for the assembly and the oxidation for can be determined by adding all assembly values.

The determination of radial and axial power profiles involves some degree of conservatism but it is not as easily identified as that for the local oxidation versus power curves. When the core is realistically pushed to achieve high peaking, the radial and axial power shapes invariably have increased peaks, but the power fall off away from the peaks becomes more drastic. Although some transient with high peaking might be identified, none of them approach the LOCA limiting conditions. Pushing these cases in the manner described creates peaking that is considerably broader in the vicinity of the peak than is expected should a core approach the LOCA limit. This broadening in the region of higher powers produces conservatism. An estimate of the margin provided is difficult to quantity but it may approach 50 percent.

22. Question: In order to ensure that long term cooling can be established, Section 8 stated that the last calculation of the ECCS Evaluation Model is the demonstration that the timing of the operator action to assure core throughput flow is early enough to prevent boric acid crystallization within the core. Describe how the timing of this operator action is calculated.

Response: The timing of operator action to realign ECCS to supply water to the RCS hot legs is approximately 17 hrs. This action will provide a throughput flow and prevent boric acid crystallization for cold leg breaks. The timing is specified in plant technical specifications or in the operating procedures. Because the problem of particulate concentration is dependent on system design and reactor power and not dependent on fuel design it will generally be true that the previous basis for the technical specifications will remain valid for the BWFC fuel. In such cases a reference to the currently approved previous calculations will be provided. In the event that a customer wishes to alter a technical specification that might affect the timing of operator action, power level change or change in the ECCS systems, a calculation of the time dependent concentration of boric acid will be done to confirm or set the timing of operator action.

The following is an excerpt from the response to question 57 of this set which describes the concentration process.

> For plants with cold leg injection, the long term cooling mode following a cold leg break is one of extended core boiling. ECCS water is drawn from the reactor building sump, cooled, and injected into the plant cold legs from which it flows to the downcomer. At the downcomer, however, the hydrostatic balance between the core and the downcomer prevents most of the injection water from flowing to the core. Only that amount of water necessary to match the boiling flows to the core; the rest flows

into the broken cold leg, out of the break, and back to the sump. Water in the core is boiled, and steam is passed through the loops to the break and the containment where it is condensed by the building cooling system and returned to the sump as water. The procedure cools very well but can pose a problem. Boric acid does not vaporize at the same rate as steam, and most is left in the solution. (While most calculations assume all boric acid is held in solution, a good portion actually does vaporize.) This causes a gradual buildup in concentration of the boric acid within the core region. The process cannot start for the case of hot side breaks as the ECCS always flows through the core for those events and the distillation process never starts.

Although the solubility saturation limit for boric acid is considerably above the solute concentration of the injection water, the process, unchecked, could cause high enough concentrations to lead to precipitation of borate crystals. The injection solute is about 1.22 weight percent boric acid (this number will vary by plant) and the solubility limit at 212$^{o}$F is slightly over 35 percent. Figure 57-1 shows the saturation solubility of boric acid as a function of temperature. The figure is derived from reference 57.1 and was presented as part of the B&W evaluation model for B&W designed NSS in reference 57.2, BAW-10091 Supplement 1 page 3-90. The most common procedure is to establish a small amount of hot leg injection which, once larger than the core boiling rate, will cause the flow in the vessel to reverse and proceed from the core through the downcomer and out the break. The flow from the core to the downcomer will be at the core concentration and act to remove boric acid from the system. This leads to the eventual control of the concentration process and gradual decrease in the core concentration of boric acid.

To calculate the effect of this process a simple two-step concentration calculation is done. The first step initiates at the time of the accident. The mixing region is taken as the core and the upper plenum up to an elevation equal to the bottom of the cold leg pipe. The concentration of boric acid is considered uniform within the mixing region with the initial concentration being equal to that of the initial ECCS injection concentration. Water is removed and added to the system at the rate of core boiling. Boric acid is added to the mixing region with the replacement water at the concentration in the downcomer. The downcomer concentration is initially that of the initial ECCS but after the start of sump recirculation it will deplete as the core concentration builds. No boric acid carry over with the vaporized water is credited (the expected carry over concentration is about 300 ppm). This step ends with the operator action to start core flow through.

Depending on the plant the second step adds the modeling of some degree of core throughput flow to the process. Usually this will be a small amount of hot leg injection. To the degree that core boiling remains, water is still removed and boric acid left behind. However, to the degree that the new injection is not fully boiled, water is removed from the mixing region along with boric acid at the concentration of the mixing region. As boiling decreases with decay heating the concentration eventually peaks and starts to fall back to the average concentration of the initial ECCS injection.

So long as the peak concentration remains below the saturation limit (see Figure 57.1) the timing of operator action is adequate. Should a revision to the operator action be necessary, the calculations are repeated assuming various

timings until an acceptable action time is identified. The amount of assured core throughput flow can also be varied to allow differing operator action times.

23. Question: In discussing compliance with Appendix-K, item II.1.a, Section 9 stated, "Included in each [code manual] is a description of the code interfaces, the manner in which the codes are used, and the basis for selection of input parameters to the codes." However, review of the RELAP5/MOD2-B&W, FRAP-T6-B&W, REFLOW3B, BEACH, and FOAM2 manuals did not find this information. Clarify where this information can be found in the code manuals or when it will be provided for review.

Response: The statements in Section 9, item II.1.a of BAW-10168 concerning code interfaces for the evaluation model and how the codes are used in the evaluation model are incorrect. The computer code topical reports contain guidance on how the code can be used to solve a range of problems, some general information on how to interface with other codes, and information on the selection of input parameters. Specific information on the interfacing of the codes and selection of inputs for application of the evaluation model is contained in the evaluation model topical report, BAW-10168. Code interfaces are provided in Figure 4-1 of volume 1 for the large break model and Figure 4-1 of volume 2 for the small break model. Input selection for the codes is governed by section 4 of the volumes.

The second paragraph of Section 9 item II.1.a will be replaced with the following text at the first report revision:

> "The topical reports include the derivations of the equations as used in the codes, starting with fundamental physical laws and including all approximations. Any assumptions made in the derivation of the solution technique used by the code and the value of all code specified parameters are disclosed in these reports. The reports also describe the general application of

the codes to applicable problems including potential code interfaces and input parameter selection."

24. Question: Figure 4-1 shows the LBLOCA code interfaces in the B&W EM methodology.

a. The figure shows that RELAP5/MOD2-B&W is used to calculate the system response from time zero to the end of blowdown (EOB). The resulting core enthalpy, pressure, and mass flux from RELAP5/MOD2-B&W are then passed to FRAP-T6-B&W which calculates the hot rod response from time zero to the end of the adiabatic heatup (EOAH). Because the time of EOAH is greater than the time of EOB, there is a period where the required boundary conditions for FRAP-T6-B&W are not provided. Clarify the code boundary condition requirements for FRAP-T6-B&W during the period in question and discuss how they are provided in the EM methodology.

Response: Two key parameters affect fuel pin heatup during the adiabatic heatup period (from the EOB to the beginning of reflood): (1) core decay heat and (2) clad outside surface heat transfer coefficient. The core decay heat used is discussed in BAW-10168 Volume I Section 4.3.2.1 [24.1]. It consists of 120% of the 1971 ANS proposed standard plus allowance for actinide decay and continued fission power. The surface heat transfer coefficient is set to zero during this period.

b. The figure also shows that core parameters at the end of adiabatic heatup are passed from FRAP-T6-B&W to BEACH. Clarify which core parameters are passed.

Response: The core parameters (at EOAH) passed from FRAP-T6 to BEACH are fuel rod temperatures and fuel pellet-to-clad gap conductance for each axial node.

c. This figure and Figure 4.1 of Volume II regarding SBLOCA code interface do not show any mechanism for possible feedback between the codes that calculate the system response to the same portion of the accident. How are differences in the code results handled? For example FRAP-T6-B&W calculates the hot rod response from the beginning of the accident to the end of the event. RELAP5/MOD2-B&W and REFLOD3B also calculate portions of the accident calculated by FRAPT6-B&W. If the FRAP-T6-B&W analysis shows more flow blockage due to rod swell and rupture than the REFLOD3B or RELAP5 results, how is this type of difference handled? For SBLOCA, specifically clarify how the differences in the mixture levels calculated by RELAP5/MOD2-B&W and FOAM2 are handled.

Response: There are no duplications of roles in the B&W EM code package shown in Figure 4-1 of Volume I or Volume II, and the limited exchange of data between codes is better characterized as time-dependent boundary conditions than as feedback. The interactions are arranged in series through the use of a hierarchy for the computation of certain events or phenomena. The application of each code is designed such that the results of the combined applications are more conservative than those that could be achieved by a fully integrated application.

Although not an expected event for recirculating steam generator plants, an example of the hierarchy occurs in the handling of a blowdown rupture. In such a circumstance both RELAP5/MOD2-B&W (RELAP) and FRAP-T6-B&W (FRAP) would calculate the occurrence of rupture within the hot assembly. Although the results of these two codes tend to agree reasonably well with each other, they can not be expected to coincide exactly, and the

following procedure applies: The timing of rupture and the cladding strain effects, on heat transfer and oxidation rate for the peak cladding temperature calculation, are taken from the FRAP-T6 prediction. RELAP5 accounts for the effects of rupture --flow blockage, cladding strain (which increases metal water reaction), and the addition of interior oxidation --on the fluid state. This is appropriate because the RELAP5 role is to provide fluid boundary conditions to FRAP-T6 and not to calculate the final cladding temperature. Because of the heat transfer simulation, RELAP5 will generally calculate a rupture earlier than would FRAP-T6. In this case the timing of the rupture in RELAP5 will not be adjusted to agree with FRAP-T6 and the resultant post rupture flow decrease is treated as a conservatism. Should the reverse occur and FRAP-T6 predict a rupture prior to RELAP5 then the time of rupture in RELAP5 will be forced to coincide with FRAP-T6 and RELAP5 restarted for the remainder of blowdown. This exchange of data occurs once in a run and is considered as a time-dependent boundary condition.

The rupture induced strain for the purpose of determining flow blockage is recognized as different from the strain used to compute cladding heat transfer areas and oxidation surfaces. NUREG-0630[24.2] provides separate curves for these effects. The only code in the B&W evaluation model to calculate a flow blockage effect is RELAP5/MOD2-B&W. REFLOD3B evaluates only the average core and no provision is made for rupture within the code. Flow diversion away from the rupture location is treated (bounded) by applying the average channel flooding rate to the hot channel temperature calculation. Radial effects during reflooding, primarily the greater conversion of water to steam in the higher powered assemblies, promote higher effective flooding rates in

the hot assemblies than in the colder assemblies. This has been observed the CCTF and SCTF facilities, which are large enough to incorporate radial power shaping. Similar trends are observed in these facilities for asymmetric power and initial temperature tests. Additionally, as provided in the response to question 53 of this set, the FLECHT-SEASET tests with simulated ruptures support a conclusion that if cross bundle flow diversion does exist it is not of consequence. Therefore, the use of the average channel flooding rate for the hot assembly bounds the effect of rupture induced crossflow. The treatment of rupture effects during the reflood period for the hot channel cladding temperature differs between the original and revision 1 of the evaluation model.

For the original evaluation model, FRAP-T6-B&W, as discussed, uses boundary conditions, which include the effects of flow diversion to compute the hot spot temperatures. In doing so FRAP-T6 must adjust the local cladding-to-fuel gap and cladding surface area, upon rupture, according to the NUREG-0630 recommendation for strain to be used to compute cladding heat transfer areas and oxidation surfaces. BEACH is restarted at the time of rupture with its gap coefficients and cladding surface areas set equal to those predicted by FRAP-T6 just following rupture. As with the possible forcing of rupture in RELAP5, this exchange of information is considered a time-dependent boundary condition.

For revision 1 of the evaluation model the FRAP-T6 code has been replaced by an upgraded BEACH version during the reflooding calculation. This required the addition of full gap and rupture simulations to the BEACH coding. Therefore, because the calculation for cladding temperature is performed within a single code there is

no longer any occurrence of a time-dependent boundary condition during the reflood portion of the transient evaluation.

The hierarchy for mixture height and liquid inventory within the reactor vessel during small break LOCA is as follows: The core liquid inventory used in the FOAM code is taken from RELAP5/MOD2-B&W. The mixture height and steam flows, as calculated by FOAM2, are used in FRAP-T6-B&W to compute hot spot cladding temperatures. Because the RELAP5 mixture level prediction may not agree with the FOAM2 prediction, experience and test FOAM2 runs are used to determine when the core liquid inventory has decreased to an amount at which core uncovery can occur.

25. Question: Although the codes used to calculate the system response to a LBLOCA were individually assessed in the code topical reports and plant calculations using all the codes were provided in Appendix A, a LBLOCA analysis demonstrating the conservatism of the overall B&W EM methodology has not been provided. Provide a LBLOCA analysis that exercises each part of the EM methodology and demonstrates the method's conservatism to calculate the PCT by comparing to test data. Tests that could be used to perform this analysis include LOFT Test L2-3 and L2-5, Semiscale Mod-3 Test S-07-6, and Semiscale Mod 1 Test Series 4. These tests are listed in order of preferred use for this analysis (nuclear over non-nuclear and full-scale, non-nuclear over LOFT scaled, non-nuclear). As part of the information supplied, discuss how B&W ensures the information passed from code to code is transferred correctly (e.g., the conditions on initialization of REFLOD3B are consistent with the EOB conditions calculated by RELAP5/MOD2-B&W).

Response: Semiscale MOD-1 experiment S-04-6, a double-ended guillotine 200% break at the pump discharge, was selected for comparison with the B&W LOCA EM methodology. The blowdown thermal-hydraulic phase of this test was benchmarked as part of the RELAP5/MOD2-B&W topical report and documented in Appendix G of BAW-10164 with additional information provided in the response to question 12 of the first round of question on RELAP5/MOD2-B&W. That work will not be repeated in this response and the reader is referred to Appendix G and the response to question 12. This response completes the analysis through post-blowdown using REFLOD3B to calculate core flooding rates and BEACH to calculate reflood heat transfer coefficients. The hot assembly temperature analysis using FRAP-T6-B&W was then made for the entire LOCA transient.

The modeling criteria and assumptions used for the post-blowdown analysis are consistent with the EM methodology described in BAW-10166. The REFLOD3B, BEACH, and FRAP-T6-B&W models are briefly discussed below. Data transfer between the codes is preformed and documented in accordance with the B&W LOCA Evaluation Model and B&W Fuel Company quality assurance procedures and requirements. These procedures require the documentation of all inputs and outputs of computer codes, the source of the inputs (that must be a qualified source), any manipulation of source data to obtain a format suitable for code use, and any manipulation of output to obtain a format suitable for secondary uses. The resulting documentation is validated by an independent second engineer and maintained at B&W storage facilities for the expected life of the application, usually 40 years. These documents are available for inspection the upon request at B&W's offices in Lynchburg, Virginia.

REFLOD3B Model

The reactor vessel is represented by four volumes as shown in Figure 25-1. This noding scheme considers thermal hydraulic behavior occurring in the reactor vessel during the reflooding transient and is adequate to predict the core flooding rate as demonstrated in the benchmark of FLECHT-SEASET experiment 33338, Appendix I of BAW-10171. The primary system piping is represented by two loops similar to the blowdown model with reduced noding since the core flooding rate is not sensitive to loop nodalization. Flow resistance through the primary coolant pump is based on a locked rotor assumption. A 0.85 psi pressure drop penalty, to account for losses due to steam-ECC water interaction, is imposed on both loops near the ECC injection point.

An investigation of the Semiscale S-04-6 experimental data indicates that the concept of end-of-blowdown as a distinct

event does not apply. An approximate pressure equilibrium between the system and the break backpressure occurs at about 35 seconds, but this does not result in a termination of break flow. Furthermore, the intact loop accumulator water is at least partially bypassed until core reflooding starts at about 51 seconds. Such results are not unexpected in Semiscale and are generally associated with the atypical downcomer design, that has an extreme metal surface area-to-liquid volume ratio. Because system behavior like this is not representative of larger scale facilities, the evaluation model cannot model it directly. To circumvent these difficulties and provide the most reasonable comparison for the overall evaluation model, the REFLOD3B simulation was initiated to give a start of core reflooding at the same time as the experiment, 51 seconds after LOCA initiation. Therefore, from the calculated end-of-blowdown, 21 seconds, to the bottom of core recovery, 51 seconds, the test injection systems continued to operate, accumulator inventory decreased appropriately, but most of the ECC water was forced to bypass. The core during this time was assumed to under go adiabatic heatup in concurrence with the evaluation model assumption for the refill period.

Additional changes were made to simulate the 5.5-foot core in the S-04-6 system because REFLOD3B is designed specifically for the typical 12-foot PWR core. To achieve this, the core and downcomer material and fluid volume heights in Semiscale were increased by 6.5 feet. The core power, heat transfer areas, and fluid volumes were conserved. The result is essentially a "stretched" core which contains the same materials and generates the same power but is 12 feet instead of 5.5 feet high. Conservation of fluid volume decreases both the core and downcomer flow areas resulting in an increase in net downcomer driving head and thereby higher flooding rates. Quench front advancement, at these higher flooding rates, when evaluated as to the amount of core power being generated below the quench front is reasonable as is the volumetric flooding

rate. The calculated flooding rates are reduced by the ratio of the core flow area for the stretched simulation to the actual Semiscale core flow area when input to BEACH since BEACH can simulate the 5.5-foot core.

The REFLOD3B model is initialized with thermal hydraulic data from RELAP5 at the end-of-blowdown (EOB), 21 seconds. The core temperatures, water remaining in the pressure vessel, primary metal slab temperatures, steam generator secondary side conditions, and accumulator conditions are all set to the RELAP5 EOB values. The system pressure is initialized at the suppression tank pressure (34.8 psia) and remains at that pressure during the adiabatic heatup period. Fluid within the reactor vessel is initially in a saturated state. The intact loop is initialized at a superheated condition corresponding to the suppression tank pressure and the secondary side temperature.

BEACH Model

The BEACH model consists of 12 axial nodes as shown in Figure 25-2. The EM recommended BEACH noding pattern of three fluid volumes per grid span (see Appendix C of BAW-10166) was followed for the upper and lower spans. In the middle span, however, the noding was increased to correspond to the power steps of the electrical heaters. Another approach, averaging the power over the length of the nodes, was not selected because of the magnitude of the steps in power necessitated by the 5.5-foot core. Time-dependent boundary conditions, such as inlet velocity, pressure, and fluid temperature from REFLOD3B were imposed at the core inlet. The upper plenum pressure was set equal to the suppression tank pressure (34.8 psia). The initial heater rod temperature distributions were obtained from FRAP-T6-B&W at the end-of-adiabatic heatup.

FRAP-T6-B&W Model

The axial nodalization is the same as that of the BEACH model shown in Figure 25-2. Since FRAP-T6-B&W is designed specifically for a nuclear fuel rod, it was necessary to make adjustments to the gap coefficient and the conductivity of $UO_2$ to simulate the Semiscale electrical heater. The fuel-cladding gap conductance term was arbitrarily increased to be representative of the contact which occurs between the boron nitride in the Semiscale heater rod and the cladding. Additionally, the conductivity of $UO_2$ was increased by a factor of eight to correspond to that of boron nitride. The heat capacities of boron nitride and $UO_2$ are not sufficiently different to warrant an alteration of that parameter. A comparison of the radial temperature distributions between the RELAP5 and FRAP-T6-B&W calculations confirmed the use of these factors as adequate to simulate the heat transfer characteristics of the Semiscale heater rod.

Since metal-water reaction did not occur in the experiment (stainless steel core simulator), none was allowed in the FRAP-T6-B&W calculation even though the temperatures would indicate a small degree of reaction. Therefore, a one-to-one comparison, demonstrating the conservatism of the model, was obtained that is not complicated by the presence of an extra power source in one portion of the comparison. Hot channel flows, enthalpies, pressures, and core decay power from RELAP5 and reflood HTCs from BEACH are used as boundary conditions for the FRAP-T6-B&W thermal analysis.

Results of Analysis

The post-blowdown analysis was started at the EOB (21 seconds) with the REFLOD3B model. A sequence of events is presented in Table 25-1. As previously discussed, the refill period (adiabatic heatup) was extended for approximately 30 seconds

due to the longer refill time observed in the experiment (expected consequence of the Semiscale Mod-1 downcomer). This atypical behavior is not observed in larger experimental facilities (such as UPTF test number 5[1]) and, as discussed by CREARE in TM-1279[2], is not expected in full size PWRs. Core reflooding began at 51 seconds. The intact loop accumulator emptied at approximately 70 seconds in the test and 94 seconds in the calculation, 24 seconds later. As the downcomer was quickly filled in both the calculation and the test, the elongated period of accumulator injection had no substantial impact on the remainder of the calculation. Figure 25-3 compares the downcomer levels for the test and the calculation. The level displayed for the calculation has been adjusted by the downcomer stretched to actual flow area.

The difference in the peak downcomer levels is due to the positioning of the top downcomer differential pressure tap about 9 inches below the cold leg nozzle. The rapid level drop at 90 seconds in the experiment is caused by downcomer boil-off. This mass depletion occurs just after the intact loop accumulator injection ceases, and is a result of heat transferred from the downcomer walls. As discussed in detail in TREE-NUREG-1077[3], this phenomenon is unique to the Semiscale MOD-1 facility, being caused by the large metal-to-fluid volume ratio of Semiscale, and will not be appreciable in a PWR downcomer. REFLOD3B could not simulate the boil-down because the code does not allow the boiling of water in the downcomer, region 3R. Although downcomer and lower plenum metal masses are modelled, the resultant heat flux is only used to update the water temperature at the core inlet. As such the code is limited for small scale comparisons, but will do well for larger facilities in which the downcomer boiloff does not occur.

Notwithstanding the excess head available for reflooding, the apparent flooding rate remained conservative by a factor of

3 to 6 (see Figure 25-4). The REFLOD3B calculation is terminated at 150 seconds due to an excessive increase in computation time. At that time the flooding rate has stabilized and is slowly decreasing. Simulating the core inlet flow rate by holding the flooding rate constant, for the rest of the evaluation, saved considerable time and resulted in a slight conservatism, from a benchmark viewpoint, in that continuation of the run would have resulted in slightly lower flooding rates and slightly higher cladding temperature predictions. Such a termination of the REFLOD3B run would not be allowed during a licensing calculation.

The hot spot temperature responses at 25 inches and 29 inches above the bottom of the heated section are shown in Figures 25-5 and 25-6 respectively. The heater rod undergoes adiabatic heatup from 21 seconds (EOB) to 51 seconds (BOCREC) as required by the evaluation model and Appendix K to 10CFR50.46. This results in a rod heatup rate higher than the test as some cooling is evident in the experiment. Following the initiation of core reflooding, both the experiment and calculation experience sufficient heat transfer to terminate the cladding temperature rise within a few seconds. Over the next 60 seconds both show a gradual temperature decrease. The more rapid decrease in cladding temperature in the calculation over the test, that occurs at the 25-inch level, indicates that the heat transfer coefficient predicted for that elevation is about the same as the test was experiencing. The steeper cooldown occurs because of the higher cladding temperature. On the other hand, the similar slopes of the temperature responses at the 29-inch level indicates that the calculated heat transfer coefficient at that elevation must have been considerably below the experimental value.

The cladding heatup observed in the test after 100 seconds is most likely caused by a drop in core inventory related to the decrease in downcomer driving head. A verification of the

relationship through a decrease in the core inlet flow was not present in the experimental data. The core flow data do show a substantial decrease in negative flow spikes at approximately 100 seconds. This leads to the conclusion that the downcomer boil-off resulted in a stagnation of the quench front within the core and a momentary decrease in the cooling capabilities of the flow above the quench front because of that stagnation. Similar behavior has been observed in the BEACH code when saturated water rather than subcooled water is used for the assembly inlet condition. By about 200 seconds, the downcomer level has recovered sufficiently to force flooding again, and core inlet enthalpy is probably subcooled again. The calculational results could not reproduce the plateau in cladding temperature between 100 and 200 seconds because of the limitation in the REFLOD3B code on downcomer boiling; however, the results are characteristic of a larger scale facility that floods in a more monotonic pattern.

Conclusions

The results of the comparison are subject to some of the limitations in applying portions of the B&W evaluation model to substantially and atypically downscaled facilities. Notwithstanding those limitations, however, the relevant portions of the evaluation model are shown to produce physically realistic results and to comprise substantial conservatism. Peak cladding temperatures during blowdown are predicted higher than the data by 300 to 500 F. The temperature margins established during blowdown are preserved in the post-blowdown until the cladding approaches quenching conditions. This indicates a substantial degree of conservatism in the both the pre- and post-blowdown modelling.

Therefore, the comparison affirms that the B&W LOCA EM methodology can adequately and conservatively predict the response of a pressurized water reactor system to the occurrence of a loss-of-coolant accident.

References

1. Upper Plenum Test Facility -UPTF- , Test No. 5, Downcomer Separate Effect Test, Siemens AG, Erlangen, West Germany, October 1987.

2. M. G. Izenson and C. J. Crowley, TM-1277, Scaling of Flashing Transient Behavior to Full Scale, Creare Inc., September 1988.

3. J. M. Cozzuol, TREE-NUREG-1077, Thermal-Hydraulic Analysis of Semiscale Mod-1 Integral Blowdown-Reflood Tests (Baseline ECC Test Series), EG&G Idaho Inc., March 1977.

Table 15-1. Sequence of Events.

| Events | Time, seconds | |
|---|---|---|
| | Data | RELAP5-EM |
| Leak Initiation | 0.0 | 0.0 |
| Steam Generator Isolation | 1.0 | 1.0 |
| Intact Loop Accumulator Injection | 17.0 | 13.3 |
| Intact Loop LPI Injection | 21.0 | 22.0 |
| End-of-Blowdown (EOB) | >30.0 | 21.08 |
| Bottom-of-Core Recovery | 51.0 | 51.0 |
| Intact Loop Accumulator Empty | 70.0 | 94.0 |
| Hot Spot Temperature Turnaround* | 65.0 | 54.0 |
| Hot Spot Quench* | 190.0 | 160.0 |

*Located 25 inches from the bottom of the heater.

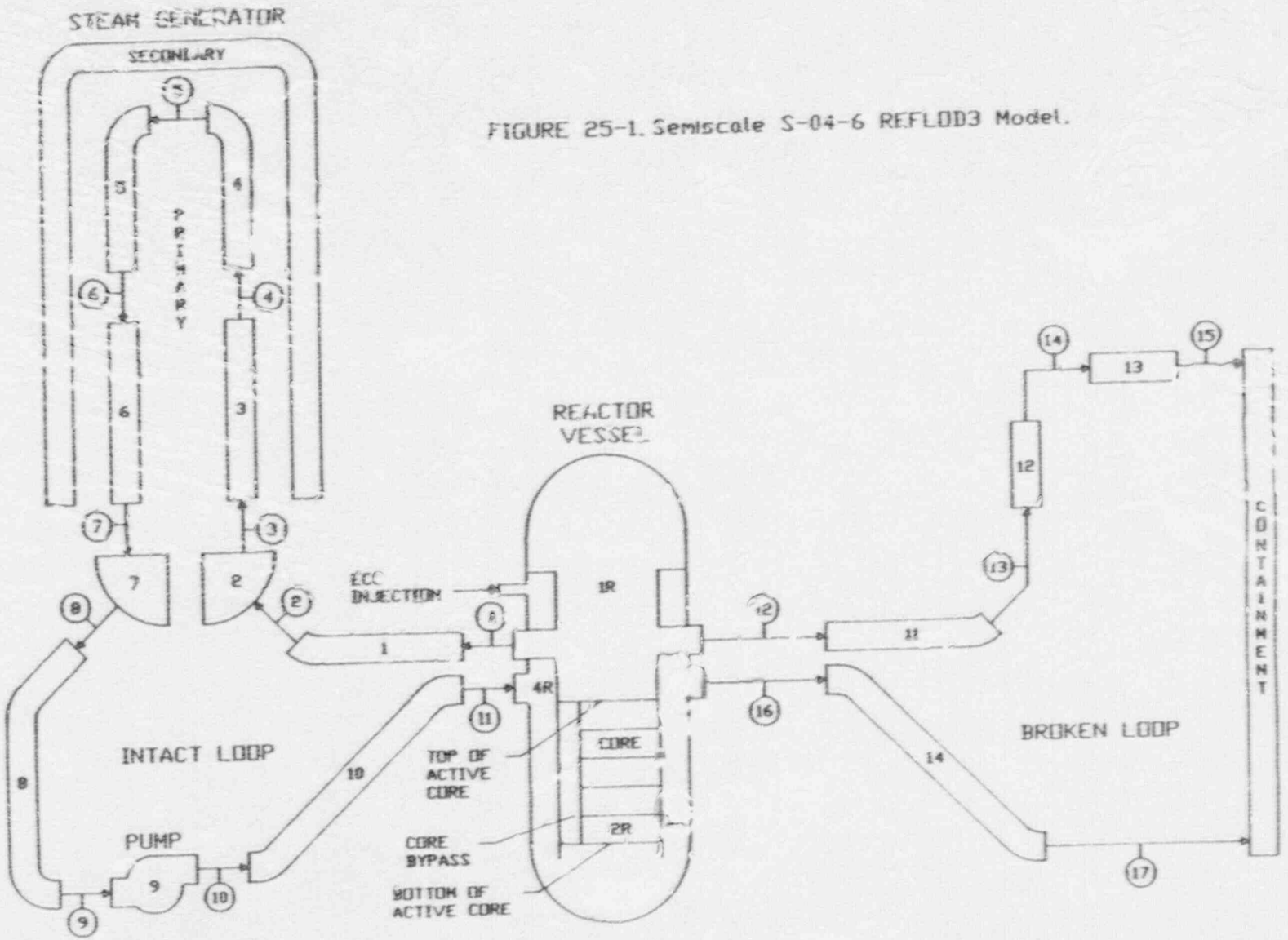
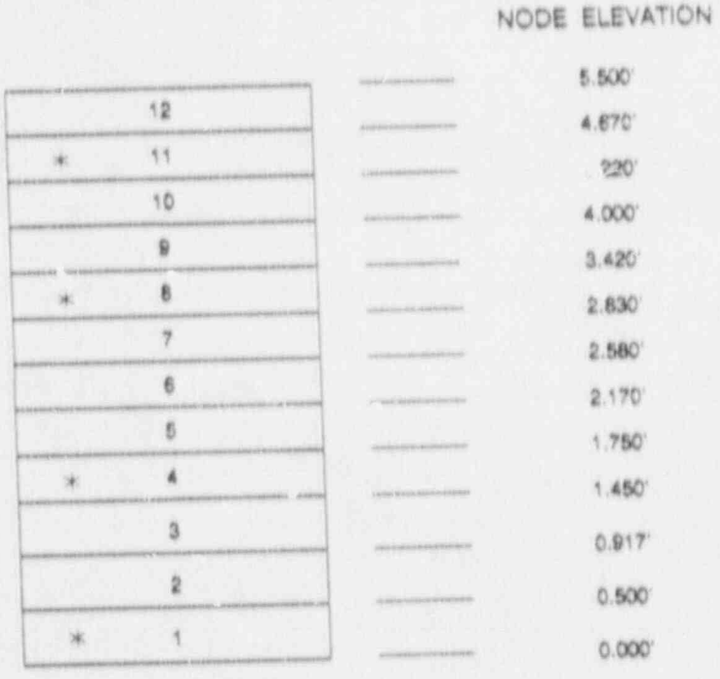



FIGURE 25-1. Semiscale S-04-6 REFLOD3 Model.

FIGURE 25-2. BEACH AND FRAP-T6-B&W NODING DIAGRAM.




* GRID NODE

FIGURE 25-3. SEMISCALE S 04-6 -- DOWNCOMER LIQUID LEVEL

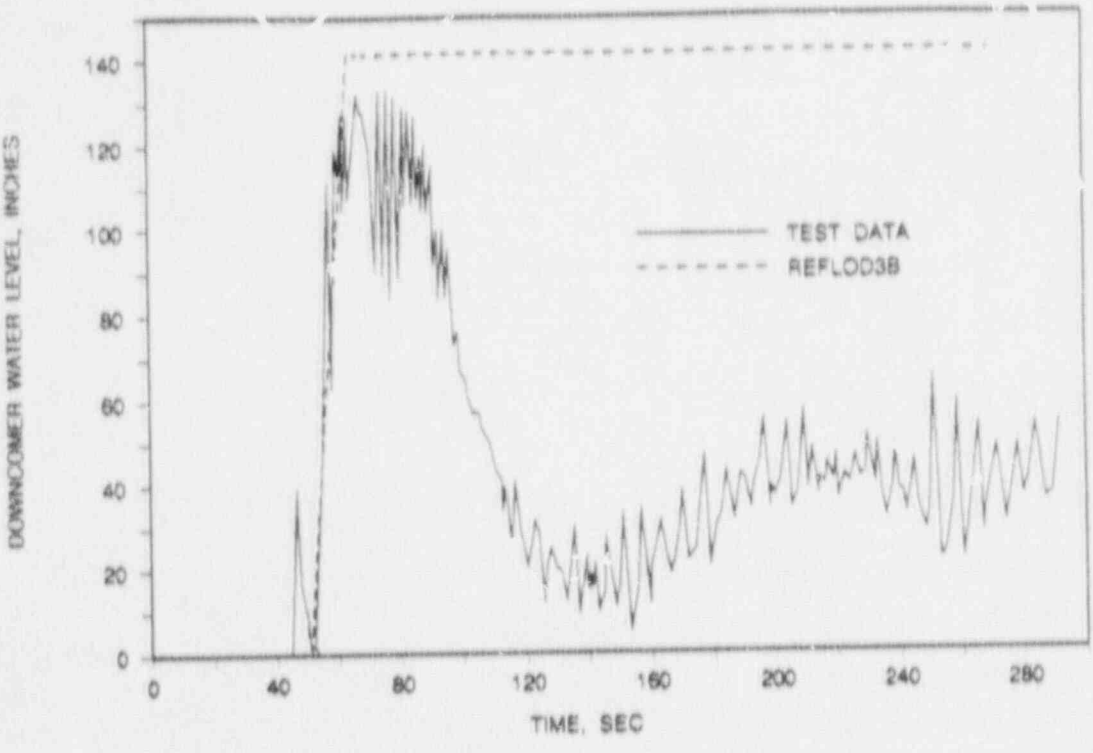



FIGURE 25-4. SEMISCALE S-04-6 -- CORE INLET FLOW.

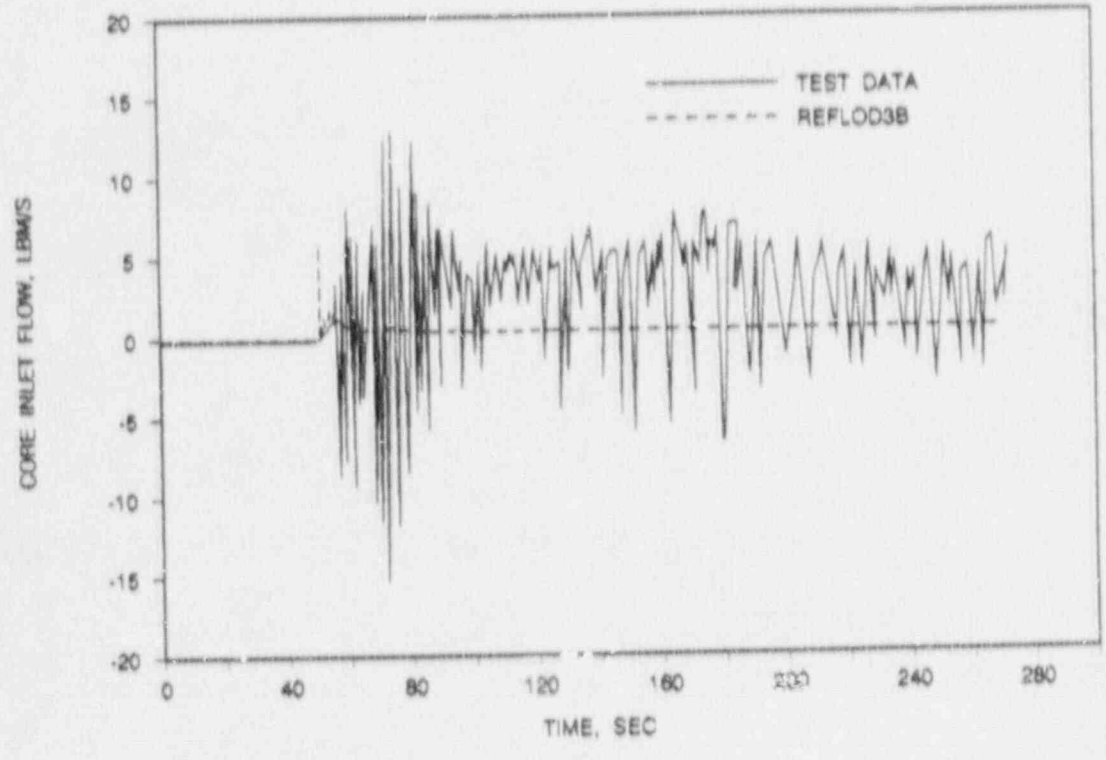


FIGURE 25-5. SEMISCALE S-04-5 -- HOT ROD PCT AT 25 INCHES.

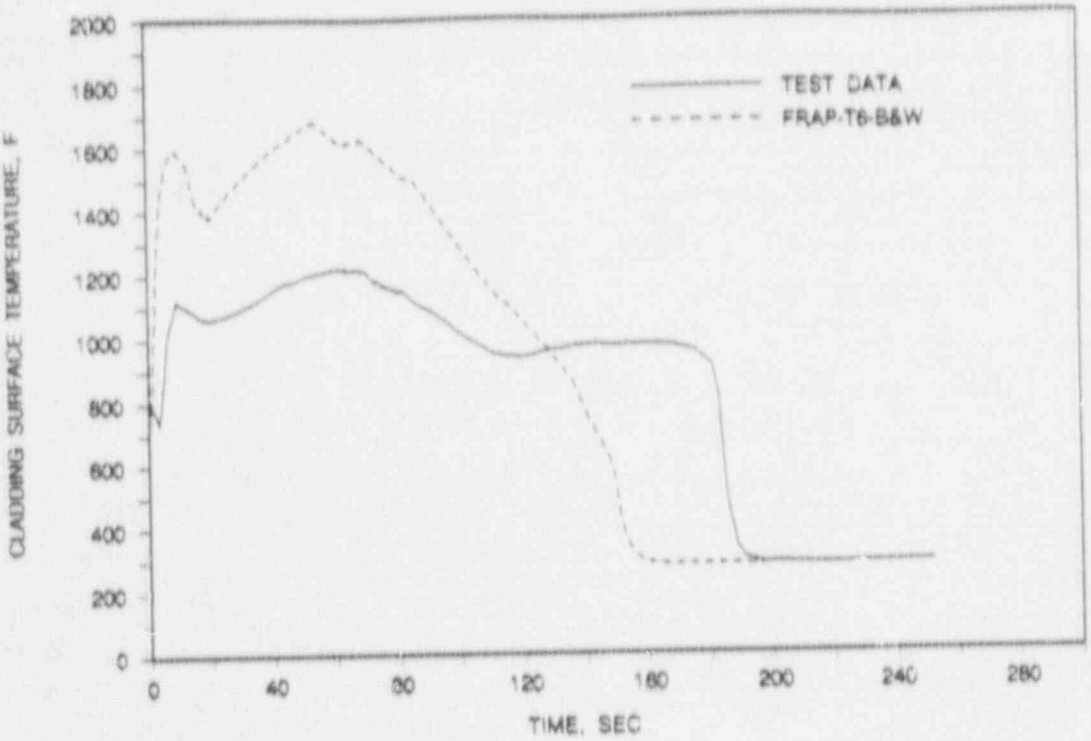
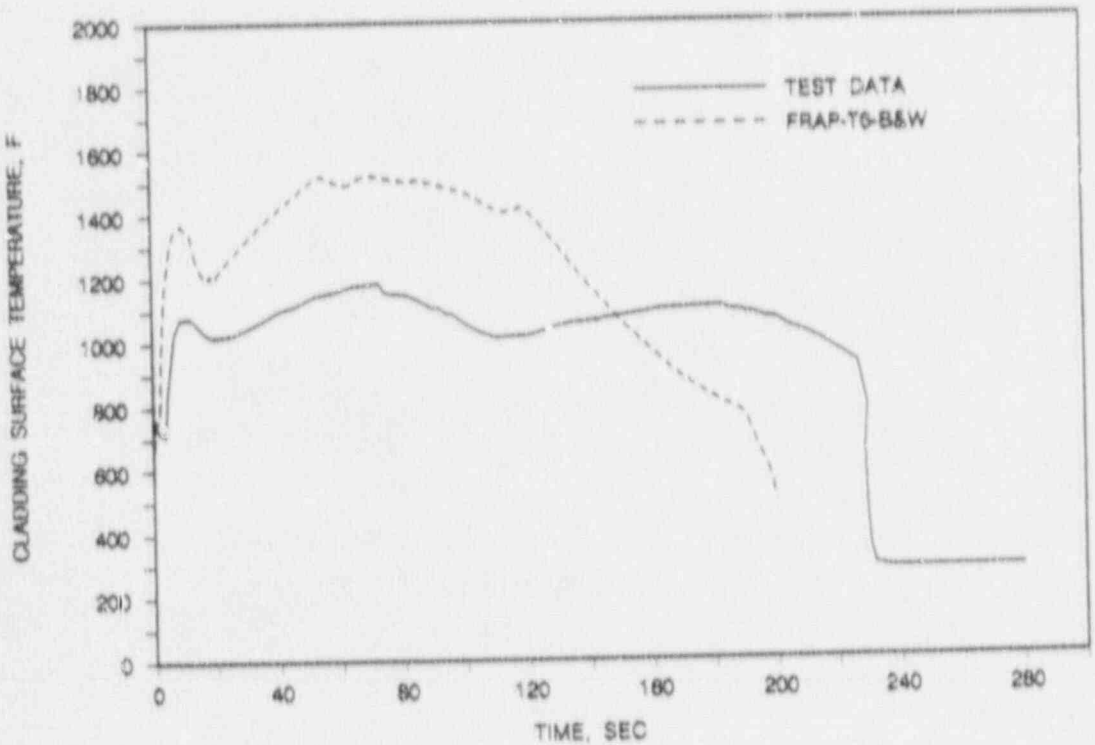



FIGURE 25-6. SEMISCALE S-04-6 -- HOT ROD PCT AT 29 INCHES.



26. Question: Each of the codes used in the LBLOCA and SBLOCA EM methodologies have a number of user inputs and option selections. To ensure that each code is used within its capabilities and with proper input, information justifying all selected options and input data, including defaults, should be presented. List all options, with bases for the chosen options, to be used for LBLOCA and SBLOCA analyses.

Response: The documentation of the code options in a list is not the way B&W has selected to control the evaluation model. As presented in 10 CFR 50.46 "An evaluation model is the calculational framework for evaluating the behavior ... It includes ... all information necessary for application of the calculational framework ... such as mathematical models used, assumptions ... , procedure for treating input." Thus, the evaluation model is a guide to the calculation sufficient in detail to assure that application of the guide accomplishes the intended calculational procedure.

In order to simplify understanding and maintenance of the evaluation model, the model reporting or documentation has been tightly controlled to eliminate to the extent possible duplication of information. Methods controlled by the evaluation model are presented in context and on as generic a basis as possible. Thus, the description of the flattening factor for the power at the hot spot is only provided once while it is incorporated in three codes; RELAP/MOD2-B&W, FRAP-T6-B&W, and BEACH.

B&W will provide specification of the options to be used in the calculations within context in the evaluation model report and to the extent necessary to assure that the proper and intended options are used. B&W will not provide documentation for options or approaches not to be used. It is, however, apparent that in the original release of BAW-10168, RSG LOCA, the B&W evaluation model report for recirculating steam

generator designs, some of the information required was not included. To speed review a list of that information is provided as Table 26-1. The list is organized according to the documentation and will be used to update the evaluation model report in the future.

One specific type of option deserves unique treatment. At several locations throughout all of the computer codes, correlations have been programed to include user input multipliers. This was done to make provision for sensitivity studies and in some cases to allow for code applications to problems other than those of 10CFR50.46. It is not the intent of the evaluation model to allow variation in these constants. Therefore, unless specifically stated to the contrary in the evaluation model report these constants shall all have a value of one. Programed constants that are part of the correlations or part of B&W's implementation of the correlations will have the value as published in the individual code topical reports. The evaluation model will be revised to include these statements.

Inputs used during an evaluation can be categorized as follows:

Generic: User supplied values or constants whose values are controlled by the evaluation model. The materials properties are a good example of this type of input.

Prescribed: Input for which a determining procedure is specified in the evaluation model without the specification of a value. The use of hot fluid volumes within the RELAP5/MOD2 model is a good example of this type of input.

Plant: Input which is taken from documentation for the individual plant or plants to be covered by the evaluation. Plant geometry inputs are examples of this type of input.

Case: Input which will vary depending on the accident being evaluated. Break area is a good example of this type of input.

The B&W evaluation model controls these different types of inputs in differing ways. Generic and Prescribed inputs are controlled in the same fashion as code options, and are documented in context within the evaluation model report. As with the code options, it is apparent that the original release of BAW-10168, RSG LOCA, the B&W evaluation model report for recirculating steam generator designs, does not contain all of the information required on generic inputs. To speed review a list of that information is provided as Table 26-2. The list is organized according to the documentation and will be used to update the evaluation model report in the future.

Plant input is controlled by B&W internal calculational procedures and not by the evaluation model. These procedures are written to adhere to ANSI quality assurance standards. For the most part these procedures require that the input come from controlled design documentation, that it be referenceable, that its use be documented, and that an independent review be conducted to assure that this has been done. The documents attesting to this for any given evaluation are controlled documents stored at B&W and available for audit at any time.

Case input is much akin to assumptions. It is not controlled by either the evaluation model or B&W procedures other than that it must be documented along with the plant input in controlled stored records of the calculation. Case input is also available for audit at any time.

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model

| OPTION | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Flow Film Boiling Lock-in | No lock-in until $T_w - T_s \geq 300$ F |
| Fine Mesh Rupture Option | Not used |
| Concentric or Non-concentric Fuel Pellet Simulation | Use Concentric with TACO2<br>Use Non-concentric with TACO3 |
| Critical Flow Model | Subcooled - Ext. Henry-Fauske<br>Two-phase - Moody<br>Superheat - Murdock-Bauman |
| Leak Flow Slip Model | LBLOCA - Moody Slip<br>SBLOCA - No Slip |
| Criteria for Break Discharge Coefficient | LBLOCA - No change in coefficient during run<br>SBLOCA - coefficient changes with leak void fraction |
| Internal Critical Flow | Flow is limited except for special paths: downcomer to reactor vessel upper head bypass path, pressurizer surgeline, & accumulator lines |
| Friction | Calculated by RELAP5 |
| Homogeneous-Equilibrium | Only in the core region |
| Heat Transfer Model | Core model is used for the active core heat structures the System model is used elsewhere |
| CHF & Film Boiling Correlation | The correlation combination with BWCMV for high pressure and high flow is used. Condie-Bengston IV is selected for flow film boiling |

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Metal Water Reaction Model | Baker-Just |
| Rupture Temperature | Instantaneous ramp rate during pre-plastic region; Plastic weighted, time averaged ramp rate thereafter |
| Accumulator Wall Heat Transfer | Yes |

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| FRAP-T6-B&W | |
| Flow Film Boiling Lock-in | No lock-in until $T_w - T_s \geq 300$ F |
| CHF & Film Boiling Correlation | The correlation combination with BWCMV for high pressure and high flow is used. Condie-Bengston IV is selected for flow film boiling |
| Metal Water Reaction Model | Baker-Just |
| Rupture Temperature | A plastic weighted, time averaged ramp rate is used |
| Licensing Audit Code Models | All of the Licensing Audit Code Models are used except for: Cladding Specific heat, Elastic Modulus, Fuel Deformation, Operating Power x 1.02, and ANS Decay Power x 1.20 |
| Clad Deformation | No Balloon Option, clad failure occurs per rupture curves |

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| REFLOD3B | |
| CRF / Core Heat Transfer | CRFCKN option: CRFCKN correlation for carry out coupled with FLECHT/ANC correlation for core heat transfer |
| Core Bypass | The core bypass model is selected |
| Vent Valve Model | May be selected by user to simulate downcomer to upper head bypass flow. Vent valve steam condensation efficiency in Equation 2-68.4 of the REFLOD3B topical is set to 1.0 |

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
|---|---|
| BEACH | |
| Friction | Smooth pipe option |
| Reflood | Reflood option selected |

Table 26-1 Additional Evaluation Model Guidelines

Code Options Used in Evaluation Model - Continued

| OPTION | SELECTION |
| --- | --- |
| FOAM2 | |
| Two Phase Slip Corr. | Wilson |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

| INPUT | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Fluid Volumes | Hot - from design drawings |
| Attached Piping Volumes | Only the accumulator line volumes are included. The total of other attached piping volumes lies within the accuracy of the system volume calculation and are not included |
| Initial Reactor Coolant System Flows | The system flows are those used in the at power minimum DNB analyses. The hot and cold leg temperatures are set by nominal control system response to that RCS flow |
| Two Phase Pump Degradation | Use Table 2.1.5-2 BAW-.)164 |
| Transition Quality for Single to Two-Phase Flow Correlation | 0.0 |
| Moody Slip Parameters | $0.5 \leq$ slip ratio $\leq 2.0$ |
| Leak Flow Smoothing | LBLOCA - $-0.001 \leq X \leq 0.001$<br>SBLOCA - $0.01 \leq \alpha \leq 0.70$ |
| SBLOCA Discharge Coefficient Switch | $Cd = 1.0 \quad \alpha \leq 0.70$<br>$Cd = 0.7 \quad \alpha > 0.70$ |
| Initial Inventories for Reactor Coolant System, Secondary System, and ECCS Systems Pressure | Set by nominal operation design levels. e volumes for attached pip.g except for the pressurizer surgeline are not included in the LOCA model |
| Primary Metal | Structures are lumped together according to material properties, thicknesses, and location. Grouping is user controlled |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| RELAP5/MOD2-B&W | |
| Over Power Factor | 2% applied to both average and hot assembly powers |
| Power Flattening | 5% of the heat generated in the hot assembly is deposited directly in coolant. This factor is not applied to the average core |
| Decay Heat | 120% ANS 1971 based on 102% core power |
| Actinide Heating | Fit to envelope of 1979 ANS 5.1 Standard |
| Initial Fuel Temperatures | Adjusted to agree with an NRC approved steady-state fuel performance code (TACO2/TACO3) |
| Rupture Data | NUREG-0630 ramp rate dependent data |
| Time Step Control Option | Option 3, mass error checking, consistent hydrodynamic and heat structure solution time advancement |
| ECCS Fluid Temperatures | Set at nominal year average temperatures per system |
| ECCS Time Delays | Includes provision for signal, diesel start-up, pump start-up, and line filling for all evaluations |
| Containment Pressure | Set from FSAR |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| FRAP-T6-B&W | |
| Power Flattening | 5% of generated heat deposited directly in coolant |
| Over Power Factor | 2% applied |
| Power Distribution | Appropriate for the average fuel pin in the highest powered fuel assembly in the core. Axial peaking is the same as for RELAP5/MOD2-B&W evaluation and the radial peak is the relative hot assembly power |
| Decay Heat | 120% ANS 1971 based on 102% core power |
| Rupture Data | NUREG-0630 ramp rate dependent data |
| Initial Fuel Temperatures | Adjusted to agree with an NRC approved steady-state fuel performance code (TACO2/TACO3) |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| REFLOD3B | |
| Unrecoverable Loss Factors | Input separately from friction loss factors |
| Loss Factors Source | RELAP5/MOD2-B&W steady-state prediction |
| Fluid Volumes | Hot - from RELAP5/MOD2 model |
| ECCS Time Delays | Includes provision for signal, diesel start-up, pump start-up, and line filling for all evaluations |
| Containment Pressure | Set from FSAR |
| Decay Heat | 120% ANS 1971 based on 102% core power |
| RC Pump Resistance | Appropriate for locked rotor condition |
| ECCS Fluid Temperatures | Set at nominal year average temperatures per system |
| Primary Metal | Primary metals are included with structures lumped together according to material properties, thicknesses, and location. Upper head metal is modelled in the RCS hot legs. Grouping selections are user controlled |
| Film Coefficients for Non-Core Heat Slabs | Covered portion of Reactor Vessel 200 Btu/Hr-ft$^2$-F<br>Uncovered portion of Reactor Vessel 20 Btu/Hr-ft$^2$-F<br>Remainder of the Primary System 1000 Btu/Hr-ft$^2$-F |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| REFLOD3B | |
| Cold Leg Steam Water Interaction Pressure Drop | 0.85 psia during accumulator injection and 0.5 during pumped injection |
| System Initialization | Core and upper head regions are saturated steam, hot legs and steam generators are superheated steam |
| Constants for CPFCKN | $C_{SUP} \geq 1.025$ ( ) , $C_T = 1.0$ (1/s) , $C_N = 1.0988$ (1/ft) |
| Condensation efficiency for Steam Vented Through the Upper Head Spray Nozzles | 1.0 |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| BEACH | |
| Form Loss Factors | From assembly tests |
| Power Flattening | 5% of generated heat outside of fuel pin |
| $C_{maxDB}$ | Use value from Equ. 2.1.3-105.1 of BAW-10168 Rev. 2 |
| Correlation for $F_{gn}$ | Use the Yao, Hochreiter, and Leech correlation as described in Section 2.1.3.7.1.of BAW-10168 |
| Grid Atomization Factor | Use Equation 2.1.3-99 of BAW-10168 revision 2 with n equal to 2.7 |
| Fuel Temperatures | Initialized to FRAP-T6 results at the end of adiabatic heat up |
| Gap Multipliers | Initialized to those used by RELAP5 to obtain the initial (time 0) hot channel fuel temperatures |

Table 26-2 Additional Evaluation Model Guidelines

Generic and Prescribed Inputs for the Evaluation Model

- Continued -

| INPUT | SELECTION |
|---|---|
| FOAM2 | |
| Steam Generation from Reactor Vessel Metals | Yes |
| Steam Generation from Flashing | Yes |

27. Question:

a. For the crossflow resistance sensitivity study discussed in Section A.3.4, the base crossflow resistance was multiplied and divided by 100 to determine the sensitivity of the calculated results. The study showed essentially identical results for the three cases. Do you intend to use the base case crossflow resistance in all future EM calculations? If so, identify the resistance factors used in the base case study. Justify the applicability of these resistance factors to all fuel bundle types to be analyzed.

Response: The crossflow resistance factor used in the evaluation model studies expressed in unitless form is 50.0 based on a flow area of 1.3 square feet. This resistance factor was calculated from a correlation developed by Babcock & Wilcox and documented in Appendix H of the CRAFT2 code topical report[27.1]. The crossflow resistance is given as a correlation of data measured at the B&W Alliance research facility for flow between adjacent subchannels and is based on the velocity upset or mismatch between the assemblies. The resistance correlation is given as a function of fuel assembly pitch-to-fuel pin diameter, the number of fuel assemblies in the regions for which the crossflow is modeled, and the ratio of the Reynolds numbers in the regions.

The pitch-to-diameter ratio will vary only slightly with fuel type. That is, for all 17-by-17 fuel assembly designs, even the OFA (Optimized Fuel Assembly), this ratio is nearly the same. For differing fuel assembly types the ratio does not change substantially but it should be updated. B&W will use the base cross-flow

resistances for all 17-by-17 applications and update the resistance for applications to alternate fuel types such as 14-by-14, 15-by-15, or 16-by-16.

The number of fuel assemblies in the adjacent regions is a function of the noding detail selected for the model. As this is fixed by the evaluation model, this determinant for the resistance will not be altered from plant to plant.

The ratio of Reynolds Numbers is determined by the initial flow relationship between the cross-flowing regions. As the hot channel initial flow differs from the average channel flow only by the amount of the chimney effect, the difference in Reynolds Number ratio between plants will be inconsequential.

Combining these considerations the deviation in cross-flow resistance from one plant and fuel design to another will be small. In particular, these differences will be much less than those imposed by the crossflow sensitivity study. Therefore, B&W will employ the base crossflow resistance for all applications, updated only for a major change in fuel type such as the difference between 15-by-15 and 17-by-17 fuel.

b. Appendix K, Item I.C.7.a, requires the effects of crossflow between hot and average channels be considered. Although a crossflow resistance study was performed, it only considered the initial crossflow resistance. The effects of rod swell and rupture on the crossflow resistance were not considered in this study, nor were the effects discussed in the sections on the rod swell and rupture model. Clarify how the effects of rod swell and rupture are included in the crossflow resistance.

Response: The effects of rod swelling and rupture within the core region are addressed by the core model if and when rupture occurs. The core flow model is a network of axial and radial flow paths connected at the core nodes. Upon the occurrence of rupture, the flow resistance for the axial flow paths connected to the node within which the rupture has been calculated are increased. Although the modelling is general the rupture will most likely occur in the hot channel. This results in a revised resistance network that can interact appropriately with the fluid properties as they exist. Depending on the core flow conditions at the time of rupture, the additional resistance to flow through the ruptured area could cause significant flow diversion away from the hot channel at the elevation of the node upstream of the rupture and back into the hot channel at the elevation of the node downstream of the hot channel. Experience shows, however, that the core conditions are typically such that the diversion of flow out of the hot channel is limited.

The modelling neglects the effect of adding resistance to the crossflow paths. As with flow resistance for axial flow, the resistance to flow radially will be increased as ruptures appear in the hot assembly. This increase in flow resistance to radial flow would tend to retain some coolant within the hot assembly near the locations of the ruptures which would otherwise be passed radially out of the hot assembly. The BWFC evaluation model makes the conservative simplifying assumption that the resistance to flow in the radial dimension is not affected by the occurrence of rupture. Therefore, the model tends to promote flow away from and out of the hot assembly.

No provision is made to increase the flow resistance in

an axial segment because of the effects of prerupture strain. These strains result in slight bulges in the cladding surface that intrude into the flow channel. The geometry is prerupture and is therefore similar to a nozzle with a well rounded inlet and a well rounded exit. Such a configuration would not add substantially to the flow resistance for the degree of strain that occurs prior to rupture.

The study for crossflow resistance sensitivity included in the evaluation model report did provide for the effects of swelling and rupture. Those events, however, were not calculated to occur during blowdown. Rupture will occur for these runs during reflood, and the resultant flow diversions, if any, are allowed for by conservatisms in the BEACH code. Because core cooling during reflood is limited for recirculating steam generator plants, the probability that a calculation during which a blowdown rupture occurs can meet the criteria of 10 CFR 50.46 is small. Thus the sensitivity study evaluated the most probable circumstances for application of the evaluation model.

References:

27.1 CRAFT2 - Fortran Program for Digital Simulation of a Multinode Reactor Plant During Loss of Coolant, BAW-10092-A Rev. 3, July, 1985.

28. Question: Sections A.2.1 and A.3.6 discussed the results of the time step sensitivity studies performed for RELAP5/MOD2-B&W AND FRAP-T6-B&W. The studies discussed the results of reducing the maximum time step in the base case without discussing the size of the time step used in the base case calculations. Provide the time step size used in the base case calculations so the time steps used in future EM calculations can be judged for adequacy against this time step size. Clarify if the time step sizes used in the base case calculations will be used in EM calculations. Also, provide the results of time step sensitivity studies for REFLOD3B and BEACH.

Response: For production runs B&W will use those time steps as requested by the base run of the RELAP5/MOD2-B&W time step sensitivity study. These time steps were:

RELAP5/MOD2-B&W

| Time Period Value (sec.) | Minimum Time Step (sec.) | Maximum Time Step (sec.) |
|---|---|---|
| 0.0 to 0.2 | 0.000001 | 0.002 |
| 0.2 to 1.0 | 0.000001 | 0.005 |
| 1.0 to 16.0 | 0.000001 | 0.02 |
| 16.0 to 22.0 | 0.000001 | 0.005 |

The base run for th. FRAP-T6 evaluation model time step studies was conducted at the time step sizes listed below. For production cases the base run time steps will be used fixing the changeover at a convenient time following the end of blowdown.

FRAP-T6-B&W

| Time Period Description | Time Period Value (sec.) | Time (sec.) |
|---|---|---|
| Period of Blowdown | 0.0 to 20.0 | 0.01 |
| Post Blowdown | 20.0 on | 0.2 |

No specific time step studies have been performed with the BEACH code. BEACH uses the same time step controller as does RELAP5/MOD2 36.02 from which it was derived. The maximum time step size used in BEACH is 0.01 seconds, one-half the size of the largest time step used in the RELAP5/MOD2-B&W large break LOCA EM (BAW-10168). Therefore the convergence shown by the RELAP5/MOD2-B&W time step study supports the conclusion that BEACH results will also be converged. Additionally, it was shown during BEACH development that further refinement of the model spatially does not substantially alter the results. A 10-node model, which replicated the original 20-node model except for the number of fluid volumes, was generated and run. The results agreed closely with the corresponding 20-node model. This lack of sensitivity to node size is consistent with convergent time advancement.

REFLOD3B time advancement is controlled automatically by the program using accuracy and convergence criteria. Time step size or limits on time step size are not input by the user. The solution is by a variable order, variable time step size, Adam's predictor-corrector method, of the Gear type. Section 2.13 of the REFLOD3B topical BAW-10171 describes time step control in detail.

29. Question: Figure A-23, as well as a number of other figures in Appendix A, shows the mass flow rate at the peak power location. The core flow at this location shows an increase from 2 to 6 seconds that results in the rod temperatures turning over in the core (Figure A-25). Clarify the reasons for the core flow increase calculated. Is the core flow increase due to flashing of fluid in the lower plenum? Has B&W performed sensitivity studies on the lower plenum nodalization to confirm that the core response to lower plenum flashing is conservatively calculated? Provide the results of such studies.

Response: The flow recovery during this period is a typical result of LOCA calculations. The effect is apparent in large break LOCA calculations by all three U.S. PWR vendors. This typical core flow response to the LOCA is primarily controlled by the relationship between the intact loop pump coastdown and the leak flow as shown in Figure A-60. As the leak flow rate continues to decrease, the flow into the downcomer from the intact loops exceeds the flow out the downcomer. The net effect increases the liquid head in the downcomer and results in an increase in the core flow. Flashing in the lower head occurs during this period, but is not sufficient to produce the flow surge. To assure a realistic treatment of the lower head, the nodalization in the lower vessel region has been reviewed for relationship to physical constraints and the results of the analyses for non-physical behavior. Nothing has been uncovered that would indicate a modeling adjustment. The results of the core noding study shown in Figure A-46 of Appendix A of BAW-10168, further confirm that the phenomenon is controlled by the break and is not sensitive to the vessel nodalization. Therefore, the additional nodalization sensitivity study is not considered necessary.

30. Question: Section A.3 discussed the changes, made to the RELAP5/MOD2-B&W base model after completing an initial set of sensitivity studies. Provide additional information regarding the following changes.

a. Change 1 stated that there was no lock-in to film boiling before cladding superheat exceeded 300 F. Does this mean the initial calculations locked-in to film boiling before the cladding superheat exceeded 300 F? If so, clarify why this was done in the initial calculations.

Response: Section 2.3.3.1 of the RELAP5/MOD2-B&W topical report (equation 2.3.3-7) states the post-CHF heat transfer correlation selection logic. This logic is consistent with the EM methodology used for the B&W plants (computer code THETA1-B ). During the implementation of the EM heat transfer package an option was added to lock into the permanent film boiling when the transition boiling correlation is selected and the transition boiling heat flux is higher than the heat flux calculated using pre-CHF correlations. This option was inadvertently selected for the earlier analysis.

b. Clarify how the vessel upper head junction (Change 3) was revised because all of the vessel node diagrams show the same nodalization in the upper head region. Discuss the basis for the revised model.

Response: The change 3 should have stated "revised the reactor vessel upper head junction input, the junction area and the junction resistance factors." The input to this junction was modified from the earlier input

model to reflect the correct geometry of the upper head.

c. Change 4 stated Moody phase slip was modeled at the break. Is Moody phase slip to be used in the plant EM calculations? Clarify which slip model will be used with the Extended Henry-Fauske critical flow model. Also, clarify whether the stagnation or static pressure and enthalpy will be used in using the critical flow tables in Appendix C of BAW-10164P.

Response: The options selected for the choked flow in the plant calculations are as follows.

| | |
|---|---|
| Subcooled | : Extended Henry-Fauske. |
| Saturated | : Moody with Moody slip. |
| Superheated steam | : Murdock-Bauman. |

Thermal equilibrium enthalpy and the static pressure in the volume upstream of the break junction are used to determine the fluid condition for the selection of the choked flow correlation. Therefore, the slip ratio is equal to 1.0 in the subcooled condition based on the equilibrium enthalpy.

d. Change 6 stated a detailed 24 segment rod model was used. What code used this rod model? Is it referring to axial or radial segments? Will the 24 segment rod model be used for plant EM calculations? If so, is this model supported by sensitivity studies?

Response: Change 6 should have stated "a detailed 24 axial segment rod model of equal size was used in the RELAP5 hot channel". It is to be noted that the section A.3 reflects the changes between the original

RELAP5/MOD2-B&W model and the model used in the final EM studies. This model will be used in the plant EM calculations. This pin model existed in the code model that was used for the final sensitivity studies reported in Section A.3.

e. Change 7 stated there was a switch to the Condie-Bengston film boiling correlation. Does this mean the maximum of the Condie-Bengston and Rohsenow-Choi correlation was no longer used as discussed in Section 4.3.4.9, Volume I?

Response: Change 7 is consistent with the description given in Section 4.3.4-9. In the initial studies using RELAP5/MOD2-B&W the heat transfer coefficient in the film boiling regime is taken as the maximum of CSO and Rohsenow-Choi correlations. In the final studies, the heat transfer coefficient in the film boiling regime was taken as the maximum of Condi-Bengston IV and Rohsenow-Choi correlations as stated in Section 4.3.4-9. Therefore one of the differences between the initial and the final models is the use of Condi-Bengston IV correlation instead of the CSO correlation as stated as Change 7 in Section A.3.

31. Question: In discussing the effect of changes to the RELAP5 base model, Section A.3 stated that using the ECCS bypass model and the allowance of slip at the break shortened the blowdown period by allowing higher energy removal rates from the system. Clarify how this is conservative because by shortening the blowdown period it also shortens the bypass period as the end-of-bypass was defined to be the end-of blowdown.

Response: Appendix K permits two options in the calculation of ECCS bypass flow for cold leg breaks. In the initial model, the injected ECC water during the bypass period was subtracted later from the water remaining in the RV and the inlet lines at the EOB. This method is unrealistically conservative and results in a prolonged adiabatic heatup period. The model was revised to utilize the RELAP5 control variables to calculate and implement the bypass during the bypass period instead of delaying it until the end-of-blowdown.

Provision for slip flow was implemented in the Moody discharge model to maintain a continuity in two phase flow from upstream junctions to the leak path. This permits continuous steam flow and flashing in the upstream volumes. The modeling is now more consistent with the upstream flow calculations which, based on the RELAP5/MOD2-B&W momentum equation, include slip effects.

The result of these changes is not so much a shortened bypass period as it is an increase in the amount of vessel retained liquid at the end of blowdown. Previous to the change in the bypass model, ECCS water injected in the middle of blowdown could be and was forced out of the system during the remainder of blowdown. Integrating the ECCS injection and discarding the water again after blowdown

created a double accounting which usually dried out the reactor vessel. By not double accounting for bypass some liquid inventory generally remains in the reactor vessel at the end of blowdown. That inventory in effect shortens the period of adiabatic heatup. The present models are clearly less conservative than the previous ones. They remain conservative however -- because literally all blowdown ECCS water is actually bypassed -- and are within the Appendix K allowances for these types of models.

The shortened period of blowdown by itself is inconsequential. Provided that blowdown cooling has been unaffected by a shortening of the blowdown period, such a change causes a minor increase in conservatism because the adiabatic heatup and reflood periods occur at higher decay heat levels. In any case, when the spectrum approach is used to define the worst case LOCA, the appropriate most conservative blowdown period will be identified.

32. Question: Section A.3 discussed changes to the REFLOD3B model. Will these changes also be used for plant EM calculations. If so, clarify the bases for the changes.

Response: Since the base EM studies reported in Section A.2 using REFLOD3B, two code changes (Changes 1 and 2 given in Section A.3) and three input model changes (Changes 3 through 5 given in Section A.3) were made. As reported in the Section A.3 the spectrum and the time-in-life studies reported in this section were made after these changes had been made. These changes will also be used for plant EM calculations.

The two code changes have been reported in Revision 1 to the REFLOD3B code topical report BAW-10171. The quench logic change (Change 1) was added to remove a discontinuity in the core heat slab 1 quenching criteria (Appendix F-1 of BAW-10171). The second code change (Change 2) should have stated "Transition and the quasi-steady period core outlet flow modification". The change in the core outlet flow (Section 2.7 of BAW-10171) was made to avoid superheating of the fluid in the upper plenum region (See the description in the subsection - Period I in Section 2.7 of BAW-10171).

The model changes 3 and 4 were error corrections to the base input model. The change 5 was made to make the break junction $C_D$ value calculation consistent with the calculation used in the blowdown model.

33. Question: Section A.3.3 discussed a RELAP5/MOD2-B&W pump degradation study. Maximum and minimum two-phase degradation curves based on previous B&W work were used in addition to the two-phase degradation curve from NUREG/CR-4312. Only slight differences between the three calculations we e noted. However, it appears the bounding curves were developed for pumps in B&W plants. Provide a basis that the bounding curves used in the study are bounding for the types of pumps to be analyzed in Westinghouse and Combustion Engineering (CE) plants.

Response: The reactor coolant pumps (RCPs) for all three pressurized water reactor vendors do not vary strongly. NSS designers have typically subcontracted the pumps either within or outside of their own organizations, which has created a common set of RCP suppliers. The reactor coolant pumps used on McGuire and Catawba (Westinghouse NSS) for example are the same series Westinghouse pumps as those used at Oconee I (B&W NSS). All of the vendors use pumps of approximately the same specific speed and with similar operational characteristics. Reactor coolant pump performance under two-phase conditions is thus not expected to vary widely with the NSS design.

The two-phase degradation data and bounding curves presented in the evaluation model study were taken from the performance of the Semiscale facility pumps during tests under two phase operation. As discussed in sections 6.8.2 and 6.8.3 of NUREG-1230[33.1], the impact of two-phase conditions on the Semiscale pump was more severe head degradation than has been measured for pumps closer in design to full sized RCPs. Therefore the study conducted in the evaluation model report dealt with degrees of pump degradation that were already more severe than those expected during a LOCA. The conclusion of the study was

that the more severe the degradation the more conservative the LOCA results. This resulted in the establishment of one of the higher degradation curves as the curve to be used in the LOCA evaluations. This curve, in light of the NUREG-1230 findings, will clearly be bounding for application over the range of plants considered by the evaluation model.

34. Question: For the break spectrum analysis discussed in Section A.3.8, why was the percentage of gamma energy deposited in the fuel set at 97.3% for the blowdown portion of the accident as opposed to the 95% proposed by B&W for its EM analysis?

Response: During the development of the evaluation model certain phases of development proceeded in parallel. In order to proceed with some of the analyses, values for selected non-critical parameters were taken from the B&W evaluation model for B&W designed NSS. This occurred for the gamma energy deposition factor. A significant portion of the analysis for the evaluation model topical report was completed prior to the establishment of the 95% value. B&W has examined the analysis performed with 97.3%, generally sensitivity studies, and has determined that none of the conclusions drawn are affected by the changed value for the gamma deposition factor. The B&W LOCA Evaluation Model for Recirculating Steam Generator Plants specifies 95 percent, and that value will be used on all future applications.

35. Question: The REFLOD3B analysis of the most severe break case was discussed in Section A.3.10. In point 3 of the section, two areas where the loss of one train of pumped ECC injection would affect the results were discussed. These were keeping the downcomer full and ECCS spillage out the break. This discussion is confusing and the basis for the statement that the downcomer would remain full and the previous REFLOD3B cases valid is unclear. Clarify the meaning of this paragraph and the basis for the conclusions.

Response: In the REFLOD3B analyses, only one train of the pumped ECCS is permitted based on the single failure criterion. This consists of one RHR, one SI and one charging pump. However, the sensitivities presented in Appendix A except the base case (final analysis) were performed with one RHR, two SI, and two charging pumps. The additional high pressure pumps, one SI and one charging, deliver a total of approximately 120 lbm/sec. The arguments of section A.3.10 are intended to show that the excess 120 lbm/sec injection did not increase the core flooding rate.

In REFLOD3B, the driving force for core flooding is the head of water in the downcomer above the bottom elevation of the core. The height of this head is limited to the height of the bottom of the cold leg nozzles. If ECCS water is injected at such a rate that the downcomer would fill above this height, then that water, the excess above the bottom of the nozzle, is assumed by the code to have spilled out of the break. Therefore, the driving head for the reflood rate has an upper bound limit which can not be exceeded. For the original cases discussed in section A.3.10, downcomer fillup occurred during accumulator discharge and the spillage rate off the top of the downcomer was greater than 120 lbm/sec after the accumulator discharge. Thus a reduction in ECCS flow of 120 lbm/sec would not alter the calculated flooding

rate. Although the scale is not good, a comparison of Figures A-64 and A-88 confirms the result.

36. Question: In Figure A-44, the break flow from the reactor vessel side of the break for three core nodalizations was compared. Clarify why the break flow for the 2x12 case was lower than the other two cases in the period from approximately 16 to 20 seconds.

Response: This is caused by a reduction in fluid density in the reactor vessel (RV) side of the break during the time of interest. In the 2 x 12 case, the heat transfer in the three lower volumes in the core increases as a result of the increased axial noding. The core reverse flow into the lower plenum volumes (314 and 312 of Figure A-2) increases due to increased vapor generation as shown in Figure A-46. This, in turn, retards water flow from the lower head (volume 310) into the lower plenum (312). Less water is thus entrained into the downcomer and out the break, and more water remains in the RV at the EOB. The leak flow rate decreases as the mass flow and density from the lower vessel region decrease. Although the core axial noding slightly affected the hydraulic behavior in the lower head and lower plenum region, the overall impact on system response was negligible.

37. Question: Appendix A discussed the results of nodalization studies for RELAP5/MOD2-B&W and REFLOD3B but did not provide the results of similar studies for BEACH and FRAP-T6-B&W. Provide the results of nodalization studies for these codes to demonstrate solution convergence with the nodalizations B&W intends to use in EM analyses.

Response: The BEACH and FRAP-T6-B&W codes are not subject to convergence problems caused by insufficient noding in the manner that RELAP5 and REFLOD3B are. For BEACH and FRAP-T6-B&W the noding arrangements used are governed by the physics modelled in the codes and the degree of detail desired in the answer. For both codes, increases in noding merely refines the average answers received from noding arrangements of lesser detail. Both BEACH and FRAP-T6-B&W employ approximately 21 axial nodes in the evaluation model. This gives an average node height of about 0.6 feet. Resolution of the temperature to a smaller region of the fuel would not alter the results and would only incur unnecessary expense. The following paragraphs present the rationale for the noding detail used in the EM applications of the codes.

The noding arrangement to be used in the BEACH code is not variable in the sense that RELAP5 is. During development of the BEACH code a given arrangement was selected and compared to data for justification. The noding used in any application must comply with that arrangement for the results to be considered as valid results of the BEACH code. The arrangement and approach, which may not dictate the absolute number of nodes, is presented and justified in Appendix C of the BEACH topical report. Question 18 of the first round of questions on the BEACH topical report, BAW-10166, requested further information on noding arrangements as does this question and the response is repeated in part here.

BEACH axial and radial noding is based directly on the basic modeling physics selected for the evaluation of the reflooding process. Although some experiments, such as the CCTF tests, have shown that radial effects within the core during reflood promote extra cooling in the hotter regions, the B&W Evaluation Model simulation of this process is one-dimensional. The BEACH model is a single stack of fluid volumes oriented vertically and representing the average hydrodynamic conditions within the hot fuel assembly. Under this assumption, the boundary conditions for BEACH are taken from the REFLOD3B simulation of the average core reflooding. The addition of a second radial channel to BEACH would only replicate the present solution unless the driving boundary conditions were to include radial variability. Thus, until a more sophisticated approach is adopted there is no need to conduct radial noding sensitivity studies for the BEACH code.

Axially the noding is again selected in accordance with the physical modeling. In this case, the noding is tied to the grid models and the distribution of grids within the fuel assembly being studied. All present B&W-designed fuel assemblies would call for the same axial noding as was used in the FLECHT benchmarks. The axial noding, detailed in Appendix C of the BEACH topical report BAW-10166, is a series of seven, three-node groups. As each of the three nodes within an axial group experiences a different cooling pattern, it is necessary to use three nodes. Additional nodes, however, do not substantially alter the results.

The response to BEACH topical question 18 continues to describe some of the alternate noding schemes that were used at one time or another during BEACH development, what was observed from each, and how they lead to the

current 21 node approach. The response finishes with:

It is concluded from this series of models that:

1. The three regions of grid effects on heat transfer must be modelled by individual nodes.

2. Once each region is modelled, there is little change to the results with additional nodes.

3. The 21-node grid model is adequate for LOCA evaluation model predictions.

Although 21 nodes were used in the benchmark evaluations, the requirement is to use at least a three-node pattern between grids for grid spans near the location of peak cladding temperature. The 21-node model is just the result of applying such a pattern uniformly over an assembly with seven grid spans. The modelling of the assembly below the first interior grid or above the last interior grid need not follow the three-node pattern. These regions of the assembly, being on the very ends are well removed from the locations of peak cladding temperature, may be modelled with fewer or more than three nodes. The selection is made by the user of the evaluation model and is usually based on a desire to preserve an approximately consistent node height along the fuel assembly. Between the lowest and highest interior grids the user must employ a three-node pattern per grid span. Reference to Appendix C of the BEACH topical and to the response to question 18 of the first round of questions on that topical can be made for further detail.

Because of the grid induced heat transfer pattern during reflood, the FRAP-T6-B&W noding for large break evaluation model applications has been selected to correspond to the BEACH noding. This scheme is also acceptable for the blowdown

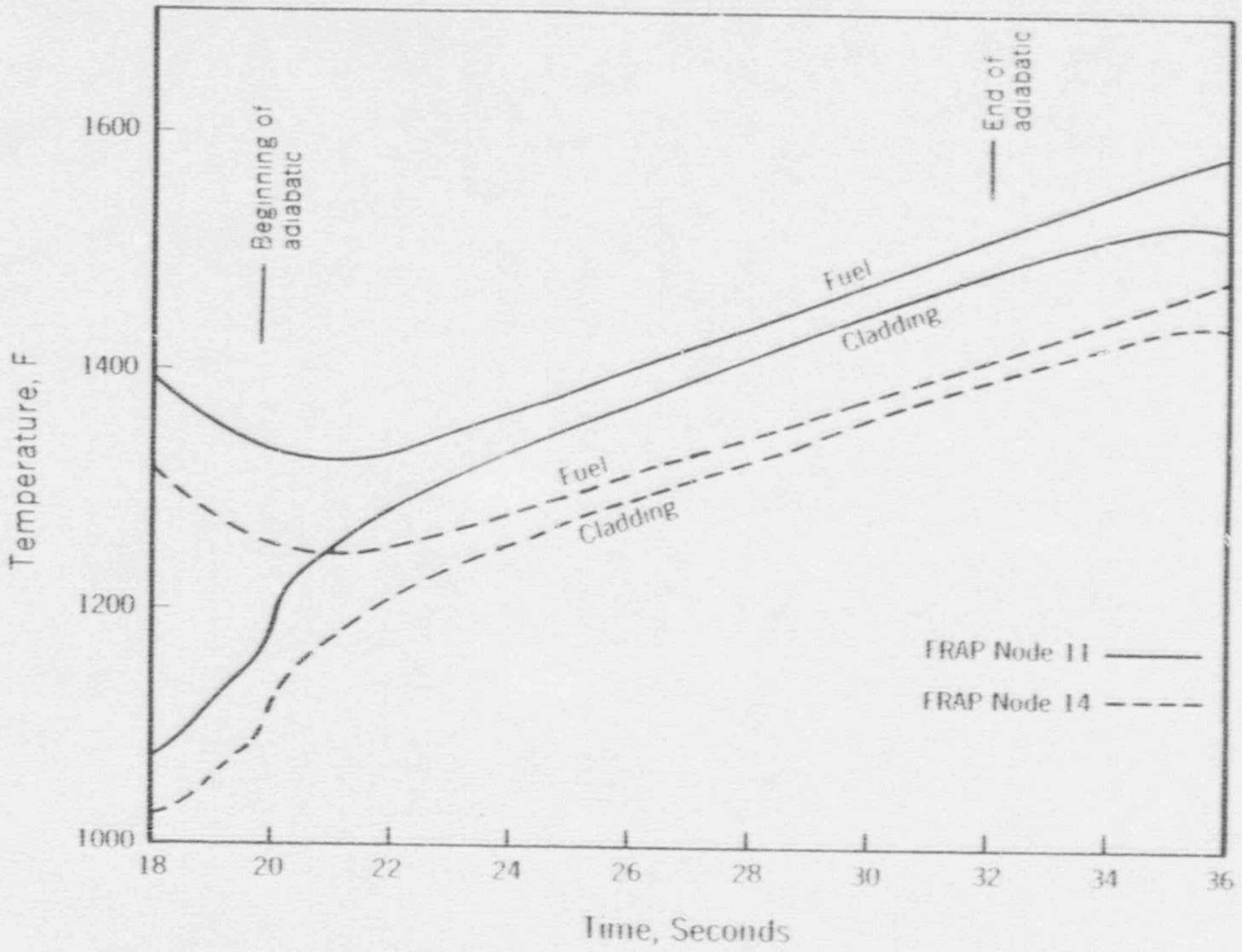
and adiabatic heatup periods because axial variations in boundary conditions within the resultant, 0.6 foot, node height are not strong and axial heat conduction insignificant. Therefore, no noding studies are indicated for the FRAP-T6-B&W code.

For small break evaluations the prediction of cladding temperatures at 0.6 foot increments is sufficient for the purposes of compliance with 10 CFR 50.46. The motivation here is more one of model assurance than of heat transfer performance. Grid effects are not considered to be dominant in the steam only flow that occurs during core uncovery for small breaks and the three node per grid span rule is not required. Using the same model as for large breaks, however, may result in some economies of model preparation and limit the opportunities for erroneous inputs. Therefore, the same noding as for large breaks will be used when the core uncovery transient lasts for under 500 seconds. For small break LOCA evaluations with core uncovery times in excess of 500 seconds nodes which continually lie below the mixture height may be combined to provide faster running times.

38. Question: In Figure A-90, there appears to be a change in the rate of the fuel rod temperature increase during the refill period. Clarify the reasons for this change in slope because the rods are said to undergo an adiabatic heatup during the refill period.

Response: Figure A-90 of BAW-10168 shows a change in the rate of temperature increase for the cladding at about 20.4 seconds or about one second after blowdown. This effect is caused by the cladding and the fuel coming into approximate equilibrium with each other at slightly after 20 seconds and then continuing to heat-up. Figure 38-1 provides the cladding and fuel average temperature histories during the adiabatic period for FRAP-T6 nodes 11 and 14. Just prior to the end of blowdown, cooling of the cladding has been sufficient to support a two to three hundred F difference between the fuel average temperature and the cladding surface temperature. At end-of-blowdown, 19.6 seconds, this difference is 170 F for node 11 and 175 F for node 14. The immediate response of the cladding is to rise to near equilibrium with the fuel. At 22 seconds, the fuel-to-cladding temperature difference is 40 F for node 11 and 45 F for node 14. In turn the fuel temperatures have decreased, but by only about 10 F, creating a cladding temperature rise rate of 50 F per second. Thereafter, until the end of adiabatic heatup, the cladding and fuel temperatures rise together, separated by about 30 to 35 F, at a rate of approximately 20 F per second.

Figure 38-1 FRAP-T6 Fuel and Cladding Temperatures



39. Question: The following questions are related to the time-in-life study discussed in Section A.3.9.

a. The study was performed with a peak linear heating rate of 13.2 kW/ft. Justify the validity of the results of this study for other peak linear heating rates.

Response: The purpose of the study is to establish the general relationship between burnup and peak cladding temperature (PCT) and not any absolute value of PCT. The results demonstrated that the initial fuel stored energy is the most dominant factor in influencing the PCT. This in turn is used during the application of the evaluation model to select the burnup, that for which the fuel temperature is the highest, at which the application will be run. The study is valid for all linear heating rates so long as 1) it was performed in a manner which involved the dominant LOCA phenomena for each member of the study and 2) the burnup dependency of the studied variables is well behaved. Using 13.2 Kw/ft as the local heating rate fixed the cladding temperatures achieved during the study at values approaching the criteria of 10CFR50.46. Therefore, all significant LOCA phenomena were involved in the course of each evaluation. The variation of the variable parameters; pin pressure, fuel temperature, and cladding inner and outer initial oxide layers; is well behaved with no singularities which could altered the observed trends. Therefore, the results of the study are considered valid for all peak linear heating rates so long as the occurrence of cladding rupture remains within the post blowdown period. Should the peak linear heating rate or the internal pin pressure be increased to the point where cladding rupture occurs

during blowdown, the phenomenon of blowdown flow diversion caused by rupture related flow blockage would become involved in the prediction. As the present study did not involve that phenomenon, it can not be considered as valid when that phenomenon is important to the outcome of a calculation. A similar result, incidentally, is observed in studies on B&W fuel in B&W designed plants at peak linear heat rates of 18.0 Kw/ft.

b. Provide the basis on why the results of the time-in-life study are valid for each fuel reload in the fuel cycle.

Response: The purpose of the LOCA calculation is to determine the maximum local heating rate at which any fuel assembly in the core can be operated and remain within the criteria of 10CFR50.46. This study shows that doing the LOCA evaluation for the cycle and the burnup within a cycle for which the maximum initial volume averaged fuel temperature occurs will result in predicting the highest peak cladding temperature for any fuel assembly in the core. To do this the study encompassed all cycles through which a batch of fuel will proceed during its useful life.

That this study remains applicable for differing batches of fuel depends on the design changes involved in the new fuel batch. If the new batch involves any design changes, those changes are reviewed for the applicability of all licensing and performance calculations and the effected calculations repeated. During this review, the burnup dependency of the new fuel would be compared to that for which the LOCA evaluation, and reference burnup sensitivity study, had

been performed. If the dependencies do not correspond to those upon which the LOCA evaluations are based then the studies are repeated and a new or upgraded burnup sensitivity study provided as a base for the LOCA evaluations.

c. Show for all reloads in the fuel cycle that the end-of-life (EOL) case with peak rod pressure does not result in a blowdown rupture and a higher PCT than the beginning-of-life (BOL) maximum stored energy case.

Response: The burnup sensitivity of Appendix A showed that the initial fuel temperature dominates the initial pin pressure in controlling the occurrence of rupture. This will remain true so long as the study of Appendix A can be applied to the fuel being evaluated. One critical point to the studies applicability is that the rupture always occur after the end-of-blowdown. Therefore, the initial fuel temperature, initial pin pressure, and blowdown cooling capability of the plant fuel combination will be reviewed to assure that conditions at the end of life can not cause a blowdown rupture of the fuel pin. Such comparisons and arguments to this point as are necessary will be documented on an application specific basis. If arguments based on existing evaluations can not clearly establish that a blowdown rupture will not occur, then an end of life application of the evaluation model will be made to check that condition for possibly being the worst case.

d. In discussing the comparison of the 40000 MWD/Mtu case and the BOL case, Page A-17 stated, "The results of that hot fuel rod heatup calculation indicate that the technique used by B&W for post-blowdown heat transfer

calculations is adequate and provides reasonable accuracy for this study." Clarify the meaning of the sentence. Provide additional discussion on the comparison of the two cases to support the conclusion made in the sentence. Will the approach discussed be used in EM calculations.?

Response: The approximation method described on page A-17, for obtaining the reflood heat transfer coefficients, was used to reduce computational expenses. It will not be employed for any plant specific applications. For those both REFLOD3B, core flooding rate, and BEACH, reflood heat transfer coefficient, calculations will be made for each case.

The approximation used in the burnup study was to apply the reflood HTCs for the 100 MWD/Mtu case to the 1000, 40000, and 60000 MWD/Mtu cases. This eliminated the need to make 3 REFLOD3B and 3 BEACH runs. To provide assurance that the approximation was reasonable for use in the study, REFLOD3B and BEACH runs were made for the 40000 MWD/Mtu case and compared to the their respective approximations. The results of that comparison showed that the actual results and their approximations were in relatively close agreement. Most important was the agreement between the two reflood heat transfer coefficients, HTCs. The variation between the actuals and the approximations was too small to alter the trends observed in the burnup sensitivity study. The wording in the sentence on page A-17 should perhaps have read, "the approximation, used by B&W for this study, for the post-blowdown heat transfer calculations provides reasonable accuracy and is adequate for the purposes of the study."

40. Question: The sensitivity studies (such as pressurizer location, loop nodalization and break flow nodalization studies) in Appendix A were performed using the base nodalization and a particular set of user input options. Discuss the potential for the results of the sensitivity studies to be affected by the input to the model, and justify the applicability of the results of the studies to all the plant types to be analyzed. For example, would the results of these studies be different for the different Westinghouse designs (three or four loop plants) and CE designed plants because of different input?

Response: The evaluation model is a guide to the application of a series of codes for the purpose of calculating the response of the reactor system to a hypothetical loss-of coolant accident. The model rests for its base on the sound application of the physics of the problems solved, direct experimental correlation, selected integral experiments, theory, sensitivity studies, peer review, and selected conservatisms both dictated and unilateral. For large breaks the transient is characterized by rapid, violent system depressurization that can produce large gradients in fluid conditions throughout the system. These, in turn, affect the depressurization rate (flashing), core flows and cooling, and leak flows. The sensitivity studies are performed to understand the interaction of parameters and to determine their generalized impact on the overall system responses. This understanding and the generalized interaction of parameters is broadly applicable to all light water reactor and fuel designs. Only the specific result of a sensitivity study will change from plant to plant or fuel design to fuel design.

The studies performed for the B&W recirculating steam generator plant evaluation model, provide both generalized information and specific results. As mentioned the

generalized information can be directly applied to plant configurations or designs which differ from the base on which the sensitivity study was performed. To the extent that a specific result is important to a LOCA evaluation, however, that result may have to be redetermined for an alternate plant design and would certainly need to be justified.

The following discussions of each of the sensitivity studies documented in Appendix A of BAW-10168, the B&W RSG Evaluation Model topical, develop the generalized and specific results, knowledge, gained from the study and documents the extent to which the studies will be applied to alternate plant designs. For convenience, the studies are listed in the order that they appear in Appendix A and by the applicable subsection number. Three characterizations of the studies have been developed:

1) Generic: The study developed or verified general guidance, is broadly applicable, and will be applied to the alternate plant types covered by the evaluation model.
2) Confirmable: The study developed or verified general guidance that is expected to apply broadly but for which judgement is reserved pending the results of a base case. For these studies, application of the evaluation model to the alternate design will be made and parameter behavior compared to the reference sensitivity study base to confirm that sufficient uniformity of results exist to apply the study to the new design. The rationale and reasoning used to justify the application of the study to an alternate plant type will be presented in the application topical for the plant type for NRC review and approval. If the study cannot be applied or extended and the information is still required for the application of the evaluation

model, it will be repeated with the first applications report for that plant type.

3) Specific: The study developed or verified a specific result and is therefore not broadly applicable. If the study cannot be extended or justified and the information is still required for the application of the evaluation model, it will be repeated with the first applications report for the plant type.

### A.2.1. RELAP5/MOD2-B&W Time Step Study

Generic. This study verified in a light water reactor geometry that the RELAP5 time step controller governs the code solution sufficiently to assure converged results. Alternate system designs within the group to be covered by the evaluation model will not change that result.

### A.2.2. RELAP5/MOD2-B&W Loop Noding Study

Generic. This study verified the general noding requirements within the loop for recirculating steam generator plants. In conjunction with the break noding study the results can be applied to the separate regions of the hot leg, the steam generator, and the cold leg. Alternate system designs within the group to be covered by the evaluation model will not change the noding requirements.

### A.2.3. REFLOD3B Loop Noding Study

Confirmable. This study verified the noding detail used in the REFLOD3B code. It is applicable to plants with a one to one correspondence of hot and cold legs. A separate study will be performed for the first

application to the Combustion 2-by-4 design to confirm the noding detail. For other system designs within the group to be covered by the evaluation model, the results of this study will be applied.

A.2.4. REFLOD3B Primary Coolant Pump Rotor Resistance Study

Generic: This study showed a considerable reduction in flooding under a locked rotor assumption. The study affirms the generally accepted data on loop resistance effects on reflooding rates and will be applied for all system designs covered by the evaluation modal.

A.2.5. REFLOD3B Maximum ECCS Injection Study

Specific. Until recently, the prevailing results have shown that the maximum ECCS case will be more severe. The study will be performed on a plant specific basis.

A.3.1. RELAP5/MOD2-B&W Break Nodiring Study

Generic. Per Appendix K of 10CFR50.46, for design basis LOCA's the break location is always within one of the reactor coolant system pipes or one of the attached pipes. This study verified that hydraulic stability is achieved by providing at least one control volume in the pipe between any adjacent component and the break node. The study is applicable to all plants covered by the evaluation model.

### A.3.2. RELAP5/MOD2-B&W Pressurizer Location Study

Generic. Although the assumption placing the pressurizer in one of the intact loops was somewhat conservative, this study showed that there is little difference in results when the pressurizer is modelled in the broken loop. The lack of sensitivity to pressurizer location is expected to hold for all designs covered by the evaluation model and this study will be not be repeated.

### A.3.3. RELAP5/MOD2-B&W Pump Degradation Study

Confirmable. This study established a most severe pump degradation multiplier by altering the pump effects on the core flow. Although it is expected that the LOCA core flow histories for all plants covered by the evaluation model will be similar, that should be established prior to adopting the results of this study. A base evaluation of any alternate design will be conducted on the first application of the evaluation model to that design. If the core flow history of the new design generally agrees with the history of the reference design, this study will be applied to the new design. On the other hand, if it does not, the study will be repeated.

### A.3.4. RELAP5/MOD2-B&W Core Noding Crossflow Study

Generic. This study verified that cross flow in a light water reactor is limited and does not alter the course of a LOCA evaluation substantially. The study is dependent only on the very basic aspects of the fuel design, which are consistent across the range of designs to be considered, and will be applied for all system designs covered by the evaluation model.

A.3.5. RELAP5/MOD2-B&W Core Noding Study

Generic. In conjunction with the core cross flow study this study verified that the modelling of light water reactor core in six axial segments with a hot and an average channel provides sufficient spacial detailing for both model convergence and results accuracy. As the basic core arrangement and fuel design is not altered across the range of designs to be considered, the results of the study will be applied for all system designs covered by the evaluation model.

A.3.6. FRAP-T6-B&W Time Step Study

Generic. This study verified that the time step selection for FRAP-T6-B&W provided converged results for the spacial detail modelled in the base runs. Because, as per the discussion for the next study, the spacial detail required for the FRAP-T6-B&W model will not be altered for the other designs covered by the evaluation model, this study will remain valid and be applied for all designs.

A.3.7. FRAP-T6-B&W Radial Fuel Segmentation Study

Generic. This study verified that the number of solution points selected for radial representation of the fuel pin used by the base FRAP-T6-B&W model was adequate. The study is dependent only on the very basic aspects of the fuel design, which are consistent across the range of designs to be considered, and will be applied for all system designs covered by the evaluation model.

A.3.8. Spectrum Analysis

Specific. This study is not actually a sensitivity study from the standpoint of input selection for the evaluation model, but was included to show that the sensitivity studies performed were done with break size and location which, if not the actual worst case, were very close. A minimum of three break sizes will be analyzed on the first application of the evaluation model to any new design. If a larger spectrum is required to identify the worst case LOCA then it will be performed.

A.3.9. Time-in-Life-Study

Confirmable. This study showed that the initial fuel temperature dominated the results of variable variation across the range of burnups currently being licensed. The results will remain valid so long as the maximum burnup or the fuel design is not altered such that a high pressure fuel pin can be made to rupture during blowdown (all ruptures occurred in the post blowdown period for the study). If an examination of the fuel performance and a base run for the alternate design can-not assure that the end-of-life fuel pin conditions will not result in a blowdown rupture, a specific calculation of the condition will be performed.

A.3.10. Most Severe Break Case

Specific. This study is not a sensitivity study and was included only to show cumulative impact of the sensitivity studies and model updates performed. Any application of the evaluation model will include a unique affirmation and calculation of the most severe break case.

41. Question: Section 4.3.4.9 and Revision 1 to BAW-10164P indicated a change of the film boiling correlation in the EM core model from the CSO correlation to the Condie-Bengston correlation. One of the benchmark calculations performed for RELAP5/MOD2-B&W described in BAW-10164P, Semiscale Test S-04-6, was run before this change. Also, the Test S-04-6 EM calculation used the homogeneous slip option for calculating the phasic velocities from the break flow. BAW-10168P states that the Moody slip model will be used in this calculation. Therefore, repeat the Test S-04-6 EM calculation with the modified core heat transfer package and the Moody slip break flow model and provide the results.

Response: In order to respond to question 12 of the first round of questions on RELAP5/MOD2 - B&W (BAW-10164), the Semiscale MOD-1 test S-04-6 was reanalyzed using the revised EM options including the modified core heat transfer package and the Moody slip break flow model. As a result Appendix G.1 of BAW-10164 was rewritten using the updated benchmark and submitted as the response to question 12. This response supercedes the original Appendix G.1 of BAW-10164. The work was transmitted to the NRC by letter form J. H. Taylor of B&W to J. A. Norberg of NRC, dated November 23, 1988.

42. Question: One of the major modifications to the RELAP5/MOD2-B&W base code was the addition of the core EM heat transfer package. The only assessment of this new model presented in BAW-10164P was the EM calculation of Semiscale Test S-04-6. Describe what, if any, other methods have been used to verify proper implementation of the new model in the RELAP5/MOD2-BAW code and adequate calculation of the core heat transfer.

Response: In addition to the Semiscale MOD-1 LBLOCA Test S-04-6, Semiscale MOD-2C SBLOCA Test S-LH-1 was simulated using RELAP5/MOD2-B&W. The EM core heat transfer and choked flow model options were used in the Test S-LH-1 simulation. The results were submitted to the NRC as the response to Question 17 related to the RELAP5/MOD2-B&W code topical report BAW-10164.

The EM core heat transfer model described in the RELAP5/MOD2-B&W code topical report BAW-10164 was implemented in the code in accordance with the B&W established procedure for the code development. This procedure includes detailed hand calculational checks and sample test runs to verify that the model is implemented correctly as described in the code manual or in the code topical report. The B&W Q/A procedure for code development is further discussed in the response to Question 1a. of the second round of questions on the RELAP5/MOD2-B&W topical report BAW-10164.

The benchmarks and the B&W Q/A procedures for code development are sufficient to verify that the EM core heat transfer model is properly implemented in the code.

43. Question: With regard to the LBLOCA assessment of Semiscale Test S-04-06 using RELAP5/MOD2-B&W described in Appendix G of BAW-10164P:

a. Page G-7 stated, "The Cycle 36.04 pressure response near the broken loop simulated pump suction side, as shown in Figure G.1-6, supports the conclusion made from Figure G.1-4 that the HPI flow rate difference is the cause for the prediction of higher pressure than the data in the 1.0 to 8.0 second time period." Clarify how Figure G.1-6 supports the conclusion made regarding the pressure comparison in Figure G.1-4. Also, why were the measured and input HPI flow rates different?

Response: In response to Question 12 of first round of questions on the RELAP5/MOD2-B&W topical report, Semiscale MOD-1 Test S-04-6 was reanalyzed. Section G.1 of BAW-10164 was rewritten and submitted to the NRC. Therefore the response to this question will be based on the reanalysis results.

Figures G.1-4 and G.1-6 are replaced by Figures 12.5 and 12.7 of Question 12, and page G-7 is replaced by page 18 in response to Question 12.

The input HPI flow rates are the average values from the measured data, and these input values are the same as those used in the RELAP4 model given in Reference 7 of BAW-10164. The base case prediction of higher pressure near the pump side, shown in Figure 12.5, cannot be explained by the difference in the HPI flow rates. The difference can be due to the difference in the calculated versus measured ECCS flow rates and due to the interaction of the ECCS fluid with the system fluid near the break location. From Figures 12.5 and 12.22 it can be observed that the pressure in the EM case started

deviating from the data after the accumulator injection started, which seems to indicate the effect of condensation. Therefore, the following changes to the text are made.

The sentence, " The difference between the measured and the input values of the HPI flow rates near this break location is the cause of this difference," is to read, " The difference between the measured and the calculated values of the ECCS flow rate and the interaction of the ECCS water with the system fluid near the injection location is believed to be the cause of this difference."

The sentence, "The Cycle 36.04 pressure response near the broken loop simulated pump suction side, as shown in figure 12.7, supports the conclusion made from Figure 12.5 that the HPI flow rate difference is the cause for the prediction of higher pressure than the data in the 1.0 to 8.0 second time period," is to be replaced by, "This suggests that the primary system pressure is strongly influenced by the vessel side break than the pump side break as shown in Figures 12.4, 12.5 and 12.7."

b. Page G-9 listed the differences in the core flow calculated by the base and EM cases as compared to the test data. Clarify the reason or reasons for the differences in core flow shown in Figure G.1-18.

Response: Figure G.1-18 is replaced by Figure 12.20 and page G.9 is replaced by page 21 of the answer to Question 12.

From the Figure 12.20 it can be seen that both the base case and the EM case predicted generally lower flow rates in the lower plenum (hence in the core) than the data. The effect of this lower flow rate prediction is reflected in the prediction of higher cladding temperatures as shown in Figures 12.32 and 12.33.

In Test S-04-6, the pump was coasted down from 2400 rpm to 1500 rpm which was held constant for the duration of the transient. The flow rate into the downcomer from the intact loop cold leg, shown in Figure 12.19, is a measure of the pump performance. From Figure 12.19 it can be seen that the code correctly predicted the flow rate for about 6 seconds. From 6 to 14 seconds, the code generally calculated lower flow rates than the data observed.

The vessel side break flow rate is shown in Figure 12.14. The difference between this break flow rate and the intact loop flow rate into the downcomer is provided by the downcomer. In the calculation, the downcomer was found to be providing more flow to the break than it received from the lower plenum. For example, in the EM calculation the break flow was higher than the data after about 7 seconds. During this period the flow rates into the downcomer from the intact loop (Figure 12.19) and from the lower plenum (Figure 12.20) were lower than the data. Therefore, the downcomer would be voiding slightly faster in the prediction than in the test.

During the early part of the transient the lower plenum flow rate reflected the vessel side break flow rate. From 0.0 to about 1.5 seconds the EM break flow rate was higher than the data which was reflected in the lower plenum flow rate (Figure 12.20). From about 1.5 to 2.5 seconds the EM break flow rate was lower than the data.

As a result, the calculated flow rate in the lower plenum was lower than the data during this period.

c. The calculated and measured fluid temperatures at the core inlet and in the upper plenum were compared in Figures G.1-28 and G.1-29. Although superheated vapor was calculated in both the base case and EM analyses, neither is as high as the amount of superheat in the test. Clarify the reason for this difference. Could this difference affect the ability of RELAP5/MOD2 - B&W to properly calculate temperatures at the end of blowdown in a licensing calculation?

Response: Figures G.1-28 and G.1-29 are replaced by Figures 12.30 and 12.31 of the answer to Question 12.

The important parameter from the licensing point of view is the cladding (fuel rod) temperature. The parameters that affect the core heat transfer (and hence the cladding temperature) are the core flow rate and the core fluid temperature. For a conservative cladding temperature prediction, the core heat transfer to the fluid should be lower. As explained in the previous section, as part of question 42.b, both the base and the EM models predicted lower core flow rates and higher cladding temperatures than the data during the blowdown period and therefore both predictions are conservative for licensing applications. The lower core flow rates and lower core heat transfer will affect the core fluid temperature. It is to be noted that it would be difficult to obtain the average fluid temperature condition in regions like the lower plenum and the upper plenum using one or two thermocouples. Therefore, the calculated fluid temperatures shown in figures 12.30 and 12.31 do not actually represent the local measurements.

From Figures 12.30 and 12.31 it can be observed that in the test as well as in the prediction the fluid in the lower plenum and in the upper plenum remains saturated during the major portion of the blowdown period. The differences between the measurements and the prediction near the end of blowdown can be attributed to the reasons explained above. Indeed, from Figure 12.30 it can be seen that the calculated vapor temperature in the lower plenum does show superheated steam conditions near the end of blowdown.

The effect of the superheating predicted by RELAP5/MOD2-B&W in the core region on licensing calculations is limited to the fluid temperature used by FRAP-T6 for the last one or two seconds of blowdown. The initial condition for the core in REFLOD3B is selected as saturated steam for conservatism. The density of saturated steam, being higher than that of superheated steam, allows a larger core steam mass which superheats in the loops once reflooding starts. This causes a higher degree of initial steam binding than would occur if the core was initialized in a superheated condition and slightly retards the initial flooding rates. The FRAP-T6 evaluation after blowdown is adiabatic during the refill period and uses BEACH heat transfer coefficient that are normalized to the saturation temperature with the fluid temperature assumed to be the saturation temperature after the refill period. The BEACH code is initialized with superheated steam in the core at the same temperature as the cladding. Therefore, except for the last one or two seconds of the blowdown the prediction of superheat in the core region is not used in the prediction of the cladding temperature and does not effect those results. The acceptability of

RELAP5/MOD2-B&W for licensing calculations is demonstrated by the prediction of conservative cladding temperatures as shown in Figures 12.32 and 12.33.

44. Question: Section 4.3.1 of Volume I stated that the core and core boundary are modeled as homogeneous-equilibrium volumes. Though the RELAP5/MOD2-B&W-EM analysis of Semiscale Test S-04-6 using the homogeneous-equilibrium model and nonhomogeneous-nonequilibrium core models both calculate the presence of superheated steam in the core during blowdown, neither calculation showed as much superheat as the S-04-6 test data. To verify that the proposed core model does not distort the end of blowdown conditions, repeat the core nodalization study assuming the core and core boundary nodes are nonhomogeneous-nonequilibrium and provide a comparison of these calculated results with the earlier study.

Response: As explained in the response to Question 43b and 43c, in the simulation of the Semiscale Test S-04-6 the net effects of core fluid temperature, core flow rates, and core heat transfer in both predictions are higher cladding temperatures than the data as shown in Figures 12.32 and 12.33. Indeed, the use of EM options that included the equilibrium and homogeneous fluid conditions in the core regions resulted in very highly conservative cladding temperatures. The equilibrium and homogeneous fluid conditions were selected in RELAP5/MOD-2-B&W in the core region to properly interface with the FRAP-T6 code. FRAP-T6-B&W uses homogeneous equilibrium fluid conditions in the fluid channel, and it is designed to predict conservative results under these limitations in the fluid channel. Therefore, the same fluid conditions need to be used in the system code, RELAP5/MOD2-B&W, for consistency in boundary conditions. Furthermore, as explained in the response to Question 43c, local superheating toward the end of blowdown will not influence the boundary conditions used in the other codes of the evaluation sequence excepting for the sink temperature during the last one or two seconds of blowdown.

As the cladding predictions are conservatively predicted under the equilibrium treatment utilized in the evaluation model, there is no need to conduct further sensitivity studies.

45. Question: RELAP5/MOD2-B&W is based on RELAP5/MOD2, cycle 36.04. Since versions 36.05 and 36.06 have been released and included a number of error correction updates from version 36.04, discuss whether any of these error correction updates have been implemented in RELAP5/MOD2-B&W. If so identify the updates. If not, justify not including the update, and discuss what impact not including the update had on past benchmark and demonstration calculations as well as what impact it could have on future licensing calculations.

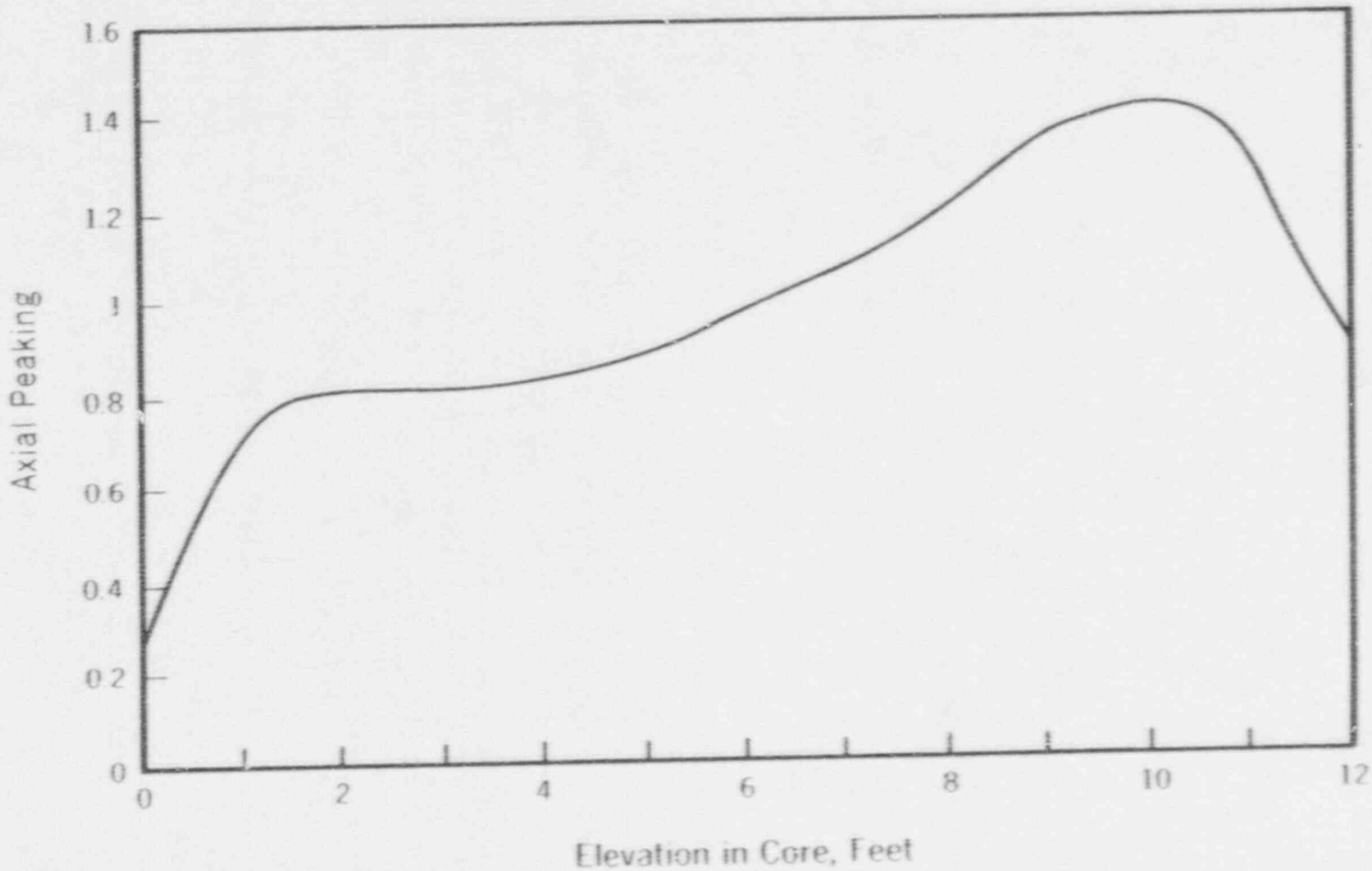
Response: B&W has reviewed the revisions to RELAP5/MOD2 contained in cycles 36.05 and 36.06 and has implemented all revisions contained in the 36.05 update. The 36.06 updates are for applications on a CRAY computer, are therefore inappropriate for RELAP5/MOD2-B&W, and have not been incorporated.

46. Question: Section 4.3.1.2 of Volume II stated that an outlet skewed power profile, consistent with technical specification limits, would be used to calculate the PCT. Will this same outlet skewed power profile be used in the RELAP5/MOD2-B&W analysis? If not, provide justification for not using the same power profile in the analysis. Has sensitivity study been performed to determine the limiting power profile, or is the limiting power profile to be determined on a plant specific basis? If not, provide additional information regarding the specific power shape to be used in the B&W EM analyses and justification that this power profile is limiting and bounding for all plants to be analyzed.

Response: The outlet skewed power profile indicated in the section will be used for RELAP5/MOD2-B&W, FOAM2, and FRAP-T6. The selection of an outlet skewed power shape for small break LOCA, is based on the phenomena of the accident and not on sensitivity studies. Cladding temperature excursions during SBLOCA occur primarily because there is a period of time during which the ECCS systems are not able to provide injection at a rate greater than core boiling. This leads to a two-region core with the lower part experiencing pool nucleate boiling and the upper portion being cooled by the steam generated in the lower portion. An outlet skewed power profile is selected because (1) the skew places less power below the swell level and thus produces a lower swell level and steaming rate, and (2) more power is placed in the uncovered portion of the core causing higher temperature excursions.

As with other areas of evaluation, the selection of the specific power shape is a combination of pragmatism and conservatism. The axial power distribution shown in Figure 46-1 is a somewhat realistic shape that provides an axial peak of 1.4 at the 10-foot core level while limiting the power below the core midplane. The shape both focuses power at the hot spot and limits the boiling below the mixture level. It is consistent with those used in previous recirculating steam generator SBLOCA evaluations and may be found in both the McGuire and Catawba FSARs. Figure 46-1 will be used for all SBLOCA evaluations. The boil-off evaluations will use this axial shape with the average core radial peak for inventory loss and steam flow. The hot channel cooling calculations will use this shape with a radial factor sufficient to match the plant technical specification limit on total peaking at the 10-foot level.

Figure 46-1 SBLOCA Power Shape



47. Question:

a. Section 4.3.1.4 of Volume II stated that provisions are made to conservatively calculate the effects of rod insertion through the use of a minimum tripped rod worth and appropriate delay and insertion times. Will the values used for these parameters be justified on a plant specific basis? If not, provide additional information including the specific values for these parameters to be used in B&W EM analyses and justification that these parameters are conservative for all plants to be analyzed. Also, Section 4.3.1.4 did not discuss the reactivity coefficients for the fuel temperature, moderator temperature and void to be used in the kinetics model. Clarify how the Appendix K requirement to use the minimum plausible values for these coefficients is met in the B&W SBLOCA EM methodology.

Response: For small break LOCA the dominant reactivity effect controlling plant shutdown is the insertion of the rods into the reactor core. Because the liquid density within the core changes slowly relative to rod insertion times, the moderator feedback is relatively insignificant. The Doppler effect is more important but remains substantially less significant than that of the tripped rods. Furthermore, power generation during the initial period of a small break transient is not critical until after the steam generators become ineffective.

The values used for these parameters are plant-specific and validated with each reload. The moderator void and Doppler coefficients are the same as those used for the large break evaluation appropriate for the plant and

fuel being licensed (see the response to Question 3). The rod worth and insertion times are taken from the safety analysis performed for the safety analysis report applicable to the plant and fuel being licensed under the assumption that the most reactive rod is stuck and does not enter the core. These rod worths are conservative with considerable margin available and are verified with each reload report. For the rod worth, rod worths for the cycle are calculated and compared to those used in the safety analysis report to assure that the calculated worths are higher than those used in the analysis. For the insertion times, an actual test is performed at the plant prior to the return to power after each refueling.

b. The section also stated that, "after the end-of-blowdown, the core is assumed to be shut down by the fully inserted control rods and the use of borated water for recirculation." Because the end-of-blowdown usually describes the first portion of a LBLOCA, clarify the meaning of end-of-blowdown as it applies to a SBLOCA. How is the above statement implemented in EM calculations:

Response: B&W concurs that the terms blowdown and end-of-blowdown do not have clear meanings when applied to small break LOCA. The meaning of Section 4.3.1.4 is that the refill of the plant and core during SBLOCA will be by borated water and that the rods plus the borated water will keep the reactor subcritical as it cools and the Doppler effect adds reactivity.

48. Question: Section 4.3.1.7 of Volume II stated that a power distribution factor of 95% would be used during the blowdown phase of the analysis to evaluate the power for the hot channel analysis (RELAP5/MOD2 - B&W). Because the RELAP5/MOD2-B&W SBLOCA model does not include a hot channel, clarify the meaning of this statement.

Response: The statement in section 4.3.1.7 of Volume II of BAW-10168 is wrong. The first paragraph of this section should read as follows:

> As the thermal hydraulic model (RELAP5/MOD2-B&W) for the small break LOCA evaluates only the average core, there is no provision for a hot channel power distribution factor in this model. A power distribution factor of 0.95 is used during the analysis for the hot channel using FRAP-T6-B&W. This factor is based on the consideration that 5.0 percent of the fission energy is deposited directly in the coolant rather than in the fuel or cladding; thus, its heat is not transmitted through the cladding in the removal process.

49. Question: Section 4.3.1.8, Vol. II, did not discuss whether the SBLOCA EM method calculates the metal/water reaction on the cladding inside surface if rupture is calculated. Clarify how the Appendix K requirements that the metal water reaction be calculated on the inside surface no less than 1.5 inches each way from the rupture location are met.

Response: The effects of rupture on the results of small break LOCA calculations are addressed in this response for the computation of peak cladding temperature and in the responses to Questions 53 and 55 for rupture influences on other aspects of the model. The cladding temperature response for the small break LOCA, is determined by application of the FRAP-T6 computer code. FRAP-T6 will be used in a full pin simulation and will provide for the computation of rupture should conditions warrant. Once rupture has occurred all of the implications of rupture appropriate for the cladding temperature calculation will be included. The cladding will be expanded with the resultant decrease in the fuel clad gap coefficient, the cladding surface area will be increased according to the rupture strain, the cladding and oxide layers will be thinned, and metal water oxidation will be computer on both the inside and outside of the cladding for the ruptured node. As the noding detail for the SBLOCA model is the same as for the LBLOCA model above the mixture height, the node that is exposed to inside oxidation will be approximately 0.6 feet high, about 7 inches. The treatment thus accorded the rupture effects on the peak cladding temperature calculation fully addresses the requirements of 10CFR50.46 Appendix K.

50. Question: In discussing compliance with Appendix K requirements regarding the noding near break and ECCS injection points (Item I.C.1.d) and sensitivity studies to demonstrate solution convergence by time step and nodalization (Items II.2 and II.3), as well as other important parameters, Section 4.3.2.1 referenced a B&W report, BAW-10154, and the sensitivity studies in Appendix A of Volume I. The report referenced was a study performed for B&W plants and the studies in Appendix A were performed for LBLOCA's. Justify the applicability of the referenced report and studies to plants with recirculating SGs or SBLOCA's. Alternately, provide the results of sensitivity studies that are directly applicable to SBLOCA's and recirculating SG plants.

Response: The study presented in the referenced report BAW-10154) was conducted for B&W-designed NSSs and was performed using the CRAFT2 computer code. Thus, the specific noding arrangements studied and inputs used cannot be applied directly to modeling of other PWR designs using RELAP5/MOD2. Rather, the general conclusions derived from the CRAFT2 studies can be extended to modeling situations in which the sensitivities of concern are similar. For example, the pressure disturbance caused by the interaction and mixing of cold ECC fluid with steam in the vicinity of an injection site was determined in BAW-10154 to be controlled primarily by node size and local quality during injection. Because the process modeling for steam-water condensation in RELAP5/MOD2 is less sensitive than the instantaneous equilibrium calculation in CRAFT2, it is reasonable to conclude that a stable noding scheme derived for the latter can be carried to the RELAP5 model.

The cold leg noding, at the ECCS injection point, was also verified by studies performed for the large break LOCA

evaluation model. The analyses included conditions of high accumulator injection flow into highly voided cold leg piping. A stable convergent solution was obtained by modeling most of the piping as a single volume into which the injection occurred. A single small volume between the injection node and the pump volume was maintained to allow for simulation of ECCS spill back into the pump suction piping. This nodalization is equally applicable to the SBLOCA situation which has considerably slower depressurization and lower accumulator injection flow rates.

Certain of the studies performed for the large break model in BAW-10168 pertain as well to the small break model. Because of the slower depressurization of the SBLOCA transient, noding arrangements created to provide sufficient spatial detail in the calculated fluid properties during large break transients are also adequate for SBLOCA. For example, the results in Appendix A of BAW-10168 for noding near the break, core axial noding, and time step are valid bases for both small and large LOCA modeling.

Despite the differences in design, the same heat transfer modes are encountered in both the once-through (OTSG) and the recirculating (RSG) steam generators. The B&W study contained in BAW-10154 demonstrates that a six-axial-node model can provide adequate heat transfer mode definition. The eight-node model used in the RSG evaluation model provides somewhat better spatial definition of the steam generator heat transfer modes and is therefore of sufficient accuracy for the prediction of SBLOCA.

In addition to code convergence-based noding arrangements, there is the clear requirement to make the noding correspond to plant geometry in such a way as to produce a plant-representative simulation of the SBLOCA transient.

Typically, the small LOCA progresses as a series quasi-steady conditions that are largely controlled by the various manometers set up within the reactor coolant system. This requires that the vertical sections of each manometer be properly noded with accurate representation of fluid inventory as a function of elevation in the manometer. In the downcomer, for example, the liquid volume below the bottom of the cold leg nozzle must be preserved or the wrong fluid inventory will be trapped in the reactor vessel when natural circulation stops. Similarly, the elevations within the cold leg must be accurately modeled for prediction of reactor coolant pump spillover and loop seal clearing.

The extent to which the basis for the SBLOCA noding rests on studies made for the B&W NSS design or on LBLOCA studies for the RSG evaluation is reasonable. Furthermore, substantial basis for the noding relates directly to appropriate representation of the plant geometry in the model. It is concluded, therefore, that the model for SBLOCA as provided in volume II of BAW-10168 is well founded and that the requirements of sections II.2 and II.3 of Appendix K to 10CFR50.46 are met.

51. Question: One area where SBLOCA plant nodalization is particularly important is in the area of the loop seal because of the influence it can have on loop seal clearing. Provide the results of sensitivity studies on loop seal nodalization to justify the proposed SBLOCA nodalization in this area (as shown in Figure 4-2) accurately calculates this phenomenon.

Response: As discussed in the response to question 50 of this set, the correct prediction of loop seal clearing places some constraints on the nodalization of the evaluation model. These constraints, however, are not properly established through the use of noding studies alone. The mechanism of the pump suction loop seal clearing is a static force balance between the liquid level in the loop seals and the net positive head in the reactor vessel downcomer. To properly simulate the manometric force balance, the pump suction piping must be divided into three geometric regions; downhill leg, horizontal section, and uphill leg. At a minimum separate control volumes for each of these regions are required to adequately calculate the flow dynamics and loop seal clearing. The SBLOCA EM noding presented in Figure 4-2 of Volume II of BAW-10168 is based on this loop seal modeling criterion. The results of the S-LH-1 benchmark, response to question 17 of the first round of questions on BAW-10164 confirm that the modeling criteria applied to the pump suction piping are sufficient to compute the occurrence of loop seal clearing including proper correspondence to the downcomer driving head.

52. Question: In discussing the SBLOCA break spectrum study, Section 4.3.2.3 stated that, "a spectrum of break areas is studied to ensure that breaks 1.0 $ft^2$ and less have been adequately investigated." Define what is meant by "adequately investigated".

Response: An adequate investigation comprises a spectrum of SBLOCA break sizes sufficiently broad as to provide assurance that the evaluation of any LOCA of break area 1.0 $ft^2$ or less which potentially could be evaluated under 10 CFR 50.46 would meet the applicable acceptance criteria. This is usually accomplished by an examination of the interactions of break size, break location, steam generator action, ECCS system performance, and ECCS design. At least one event should be run which relies on each of the major ECCS systems during the critical phase of the LOCA, ie. when the core liquid inventory is minimum and the cladding temperature peaks. This means a larger break which is cooled by the HPI, Accumulators, and the LPI systems; a smaller break which has not depressurized to the LPI system at the time of minimum core inventory, and a very small break which is cooled only by the HPI system and does not rely on the Accumulators.

The effect of the steam generators should be considered as events may limit their capability to remove energy. For example breaks which depressurize to between 1000 and 600 psi are probably more severe for an RSG plant than smaller breaks. The effect of break location generally calls for the accident to be evaluated with the break at the bottom of the cold leg piping between the RC pump discharge and the reactor vessel so that the portion of the ECCS injected into that loop is lost out of the break before it can assist in cooling the core. A properly defined spectrum, tacitly five to seven cases, will usually suffice to provide assurance

that all significant phenomenological interplays have been assessed. The specific spectrum, of course, depends on the particulars of the plant design and must be defined on a plant-specific basis.

53. Question: Sections 4.3.2.4, 4.3.2.7, and 4.3.4.2.2 stated that, because the amount of swell and the steaming rate in the hot channel is considerably greater than in the average channel, use of average channel conditions in the hot rod analysis implicitly allowed for the effects of flow blockage and flow diversion to neighboring bundles. Clarify the meaning of these statements. Provide test data and /or evidence to justify this approach.

Response: The applications of the various computer codes for the small break model was shown in Figure 4-1 of volume II of BAW-10168, the B&W RSG LOCA evaluation model. In this arrangement, a temperature calculation for the cladding, hot channel analysis, is only performed when RELAP5/MOD2-B&W and FOAM2 indicate that the core is partially uncovered. Sections 4.3.2.4, 4.3.2.7, and 4.3.2.2 address inherent conservatisms in the boundary conditions used in the FRAP-T6 analysis of the peak cladding temperature.

Both the RELAP5/MOD2-B&W and the FOAM2 models are average channel models. Therefore, the mixture height and the steam flow above the mixture as calculated by FOAM2 are appropriate for core locations with radial peaking of 1.0. The hot channel analysis, however, is done for a core location where peaking is assumed to be approaching the LOCA limits, requiring a radial peaking of between 1.3 and 1.5. With the power below the mixture level in the hot channel at least 30 percent higher than the power in the average channel, correspondingly more steam will be generated in that channel. This steam will interact with the liquid through the phase separation model to increase the mixture height for the hot channel over that for the average channel. This in turn will cover a larger fraction of the fuel assembly below the mixture level to further increase the boiling. It would be reasonable to expect as much as a

0.5 foot increase in mixture height for the hot channel over the average channel. Combining the higher mixture level with the larger boiling rate the steam flow above the mixture level should be about 50 percent larger for the hot channel than for the average channel. Figure 53-1 depicts the situation.

Evaluations of flow diversion from rupture zones under the conditions appropriate for small break LOCA show that significant diversion does not occur (see following paragraphs). Flow for a given subchannel may distribute to a neighboring subchannel and return to the original subchannel just above the blockage. This momentary diversion, if it in fact occurs, is compensated for by enhanced heat transfer at the rupture site. A realistic simulation of the hot channel, therefore, would set the mixture height and steam flow to the values appropriate for the hot channel, causing an increase in the heat transfer coefficient and a decrease in the vapor superheat. The use of average channel values within the hot channel analysis for mixture height and steam flow is therefore quite conservative.

Direct experimental verification of the rupture-induced flow diversion during small break LOCA is not readily available. Conclusions drawn from reflood experiments in test facilities, CCTF and SCTF, which are large enough to incorporate radial power shaping, indicate preferential flow to the hot regions. Similar trends are observed in these facilities for asymmetric radial power and initial temperature tests.

The FLECHT-SEASET blockage tests support a conclusion that rupture-induced flow diversion is not of consequence. This infers from the observed temperature response of the

unruptured fuel pins which, if flow diversion occurs to any substantial degree, should show a decrease in cladding temperature when rupture-induced flow blockage is simulated.

One of the FLECHT rupture simulation tests is documented in the response to NRC question 13 of the first round of NRC questions on the BEACH topical report, BAW-10166. In comparing the test with rupture simulating sleeves to the reference test without the sleeves, the test assembly can be seen to have responded to the occurrence of rupture in two ways, see Figures 53-2 and 53-3. Cladding temperatures for fuel rods in the bypass zone, those that did not have simulator selves, responded to both tests in the same way, i.e., these rods experienced the same amount of cooling. Cladding temperatures for fuel rods in the blockage area, those with simulator sleeves, responded to both tests in the same way up to the bottom of the rupture zone. Through the rupture zone and for 1 to 1.5 feet downstream of the rupture zone, the cladding temperature dropped to about 100 F below that expected for an unsleeved response. Above the rupture zone, the cladding temperature was again characteristic of an unruptured condition.

The test data are characteristic of what is expected if no subchannel diversion or only adjacent subchannel flow diversion occurs. Figure 53-2 presents the results of the rupture simulation test in contrast to the results from an unblocked test. The area on the figure label led "zone of influence" divides the assembly axially into three regions. Below the zone there are no substantial rupture effects. The zone itself is defined as that height over which rupture induced substantial beneficial effects. Above the zone there is again a region of substantially no impact. Had significant flow diversion occurred, the cladding temperatures in the this third region would have showed

different results. Under a condition of significant flow diversion, the cladding above the rupture zone for a rod which simulated rupture should be experiencing reduced steam flows resulting in increased surface temperatures. Similarly the cladding above the rupture zone for rods which did not simulate rupture, say in the bypass region, should be experiencing increased steam flows resulting in decreased surface temperatures. Neither of these occur. The cladding temperature response above the rupture zone is nearly identical for the unblocked and the rupture simulating (blocked) tests. The conclusion is that, with rupture induced blockage, axially distributed, substantial net channel-to-channel flow diversion does not occur.

Further experimental evidence, more directly applicable to small break LOCA is available from the FLECHT-SEASET 21-rod flow blockage tests. During these tests an electrically heated assembly simulator containing 21 rods was tested for steam cooling under unblocked, partially blocked, and fully blocked conditions, with both coplanar and distributed blockages. Figures 53-4, -5, and -6 show information taken from the data and analysis report for these tests[53.1]. Figure 53-4 shows the ratio of Nusselt numbers for the partially blocked test 41401B to the unblocked test 44401A for the rods that simulated the blockage (center nine rods). The influence of rupture on the heat transfer coefficient can be seen to be positive from the figure. Figure 53-5 shows the vapor temperature as a function of axial position for both the unblocked test and the partially blocked test, again for rods that simulated rupture. Although rupture was simulated between 72 and 74 inches, no increase in vapor temperature for rods simulating rupture has occurred by 96 inches, the highest elevation at which measurements were made. Again, the conclusion is that no substantial flow diversion occurred even under coplanar blockage. The lack

of flow diversion for these tests is explained by the low Reynolds number, 5000. At higher Reynolds numbers, above 10000, there is evidence of some flow diversion for coplanar blockages. The Reynolds number with the steam cooling region for a small break LOCA is less than 2000. Finally, Figure 53-6 shows the Nusselt number ratio for a case with distributed rupture simulation and the base case.

The approach used by B&W for hot channel hydraulic analysis during small break LOCA does not account for rupture. The effects of rupture, however, do not perturb the hydraulic simulation as flow diversion under the conditions of small break LOCA is not significant. Furthermore, the use of average channel fluid conditions for the hot pin heatup analysis builds considerable conservatism into the prediction of peak cladding temperature.

References:

53.1 NUREG/CR-2444, EPRI NP-2014, WCAP-9992, Volumes 1 & 2, "PWR FLECHT SEASET 21-Rod Bundle Flow Blockage Task Data and Analysis Report", September 1982.

Figure 53-1

Average Core Steaming and Hot Assembly Steaming

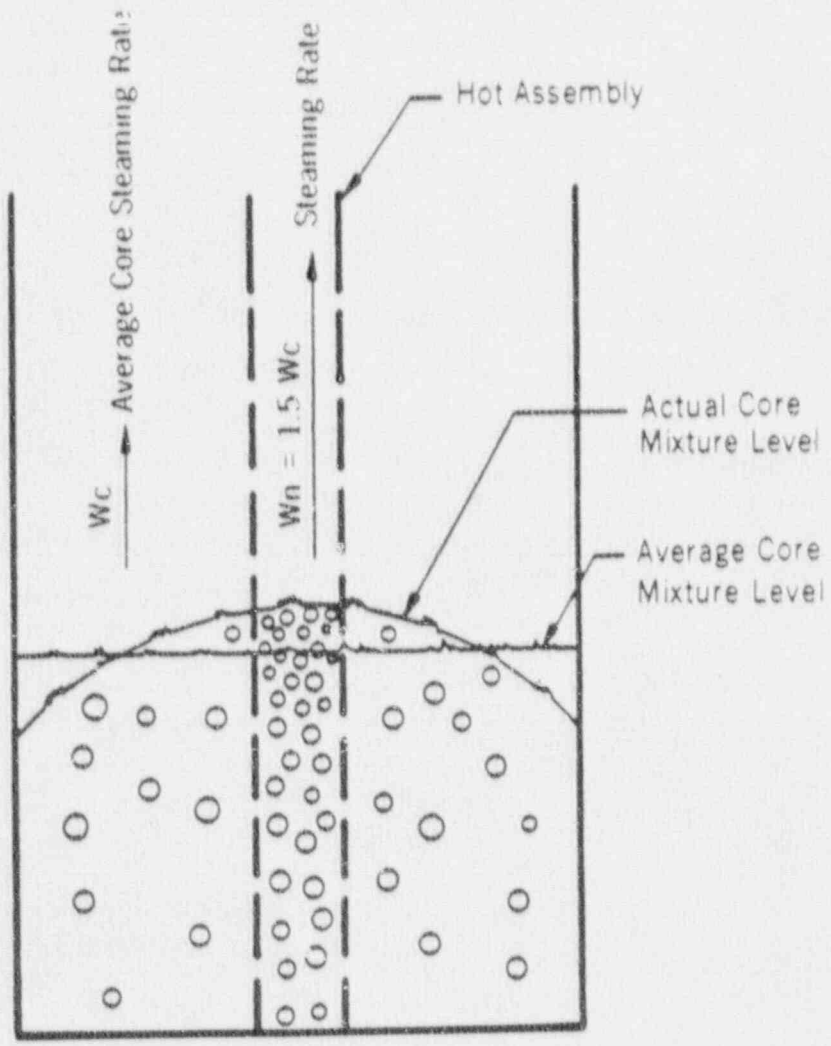


FIGURE 53-2. PEAK CLAD TEMPERATURE VERSUS ELEVATION FOR BLOCKAGE TEST 61607.

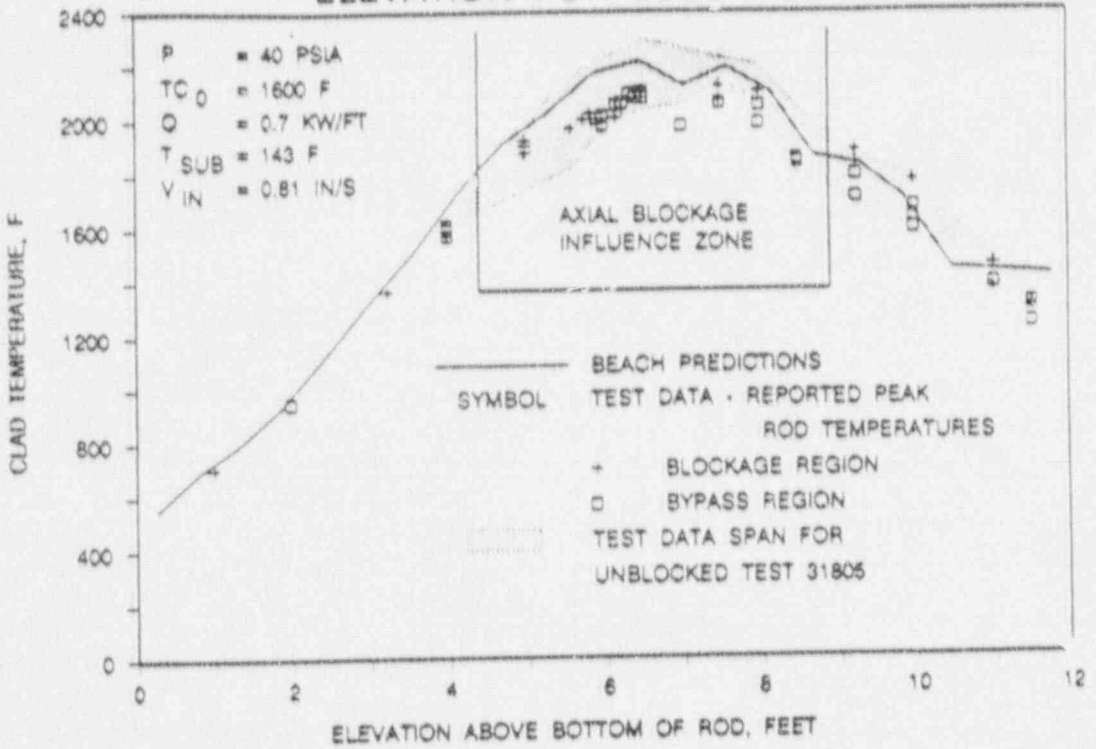



FIGURE 53-3. PEAK CLAD TEMPERATURE VERSUS ELEVATION FOR REFERENCED UNBLOCKED TEST 31805.

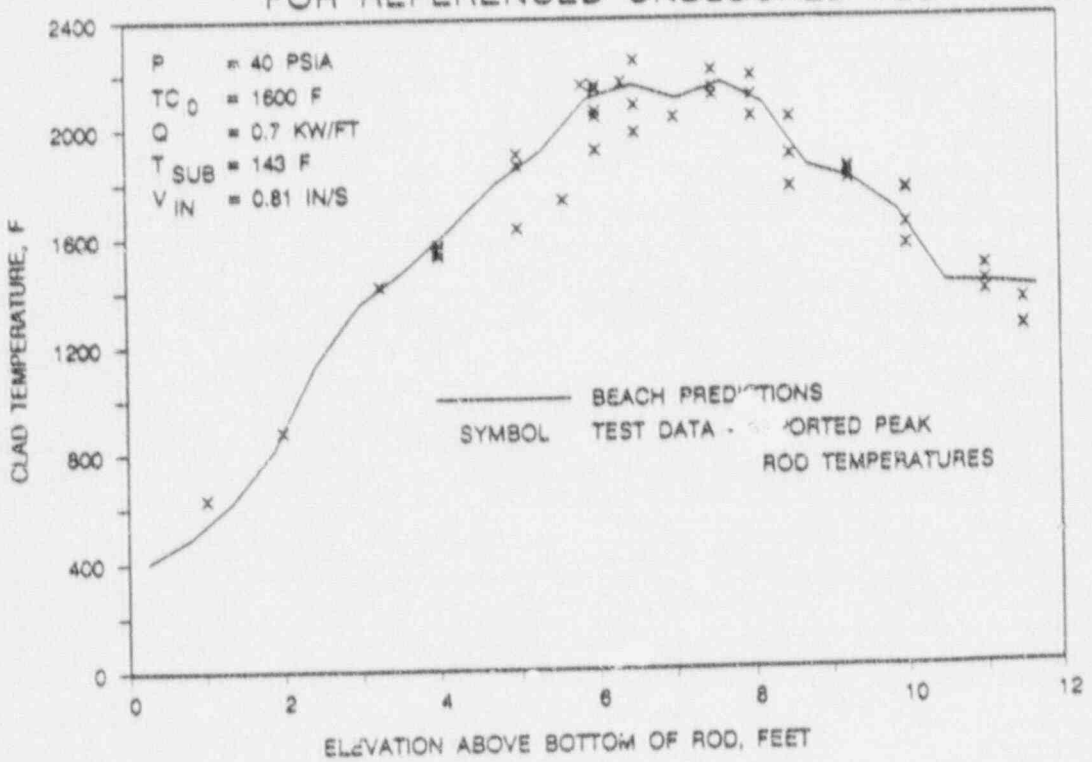


FIGURE 53-4. RATIO OF NUSSELT NUMBERS VERSUS CORE POSITION.

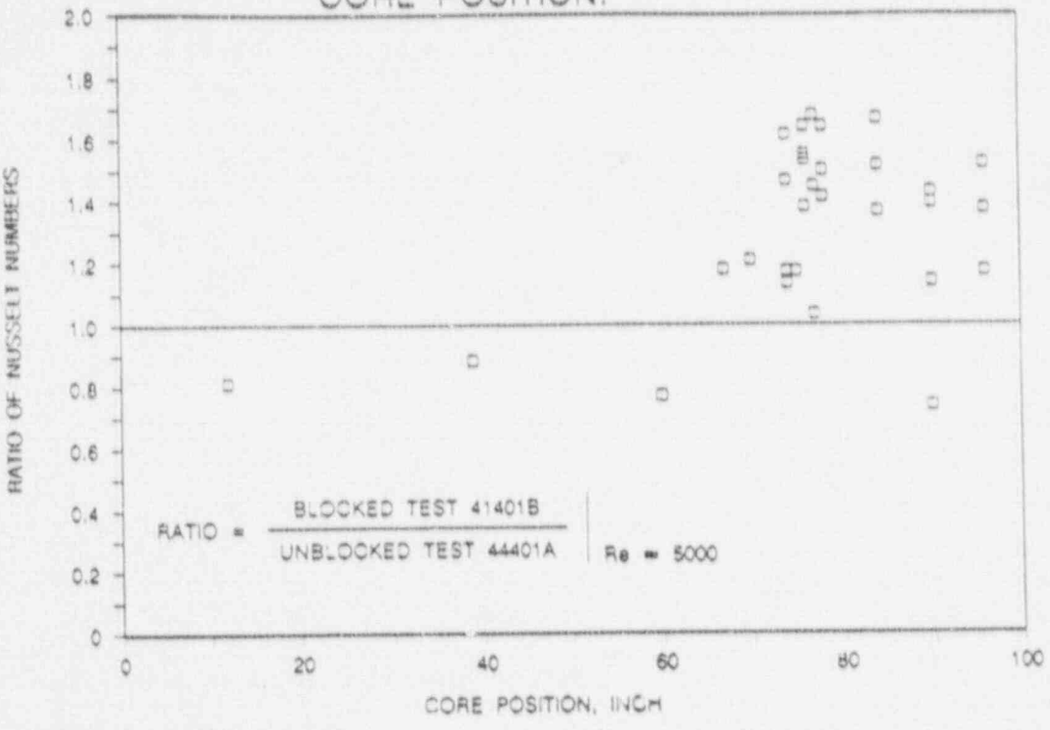



FIGURE 53-5. VAP TEMPERATURE VERSUS CORE POSITION.

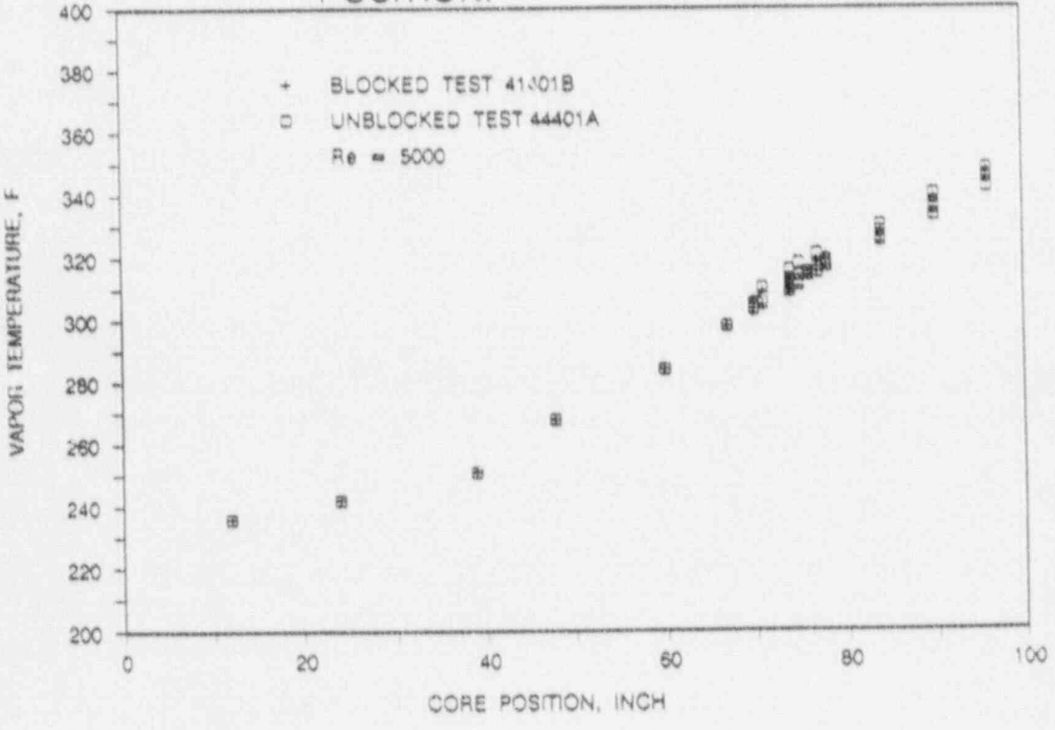
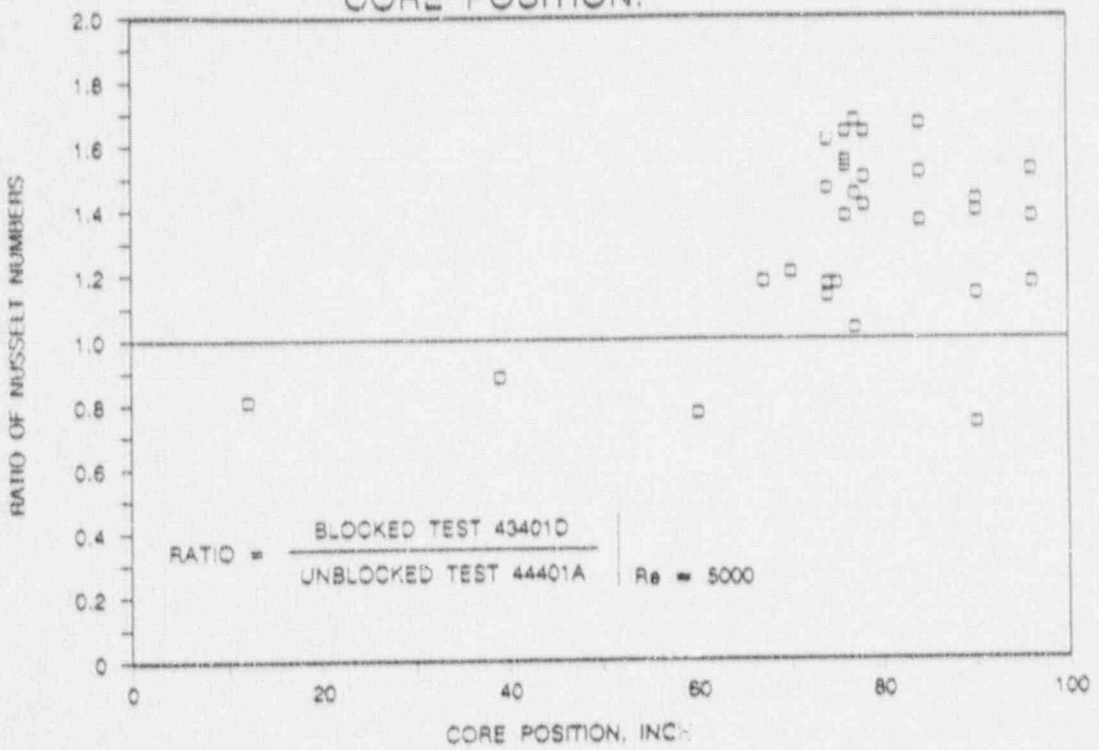


FIGURE 53-6. RATIO OF NUSSELT NUMBERS VERSUS CORE POSITION.



54. Question: Section 4.3.3.1 stated that, for SBLOCA's with the pumps operating, the cladding temperature is evaluated either by the LBLOCA blowdown technique or by a steady state calculation that used conservative boundary and fluid conditions to determine the upper limit of any temperature excursion.

a. Clarify how B&W determines which approach to be used in a plant analysis.

Response: A Small Break LOCA (SBLOCA) with the reactor coolant pumps operating is characterized by a continuous flow of coolant through the core. The pump induced core flow evolves from its initial subcooled condition to one of high quality and back to liquid during the course of the transient. The reactor coolant pumps cavitate and degrade in performance during the transient recovering to full volumetric flow as the system void fraction approaches 100 percent. Cladding temperature calculations based on forced steam flow with the high pressure ECCS flow added have been used to bound the temperature excursions for all possible such accidents. This eliminates the need to do case specific cladding temperature evaluations.

The most limiting core cooling situation occurs when the plant is at the peak in void fraction for the specific SBLOCA (for most SBLOCA cases this is essentially 100 percent). Under the condition that the core flow does not evolve to a 100 percent quality before the ECCS injection can absorb core decay heating without superheating, the core cooling can be conservatively bounded by assuming that the core inlet volumetric flow is the same as that at power operation and that this flow is 100 percent saturated steam plus

the high pressure ECCS injection. This evaluation has been conducted repeatedly using various forced convection to steam and superheated steam models with the resulting cladding temperature being held to within 200 to 300°F of the steam superheat temperature. For a 3500 MWt plant with radial peaking of 1.7 the maximum exit steam temperature at 500 seconds is limited less than 800°F (500 seconds is a representative time of high void fraction development for accidents that do not depressurize to low pressure injection pressures quickly). Therefore, even with the peak power directly at the exit of the core the peak cladding temperature remains below 1100 F, out of the range for metal water reaction, and well below the limit of 10 CFR 50.46.

The above evaluation will be applied to all pumps running SBLOCA evaluations made under the B&W recirculating steam generator evaluation model. The criterion for application will be that the ECCS injection is capable of absorbing the plant decay heat without superheating before the core inlet void fraction as calculated by RELAP5/MOD2-B&W reaches 100 percent. B&W has never evaluated an accident that would fail to meet this application criterion and does not expect to for any plant covered by this evaluation model. Only if the criterion is not met, will it be necessary to run FRAP-T6 using the large break LOCA approach to evaluate the cladding temperature.

Section 4.3.3.1 of volume II of BAW-10168, the SBLOCA evaluation model, is incorrect and will be changed to reflect the above position at its next revision.

b. Provide the results of assessment studies where the LBLOCA blowdown technique was applied for a SBLOCA with the pumps operating to verify the validity of this methodology for calculating the PCT.

Response: Applying the McEligot correlation for single phase heat transfer to the conditions given in the response to part a provides the following relationship for a peak linear heat rate of 14 KW/ft.

$$T_w = T_g^2 / (T_g - 220) \qquad \text{with } T_g \text{ and } T_w \text{ in F.}$$

Thus even if the peak power were at the core exit the wall temperature would be limited to less than 1100 F. The conservatism of the approach is demonstrated by the LOFT series of pumps-running tests which showed no cladding temperature excursion during the period of pump operation.

c. With respect to the steady state approach, provide a more complete description of the method and an assessment to verify the technique conservatively calculates the PCT for SBLOCA's with the pumps operating, or provide the reference if the method has previously been approved by NRC for use with plants with recirculating SG's.

Response: The responses to Parts a and b of this question have applied the steady state approach to a representative plant under conservative assumptions.

d. Clarify if you intend to analyze SBLOCA with RC pumps on. If so, provide the benchmarks required to comply with TMI actions plan Item II.K.3.5 of NUREG-0737, "Automatic Trip of Reactor Coolant Pumps".

Response: The plants covered by this evaluation model employee either automatic or manual pump trip upon the occurrence of signals verifying an SBLOCA situation. In either case, the results of SBLOCA cases with such early pump trip times have been shown to be the same or less severe than those during which the pumps were lost at the onset of the accident. Therefore, B&W will not evaluate SBLOCA with the reactor coolant pumps running for the purpose of showing compliance with 10CFR50.46.

e. For the SBLOCA analysis with RC pumps tripped, clarify the time of pump trip used in the analysis, such as the beginning of SBLOCA, the time of reactor trip, the time of receipt of RC pump trip signal for automatic trip, or two minutes after the RC pump trip criterion is reached for manual trip. Justify conservation of the trip time used and compliance with Item II.K.3.5.

Response: The timing of the reactor coolant pump trip will coincide with the initiation of the accident. RC pump operation during SBLOCA has two major effects; one is maintaining core cooling via forced circulation, and the other causing continued depletion of the primary system liquid inventory after a substantial void in the primary system has formed. There is a threshold time for this continued depletion of inventory and studies have shown that running of the pumps up to that time actually produces less severe results. The conclusion from the W analysis in Section 10 of WCAP-10081A is that the delayed pump trip in conformance with the W Operating Procedure Guidelines will produce PCTs almost identical to the FSAR calculations which assume the pump trip coincident with the reactor trip. This statement is in agreement with B&W studies on B&W

designed NSS. Thus, tripping of the pumps at the initiation of the event produces a conservative evaluation.

55. Question: Sections 4.3.4.1 and 4.3.4.2.2 stated that the cladding swell and rupture model in the SBLOCA are the same as the LBLOCA model and based on NUREG-0630 data. It further stated that occurrence of rupture is determined during a thermal analysis of the hot rod, and that, if rupture is indicated, flow blockage is calculated for the hot channel. In the B&W methodology, is it possible for rupture to be calculated in the hot rod analysis but not the system analysis? If so, how are the effects of rupture fed into the system analysis?

Response: The statement indicating that flow diversion is calculated if a rupture is predicted in the hot channel is wrong. The effects of rupture and the attendant minimal flow diversion are conservatively bounded during small break LOCA through the use of average core fluid boundary conditions within the FRAP-T6 analysis. This position is fully explained and justified in our response to question 53 of this set.

Within the B&W methodology, the system analysis is concerned with the behavior of the average core and reactor coolant system during the accident while the hot channel analysis is concerned with the peak power location. It is quite possible, even probable that a temperature increase sufficient to cause rupture will be limited to only a few rods for which local power levels approach those at the hot spot. In such a case only the hot spot will experience rupture. The effects of rupture are not fed back into the system analysis. Rupture is a local effect and does not cause any alteration of the expected corewide response.

56. Question: Section 6 of Volume II stated that if temperatures above 1800°F were calculated during the SBLOCA analysis, compliance to Criterion 3 of 10 CFR 10.46 (maximum core wide cladding oxidation is limited to 1%) would be demonstrated by showing that the degree of oxidation in the SBLOCA analysis was bounded by the LBLOCA studies or directly, using the LBLOCA techniques. For PCT's below 1800°F the core wide oxidation would be considered essentially zero. The 1800°F limit appears to be based on the phase change in the metal/water reaction rate at this temperature. However, because significant oxidation can occur at temperatures below 1800°F if the rods remain at these temperatures for a long enough time, a criterion based on temperature alone may not be adequate. Discuss how a time duration effect would be accounted for.

Response: The selection of a greater than 1800°F peak cladding temperature as a criterion for evaluating whole metal water reaction was based on representative cladding heat up and cooldown rates occurring during small break LOCA, SBLOCA. These rates, or rather the signature of the cladding temperature transients, tend to be reasonably constant across the SBLOCA spectrum. The main difference between accidents being the duration and depth of core uncovering. This leads to the conclusion that the shape of the cladding temperature excursion for any SBLOCA that peaks at 1800°F will look very much like that for any other SBLOCA that peaks at 1800 F. A calculation of the metal-water reaction for such a shape produced a local metal-water reaction of 3.3 percent. Comparing this to the calculation of whole core metal water reaction done for the worst case LBLOCA in Appendix A of BAW-10168, which resulted in 0.46 percent whole core with maximum local at 4.7 percent, shows that the whole core oxidation for the small break would be less than 0.25 percent. Thus, at the time of the original

draft of BAW-10168, it was decided that there need be no explicit evaluation of whole core metal water reaction for SBLOCA events for which the peak cladding temperature was held below 1800 F.

Notwithstanding the conclusion of the previous paragraph, B&W has decided to alter the criterion for initiating a specific calculation of whole core metal-water reaction determination for SBLOCA. The whole core oxidation study performed in Appendix A of BAW-10168 produced 0.46 percent core wide oxidation with a 4.7 percent local peak. As the radial power distributions of large and small break LOCA are not dissimilar, and as most oxidation for both LBLOCA and SBLOCA occurs in the upper regions of the core, this leads to a possible criterion for SBLOCA evaluation based on the LBLOCA results. Therefore, a case-specific evaluation of the whole core metal-water reaction will be done for any SBLOCA that results in a peak local oxidation of more than 5 percent. This criterion is as simple to apply as the 1800$^{o}$F peak and more directly measures the time at temperature effect. The criterion is conservative for two reasons: (1) the reference study only generated 0.46 percent reaction with 4.7 local which would indicate a need to be at 6 percent local before 1 percent core wide could result, and (2) more of the core is exposed to temperature excursions for LBLOCA than for SBLOCA because for the latter most of the core is covered by mixture. The evaluation performed may be a case-specific execution of the whole core metal-water reaction method or it may be a point-by-point comparison of local oxidation values to a reference LBLOCA result. The relative sections of BAW-10168 will be altered to reflect the new approach at its first revision.

57. Question:

a. In discussing the establishment of long term cooling, Section 8 made reference to NRC approved procedures. Identify the documents referred to. Also, clarify the basis for the statement that providing a core throughput is sufficient to prevent boric acid crystallization in SBLOCA's as well as LBLOCA's. Provide analyses or experimental results to demonstrate this is sufficient to keep the boric acid in solution.

Response: Operating procedures to establish long-term cooling and to control the boric acid concentration for any operating plant are required by the NRC and are obtainable from the plant. As the procedures vary on a per plant basis, the specifics are not appropriate for discussion in the LOCA evaluation model. The general procedure is to create an assured core flow that will wash highly acidic water from the system and replace it with mildly acidic ECCS water. The last calculation of the evaluation model is a concentration and mixing calculation to verify that, whatever the operational procedure used by the plant, the actions and timing (about 17 hours but not more than 24 hours) are sufficient to hold the boric acid concentration level below saturation.

A brief description of the problem may be in order. For plants with cold side injection, the long term cooling mode following a cold leg break is one of extended core boiling. ECCS water is drawn from the reactor building sump, cooled, and injected into the plant cold legs from where it flows to the downcomer. At the downcomer, however, the hydrostatic balance between the core and the downcomer prevents most of the

injection water from flowing to the core. Only that amount of water necessary to match the boiling flows to the core; the rest flows into the broken cold leg, out of the break, and back to the sump. Water in the core is boiled and steam is passed through the loops to the break and the containment where it is condensed by the building cooling system and returned to the sump as water. The procedure cools very well but does pose a problem. Boric acid does not vaporize at the same rate as steam and most is left in the solution (while most calculations assume all boric acid is held in solution, a good portion actually does vaporize). This causes a gradual buildup in concentration of the boric acid within the core region. The process cannot start for the case of hot side breaks as the ECCS always flows through the core for those events and the distillation process never starts.

Although the solubility saturation limit for boric acid is considerably above the solute concentration of the injection water, the process, unchecked, could cause high enough concentrations to lead to precipitation of borate crystals. The injection solute is about 1.22 weight percent boric acid (this number will vary by plant) and the solubility limit at 212$^{o}$F is slightly over 35 percent. Figure 57-1 shows the saturation solubility of boric acid as a function of temperature. The figure is derived from reference 57.1 and was presented as part of the B&W evaluation model for B&W designed NSS in reference 57.2, BAW-10091 Supplement 1 page 3-90. The most common procedure is to establish a small amount of hot leg injection which, once larger than the core boiling rate, will cause the flow in the vessel to reverse and proceed from the core through the downcomer and out the break. The flow from the core to

the downcomer will be at the core concentration and act to remove boric acid from the system. This leads to the eventual control of the concentration process and gradual decrease in the core concentration.

h. Section 8 stated that the computation of the timing of this action for SBLOCA's is bounded by the LBLOCA calculation and that analysis would be used to satisfy the long term cooling requirement for SBLOCA's. Provide the basis for concluding the LBLOCA calculation bounds the SBLOCA calculation.

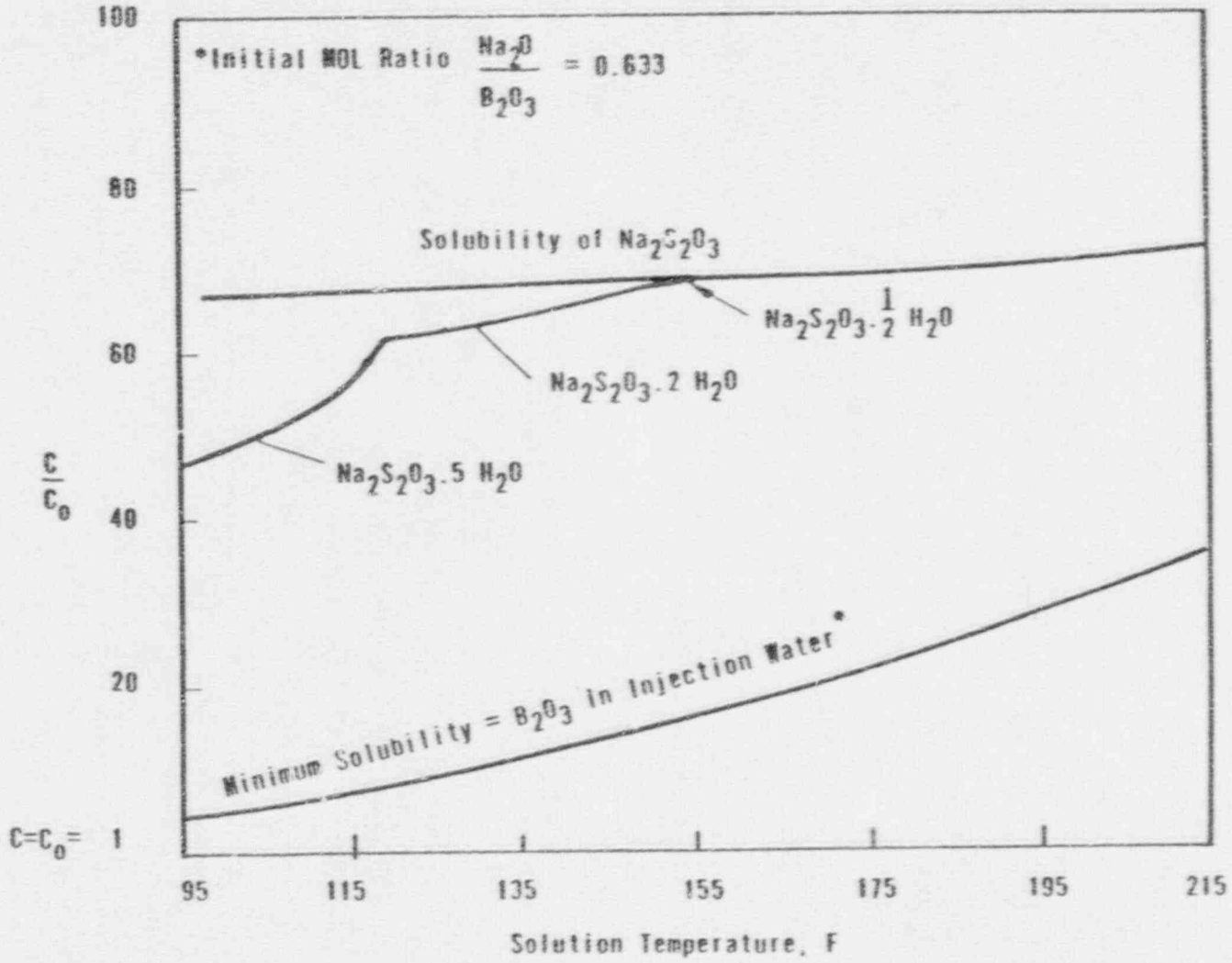
Response: There is little difference in the boron crystallization phenomenon between the LBLOCA and the SBLOCA except in the pressure, temperature, and timing of the process. The SBLOCA is a relatively slow depressurization process such that the primary pressure and temperature during distillation may be higher than for the LBLOCA. The solubility of boric acid in water is independent of pressure and increases with the temperature of the solution. To the extent that natural circulation continues during the accident, the start of core boiling will be delayed and the distillation will occur at lower decay heat rates and take longer to cause critical concentrations. For some events, 2-inch diameter breaks and smaller, the core may be continuously cooled by the steam generators and no distillation process is initiated. In any case, the conditions for the concentration of boric acid are all less conducive to the buildup of problem concentrations for small breaks than they are for large breaks. Thus any computation of control for the LBLOCA will bound the SBLOCA.

References:

57.1 "Solubility Isotherms in the System $Na_2O$-$B_2O_3$-$H_2O$ at 0-94 C", U. S. Borax Research Corp.

57.2 Supplementary and Supporting Documentation for B&W's ECCS Evaluation Model Report with Specific Application to 177-FA Class Plants with Lower-Loop Arrangement, BAW-10091 Supplement 1, Babcock and Wilcox, December 1974.

Figure 57-1 Comparison of Solubility Curves for Chemical Additives



58. Question: Although the codes used to calculate the system response to SBLOCA were individually assessed in the code topical reports, a SBLOCA analysis demonstrating the overall conservatism of the B&W methodology for small breaks has not been provided for review. Provide a SBLOCA analysis with the RC pumps off that exercises each part of the EM methodology and demonstrates the method's capability to link together the methodology codes and to conservatively calculate SBLOCA phenomena and the PCT.

Response: The basic SBLOCA methodology used in the RSG evaluation model is identical to that employed by the B&W evaluation model for B&W-designed NSSs except for the individual codes. The ability to successfully couple individual code results in the way proposed and to generate a conservative plant evaluation has been demonstrated in the licensing of that evaluation model. Reference can be made to the evaluation model reports and application reports for the evaluation model for B&W designed NSSs; BAW-10154, BAW-10052, and BAW-10064.

The Semiscale MOD-2C S-LH-1 experiment benchmark provided in response to Question 17 of the first round of questions on RELAP5/MOD2-B&W (BAW-10164) demonstrated the ability of RELAP5 to calculate the basic hydraulic phenomena that occur during SBLOCA. This benchmark has been extended to include a calculation of the resultant cladding temperatures. The calculation involved the transfer of core liquid inventory history and pressure from RELAP5/MOD2-B&W to FOAM2, the computation of a core swelling level in FOAM2, and the use of that swell level in FRAP-T6 to compute a cladding temperature transient. Therefore, all couplings between codes of the SBLOCA evaluation model were exercised. The results are documented in the response to Question 5 of the second round of questions on RELAP5/MOD2-B&W.

The benchmark of Semiscale MOD-2C S-LH-1 predicts the cladding temperature response at two elevations, the 10.5-foot level and the 8.23-foot level. The data showed two periods of core uncovery, one associated with loop seal clearing and one associated with core boil down and refill. Both of these are seen in the prediction. The highest temperature occurred for the second, or boil-off, peak at both elevations. The EM prediction was approximately 940°F and 920°F for the 8.23-and 10.5-foot elevations respectively and the data measured 850°F and 800 F. This gives an over-predictions of about 90 and 120 F.

Although the benchmark of S-LH-1 showed some degree of conservatism, it did not represent the major conservatism present in the evaluation model. In fact, the degree to which the evaluation mode. can be applied to a small scale facility like Semiscale results in nearly a best estimate approach, provided the core power is not adjusted up by 20 percent. This occurs because the major conservatism included in the evaluation model is associated the amount of radial core power peaking. In the B&W evaluation model, the steam flow that is used to cool the hot assembly is derived from the average core mass flux. As discussed in the response to Question 53, in an actual plant the steam flow in the upper portion of the hot fuel assembly would be higher by a factor equal to or greater than the radial peaking. With the flat or one-dimensional core of the Semiscale tests, there are no radial effects that would demonstrate the effect of hot assembly steam flow. Using the average channel steam flow is estimated to add about 50 percent to the difference between the cladding temperature and the saturation temperature for the system pressure. Therefore, if the Semiscale facility were actually a small section of a larger facility with plant typical radial power

peaking, the resultant temperature predictions of the EM would have been 1160° and 1130°F for the 8.23-and 10.5-foot predictions respectively. This would have provided temperature margins of 310°F and 330°F between the prediction and the data.

59. Question: Figure 4-1 shows the interfaces between the small break analysis codes. One of the parameters passed from RELAP5/MOD2-B&W to the FOAM2 code is the quiet water level. Define what is meant by the quiet water level and discuss how this parameter is calculated.

Response: The quiet water level is another term for the collapsed water level. It is the height at which the liquid steam interface would occur in a region under the assumption that all steam was moved to the top of the region and all liquid was moved to the bottom. One of the inputs for the FOAM2 code is the amount of water available from which to generate a froth or mixture level. This parameter is passed to the FOAM2 code as the quiet or collapsed water level for the core. The core quiet water level is calculated from RELAP5/MOD2-B&W as the liquid contained in all nodes in the reactor coolant system from which water would fall into the core under quiescent conditions. This includes water in the core regions and water that may be suspended in the upper plenum and possibly in the hot legs or the riser section of the steam generator tubes. The use of suspended water is legitimate because a conservative liquid inventory treatment will overpredict frothing within the thermal hydraulic code, RELAP5/MOD2-B&W in this case. When the more accurate, lower, mixture level is determined, from the FOAM2 code, water that has been pushed into the regions above the core but not over the top of the steam generator should be modeled as in continuous connection to the core mixture.

60. Question: In discussing compliance with Appendix K requirement of no return to nucleate boiling during the blowdown phase of LOCA, Item I.C.4 of section 4.4 stated that, "once CHF was calculated at an axial location, a return to nucleate boiling was delayed as described in Section 4.3.3.2, which is in agreement with Appendix K." Section 4.3.3.2 stated, "Upon refill of the core, a return to pool nucleate boiling for the portion of the core being recovered by the steam-water mixture is allowed." This statement does not describe lockout of return to nucleate boiling in the Appendix K sense but rather the logic that would be expected in implementing a boiling curve approach to core heat transfer. Clarify the meaning of the above statement made in the discussion on compliance with Appendix K.

Response: The Appendix K requirement for lock-out of nucleate boiling heat transfer regimes is keyed to the blowdown phase of the accident. "Blowdown" is large break terminology and has little meaning for small break events. The literal extension of the requirement to the regime of small break LOCA will, as has long been recognized, create nonsensical evaluations. The more logical extension of the criterion is to extend the intent based on sound technical practice. During large break LOCA, the calculation of departure from nucleate boiling, DNB, could occur within a fraction of a second of the initiation of the event at a time when fuel temperatures remained near those of normal operation. As a result, the cladding temperatures could rise several hundred degrees in a matter of seconds. The capability for return to pre-CHF heat transfer depends on both fluid conditions and on pin surface temperatures in such a way that there is a wide range of conditions over which a pin may or may not rewet. If a return to pre-CHF heat transfer were allowed, the consideration of certain

accident scenarios and possible cladding temperature transients might be eliminated because a temperature or fluid condition threshold was missed. To preclude this possibility and to provide assurance that the worst case conditions were being evaluated, no return to nucleate boiling was allowed for the momentary conditions encountered during large break LOCA calculations. Additionally, if a return to nucleate boiling were allowed, the fuel energy would be substantially reduced while nucleate boiling remained active. Preventing the return provided assurance of conservatism in the calculated results during the blowdown period.

The situation is much different during small break LOCA. The plant is evolving slowly in a sequence of quasi-steady states. Conditions that lead to DNB or to the return to pre-CHF heat transfer are not momentary but, once established, remain for extended periods. If an acceptable situation for rewet occurs at an elevation, then it can be expected that the conditions will become more favorable with time. There is little concern that the rewet would be a momentary threshold effect. Areas of conservatism in the model are readily identified and understood so that there is a high degree of confidence in the overall conservatism. In short, the heat transfer environment of a small break LOCA is much more akin to the environment during the reflood phase of a large break LOCA than it is to the blowdown phase. It is B&W's reading that the proper implementation of requirement C.4.e of Appendix K to small break LOCA is as appropriate for the reflood phase of a large break LOCA. This interpretation, which allows a return to nucleate boiling, is consistent with all currently approved SBLOCA evaluation models. Thus "heat transfer assumptions characteristic of return to nucleate boiling (rewetting) shall be permitted when justified by the calculated local

fluid and surface conditions."

61. Question: The compliance section in Volume II did not discuss B&W's position on the Appendix K required lockout of return to transition boiling when cladding superheat exceeds 300°F (Item I.C.5.b.3). Clarify how B&W intends to comply with this requirement.

Response: The restriction on the use of the transition boiling correlations contained in items I.C.5.b.2 or I.c.5.b.3 of 10CFR50.46 Appendix K after cladding temperatures of more than 300°F above the saturated fluid temperature have been calculated applies to the blowdown phase of the LOCA. As with our response to question 60 of this set, B&W believes that the intent of the restriction was to limit blowdown heat transfer during large break LOCA. The restriction does not apply to the reflood phase of a LOCA. For the reasons stated in the response to question 60 B&W will treat small break LOCA similarly to the reflood phase of a large break LOCA for these heat transfer provisions. The resultant SBLOCA evaluation model is consistent with all currently approved SBLOCA evaluation models. Therefore, the restriction of I.C.5.b.2 or .3 will not be applied in the small break evaluation model.

62. Question: Item I.D.2. of Section 4.4. stated that containment pressure was not a critical factor for SBLOCA's as long as the value used was reasonable. Will the value used be justified on a plant specific basis? If not, provide additional information to clarify B&W's approach to meeting the Appendix K requirement. This includes a discussion on what is meant by a "reasonable value", the containment pressure B&W intends to use, and justification that this pressure is applicable to all the plants to be analyzed.

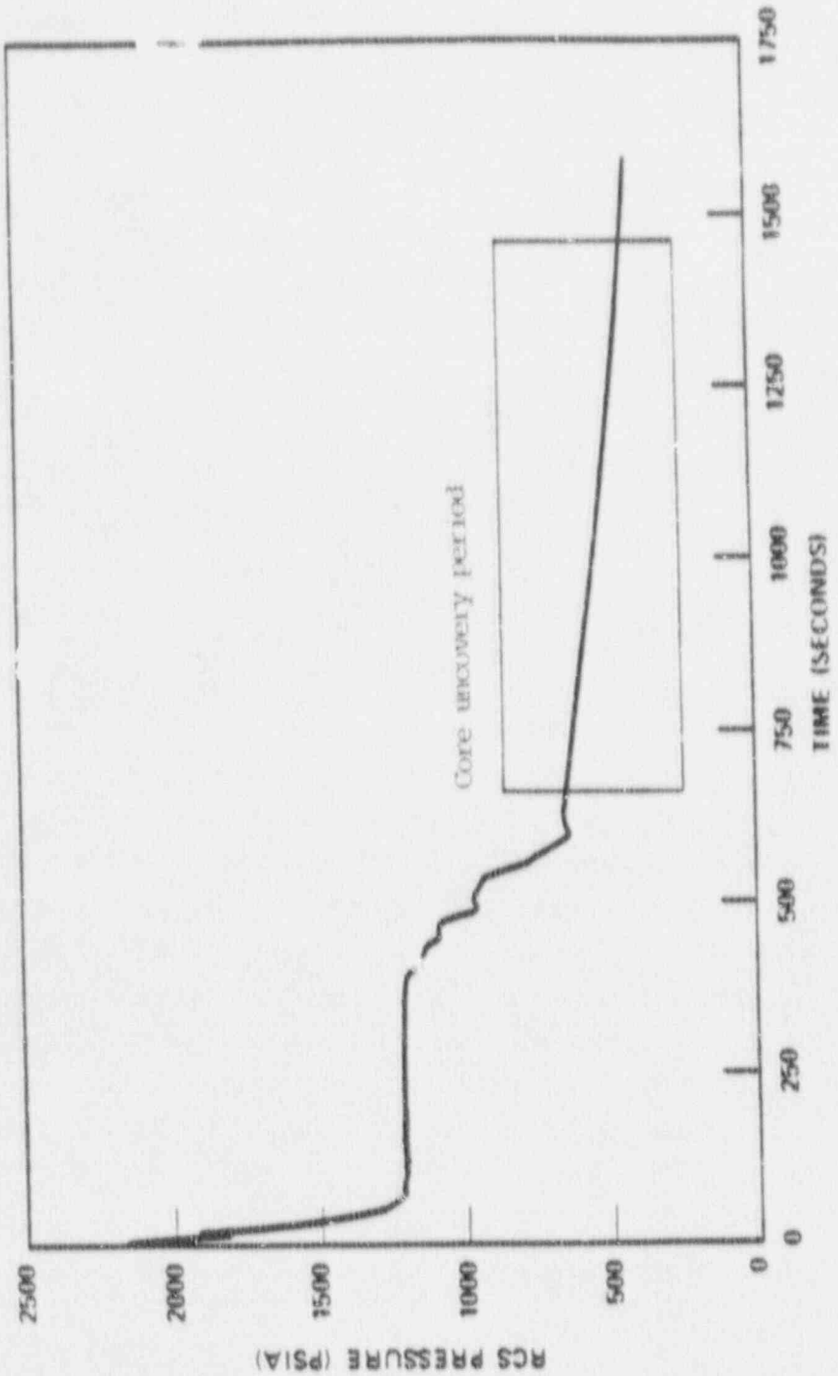
Response: Most small break LOCAs experience only critical or choked break flow and all SBLOCAs spend most of the transient under conditions of choked flow. Figures 62-1 through 62-2 show system pressure histories for representative SBLOCA transients taken from FSARs for plants designed by Westinghouse and B&W. The timing of the occurrence of peak cladding temperature is indicated by a small box on each curve. Current PWR containment designs have LOCA pressures that range from subatmospheric to 50 or 60 psia. As can be seen from the pressure ratio between the system and the containment the leak flow for this class of small breaks must be choked for times near the occurrence of peak cladding temperatures. Thus only the larger, generally much less severe, of the SBLOCA breaks can experience subcritical flow. So long as choked flow exists, the containment pressure is irrelevant to the course of the accident. In fact, for events for which the peak cladding temperature occurs at a system pressure of 100 psia or more, the containment pressure is immaterial.

The larger class of small breaks may evolve at low enough pressures to interact with the containment pressure. Such accidents are mitigated by the low pressure injection system, do not have the extended periods of core uncovery (usually do not have any core uncovery), and do not involve a core reflood period. Interaction between the LOCA and the containment

pressure can be conceived of in two areas. The first involves the possibility that lowering of the containment pressure might extend blowdown and encourage liquid entrainment to the level that the RCS inventory is excessively decreased. This effect takes place when system inventories are controlled by the flow and entrainment processes present in large break LOCAs and is one of the phenomena used to differentiate between the large and the small breaks. Because the effect is part of the division between the two classes of LOCA it is appropriate for consideration in the large break class but does not affect small breaks (the LBLOCA model includes special provisions and assumption on containment pressure which conservatively enhance this aspect of the transient). Therefore, for SBLOCA evaluations the containment pressure selected should not be skewed for the purpose of enhancing entrainment of liquid.

The second effect involves the relationship between the containment pressure and the ECCS charging rate. The higher the containment pressure, the lower the ECCS charging rate and vice versa. Thus, for conservative evaluations, the containment pressure should be set at or above that which is realistically expected. To assure conservatism, the containment pressure used in the SBLOCA evaluations will be held constant at or above the highest containment pressure reported in the FSAR for any event (may not even be a LOCA). This pressure is reasonable and slightly higher than expected, creating an underprediction of the low pressure injection ECCS flow and therefore a conservative evaluation of the accident.

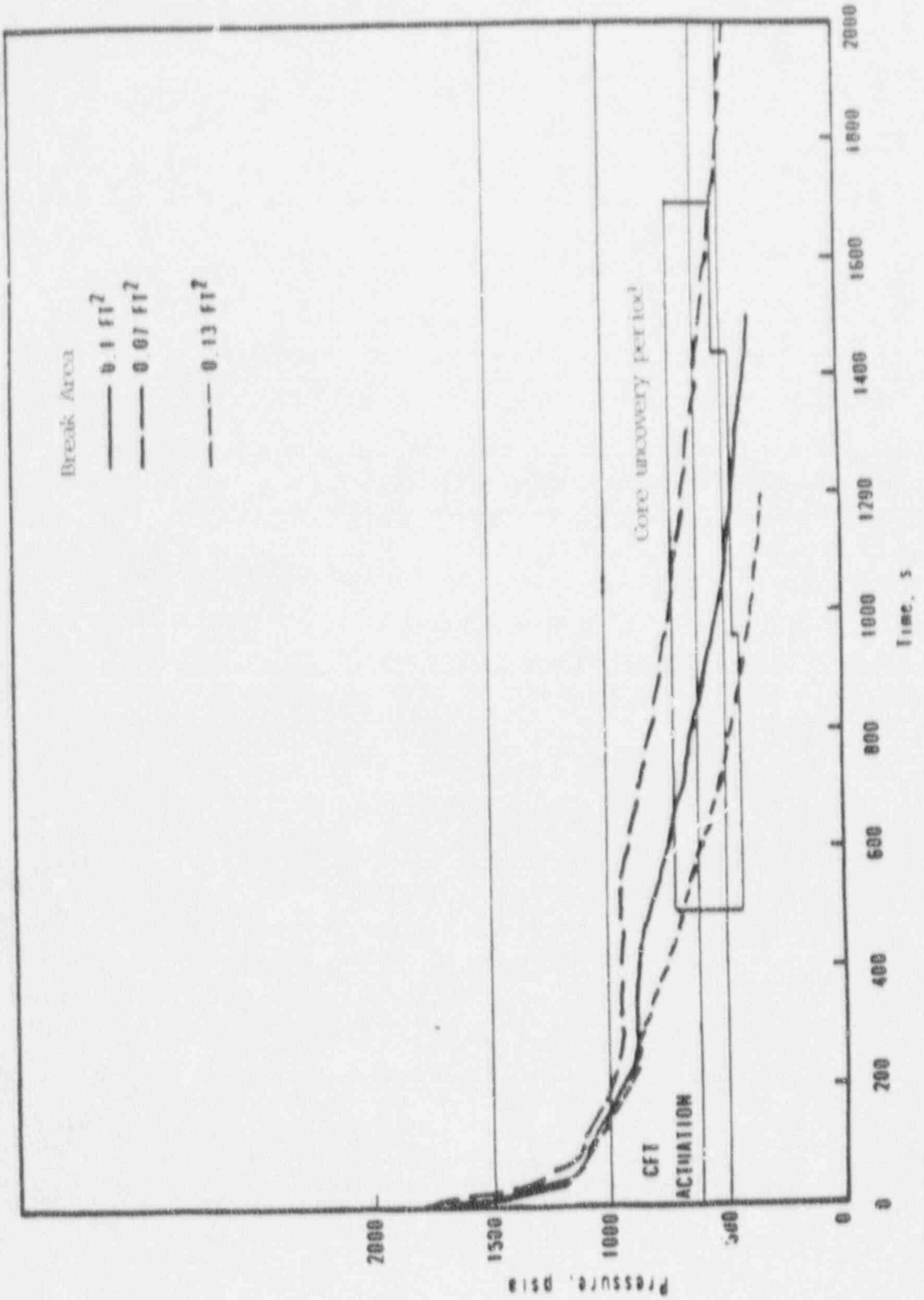
Figure 62.1 SBLOCA PRESSURE FOR WESTINGHOUSE DESIGN




RCS PRESSURE 4 INCH SMALL BREAK

CATAWBA NUCLEAR STATION

Figure 62.2 SBLOCA PRESSURE FOR B&W DESIGN



63. Question: In discussing compliance with Appendix K, Item I.D.3 of Section 4.4 stated that the effects of injecting accumulator tank cover gas were not considered in the SBLOCA model because they were beneficial or inconsequential. Provide additional information to justify not including the effects of the cover gas injection, including the potential effects of neglecting the gas injecting on natural circulation, core heat transfer, and steam generator heat transfer.

Response: The effect of accumulator tank cover gas ($N_2$) is discussed in the response to Question 17 (LBLOCA) and further expanded relative to small break LOCA herein. The possibility of cover gas injection into the reactor coolant syster at a time critical to core cooling exists only for those breaks that depressurize to the low pressure injection system pressure range before the ECCS mitigates the accident. For system pressures above about 150 psia the cover gas will not expand beyond the volume of the tank in an polytropic process.

Breaks that are mitigated by the low pressure system do not have significant core uncovery (usually there is no core uncovery) and are cooled by pool boiling for cold leg breaks or forced flow, by the injection system, for hot leg breaks. None of these events will evolve to a natural circulation condition. For a cold leg break, the gas would accumulate in the upper regions of the downcomer, cold leg, and reactor coolant pumps or pass out of the break. The gas could create a slight over-pressure on the system and would not be transferred to the core. For hot leg breaks, the gas would accumulate in the same regions of the downcomer and cold legs but be gradually swept through the core by the injection flow. Once in the core, the gas would accumulate in the upper plenum. Because the core will be experiencing

forced flow cooling, the presence of small amounts of $N_2$ will not substantially alter the core heat transfer coefficient. None of these breaks rely on the steam generators to provide core cooling, so accumulation within the steam generator tubes, even if possible, is inconsequential.

Accidents that under-go the most limiting conditions at pressures above 150 psia will eventually depressurize to below 150 psia. These events fall into two categories: (1) those that depressurize spontaneously and (2) those that are depressurized according to plant operational procedures. If the system depressurizes spontaneously, then at the time of depressurization the core cooling situation is exactly as described for the larger breaks and the injection of some Nitrogen is inconsequential. These events do not evolve to a natural circulation condition and are independent of the steam generators. If the system is forced to depressurize by the operations staff, then the operators will take action to prevent the injection of accumulator cover gas in the same way as for a normal shutdown.

Therefore, the cover gas on the accumulator tanks has no significant interaction with the system during small break LOCA and need not be included in simulations of such events.

64. Question: In discussing compliance with Appendix K requirement regarding refill and reflood heat transfer, Item I.D.5 of Section 4.4 stated that, if core uncovery is calculated, as the core is recovered the rods are cooled by film boiling until nucleate boiling can be appropriately established. Define what is meant by "appropriately established".

Response: As discussed in Section 4.3.3.2, FRAP-T6-B&W is used to evaluate the cladding temperature response for small break LOCA including the core recovery. During an SBLOCA transient, prior to the advancement of the mixture level and the recovering of a given elevation of the core by mixture, that elevation is cooled by convection and radiation to vapor. When that level is again covered by mixture, the heat transfer regime switches to film boiling. At the decay heat levels at which SBLOCAs are evaluated, film boiling produces abundant cooling which returns the cladding to within a few degrees of saturation over a period of one to three hundred seconds. In reality, the cooldown of the cladding should initiate with film boiling, but it will transition to pool nucleate boiling as the cladding temperature approaches to within about 600°F of the saturation temperature. This produces a temperature trace that agrees with the simulation at first but transitions rapidly to saturation as the quench temperature is reached. Because there is no real concern over the accuracy of the post recovery cladding temperature calculation and because the exclusive use of film boiling is conservative, the B&W simulation of the post recovery cladding temperature will not simulate a clad quenching and a return to nucleate boiling.

Should it become important to simulate a return to nucleate boiling, B&W will apply the interpretation of Appendix K put

forward in the responses to Questions 60 and 61 of this set and will modify the logic of FRAP-T6-B&W to incorporate a SBLOCA option to use a pool nucleate boiling heat transfer coefficient for cladding below the mixture level and within 600°F of the saturation temperature.

65. Question: Section 4.2 stated that if RELAP5/MOD2-B&W predicted the core is covered with liquid throughout the transient, no thermal analysis is required and the compliance with 10 CFR 50.46 is assured. What criterion is used to determine whether the core is considered covered or uncovered in the RELAP5/MOD2-B&W calculation? Justify that the criterion chosen will conservatively determine core uncovery.

Response: The criterion ultimately used to decide if the core has uncovered is the core mixture level calculated by the FOAM2 code. Because the RELAP5/MOD2-B&W mixture level prediction may not agree with the FOAM2 prediction, FOAM2 test runs are used to determine when the core liquid inventory has decreased to an amount that allows uncovery. Once the liquid inventory has fallen to this level a FOAM2 run is made to verify the mixture level. To avoid repeated unnecessary applications of the FOAM2 code, the swell level is estimated from the RELAP5/MOD2-B&W collapsed liquid level using a 1.3 factor. This factor is based on FOAM2 results conducted at a 2% power level (the decay heat level at about 2000 seconds) and at 1100 psia system pressure. It neglects steam production due to primary metal heat and flashing. So long as the core power is above 2% and the system pressure is below 1100 psia the estimated swell level, for swelling near or over the top of core, will always be underpredicted by the factor. Therefore, the swell level estimated with this factor provides a conservative guide to the start of core uncovery. Once the rough time period of core uncovery is established, the FOAM2 code is run repeatedly as the core conditions change to provide a map of the core mixture height versus time. This FOAM2 generated mixture height is transferred to FRAP-T6-B&W in order to compute the cladding temperature.

FOAM2, by itself, is not intended to produce a conservative mixture level calculation. The code is a best estimate simulation and has been benchmarked extensively against level swell experiments (see the FOAM2 report, BAW-10155). The use of FOAM2 within the B&W evaluation model, however, assures a conservative mixture height evaluation because average channel inputs are used. Hot channel steam flow and mixture height are both higher than the average channel leading to a conservative cladding temperature evaluation when average channel parameters are used. Question 53 of this set presents the discussion leading to this conclusion in more detail.

References:

65.1 BAW-10155, "FOAM2-Computer Program to Calculate Core Swell Level and Mass Flow Rate During Small-Break LOCA," Babcock & Wilcox, November 1982.

# APPENDIX E

Responses to Round 2 Request
for Additional Information

1. (a) Revision 1 of BAW-10168 stated that the ECC transport time, the time for the ECC to fall to the lower plenum once it enters the downcomer, is neglected based on Upper Plenum Test Facility (UPTF) Test No. 5. However, the UPTF data showed an approximately 2 second delay from the time the ECC entered the downcomer to the time the lower plenum collapsed level began to increase. Therefore, clarify your reasoning for neglecting the ECC transport time in the REFLOD3B analysis and discuss the specific data from UPTF Test No. 5 that supports your position.

Response: In Section 4.3.6.5 of the EM topical report BAW-10168 it is stated that the time required for ECCS water to pass from the reactor vessel inlet to the lower plenum is neglected and refill begins at the end of blowdown. The time delay was neglected based on the consideration that in reality bypass of the ECC water occurs at the broken cold leg nozzle, that when the flow at this nozzle is directed toward the reactor vessel bypass no longer occurs, and that ECC water in transit across the downcomer would, therefore, not be ejected from the reactor coolant system. The water in transit across the downcomer at the end of blowdown would fall into the lower plenum during the first several seconds of refill essentially eliminating the transit time delay in question. These observations are supported by UPTF test 5A. Figure 1-1, the test vessel inventory for test 5A, shows that partial ECC retention starts much earlier than the end of blowdown and full retention precedes the end of blowdown by several seconds. Figure 1-2 provides the inventory for UPTF test 4A and demonstrates the same effects. Both of these figures and the sequence of events for test 5A (refer to Table 3-1 of Reference 1.2)

| | |
|---|---|
| Begin ECC injection | = 24 s (Plot time) |
| Begin Opening cold Leg Break | = 29 s |
| ECC Enters Downcomer | = 31 s |
| ECC Reaches Bottom of Downcomer | = 32.5 s |

show a 1.5-second delay, in agreement with the question, from the first penetration of the downcomer until ECC water is observed in the lower parts of the test vessel. Under Revision 1 of the RSG LOCA evaluation model, that delay occurs as the ECCS first delivers water to the reactor vessel and should not be repeated during refill.

Although the position that transit liquid exists in the downcomer at the end of bypass, as defined in the BWFC RSG LOCA evaluation model, is correct and supportable by the experimental studies in this area, it does not necessarily agree with the requirements of Appendix K with respect to the bypass of ECCS water. To achieve a more explicit agreement with Appendix K requirements, BWFC will alter both the accounting for gravity time delay and the definition of end-of-bypass. A gravity time delay appropriate for the freefall of fluid from the bottom of the cold leg nozzle to the bottom of the active region of the core will be accounted for in the evaluation. The end of bypass will be defined in relation to the end-of-blowdown as a predecessor event using a correlation developed from the UPTF tests, but ECCS water will still be bypassed by the RELAP5/MOD2-B&W calculations until the end-of-blowdown. To adjust for the excess bypass, occurring over the last 2 or 3 seconds of blowdown, that amount of water will be reintroduced to the vessel in the REFLOD3B code at the beginning of refill.

UPTF Tests and CCFL Correlation

The UPTF test facility simulates, at full scale, a 3900 MWt German PWR that closely represents a full scale US PWR (see Reference 1.3 for scaling details). Steady state and transient tests in the facility show that the reason for early partial delivery of the ECC water is

strong multi-dimensional effects in the downcomer. To evaluate and quantify these effects, a series of steady state downcomer countercurrent flow tests were run and countercurrent flow limit (CCFL) correlations developed to fit the data. Details of these tests, the results, and the correlations are given in Reference 1.3. Equations 4 and 5 of reference 1.3 are both $J^*$ (the Wallis parameter) CCFL correlations that have been fit to the data. The value of $J^*_{g',eff}$ determines the potential for ECCS bypass. If $J^*_{g',eff}$ is zero or negative the steam flow is insufficient to entrain liquid and bypass will not occur. If $J^*_{g',eff}$ is positive some entrainment will be occurring and for low values of $J^*_{g',eff}$ partial bypass is occurring. At present the RSG evaluation model does not intend to deal with partial bypass. Because the form of equation 4 may be somewhat more standard, and the report indicates that it does a slightly better job of fitting the data, this correlation has been selected for use in the RSG LOCA evaluation model.

$$J^{*\,1/2}_{g,eff} = \left[ J^*_g - f * J^*_{g,cond} \right]^{1/2} = C + m * J^{*\,1/2}_f$$

where f, C, and m are correlation factors giving:

f = condensation efficiency,

C = effective dimensionless steam upflow at complete bypass, and

m = slope of steam flow/water flow relationship.

Curve 1 of Figure 1-3 compares the correlation to data for multiple loop ECCS injection conditions. The reference includes a different correlation of the same form for single loop injections (curve 2 of Figure 1-3), but those conditions would not be appropriate for application in the evaluation model. As can be seen in

the figure the correlation is excellent.

As further support for the UPTF results, Izenson and Crowley at Creare (Reference 1.2), evaluated UPTF Test 5 and the earlier experiments in Creare test facilities at 1/15 and 1/5 scales. They concluded that the ECC bypass effects due to flashing are small at full scale compared to results of model tests and that the bypass rate observed in Test 5 appears to be the maximum achievable in the UPTF facility. Therefore, because the UPTF facility is geometrically similar to U.S. PWR designs, the correlation will be conservatively applicable to the determination of the end-of-bypass for the RSG LOCA evaluation model.

Application within the Evaluation Model

For application to the evaluation model, the current practice of bypassing all ECCS injection up to the calculation of end-of-blowdown (flow toward the vessel at the vessel side of the break) in RELAP5/MOD2-B&W is continued. The UPTF CCFL correlation is used after the blowdown calculations are complete to determine the time at which bypass should have ended. The ECC injection flow rate and the ECC subcooling in the correlation are replaced by the predicted liquid flow rate and subcooling of the water that is entering the downcomer from the intact loop cold legs. These values are more appropriate to use because they represent the state of the liquid entering the bypass region. In the UPTF steady state tests steam was not injected into the cold legs so that the liquid state at injection and in the downcomer were the same. The ECCS water bypassed between the end-of-bypass predicted by the CCFL correlation and the end-of-bypass predicted by RELAP5/MOD2-B&W is then added to the reactor vessel inventory at the end of blowdown and

included in the initial REFLOD3B lower plenum inventory. Should the UPTF CCFL correlation not indicate the end-of-bypass prior to the end-of-blowdown then the end-of-bypass shall be taken as the end-of-blowdown and no additional or replacement water added to the initial conditions for the REFLOD3B code. Consequent to this the delivery of post blowdown ECCS water to the lower plenum is delayed by the time appropriate for the free fall of fluid from the bottom of the cold leg nozzle to the bottom of the active region of the core.

As an example, the CCFL correlation was applied using the downcomer steam flow rates and the system conditions from a LOCA limit case in the McGuire/Catawba LOCA analysis topical, BAW-10174. The calculated $J^*_{g,eff}$ and $J^*_f$ values, after the accumulator injection began, are shown in Figure 1-4. In the figure, positive values indicate upward flow and negative values downward flow. The negative value of $J^*_f$, indicates that some penetration of the ECCS water into the lower plenum should have occurred as soon as the accumulators started injection (cold leg filling and other time delays set aside). The positive value of $J^*_{g,eff}$ prior to 19.5 seconds, however, indicates that the downcomer steam flow is sufficient to partially bypass the ECCS injection. After 19.5 seconds the ECCS should not have been bypassed. As the end-of-blowdown for this case was 21.2 seconds, the last 1.7 seconds of bypass was inappropriate.

This new approach more clearly accounts for bypass and ECCS injection effects while retaining substantial conservatism. The early penetration of the ECCS into the lower plenum occurs because of three-dimensional effects not represented in one dimensional codes such as RELAP5/MOD2-B&W. Figure 1-5 compares the downcomer liquid flow rate, from the example above, as calculated

by RELAP5/MOD2-B&W to that predicted by the UPTF CCFL correlation. Even with complete bypass, RELAP5 was causing liquid to flow up and out of the break while the correlation indicated that some liquid flow should be downward. This partial penetration of the ECCS water will not be credited in the evaluation model and forms a substantial conservatism. The direct consideration of the liquid fall time more clearly accounts for that consideration.

(b) The RSG LOCA EM defines the end-of bypass as the end-of-blowdown, which is defined as the time reverse flow is calculated at the vessel side break. While these definitions are appropriate for most situations, clarify how they allow the EM to handle the following situation: Reverse flow is calculated at the vessel side break due to steam condensation on the ECC injected into the intact cold leg causing a low pressure zone, which causes reverse steam flow up the downcomer so that the ECC is unable to penetrate the downcomer. In this situation, would a more appropriate definition of the end-of-bypass be one that is based on downflow in the downcomer, and one that includes the effects of countercurrent flow limitation phenomena?

Response: The situation described in the question has not occured in evaluation model calculations and is not expected to occur in reality because of multi-dimensional downcomer flow effects. As shown in the response to part "a" of this question the conditions in the downcomer toward the end of blowdown support first partial and then full penetration of ECCS water into the lower plenum several seconds prior to the end-of-blowdown. Even if the suggested scenario were to occur the ECCS liquid would not be being bypassed from the reactor coolant system and there would thus be no need to adjust the end-of-bypass definition. Notwithstanding these conclusions, BWFC agrees that a definition of the end-of-bypass based on countercurrent flow limits would be more appropriate and has adopted such an approach as outlined in the

response to part "a" of this question.

In practice, the "end-of-blowdown" has been (and is) taken to be that time in the calculation at which it is both appropriate and acceptable to make the transition from the blowdown calculational technique to the reflood technique. This switch in methods and models is, of necessity, somewhat arbitrary, but it does recognize that the dominant physical processes being analyzed change at some point in the transient. In reality, the change is not so distinct or abrupt as it must be represented to be in the modeling. For example, experiments show that partial refilling of the lower plenum starts as soon as the accumulators begin injection. Furthermore, these experiments show that the lower plenum is full at the time that the containment and reactor coolant system pressures approach each other indicating that there should be no "refill period" and that reflooding of the heated core follows immediately. Thus, definitions of end-of-blowdown and end-of-bypass that comprise marked periods of refill prior to reflooding are inherently conservative and should be acceptable logic for switching the calculational technique.

Calculations performed to date by BWFC using its RELAP5 based evaluation model have not shown eratic or irregular occurences of end-of-blowdown as defined by the negative vessel side break flow criteria. However, in order to assure that the observations of the above paragraphs continue to apply, any occurence of end-of-blowdown that appears to have been caused by an eratic process and that appears to have shortened the blowdown period by more than four seconds will be rejected and the evaluation either continued or examined for possible corrective actions. The typical refill period for the plants covered by the BWFC evaluation model is 9 to 10 seconds.

Therefore, the clause above would be activated long before the refill period could be totally eliminated.

(c) If loop seal refill could occur as a result of ECC back flow into the loop seal during periods of steam upflow in the downcomer, is the RSG LOCA EM capable of calculating this phenomenon with RELAP5/MOD2-B&W? If not, justify why the EM should not be required to calculate this type of phenomena.

Response: The nonhomogeneous option of RELAP5/MOD2-B&W is used for evaluation model loop and the downcomer flow calculations. This allows countercurrent flow in the cold legs giving RELAP5/MOD2-B&W the capability of predicting ECC back flow towards the loop seal. If a seal were to form, the modelling of the cold legs, including the nonhomogeneous option, is sufficient to force the establishment of appropriate RCS conditions for the clearing of the seal. However, during blowdown the cold leg steam velocities in the intact loops are much higher than those at which countercurrent flow could occur. Thus, in practicality, backflow of the ECCS and loop seal formation does not occur. Some backflow has been observed experimentally in UPTF Test 5 (see Reference 1.1). These experiments, however, did not allow or model loop steam flow (the cold leg piping was closed off at the pump simulator) and are therefore inapplicable with respect to liquid backflow.

References

1.1 2D/3D Program Upper Plenum Test Facility Quick-Look Report, Test No. 5, Downcomer Separate Effect Test, U9 316/87/17, KWU, October 1987.

1.2 M.G. Izenson and C. J. Crowley, Scaling of Flashing Transient Behavior to Full Scale : Evaluation of UPTF Test 5A, TM-1277, Creare Inc., September 1988.

1.3 2D/3D Program Draft Report, Summary of Results from the UPTF Downcomer Separate Effects Tests, Comparison to Previous Scaled tests, and Application To U.S. Pressurized Water Reactors, MPR Associates, Inc., December 1989.

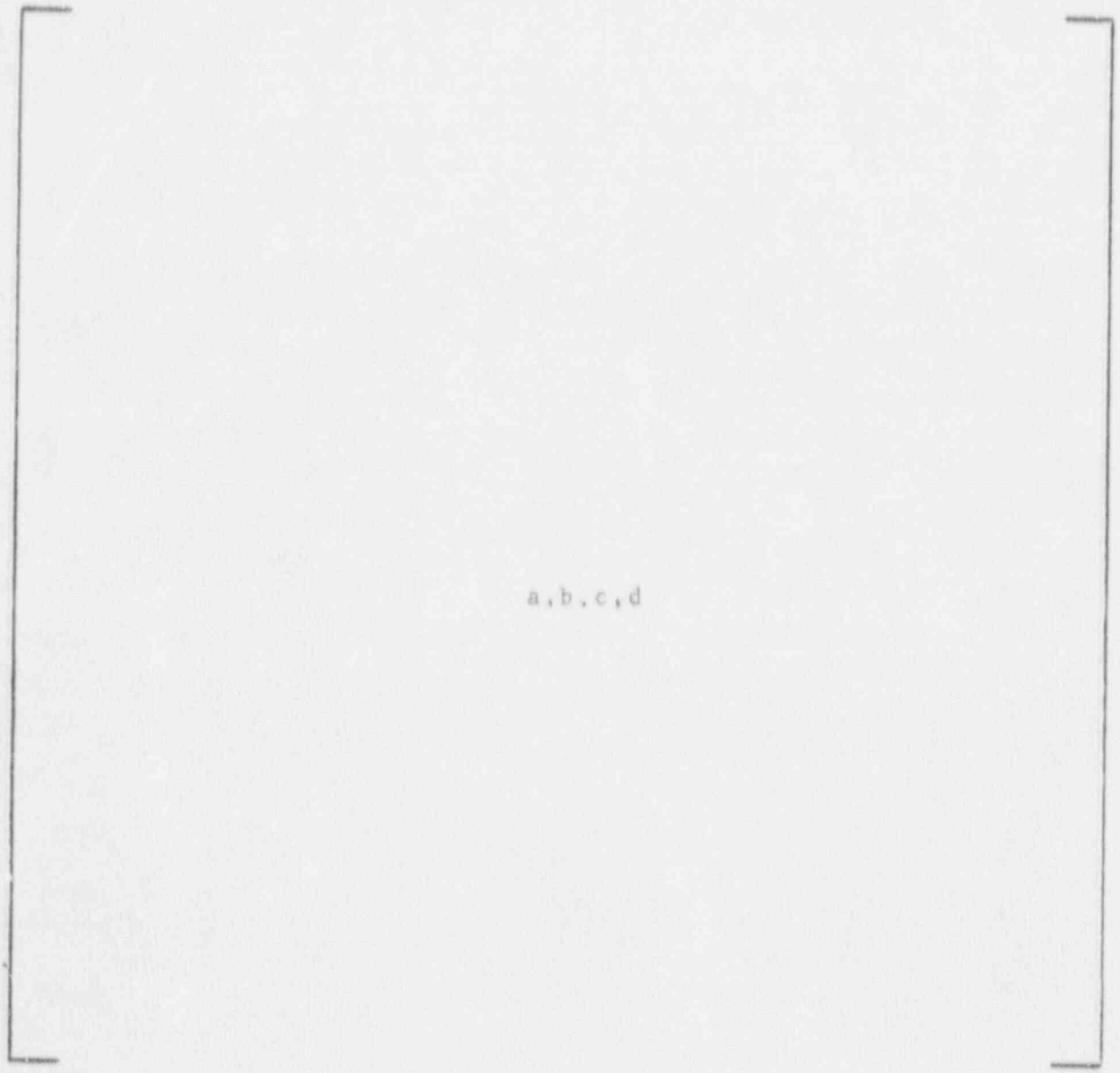



Figure 1-1 Test Vessel Inventory For UPTF Test 5 - Phase A[1-3]

Figure 1-2 Test Vessel Inventory For UPTF Test 4 - Phase A[1-3]

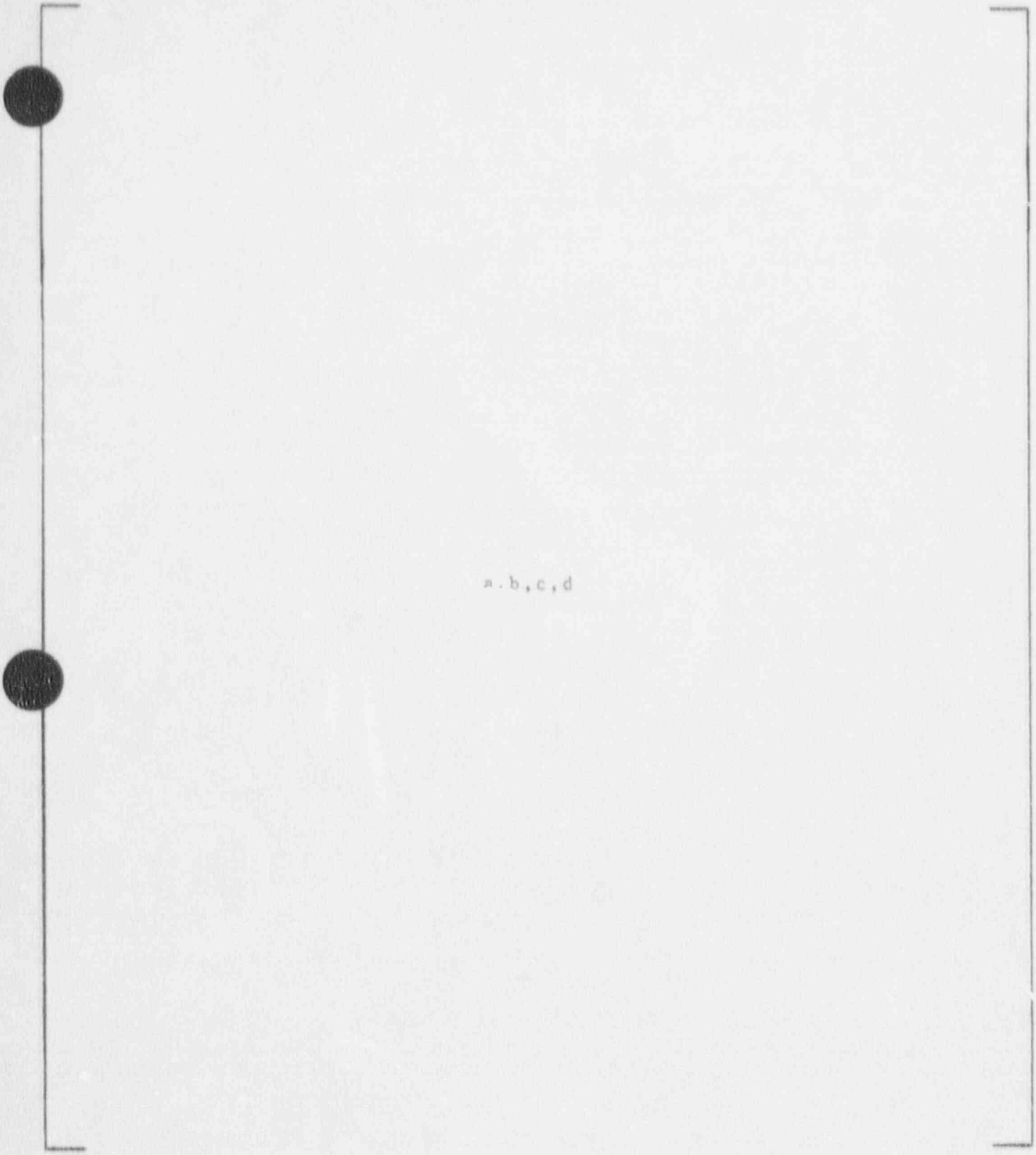



Figure 1-3 UPTF Steady-State Counter-Current Flow Tests Dimensionless Steam And Liquid Velocities Correlated With Equation 4 Given In Reference 1-3

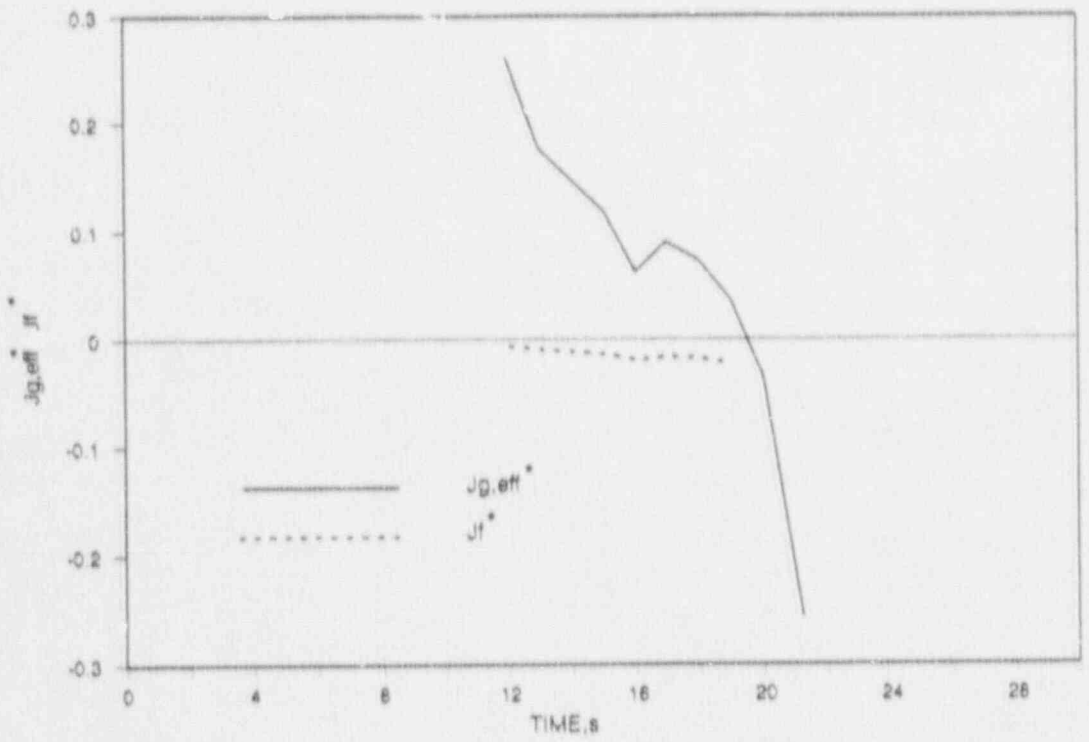



FIGURE 1-4. DIMENSIONLESS DOWNCOMER VELOCITIES CALCULATED USING THE CCFL CORRELATION.

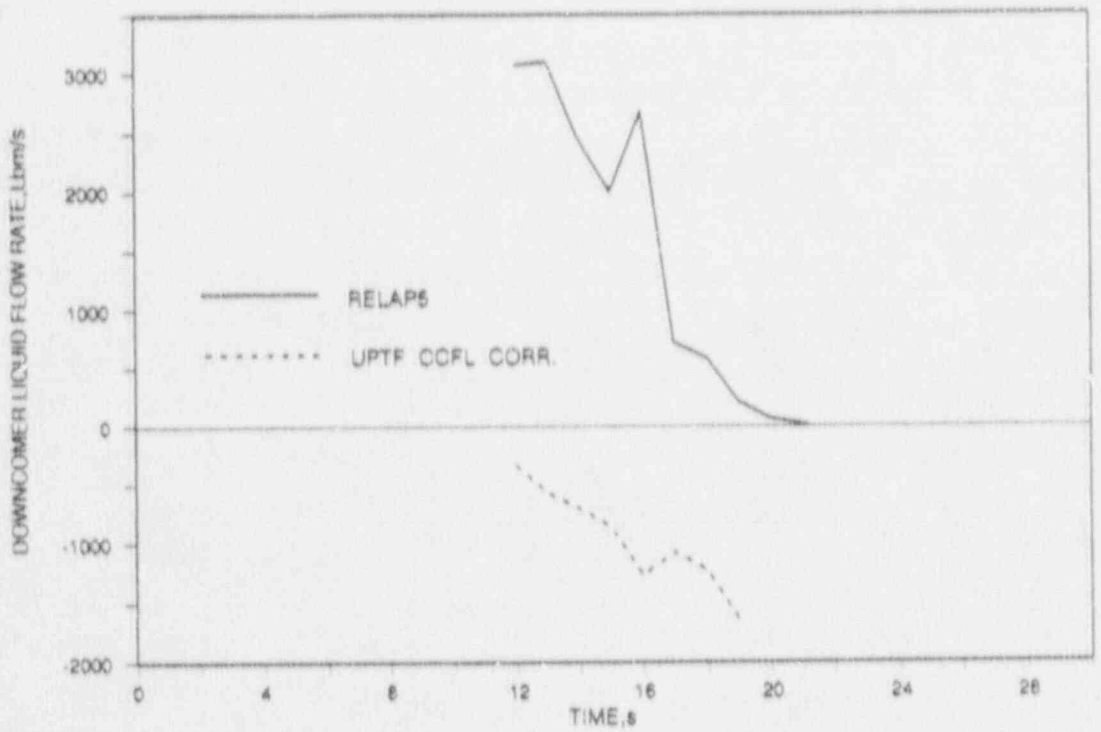



FIGURE 1-5. DOWNCOMER LIQUID FLOW RATES FOR A PLANT CASE.

2. For large break LOCAs, you stated that local cladding oxidation would be calculated as long as the cladding temperature was above 1500°F and the core mixture level was below the top of the cladding, and that the mixture level would be conservatively assumed to be 20% above the REFLOD3B calculated core collapsed liquid level.

(a) Clarify why a 20% increase in the REFLOD3B collapsed liquid level is conservative for all possible core conditions that could be encountered during reflood and for all plants covered by the EM.

Response: To determine the amount of local or corewide cladding oxidation, the simplest approach would be to extend the cladding temperature in time until the rate of continued oxidation was reduced to an insignificant level. As is developed in Part b to this question, that would involve the computation of cladding temperature until the temperature had dropped to about 1000 F. Unfortunately, conservatisms included in BEACH during post-peak cooling are excessive for such calculations and necessitate the use of an alternate criterion for determining the effective end of cladding oxidation.

An illustration of the conservatism is provided in Figure 2-1, which shows cladding temperature and core quench height as functions of time. The cladding temperature in the figure is for the location of peak cladding temperature, the 6.3-ft elevation, for the 8-ft LOCA Limit calculation from the Duke application report, Reference 2.1. The quench front passes 6.3 ft at 370 seconds, with the BEACH cladding temperature still at 1850 F. To quench, the cladding must be at 800 to 900 F. The dashed line in Figure 2-1 represents of the temperature trace that would result from a less conservative more accurate code. In order to achieve a conservative prediction of cladding oxidation without including all of the conservatisms of BEACH post-peak cooling, BWFC will use the BEACH predictions until the

cladding temperature is reduced below 1000 F, see part "b" of this response, or until the quench front is predicted to be at or above the location being evaluated.

The original determination of quench height as proposed by BWFC used the REFLOD3B collapsed water level plus 20 percent for the quench height. That technique is conservative for the whole core oxidation determination but may not be valid for local metal-water calculations. In particular, the approach may cut off oxidation too soon at lower elevations and extend oxidation unreasonably at higher core elevations. Therefore, the collapsed water level plus 20 percent has been replaced by the quench height as calculated by REFLOD3B.

The application of the REFLOD3B quench height as one of the criteria for terminating the local oxidation calculation involves two considerations: First, the validity of the REFLOD3B quench correlation, the Lee correlation with modifications to prevent the computation of top-down quenching, should be demonstrated. Second, the application of the average core quench front advancement to regions of the core with power levels above average must be validated.

The modifications to the Lee correlation for application within REFLOD3B consist of setting the quench front velocity to the absolute value of the velocity calculated by the Lee correlation and limiting it to the cold fill rate. These modifications are fully explained in the REFLOD3B topical report, BAW-10171. This prevents the computation of top-down quenching, making the quench height a monotonic function of time. Since the unmodified correlation tended to predict top-down quenching when the quench height reached 9 feet, there is little difference between the modified and unmodified

correlations prior to quenching at the 9-ft elevation. Reference 2.2, in Chapter 8 and in Appendices G and H, presents comparisons of the Lee correlation to 40 different tests. Comparisons are available for 17 x 17, 15 x 15, and 15 x 15 skewed power bundle configurations. In general, the correlation agrees quite well with the data but this is not always the case for the 17 x 17 data.

The differences between the FLECHT-SEASET data and the correlation do not invalidate the correlation for application to the 17 x 17 arrangement for two reasons: the modifications to the correlation for use with REFLOD3B substantially improve the correlation for the 17 x 17 geometry, and the FLECHT-SEASET data are for electrically-heated rods without gaps. Figures 2-2 through 2-7 compare the modified Lee correlation to the unmodified version and the data for six of the FLECHT-SEASET tests that showed substantial deviations. In the figures, the two versions of the correlation agree very well for quench levels below about 9 ft, and the modification produces much better agreement with the data and its trends above that height. Some additional comparisons of the base and modified correlations to data are available in Reference 2.3. The gap in a nuclear fuel rod allows quenching to proceed much faster than is the case for electrical simulation rods without gaps. The OECD Halden Reactor Project, Reference 2.4, examined the quenching behavior of different fuel rod simulators in comparison to nuclear fuelled rods. Figure 2-8, reprinted from Reference 2.5, presents a comparison of quench times for rods that simulate gaps and the FLECHT-type rod. As shown in the figure, rods that simulate the gaps in nuclear fuel elements quench up to 150 seconds earlier than do FLECHT type rods.

Multidimensional effects substantially improve hot channel heat transfer over that observed in one-dimensional simulations. This has been observed in the both SCTF and CCTF experiments. Within the hotter fuel assemblies, substantially more water is boiled and entrained by the reflooding process than within the colder assemblies. Therefore, as the core fills, hydrostatic head imbalances are set up between the cold and hot fuel assemblies that lead to increased flow to the hottest assemblies and correspondingly lesser flow to the coldest assemblies. The end result is a flattening of the cladding temperature performance, most notably the quenching time, across the core radially.

Figures 2-10 and 2-11 provide examples of the results of multidimensional effects studies from SCTF and CCTF, References 2.5 and 2.6 respectively. The SCTF configuration is a single row of eight fuel bundle simulators. Within Reference 2.5 a comparison is made between a flat bundle average power distribution and a "steep" bundle average power distribution. Figure 2-9 shows the two distributions. The peak power for the steep profile test is about 20 percent higher and the coldest power about 20 lower than the flat power profile simulation. Figure 2-10 shows that the variation of quench time with average bundle power, three different axial positions are shown, is essentially flat. Other comparisons made within the reference confirm the effect. Figures 13 and 14, of the reference, provide heat transfer coefficients and show about a 20 percent increase for the hot power zone in the steep profile over the same bundles in the flat profile.

The power peaking for the central region of the CCTF facility was set to a relative peaking of 1.364 with the immediate surrounding bundles at 1.195 and the cold

bundles at 0.761. Figure 2-11 shows a comparison of quench time versus elevation for th· three regions. There is little difference between the regions as to quench time and certainly none as large as the power level differences. Thus, quench time is essentially independent of radial peaking, confirming that the average core quench time can be reasonably applied to the hot channel for the prediction of cladding quench behavior.

From the foregoing discussion, the Lee correlation as implemented within the BWFC evaluation model constitutes a sound approach to the termination of the cladding oxidation calculations. Additionally, the following comprise margins of conservatism within the approach:

1. Metal-water reaction is effectively limited to temperatures above 1000 F, whereas quenching typically occurs between 800 and 600 F. By using the quench time as one of the limits for calculation of the oxidation, oxidation may be calculated when the cladding would actually be below 1000 F.

2. The occurrence of top down quenching is real and is likely to occur in a core undergoing the final phases of a LOCA recovery. By disallowing this, those portions of the upper regions of the core that would have experienced early quenching (the upper 25 to 15 percent) will be allowed to oxidize to a higher degree than could actually occur.

(b) With a cutoff temperature of 1500 F for the local cladding oxidation calculation, discuss what effects this would have on the calculation of core wide oxidation, and justify that the local and core wide oxidation values will not be underpredicted to meet 10 CFR 50.46 criteria.

Response: A temperature of 1500 F is used in a number of applications as the cutoff temperature for the computation of cladding oxidation. When the purpose of the calculation is determining material temperatures, this is justifiable because the heating rate at that temperature is negligible. While designing the whole core metal-water reaction technique, BWFC selected 1500 F as a cutoff temperature because that temperature was common in these other applications. However, review of the oxidation rate for green fuel at 1500 F, given the one percent whole core limitation, suggests that a lower cutoff temperature is more appropriate.

Integrating the Baker-Just correlation and solving for time provides the following relationship for the time required to achieve a given amount of oxidation at a fixed cladding temperature:

$$\delta t = \frac{10^4 \rho_c^2}{K e^{-\Delta E/RT_c}} \left[ (R_0 - r)^2 - (R_0 - r_0)^2 \right]$$

where,

| | | |
|---|---|---|
| $\delta t$ | = | time to oxidize, sec, |
| $\rho_c$ | = | cladding density, 6.5 g/cm$^3$, |
| K | = | rate constant, 33.3 g$^2$/cm$^4$-sec, |
| $\Delta E$ | = | activation energy, 45500 cal/gmole, |
| R | = | gas constant, 1.987 cal/gmole-K, |
| $T_c$ | = | cladding temperature, K, |
| $R_0$ | = | outside cladding radius, 0.00475 m, |
| $r_0$ | = | initial radius of oxide metal interface, m, |
| r | = | radius of oxide metal interface, m. |

The time required for green cladding to reach 0.1 percent oxidation at 1500 F ($r_0 = R_0 = 0.0047500$ m, $r = 0.0047492$

m, the clad nominal thickness is 0.0006096 m) is [illegible] 6 seconds. At 1000 F, the time required for the same 0.1 percent reaction is approximately 8,000 seconds. Because a 0.1 percent reaction starts to become significant to the calculation of whole core metal, 1500 F is too high a cutoff temperature. On the other hand, 8,000 seconds is much longer than the temperature excursion spans predicted for large or small break LOCA, making a cutoff at 1000 F more than acceptable. Therefore the procedure for computation of metal-water reaction has been revised to compute oxidation for cladding temperatures above 1000 F. The whole core oxidation will become an integration of the local oxidation, wherein that oxidation is allowed to accrue until the cladding temperature is reduced to below 1000 F or the location has quenched in accordance with the Lee correlation for quench height as outlined in the response to part "a" of this question.

References:


2.1 Mark-BW Reload LOCA Analysis for the Catawba and McGuire Units, BAW-10174, B&W Fuel Company, Lynchburg Virginia, September 1989.

2.2 N. Lee, S. Wong, H. C. Yeh, L. E. Hochreiter, PWR FLECHT SEASET Unblocked Bundle, Forced and Gravity Reflood Task Data Evaluation and Analysis Report - NRC/EPRI/Westinghouse Report No. 10, NUREG/CR-2256, EPRI NP-2013, WCAP-9891, November 1981.

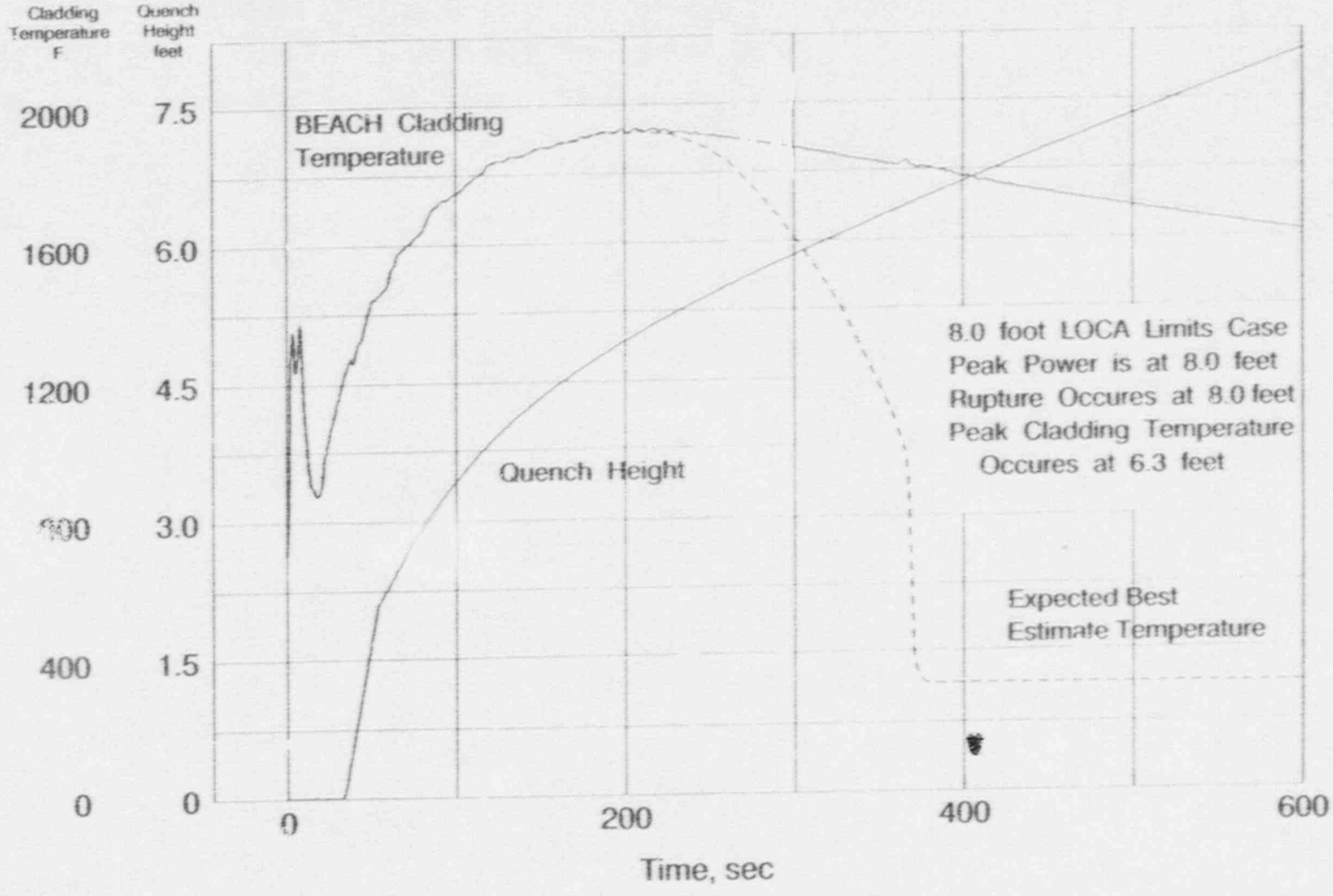
2.3 C. K. Nithianandan and J. R. Biller, "REFLOD3B: A Computer Code Simulating PWR System Behaviors During Reflood," Paper accepted for publication in a Symposium Volume of the 1990 ANS Winter Meeting to be held in Washington DC on November 11-16, 1990.

2.4 T. Johnsen, C. Vitanza, Results of the LOCA Tests with Semiscale Heaters (IFA-511.5), HPR 313, Institute for ENERGITEKNIKK OECD HALDEN REACTOR PROJECT, P. O. Box 173, N-1751 Halden, Norway, May 1984.

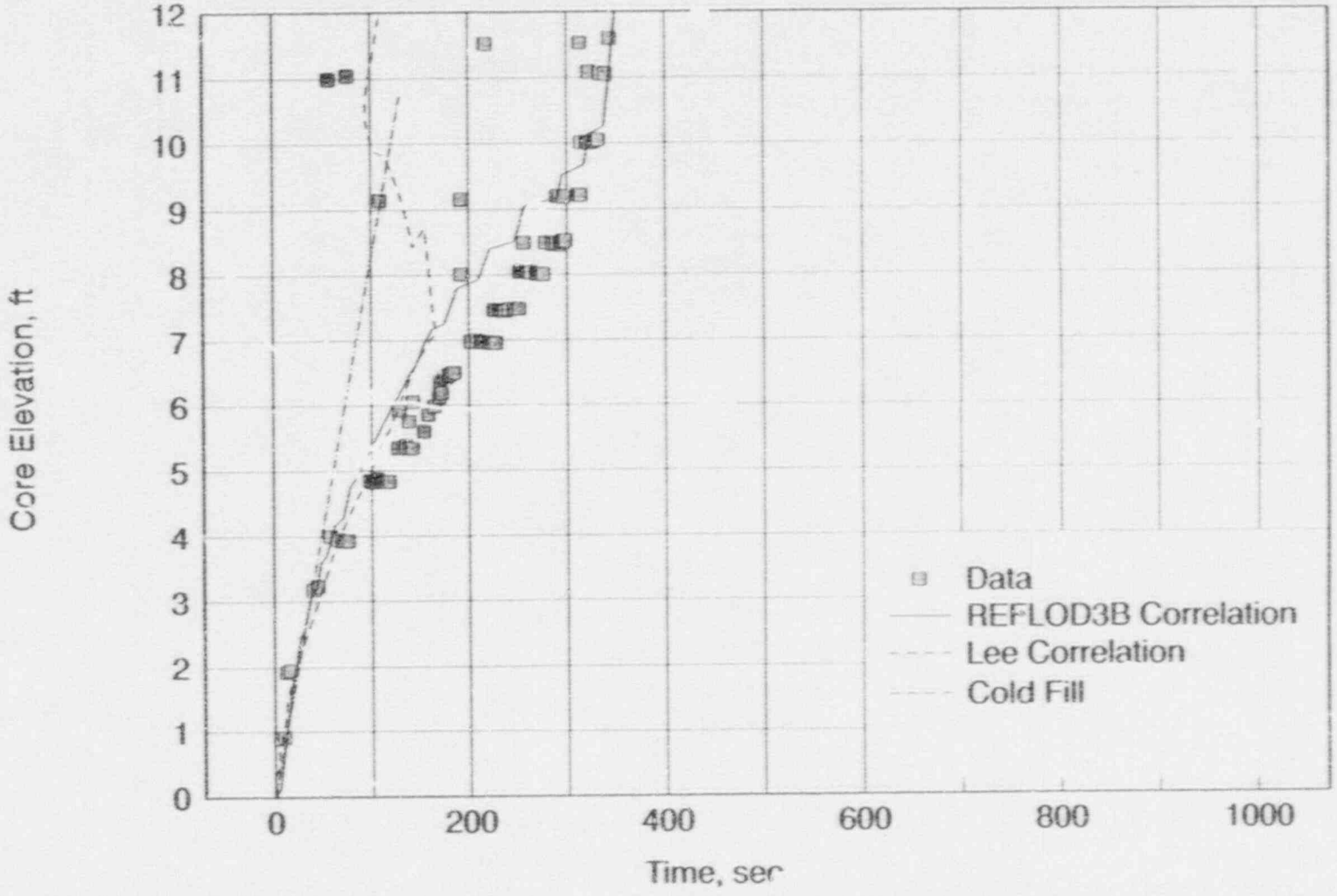
2.5 T. Iwamura, M. Osakabe, Y. Sudo, "Effects of Radical Core Power Profile on Core Thermo-Hydraulic Behavior during Reflood Phase in PWR-LOCAs," Journa of Nuclear Science and Technology, 20[9], pp 743 751, September 1983.

2.6 H. Aki o, T. Iguchi, Y. Murao, "Core Radial Profile Effect on System and Core Cooling Behavior during Reflood Phase of PWR-LOCA with CCTF Data," Journal of Nuclear Science and Technology, 22[7], pp 538 - 550, July 1985.

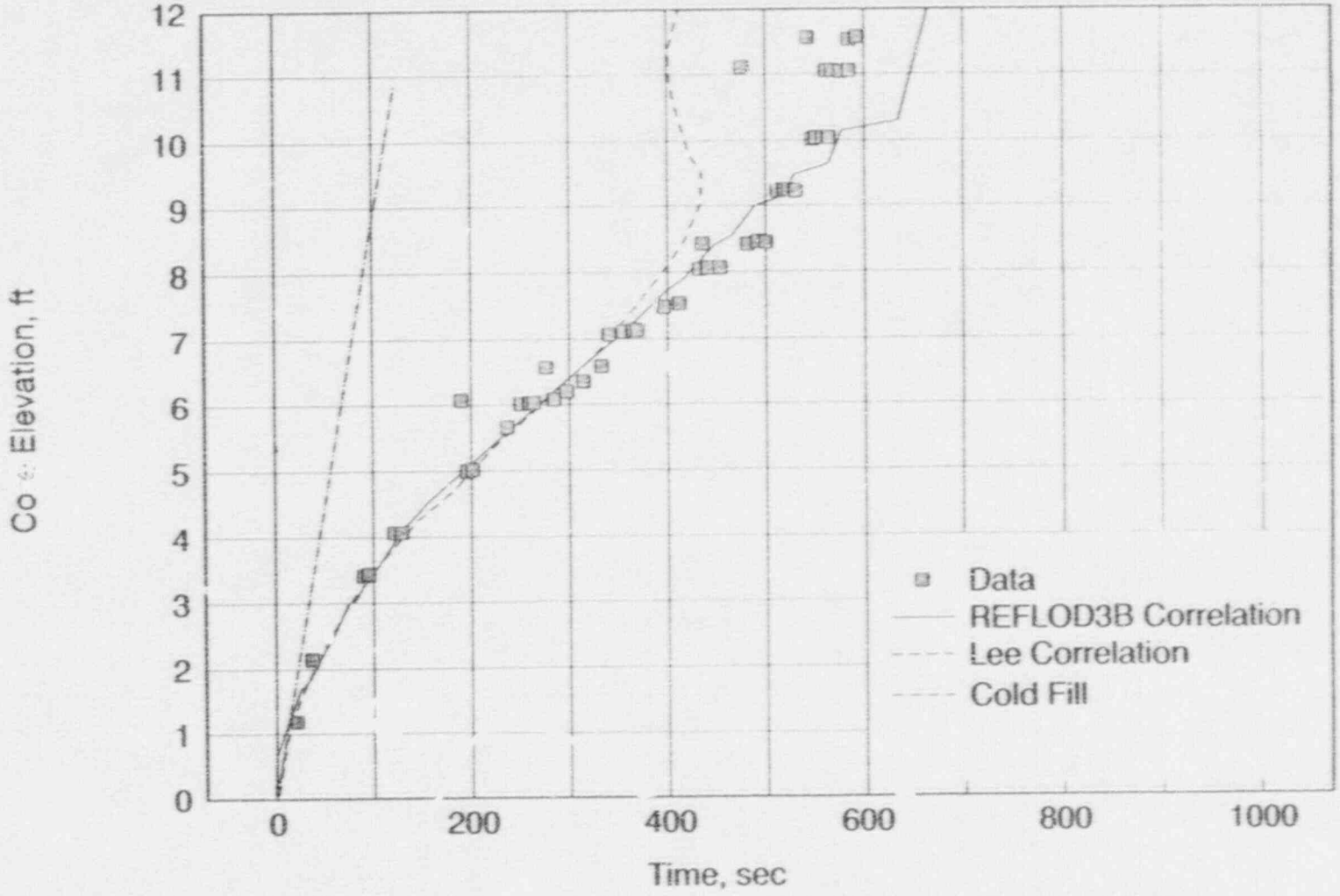
## Figure 2-1 CLADDING TEMPERATURE and QUENCH HEIGHT



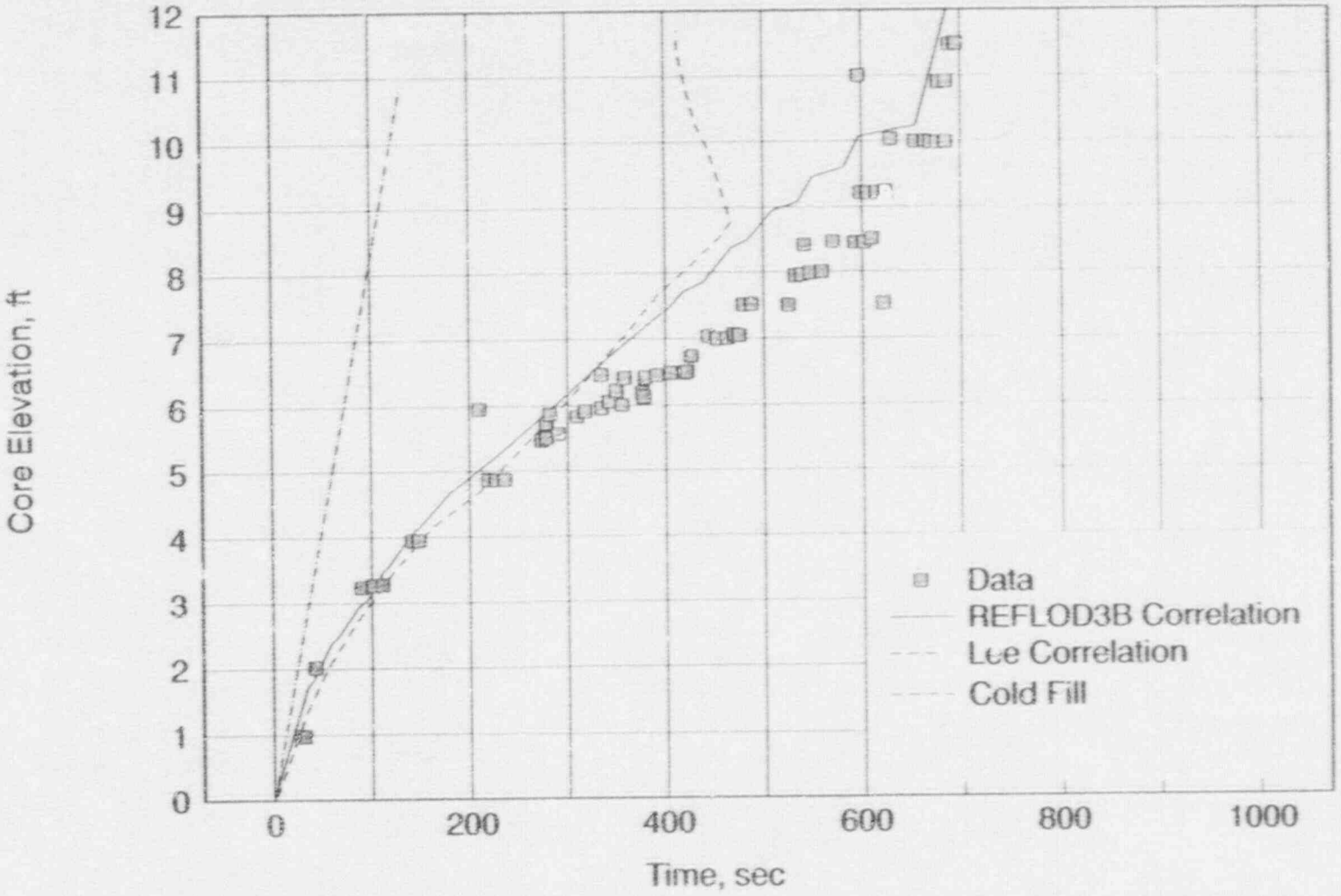
## FIGURE 2-2 Quench Height FLECHT SEASET 30518



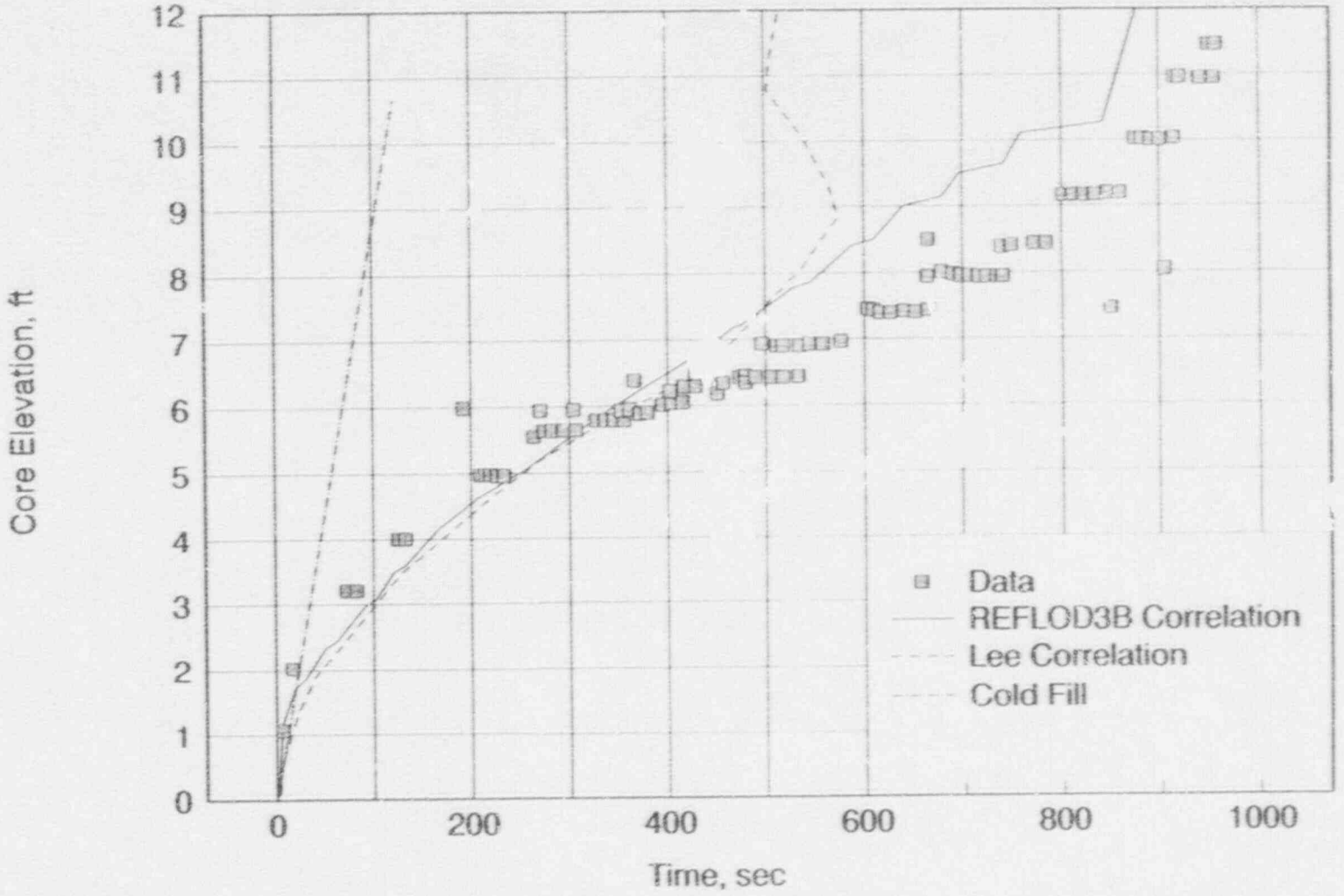
# FIGURE 2-3 Quench Height FLECHT SEASET 31504



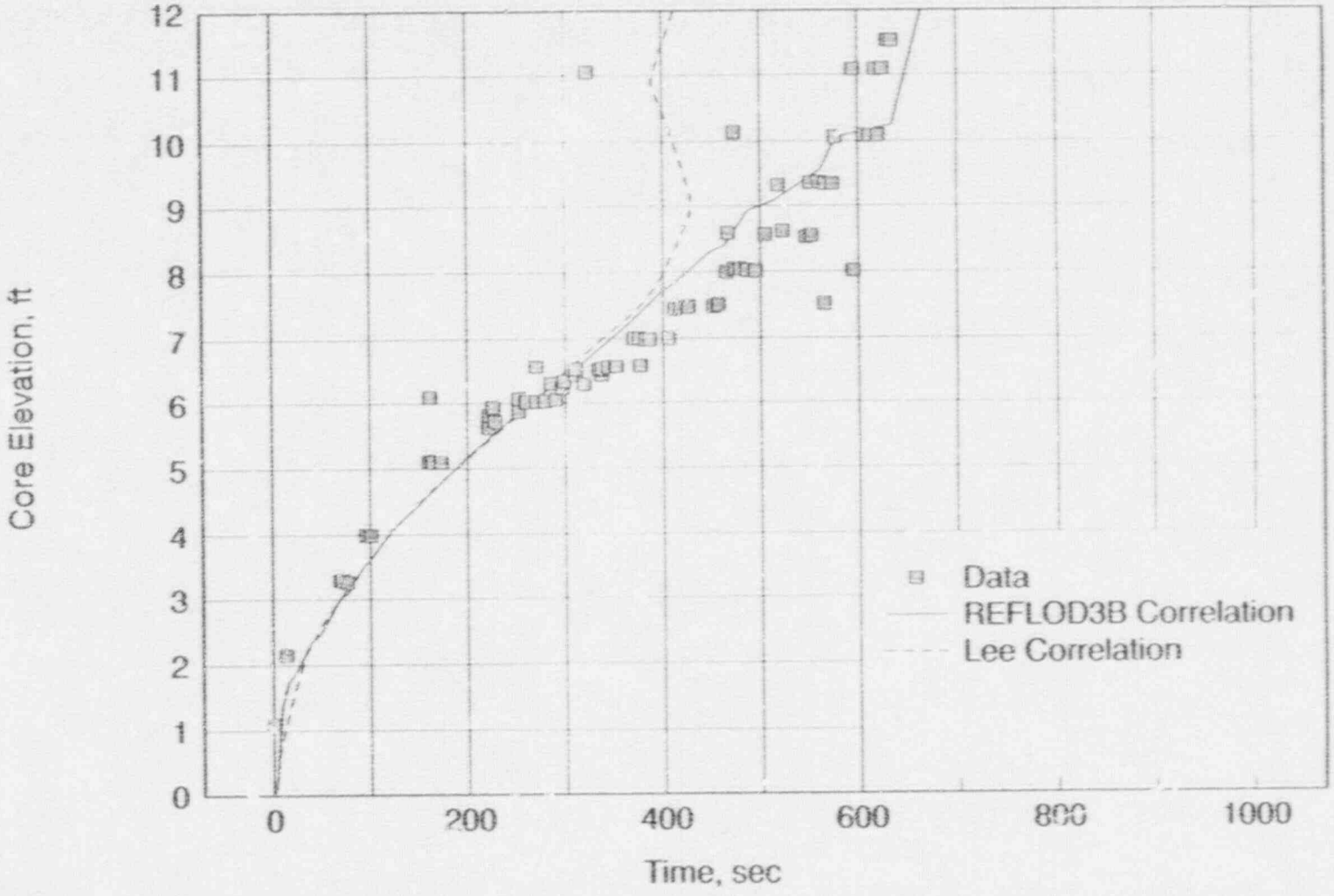
## FIGURE 2-4 Quench Height FLECHT SEASET 31805



## FIGURE 2-5 Quench Height FLECHT SEASET 32235



# FIGURE 2-6 Quench Height FLECHT SEASET 32333



# FIGURE 2-7 Quench Height FLECHT SEASET 34006

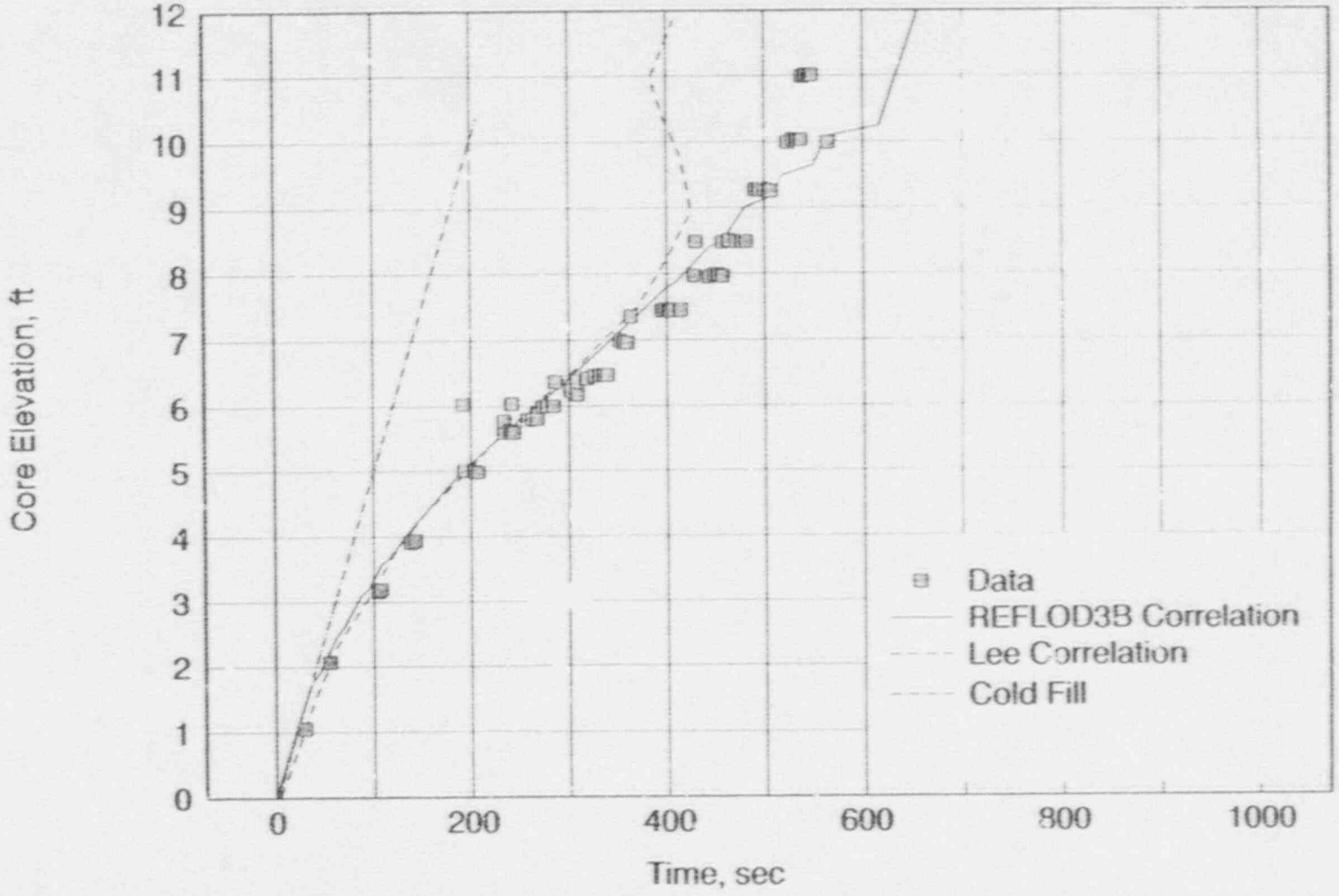


FIGURE 2-8 Cladding Temperatures for REBECCA, SEMISCALE, and NUCLEAR RODS During Reflooding (flooding rate = 2.6 inches/sec)

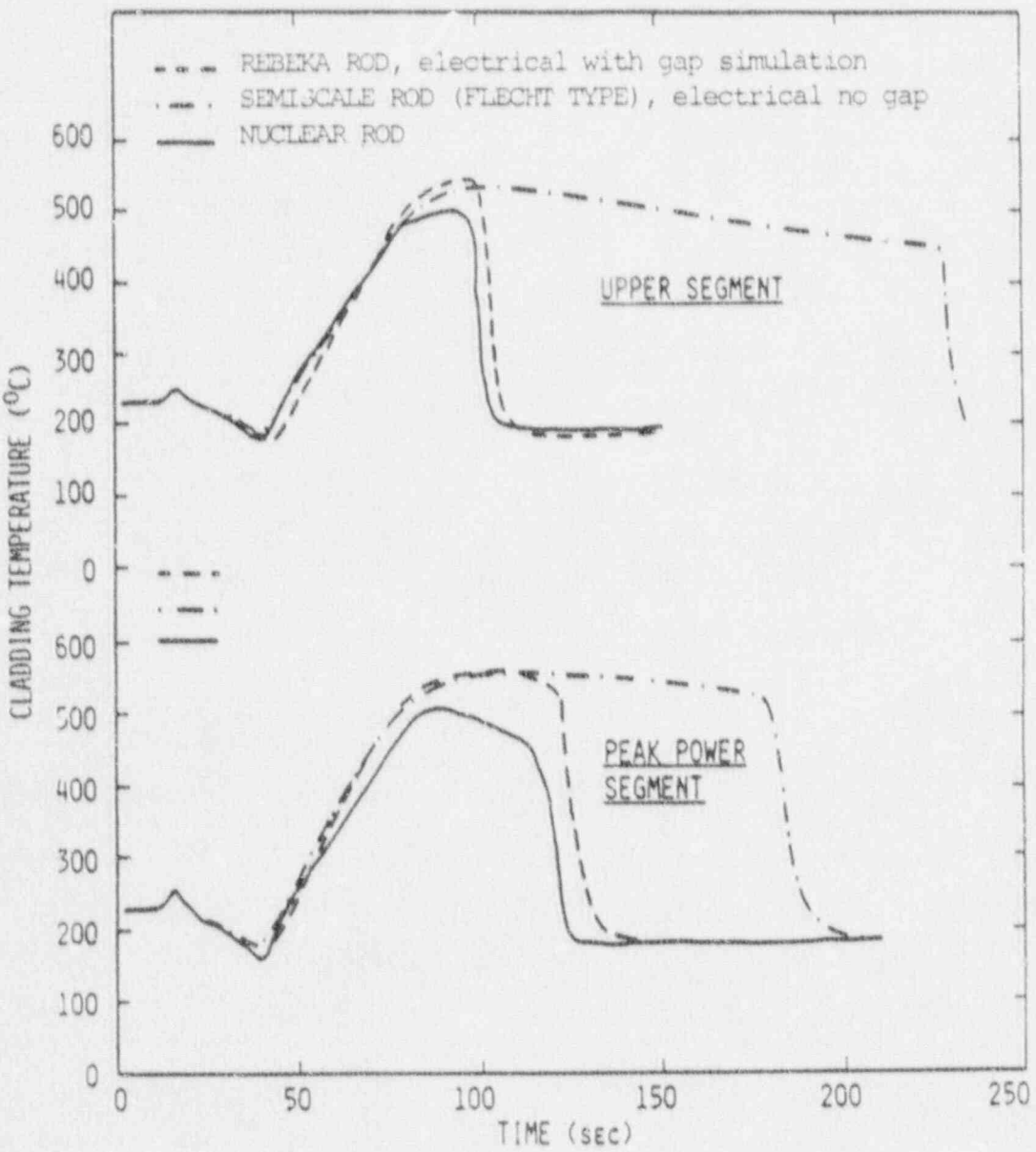


FIGURE 2-9 SCTF FLAT and STEEP RADIAL POWER SHAPES

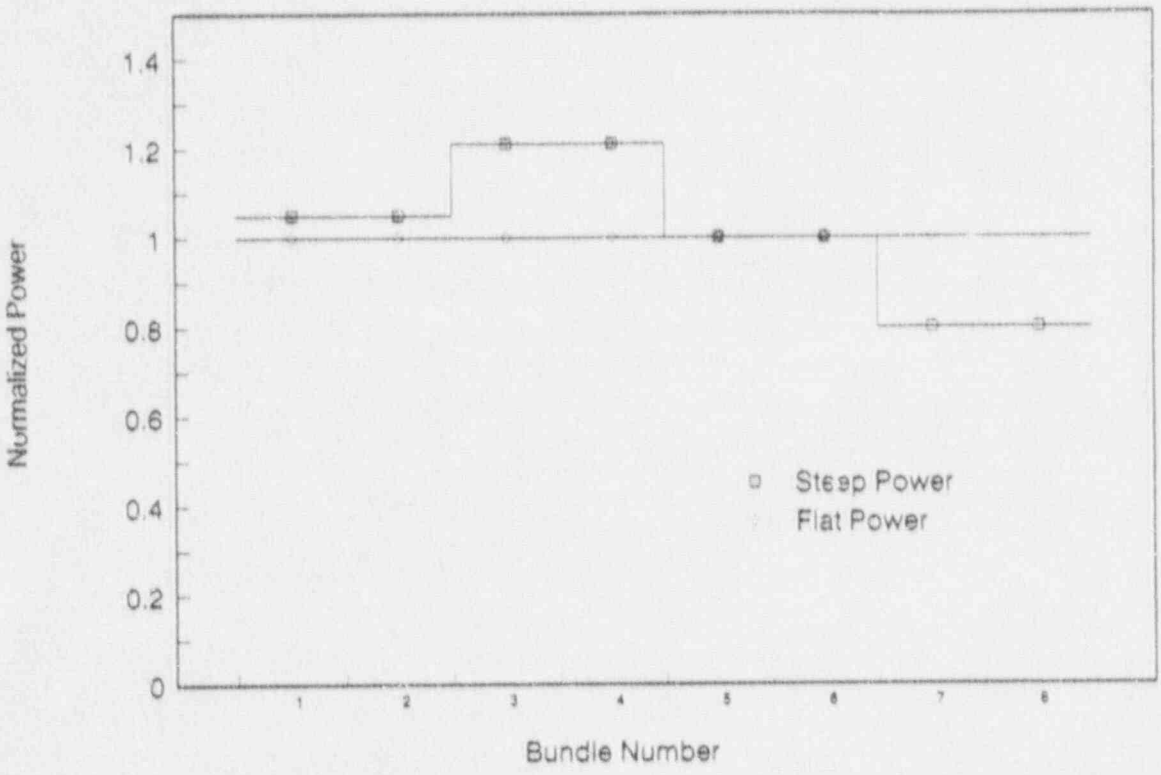



FIGURE 2-10 SCTF Flat and Steep Power Shape Quench Times

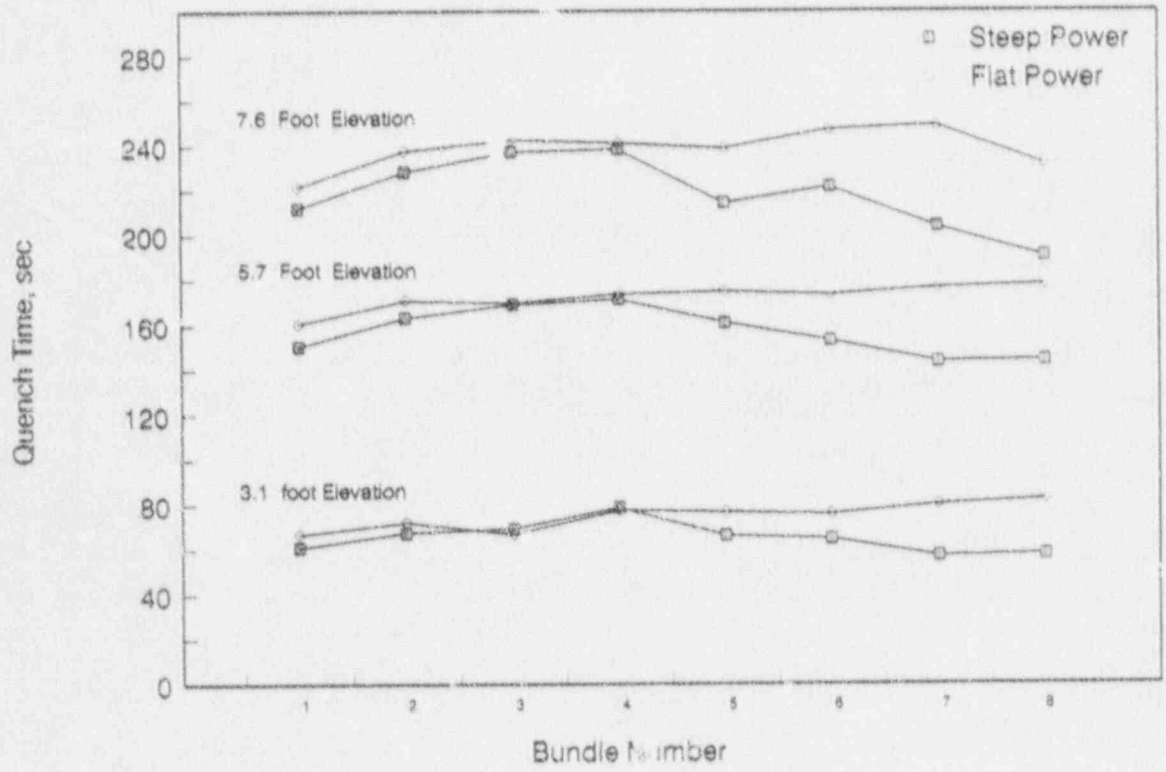
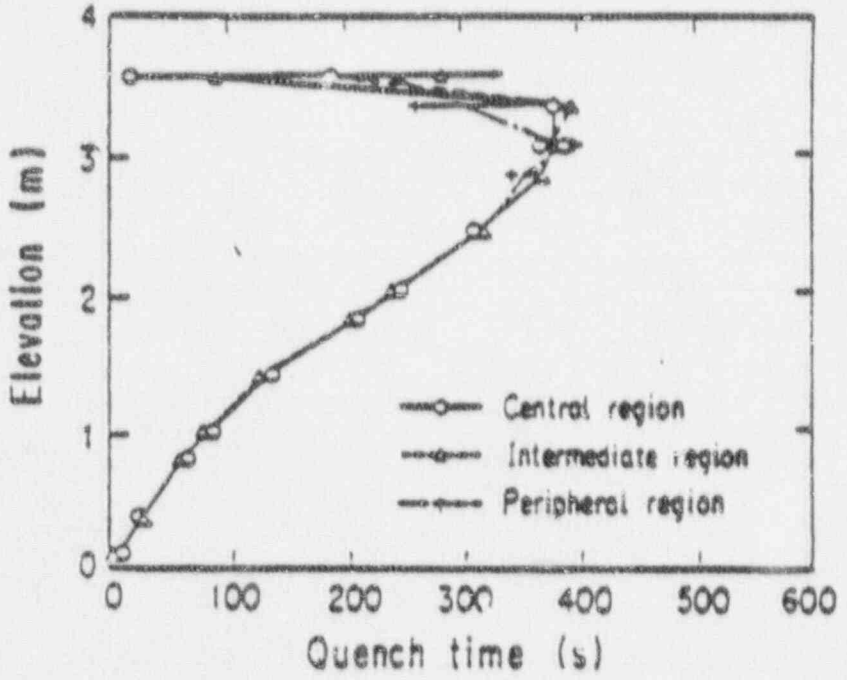
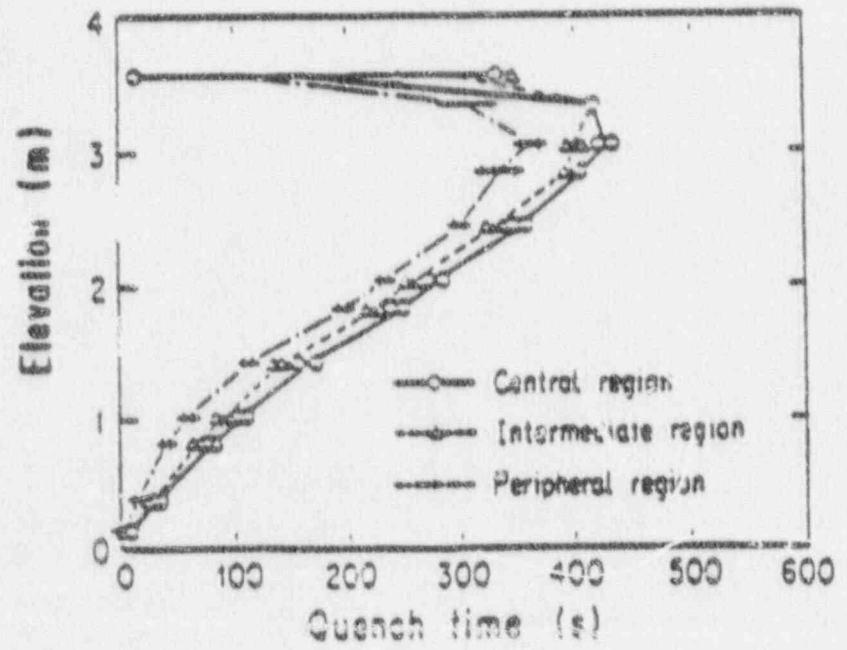


FIGURE 2-11 CCTF Quench Times for Flat and Steep Radial Power Shapes.




(a) Flat radial power test




(b) Steep radial power test

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3. The EM guidelines for code options specified in Table 9-1 of BAW-10168 indicate that all the Licensing Audit Code Models are used for FRAP-T6-B&W, except for cladding specific heat, elastic modulus, fuel deformation, operating power x 1.02 and ANS decay power x 1.2. Clarify what models are added to FRAP-T6-B&W or how the code input is used for LOCA analyses to meet the Appendix K requirements in the above exception areas.

Response: The following discussion applies to the five original FRAP-T6 "Licensing Audit Models" that are not described as being used in the application of FRAP-T6-B&W to LOCA licensing calculations under the B&W RSG LOCA evaluation model.

Cladding Specific Heat Model: This parameter is a material property, and, as explained in the evaluation model topical report, material properties are taken from current nationally recognized data bases and approved for use within the B&W quality assurance program. The heat capacity coded into FRAP-T6-B&W is from Brooks and Stansbury and provided in table form on page D-1.2 of the FRAP-T6-B&W topical, BAW-10165. The licensing audit model from the original FRAP-T6 code is not available in FRAP-T6-B&W. This provides consistency among all codes used in the evaluation model relative to the cladding specific heat.

Cladding Elastic Modulus: This parameter is a material property, and, as explained in the evaluation model topical report, material properties are taken from current nationally recognized data bases and approved for use within the B&W quality assurance program. However, for this particular parameter the same correlation is used as was employed in the licensing audit model. Therefore, although not described as such the code is using the licensing audit model for this parameter.

Fuel Deformation Model: The function of the fuel deformation model was replaced by a new fuel pin model that was added the FRAP-T6 to during the development of FRAP-T6-B&W. The new pin model assures that FRAP-T6 fuel and cladding calculations are consistent with RELAP5/MOD2-B&W and incorporate a rupture deformation model based on NUREG-0630.

Operating Power at 102 Percent of Rated Power: The FRAP-T6 evaluation is for a hot fuel pin representing the average fuel pin in the hottest fuel assembly in the plant. The power of this fuel assembly is set by input in the RELAP5/MOD2-B&W and FRAP-T6-B&W codes to be representative of the hottest fuel assembly including appropriate provision for the plant power to be at 102 percent of rated power. Therefore this original FRAP-T6 licensing audit calculation option is not required.

Decay Power of 120 Percent of the ANS 1971 Standard: The FRAP-T6 power versus time curve is a data transfer from the RELAP5/MOD2-B&W results for the case being evaluated for the blowdown period. Thereafter, the power curve is a tabular input of a fit to the 1971 ANS 5 proposed standard which has been time shifted to match the power result of RELAP5/MOD2-B&W at the end of blowdown. Both sources include the extra 20 percent on decay heating required by Appendix K. This eliminates the need for a separate calculation and assures that all the codes are operating on the same power history.

4. The EM guidelines for inputs specified in Table 9-2 of BAW-10168 indicate that, for REFLOD3B, the parameter $C_{sup}$ should be greater than or equal to 1.025. However, your response to NRC questions on REFLOD3B, JHT/88-152, August 15, 1988, stated that $C_{sup}$ of 1.05 would be used for LOCA EM analyses. (a) Clarify the reasons for this difference. (b) Because there is no upper bound on the $C_{sup}$, what would be the effects of a large value of $C_{sup}$ on the overall REFLOD3B calculation and on the reflood rate in particular?

Response: The $C_{sup}$ factor is an adjustment to the REFLOD3B carryout calculation to eliminate nonphysical pressure oscillations that can occur in the code during the developing reflooding period (see section 2.7 of the REFLOD3B topical report, BAW-10171). If, in the early stages of reflood, liquid carryout is insufficient, the upper plenum will become superheated. Because REFLOD3B is an equilibrium code, the desuperheating process that occurs when the liquid carryout increases can become unstable. To prevent such nonphysical instabilities the adjustment factor, $C_{sup}$, is used to carry sufficient water into the upper plenum to prevent superheating.

From equation 2-81.1, $C_{sup}$ should be greater than 1.0 but only to the extent necessary to provide a two-phase condition in the upper plenum (volume 1R). During the development of REFLOD3B different values were used for $C_{sup}$, including both 1.05 and 1.025. A value of 1.025 was eventually found to be sufficient to prevent upper plenum superheating and has been used in current evaluations. A value of 1.05 would also prevent superheating of the upper plenum, but would constitute an unnecessarily larger adjustment of the REFLOD3B solution. In the responses to REFLOD3B questions provided on August 15, 1988, the value of $C_{sup}$ was stated at 1.05 because the acceptability of 1.025 had not yet been established and BWFC felt that 1.05 would be required. This was also expressed in verbal communication on the subject. With the latest update

to BAW-10168, however, work has been accomplished to show that a value of 1.025 is sufficient and that value has been adopted. The guidance provided to REFLOD3B users is to set $C_{sup}$ to 1.025 and to only increase it if it is necessary to prevent upper plenum superheating.

The use of higher values for $C_{sup}$ perturbs the results of REFLOD3B calculations and it is not clear as to the net effect of these perturbations. During the early phases of reflooding a higher value would result in lower flooding rates and lower core liquid levels. The lower liquid levels would, at later times, when the entrainment is calculated using the CRFCKN correlation, result in higher flooding rates. It is the BWFC experience that such a trade off will not completely compensate for the earlier lower reflooding rates and that higher values of $C_{sup}$ would lead to slight degrees of conservatism. However, this factor was not intended to impose conservatism and should not be so employed. Its value should be kept as close to 1.0 (currently set to 1.025) as possible to prevent the superheating of the upper plenum.

5. Appendix K requires that the two-phase critical break flow be calculated with the Moody critical flow model and with various discharge coefficients. In using the Moody model for SBLOCA analyses, you indicated that the discharge coefficient will not be varied because this will be covered by the break spectrum study. Revision 1 of the RSG LOCA EM indicates that a normalized discharge coefficient of 0.7 will be used with the Moody model when the break void fraction is greater than 70% to make the calculated two-phase critical flow more best-estimate. Clarify how this two-phase critical flow model in SBLOCA analysis meets the Appendix K requirement.

Response: The leak discharge coefficients selected by BWFC for use during small break LOCA evaluations are:

| | |
|---|---|
| Subcooled Discharge: ($\alpha \leq 0.0$) | $C_d = 1.0$ , using extended Henry-Fauske |
| Low Void Saturated Discharge: ($0.0 \leq \alpha \leq 0.7$) | $C_d = 1.0$ , using Moody |
| High Void Saturated Discharge: ($\alpha > 0.7$) | $C_d = 0.7$ , using Moody. |

The use of a void fraction dependent discharge coefficient assures the achievement of conservative small break LOCA predictions and can be shown to comply with 10 CFR 50.46 and Appendix K requirements. This response will demonstrate that the leak modelling selected by BWFC for SBLOCA is essentially a best estimate model and will explain the relationship that leak models must have to best estimate predictions and to each other to achieve acceptable results for a spectrum of SBLOCAs. The response will explain the selection of the BWFC approach and demonstrate how the BWFC SBLOCA leak modelling adheres to the requirements of 10 CFR 50.46 and Appendix K.

## Relation of BWFC Leak Modelling to Best Estimate

In order to select the appropriate discharge coefficients for use with the correlations employed in the BWFC RSG LOCA evaluation model, the relationship of those models to best estimate flows should be established. The RSG LOCA evaluation model uses the extended Henry-Fauske model for subcooled discharges and the Moody model for both low void and high void discharges.

Extended Henry-Fauske was evaluated in TREE-NUREG-1006, reference 5.1, against semiscale test S-02-4 and found to reproduce the subcooled portion of the experiment quite well. Figure 5-1 is a reproduction of a figure from that report that shows the ratio of measured-to-calculated discharge rates for several different models. The figure shows that the measured-to-calculated ratio for Henry-Fauske lies between about 0.9 and 1.1 for the entire subcooled period. Integrating over the period leads to an average ratio of about 0.95. Therefore, extended Henry-Fauske with a discharge coefficient of 1.0 can be considered as a best estimate leak discharge correlation.

In 1971 Henry and Fauske (Reference 5.2) compared a proposed model to critical flow data at low qualities (References 5.3, 5.4, and 5.5) at several pressures. Figures 5-2 through 5-7 are replots of curves and data from reference 5.2. As can be seen in Figure 5-2, and the other figures, the model proposed by Henry and Fauske does a reasonable job of fitting the data with some small bias at the lowest of the pressures in Figure 5-2. Figures 5-3 through 5-7 also include data related to the BWFC discharge model. Added are the corresponding Moody model calculated discharge rates and the ratios of the calculated Moody mass flux to experimental data using the proposed Henry-Fauske model as representative of the data.

The ratio of measured-to-Moody discharge rates starts at about 1.1 and drops smoothly to a value of between 0.5 and 0.7 at between 80 to 95 percent void fraction. The ratio drops below 1.0 at about 30 percent void fraction. The minimum value of 0.5 to 0.7 occurs near 90 percent void fraction after which the ratio starts to rise back toward 1.0 at 100 percent void fraction. A reasonable fit to this performance would be to use a discharge coefficient of 1.0 up to a break inlet void fraction of 70 percent and drop the coefficient to 0.7 for all higher qualities.

Additional support for low void to high void discharge coefficient adjustment for the Moody model is provided in the answer to question 5 of the second round of questions on the RELAP5/MOD2-B&W code (Reference 5.6). The response to this question entailed the benchmark prediction of the Semiscale S-LH-1 test with RELAP5/MOD2-B&W. In the original submittal, the discharge coefficients were set to 1.0 for the subcooled period and 1.0 for the entire saturated discharge period. The results obtained showed that the depressurization of the system was accelerated substantially faster than the experiment showed during the period of high void discharge. This led to fairly rapid increases in the ECCS rates and the retention of excessive water within the system. To correct this, the second benchmark employed the evaluation model discharge coefficient scheme of resetting the discharge coefficient from 1.0 to 0.7 at 70 percent void fraction, resulting in a much better comparison to both depressurization and retained inventory. Figures 5-8 and 5-9 show the system pressure and system inventory for the two benchmarks as functions of time. No differences exist prior to the time marked as 70 percent voiding because the leak correlations and discharge coefficients were identical up to that time. After 70 percent voiding the slower discharge rate kept the system pressure up, which lengthened the time to accumulator

injection creating a more accurate system inventory history. The accuracy of prediction of the second benchmark is considered to be within the accuracy and availability of measured data.

The direct data comparisons and the benchmark of the S-LH-1 test show that the Moody model can be made to represent a reasonably best estimate model if a switch in discharge coefficient from 1.0 to 0.7 is taken at a void fraction of approximately 0.80 percent. Therefore, the combination of extended Henry-Fauske for subcooled break flows and Moody for saturated break flows using the BWFC-proposed discharge coefficients can be considered close to a best estimate model.

Relation of Break Flow Modelling to SBLOCA Results

For an recirculating steam generator (RSG) plant there are two critical events in the evolution of a SBLOCA during which there is a possibility of uncovering the core and causing cladding temperature excursions: (1) loop seal clearing and (2) vessel boildown. An SBLOCA may also be divided into characteristic phases according to the fluid properties at the break: subcooled blowdown, low void saturated blowdown, and the high void saturated blowdown periods. Because of the geometry of the design, the first critical event, loop seal clearing, occurs during the low void blowdown phase, and the second critical event, vessel boildown, occurs during the high void blowdown phase.

The relationship of the severity of the critical events to leak modelling can be developed from the following considerations: First, for a given discharge rate during a period, the events within the period evolve independent of discharge rates prior to the period (except for a possible

time shift). Second, calculating critical events to occur at earlier times maximizes the consequences of those events because decay heating will be higher. Thus, shortening of the preceding phases is a move in the conservative direction for the evaluation of a given phase.

Subcooled discharge: This period initiates with the start of the accident and terminates when the break flow becomes saturated. There are no critical events during subcooled discharge.

Low void discharge: The low void period immediately follows the subcooled discharge phase and terminates just following loop seal clearing. During this period, there is a low quality present at the break due to bypass flow, but the plant is relatively full and draining. Depressurization is slowed or halted because the break cannot flow sufficient volume to overcome core boiling. The key physical processes are core boiling, steam condensation in the steam generators, and hot water and steam discharge out of the break. Continued depressurization cannot occur until a high void fraction is present at the break location, and that is not possible until the RCS has drained substantially and the loop seals at the pump suctions are cleared.

During low void discharge, the system undergoes a gradual loss of liquid inventory with steam accumulation in the upper portions of the system until the down sections of the cold legs at the pump suction are filled with steam. At this time, as steam begins to flow across the cold leg spill under, the riser and horizontal sections of the cold legs and the downcomer are mostly filled with water. The core liquid level is suppressed to match the pump suction piping elevation heads. This occurrence, of a suppressed core liquid level associated with the loop seal clearing process, is the first

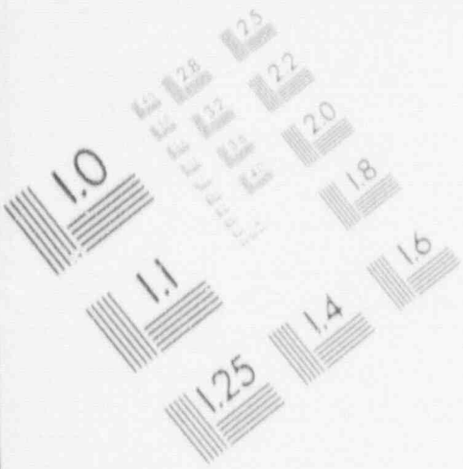
critical event for the small break LOCA. Depending on the particulars of the system and the LOCA, the core collapsed liquid levels may fall to 7 feet or 8 feet in the core with a cladding temperature excursion likely. As the loop seals are cleared, the head imbalance between the downcomer and the core is reduced, and liquid from the cold legs refills the core. The cladding returns to near saturation temperatures.

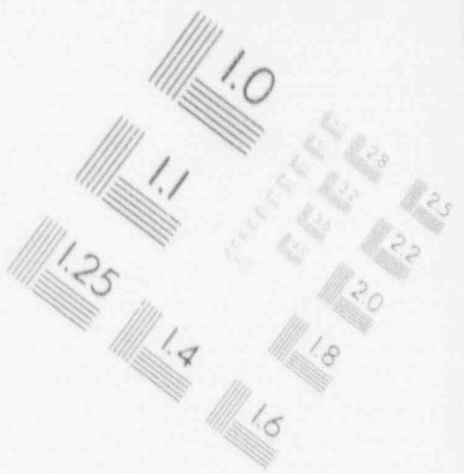
The lower the break flow rate, the longer the low void period (in particular the loop clearing phase), the longer the cladding heatup period, and the higher the cladding temperatures will be. On the other hand, as the low void period is slowed, loop seal clearing will occur at lower decay heat levels resulting in lower cladding heat up rates. The use of a spectrum approach resolves these contradictory trends. Under a spectrum all discharge rates will be run. Therefore all rates of development and prolongation of the uncovery will be analyzed. Conservatism can be achieved by assuring that, for each prolongation, the decay heat for that period is equal to or above that which would actually occur. This can be achieved by assuring that the subcooled discharge period is predicted at a best estimate or shorter duration relative to the low void discharge period.

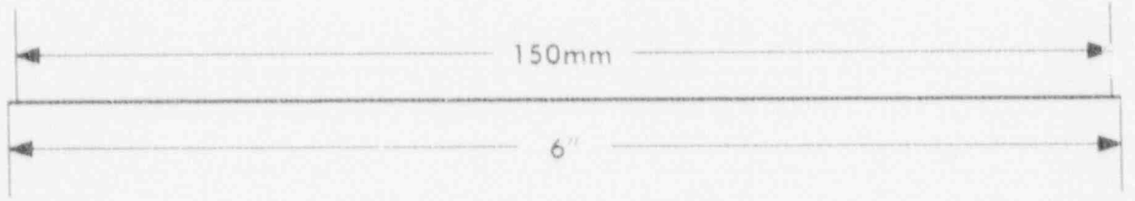
<u>High void discharge</u>: This period begins shortly following loop seal clearing and involves the extended depletion of the reactor vessel inventory. The reactor vessel and plant enter a boildown phase, metaphorically termed the "boiling pot" mode. Core boiling creates steam at a faster rate than the ECCS can supply replacement liquid. The system liquid inventory, in particular the core inventory, slowly decreases and may eventually expose part of the fuel. Termination of the decline in vessel liquid inventory occurs when the decay heat has been reduced (time dependence) and the ECCS flow has increased (system pressure dependence) such that the ECCS can

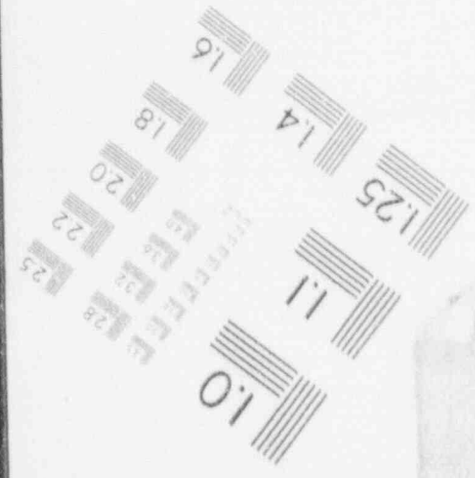
# 1

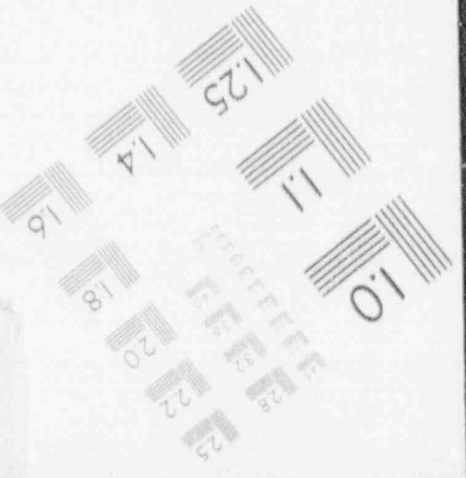
## IMAGE EVALUATION TEST TARGET (MT-3)





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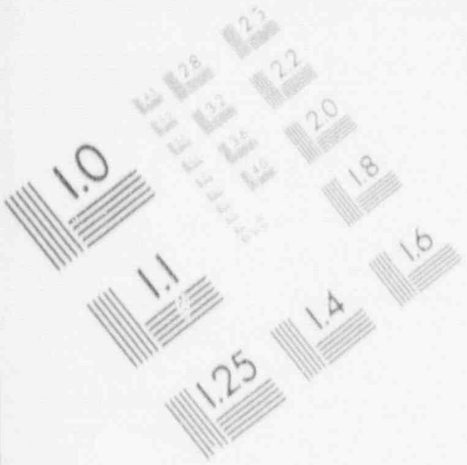
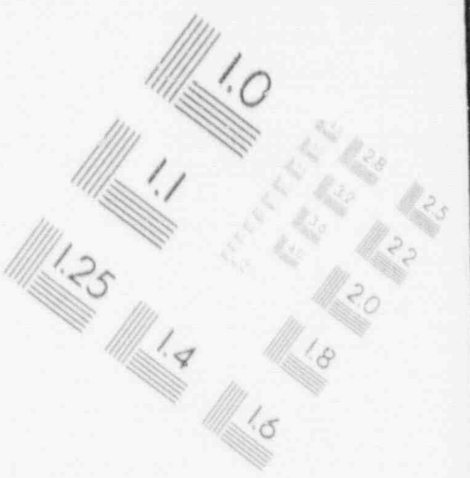
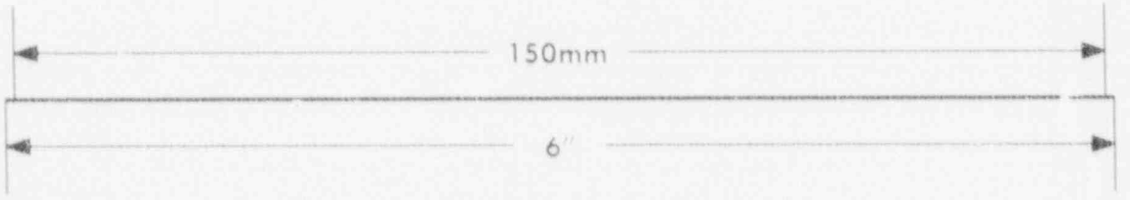
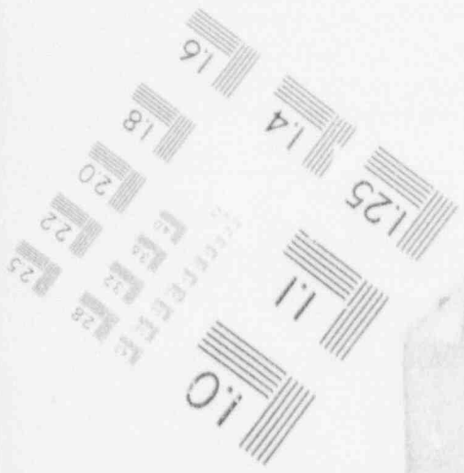
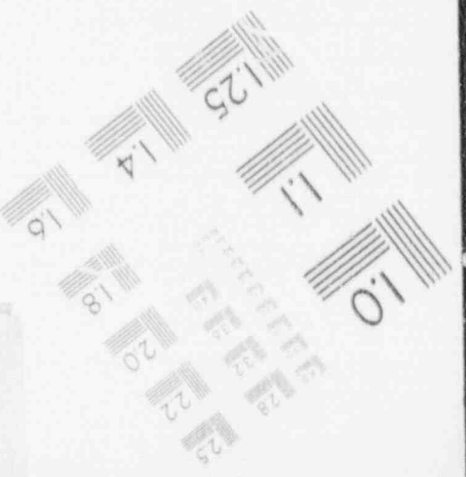
## IMAGE EVALUATION TEST TARGET (MT-3)





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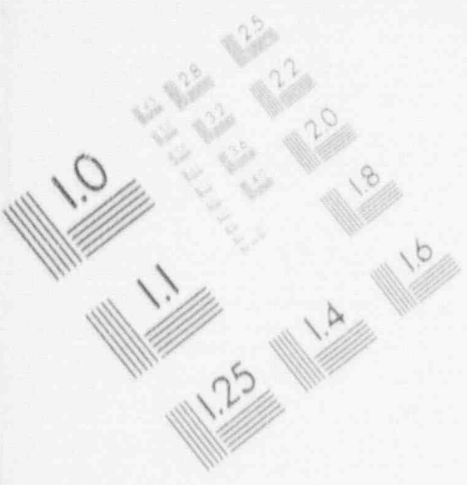
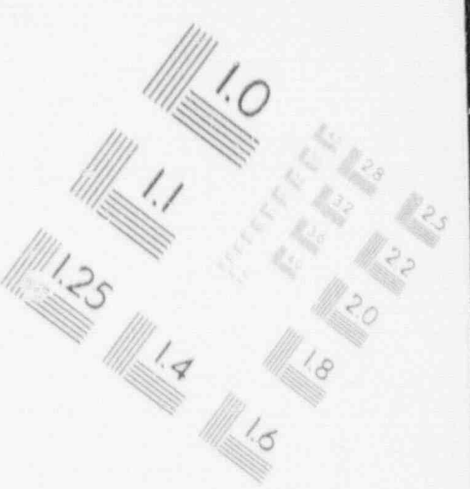
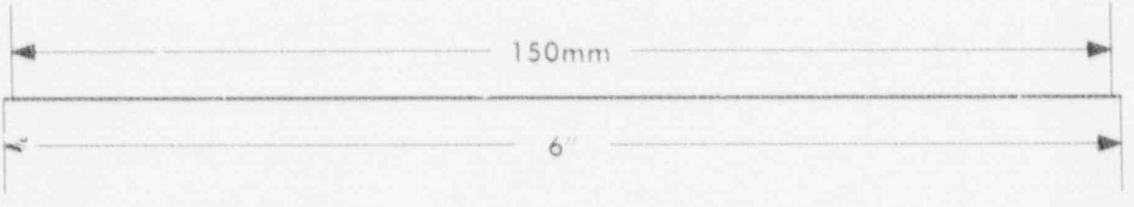
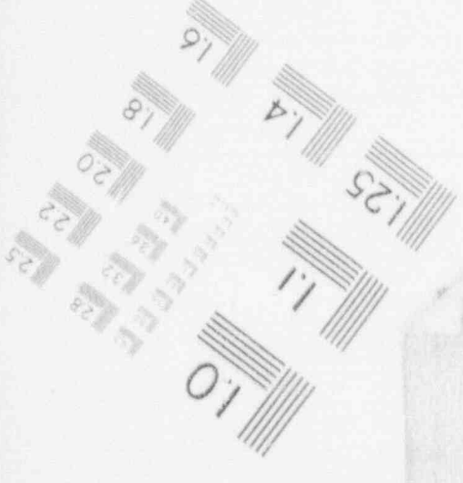
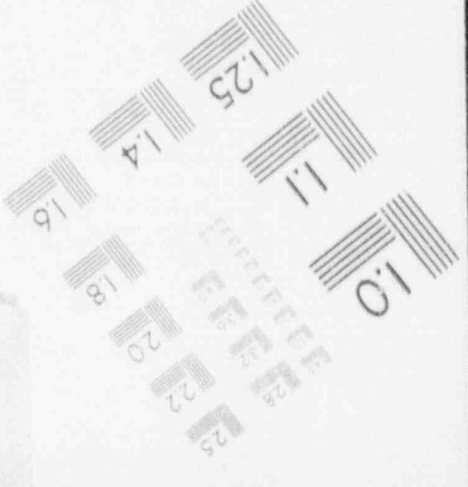
## IMAGE EVALUATION TEST TARGET (MT-3)





provide makeup water at a rate equal to the core boiling. The severity of the core exposure depends on the core liquid inventory at the start of the period and the timing of the net loss period.

The relationship of break flows to the system response during the high void discharge period are straightforward. The reactor vessel inventory at the start of this phase of the accident is essentially independent of the results of the previous periods of the accident. That is, the inventory is limited by system geometry, and cold leg draining during loop seal clearing tends to bring the inventory to this limit. With the inventory and liquid distribution of the system practically independent of the prior system discharge rates, the course of the transient after the start of highly voided discharge depends mainly on the discharge rate during the period. Nonetheless, the timing of events during the period will depend on the time that this phase starts, and that time determines the core power through the decay heat. As the amount of core boiling is a direct function of the core power, the higher the core power the more difficult will be the control of the boiling pot mode. From this, it can be concluded that the earlier the system (which will have a given high void discharge rate and thus a given depressurization) enters the high void period, the more severe will be the consequences of the high void period. That leads to the conclusion: For conservative results, the discharge rates for the subcooled and low void discharge periods should be equal or higher than best estimate relative to the high void discharge rates.

Combined effects of all three phases: Combining the relations of the low void discharge rate and the high void discharge rate a general rule develops: Accurate-to-conservative prediction for a period with a given discharge flow requires

that the discharge flows of the preceding periods be best estimate or higher than best estimate relative to the flow during the period. In other words, the low void discharge model should predict rates of discharge that are best estimates or higher than the best estimate rates that would correspond to the selected high void discharge rates. It follows that the subcooled discharge model should predict rates of discharge that are best estimates or higher than the best estimate rates that would correspond to the selected low void discharge rates. It should be emphasized that the subcooled rate should be taken as relative to the low void rate as if the low void were best estimate even though the low void rate may be purposely made conservative relative to the high void rate.

Selection of BWFC Discharge Modelling for SBLOCA

Using the foregoing general rule for selection of discharge coefficients during the three major periods of the small break LOCA requires that a coefficient of 1.0 be used for the subcooled and low void saturated periods while a coefficient of 0.7 be used for the high void period. This creates a best estimate discharge evaluation as would any arrangement of the coefficients that results in the relative ratios of 1.0, 1.0, and 0.7. Relative ratios of 1.0,1.0, and < 0.7 would constitute a more conservative approach to the high void period. Ratios of 1.0,1.0, and > 0.7 would constitute a nonconservative approach. Because there are many other conservatisms in the small break LOCA evaluations, and because a spectrum approach is used to assure that the worst case SBLOCA is identified, BWFC has elected to maintain a best estimate performance level for the break modelling.

Adherence to 10 CFR 50.46 and Appendix K

Discharge coefficients are normally used to account for break geometry and are therefore held constant during the flow process. The use of a staged discharge coefficient, as proposed by BWFC, could be interpreted as an alteration of the basic leak discharge correlation as opposed simply to selection of discharge coefficients. 10 CFR 50.46 and Appendix K, however, when specifying the use of the Moody model for saturated discharge with a series of discharge coefficients contains additional instructions that require that the resultant modelling not underpredict the consequences of the LOCA.

Appendix K section C.1.b states " ... the discharge rate shall be calculated by use of the Moody model ..." and " The calculation shall be conducted with at least three values of a discharge coefficient applied to the postulated break area, these values spanning the range from 0.6 to 1.0." These lines have generally been interpreted to required that a series of evaluations be made, each with a different discharge coefficient. The requirement has, therefore, been considered redundant to the break spectrum requirement for small breaks. The last sentence of this paragraph in Appendix K reads, "If the results indicate that the maximum clad temperature for the hypothetical accident is to be found at an even lower value of the discharge coefficient, the range of discharge coefficients shall be extended until the maximum clad temperature calculated by this variation has been achieved." When combined with a spectrum approach, this sentence indicates that the rule is not bound to the prescribed modelling in the previous parts of the paragraph if that modelling would create nonconservative evaluations. With this consideration, and the explanations provided, the discharge coefficient selection as proposed by the RSG LOCA evaluation model is considered

responsive to and within the requirements of 10 CFR 50.46 and Appendix K.

Conclusion

The wording of Appendix K requires that the Moody model be used with various discharge coefficients to assure that a conservative LOCA calculation is achieved. BWFC has determined that this requires the use of a void fraction-dependent discharge coefficient in conjunction with a break spectrum for small break LOCA transients. The nature of the BWFC leak models and selected discharge coefficients has been shown to be essentially best estimate. The reasons for requiring a certain relationship to best estimate leak rates, of one phase of the accident, normalized to other phases have been demonstrated. Finally, the required relationships among the phases of the transient and the leak modelling effects have been combined to assure that the selected technique achieves best estimate flows for any given break size. Complete coverage and assured conservatism is accomplished by performing a spectrum according to the BWFC break area selection criteria. This approach meets the requirements and the intent of the regulation promulgated by 10 CFR 50.46 and Appendix K.

References:


5.1 D. G. Hall, A Study of Critical Flow Prediction for Semiscale Mod-1 Loss of Coolant Accident Experiments, TREE-NUREG-1006, USNRC, December 1976.

5.2 Robert E. Henry and Hans K. Fauske, "The Two-Phase Critical Flow of One-Component Mixtures in Nozzles, Orifices, and Short Tubes", Transactions of the ASME - Journal of Heat Transfer, May 1971.

5.3 D. G. Maneely, "A Study of the Expansion Process of Low Quality Steam Through a de Laval Nozzle", UCRL-6230, University of California Radiation Laboratory, 1962.

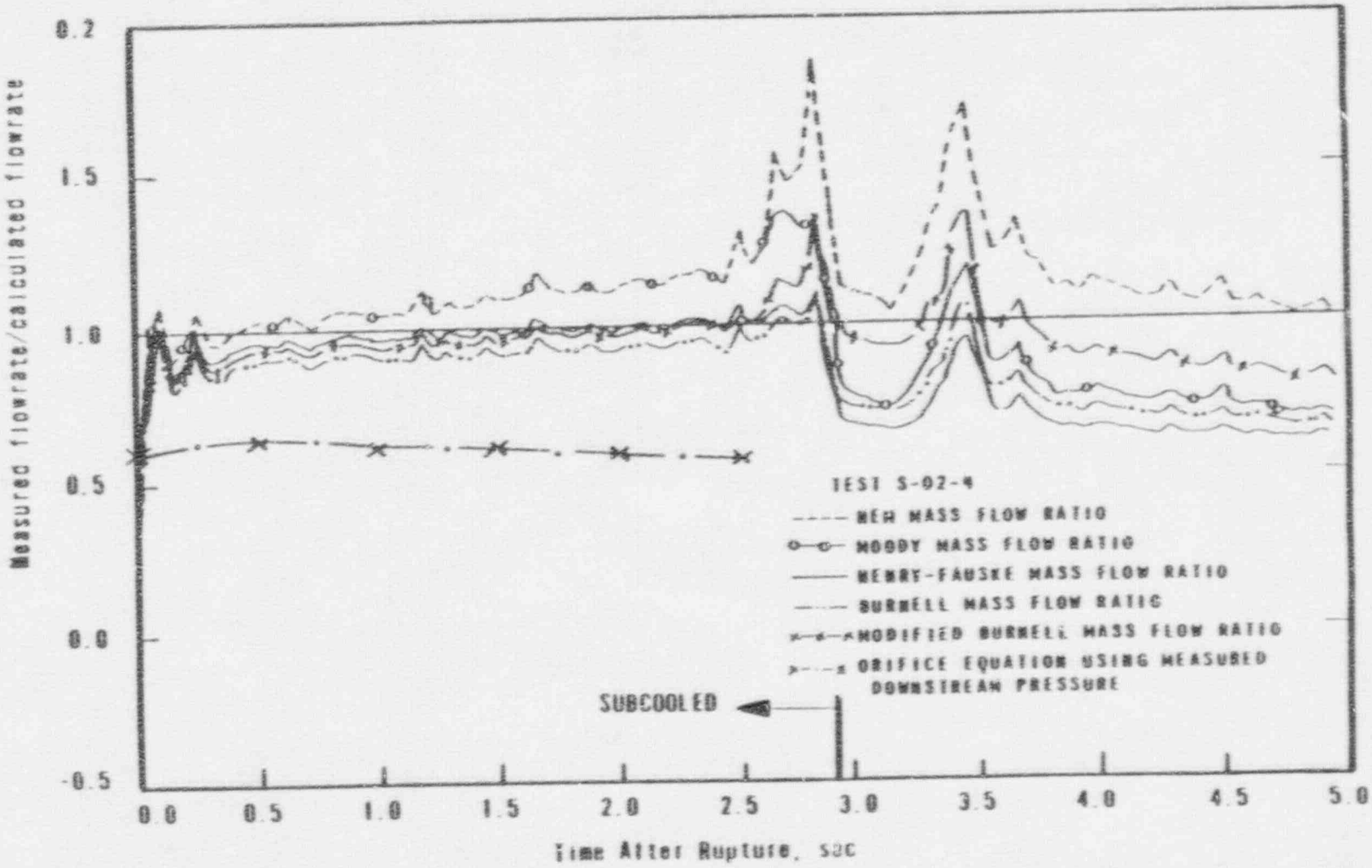
5.4 K. F. Neusen, "Optimizing of Flow Parameters for the Expansion of Very Low Quality Steam", UCRL-6154, University of California Radiation Laboratory, 1962.

5.5 H. Friedrich, "Flow Through Single-Stagr Nozzles with Different Thermodynamic States", Energie, Volume 12, 1960, p. 3.

5.6 Letter, J. H. Taylor (B&., to J. A. Norberg (USNRC), Subject - RELAP5/MOD2 Topical Report, BAW-10164P, Response to request for additional information, May 11, 1989.

Figure 5-1

RATIOS OF MEASURED BREAK MASS FLOW RATES TO CRITICAL MASS FLOW RATES CALCULATED USING FIVE CRITICAL FLOW MODELS (TREE-NUREG-1006)



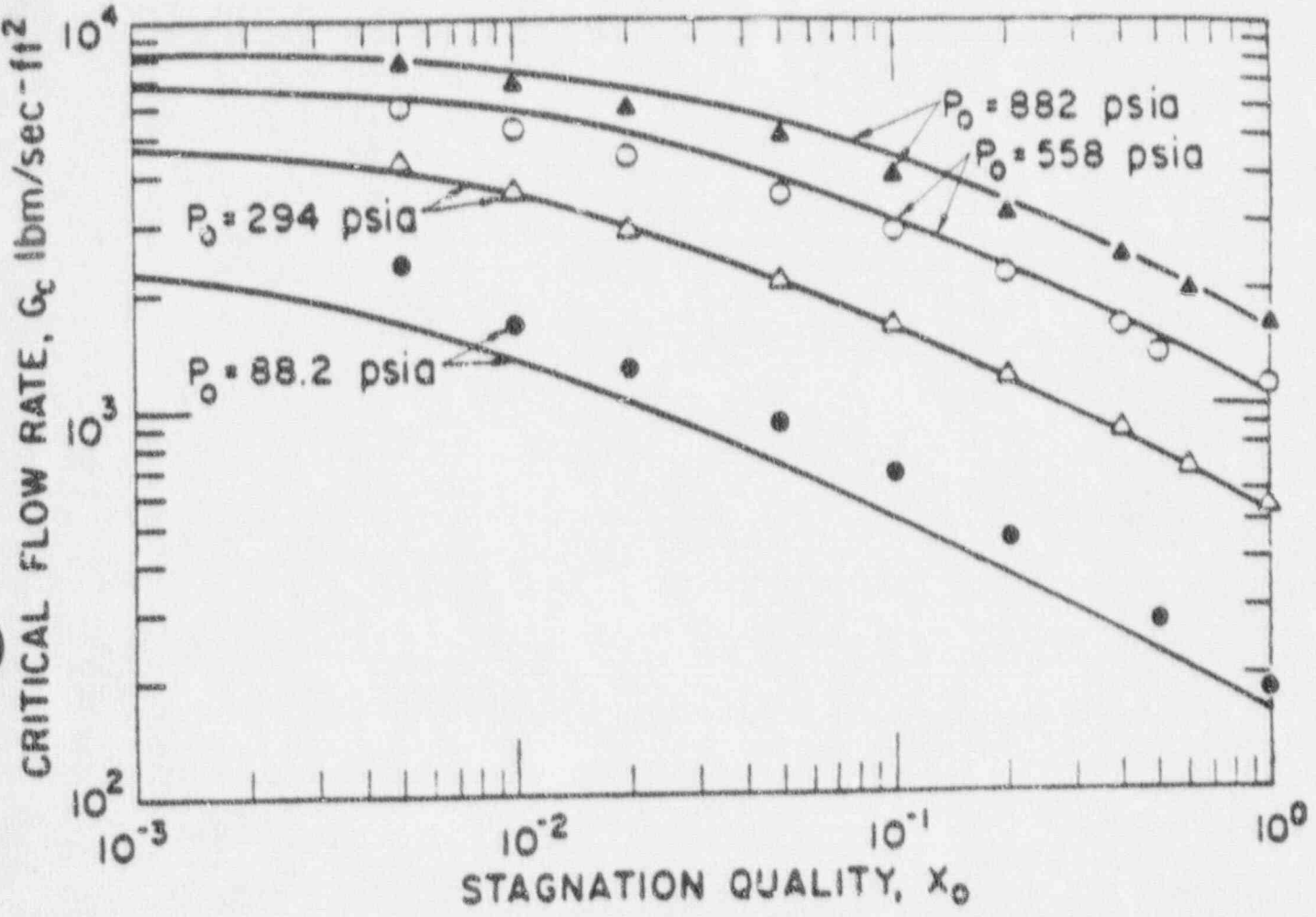
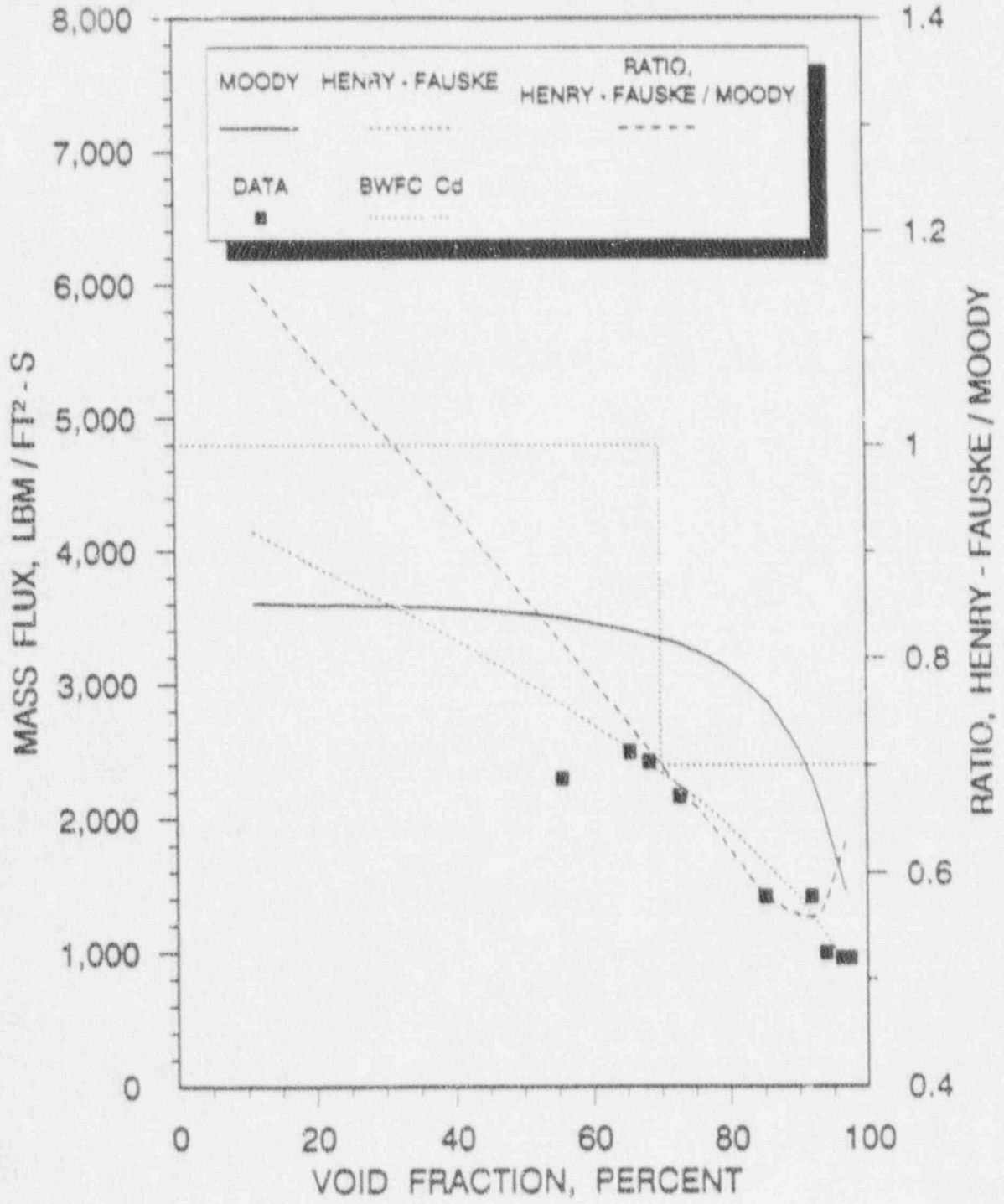
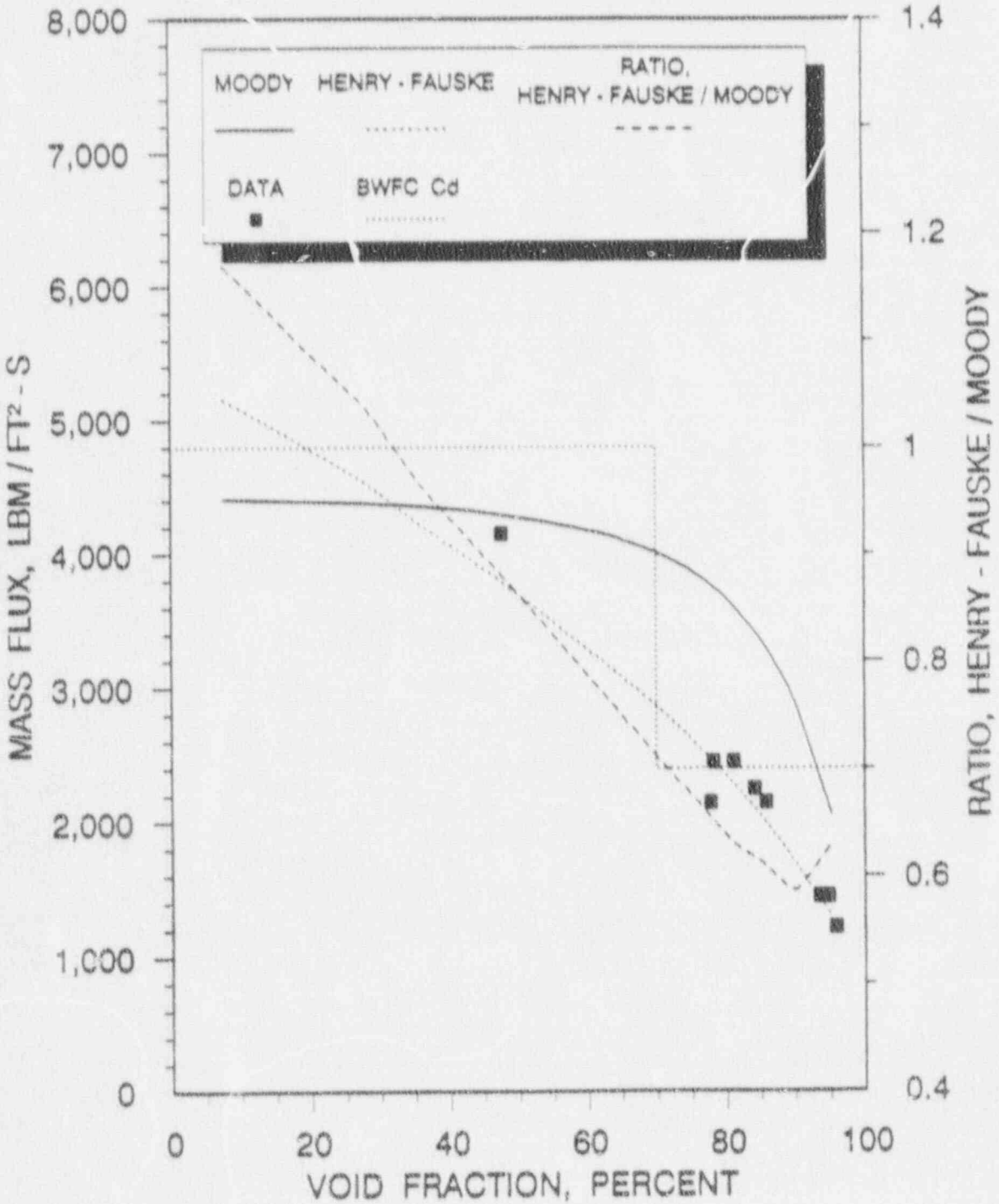



Figure 5-2 **Comparison between proposed model and short-tube, steam-water data of reference** [Reference 5.5]

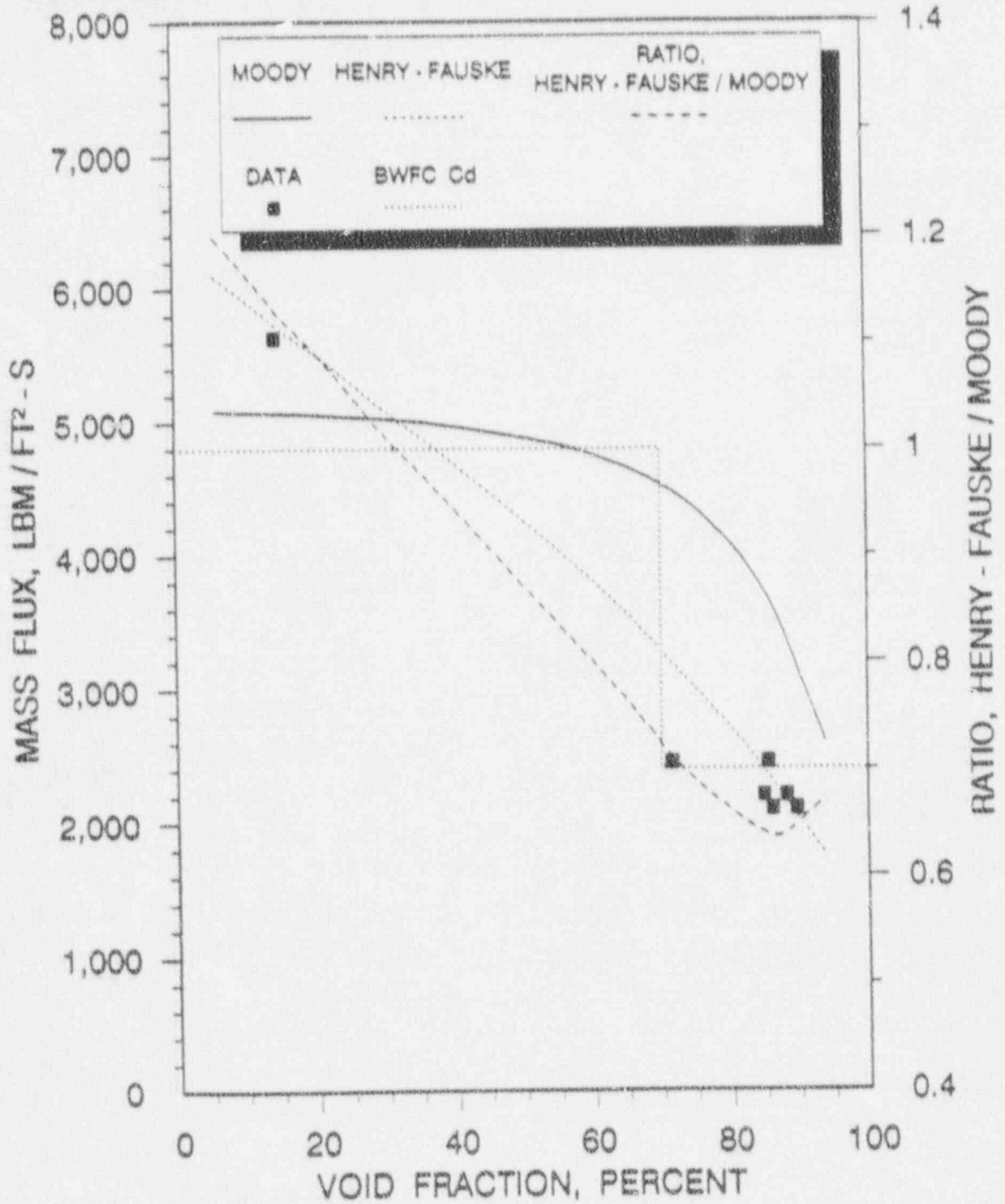
# FIGURE 3. MOODY DATA COMPARISONS. 200 PSIA



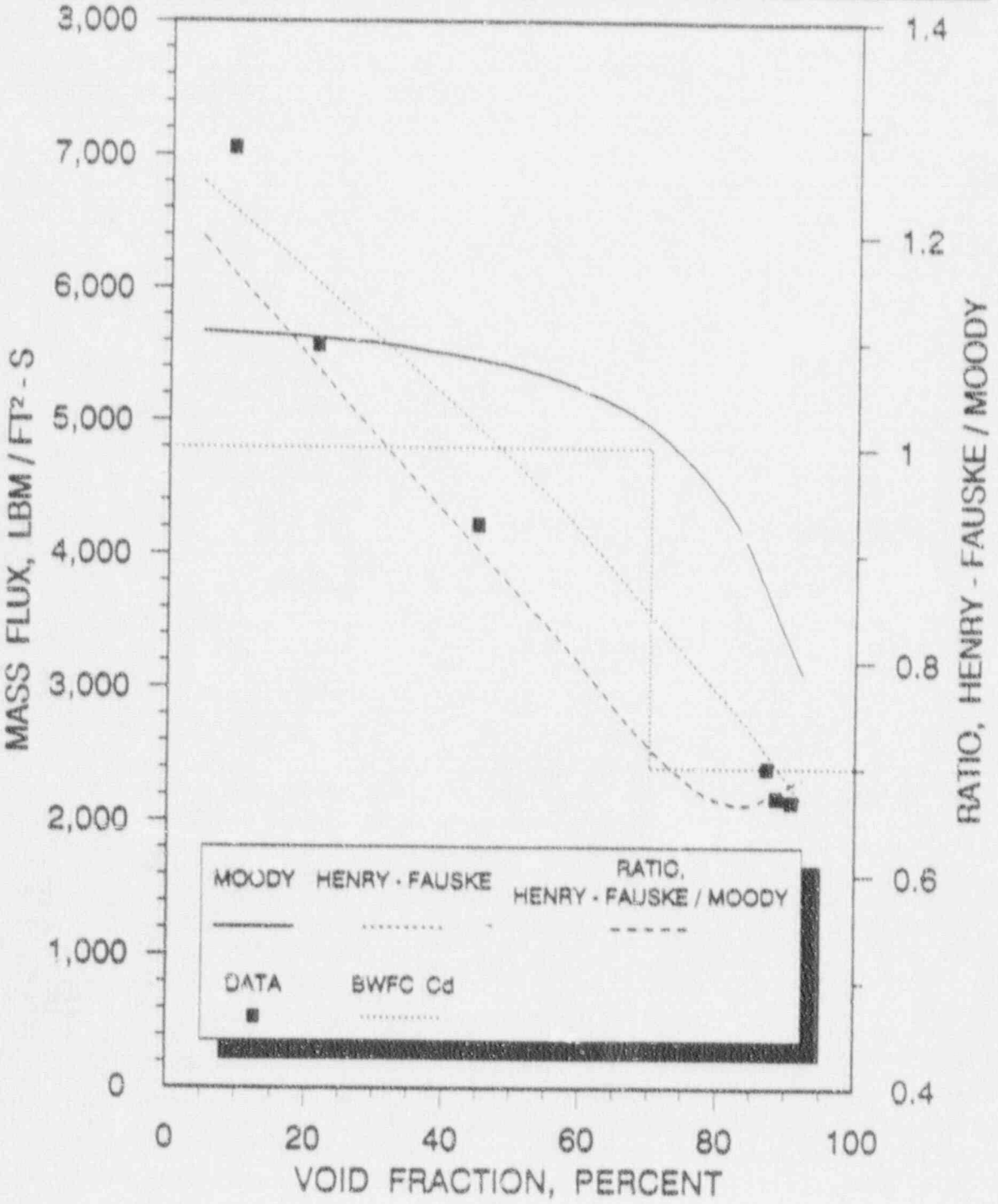
# FIGURE 5-4. MOODY DATA COMPARISONS. 300 PSIA



# FIGURE 5-5. MOODY DATA COMPARISONS. 400 PSIA



# FIGURE 5-6. MOODY DATA COMPARISONS. 500 PSIA



# FIGURE 5-7. MOODY DATA COMPARISONS. 882 PSIA

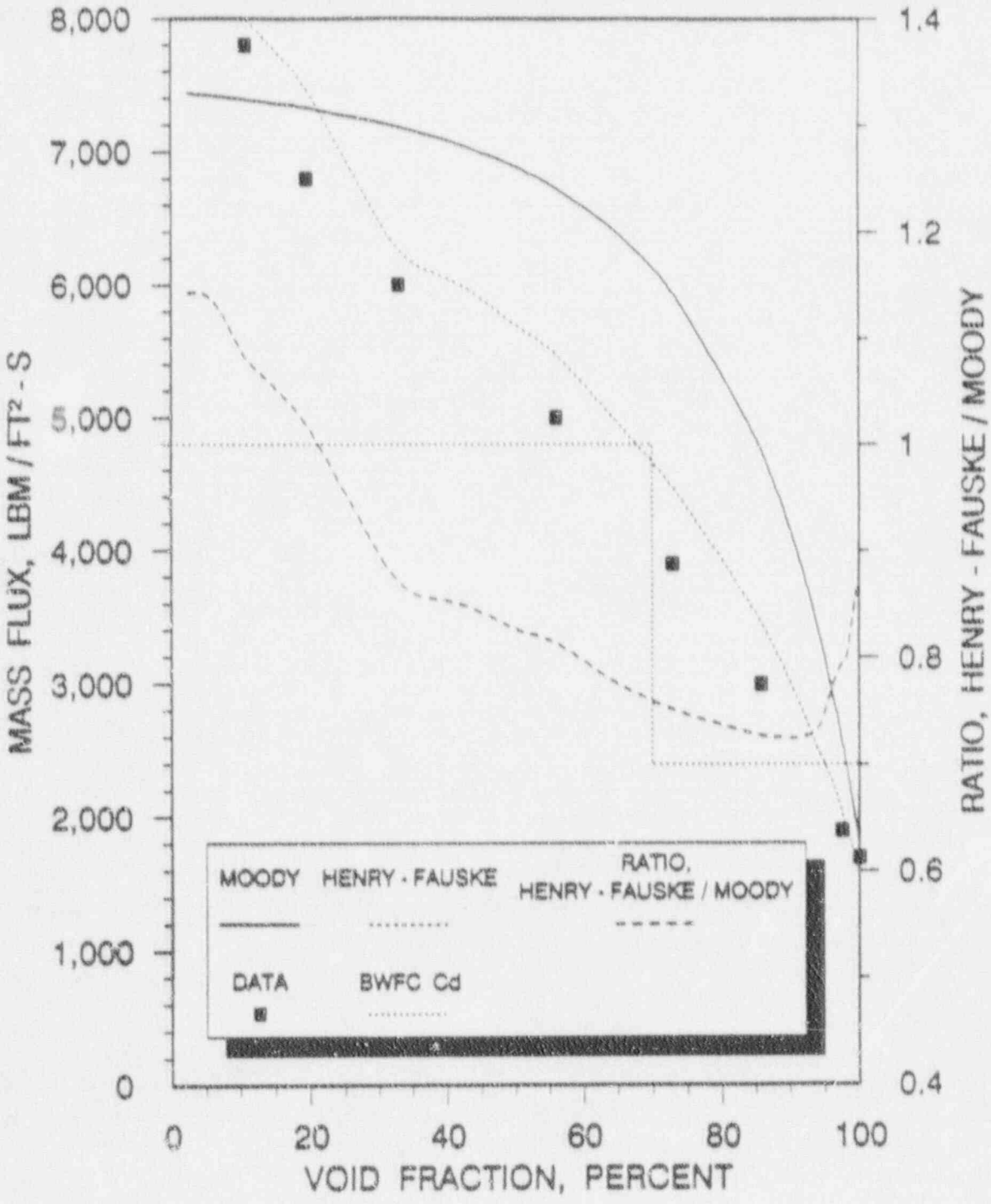


FIGURE 5-8 SEMISCALE TEST S-LH-1; PRIMARY SYSTEM NORMALIZED MASS

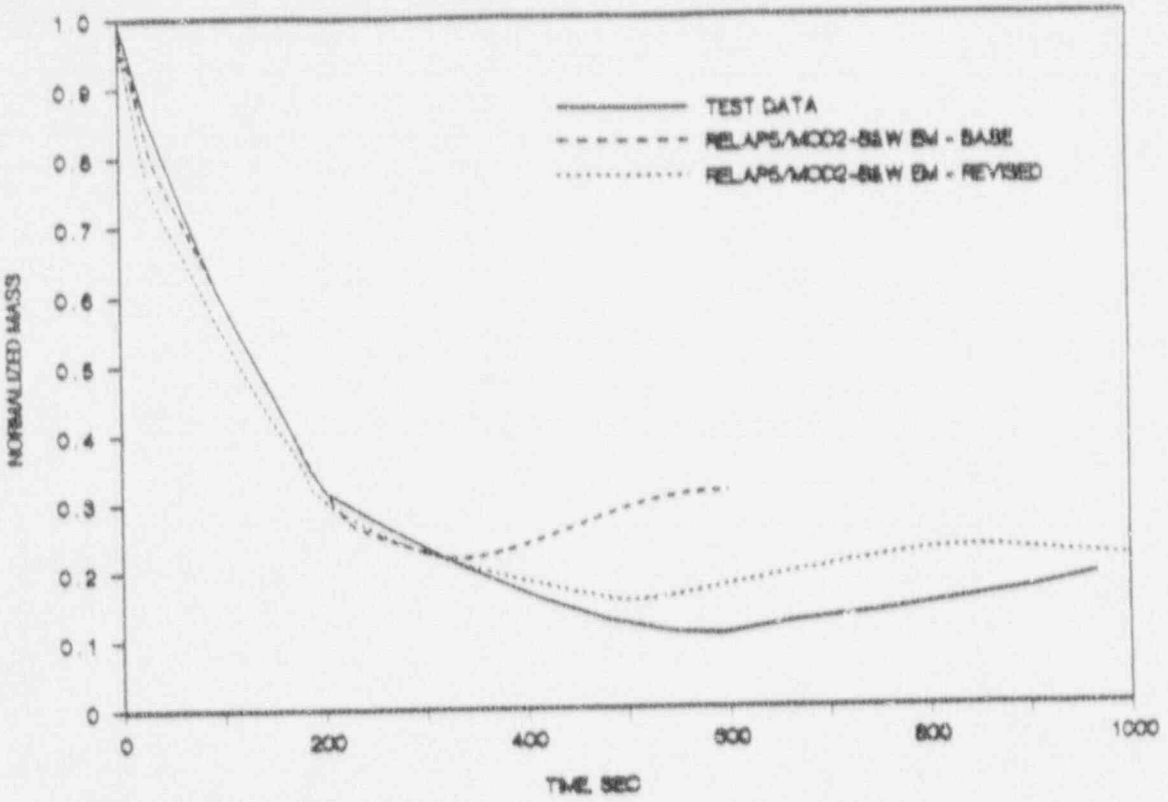
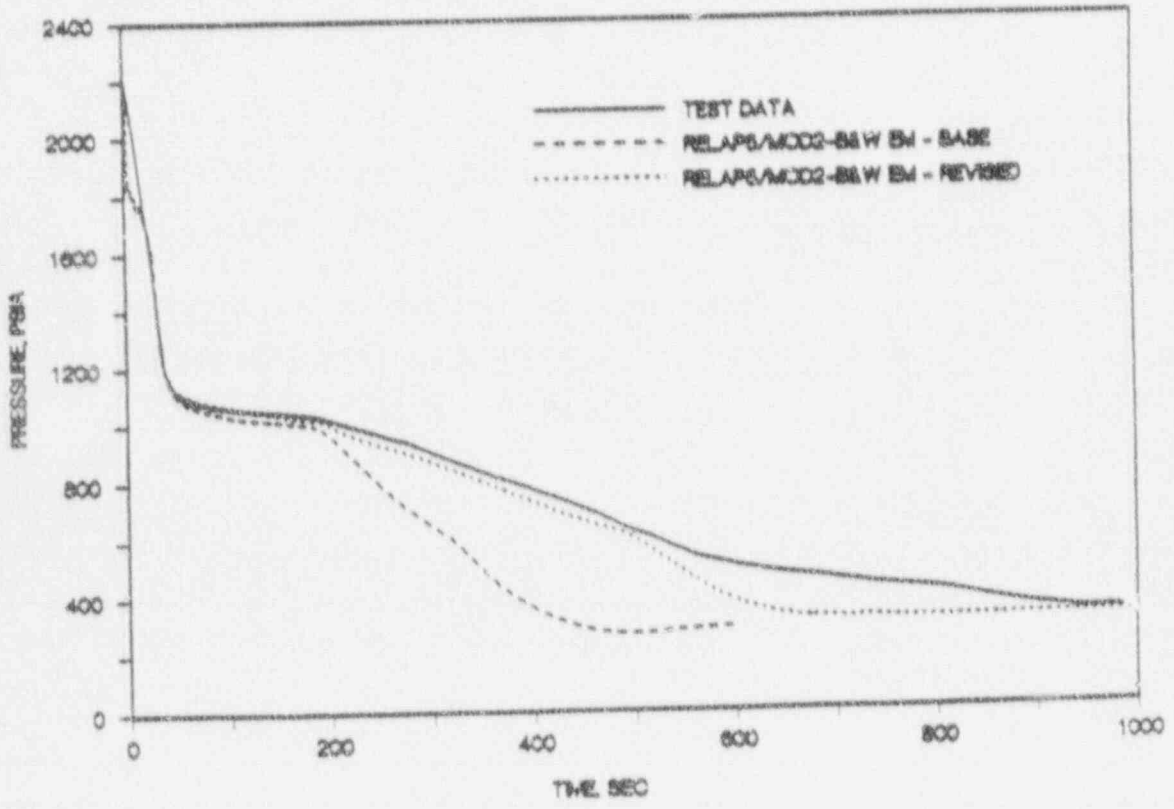



FIGURE 5-9 SEMISCALE TEST S-LH-1; PRIMARY SYSTEM PRESSURE.



6. In response to Question 9a of the previous NRC questions, you indicated that flow blockage due to pre-rupture cladding swell is not modeled in the EM, based on an estimated pre-rupture cladding strain of 20% which would add only small flow resistance because of the smooth area change.

(a) Is the estimated maximum pre-rupture cladding strain of 20% applicable to all peak linear heat generation rates? Justify your response.

Response: The cladding strain either prerupture or at rupture does not directly depend on the peak linear heat generation rate. Strain is primarily a function of the cladding temperature, the cladding temperature rise rate, and the stress to which the cladding is exposed. Because linear heating rates contribute through the heat transfer process to the determination of the these parameters, they indirectly relate to cladding strain. Furthermore, experiments by Hardy, Reference 6.1, have shown that prerupture strain can be related to rupture strain, eliminating the need for separate prerupture and rupture strain correlations. This approach is used in the BWFC evaluation model making the amount of prerupture strain dependent on the current cladding temperature, the cladding temperature rate of rise, and the imposed stress but independent of local power levels.

D. G. Hardy at the Chalk River Nuclear Laboratories has performed extensive studies on Zircaloy-4 tubing strain and rupture behavior. The effect of the stress (pressure), cladding temperature, and temperature rise rate on cladding strain is given in Figures 11 and 12 of Reference 6.1, reproduced here as Figures 6-1 and 6-2, respectively. The following observations can be made concerning the strain.

1. The strain is an exponential function of temperature until very near the failure temperature.

2. Near the failure temperature, the cladding expands asymptotically to its rupture diameter.

3. The point at which this asymptotic expansion occurs is approximately 10 to 20 percent of the rupture strain for Zircaloy-4.

Therefore, the BWFC approach which limits the maximum prerupture strain to 20 percent of the rupture strain and uses an exponential fit to determine the strain below 20 percent is a reasonable approach. This approach has been previously licensed for other LOCA applications in the THETA1-B and CRAFT2 computer codes, and in the licensing audit calculation codes TOODEE2 and FRAP-T6 (Reference 6.2).

(b) Since Appendix K, Item I.C.7.a, requires the effects of flow diversion due to cladding swell or rupture to be included in the blowdown calculation, justify why neglecting the pre-rupture swell in the LOCA EM meets this requirement. The justification may include analyses or justification to support the argument that flow diversion effects for cladding swell of 20% or less are small during the blowdown period, and, therefore, can be neglected in RELAP5/MOD2-B&W.

Response: The responses to parts b and c of this question both demonstrate that the degree of flow diversion due to prerupture strain does not adversely effect heat transfer in the rupture region and that downstream effects of the diversion are inconsequential to the determination of peak cladding temperature. The same considerations apply to the reflood and blowdown portions of the transient and both questions are covered under the response to part c below.

(c) Since the RELAP5/MOD2-B&W cladding swell and rupture model is implemented in BEACH, Revision 2, provide the same information for BEACH with respect to the reflood period.

Response: In RELAP5/MOD2-B&W during blowdown and in BEACH during reflood the effect of flow diversion during the prerupture swelling (plastic deformation) period is neglected because inclusion of the effect would not significantly alter calculated results. This can be demonstrated through an evaluation of the geometric changes to the fuel pin during prerupture strain and the impact of those changes on fuel assembly flows and heat transfer. The following paragraphs evaluate the flow resistance effects due to the maximum amount of prerupture strain (20 percent of the maximum possible rupture strain of 90 percent). Using a best estimate value for crossflow (high resistance), the effect of the prerupture strain is shown to be an average 2.5 percent reduction in fuel assembly flow with most of the diversion occurring at the plenums. This amount of diversion is considered inconsequential within the overall calculation. Using a minimum crossflow resistance assumption, the effect on flow is a diversion of about 7.5 percent, the heat transfer within the rupture region incurs a net increase of about 10 percent, and most of the diverted flow (6.5 of the 7.5 percent) is recovered just downstream of the rupture region. Thus, the effect of prerupture strain on the cladding temperature varies from inconsequential to somewhat beneficial within the rupture region.

Prerupture Strain Geometry

Swelling of the cladding during the prerupture period will produce a smooth contraction and expansion of the flow channel similar to that of a convergent-divergent

nozzle. Both experimental and calculated results indicate that the effect will extend for substantially all of the grid span within which the highest cladding temperature occurs. Figure 6-3 shows a representative strain pattern from the REBEKA test number 6 (Reference 6.3) as a function of axial position. The peak strain near the center of the grid span is from rupture while the strains on either side of the rupture were set prior to the occurrence of the rupture. These strains are not confined to the proximity of the rupture location. Similarly, an examination of the cladding temperature distribution along the grid span with the highest cladding temperatures shows that prerupture strain would be spread over the entire grid span. Figure 6-4 shows the axial distribution of prerupture strain based on cladding temperatures as predicted by the B&W RSG LOCA evaluation model. Typical rupture strain for the evaluation model is about 60 percent, which would result in a 12 percent prerupture strain. Notwithstanding, Figure 6-4 and the calculations presented in this answer are based on the 90 percent maximum rupture strain calculable by the evaluation model, giving a prerupture strain of 18 percent.

Effect on Fuel Assembly Flow Resistance

Because strain occurs over the entire grid span, differentiation between flow channels in a fuel assembly can be ignored, and the flow process can be evaluated on an assembly basis. The form loss for the affected section was evaluated at maximum strain using a standard approach for a converging diverging nozzle (Reference 6.4 pages 2-11 and A-26). This gave a $k_{form}$ of 0.01 based on the unstrained fuel assembly flow area. Because the flow area has been altered over a substantial length of the

assemble it is also necessary to reevaluate the strained section frictional loss. In the following development:

Subscript h refers to the unswelled hot channel,
Subscript b refers to the swelled or partially blocked channel,
Subscript f refers to the frictional component,
Subscript g refers to the grid,

$D_h$ = Hydraulic Diameter = 0.0365 ft,
$D_b$ = 0.0289 ft,
$A_h$ = Assembly Flow Area = 0.265 $ft^2$,
$A_b$ = 0.226 $ft^2$,
$K_{fh}$ = Hot Channel Friction Factor = 1.55,
$K_{form,g}$ = Grid Form Loss Factor = 2.0, and

$$\Delta P = \frac{f_b L}{D_b}\frac{W_b^2}{A_b^2} = \frac{f_b L A_h^2}{D_b A_b^2}\frac{W_b^2}{A_h^2} \quad .$$

This allows $K_{fb}$ to be written as

$$K_{fb} = \frac{f_b L A_h^2}{D_b A_b^2} \quad .$$

This leads to

$$K_{fb} = K_h \left(\frac{f_b}{f_h}\right)\left(\frac{D_h}{D_b}\right)\left(\frac{A_h^2}{A_b^2}\right) \quad ,$$

$$as \quad f \propto \frac{1}{Re} \quad and \quad Re \propto \frac{WD}{A} \quad , and$$

$$\frac{f_b}{f_h} = \frac{Re_h}{Re_b} = \left(\frac{W_h}{W_b}\right)\left(\frac{D_h}{D_b}\right)\left(\frac{A_b}{A_h}\right) = 1.2 \quad ,$$

under the assumption of a 10 percent reduction in $W_b$ over $W_h$. Thus,

$$K_{fb} = K_h\,(1.2)\left(\frac{0.0365}{0.0289}\right)\left(\frac{0.265}{0.226}\right)^2 = 3.23 \quad .$$

Summing from just before the grid below the swelling to just after the grid above the swelling gives

$$k_t = k_{form,g} + k_f + k_{form,swelling} + k_{form,g} \ ,$$

$$k_{ta} = 2.0 + 1.55 + 0.0 + 2.0 = 5.55 \ , \text{ and}$$

$$k_{tb} = 2.0 + 3.23 + 0.01 + 2.0 = 7.24 \ .$$

This results in a 30 percent increase of the total resistance. Expressing the resistances as $k/A^2$ terms, based on the unblocked area, gives

$$\left(\frac{k_t}{A^2}\right)_a = 79 \text{ and}$$

$$\left(\frac{k_t}{A^2}\right)_b = 103 \ .$$

Crossflow Resistance

To evaluate the effect of the added resistance, the flow diversion around the strained section, if any, must be computed and the recovery of the flow downstream of the strain determined. To do this, the ability of the flow to cross to an adjacent fuel assembly must be determined. The B&W RSG LOCA evaluation model uses a crossflow resistance model (documented in Reference 6.5 Appendix H) that proportionalizes the crossflow resistance to the ratio of Reynolds numbers of the adjacent fuel assemblies with an additive constant. Regardless of flow direction, the ratio is always set to be equal to or less than one. The maximum resistance occurs when the Reynolds numbers in adjacent assemblies are about the same and the minimum

when the flow is severely imbalanced. For flow from a single assemble to the eight assemblies that surround it, the following resistances and flow areas apply to flow between adjacent grid spans.

| | k | Area |
|---|---|---|
| Minimum Crossflow Resistance | 50 | 0.37 $Ft^2$ |
| Maximum Crossflow Resistance | 140,000 | 0.37 $Ft^2$ |

The selection of minimum or maximum resistance as appropriate for determining crossflow in a study of the impact of prerupture strain is not straightforward. Sensitivity studies during blowdown have led to the adoption of the minimum resistance case for general application during blowdown. Those studies, however, did not specifically address a local swelling and did not consider reflood conditions. In general, the hot fuel assembly will not be isolated in the middle of abnormal or severely cold assemblies and the Reynolds numbers in adjacent fuel assemblies will not differ substantially, leading to maximum crossflow resistance being the most realistic simulation. Notwithstanding, a sensitivity study was conducted that established the variation of the flow pattern with crossflow resistance. The evaluations that follow establish the impact of prerupture strain using either assumption for the crossflow resistance.

Prerupture Strain Flow Pattern

To establish the core flow pattern after prerupture straining of the cladding, flow through a two-channel network, with the hot fuel assembly as one axial channel and adjacent fuel assemblies as a second axial channel, was solved by the network resistance method. Crossflow was allowed between the two axial channels at six elevations. Figure 6-5 provides a diagram of the

modelling including $k/A^2$ values for each of the paths. The swelling was modelled in path 11 representing an axial power shape peaked near the center of the core. This network was solved both with and without swelling modelled under identical plenum conditions, and the impact of swelling on flow at a segment was taken as the ratio of the segment flow with swelling modelled to the normal segment flow.

Several sensitivity studies were performed to establish the range of potential impacts. The initial model used only the eight fuel assemblies that surround the hot assembly in the adjacent channel. This model was run as: (1) base unswelled, (2) swelling with expected (maximum) crossflow resistance, (3) minimum crossflow resistance from the correlation, and (4) one half minimum crossflow resistance. The input crossflow $k/A^2$ values are listed on Figure 6-5, and the resultant flow patterns, normalized to the base case, shown in Figures 6-6 to 6-8. As expected, maximum flow diversion occurs for the lowest crossflow case. That diversion, however, is localized, with the hot channel recovering the flow just downstream of the swelling. As crossflow increases, two effects are observed: the amount of flow diversion decreases, and the diversion becomes less localized spreading toward the plenums. For the best estimate, most resistive case, the flow diversion is somewhat less than 3 percent and occurs nearly entirely at the plenums. Comparing the minimum correlation study to the one-half minimum shows that flow diversion is approaching a limit controlled by the effect of the diverted flow on the adjacent channel pressures. An additional study, Figure 6-9, increased the number of assemblies modelled in the adjacent channel to 192, the remainder of the core, and showed that the diversion was slightly increased by minimizing the impact of the diverted flow on the adjacent channel.

Effects on Heat Transfer

From the preceding discussion, the nature of the flow in a fuel assembly experiencing prerupture strain is either a substantial localized reduction over the strained section or a relatively inconsequential reduction spread over the whole assembly. To evaluate the effect on peak cladding temperature, the flow impacts must be combined with surface area and heat transfer coefficient effects. These are assessed below for the two bounding cases of crossflow resistance, the expected or maximum resistance case and the case with minimum resistance from the correlation.

Minimum Crossflow Resistance Case

For this case, the flows upstream of the swelling are within 1 percent of those for an unswelled condition. Channel areas and other factors affecting the heat transfer process are also unchanged. Therefore, the impact of the swelling on the fuel assembly heatup calculation upstream of the swelling is considered negligible.

Within the swelling region, the flow has been reduced by something less than 10 percent, while the cladding surface area has been increased. The linearly averaged surface area, wetted perimeter, and hydraulic diameter are:

| | Base Case | With Swelling |
|---|---|---|
| Grid-to-Grid Cladding Surface Area, $ft^2$ | 49.3 | 53.2 |
| Wetted Perimeter, ft | 29.0 | 31.3 |
| Hydraulic Diameter, ft | 0.0365 | 0.0289 |

The generalized effect on the heat transfer coefficient can be assessed by the proportionality of the Nusselt number to the Reynolds number to the 0.8 power.

$$Nu \propto Re^{0.8} \quad or$$

$$\frac{h\,D}{k} = \left(\frac{\rho\,D\,v}{\mu}\right)^{0.8} .$$

Substituting W/A for ρv, solving for h, and ratioing the blocked and unblocked h gives

$$\frac{h_b}{h_h} = \left(\frac{W_b}{W_h}\right)^{0.8} \left(\frac{P_h}{P_b}\right)^{0.8} \left(\frac{D_h}{D_b}\right) .$$

Substituting values gives

$$\frac{h_b}{h_h} = 1.19 \left(\frac{W_b}{W_h}\right)^{0.8} .$$

Thus, the heat transfer coefficient in the swelling region will experience a net increase of 9 percent even with a flow decrease of 10 percent. Combining this with the area increase of 8 percent gives an expected increase in total heat transfer of 18 percent. Beyond this, there would be some interaction of the droplet field with the swelled cladding causing increased droplet breakup and enhancing interphase heat transfer. Thus, the effect of swelling within the swelled region is an increase in heat transfer and a decrease in cladding temperature.

Downstream of the swelling, the flow has returned to values representative of those upstream of the swelling, and the same argument that the flow reduction is insignificant can be applied. Here, however, the effects of the swelling on the amount of heat transferred in the swelled section will be of benefit. Within the swelled section the increased turbulence, heat transfer, droplet formation, and interphase heat transfer will combine to

increase the steam generated in that section. Therefore, the downstream conditions will be at a somewhat higher quality leading to some improvement in the heat transfer coefficient.

Maximum Crossflow Resistance Case

The considerations for the maximum resistance case are much the same as for the minimum resistance case. While the flows upstream and downstream of the swelled region have decreased slightly more than those of the minimum resistance case (2.5 percent versus 2.0 percent), they are still well within the range of inconsequential differences. The effects within the swelled section will remain the same except that with the higher flow the total heat transfer will experience an increase of about 25 percent. As with the minimum crossflow resistance case, the heat transfer downstream of the swelling will benefit from the fluid interactions within the swelling region but because of the higher flow in the swelling region these benefits will be larger. Therefore, the heatup calculations for this case as well will be either unaffected or will benefit by the modelling of prerupture strain.

Conclusion

The foregoing discussion shows the effect of prerupture swelling on the heat transfer in the hot fuel assembly to be either a minor negligible reduction or a solid improvement over the nonswelling case. Maximum flow diversion in regions removed from the swelling region was 2.5 percent, which is considered negligible. Maximum flow diversion from the swelling region was 10 percent, but surface area and hydraulic effects combine to give an 18 percent increase in the heat transfer over the region,

even with the reduced flow. Therefore, the explicit modelling of prerupture strain-induced flow diversion need not be included in the evaluation model. As none of the considerations upon which this conclusion is based rely on flow direction in the core or specific core conditions, they apply equally well to the blowdown and reflood phases of the accident simulation. Because it has been shown that the modelling of prerupture strain does not alter the computation of peak cladding temperature, the BWFC LOCA evaluation model, containing no explicit modelling for the phenomena, meets the requirements of Appendix K to 10 CFR 50.46.

References


6.1 D. G. Hardy, "High Temperature Expansion and Rupture Behavior of Zircaloy Tubing," CONF-730304, USAEC/TIC on Water Reactor Safety, March 1973.

6.2 M. P. Bohn et al., The Licensing Audit Calculation (LACE) Models in the FRAP-T4, EGG-CDAP-5144, April 1980.

6.3 K. Wieher and U. Harten, Datenbericht ... Ka-6, KfK 3986, March 1986.

6.4 FLOW of FLUIDS Through Valves, Fittings, and Pipe, Technical Paper No. 410, Crane Co., New York, New York, 1982.

6.5 Craft2 - FORTRAN Program for Digital Simulation of a Multinode Reactor Plant During Loss of Coolant, BAW-10092 Rev. 2, Babcock & Wilcox, April 1975.

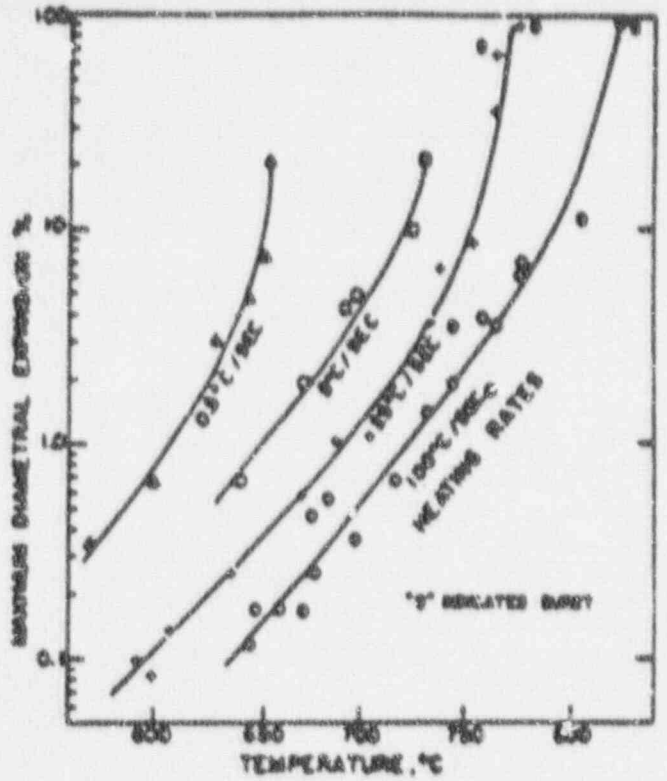


Figure 6-1 Effect of heating rate on the maximum diametral expansion vs temperature curve for tubes pressurized to 5.5 MN/m$^2$ (800 lb/in$^2$).

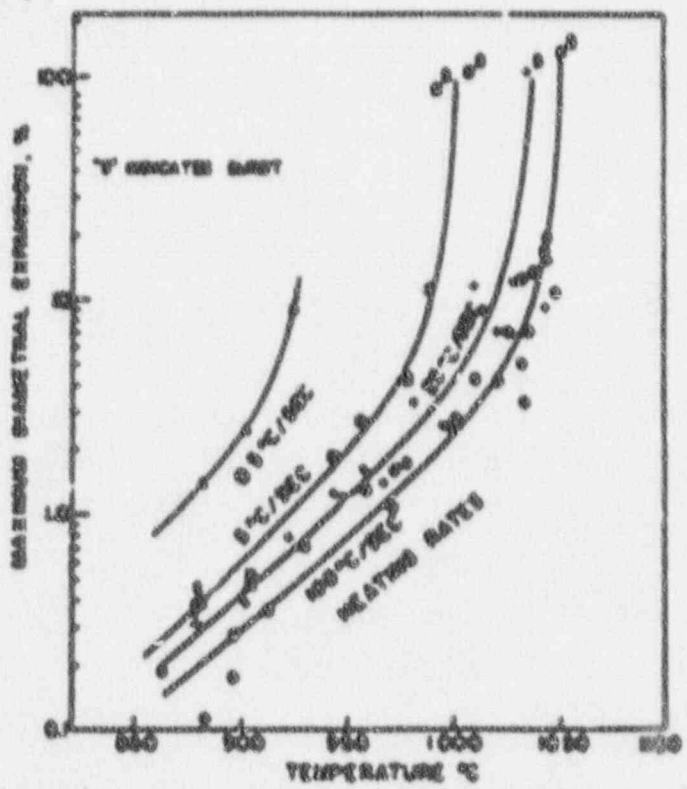


Figure 6-2 Effect of heating rate on the maximum diametral expansion vs temperature curve for tubes pressurized to 0.7 MN/m$^2$ (100 lb/in$^2$).

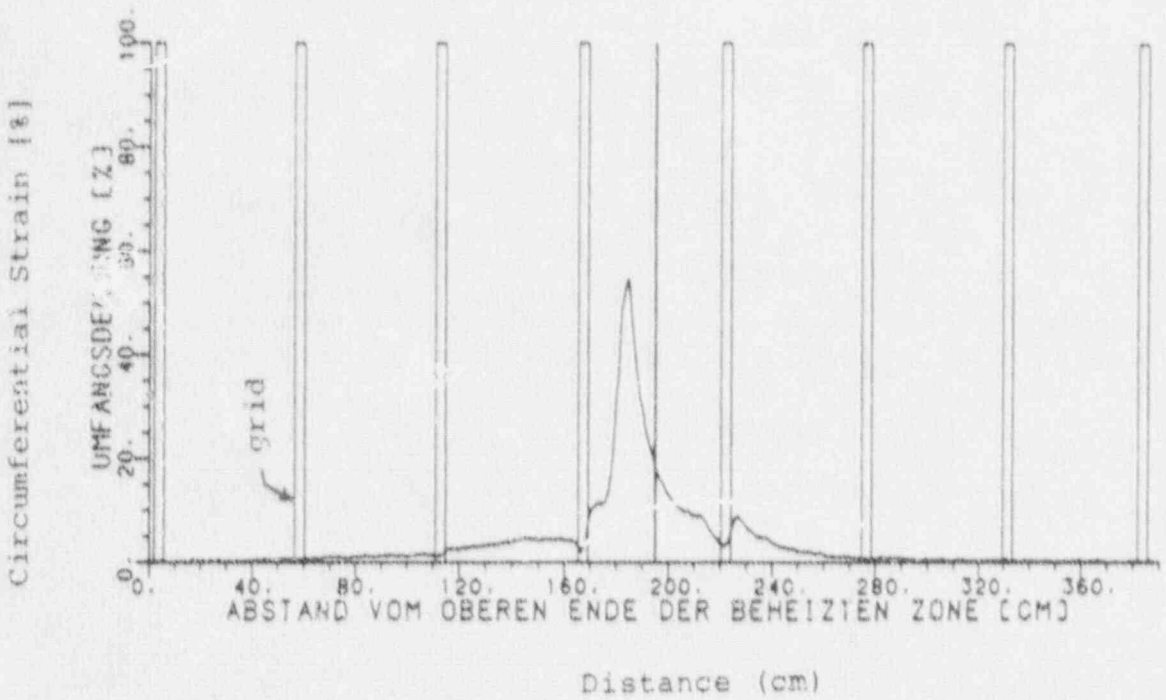
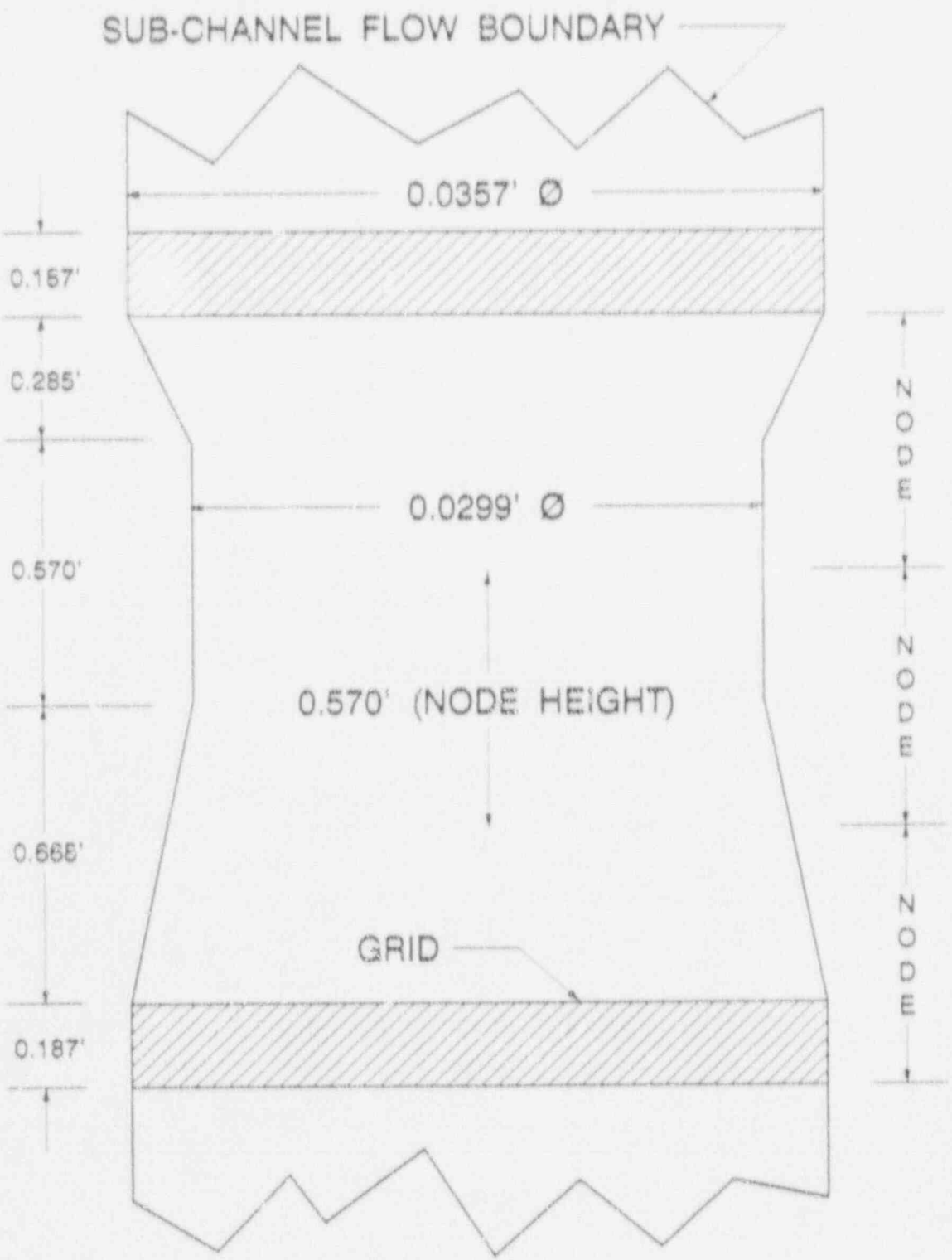
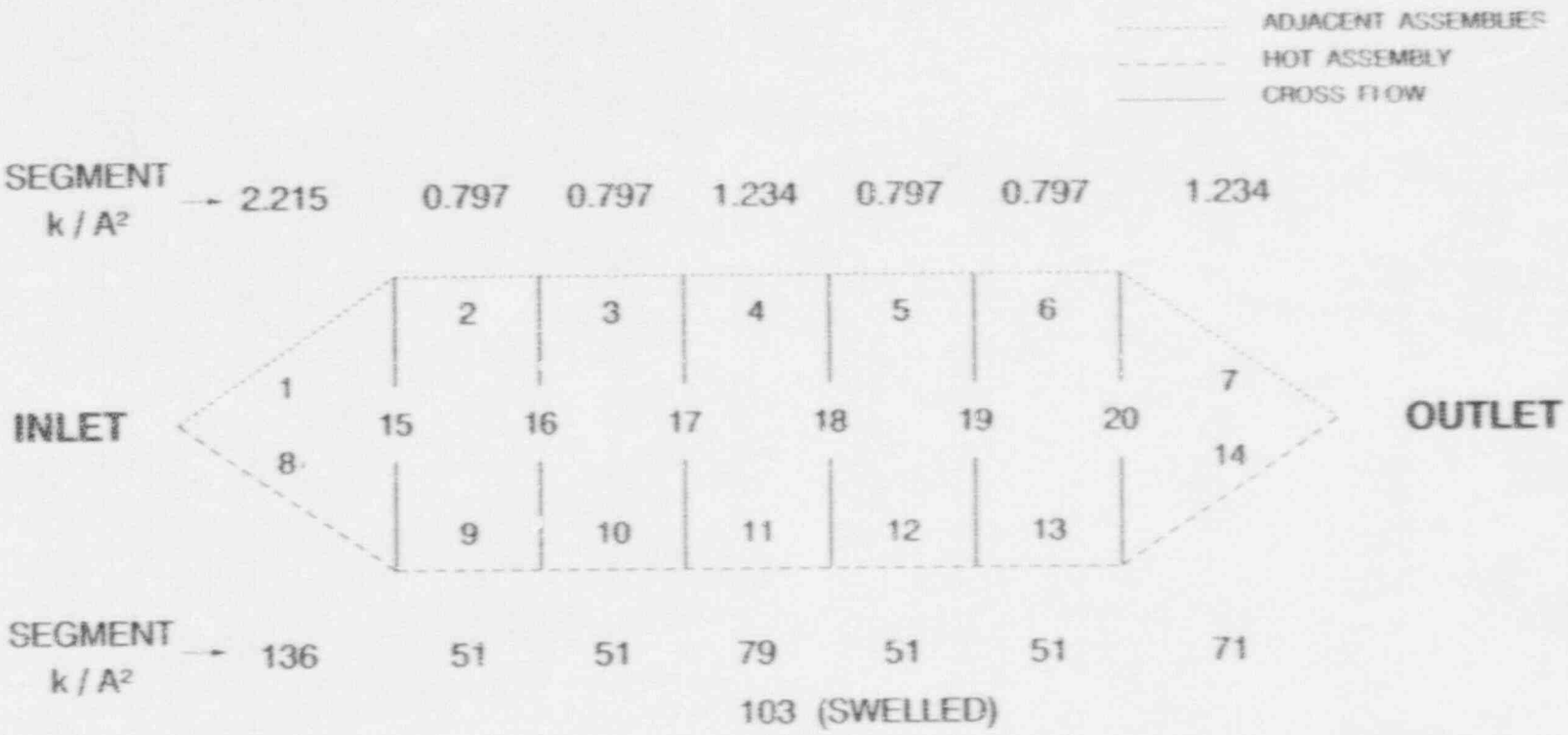



Figure 6-3. Circumferential Strain vs Distance from the Top of the Heated Section for REBEKA-6 Test Rod 49.

FIGURE 6-4. FLOW CHANNEL AREA REDUCTION DUE TO PRE-RUPTURE SWELLING.



# FIGURE 6-5. FLOW DIVERSION STUDY NETWORK--BASE CASE CONFIGURATION




| | BASE | BEST ESTIMATE | MINIMUM CORRELATION | 1/2 MINIMUM OF CORRELATION | 192 FA ADJACENT ASSEMBLIES |
|---|---|---|---|---|---|
| CROSS FLOW k / A² → | 1.0 E+6 | 1.0 E+6 | 375 | 187.5 | 375 |

FIGURE 6-6. FLOW DIVERSION STUDY NETWORK--BE CASE CONFIGURATION

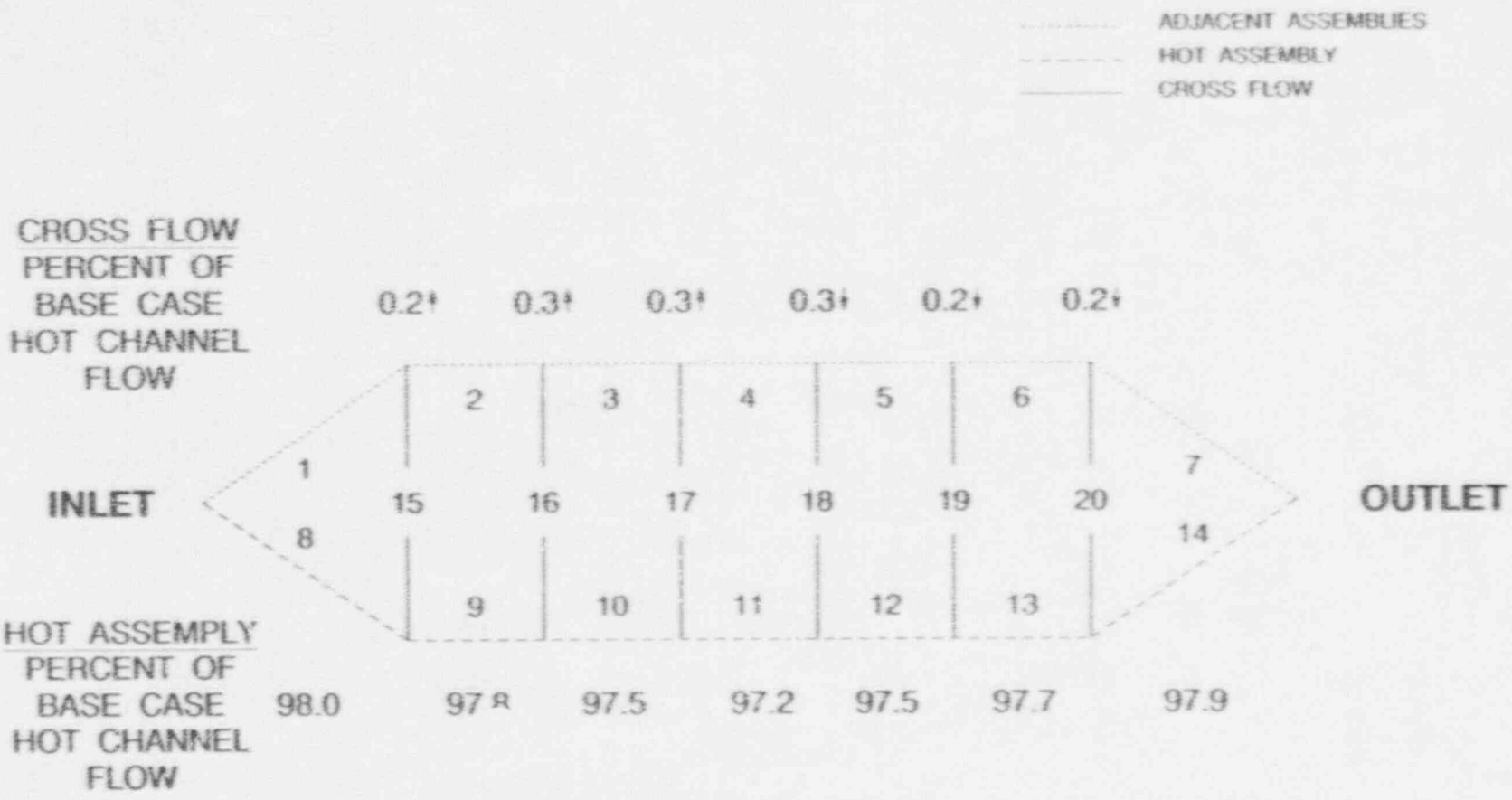


FIGURE 6-7. FLOW DIVERSION STUDY NETWORK--MINIMUM CORRELATION

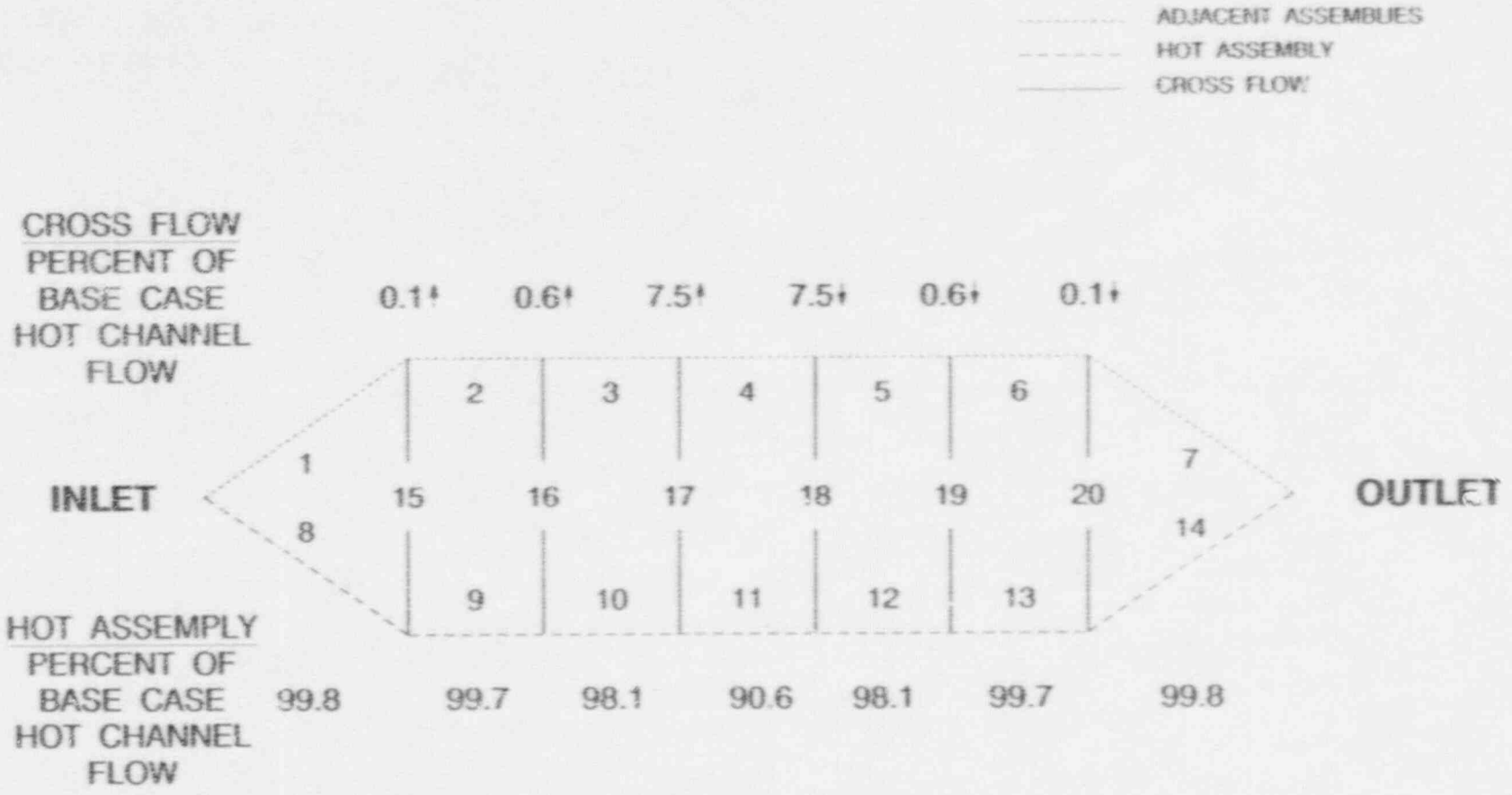


FIGURE 6-8. FLOW DIVERSION STUDY NETWORK--1/2 MINIMUM CORRELATION

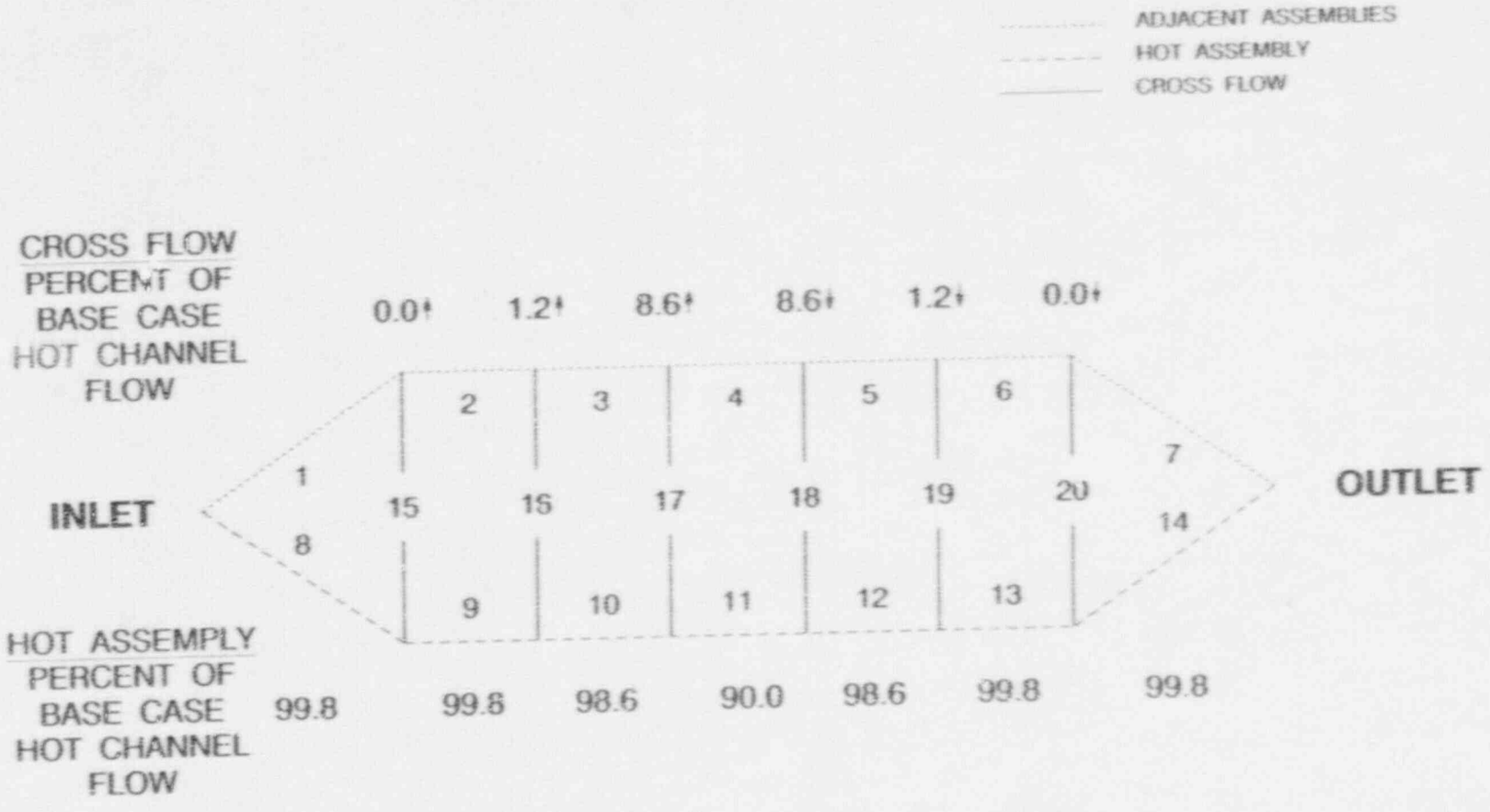
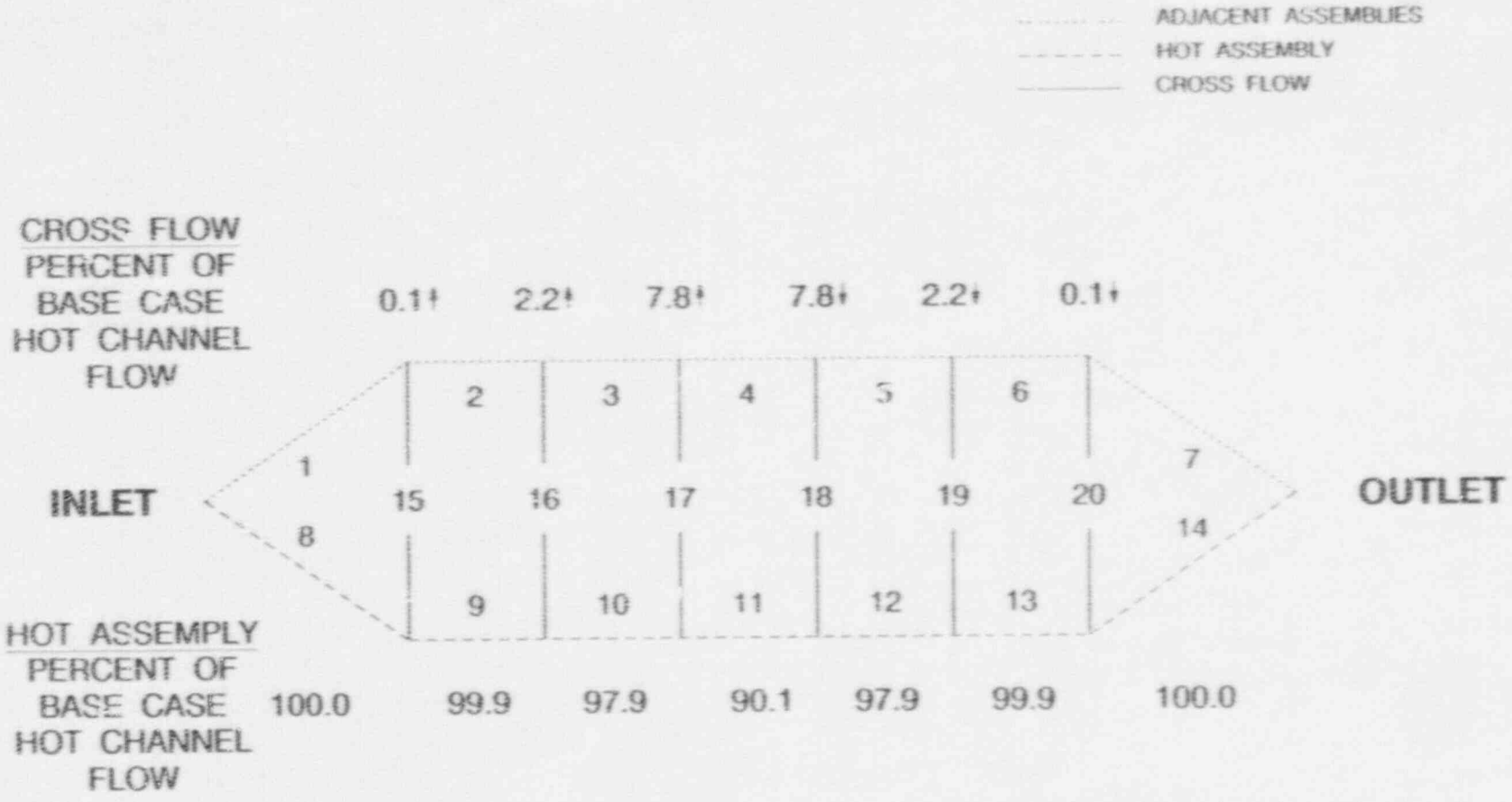


FIGURE 6-9. FLOW DIVERSION STUDY NETWORK--192 ADJACENT ASSEMBLIES



7. The interphase drag model in RELAP5/MOD2-B&W is the model carried over from RELAP5/MOD2 which tends to overpredict interphase drag. This is nonconservative for loop seal clearing phenomena because higher drag tends to clear the loop seal of all liquid even when the steam flow is not high enough to entrain the liquid out. As a result, core uncovery and the PCT may not be accurately calculated in cases when the liquid should have been retained in the loop seal. This overprediction of drag may be overcome by either model changes or nodalization. Therefore, provide a loop seal nodalization study to show that the loop seal nodalization chosen for use in licensing calculations results in the highest PCT and that liquid is retained in the loop seal when the steam flow conditions so dictate. Alternatively, provide evidence to demonstrate that RELAP5/MOD2-B&W correctly calculates loop seal phenomena including liquid retention in the loop seal for low steam flow conditions.

Response: The over prediction of drag by the RELAP5/MOD2-B&W or RELAP5/MOD2 interphase drag model occurs for the slug flow regime in relatively small hydraulic diameter flow geometries. Such geometries are limited to the reactor core, steam generator tubes, and perhaps the upper plenum regions of the reactor coolant system. During loop seal clearing for SBLOCA, the drag models are applied to regions with large hydraulic diameters, 28 to 30 inches, where the RELAP5 drag model has been shown to function acceptably. Within the following paragraphs the loop seal clearing process will be described and the controlling parameters and models identified, the dependency of loop seal clearing on these parameters and models developed, and noding sensitivity studies and experimental support for the handling of the controlling parameters discussed. These arguments will demonstrate that the clearing process is modelled appropriately by the BWFC evaluation model, including the RELAP5 drag model application. Furthermore, a provision of the BWFC evaluation model relative to the loop seal clearing process will be shown to establish a clearly conservative treatment for the very small SBLOCAs that depend on loop clearing for final depressurization.

Loop Seal Clearing

Small break LOCAs, in NSS designs that do not provide for venting of steam away from the upper plenum and with the break located between the reactor coolant pump (RC pump) and the reactor vessel, evolve through a special process known as loop seal clearing. For these breaks, the RCS depressurization proceeds slowly until after a clear steam path through the loops from the upper plenum to the break is established. Following RC pump coastdown, during the low void discharge phase, steam generated in the core can not pass to the break because a water trap exists in the RC pump suction piping. Therefore, steam accumulates in the upper regions of the plant and displaces water through the break. Gradually steam accumulation and the resultant hydrostatic heads within the system will force the clearing of the water trap in at least one of the RCS loops.

For moderate to large break areas within the small break arena, the process proceeds rapidly with system volumetric flows so high that loop clearing becomes indistinguishable as a separate process during blowdown. For the smaller breaks of the small break LOCAs, however, the process is distinctly trackable and frequently involves suspension of system depressurization until the loops are cleared. The separation between the moderate and smaller break areas occurs somewhere between 4 and 5 inch diameter breaks for the BWFC evaluation model. Because the arguments supporting the modelling within the BWFC evaluation model for moderate to larger small breaks are extensions of those for the smaller breaks and more obviously applicable, the remainder of this response will be presented from the smaller break perspective.

For the smaller of the small breaks, the RCS fluid distribution prior to loop seal clearing is illustrated in Figure 7-1. Steam accumulating in the upper regions of the

plant has suppressed the liquid level in both the core and the RCS pump suction down leg piping. Although the liquid level is suppressed in all of the plant's down legs, differences in the operational pressures and/or other characteristics of the steam generators (atmospheric dump valve characteristics, degree of tube plugging, fouling of tube surfaces, etc.) can lead to more than half a foot difference between the levels of each loop. As the down leg levels continue to fall, one or two of them are lowered past the point at which steam can be passed to the pump suction piping up leg section and the loop seal clearing process commences.

The initial flow of steam into the pump suction up leg section occurs at the same rate as the liquid level decrease. However, within a few seconds the steam and water in the up leg section have mixed causing a decrease in up leg density and thereby a decrease in static head. Because the elevation heads in the up leg sections comprise the opposing force to steam flow, the flow in the loops that are starting to clear accelerates. The process once started is self-accelerating, as the mixture in the up leg lightens more steam flows into it which lightens it further, etc.. Because of the flow preferencing to the clearing legs, those loops, that are not clearing, slow or reverse their level decrease rate. Depending on break size they may now continue through the clearing process or remain partially filled with water.

In addition to increasing the steam flow to the clearing section, the lightening of the up leg section reduces the head required to support the system flows allowing liquid to flow from the pump discharge piping and the downcomer into the core. Although this raises the core level, the most important result is that liquid that has blocked the break flow is displaced laterally allowing steam from the clearing cold legs access to the break. With steam flow established at the break, system depressurization accelerates, the amount of

steam generator condensation, if any, decreases and steam flow through the cleared loops increases. The amount and degree of loop clearing depends on the break size, the drag model, and the number of loops that are clearing. Any liquid retained in the very bottom of the suction piping at the bend will eventually be vaporized through flashing, desuperheating of steam, and heat transfer from the piping. Likewise, the cleared loops will remain liquid free because of steam generator superheating and primary metal heat transfer.

Important Factors in Loop Seal Clearing

The plant parameters that primarily govern the timing, occurrence, and degree of loop seal clearing are plant geometry, RCS inventory, break size, the steam generator controls, and the amount of steam to liquid drag. The plant geometry and system inventory control both the timing of the initiation of loop seal clearing and the amount of level depression required in the core region to achieve an initiation of loop seal clearing. Break size is important to the timing of the initiation of loop seal clearing. The steam generator controls and other loop asymmetries can significantly effect the number of loops that clear. The drag model controls the timing of the clearing process once it initiates and the amount of residual liquid remaining in the pump suction piping after full steam flow is achieved.

The most significant requirement for modelling of loop seal clearing is to assure that the appropriate amount of the RCS is occupied by steam prior to allowing steam to flow laterally from the pump suction down leg to the pump suction up leg. This can be effected by assuring that the down leg volume, represented by the control volumes above and upstream of the lowest (bottom) node in the down leg modelling (for example nodes 340-01, -02, -03, -04, and -05 in Figure 11-2 or nodes 340-01 and -02 in Figure 11-2a), approximates the volume of

the pump suction piping above and upstream of the pump suction spillunder (the spillunder is the top of the cold leg pump suction piping at that pipings lowest elevation). Additionally, the top of the lowest node (node 340-06 in Figure 11-2 or node 340-03 in Figure 11-2a for example) should approximate the spillunder elevation. These modelling choices prevent the flow of steam from the down leg to the up leg prior to the set up of the appropriate elevation heads within the system.

The second most significant requirement is in the control parameters for the steam generators. The coolant density in the up leg section of the pump suction piping is controlled by the operating pressure of the steam generator in that loop. Variations of 100 psi between steam generators are not unreasonable for the type of pressure control used in emergency operations. That in turn can lead to about a 2 percent difference in cold leg densities that, over the length of the up leg section, could result in a liquid level difference of around 2 to 3 inches amongst the four cold legs. This difference when combined with other loop imbalance factors can cause as much as a 6 inch level difference. This becomes significant since it allows the lower level loop to clear while preventing the clearing of the higher level loop or loops.

The BWFC evaluation model specifies that the steam generators shall be controlled such that the broken loop generator operates, during a SBLOCA, at 100 psi above the three intact loop generators. Therefore, for smaller break areas where the break flow does not dominate system flow, the system will trend toward the clearing of only one loop. The conservatism of this approach occurs following loop seal clearing during the core boildown phase. At that time, the elevation head imbalance between the downcomer level and the core level provides the motive force for the flow of steam to the break.

If all loops are clear, there are 4 parallel paths available to vent core steam. If only one loop clears, there is only one path to vent the same amount of steam. The difference in required head can be as high as two feet of core collapsed level and therefore of substantial significance to the prediction of peak cladding temperature.

For accident simulations in which only one loop will clear, the steam flows in the clearing loop rapidly exceed those required to completely clear the loop and the drag model is of little concern. For those that will clear all four loops, the drag model controls the void fraction development in the pump suction piping. Although any water remaining in the suction piping will eventually be vaporized, an inappropriate drag model could alter the system elevation heads for several hundred seconds. That in turn could alter the core water levels causing a misleading calculation of cladding temperature. Therefore, it is necessary that the drag model used in the SBLOCA evaluation be reasonable for the loop clearing process.

Noding Sensitivity Studies

The response to Question 11 of this set presents a noding study of the pump suction piping which shows that reducing the number of nodes representing the pump suction piping from 9 to 5 does not substantially alter the process of loop seal clearing. As documented in Table 11-2 the more detailed noding (REVISED model) caused loop seal clearing at 175 and 605 seconds for the intact and broken loops, respectively. The less detailed, EM, modelling cleared these loops at 175 and 560 seconds. Figures 11-9 through 11-12 also show that altering the noding detail preserved the important events of the simulation.

That the two noding arrangements achieve similar results is not fortuitous. Both models observe the modelling requirement that preserves the volume of the suction piping above and upstream of the spillunder location and the top elevation of the node at the spillunder location (nodes 340-06 and 240-06 for the REVISED model and 340-03 and 240-03 for the EM model). In fact the critical nodes were not altered in the translation from the REVISED to the EM model.

As a side study during the execution of the EM model benchmark presented in Question 11, the volume of the down leg section of the pump suction piping was redistributed equally between the three down leg volumes. This resulted in advancing the clearing of the intact loop to 160 seconds and the broken loop to 220 seconds. The larger and taller bottom node allowed lateral steam flow too early in the transient and at an inappropriate liquid distribution. The lower elevation head requirements for loop clearing caused the steam generator imbalances that had delayed the loop seal clearing in the broken loop to be less significant resulting in both loops clearing at about the same time. These studies demonstrate that it was not the number of nodes used in the REVISED S-LH-1 simulation that lead to successful prediction of the intact loop loop seal clearing but the proper construction of the nodes simulating the spillunder section of the piping.

Verification of the RELAP5/MOD2-B&W Drag model

The RELAP5 drag model has been questioned within the slug flow regime. These concerns have typically been raised during application of the model to slug flow conditions in small hydraulic diameter flow areas such as the core or steam generator tube regions. Within these restraints the model tends to overpredict drag, which, in the situation of loop seal clearing, would lead to an overprediction of the ability of the loops to clear. However, loop seal clearing during

small break LOCA occurs in large diameter pipes (28 to 30 inches) and at void fractions near or above the transition from slug to annular flow. Nithianandan and Shah et al., showed, in benchmarks to the G. E. level swell tests, that the model predicts the slug flow regime very well in large diameter pipes (12 inches) and has excellent agreement within the annular flow regime (Reference 7.1, Figure 21 page 291).

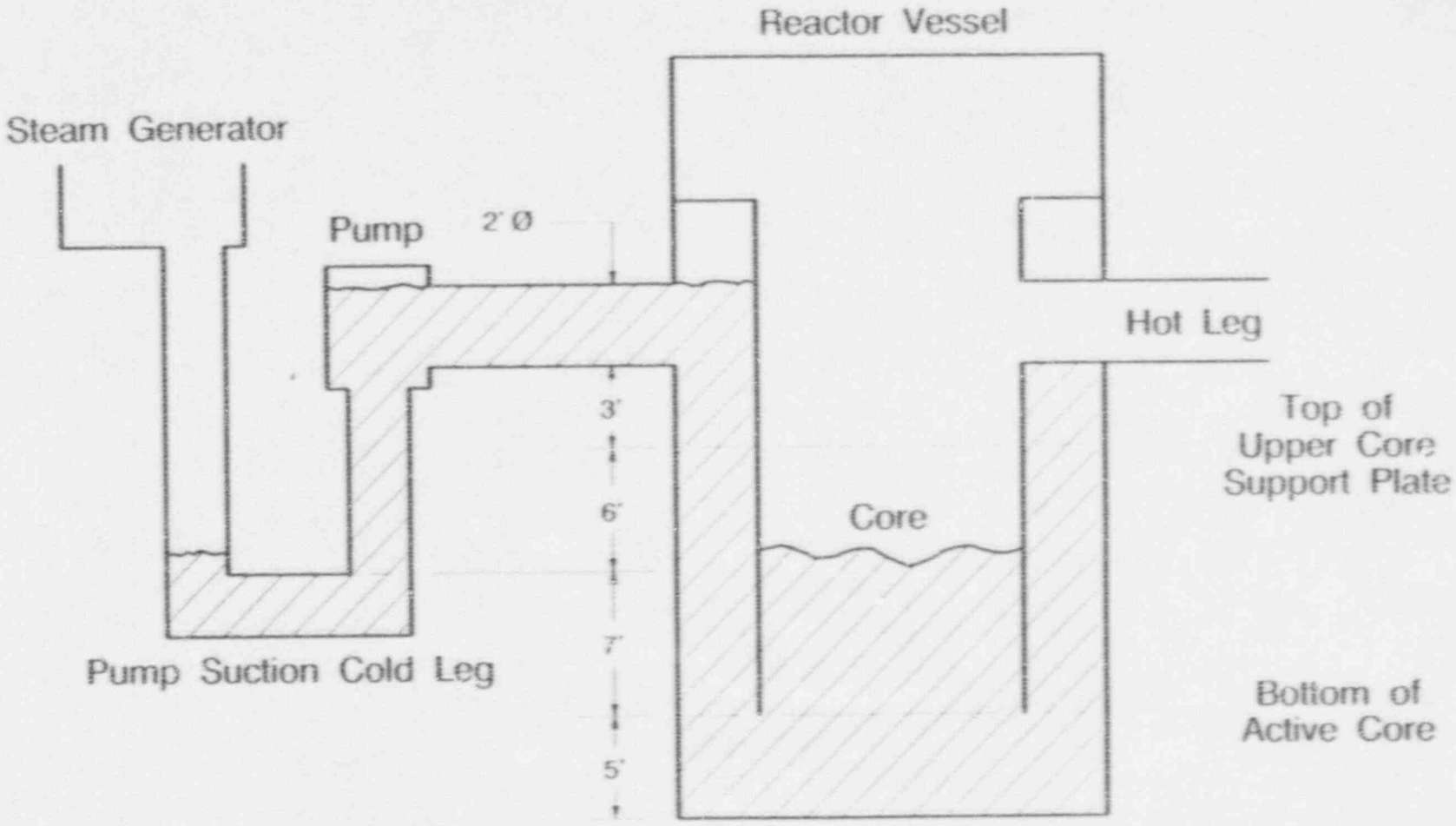
Conclusion

The modelling requirements for conservative evaluation of the loop seal clearing process during small break LOCA have been developed from qualitative reasoning considering the plant geometry and emergency operation modes. The resultant modelling requirements have been employed in a successful benchmark of the S-LH-1 Semiscale test that included the occurrence of loop seal clearing. The model predicted clearing of the intact loop within 4 seconds of what occurred in the experiment and conservatively delayed the clearing of the broken loop until late in the transient. Sensitivity studies show that the prediction of loop seal clearing is dependent on the proper modelling of selected nodes and relatively independent of noding detail away from these nodes. Benchmark comparisons show that the RELAP5/MOD2-B&W drag model predicts the slug and annular flow regimes very well in large diameter pipes. These results cumulatively verify that the RELAP5/MOD2 drag model and the BWFC evaluation model adequately represent the loop seal clearing process. Furthermore, by limiting clearing to one loop for the smallest of the SBLOCAs, the BWFC evaluation model assures a conservative treatment for those SBLOCAs to which loop seal clearing is essential for a successful outcome.

References

7.1 C. K. Nithianandan, N. [illegible] Shah et al., RELAP5/MOD2 Assessment at Babcock & Wilcox, "Thirteenth Water Reactor Safety Research Information Meeting," NUREG/CP-0072 Volume 5, U. S. Nuclear Regulatory Commission, February 1986.

# FIGURE 7-1. RSG PRIMARY LOOP GEOMETRY.



8. Since Appendix K requires the use of 1.2 times the ANS '79 standard for decay heat calculation. If the RELAP5/MOD2 B&W built-in reactor kinetic data for decay heat calculation will be used in the licensing calculation, provide a comparison of the results from the built-in data with the ANS 1971 standard to show compliance with Appendix K.

Response: Figures 8-1, 8-2, and 8-3 compare the RELAP5/MOD2-B&W curve fit for decay heating from fission fragments to the 1971 ANS-5 Committee Proposed Standard. Figure 1 of that proposed standard has been used for the comparison. The RELAP5/MOD2-B&W fit is an 11-term exponential expression that originated with RELAP4 as a fit to the Shure data. Because the Shure data also served as a basis for the proposed standard, the RELAP5/MOD2-B&W fit also in excellent agreement with the 1971 proposed standard. The maximum underprediction of the fit is 3.5 percent and occurs at 1.0 second. By 17 seconds the integrated deviation (comparison of total heat generated up to that time) between the fit and the standard is positive and remains so out to 200,000 seconds at which time this comparison was stopped.

Figure 8-1

DECAY HEAT COMPARISON - RELAP5/MOD2-B&W TO 1971 ANS PROPOSED STANDARD

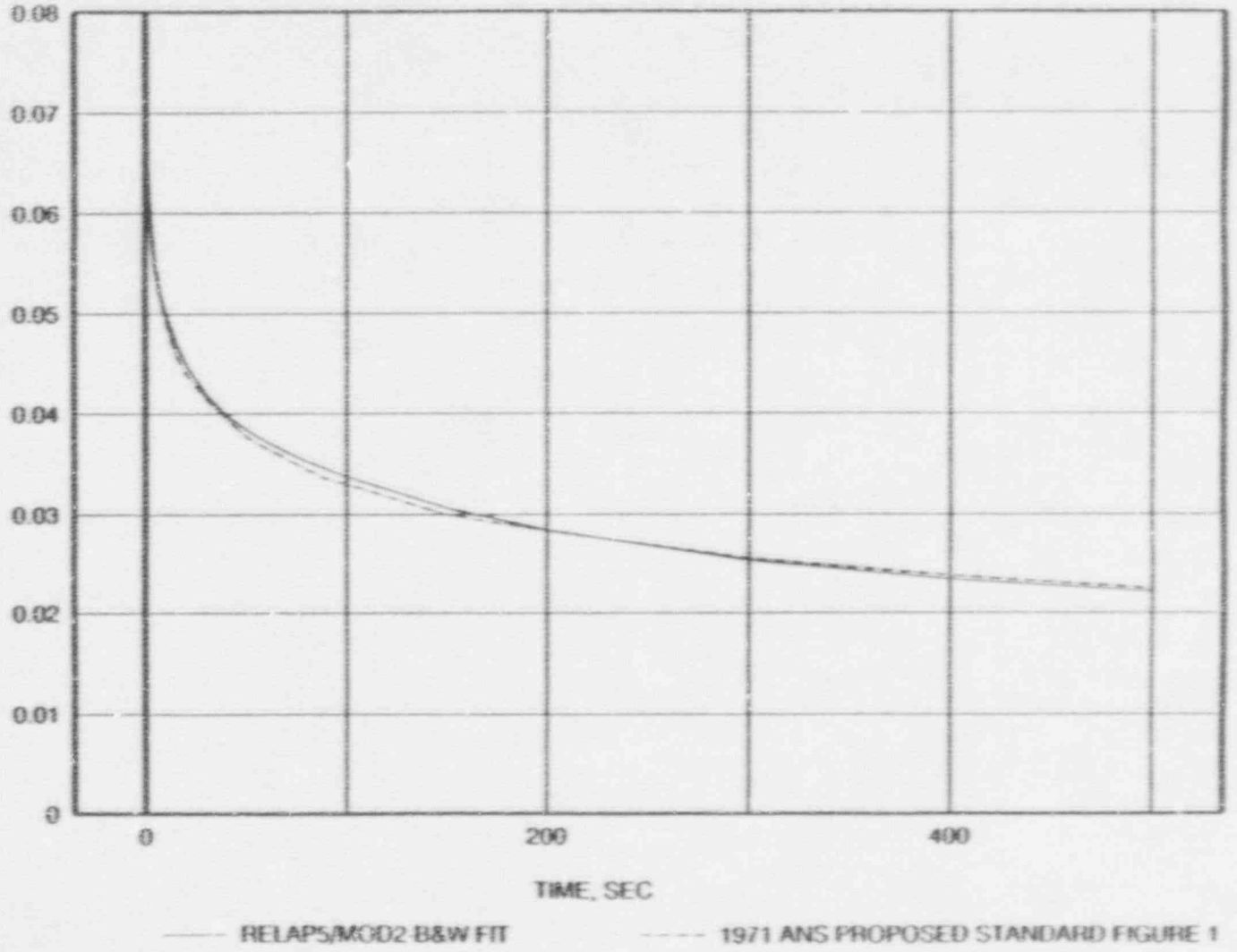


Figure 8-2

DECAY HEAT COMPARISON - RELAP5/MOD2-B&W TO 1971 ANS PROPOSED STANDARD

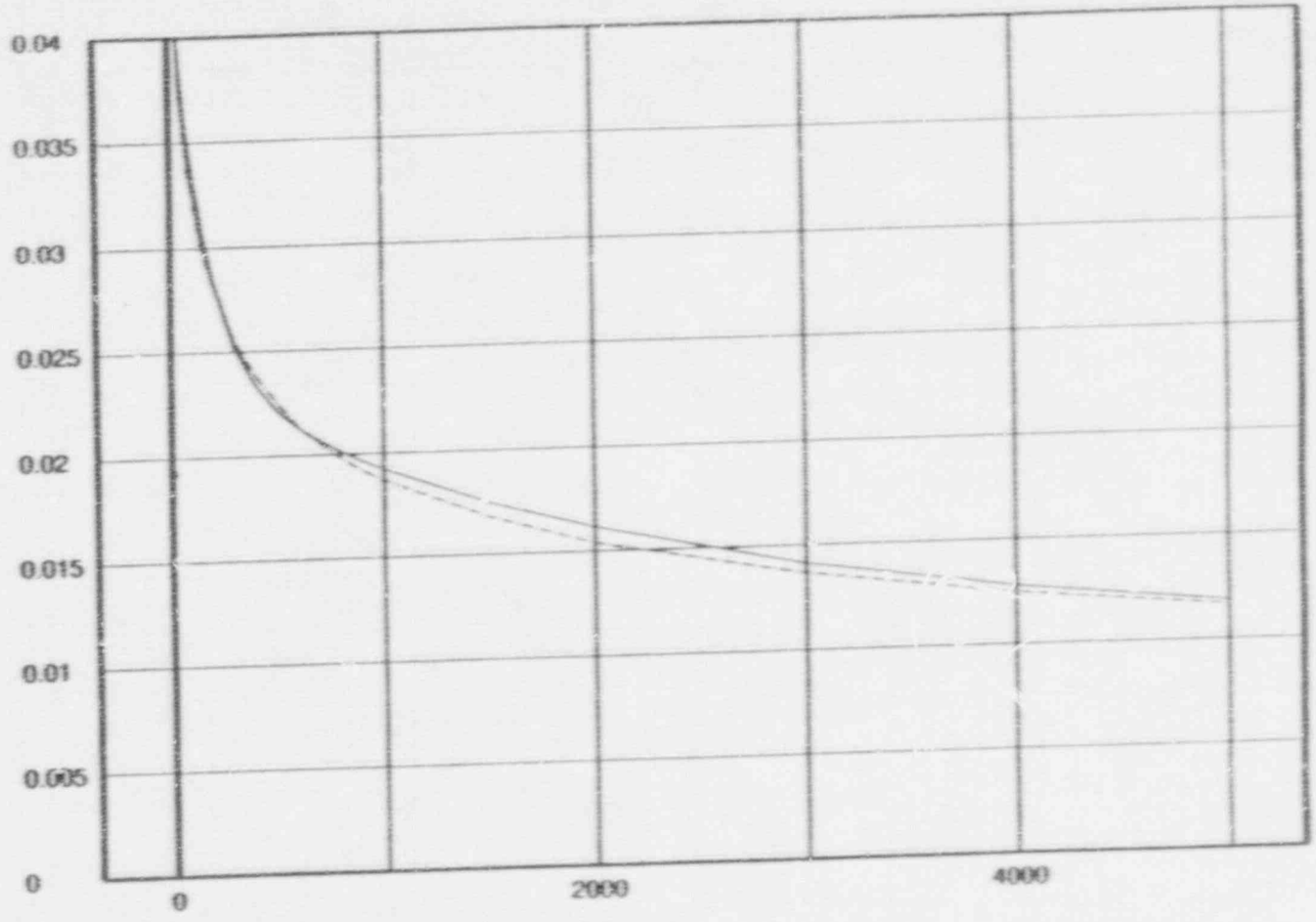
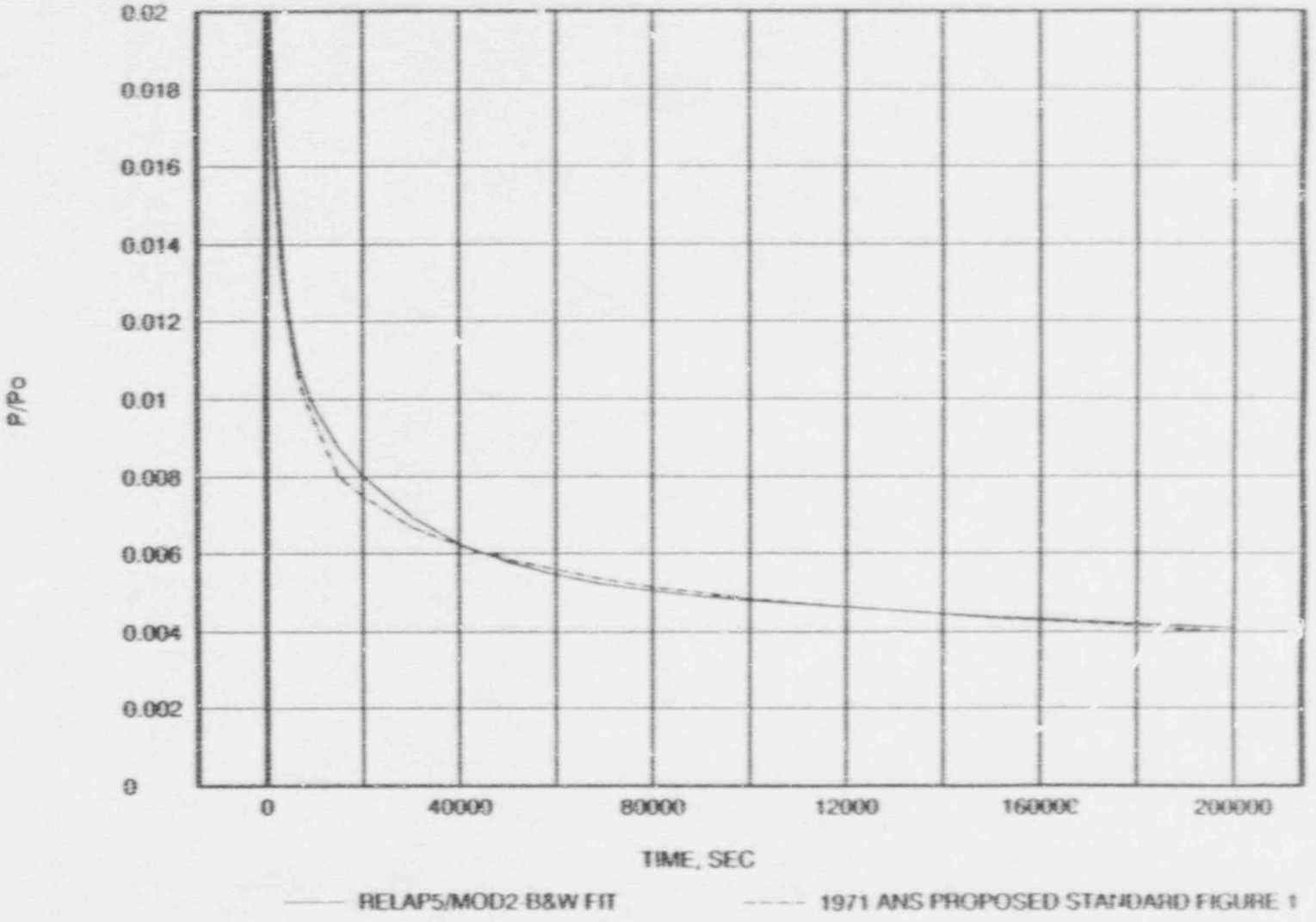


Figure 6-3

DECAY HEAT COMPARISON - RELAP5/MOD2-B&W TO 1971 ANS PROPOSED STANDARD



9. In response to Question 17 of previous NRC questions, you indicated that the pump ECCS flow rate near the reflood pressure is not sensitive to changes in back pressure. Because low pressure ECC pump performance and containment pressure vary among the RSG plants, provide additional data to show that, for the range of plants to be covered by the RSG LOCA EM, the low pressure pumped ECC will not be significantly degraded by the 50 to 100 psi RCS pressure increase associated with accumulator nitrogen injection.

Response: The response to question 17 of the first round of questions on BAW-10168 was not intended to claim that the low pressure injection systems for current PWR designs would be insensitive to a 50 to 100 psi change in back pressure. To the contrary, as can be seen in Figure 9-1, such a pressure increase could cause up to a 50 percent reduction in ECCS flow rate. Rather the intent was to say that the low pressure ECCS flow would not be sensitive to the pressure changes expected in a PWR upon accumulator gas discharge (end-of-accumulator-liquid-discharge).

The suggestion in the question that a pressure increase of 50 to 100 psi accompanies the injection of accumulator nitrogen into the reactor coolant system most likely stems from the Semiscale S-04 test series, References 9.1, 9.2, and 9.3. The results of the Semiscale tests, however, cannot be applied to full scale facilities without interpretation and allowances for the effects of scale. The addendum to this response examines the causes of the pressure responses of the Semiscale tests, addresses the relationship of the tests to full scale facilities, and demonstrates that the proper extension of the test results would predict a small pressure increase in a full scale facility. In fact, the addendum shows that the modelling of the present evaluation models is consistent with the test results.

The end of accumulator liquid discharge in a full-sized PWR should produce a small increase in system pressure that only insignificantly alters the reflooding process. That

conclusion is based on three considerations: First, the expected effect of end-of-accumulator-liquid-discharge is only a slight increase in pressure, less than 2 psi, and is functionally modelled by present evaluation techniques. Second, the injection of nitrogen gas after end-of-accumulator-liquid-discharge does not alter the system dynamics and will not cause a significant system pressure increase. Third, current low pressure ECCS employed in PWR designs supply from 15 to 40 percent excess injection, and a reduction in flow by that amount would be inconsequential to the results of a LOCA evaluation. Combining these, and using Figure 9-1, the ECCS flow drops by less than 5 percent which does not reduce the injection to below the maximum useable amount.

At end-of-accumulator-liquid-discharge, the reactor coolant system (RCS) experiences a substantial change in the thermodynamic processes that have been controlling the reflooding process. For the first 25 to 40 seconds after end-of-blowdown, the accumulators have been injecting an average of 6,000 lbm/s to 8,000 lbm/s of subcooled water into the primary system. At the end of accumulator liquid injection, that flow is stopped and the remaining ECCS systems are delivering less than a tenth of that amount (400 lbm/s to 600 lbm/s). The effect of the flows can be considered in terms of the potential for steam condensation: Prior to the end-of-accumulator-liquid-discharge, the ECCS can condense an average of 1400 lbm/s of steam -- about 8 times the core steam production capability at that time. After end-of-accumulator-liquid-discharge, the ECCS can only condense 100 lbm/s -- about 50 percent of the core steam production capability.

This sharp reduction in the condensing capability within the RCS causes a realignment of system steam and liquid flow patterns, such that the steam that can no longer be condensed is vented through the break. The RCS pressure may rise to

support the new flow pattern. Nitrogen gas injected following accumulator dry-out, is limited to about 20 percent of the gas volume (steam plus nitrogen) between the injection location and the break. That amount will not have a substantial direct pressurization impact on the RCS and has been shown to have little effect on the remaining ECCS condensation process (see addendum). Thus, the system behavior is controlled by the two-phase, single component interactions of steam and water.

Within the BWFC RSG LOCA evaluation model, specifically the REFLOD3B code, condensation on the ECCS injection is modelled in the downcomer. The efficiency of the process is 100 percent, but condensation is limited such that the resultant downcomer pressure is not lower than the containment pressure. This provides effective treatment for end-of-accumulator-liquid-discharge phenomenon. Figure 9-2 shows the effect of end-of-accumulator-liquid-discharge on the system flow distribution for a typical four-loop RSG plant. As can be seen at end-of-accumulator-liquid-discharge the flow of steam through the intact loop can no longer be completely condensed by the ECCS and about half is passed through the downcomer to the break. To support this flow, the system pressure has increased, as shown in Figure 9-3. The increase is only about 1 psi because the resistance between the downcomer and the break is relatively small. Therefore, the significant end-of-accumulator-liquid-discharge phenomena are modelled by the REFLOD3B code, and the expected results are observed in the calculations.

A system pressure increase during reflooding leads to two possible impacts on the results: (1) the specific volume of the steam in the system will decrease making the steam easier to vent and thereby increasing the core flooding rate, and (2) if the increase in pressure is substantial, the ECCS flows may be reduced sufficiently to cause a drop in the downcomer liquid level. The increase of 1 psi to 2 psi following end-

of-accumulator-liquid-discharge, as modelled by the evaluation model, results in a marginal improvement in the core reflooding rate. It is expected that the percentage change in the flooding rate will equal about one-half of the percentage change in the average steam specific volume. That would mean an increase of between 0.015 in/s to 0.025 in/s for a low pressure containment and between 0.0075 in/s and 0.0125 in/s for a high pressure containment.

The effect on downcomer level can be evaluated from Figure 9-1. The typical injection rate during reflood for a dry high pressure containment, considering only the low head ECCS, is approximately 360 lbm/s to the intact cold legs for minimum ECCS operation. A flooding rate of 1 in/s across the core inlet requires approximately 300 lbm/s. Since all recirculating steam generator plants quickly fall to flooding rates of less that 1 in/s, 60 lbm/s or approximately 16 percent of the low head injection flow is "excess" and is passed out the downcomer to the break. This percentage increases considerably if representative high head ECCS systems are included. A change of one psi results is less than a 1 percent reduction in the low head ECCS charging rate, Figure 9-1. This change is not only insignificant but also well within the margin of excess ECCS. Therefore, there is no decrease in downcomer level as a result of end-of-accumulator-liquid-discharge.

The effects of the end of accumulator liquid discharge are primarily caused by the large reduction in ECCS flow rate and not by the attendant injection of nitrogen into the reactor coolant system. The loss of condensation potential is fully modelled in the RSG LOCA evaluation model and has been shown to result in a 1 psi to 2 psi increase in system pressure. The system responses of the Semiscale tests (see the addendum) are attributable to the phenomena either treated by the evaluation model or not significant to full-scale geometries.

Therefore, the Semiscale tests responses are in agreement with the results calculated by the evaluation model. The consequences of end-of-accumulator-liquid-discharge on plant conditions and on cladding temperatures are relative relatively minor and already included in the evaluation model calculations.

References:


9.1 Crapo, Collins, and Sackett, "Experiment Data Report for Semiscale Mod-1 Tests S-04-5 and S-04-6 (Baseline ECC Tests)", TREE-NUREG-1045, EG&G Idaho Inc., January 1977.

9.2 Cussuol, "Thermal-Hydraulic Analysis of Semiscale Mod-1 Integral Blowdown-Reflood Tests (Baseline ECC Test Series)", TREE-NUREG-1077, EG&G Idaho Inc., March 1977.

9.3 Loomis, "Summary of the Semiscale Program (1965-1986)", NUREG/CR-4945, EGG-2509, EG&G Idaho Inc., July 1987.

Figure 9-1. Typical Minimum ECC Pumped Injection Flow Rate.

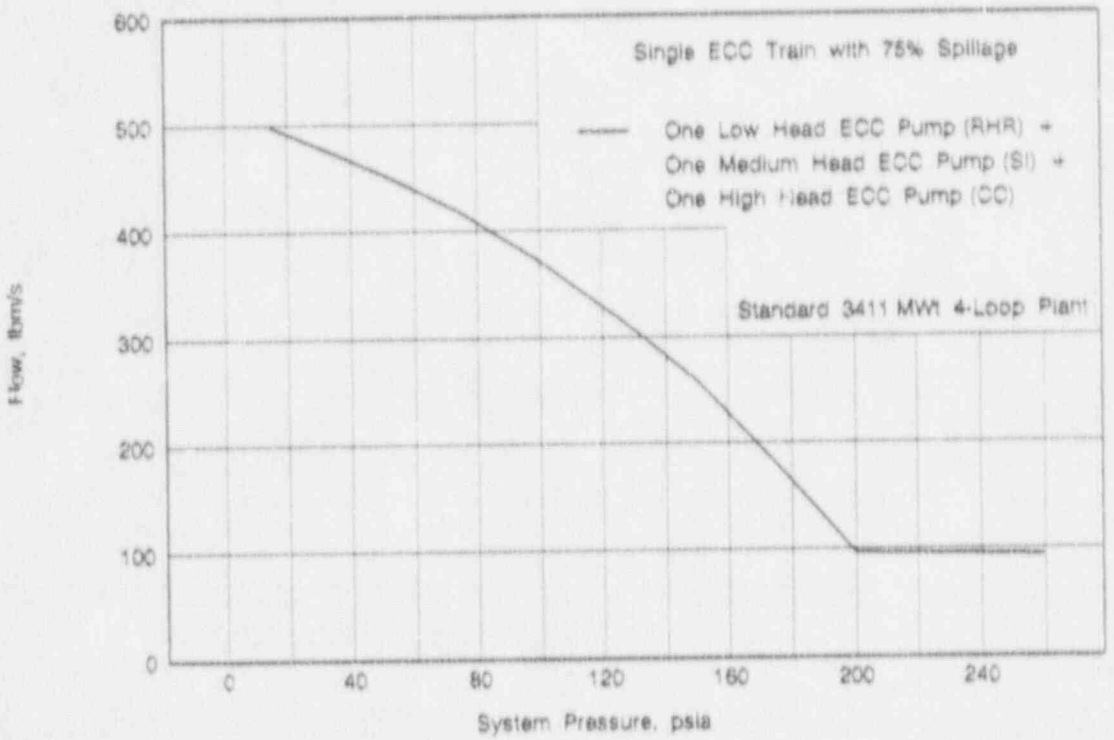



Figure 9-2. Loop Steam Flows During Reflooding.

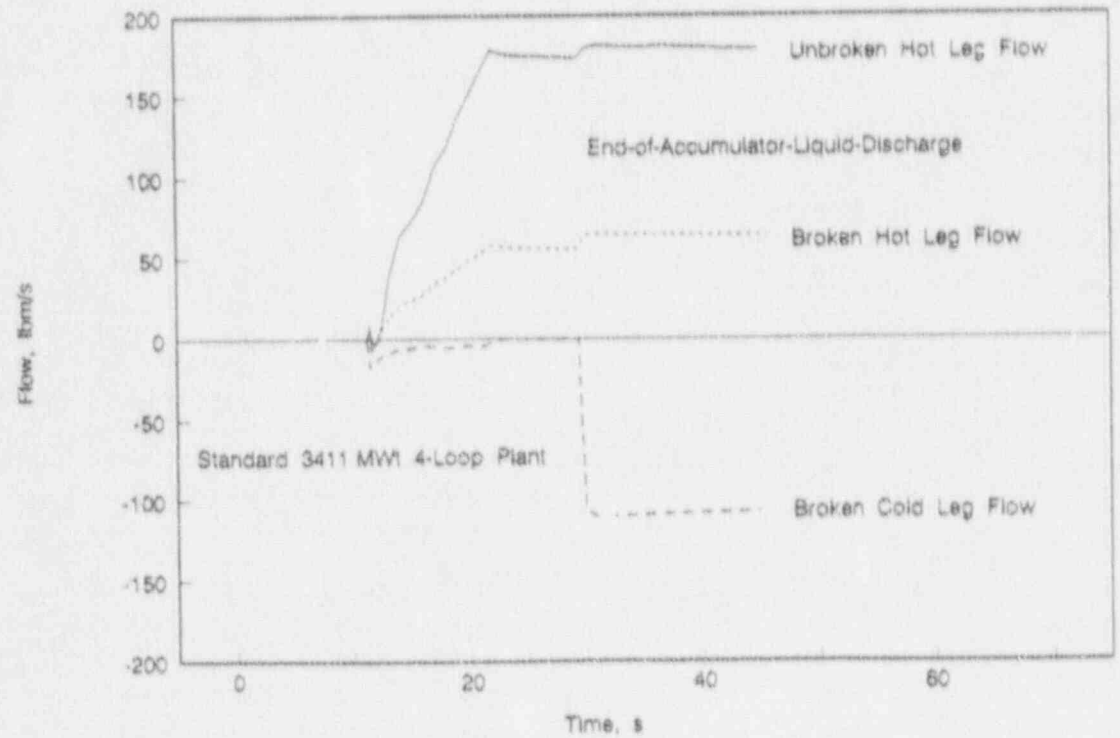
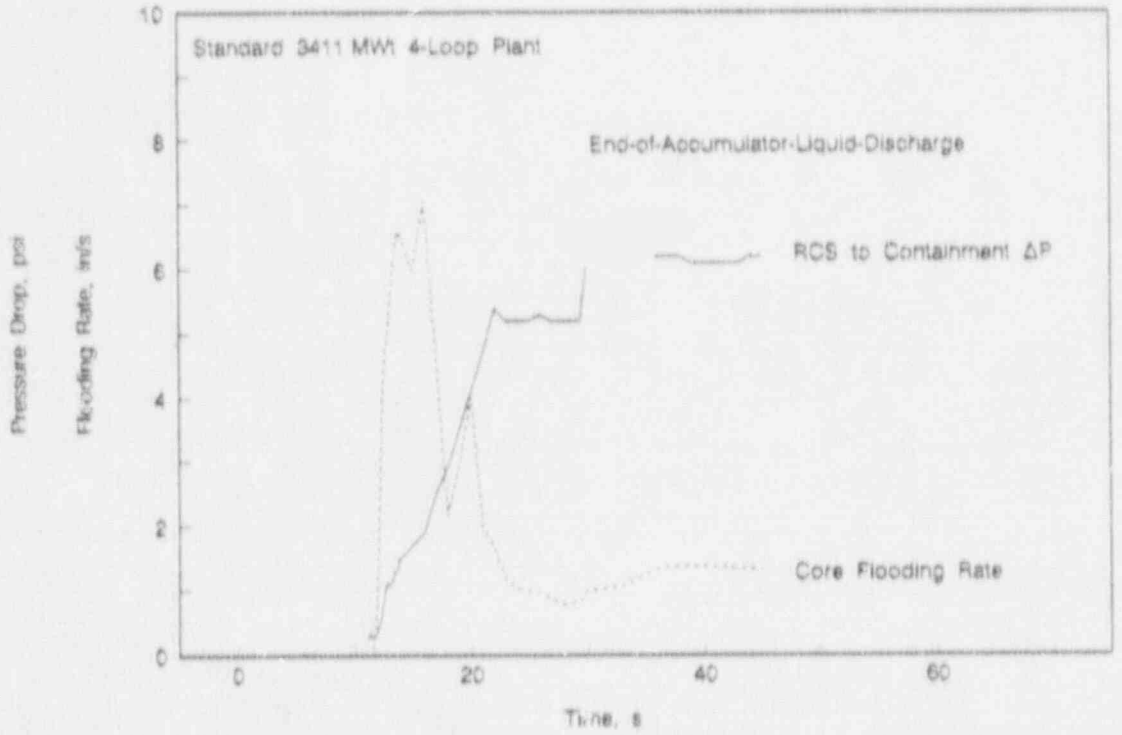


Figure 9-3. RCS To Containment Pressure Drop And Core Flooding Rate



## ADDENDUM TO THE RESPONSE TO QUESTION 9

### Introduction

The expected impact of the end of accumulator liquid discharge on the reactor coolant system during reflooding is only a small pressure increase associated with the loss of condensation potential on accumulator liquid. There is no impact expected because of the attendant injection of nitrogen from the accumulators into the system. Semiscale tests S-04-4, S-04-5, and S-04-6 did experience a range of pressure increases as high as 100 psi just after the end of accumulator liquid discharge. This discussion identifies the causes for the pressure increases in the Semiscale tests, develops the extent to which those causes and effects are appropriate to a full scale facility, and demonstrates that current LBLOCA calculational techniques, which predict a considerably smaller increase for a full scale facility, are in agreement with the Semiscale results.

### The Experimental Data

The most comprehensive tests covering end-of-accumulator-liquid-discharge have been performed in the Semiscale facility. In Reference 9.1, three tests are reported: S-04-4, S-04-5, and S-04-6. The tests experienced a range of pressure increases immediately following the end of accumulator liquid discharge. Examination of the presented data indicates that the dominant cause of the pressure increases is the removal of the condensation potential of the accumulator injection water and an oscillatory core flow pattern. The nitrogen injection following end-of-liquid-injection is a secondary contributor. S-04-4 resulted in an increase in RCS pressure of about 85 psi at the end of accumulator liquid injection; S-04-6 an increase of about 40 psi; and S-04-5 a gradual increase in pressure of about 25 psi or 30 psi. Because the scaled

accumulator flows for S-04-5 and S-04-6 were closest to those of an actual PWR, S-04-6 will be used as the basis for this discussion. The development applies nonetheless to S-04-4 and S-04-5 and explains those test results as well.

Semiscale test S-04-6 was a large break integral test in the Mod-1 configuration. The system was initialized close to PWR operating conditions and was allowed to proceed through both blowdown and reflood phases of LOCA. For this test, reflooding started at about 60 seconds. At a little after 70 seconds, the accumulator water volume in the intact loop was depleted and nitrogen gas was injected into the RCS for approximately 15 seconds. Between 70 and 80 seconds, the RCS pressure rose from 45 psia to 77 psia, Figure 9A-1. (This figure is a somewhat smoothed rendition of the measured data. For a more detailed presentation of the test, the reader is referred to Reference 9.1, 9.2, and 9.3.)

The increase in the system pressure following end-of-accumulator-liquid-discharge for Semiscale test S-04-6 was due to the loss of condensing potential caused by substantial lowering of the amount of subcooled water being injected and by an increase in core heat transfer precipitated by oscillatory core flow. The injection of nitrogen gas into the RCS that followed the loss of accumulator water injection did not substantially impact the course of the transient nor the results of the experiment.

Figure 9A-2 presents the system volumetric flows prior to end-of-accumulator-liquid-discharge, immediately after end-of-accumulator-liquid-discharge, and at an apparent settling of the system that occurs at around 100 seconds (reflood started at 60 seconds and end-of-accumulator-liquid-discharge occurred at about 70 seconds). The figure provides all major steam flow sources and sinks given as volumetric rates. The center upward flow represents the core steam flow that is split into

the two hot legs. The intact loop steam flow is superheated at the steam generator and then condensed (or at least partially condensed) at the ECCS injection site. The flow shown as "$Q_{downcomer}$" represents steam flow from downcomer boiling.

An examination of the flows shows that the system responds in an expected way. Following end-of-accumulator-liquid-discharge, condensation at the ECCS injection site is reduced to that supportable by pumped injection. Figure 9A-3 shows steam flow upstream and downstream of the ECCS injection location (The figure is a smoothed rendition of the experimental data). Although the amount of condensation is clearly reduced by the cessation of accumulator liquid flow, the process continues at an efficiency of 87 percent of the maximum supportable by the pumped ECC. As this is nearly what could be expected in the noncondensable gas-free system, the effect of the nitrogen gas on the condensation process, perhaps because of the turbulence of the flow, is marginal.

With the reduction in condensation at the ECCS injection site, the system and leak flows are altered to "rebalance" the system. Prior to end-of-accumulator-liquid-discharge, the condensation at the ECCS site, 1200 gpm, requires the absorption of about 18,000 Btu/min which is approximately twice what the pumped injection theoretical maximum is. Therefore, when the accumulator cold water flow ends, the condensation is substantially reduced. The system responds by channeling the steam flow generated in the core and the steam generators to the breaks.

If the change in venting pattern were the only occurrence at this time in the S-04-6 test, the increase in pressure would only be the 25 psi to 30 psi necessary to support the larger broken loop flows. However, the core steaming rate increases substantially, leading to the 30 psi to 40 psi pressure

An examination of the three Semiscale tests discussed in Reference 9.1 shows a relationship between the core flow oscillations and the magnitude of the system pressure increases at the time of accumulator dryout. S-04-5 had no appreciable increase in the magnitude of core flow surges at the time of accumulator dryout; there was no jump in system pressure during the test, only a gradual rise to the new quasi-steady state pressure associated with no accumulator condensation. S-04-6 showed somewhat more than a doubling of the magnitude of the flow surges and increased the system pressure by 30 psi to 35 psi. S-04-4 also increased the magnitude of the surges by somewhat more than twice but the surges in S-04-4 and the system pressure were initially higher than those in S-04-6 such that the magnitude of the surges in S-04-4 was three times that in S-04-6 with a attendant system pressure increase of 80 psi to 85 psi.

The cause of the increase in the magnitude of core flow oscillations is not readily discernable. However, there is a possible correlation between the magnitude of the flow surges and the timing of end-of-accumulator-liquid-discharge. S-04-4, which had the largest flow surges, started reflood earlier than either of the other tests (about 45 seconds), and end-of-accumulator-liquid-discharge occurred only 8 seconds after the start of reflooding. S-04-6, with flow surges of half those of S-04-4, started reflood at 60 seconds, with end-of-accumulator-liquid-discharge occurring 11 seconds later at 71 seconds. S-04-5, containing essentially no core flow surges, started reflooding at 60 seconds, but end-of-accumulator-liquid-discharge occurred 19 seconds later, at 79 seconds. Thus, the degree to which the reflooding transient has established itself may be of importance in determining the occurrence of increased core flow surging at end-of-accumulator-liquid-discharge.

One additional phenomenon occured in S-04-6 to impact system pressure. For this test, the back of the downcomer shield filled with water resulting in sufficient increase in heat transfer from the downcomer thick metal wall to boil nearly all downcomer liquid. The occurrence started at around 80 seconds and by 130 seconds had reduced the downcomer liquid inventory to about one-third of full. The steam formed during the boiloff is responsible for the second increase in system pressure observed in S-04-6. By 300 seconds the level has only risen to about one half normal.

A summary of the experimental investigations of end-of-accumulator-liquid-discharge indicates that the loss of condensation potential, due to reduced injection of subcooled water, controls the system behavior following end-of-accumulator-liquid-discharge. Due to the lowering of condensation potential, some flow redistribution will occur attended by a possible system pressure increase. The injection of nitrogen gas into the RCS does not have consequence on the system, as condensation on the available ECCS continues at a high level in the presence of nitrogen gas. Therefore, it is necessary to model the condensation process and its effects on the RCS flow pattern in the reflooding simulation, but the modelling need not include the effects of noncondensable gases.

The RSG Evaluation Model Results

Within the BWFC RSG LOCA evaluation model, specifically the REFLOD3B code, condensation on the ECCS injection is modelled in the downcomer. The efficiency of the process is 100 percent, but condensation is limited such that the resultant downcomer pressure is not lower than the containment pressure. This provides an effective treatment for end-of-accumulator-liquid-discharge phenomenon. Figure 9-2 shows the effect of end-of-accumulator-liquid-discharge on the system flow

distribution for a typical four-loop RSG plant. As can be seen at end-of-accumulator-liquid-discharge, the flow of steam through the intact loop can no longer be completely condensed by the ECCS, and about half is passed through the downcomer to the break. To support this flow, the system pressure has increased, as shown in Figure 9-3. The increase is only about 1 psi because the resistance between the downcomer and the break is relatively small. Therefore, the significant end-of-accumulator-liquid-discharge phenomena are modelled by the REFLOD3B code, and the expected results are observed in the calculations.

Deviation in PWR Predicted Effects and Semiscale Results

The deviation between the results observed in Semiscale, increases of 40 psi to 80 psia, and those predicted for an actual PWR, 1 to 2 psi, occur because of scaling and other compromises in the Semiscale facility. The facility does not scale well in vessel-to-break resistance. For S-04-6, the pressure drop across the broken hot leg during the first part of reflood was about 10 psi. The same path experiences only a 5 psi drop in a simulation of a full-size PWR. This difference could be caused by either a relatively high flow resistance in Semiscale or a relatively high steaming rate. In either case, it is more difficult for the Semiscale facility to vent steam produced during reflooding through the broken loop hot leg than it is for a full-size PWR. A similar and perhaps more severe situation exists for the cold leg. Here, Semiscale requires the expulsion of excess liquid out a small, long, and convoluted pipe to the containment while, for full-size PWRs, the excess liquid pours out a large nozzle or perhaps down a short run of large pipe.

Full-size PWRs are less prone to reflood flow oscillations than are scaled facilities. The plants are more tightly coupled and have lower loop resistances that tend to preclude

flow oscillations. Furthermore, testing at different scales has demonstrated the general trend that, as scale increases, the plant is less susceptible to oscillatory behavior. In Semiscale, core flow oscillations may have had exaggerated consequences and may even have been amplified because the core heated region is only 5.5 feet long yet runs at scaled power. Thus, the power density per foot of core is higher in Semiscale by more than a factor of two and that may amplify system responses involving energy transfer from the core.

Conclusions

Although Semiscale tests S-04-4 and S-04-6 responded to the end-of-accumulator-liquid-discharge with large system pressure increases, these increases were not caused by the injection of nitrogen gas into the RCS and are not representative of the response of full-size PWRs. Rather, the increases in pressure were caused by a severe reduction in the condensing potential of the ECCS due to a sixteen fold reduction in ECCS flow rate combined with core flow oscillations (causing increased core heat transfer and steam generation). The loss of condensation potential is fully modelled in the RSG LOCA evaluation model and has been shown to result in a 1 psi to 2 psi increase in system pressure. The core flow oscillations do not occur in all Semiscale tests (S-04-5 did not show these), and scaling tests (UPTF and CCFT) have shown that they are unlikely to occur in full-size PWRs. System responses of the Semiscale tests are attributable to phenomena either treated by the evaluation model or not appropriate for inclusion in analyses of full-scale facilities. The responses calculated by the evaluation model for the plants are in agreement with the results of the Semiscale tests. Therefore, the evaluation model already accounts for the system effects and interaction accompanying end-of-accumulator-liquid-discharge properly, and nitrogen gas need not be considered directly by the evaluation model. The consequences of end-of-accumulator-liquid-

discharge on plant calculations of cladding temperature are relatively minor and already included in the calculations.

Figure 9A-1. RCS To Containment Pressure Differential For SEMISCALE Test S-04-6.

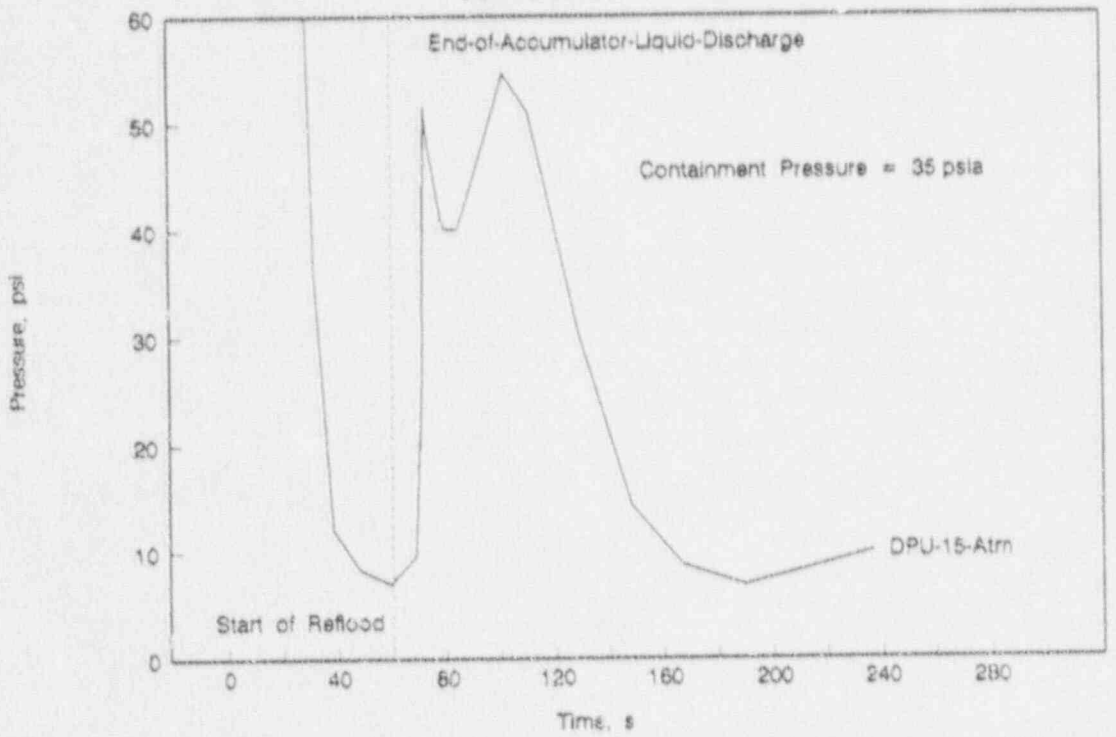
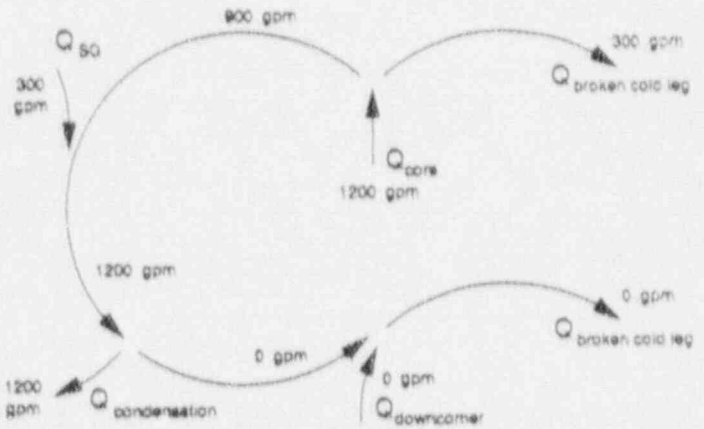


Figure 9A-2. Flow Distributions For SEMISCALE Test S-04-6.

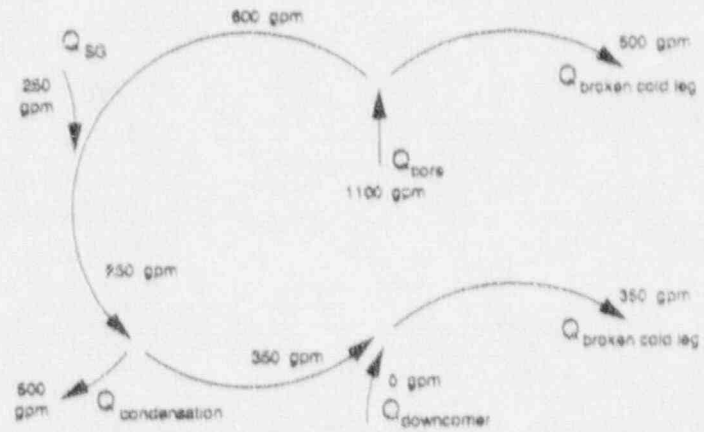
Prior To End-of-Accumulator-Liquid-Discharge

Time: 70- seconds
Pressure: 45 psia
Steam Density: 0.106 lbm/ft³




Just After End-of-Accumulator-Liquid-Discharge

Time: 70+ seconds
Pressure: 77 psia
Steam Density: 0.176 lbm/ft³




At 100 seconds;
30 seconds After Injection

Time: 100 seconds
Pressure: 77 psia
Steam Density: 0.176 lbm/ft³

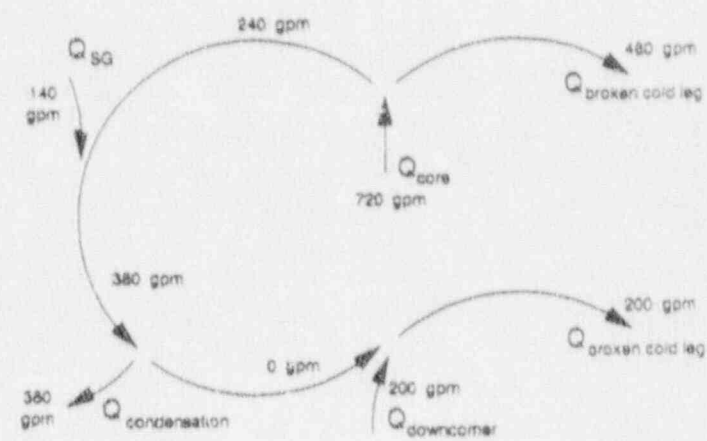


Figure 9A-3. Cold Leg Flows Upstream And Downstream Of The ECC Injection Location For SEMISCALE Test S-04-6.

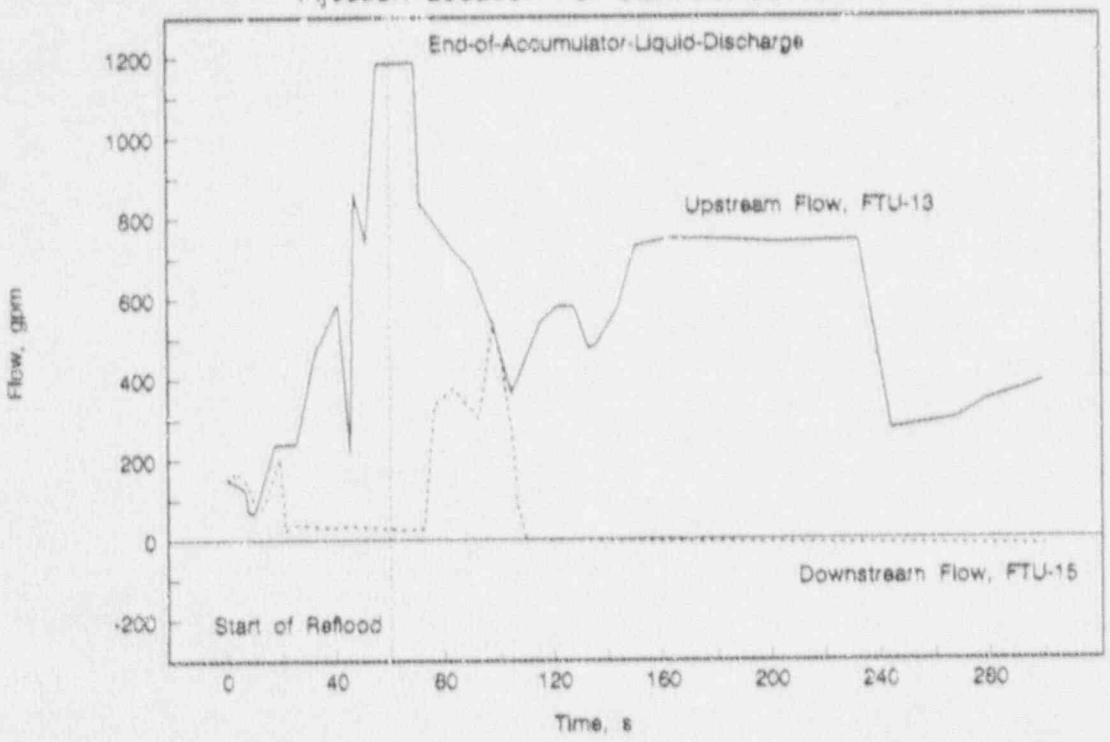



Figure 9a-4. System Mass Flows For SEMISCALE Test S-04-6.

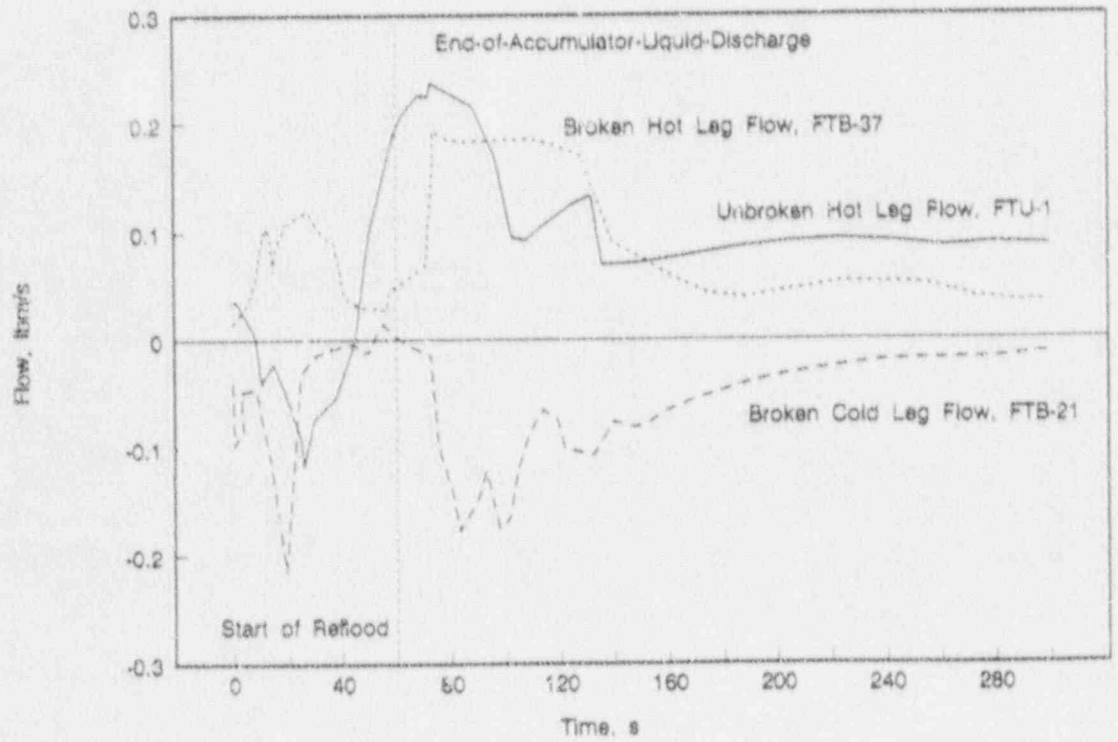
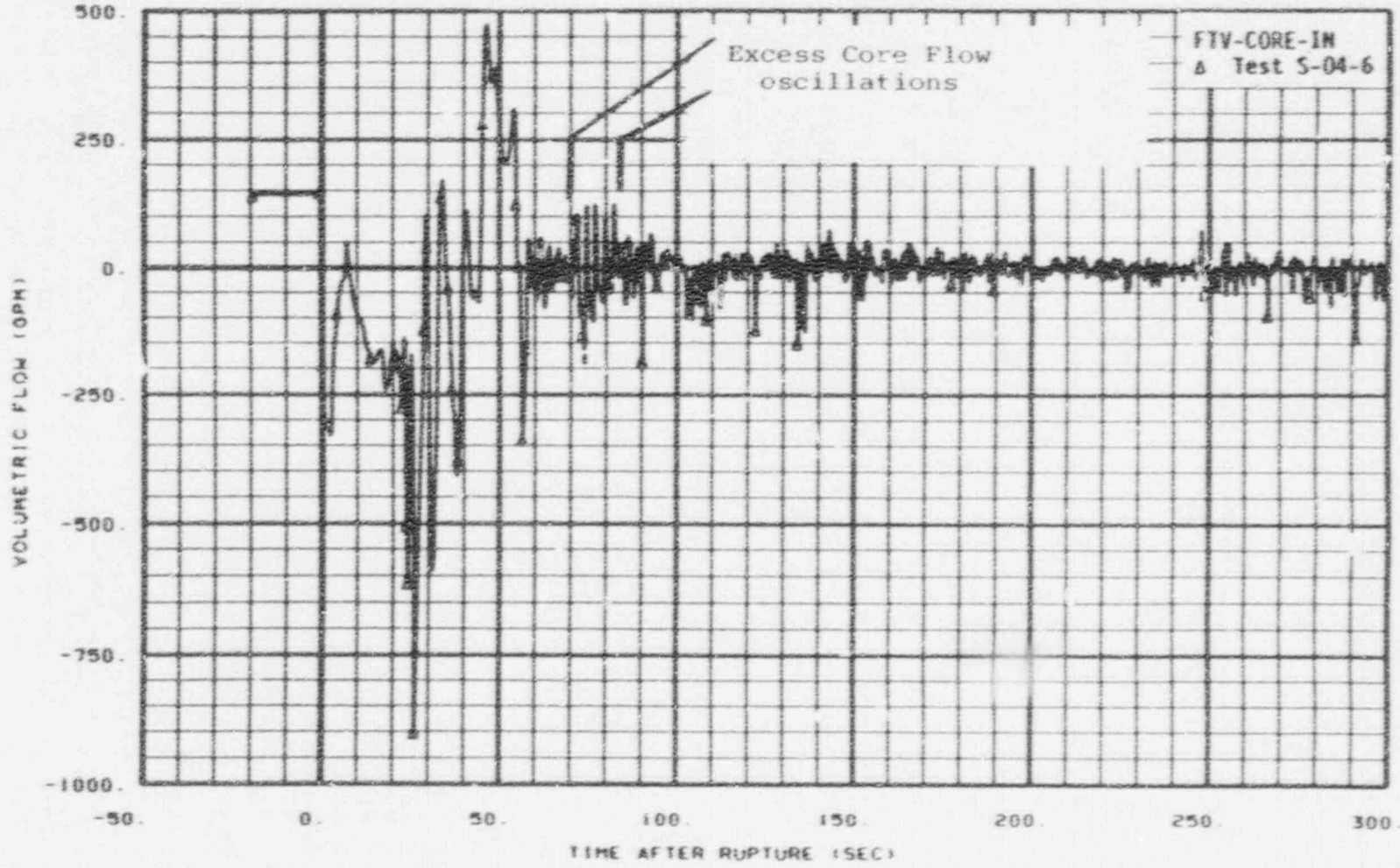


Figure 9A-4. Core Flow

SEMISCALE Test S-04-6



10. The response to Question 28 stated that no time step sensitivity studies were performed for the BEACH code. The justification given referred to the RELAP5/MOD2-B&W blowdown code time step study. However, because RELAP5/MOD2-B&W calculates the blowdown portion of the transient, the results of that study are not directly applicable to the BEACH code, which performs reflood heatup calculation. Provide additional information to justify why the study for the system blowdown code would be applicable to the BEACH reflood code or a time step study specific to the BEACH code should be provided.

Response: In the response to Question 28 related to the EM Topical Report BAW-10168P, it was stated that the maximum BEACH time step of 0.01 seconds with the code internal time step control will produce converged results. In order to provide independent verification of this, a sensitivity study on time steps has been performed specifically for the BEACH code. The case was a calculation of the Mark-BW fuel assembly for McGuire/Catawba conditions NSS with the core power peaked at the 8-foot elevation and a total peaking factor of 2.32. The noding arrangement was the standard one used for BEACH and is shown in Figure 10-1. Node 14 is at the location of peak power. A base case was executed using the recommended initial time step setting in conjunction with the RELAP5/MOD2 time step control procedure. A sensitivity case, which reduced the actual time step, was run for comparison.

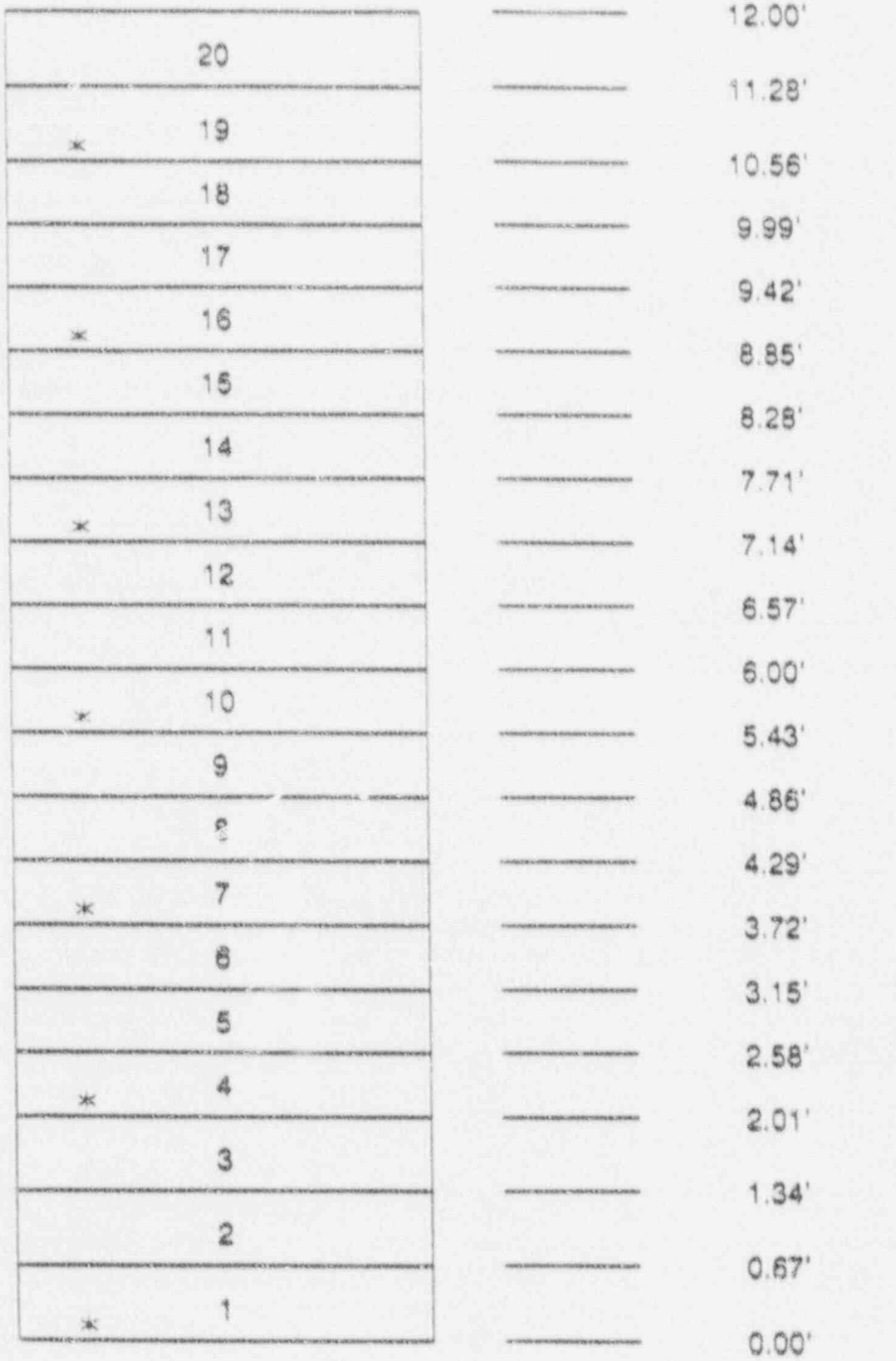
Examination of the base case showed that the time step control feature caused the time step to run back such that the average step size used was 0.005 seconds. To insure that the sensitivity study measured the capability of the time step control feature to converge, the base case was rerun using a maximum time step size of 0.0025 seconds. The table below

provides values from the two cases that are typically used to characterize a BEACH run.

| | Dt = 0.01 sec (ave. runback to 0.005) | Dt = 0.0025 sec |
|---|---|---|
| Rupture node | 14 | 14 |
| Rupture time, sec | 41.995 | 42.2075 |
| PCT node | 12 | 12 |
| PCT, F | 1899.3 | 1897.8 |
| PCT time, sec | 184 | 194 |

The peak cladding temperatures and the corresponding heat transfer coefficients for nodes 12 and 14 are shown in Figures 10-2 through 10-5. From these figures and the values given in the above table it is observed that the base run was in fact converged relative to temporal advancement. These results confirm the conclusion made in response to question 28 that the maximum input time step of 0.01 seconds with the BEACH internal time step control is sufficient to produce converged results.

FIGURE 10-1 BEACH AND FRAP-T6-B&W NODING DIAGRAM FOR MARK-BW FUEL ASSEMBLY




* GRID NODE

FIGURE 10-2. BEACH TIME STEP STUDY - 8.0 FOOT LOCA LIMIT CASE.
CLADDING TEMPERATURE AT RUPTURE LOCATION - NODE 14

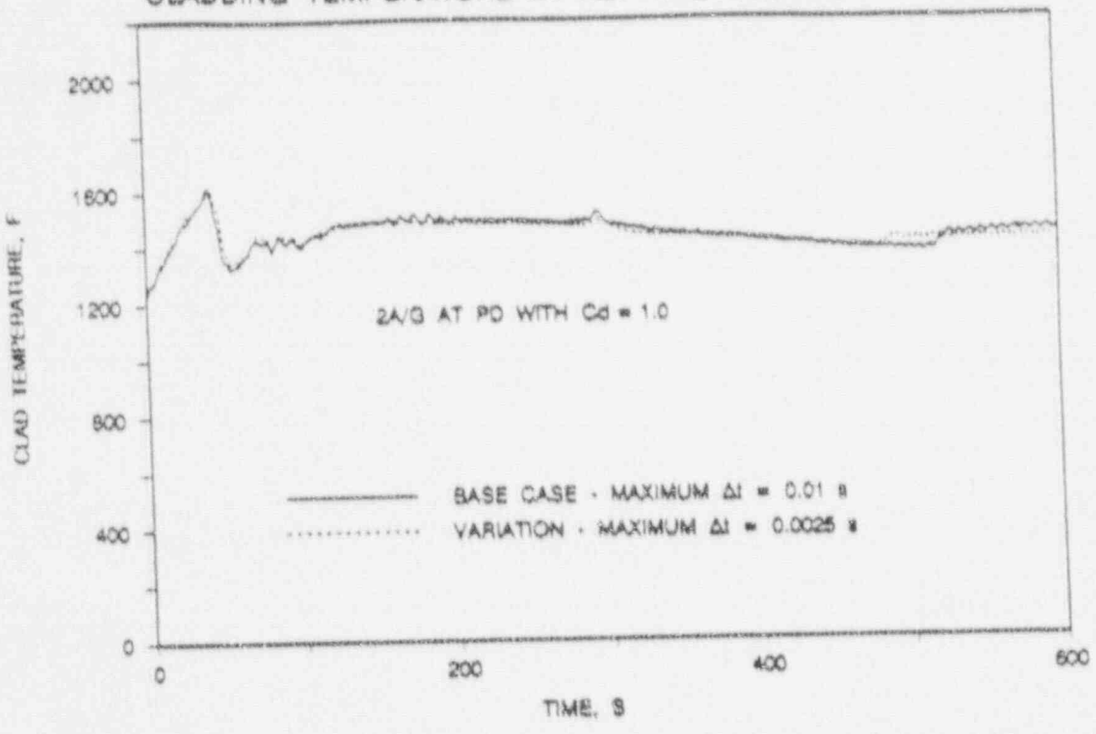



FIGURE 10-3. BEACH TIME STEP STUDY - 8.0 FOOT LOCA LIMIT CASE.
CLADDING TEMPERATURE AT PCT LOCATION - NODE 12

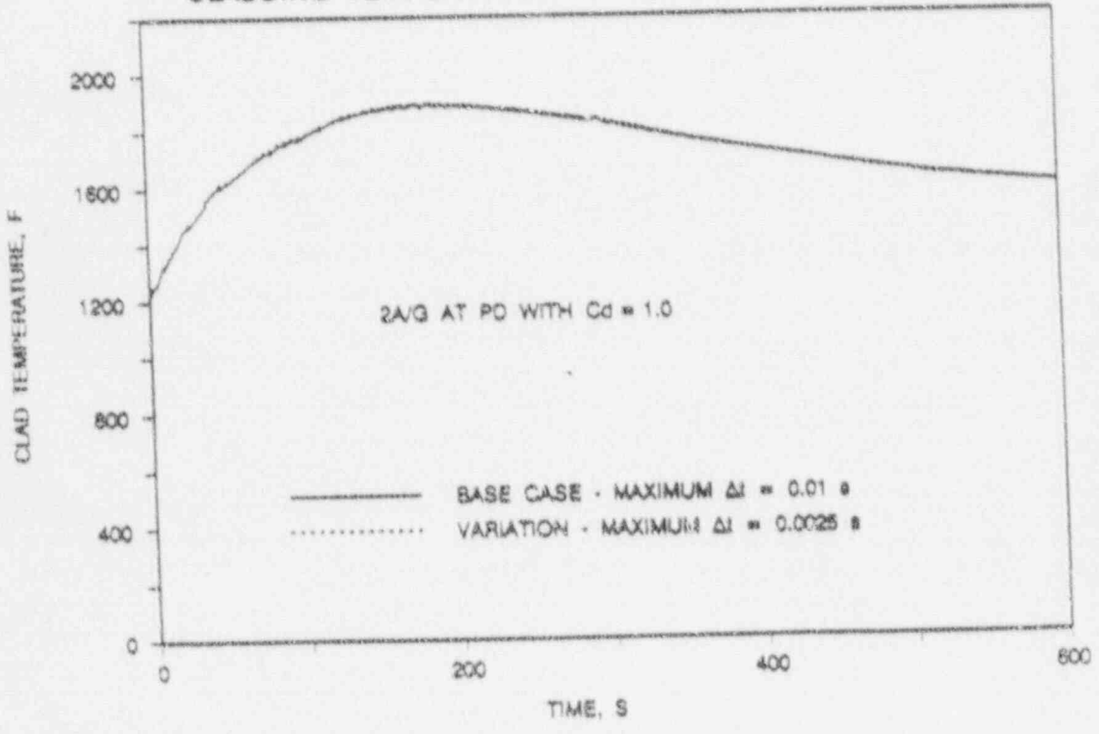


FIGURE 10-4. BEACH TIME STEP STUDY - 8.0 FOOT LOCA LIMIT CASE.
HTC AT RUPTURE LOCATION - NODE 14

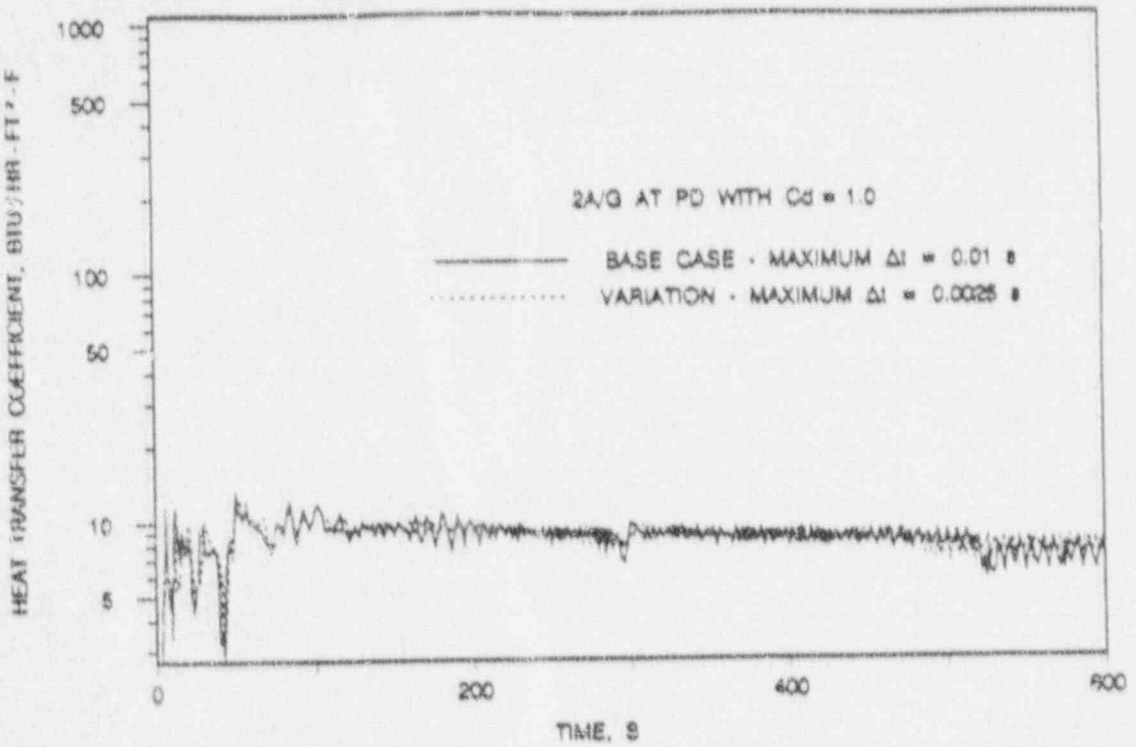
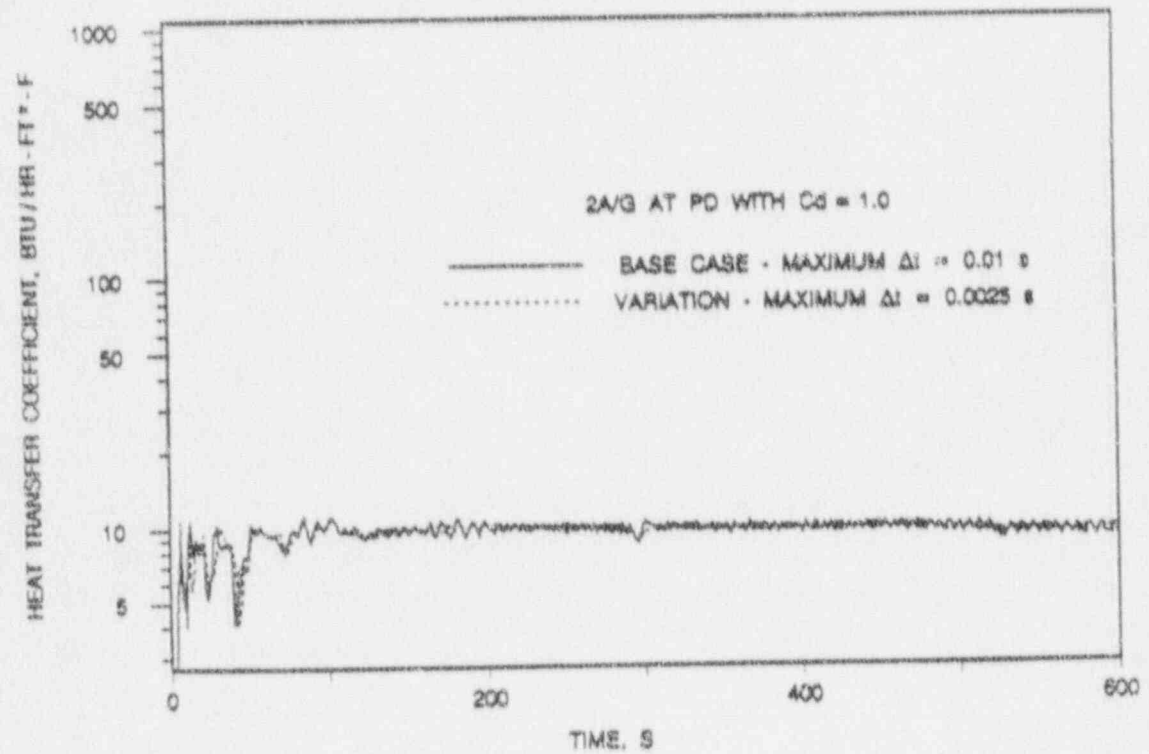



FIGURE 10-5. BEACH TIME STEP STUDY - 8.0 FOOT LOCA LIMIT CASE.
HTC AT PCT LOCATION - NODE 12



11. The response to Question 50 regarding noding and time step sensitivity studies was a qualitative discussion as to why the RSG LBLOCA sensitivity studies and the B&W plant studies are applicable to the RSG SBLOCA model. Provide a more quantitative justification of the statements made in response to Question 50. In addition, the nodalizations used for the SBLOCA benchmarks are more detailed than the SBLOCA EM model in some areas such as the steam generator, cold leg, downcomer, and pump suction upflow, and may not be appropriate for justification of the EM model nodalization. Clarify how the SBLOCA EM model nodalization was verified.

Response: Questions 7, 11, 12, and 16 of this set all refer to aspects of the SBLOCA model not benchmarked by the studies presented in the evaluation model and its supporting topicals or in responses to previous questions. In particular, the benchmark of Semiscale test S-LH-1, presented in the response to Question 5 of the second round of questions on the RELAP5/MOD2-B&W topical, employed a high degree of noding detail in the loops and steam generators and did not utilize dual flow paths for the loop-to-reactor vessel connections. To resolve this situation, the S-LH-1 benchmark was repeated using the evaluation model noding arrangement. This response provides the overall results of the new and previous benchmarks with particular attention to the specific issues raised in Question 11. The responses to Questions 7, 12, and 16 make reference to this benchmark as required to address the individual issues raised. The results confirm that the earlier benchmarks were adequate and that the qualitative arguments put forward to control the modelling result in an accurate benchmark of the experiment. Therefore, the techniques proposed in the SBLOCA evaluation model are acceptable.

Test Facility

The S-LH-1 test was conducted using the Semiscale MOD-2C system configuration shown in Figure 11-1. It consists of a

pressure vessel with simulated reactor internals and an external downcomer. The intact loop simulated three unaffected loops of a typical Westinghouse 4-loop PWR, while the broken loop simulated an affected loop in which the break is assumed to occur. The intact loop steam generator contained six inverted U-tubes, and the broken loop steam generator contained two inverted U-tubes. The reactor core simulator was a 5x5 bundle with electrically heated rods (23 rods were powered during the test). The upper head region contained a simulated control rod guide tube and two simulated support columns. The bypass line, which extended from the external downcomer to the upper head, was used to simulate the core bypass flow. A pressurizer was connected by a surge line to the intact loop hot leg. Both loops had primary coolant circulation pumps. Emergency core coolant from an accumulator and pumped injection system (LPI and HPI) were routed to the loop cold legs. An open loop secondary coolant system was used to control the secondary side pressure with feedwater and steam control valves.

Model Description

The Semiscale MOD-2C RELAP5 base model was originally developed by EG&G for the post-test analysis of experiments S-LH-1 and S-LH-2 (NUREG/CR-4438). The model, Figure 11-2, consists of 181 hydrodynamic volumes, 172 junctions, and 256 heat structures. Volume and junction parameters are calculated with nonequilibrium and nonhomogeneous models. Steam generator secondaries, ECC injection, system environmental heat losses, and both vessel and piping external heaters are modelled in detail. The core axial power profile is modelled with twelve stacked heat structures over six two-foot long axial fluid volumes. The upper head region is nodalized to allow for junctions to be connected at the elevations of the top of the control rod guide tube, core

bypass line and support columns, and at the elevation of the holes in the guide tube below the upper core support plate.

The original EG&G model was updated to completely account for available information on system initial conditions. The model was also adjusted for the most significant aspects of the BWFC SBLOCA evaluation model and for certain anomalies caused by the scale of the facility. These changes, fully discussed in the response to Question 5 of the second round of questions on the RELAP5/MOD2-B&W topical, resulted in the benchmark comparison presented earlier in the response to Question 5 and reproduced in this response as the "REVISED" prediction.

Although the "REVISED" model contained the most significant BWFC SBLOCA evaluation model features, it differed primarily by containing more nodes in selected regions. To demonstrate that these details do not impact the benchmark results significantly, the modelling was further altered to correspond as closely as practical to the EM. The required changes are discussed below, and the resulting EM nodalization is presented in Figure 11-2a.

The downcomer, Volume Component 110 is reduced from 10 to 5 volumes. Volume 1 extends from the top of the original Volume Component 110 to the elevation corresponding to the top of the core. Volumes 2, 3, and 4 extend from the top of the core to the bottom of the active core, and each volume is four feet long, similar to the baffle region nodalization in a plant model. The bottom volume (# 5) has the same volume length as the lower plenum volume (# 140). The upper downcomer volumes, 101 and 102, remain unchanged.

The reactor core, Volume Component 150, is increased from 6 to 20 volumes, and the core heat structure is increased from 12 to 20 stacked heat structures with axial length equal to the corresponding fluid volume lengths. The core is divided into

20 unequal axial lengths such that each grid is located at the node boundary, and the axial power distribution is modified accordingly.

The upper plenum, Volume 163, is divided into two equal-size vertical volumes in order to implement the dual flow path technology for the vessel outlet region. The physical flow channel is divided into upper and lower flow paths, each with one-half of the flow channel area, and each independent of the other in flow conditions. This arrangement produces a flow characteristic with steam-dominated flow in the upper path and liquid-dominated flow in the lower path during the reflux boiling phase of the transient. It requires that one flow path each from these two vertical volumes (163 and 165) must be connected to each hot leg to produce the independent flow conditions. Dual flow paths in the reactor vessel inlet region would also be used for plant modelling, however, because of the intact loop ECCS bypass in Semiscale the technique is impractical for this benchmark.

The hot leg volume component is reduced from 6 to 4 volumes. The volume connecting the hot leg to the steam generator inlet plenum is changed from a vertical to a horizontal orientation with an inclination angle of less than 15 degrees. This will provide horizontal stratification and promote draining from the steam generator inlet plenum to the hot leg. The elevation change between the pressure vessel outlet and the steam generator inlet is conserved.

The steam generator, tubesheet, and plenum volumes are combined into one volume on both inlet and outlet sides. The steam generator tube volume is increased from 8 to 16 volumes, eight volumes each on the upflow and downflow sides. The tube heat structures are also increased from 8 to 16, correspondingly.

The pump suction piping is reduced from 9 volumes (6 for the downflow and 3 for the upflow section) to 5 volumes (3 for the downflow and 2 for the upflow section). The height of the bottom volumes of both the downflow and upflow sections remains unchanged from the "REVISED" model.

The pressurizer surgeline noding is reduced from 3 volumes to 1 volume. The elevation change from the pressurizer outlet to the hot leg is conserved.

Since the secondary system pressure responses from the experiment, presented in Table 11-2, were used as boundary conditions in the benchmark calculations, no changes were made to the secondary system nodalization.

Results of SBLOCA EM Case

The steady-state initial conditions for the benchmark are presented along with the test conditions in Table 11-1. To demonstrate model stability relative to time advancement, the EM model was run with time advancement equivalent to that of the "REVISED" model and with step advancement reduced by one half. Table 11-2 and Figures 11-3 through 11-18 present the results for the larger time step case in contrast to those of the original "REVISED" model. Figures 11-19 through 11-22 show the results for the time step study. As can be observed, the alteration of time advancement does not change the results.

The results of the EM benchmark are summarized in Table 11-2 and shown in Figures 11-3 through 11-18 labeled as "SBLOCA-EM." The table and figures also present measured data and the results of the previous benchmark. The general agreement between the two benchmarks is excellent indicating that the noding for the EM model provides as effective a solution as

does the "REVISED" model noding. The SBLOCA EM model calculates primary system pressure slightly lower than that of the "REVISED" model during the early phase of the transient. This is in slightly better agreement with the test but should not be viewed as significant. As a result of the faster depressurization, the pressurizer low pressure trip occurred approximately 2 seconds earlier than for the "REVISED" case, and subsequent events, such as reactor scram, pump coastdown, ECC injection, and main feedwater and steam line isolation are also advanced (see Table 11-2).

The change of volume orientation from vertical to horizontal for the steam generator inlet volume promotes faster draining of the steam generator tubes on the upflow side in the EM modelling, Figures 11-5 and 11-7. The effect of the pump suction noding changes on the loop seal clearing and overall system response is negligible as shown in Figures 11-9 through 11-12. The lower liquid level in the broken loop pump suction downflow leg for the EM modelling between 120 to 170 seconds may not be in better agreement with the data, but does constitute a more internally consistent model as there is no longer a large disparity between the intact and broken loop cold leg liquid levels over this time period.

The effect of the noding changes in the downcomer and core is an increase in the vessel liquid levels at and after the occurrence of minimum inventory at about 500 seconds, Figures 11-13 and 11-18. Although both models predict vessel liquid levels within the expected accuracy of the data, the vessel liquid level for the EM model is a better match to the test data than the "REVISED" case. Figures 11-5 through 11-8 show that the more detailed nodalization of the steam generator tubes in the SBLOCA EM case has no significant impact on reflux boiling behavior.

In addition to the EM modelling bench mark of the S-LH-1 test, new calculations of the cladding temperature response have been performed. These calculations, Figures 11-19 and 11-20, incorporate two changes from the previous submittals that cause cladding temperature calculations to trend in opposite directions: the effects of the EM modelling of the system, which increases core liquid inventory and reduces the cladding temperatures, and the effect of a revised specification of the core liquid inventory that can be used in the EM prediction of cladding temperature (see response to question 13 of this set) which reduces the core liquid inventory available for the cladding temperature calculation and increases the cladding temperatures. Although Figures 11-19 and 11-20 do properly characterize the application of the current BWFC SBLOCA EM to the S-LH-1 test, it would be inaccurate to equate the change in cladding temperature predictions with the noding and modelling changes incorporated in the RELAP5 model to achieve the EM prediction. Had the definition of liquid available for core cooling not been altered in the heatup calculation, it is expected that the cladding temperature would have fallen somewhat and shown better agreement with the data than in the previous benchmark (i.e., no substantial conservative bias would be evident in the modelling).

The conservatism in the temperatures shown results from the consistently low core liquid inventory prediction of RELAP5 and can be directly related to the core phase separation and drag models used during the core boildown period of the simulation. If the RELAP5 phase separation model were to be replaced by a more accurate and realistic model, the core liquid inventory would be increased during the boildown period and much more reasonable cladding temperatures predicted. Because the RELAP5 model causes liquid that belongs in the core to be pushed into the upper plenum, the alteration of the core phase separation modelling would not alter the vessel liquid inventory nor other aspects of the current benchmark.

Time Step Study

The results of the time step study are presented in Figures 11-21 through 11-24 for leak flow, primary system pressure, normalized inventory, and vessel collapsed liquid level. The plots show essentially no difference in results between the two time steps used. The most noticeable deviations occur in the reactor vessel liquid level just after minimum core inventory. Here the larger time step tended to produce two or three rapid fills of the vessel followed by liquid expulsion. The refill process at this time is created by accumulator flow and is more erratic for Semiscale than it would be for a full scale facility. If behavior of this nature is noted within plant evaluations, a time step study will be conducted to assure that liquid inventories are not significantly reduced by the use of a smaller time advancement increment.

Concerns Specifically Mentioned in Question 11

The following paragraphs address each of the specific issues raised in the question.

The quantitative justification for the qualitative rationale of the BWFC SBLOCA noding selection is addressed directly by the EM benchmark as a whole. The benchmark of the test with the EM level of modelling detail produces a successful comparison to the data. Furthermore, the comparison to an alternate noding arrangement, generally of more detail, shows very little change in predicted results. Therefore, it is concluded that the qualitative reasoning used to develop the EM noding scheme embodies the requirements for spacial differentiation during small break LOCA evaluations.

The level of noding detail in the areas of the steam generator, cold legs, downcomer, and pump suction upflow has

been altered with no appreciable change in event sequences or results. Therefore, either the added detail or the more precisely allocated lower detail of the EM model provides an appropriate solution.

## Conclusions

The BWFC SBLOCA EM modelling calculated the major events of the Semiscale S-LH-1 transient, such as two-phase natural circulation, reflux boiling and liquid holdup, pump suction loop seal clearing, core liquid level depression, accumulator injection, and core recovery in the proper sequence. Both the SBLOCA EM and "REVISED" cases calculated the overall system responses in reasonable agreement with the experimental data. The proposed SBLOCA EM nodalization and the qualitative arguments that support it are sufficient to predict SBLOCA transients in a Westinghouse PWR.

Table 11-1. Comparison of Calculated and Measured Initial Conditions for S-LH-1.

| Parameters | RELAP5 | Measured |
|---|---|---|
| Pressurizer Pressure, psia | 2244.0 | 2244.0 |
| Core Power, KW | 2015.0 | 2015.0 |
| Pressurizer Liquid Level, in | 155.6 | 155.6 |
| Cold Leg Fluid Temperature, F | | |
| Intact Loop | 549.9 | 552.2 |
| Broken Loop | 554.9 | 556.7 |
| Primary System Flow Rate, lbm/s | | |
| Intact Loop | 15.7 | 15.6 |
| Broken Loop | 5.2 | 5.2 |
| Core Bypass Flow (% of total core flow) | 1.0 | 1.0 |
| SG Secondary Side Pressure, psia | | |
| Intact Loop | 860.0 | 859.7 |
| Broken Loop | 858.0 | 857.2 |
| Temperature Increase Across Core, F | 67.5 | 67.4 |
| SG Secondary Side Mass, lbm | | |
| Intact Loop | 421.0 | 374.8 |
| Broken Loop | 94.8 | 78.0 |

Table 11-2. Comparison of Calculated and Measured Sequence of Events for Semiscale Test S-LH-1.

| Events | Time, Seconds | | |
|---|---|---|---|
| | | RELAP5 PREDICTIONS | |
| | Measured | REVISED | SBLOCA EM |
| Break Opened | 0.5 | 0.0 | 0.0 |
| Pressurizer at 1827.5 psia (SIS) | 14.67 | 17.35 | 15.20 |
| Reactor Scram | 19.57 | 22.30 | 20.13 |
| Pump Coastdown Initiated | | | |
| Intact Loop | 21.35 | 24.05 | 21.90 |
| Broken Loop | 20.76 | 23.45 | 21.30 |
| Feedwater Off | | | |
| Intact Loop | 19.67 | 22.40 | 20.23 |
| Broken Loop | 19.00 | 21.70 | 19.55 |
| MSIV Closure | | | |
| Intact Loop | 22.00 | 24.70 | 22.55 |
| Broken Loop | 22.00 | 24.70 | 22.55 |
| HPIS Initiated | | | |
| Intact Loop | 41.60 | 44.40 | 42.20 |
| Broken Loop | 40.98 | 44.40 | 42.20 |
| Presssurizer Emptied | 33.90 | 40.00 | 40.00 |
| Intact Loop Seal Cleared | 171.4 | 175.0 | 175.0 |
| Broken Loop Seal Cleared | 262.3 | 605.0 | 560.0 |
| Accumulator Injection | | | |
| Intact Loop | 503.8 | 490.0 | 495.0 |
| Broken Loop | 501.4 | 490.0 | 495.0 |

Table 1 -2 Continued

Secondary Side Pressure Used in the RELAP5 Prediction

| Time, Sec. | Intact Loop, psia | | Broken Loop, psia | |
|---|---|---|---|---|
| | Measured | RELAP5 | Measured | RELAP5 |
| 0.0 | 860.0 | 860.0 | 858.0 | 858.0 |
| 20.0 | 860.0 | 860.0 | 888.0 | 888.0 |
| 40.0 | 1016.0 | 1016.0 | 1021.0 | 1021.0 |
| 60.0 | 1000.0 | 1000.0 | 1010.0 | 1010.0 |
| 100.0 | 995.0 | 995.0 | 995.0 | 995.0 |
| 150.0 | 990.0 | 977.0 | 984.0 | 940.0 |
| 200.0 | 989.0 | 958.0 | 974.0 | 910.0 |
| 250.0 | 972.0 | 929.0 | 950.0 | 890.0 |
| 300.0 | 958.0 | 900.0 | 926.0 | 877.0 |
| 1000.0 | 863.0 | 863.0 | 700.0 | 700.0 |

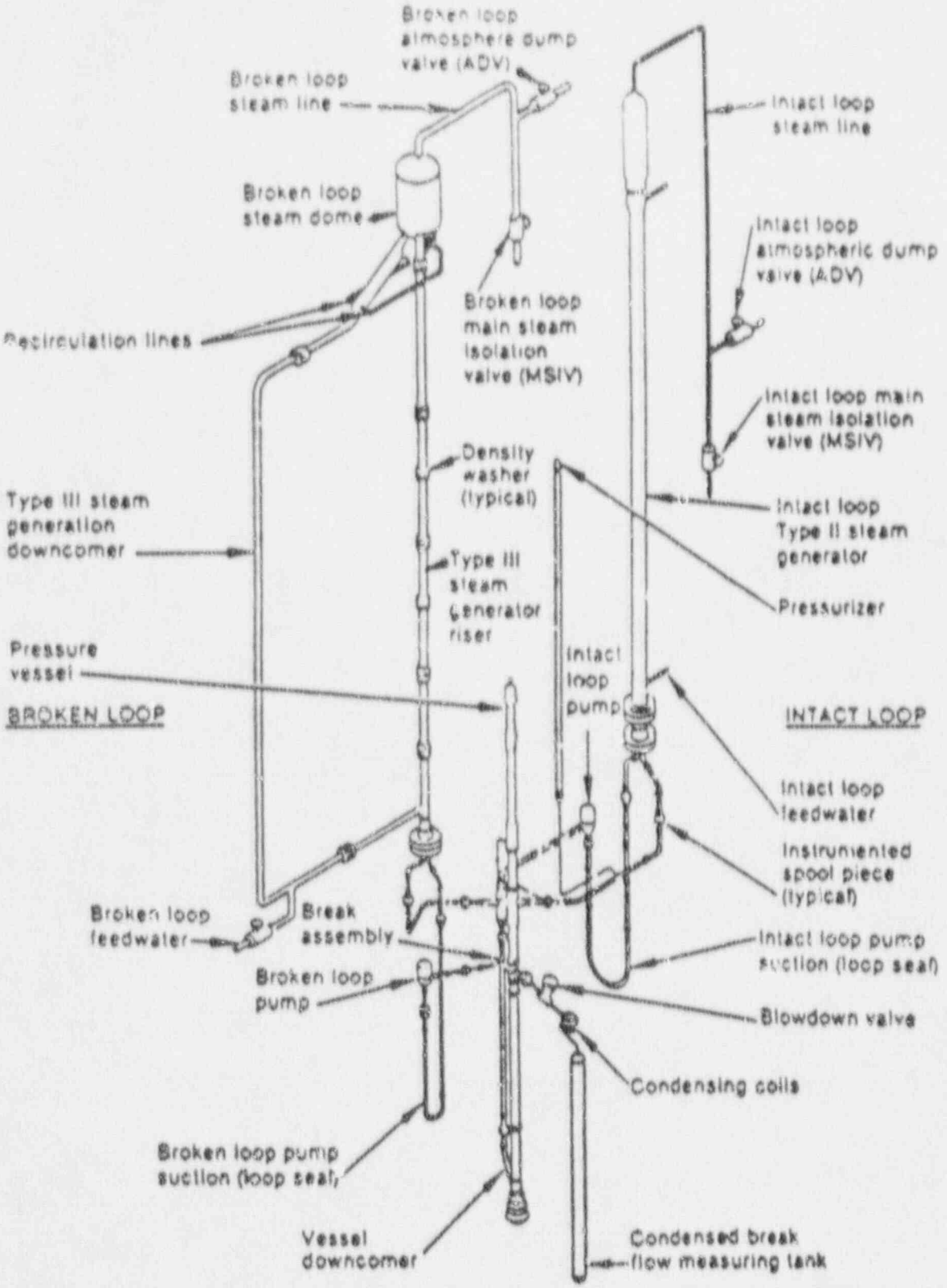



Figure 11-1. Semiscale MOD-2C System Configuration.

Figure 11-2. Semiscale S-LH-1 Base Model Noding Diagram

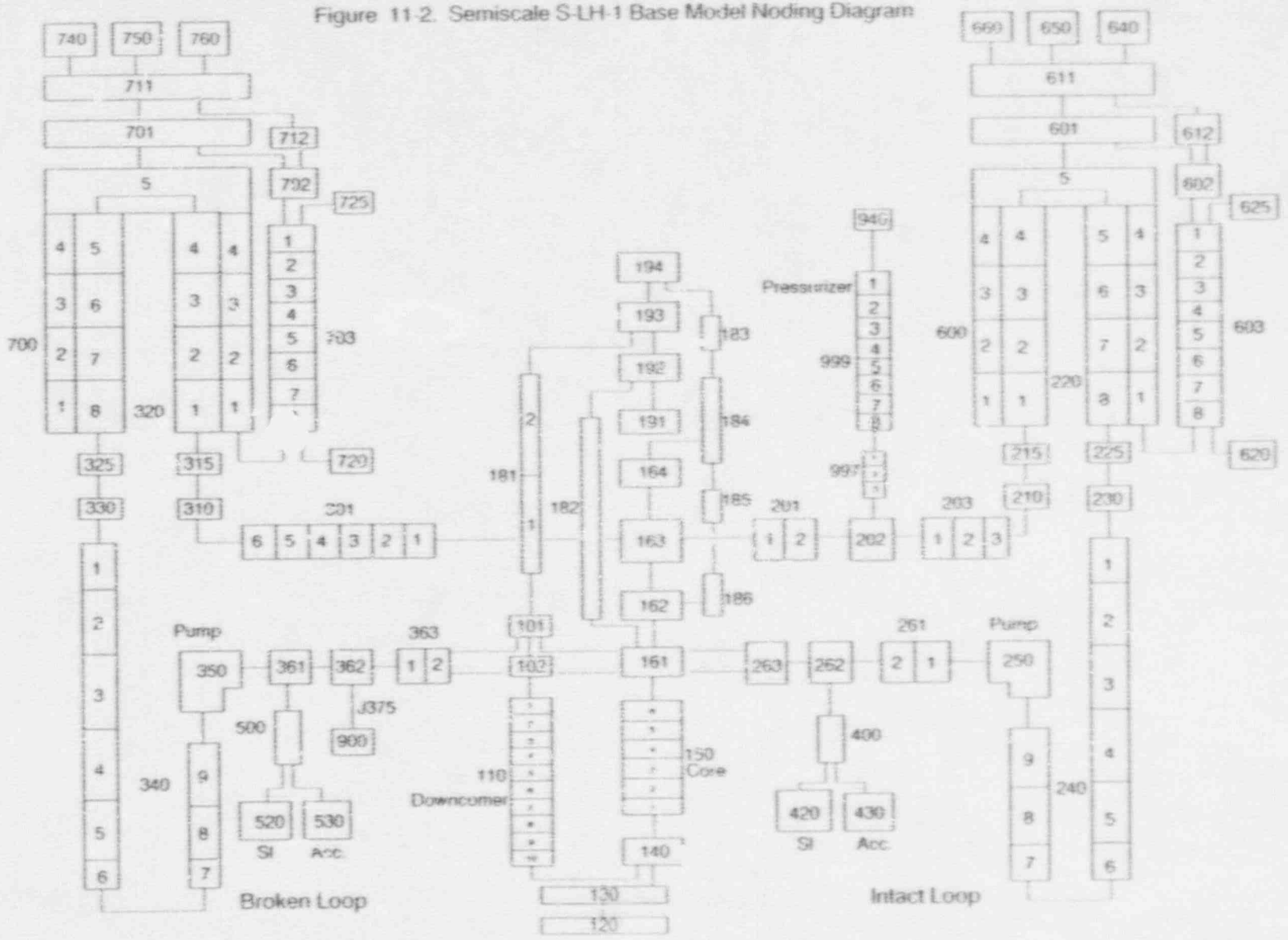


Figure 11-2a. Semiscale S-LH-1 SBLOCA EM Nodalization

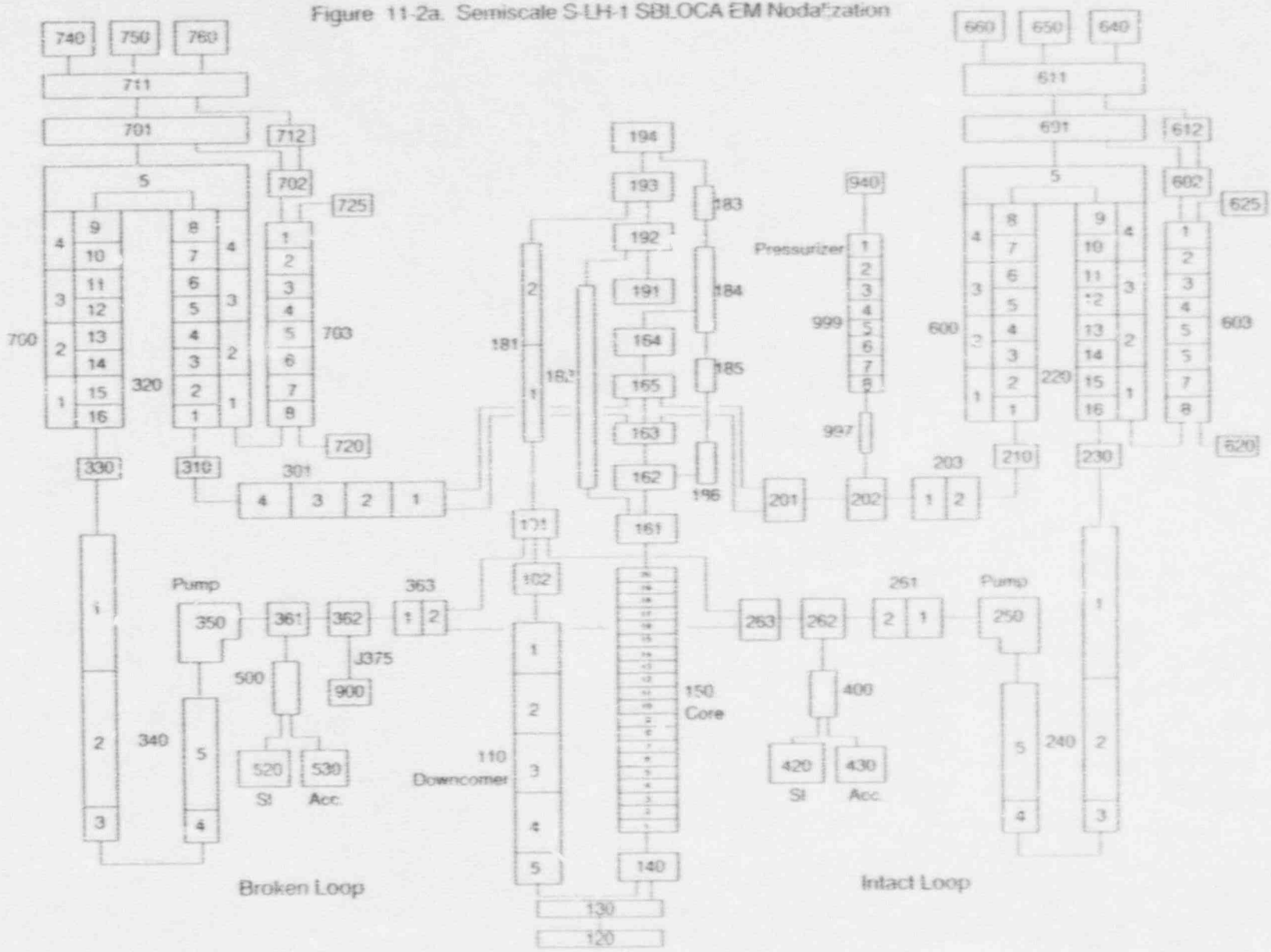


FIGURE 11-3. SEMISCALE S-LH-1 LEAK FLOW RATE

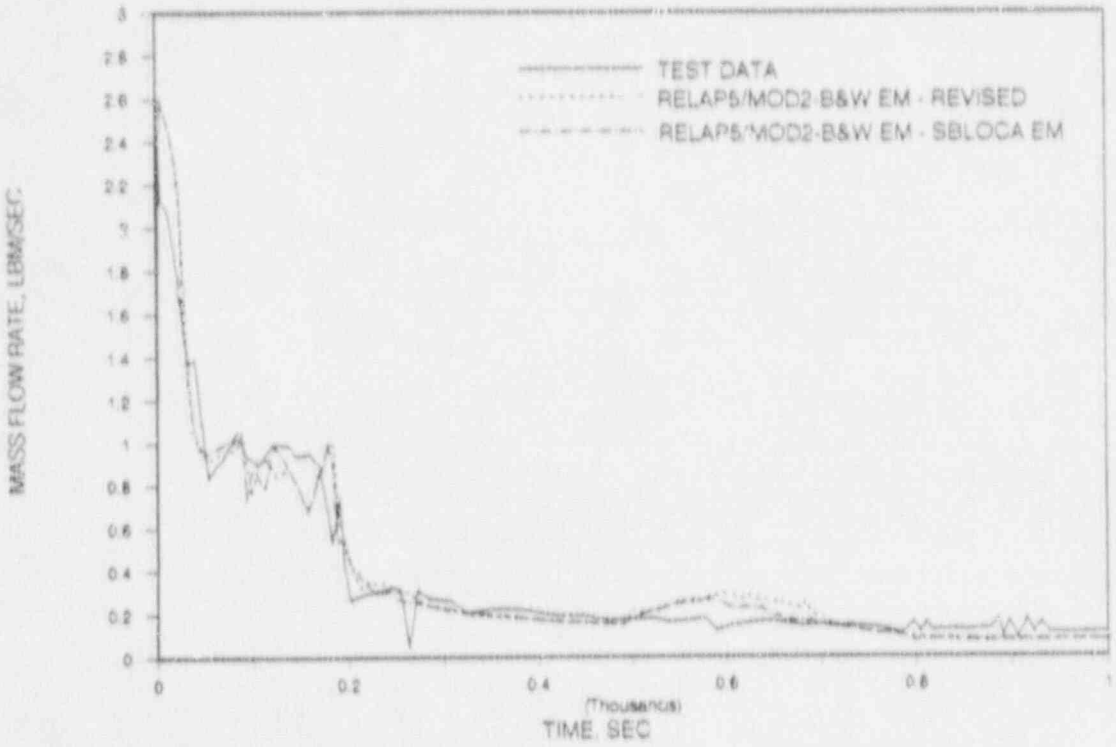



FIGURE 11-4. SEMISCALE S-LH-1 PRIMARY SYSTEM PRESSURE

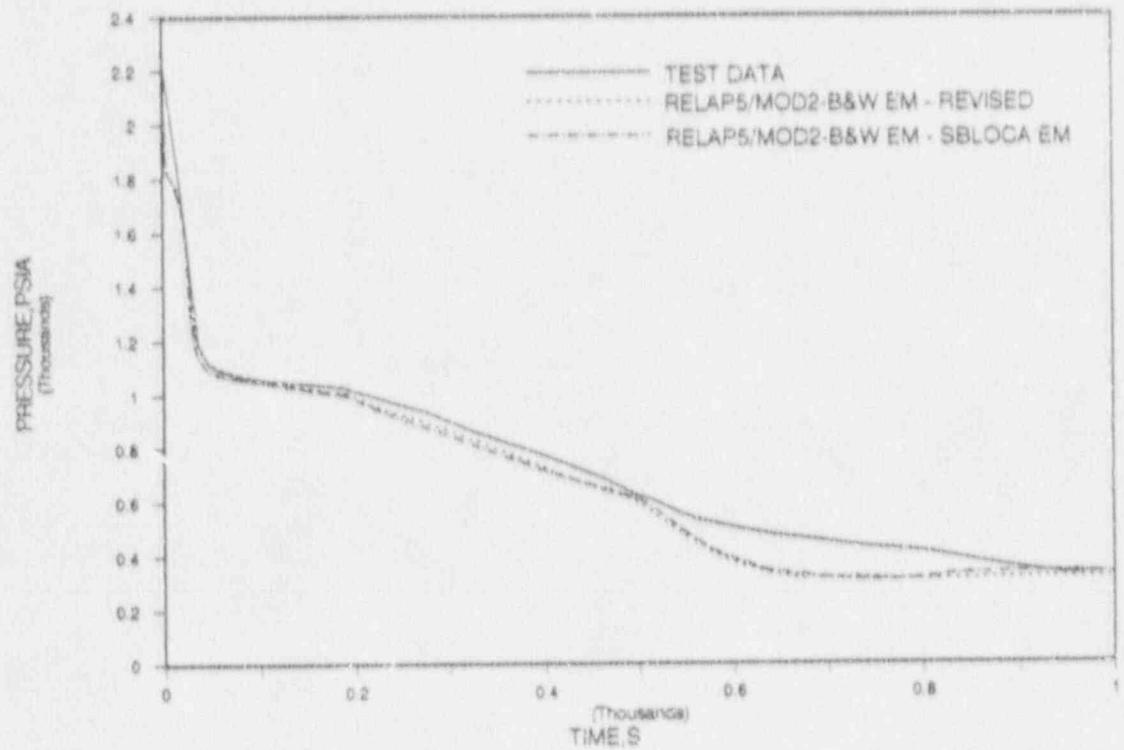


FIGURE 11-5. SEMISCALE S-LH-1 INTACT LOOP S G TUBE LEVEL-UP SIDE

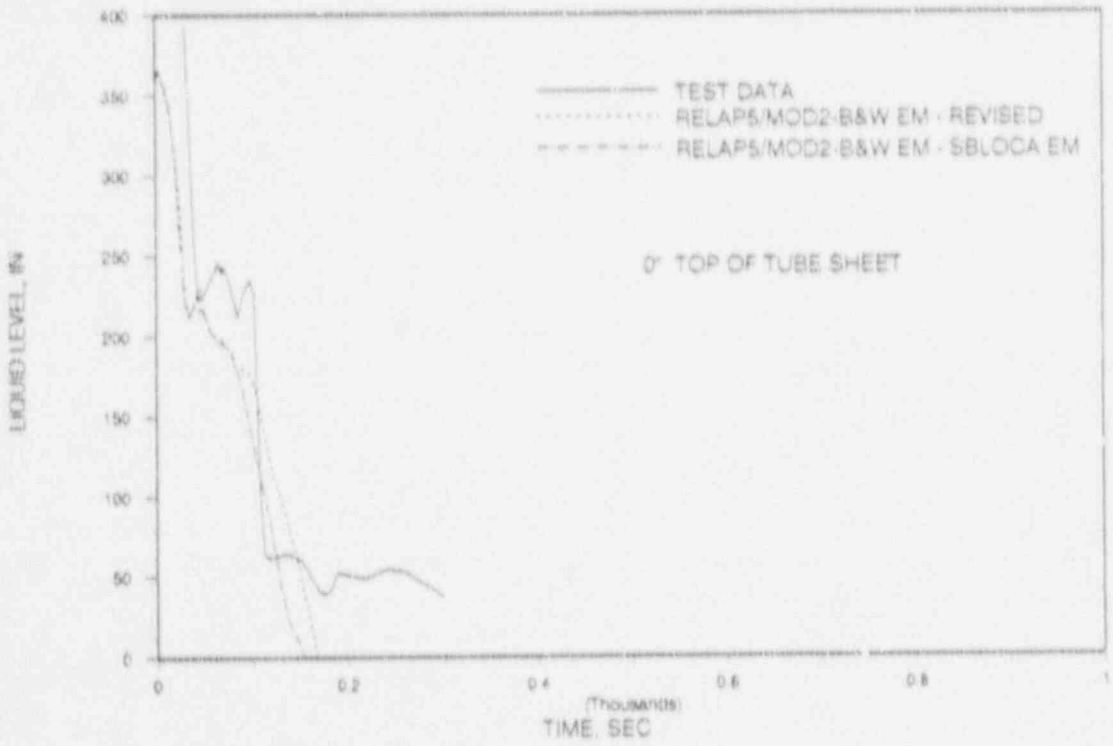



FIGURE 11-6. SEMISCALE S-LH-1 INTACT LOOP S G TUBE LEVEL-DOWN SIDE

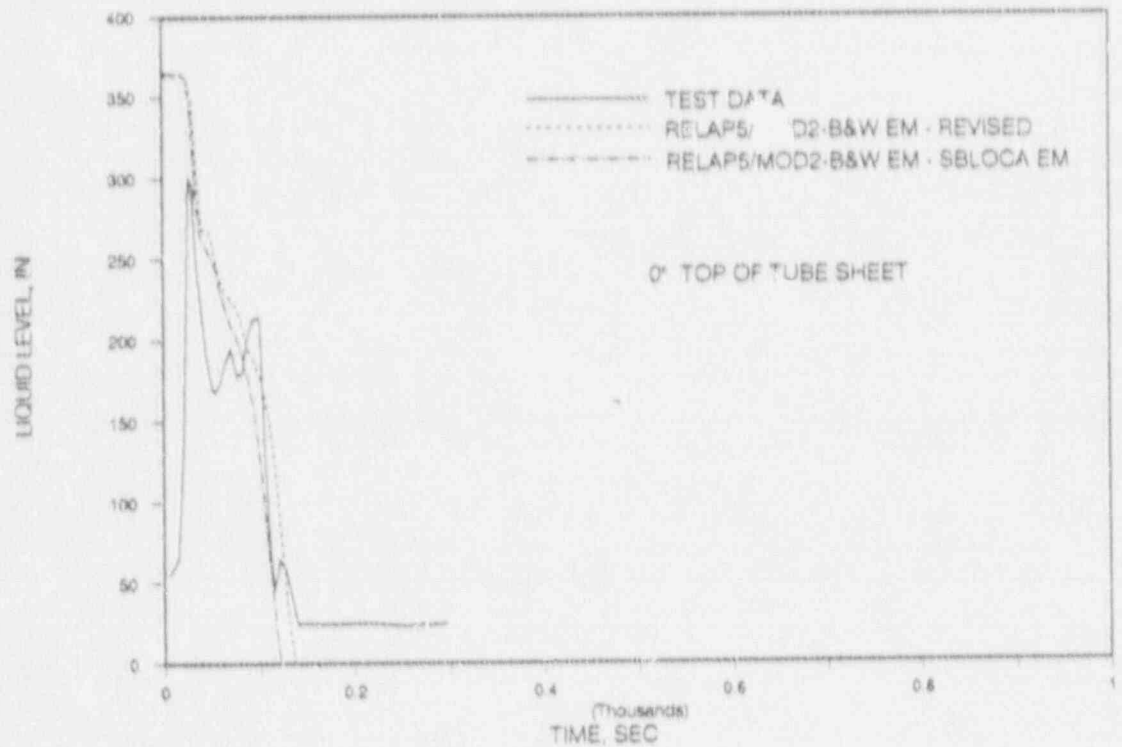


FIGURE 11-7. SEMISCALE S-LH-1 BROKEN LOOP S G TUBE LEVEL-UP SIDE

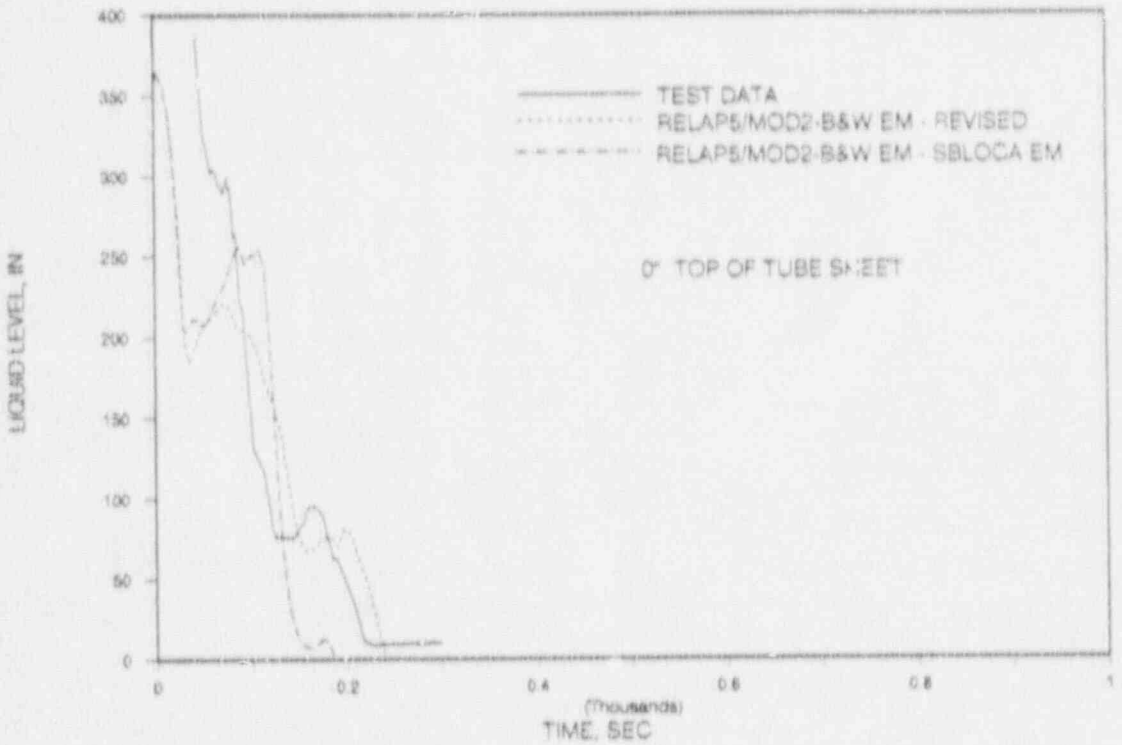



FIGURE 11-8. SEMISCALE S-LH-1 BROKEN LOOP S G TUBE LEVEL-DOWN SIDE

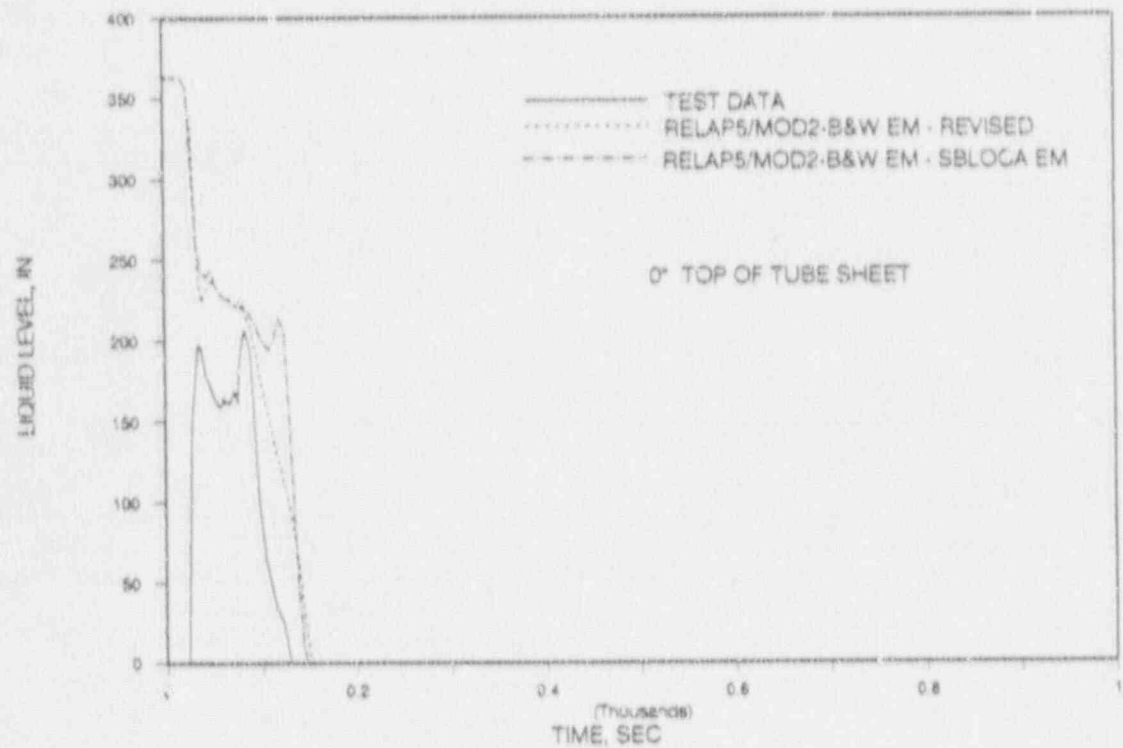


FIGURE 11-9. SEMISCALE S-LH-1 INTACT LOOP PUMP SUCTION LEVEL-DOWN SIDE

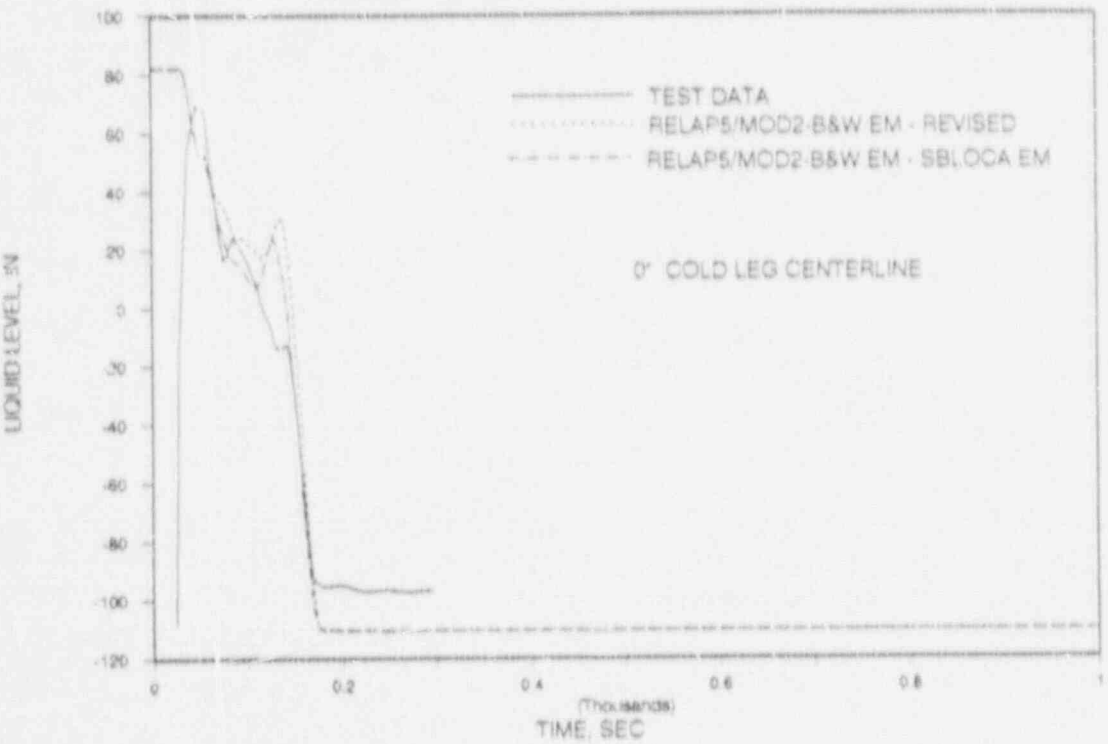



FIGURE 11-10. SEMISCALE S-LH-1 INTACT LOOP PUMP SUCTION LEVEL-UP SIDE

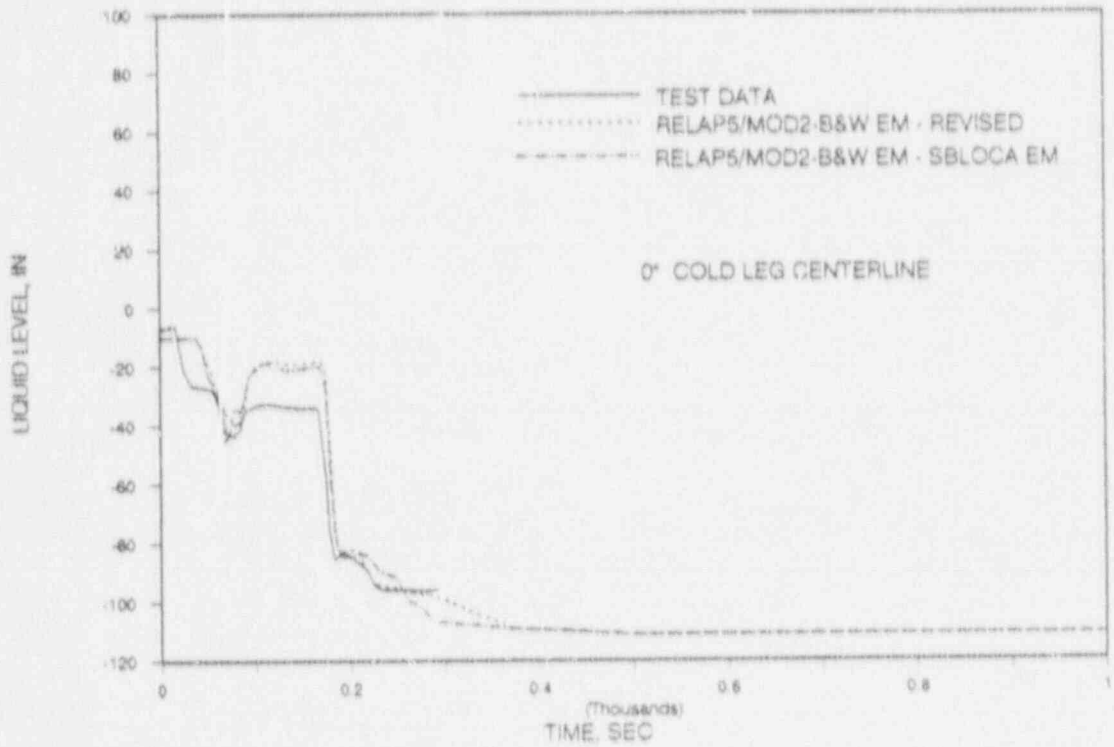


FIGURE 11-11. SEMISCALE S-LH-1 BROKEN LOOP PUMP SUCTION LEVEL LEVEL-DOWN SIDE

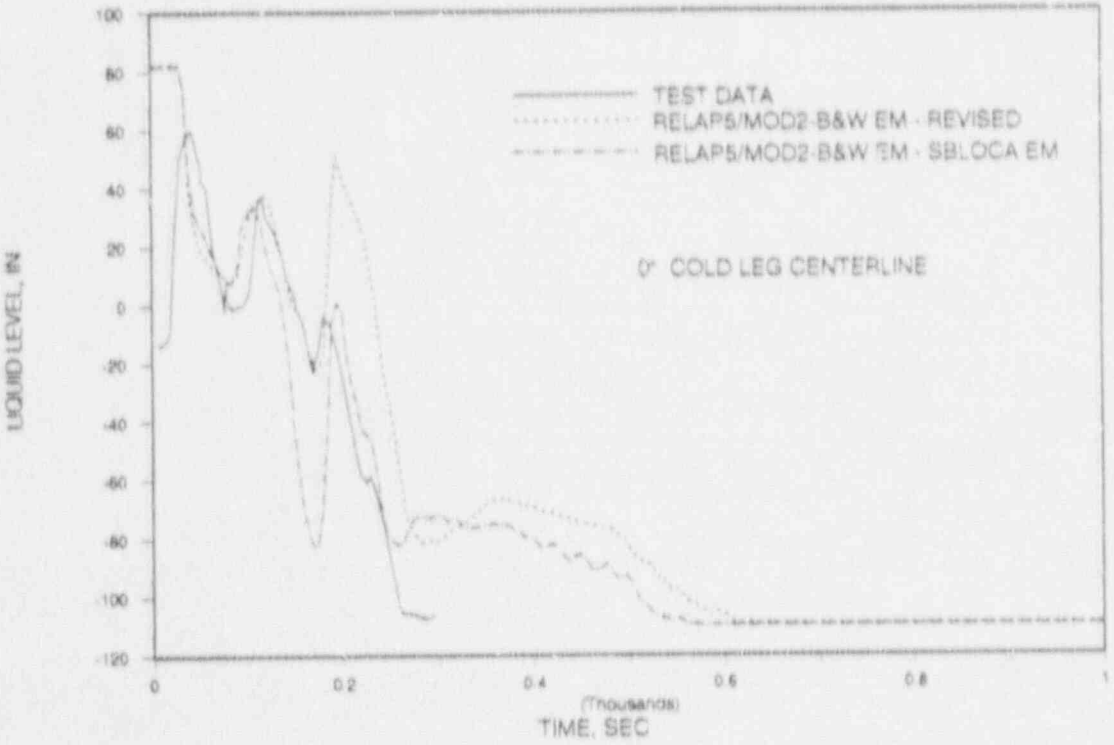



FIGURE 11-12. SEMISCALE S-LH-1 BROKEN LOOP PUMP SUCTION LEVEL-UP SIDE

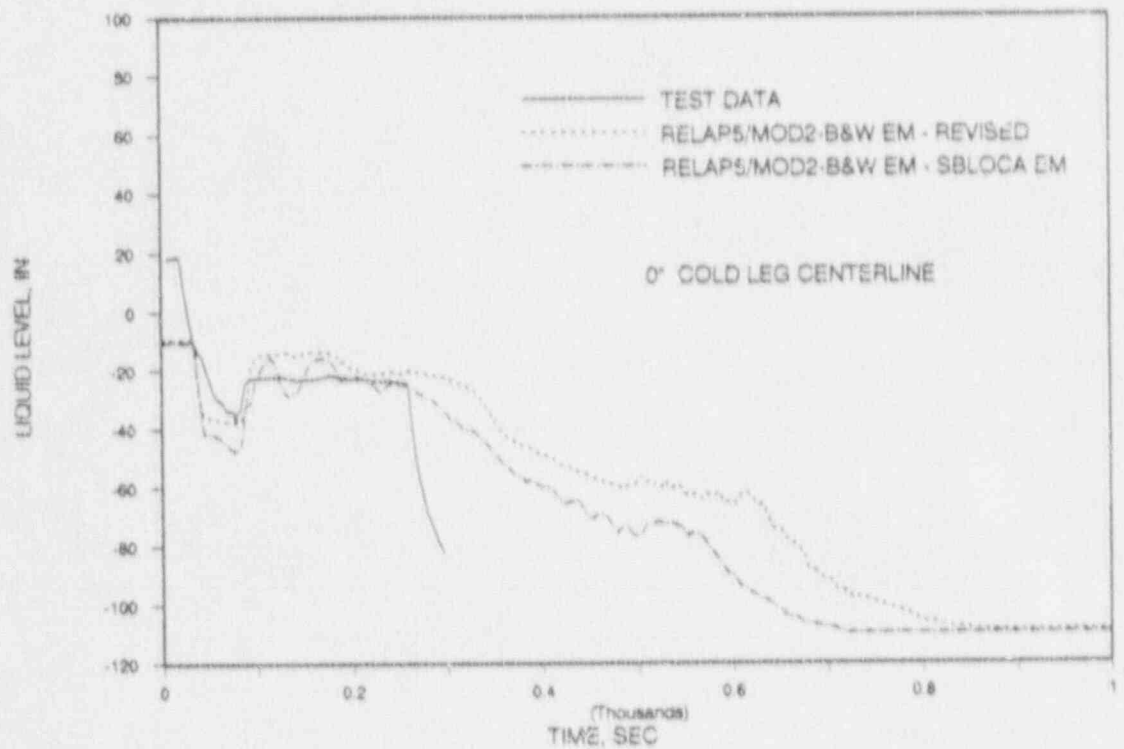


FIGURE 11-13. SEMISCALE S-LH-1 VESSEL LIQUID LEVEL

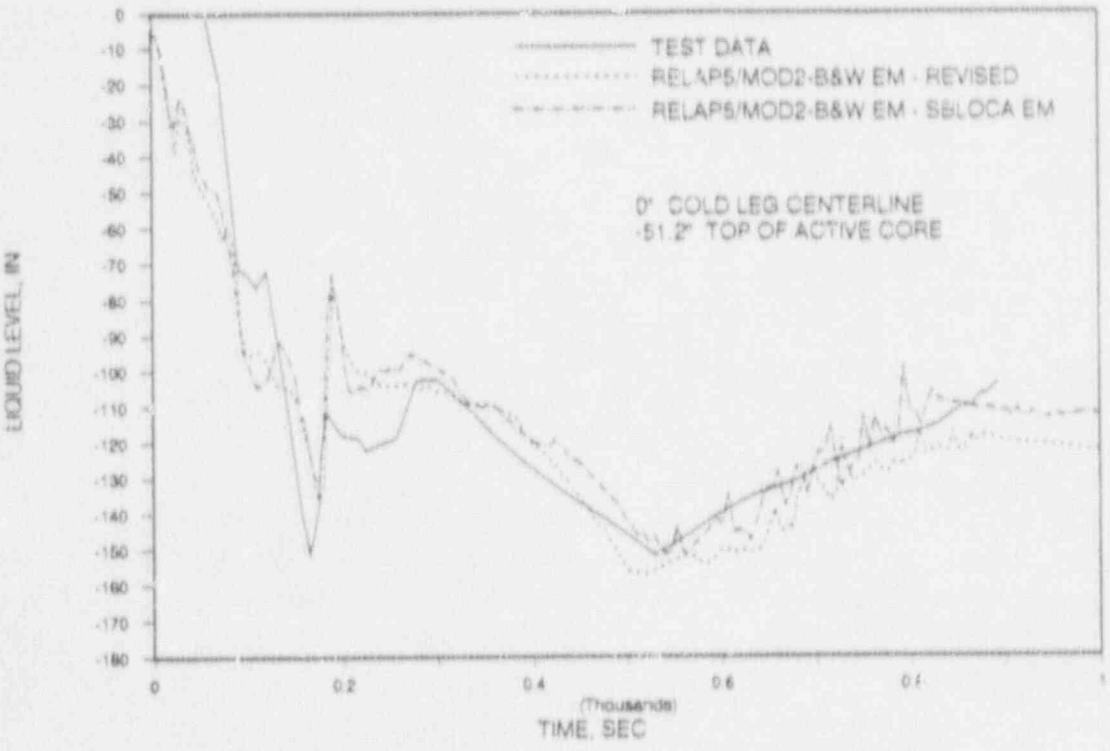



FIGURE 11-14. SEMISCALE S-LH-1 PRIMARY SYSTEM NORMALIZED MASS

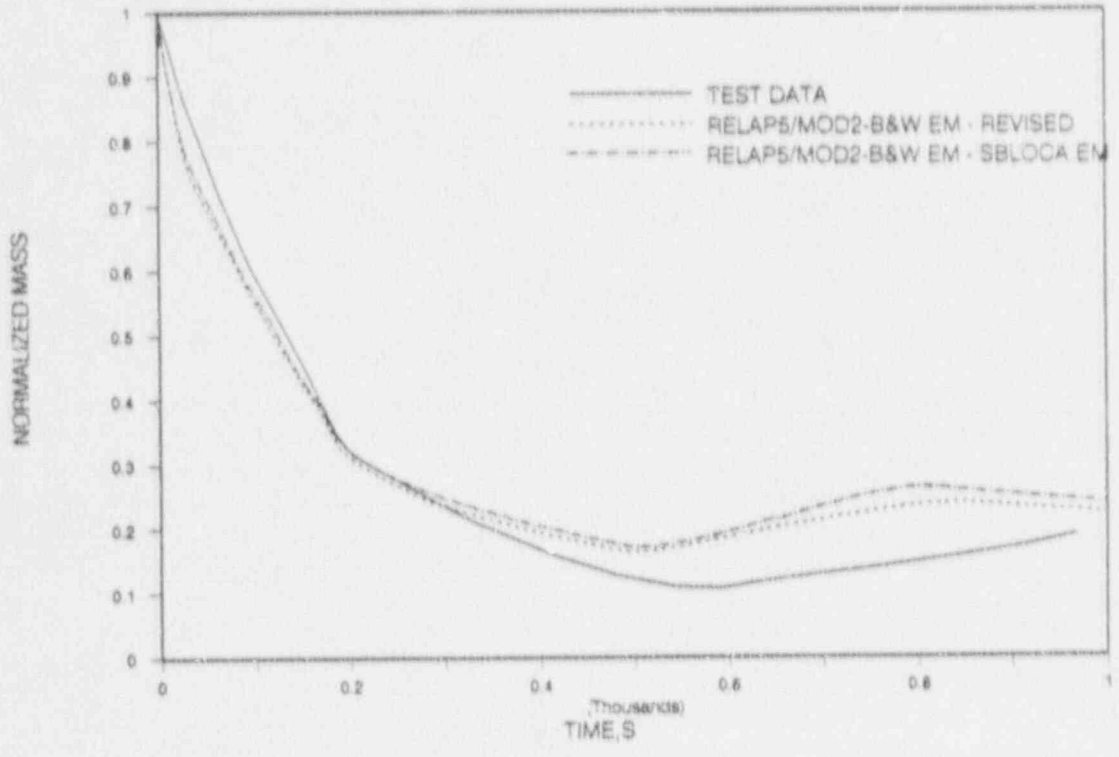


FIGURE 11-15. SEMISCALE S-LH-1 INTACT LOOP ECC FLOW RATE

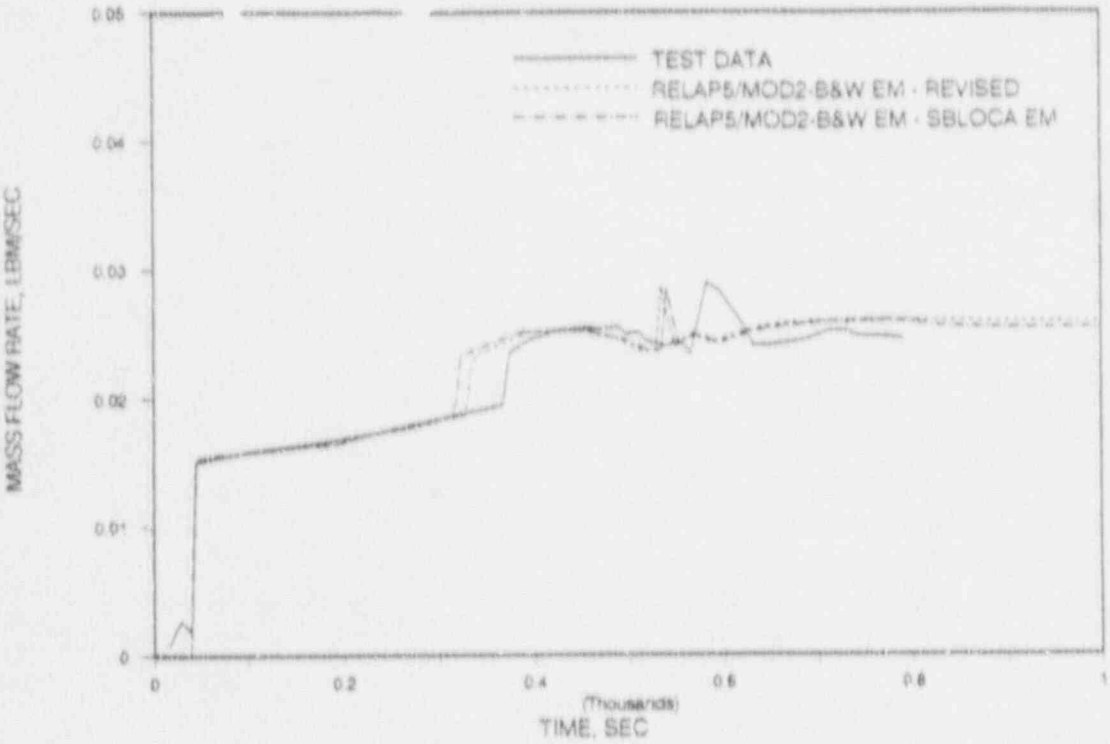



FIGURE 11-16. SEMISCALE S-LH-1 BROKEN LOOP ECC FLOW RATE

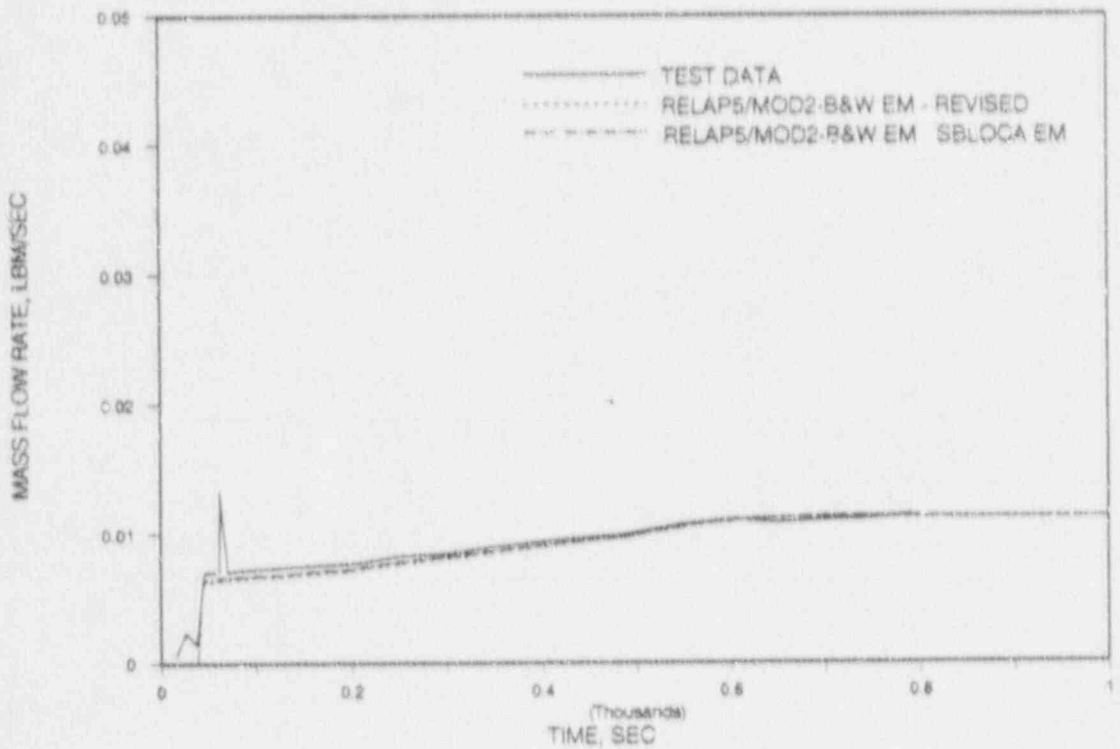


FIGURE 11-17. SEMISCALE TEST S-LH-1; VESSEL UPPER HEAD LIQUID LEVEL.

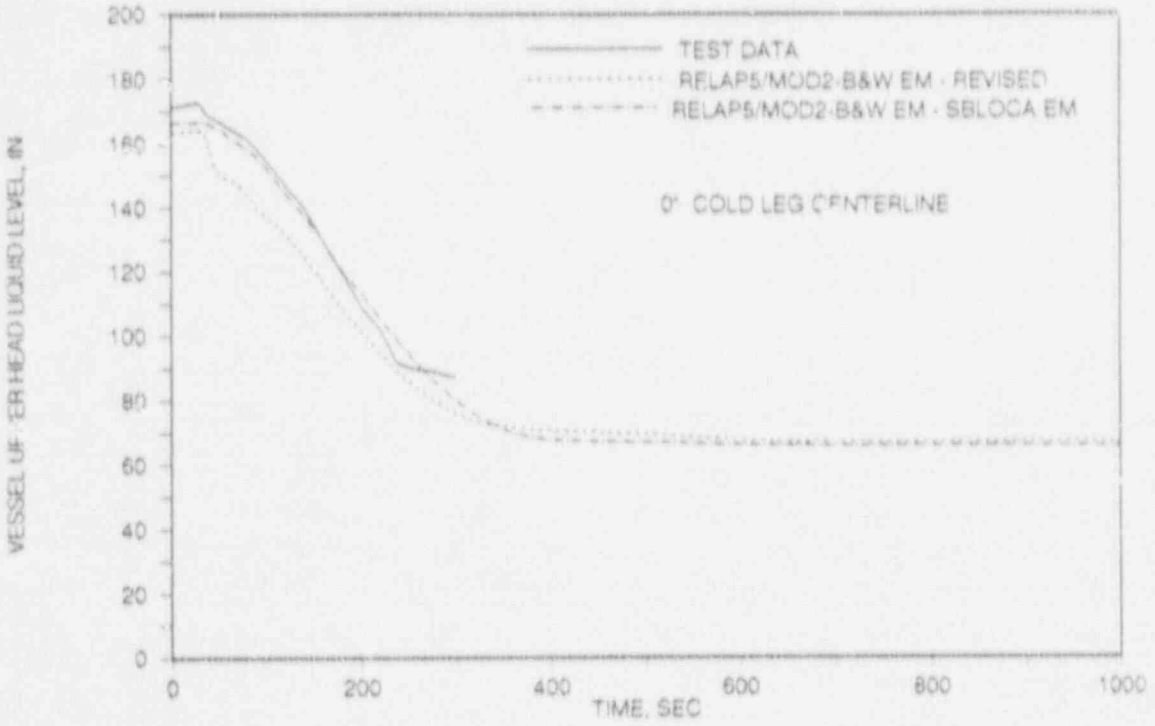



FIGURE 11-18. SEMISCALE TEST S-LH-1; DOWNCOMER LIQUID LEVEL.

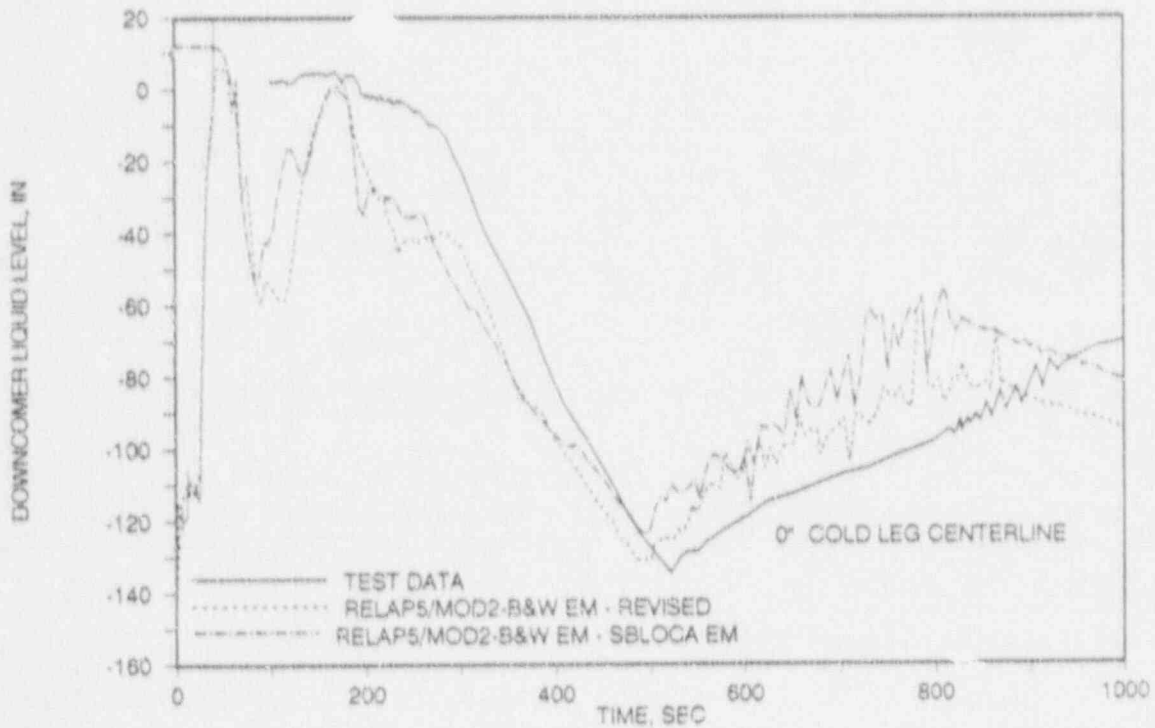


FIGURE 11-19. SEMISCALE S-LH-1 8 FT HEATER ROD SURFACE TEMPERATURE

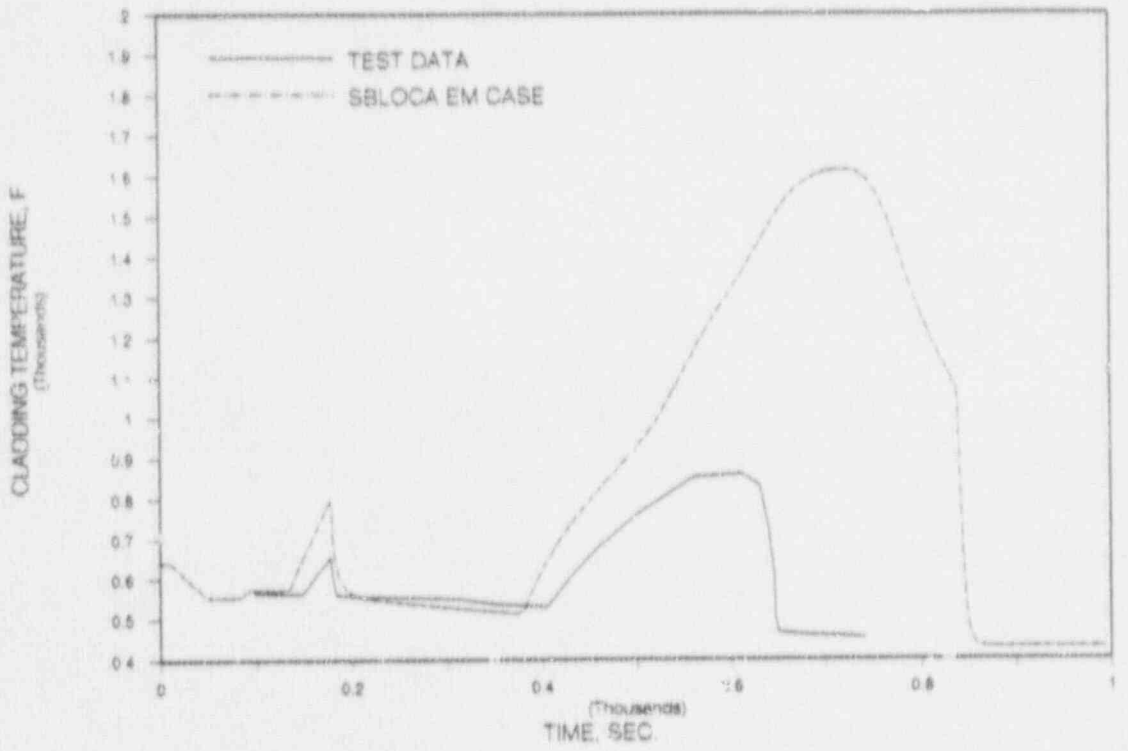



FIGURE 11-20. SEMISCALE S-LH-1 10 FT HEATER ROD SURFACE TEMPERATURE

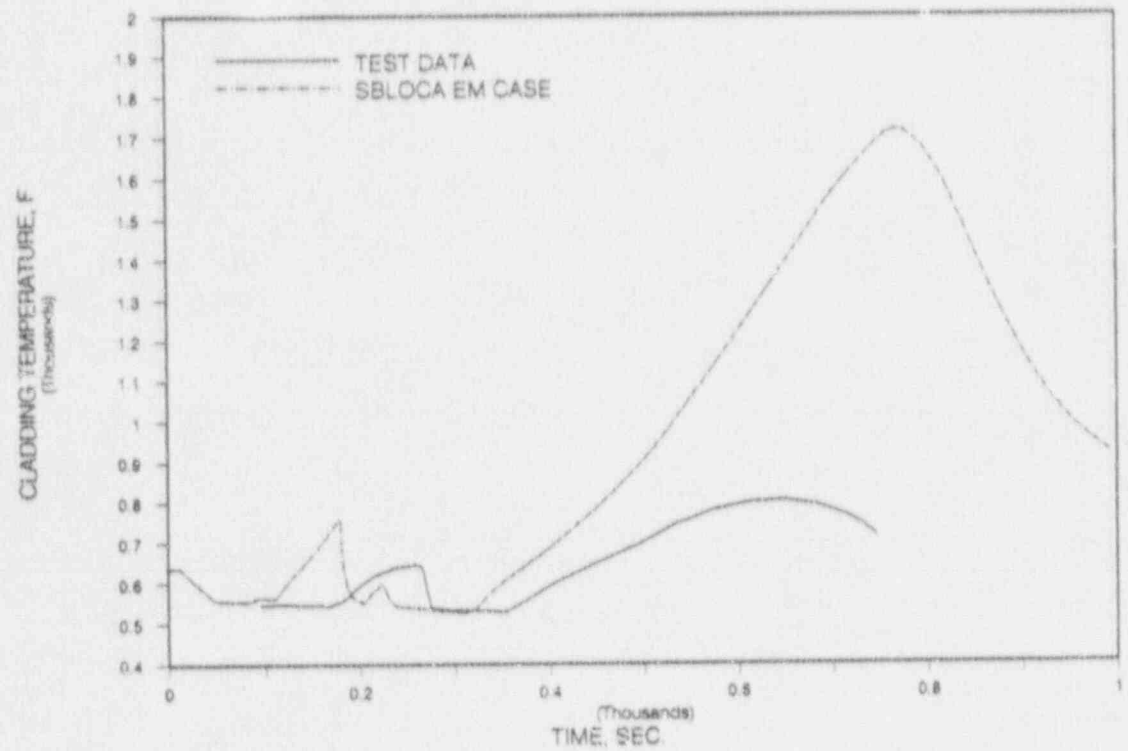


FIGURE 11-21. SEMISCALE S-LH-1 LEAK FLOW RATE

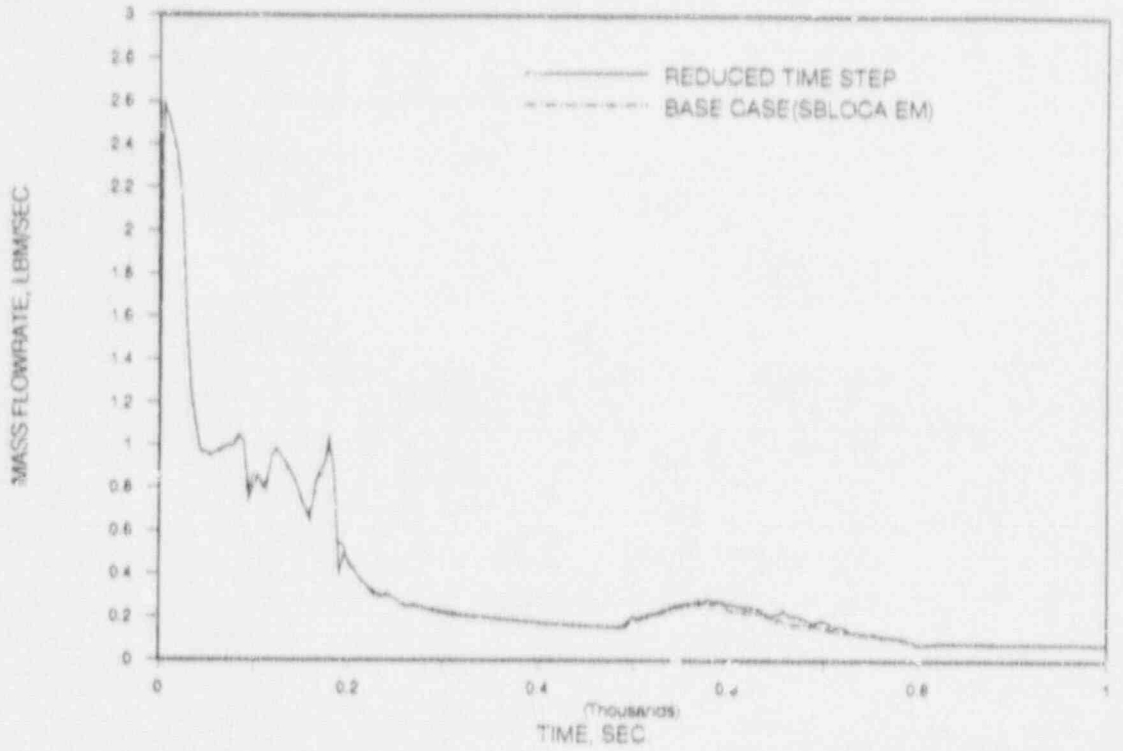



FIGURE 11-22. SEMISCALE S-LH-1 PRIMARY SYSTEM PRESSURE

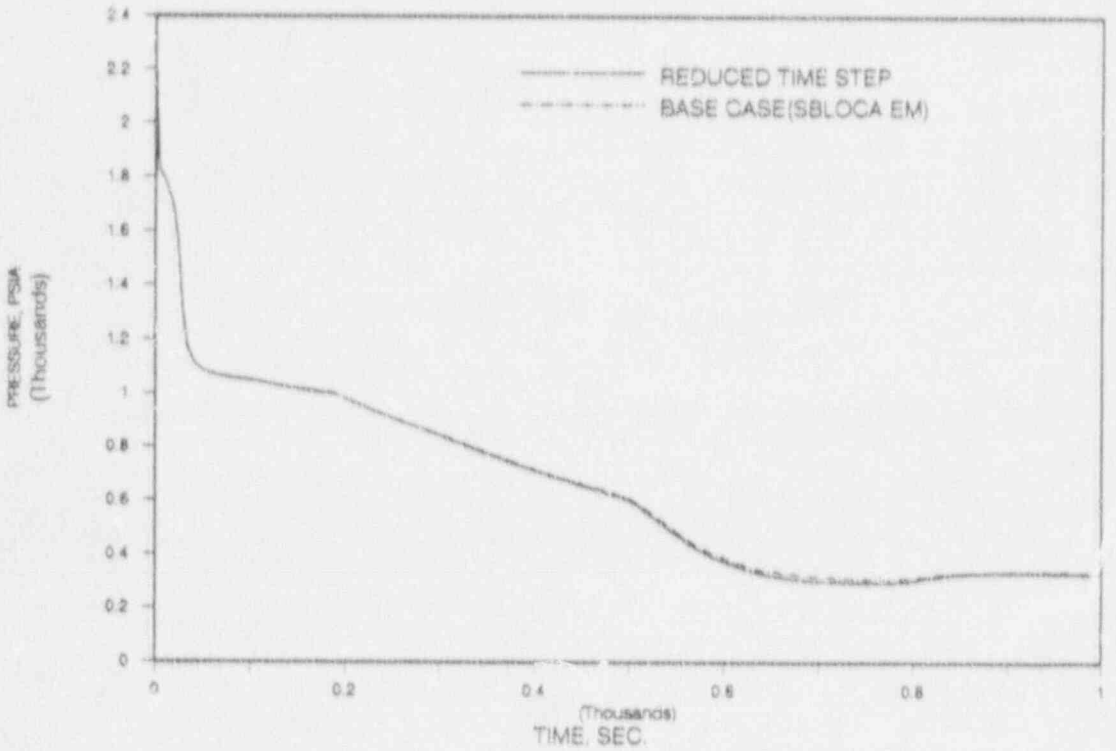


FIGURE 11-23. SEMISCALE S-LH-1 VESSEL LIQUID LEVEL

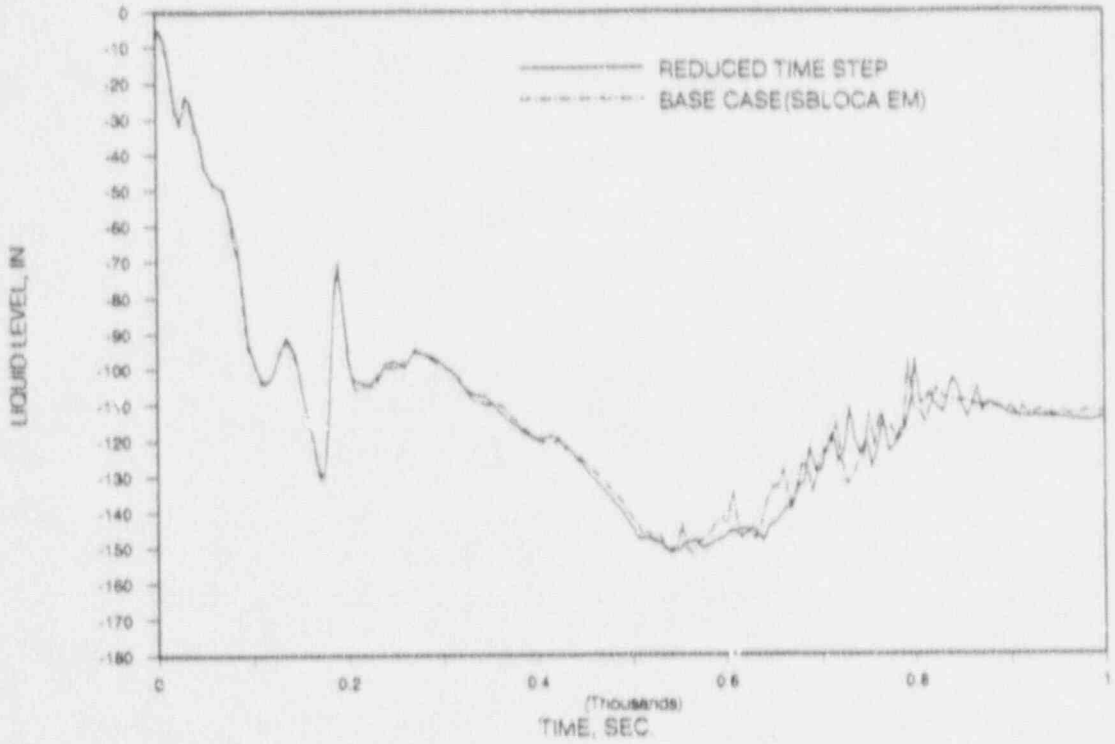
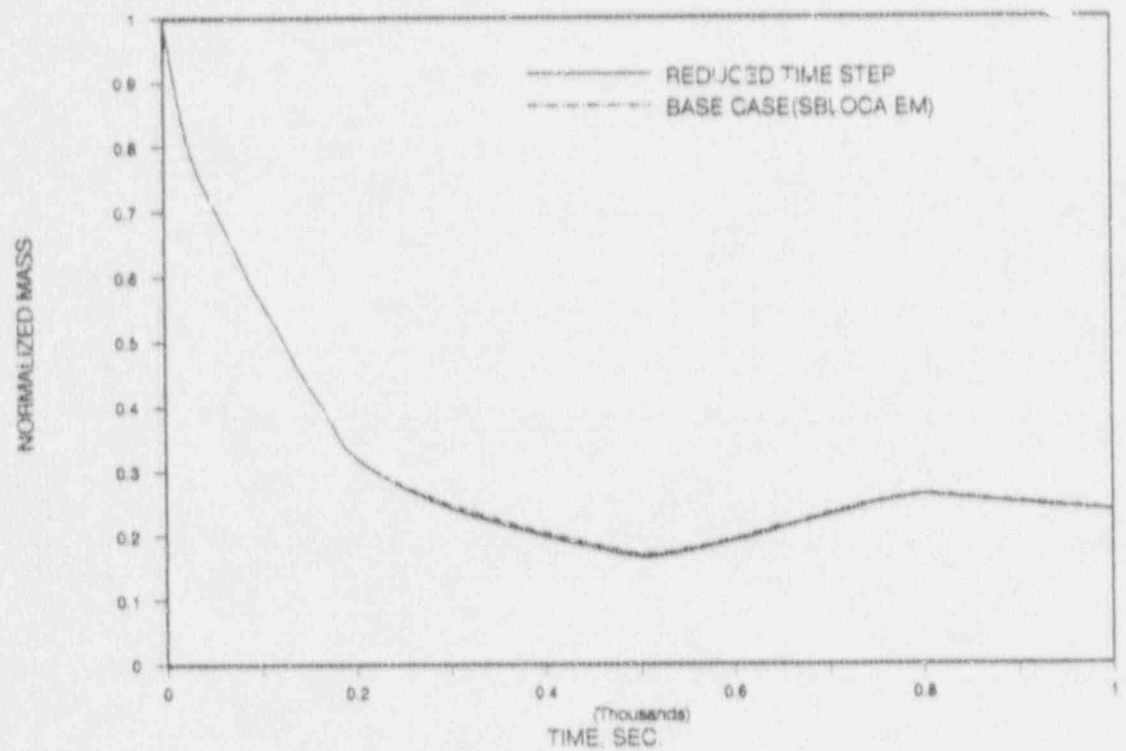



FIGURE 11-24. SEMISCALE S-LH-1 PRIMARY SYSTEM NORMALIZED MASS



12. The response to Question 51 stated that the benchmark calculation for Semiscale Test S-LH-1 verified the SBLOCA EM pump suction nodalization for pump suction loop seal clearing phenomena. However, the nodalization used for S-LH-1 was more detailed than the SBLOCA EM pump suction nodalization. Therefore, the S-LH-1 results cannot necessarily be used to justify the SBLOCA EM model. Provide the results of sensitivity studies on the pump suction nodalization, or, at a minimum, change the SBLOCA EM nodalization to be the same as that used for Test S-LH-1.

Response: The responses to questions 7 and 11 of this set cover the issue raised by this question. The response to question 7 directly addresses the factors controlling the loop seal clearing process and presents the results of noding sensitivity studies that show that the EM noding adequately models the pump suction piping. The response to question 11 documents an additional benchmark to Semiscale test S-LH-1 using a model that has been converted, to the extent practical, to the EM modelling. Both of these responses support the approach taken in the BWFC evaluation model as sufficient for SBLOCA calculations.

13. The response to Question 59 of the previous NRC questions indicated that the quiet water level passed to the FOAM2 code is calculated by combining all RCS water in the RELAP5/MOD2-B&W calculation that could fall into the core. This included water in the core and water that may be suspended in the upper plenum, hot legs, or the steam generator tube upflow. It further indicated that use of suspended water was legitimate because a conservative liquid inventory treatment will overpredict frothing within RELAP5/MOD2-B&W. However, several phenomena, which are important for SBLOCA analyses including countercurrent flow in the hot legs, flooding in the steam generator tubes and at upper core and upper plenum structures, loop seal clearing, etc., could cause the liquid to be held up above the core. Therefore, once the liquid is calculated to leave the core region by RELAP5/MOD2-B&W, it should not be considered in the FOAM2 mixture level calculation until its falling into the core is calculated by RELAP5/MOD2-B&W. This conclusion is supported by the revised calculation of Semiscale Test S-LH-1 benchmark (Letter, JH Taylor to JA Norberg, NRC, "RELAP5/MOD2 Topical Report, BAW-10164P, May 11, 1989). The core collapsed level (Fig. 5-13) from 500 to 600 seconds is below the measured level whereas the rod surface temperature at the hot spot (Fig. 5-19) turned over earlier than the data indicated because the collapsed level passed to the FOAM2 code included liquid in the upper plenum region. This indicates the ad hoc inclusion of non-core liquid in the collapsed level passed to FOAM2 is not necessarily conservative. Justify the definition of quiet water level used in the LOCA EM and demonstrate that it provides conservative SBLOCA results.

Response: The liquid inventories from RELAP5/MOD2-B&W used to create the quiet water level for FOAM2 have been revised to include only that liquid that lies within the core region. The core region is defined as the volume within the reactor vessel occupied by the fuel assemblies and their immediate supports. It extends from the bottom of the lower core plate to the top of the upper core plate. As such the liquid being used for core cooling is totally contained within the fuel assemblies or their mountings. This region extends below the active core by about eight inches and above the active core by about one foot. To assure that liquid predicted to lie below the active core is not used within the active core in FOAM2, the FOAM2 model accounts for the region below the active core using the lower plenum void fraction from RELAP5. Because the FOAM2 solution is not altered by excess liquid, the region

above the active core need not be modelled directly. At the discretion of the analyst, the region below the core active region may be deleted from both the liquid inventory accounting and from the FOAM2 modelling.

14. As discussed in your response to Question 65 of the previous NRC questions, a factor of 1.3 is used to estimate the time the quiet water level had dropped far enough in RELAP5/MOD2-B&W to require using the FOAM2 code for small break LOCA analyses. This factor was determined assuming the system pressure was less than 1100 psia and the core power was 2%. Clarify how the time of core uncovery is determined for conditions outside the assumed parameters, i.e., system pressures greater than 1100 psia or power below 2%.

Response: The conditions upon which the 1.3 factor was based were selected to lie considerably outside of the range of parameters expected during SBLOCA evaluations at times when core uncovery is credible. Two percent power occurs at 2000 seconds after plant trip while the critical time for SBLOCAs in RSG plants lies from 800 to 1200 seconds. Similarly the steam generator safety relief pressure, which controls system pressure at any time that the core could possible uncover, is close to or below 1100 psia. Therefore, as a practical guide the 1.3 swelling factor is valid for determining the need for a FOAM2 mixture level determination.

Although core liquid inventory problems are not expected at system pressures above 1100 psia or core powers below 2 percent, the need for mixture level evaluations at such conditions cannot be totally eliminated. Under such conditions the evaluation of the need for an accurate determination of core mixture height will be done on a case specific basis. If it is not obvious that the core has remained covered, than a FOAM2 computation will be made to accurately determine the core mixture level. In either case the basis for evaluation or the FOAM2 test run will be documented in B&W calculational records and in the reported results of the evaluation.

15. Your response to Question 18 of the previous NRC questions provided test data to support he values of the pressure drops to be used to account for the steam/water interaction in the cold leg. Provide additional information to justify that the test conditions (injection flow rates, injection temperatures, steam momentum, etc.) bound the conditions expected during a large break LOCA for all plants the LOCA EM could be applied to. This justification should be provided separately the pressure drops during the accumulator injection and the pumped injection periods, respectively. If the test data does not bound the plant conditions, justify that the test results were extrapolated in a conservative manner, and also provide information to justify the pressure drop chosen for the pumped injection period based on the test data.

Response: In the response to Question 18 for the EM topical report (BAW-10168) it was stated that the following pressure drop values will be used to account for the momentum loss due to the steam-water interaction in the cold legs during the reflooding period.

1. 0.85 psi will be used during the accumulator injection period.

2. 0.5 psi will be used during the pump injection period.

These values were estimated from the Westinghouse 1/14 scale, CE 1/5 and 1/3 scale 90 degree angle injection tests, references 15.1 through 15.3, and the appropriate plant conditions as described below.

<u>ECCS Flow Rate</u>

The ECCS flow rate during the accumulator injection period is about 16,000 gpm/cold leg. The equivalent flow rates in the 1/3 scale and 1/5th scale tests based on cold leg pipe area scaling are about 5200 gpm and 3100 gpm, respectively. Based on the injection pipe diameter in the plant of 8.75 inches, the injection flow velocity will be about 84 Ft/sec.

During the pumped injection period, the ECCS flow rate in a plant could vary from 1300 to 2600, gpm depending on the availability (minimum or maximum) of ECCS. This would correspond to 440 gpm or 900 gpm ECCS flow in the 1/3 scale test and 260 gpm to 520 gpm in the 1/5th scale test. The corresponding liquid injection velocity is about 7 ft/sec to 14 ft/sec.

Steam Momentum

The maximum steam momentum in the low pressure (ice containment) and high pressure (dry containment) containment plants differ. However, an upper bound value of about 0.8 psi will cover both these type plants.

Pressure Drop Measurements

A schematic of the ECC injection tests reported in references 15.1 through 15.3 is shown in Figure 15-1. The measured pressure drop, $\Delta p$, is given by $P_5 - P_5'$. A positive $\Delta p$ indicates the momentum loss and a negative $\Delta p$ indicates momentum gain.

Analysis of the Test Results.

Westinghouse 1/14 Scale Tests

The Westinghouse 1/14 scale test results are shown in Figures 15-2 through 15-3. From these figures the following observations can be made.

1. These tests covered the plant conditions during the accumulator injection period and the pump injection period with minimum ECCS availability.

2. The maximum $\Delta p$ during the accumulator injection period is 0.85 psi and it decreases with decrease in injection velocity (Figure 15-2). The maximum value for the 40 ft injection velocity case is about 0.2 psi.

3. The maximum $\Delta p$ during the pump injection period which correspond to the minimum ECCS availability condition (injection velocity about 7 ft/sec) is about 0.4 psi (figures 15-3 and 15-4).

CE 1/3 Scale Tests

The CE 1/3 scale test data are shown in Figures 15-5 through 15-7. The figures show largely the same data rearranged for differing axis parameters. The ECCS flow rate in these tests covered the pumped injection flow condition. The maximum steam momentum flux of about 0.5 psi is somewhat lower than the maximum expected value in the plant. From Figure 15-7 it can be seen that below 0.3 psi (40 $lbf/ft^2$) steam momentum flux the pressure drop is negative. From 0.3 psi to 0.5 psi range the pressure drop is not sensitive to the steam momentum flux and the maximum pressure drop is about 0.25 psi. From Figure 15-3 the effect of steam momentum flux from 0.5 to 0.8 psi is about 0.1 psi. Therefore, it the expected pressure drop upto the 0.8 psi maximum of the plants steam momentum flux range extrapolated from the 1/3 CE tests will be less than 0.5 psi.

CE 1/5th Scale Tests

The CE 1/3th scale test data are shown in Figures 15-8 through 15-10. These tests covered the pumped injection flow condition. These results show that a pressure drop of 0.5 psi is a reasonable average value.

Conclusion

The Westinghouse 1/14 scale, CE 1/3 and 1/5th scale tests together cover the plant conditions during the accumulator and pump injection periods. These test results support the selected pressure drop values that represent the momentum loss due to the steam-water interaction near the ECCS injection location.

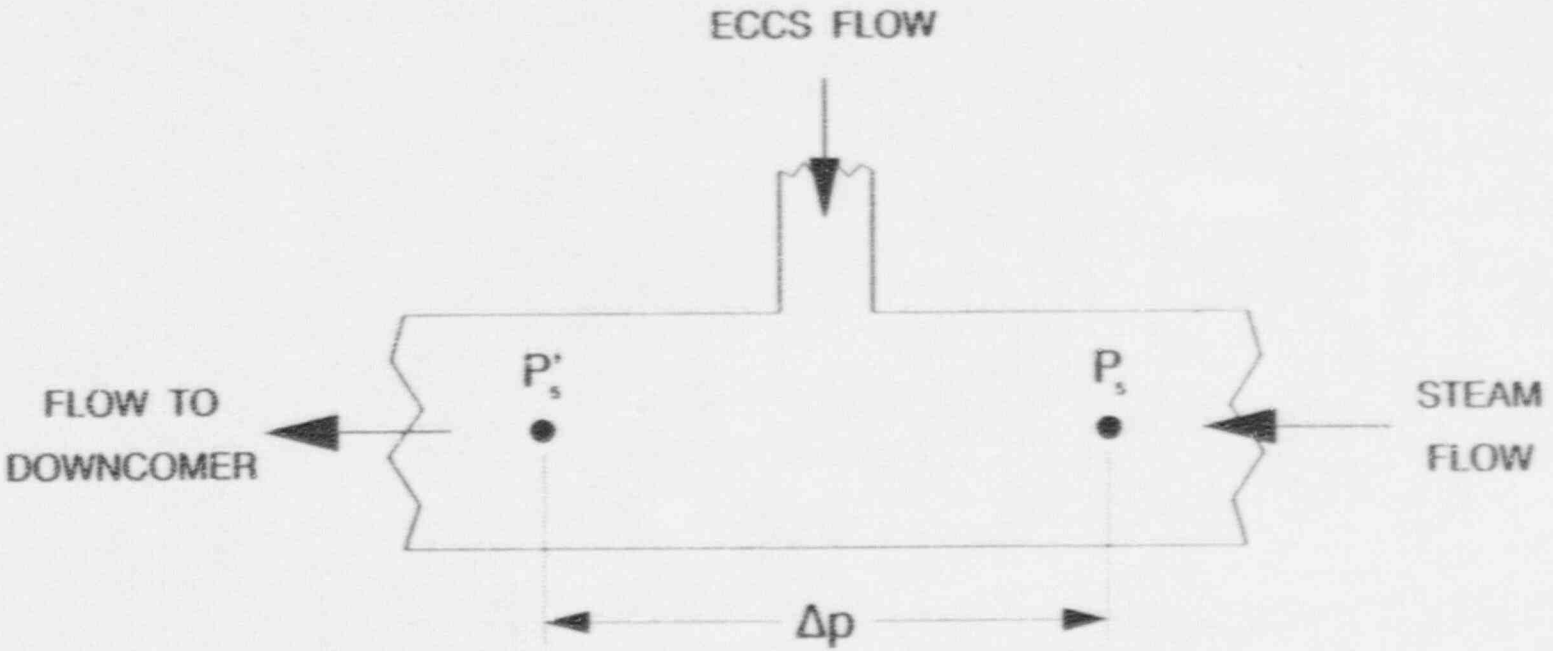
References:


15.1 G. P. Lilly, et al., Mixing of Emergency Core Cooling Water with Steam, EPRI-294-2, January, 1975.

15.2 J. R. Brodrick, et al., 1/5 Scale Intact Loop Post-LOCA Steam Relief Tests, CENPD-63, Rev.1, March 1973.

15.3 W. E. Burchill, et al., Steam Water Mixing Test Program Task D: Formal Report for the Task B and Final Report for the Steam Relief Phases of the Test Program, AEC-C00-2244-1, CENPD-101, October 1973.

FIGURE 15-1. SCHEMATIC OF THE ECC INJECTION TEST SECTION.



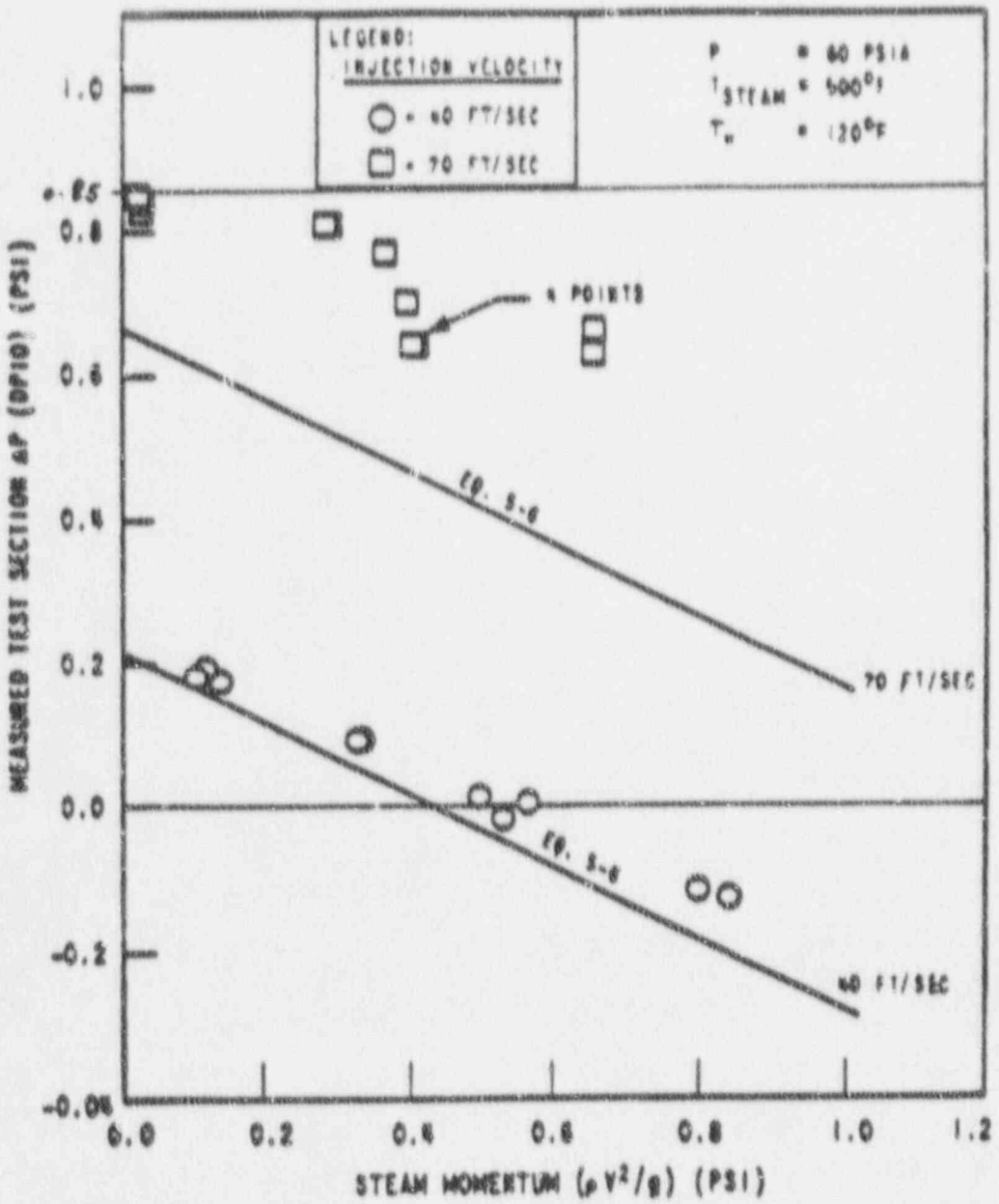



Figure 15-2 Injection-Water Flow-Rate Effect in Accumulator Range, 90° Injection Angle, 1/14 Scale[15.1]

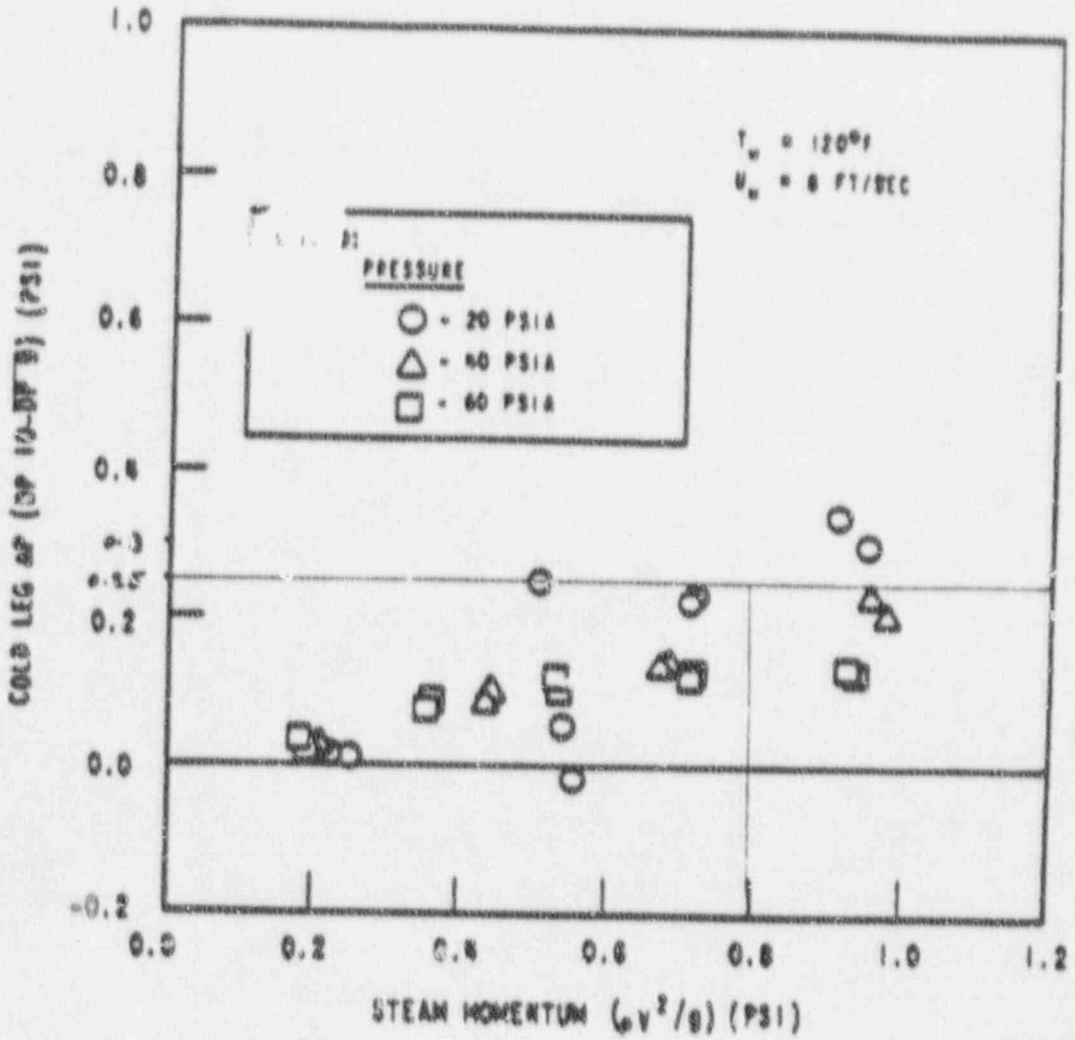



Figure 15-3 Pressure Effect in SIS Range, 90° Injection, 1/14 Scale[15.1]

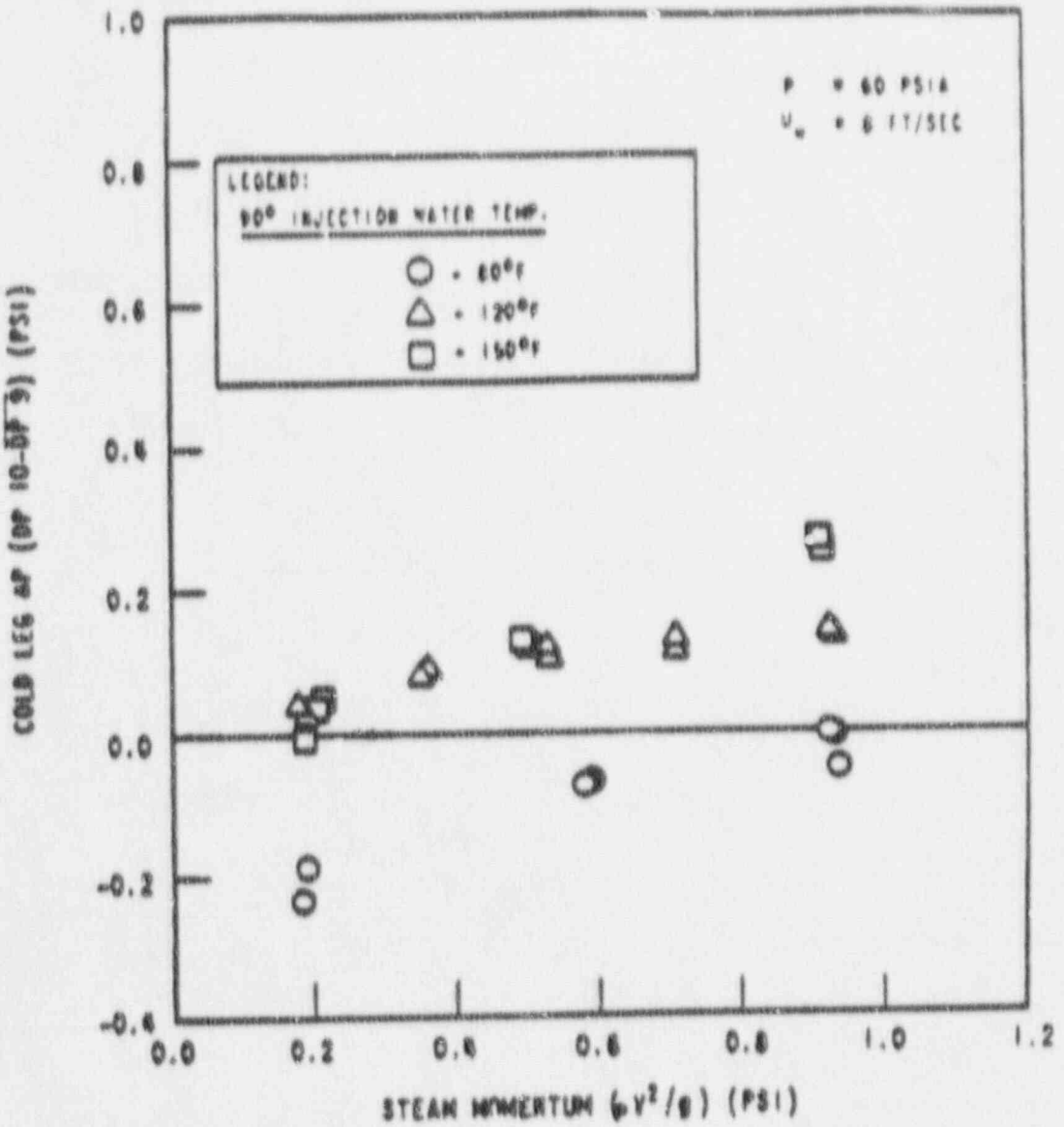



Figure 15-4 Injection Water Temperature Effect in SIS Range, 90° Injection, 1/14 Scale[15.1]

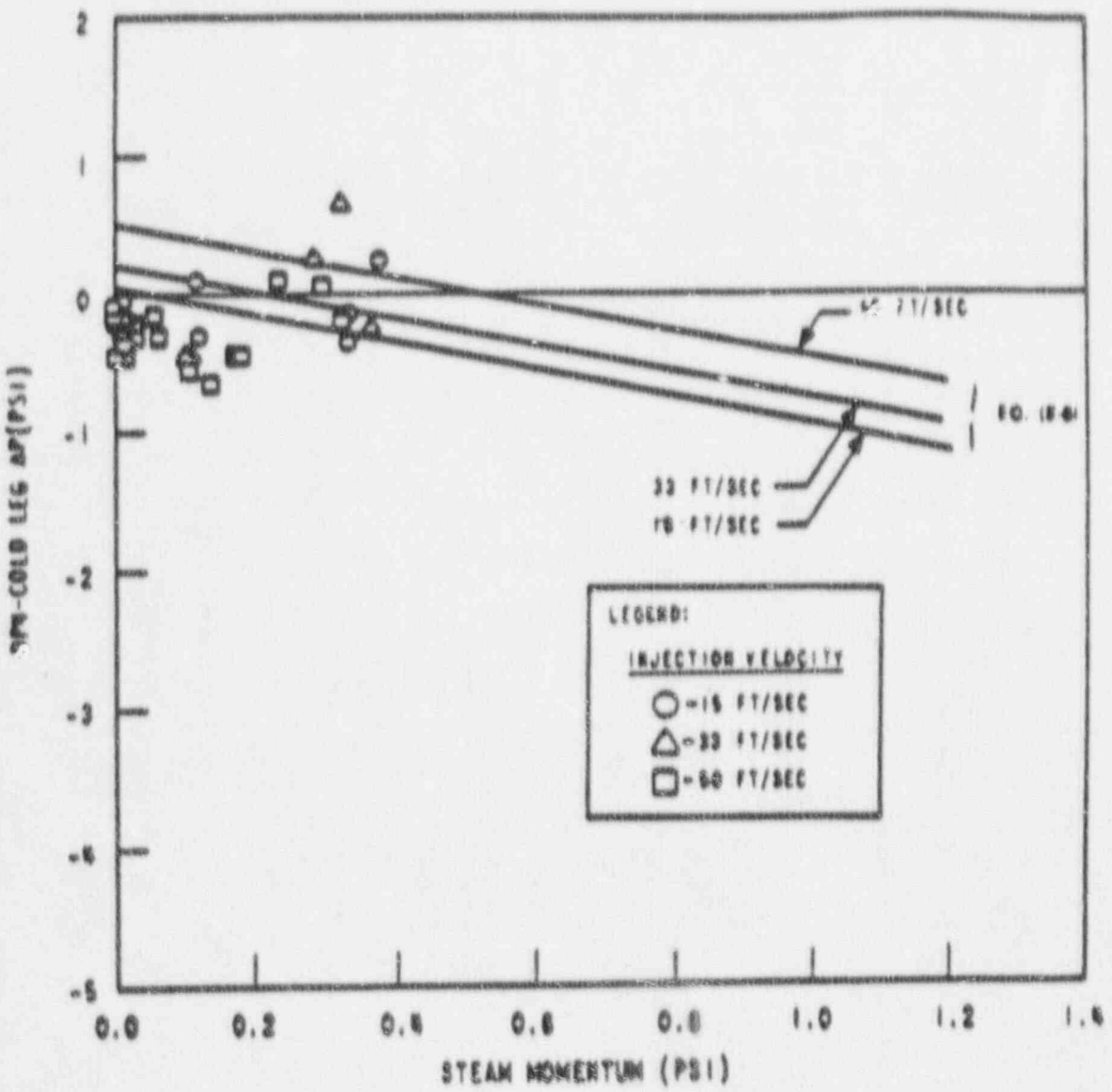



Figure 15-5 C.E. 90° Injection 1/3 Scale Data[15.1]

Figure 15.6

COLD LEG INJECTION SECTION PRESSURE DIFFERENTIAL vs VOLUMETRIC ECC FLOW[15-3]

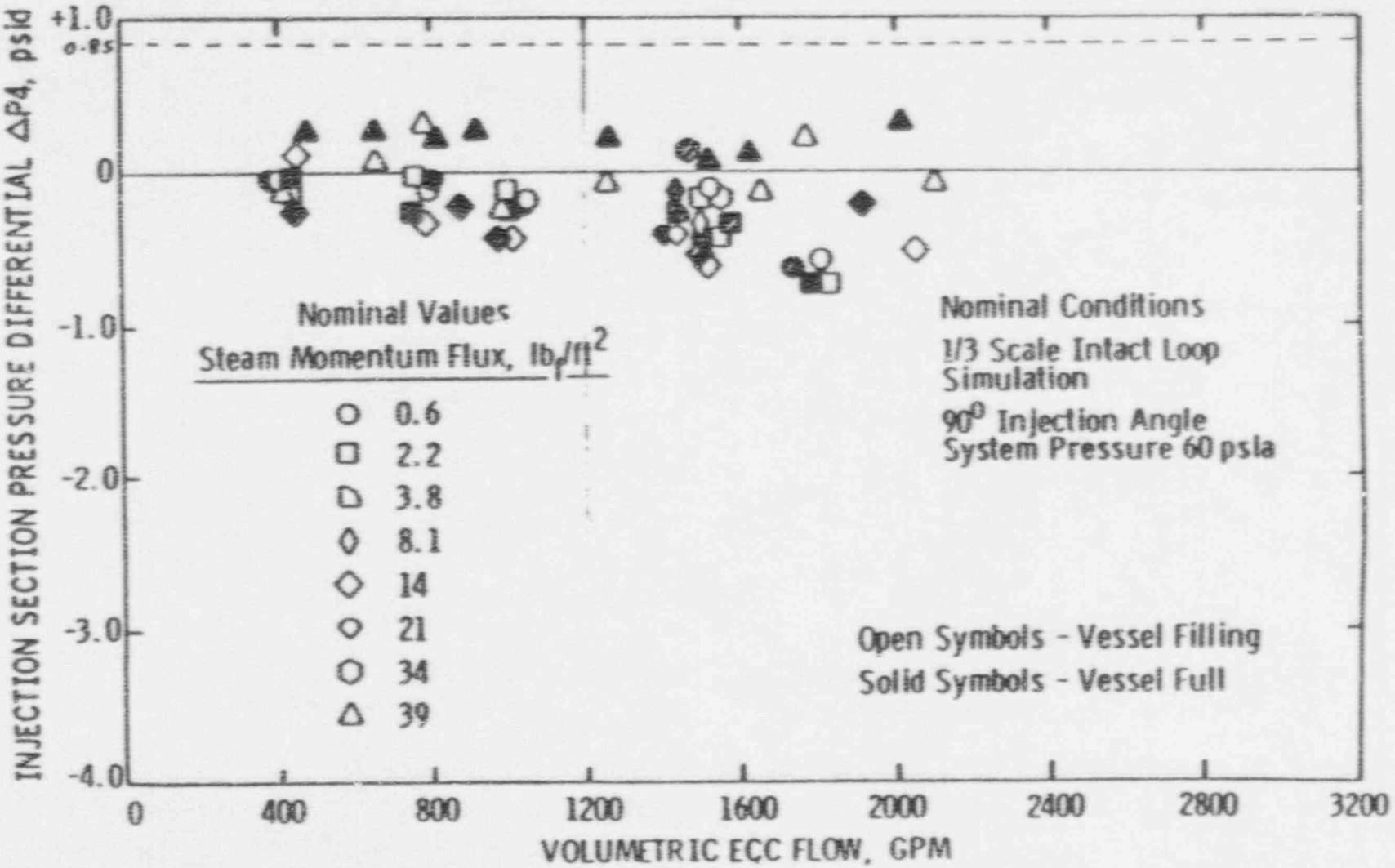
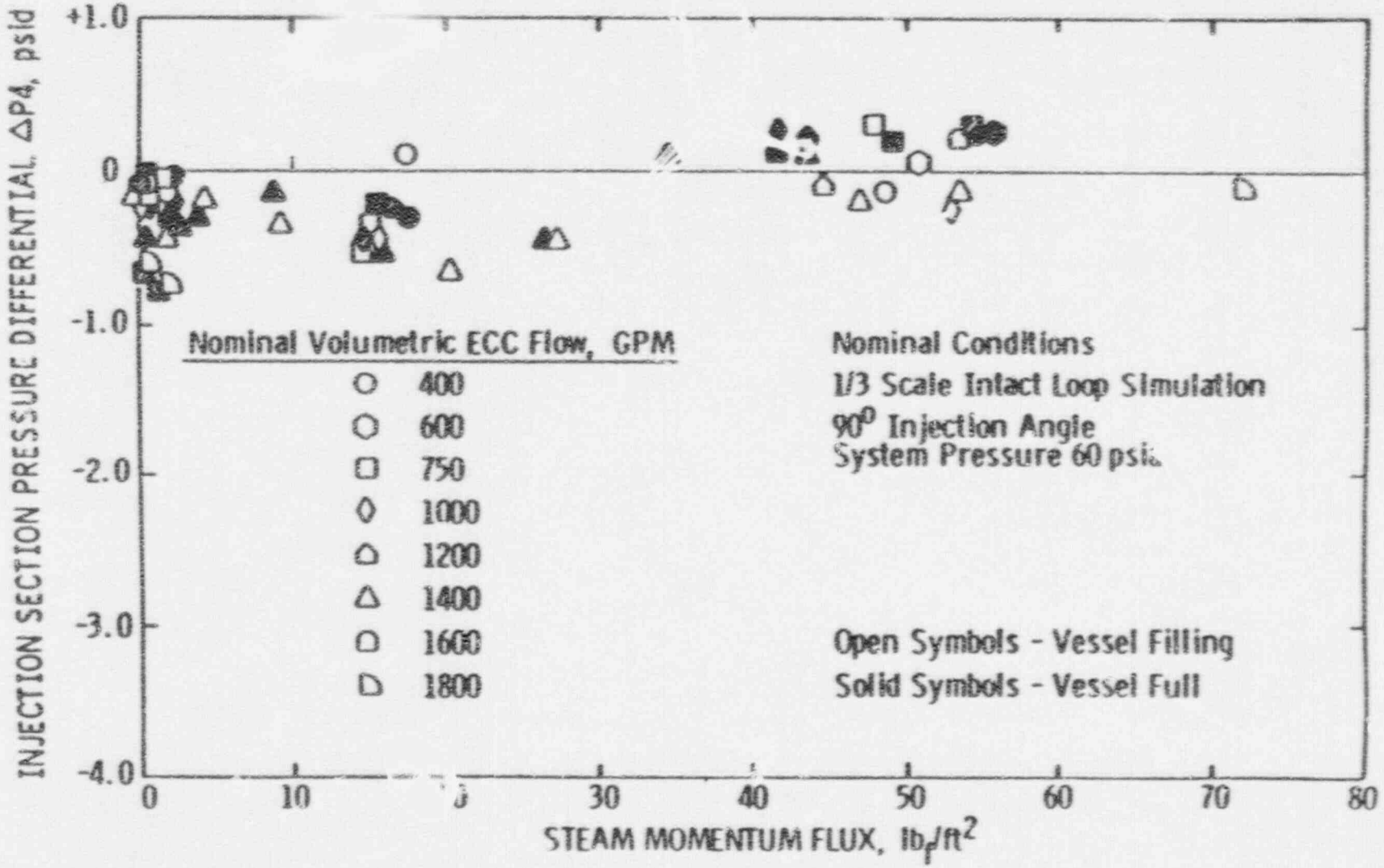


Figure 15.7

COLD LEG INJECTION SECTION PRESSURE DIFFERENTIAL vs STEAM MOMENTUM FLUX [15.3]



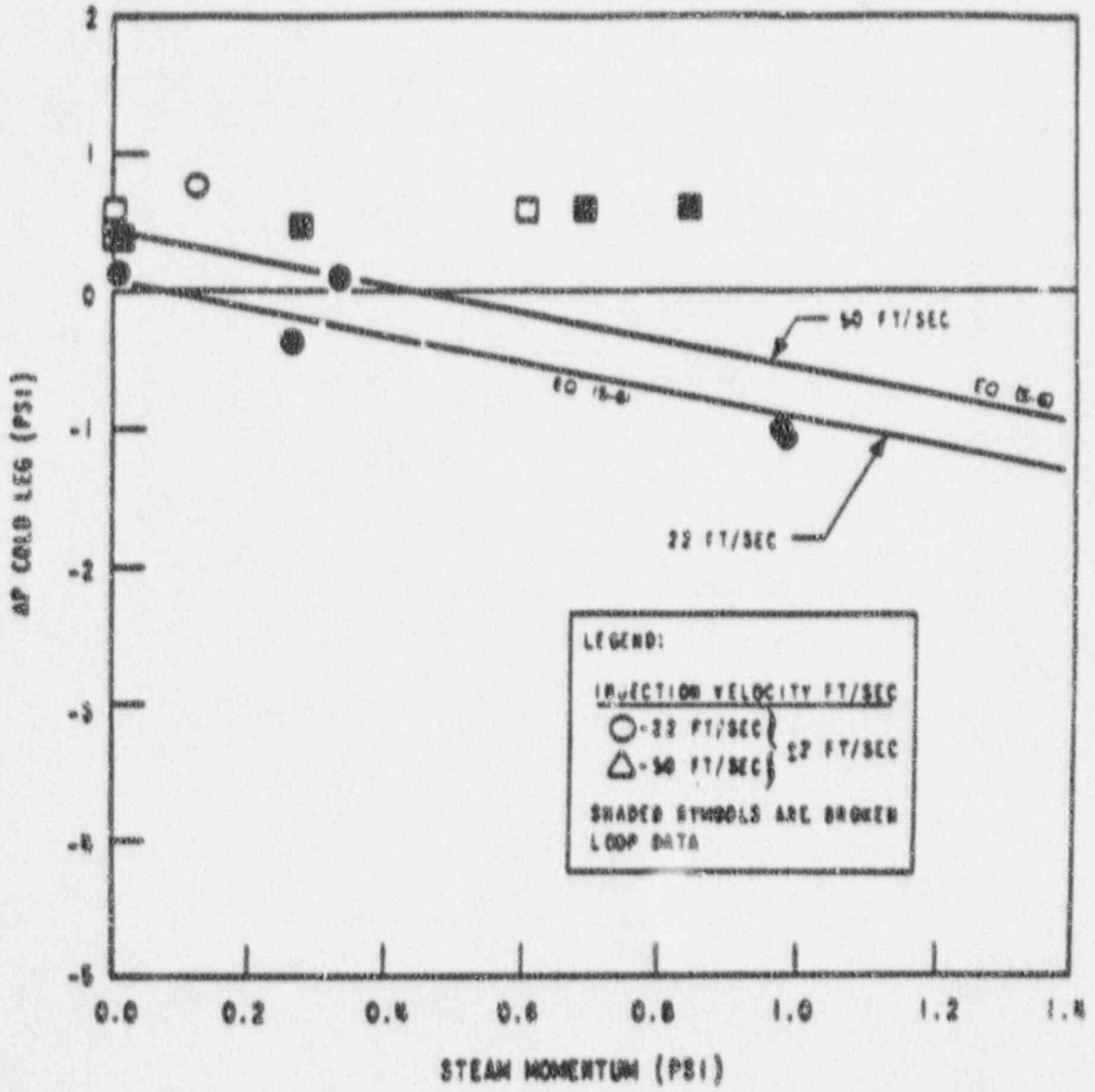



Figure 15-8 C-E Intact and Broken-Loop Data, 90° Injection, 1/5 Scale[15-1]

Figure 15-9
Pressure Differential vs Volumetric Water Flow for Series D
1/3 Scale Intact Loop [15.2]

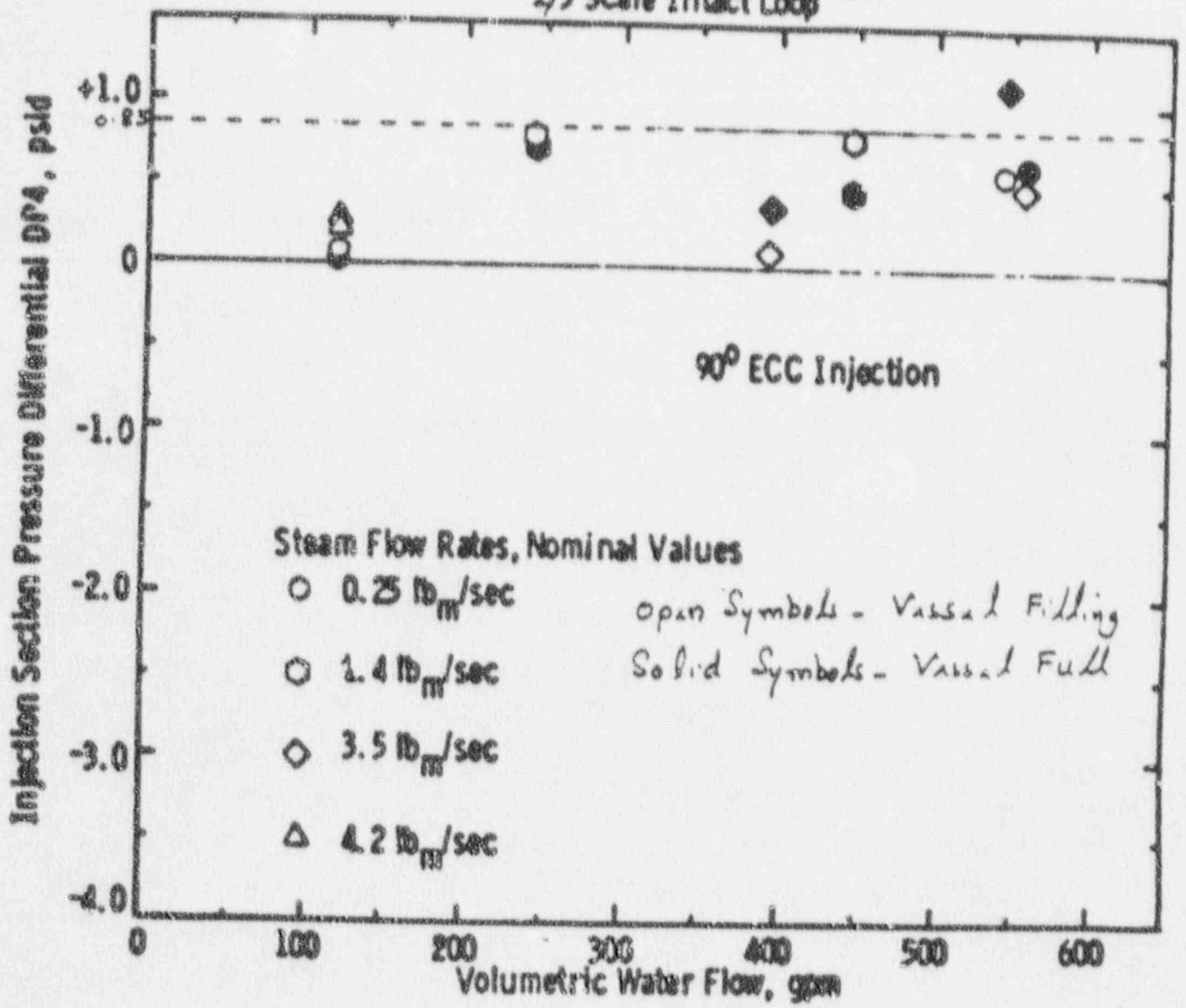
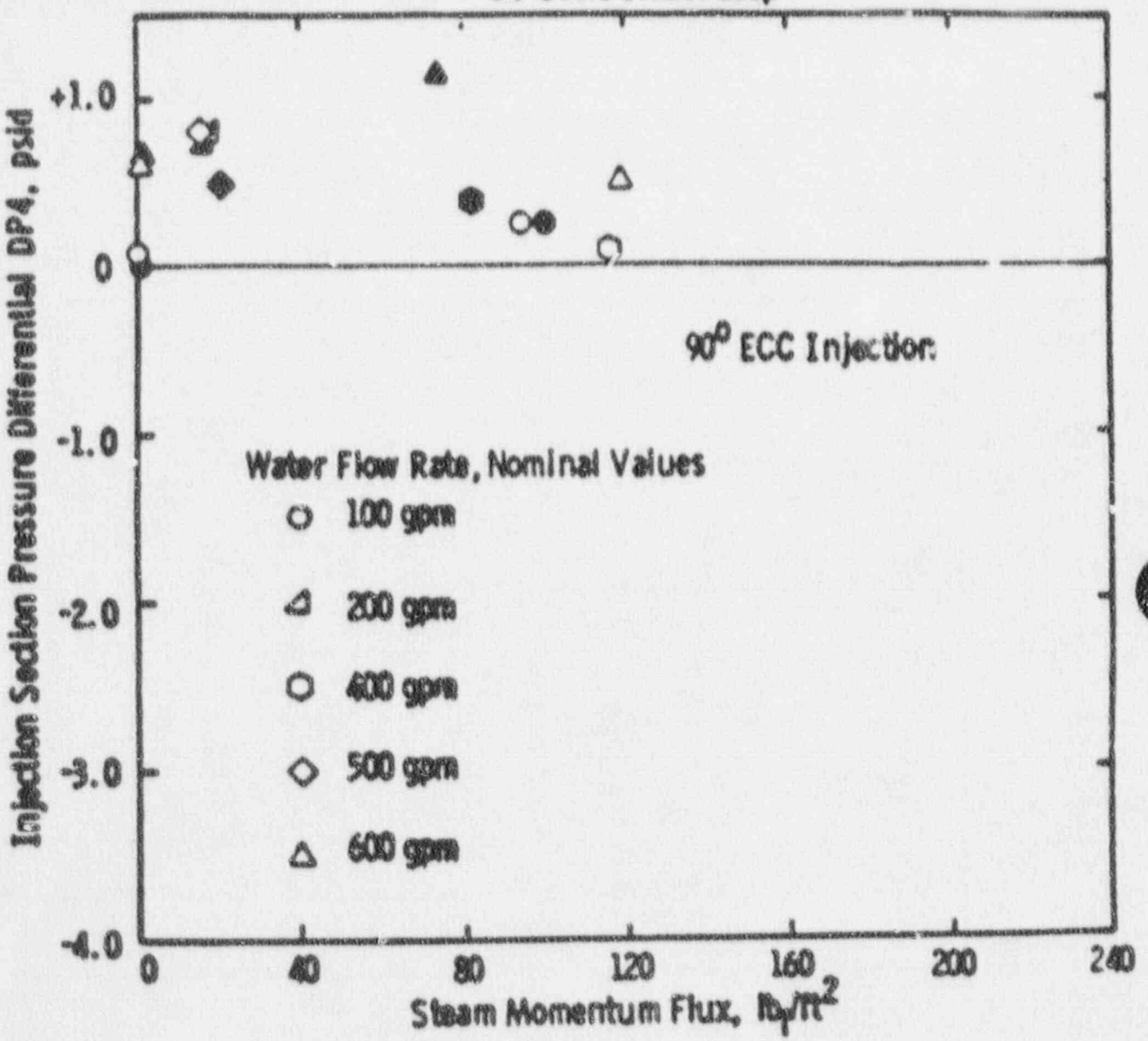


Figure 15-10

Pressure Differential vs Steam Momentum Flux for Series D
1/5 Scale Intact Loop[15.2]



16. In response to Question 5 of the NRC questions on RELAP5/MOD2-B&W, you stated that a two flow path junction model would be used to connect the hot and cold legs to the vessel. This modeling approach is not recommended by the original RELAP5/MOD2 code developers because of the possibility of unphysical recirculation flows being calculated between the three volumes connected this way. In addition, the crossflow junction was developed to specifically model this type of connection.

(a) In light of these considerations, justify using this nodalization technique.

Response: The dual flow path modelling technique and the RELAP5 cross flow junction are not intended to solve the same modelling difficulties. The cross flow junction is intended to move fluid laterally within or from a vertical column. Lateral movement of fluid is not the prime purpose of dual flow path modelling. For certain connections within the reactor coolant system model, there is a need to represent the steam-water separation effect that occurs during SBLOCA where vertical and horizontal regions are joined. At such locations, stratification of the fluids within the region is expected as the transient simulation progresses. Under stratified conditions, steam and liquid counter current flow occurs allowing liquid drain from the horizontal regions. The dual flow path approach directly addresses the stratified conditions by allowing the horizontal section to recognize differing densities across the mixture/steam only interface in the vertical section. This promotes the lateral transfer of liquid into the lower node of the vertical section, the separation of steam to the upper node of the vertical section, and the lateral transfer of steam from the upper node of the vertical section to the horizontal section. The flow paths to the horizontal section are given upward and downward orientation to further enhance the separation effect. The cross flow junction only partially addresses the modelling difficulties associated with the separation of steam and water.

Additionally, B&W has used the dual flow path approach to SBLOCA modelling for 15 years in various codes. It is an effective means of assuring that liquid will drain during slowly developing transients and eliminating the artificial steam pockets occasionally evident in the results of discrete node/flow path codes. The technique has been applied in RELAP5 for over 7 years, including both licensing and experimental benchmark activities. The dual flow path approach has repeatedly been shown to allow liquid draining without causing unphysical recirculation flows. The crossflow junction has not shown the same capability for enhancing liquid draining. BWFC does not considered the crossflow junction as an adequate solution to the problem of liquid draining. Therefore, BWFC will continue to use the dual flow path modelling where applicable.

(b) Because this technique was not used in the RELAP5/MOD2-B&W SBLOCA assessment calculations, clarify how this technique was assessed for use in licensing analyses.

Response: As discussed above, the dual flow path approach has been in use for many years at B&W. The response to question 11 of this set documents an application of the technique to reactor vessel outlet nozzles in the benchmark of the Semiscale S-LH-1 test. The base benchmark of S-LH-1, labeled the REVISED model, was preformed without the dual flow path technique. The latest benchmark, presented in the response to Question 11, applies the technique. Although the test simulates a larger break than those that benefit most from the dual flow path technique, differences between the two cases are evident in the liquid behavior of the upper plenum between the initiation of reflux boiling, about 100 seconds, and loop seal clearing for the intact loop (175 seconds). For the EM modelling, node 163 was split into two volumes accepting nodes 163 and 165 in Figure 16-1. These two nodes were then connected in a three way pattern to the hot legs. For the REVISED (non-dual flow pathed) case, node 163 dries out at 175

seconds, indicating that steam-liquid separation in the upper plenum and liquid draining from the hot legs is inhibited. For the EM case, the upper half of old node 163 (now node 165) dries out earlier than node 163 in the REVISED case, while the lower node retains a substantial and appropriate amount of liquid. The EM case results correspond to the behavior expected in the geometry of the upper plenum/hot leg connection and the base, REVISED, model produces unphysical results. This indicates that steam separation, counter current steam-liquid flow, and liquid draining back into the reactor vessel were more effectively modelled in the EM case. Figures 16-2 through 16-5 show the flows and junction void fractions to 500 seconds.

(c) Clarify what procedures B&W has in place to identify and correct the calculation of the unphysical recirculation flows.

Response: The possibility of unphysical recirculation flows is created by inadequacies in the RELAP5 solution technique. One reason why a radially split downcomer was abandoned for the BWFC large break evaluation model was the occurrence of unphysical recirculation flows within the downcomer noding after the initiation of accumulator injection. However, such flows must be induced by relatively large forcing functions between control volumes. The internode parameter differentials during SBLOCA calculations are small and will not lead to erroneous solutions by the RELAP5 solver, see Figures 16-2 through 16-4. To assure this during application of the evaluation model, the flows in all junctions within the dual flow path noding group will be plotted and examined for unphysical flow patterns. Should the net transport of steam and water across the connection exceed that expected for an accurate solution, corrective measures will be taken. The NRC will be informed of any deviations from the evaluation model required to correct such a problem; justification for the

correction will be provided; and NRC approval of the deviation will be sought.

FIGURE 16-1 DUAL FLOW PATHS NODING DIAGRAM

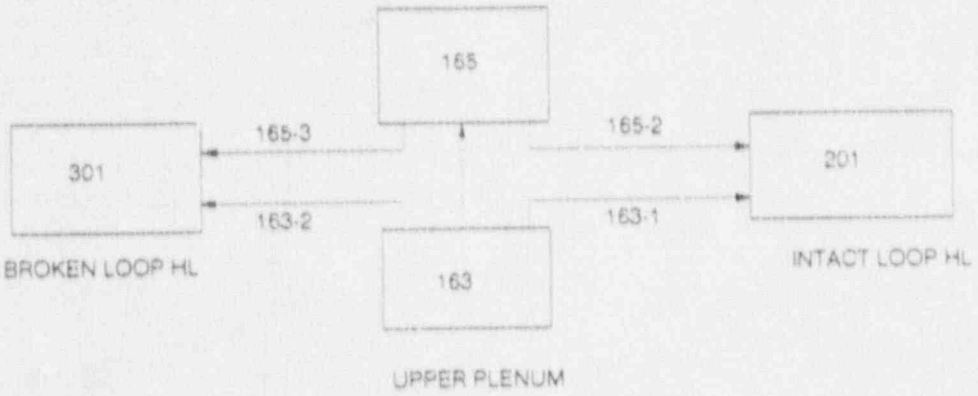



FIGURE 16-2. INTACT LOOP HOT LEG DUAL JUNCTION FLOWRATES

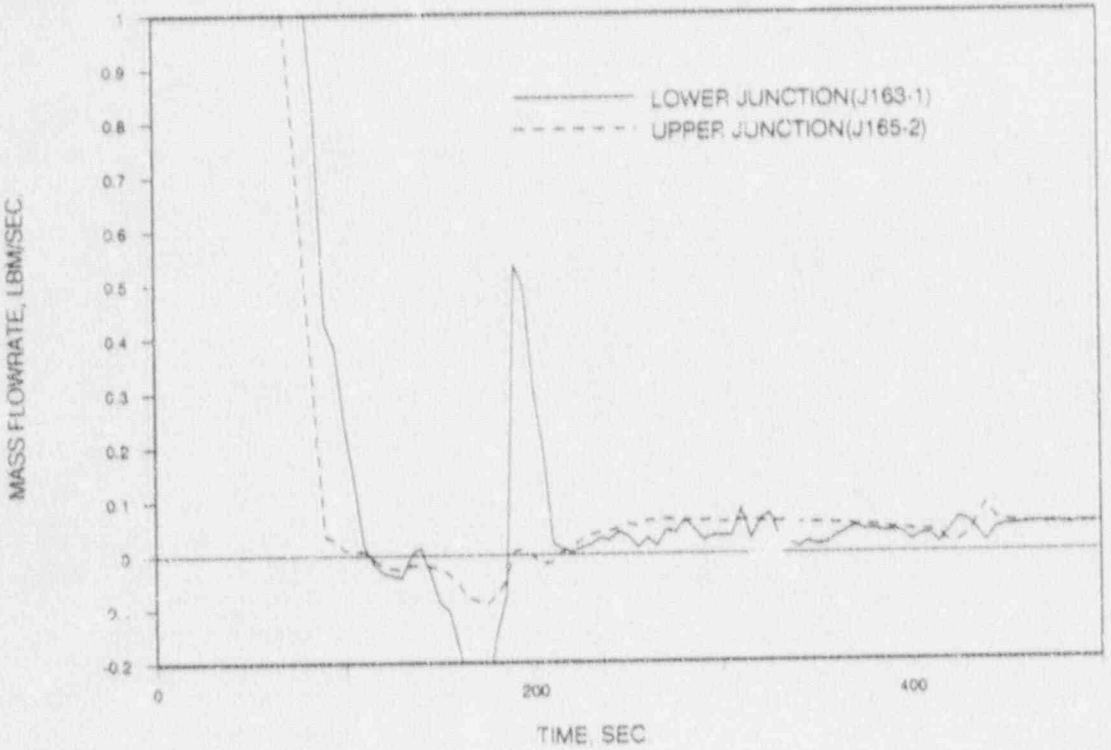


FIGURE 16-3. BROKEN LOOP HOT LEG DUAL JUNCTION FLOWRATES

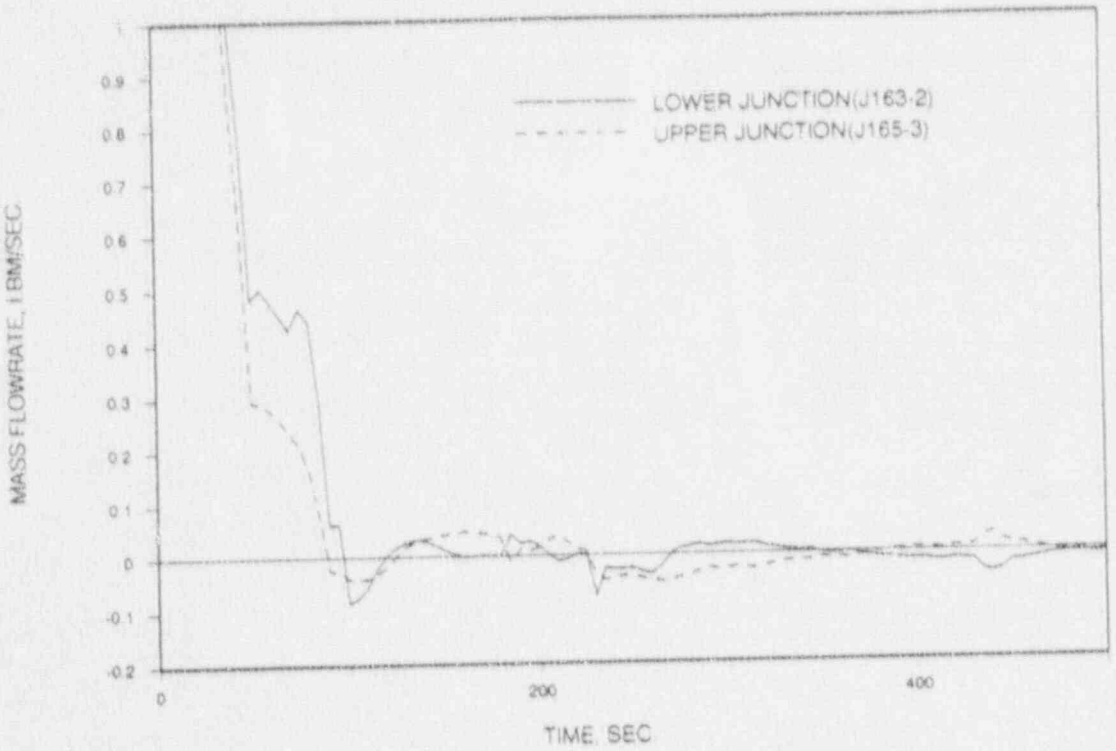



FIGURE 16-4. JUNCTION FLOW FROM VOLUMES 163 TO 165

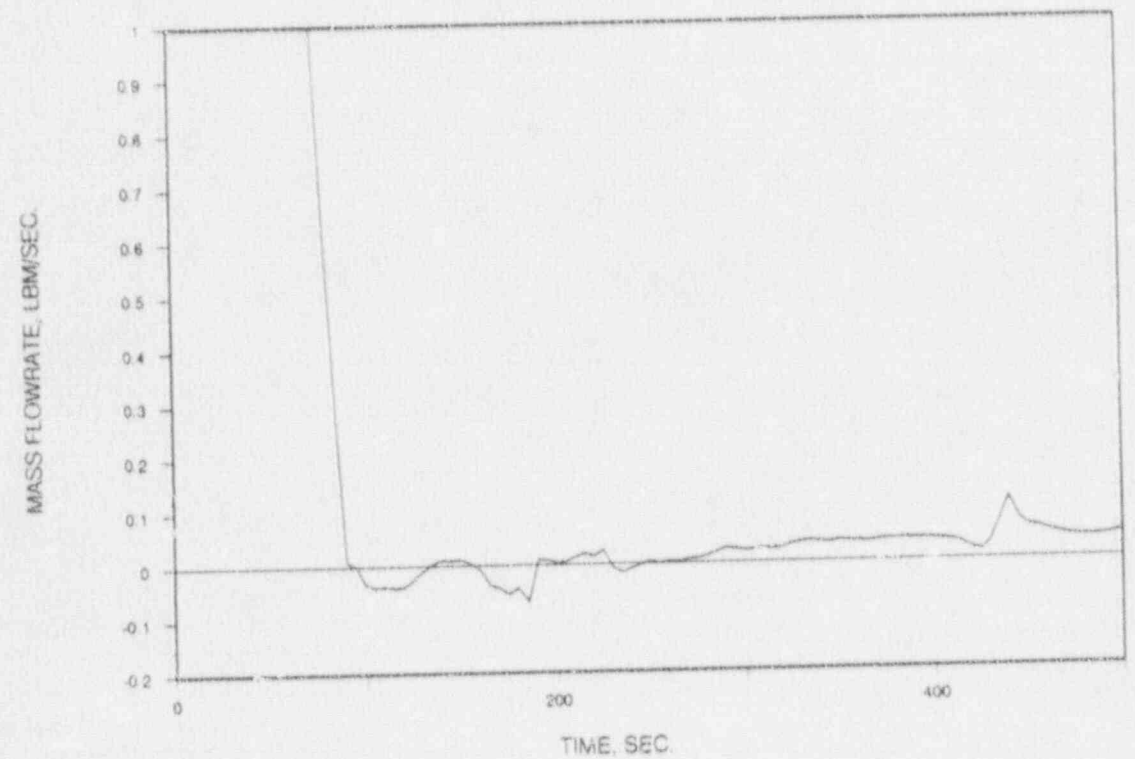


FIGURE 16-5 INTACT LOOP HOT LEG DUAL JUNCTIONS VOID FRACTION

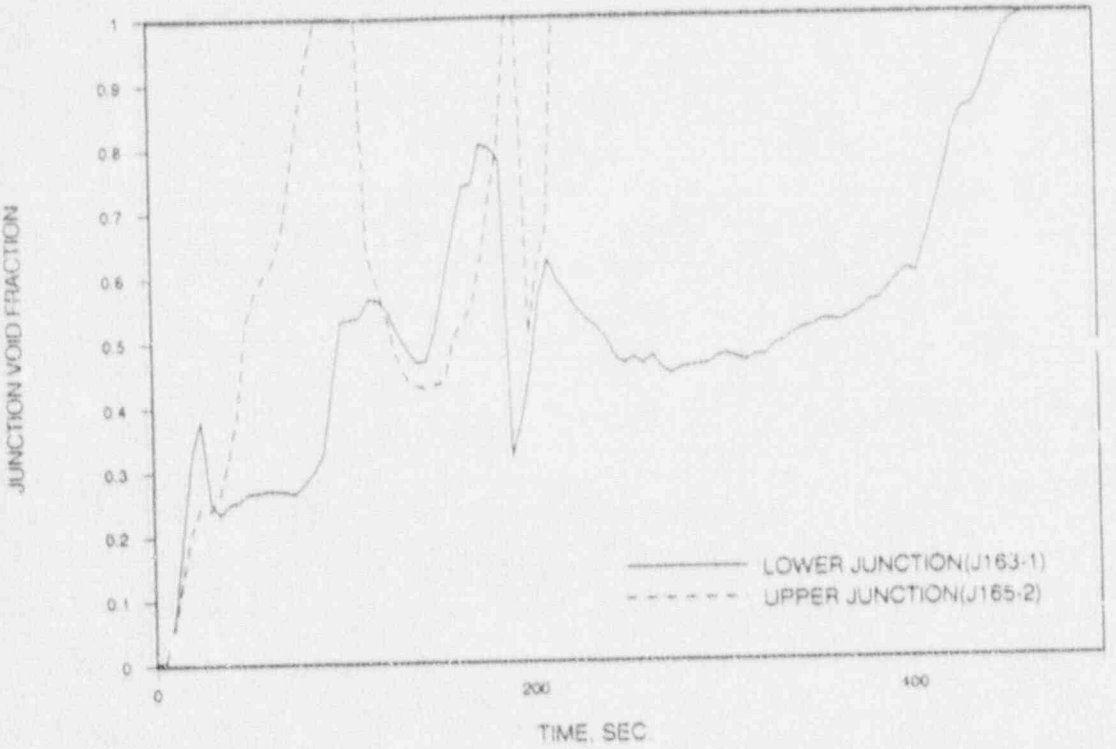
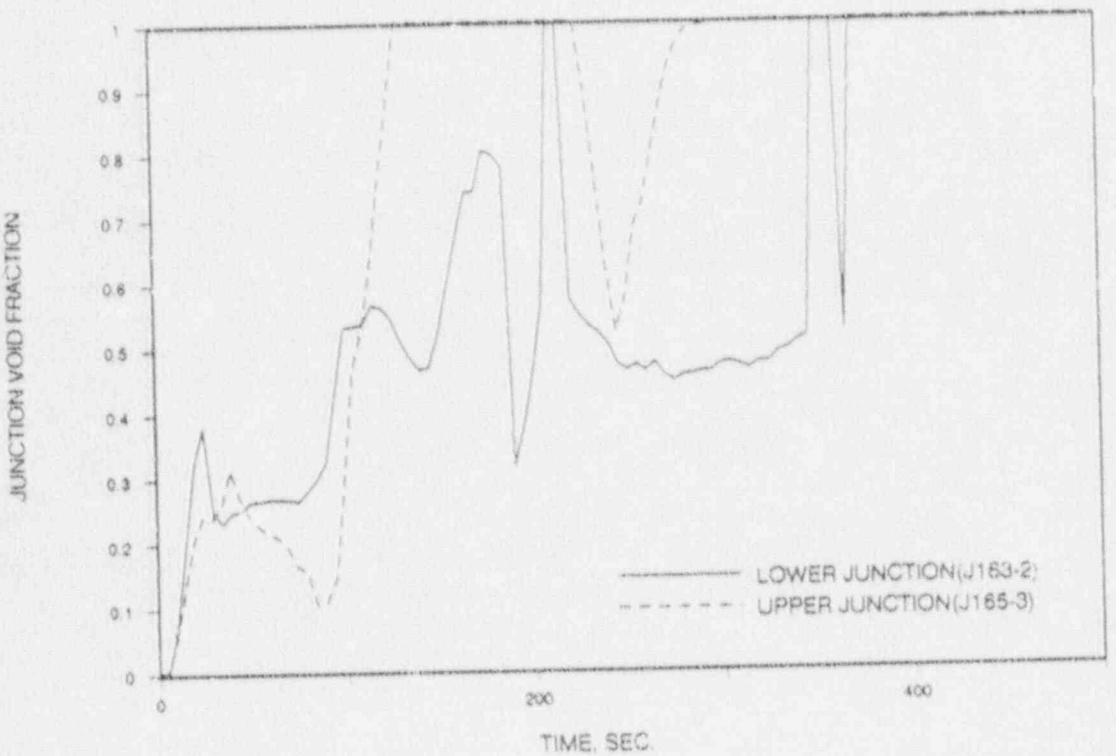



FIGURE 16-6 BROKEN LOOP HOT LEG DUAL JUNCTIONS VOID FRACTION



## ADDITIONAL INFORMATION for EVALUATION MODEL REVIEW

Developments in the BWFC evaluation model have occurred that alter the RELAP5/MOD2-B&W noding diagrams presented in volumes I and II of the evaluation model report, BAW-10168. Although the alterations are considered to lie within the original scope intended by the figures and words of the evaluation model, it has been decided to present new noding diagrams to clarify the current state of the RELAP5 models.

### Large Break Noding

The current noding for LBLOCA calculations with RELAP5/MOD2-B&W is illustrated in Figure 1a and 1b. This noding is for a Westinghouse 3411 Mwt class plant and would require some alteration for application to Combustion Engineering designs. Other regions of the model that may change according to the plant being analyzed are:

1) The secondary noding may require modification depending on the steam generator model. Typically the locations of the main and or auxiliary feed water nozzles will be adjusted as dictated by the design.

2) The user may employ more detailed noding in the upper plenum modelling if changes in upper plenum hardware and flow patterns warrant.

### Small Break Noding

The current noding for SBLOCA calculations with RELAP5/MOD2-B&W is illustrated in Figure 2a and 2b. As with the LBLOCA modelling, this noding is for a Westinghouse 3411 Mwt class plant and would require some alteration for application to Combustion Engineering

designs. Additionally, the secondary side and upper plenum modelling may be varied as in the LBLOCA model.

FIGURE 1a RELAP5/MOD2-B&W LBLOCA NODING DIAGRAM

REACTOR COOLANT LOOPS

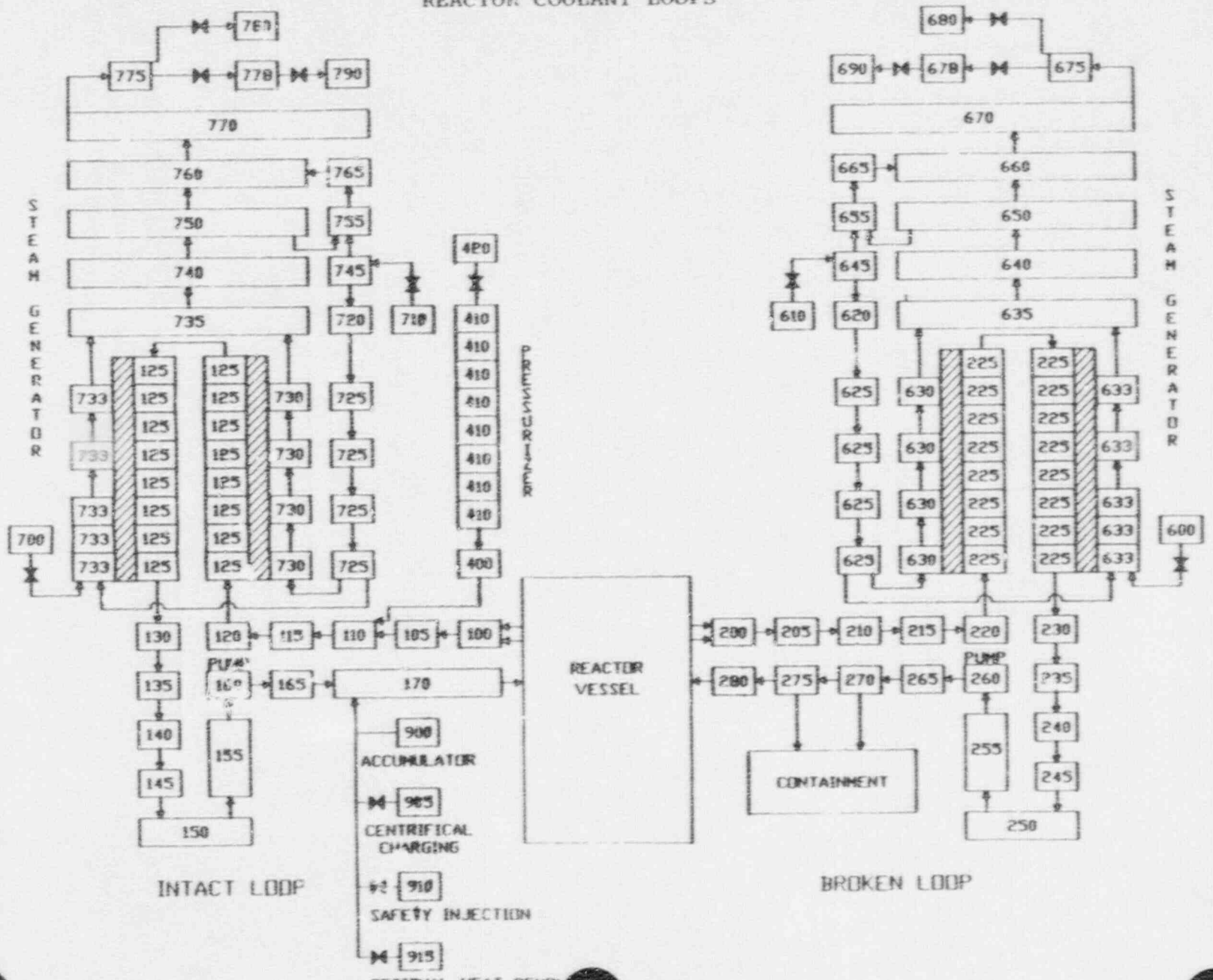


FIGURE 1b RELAP5/MOD2-B&W LBLOCA NODING DIAGRAM

REACTOR VESSEL

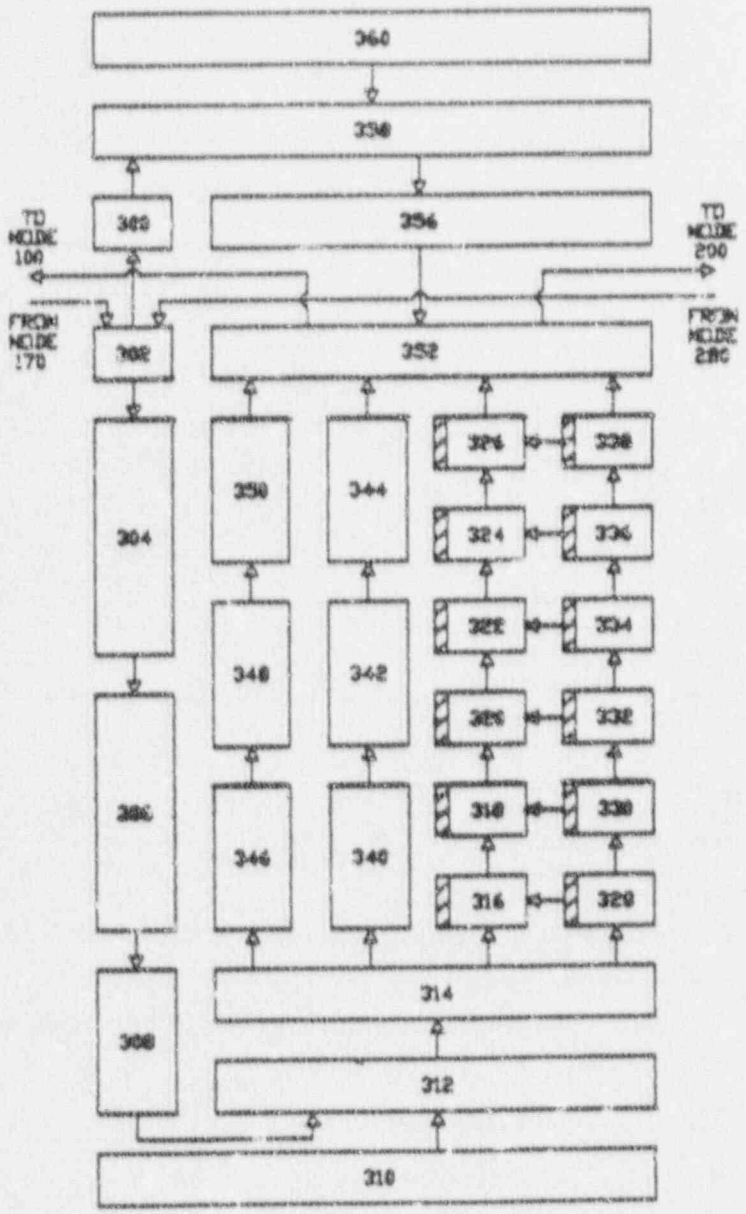
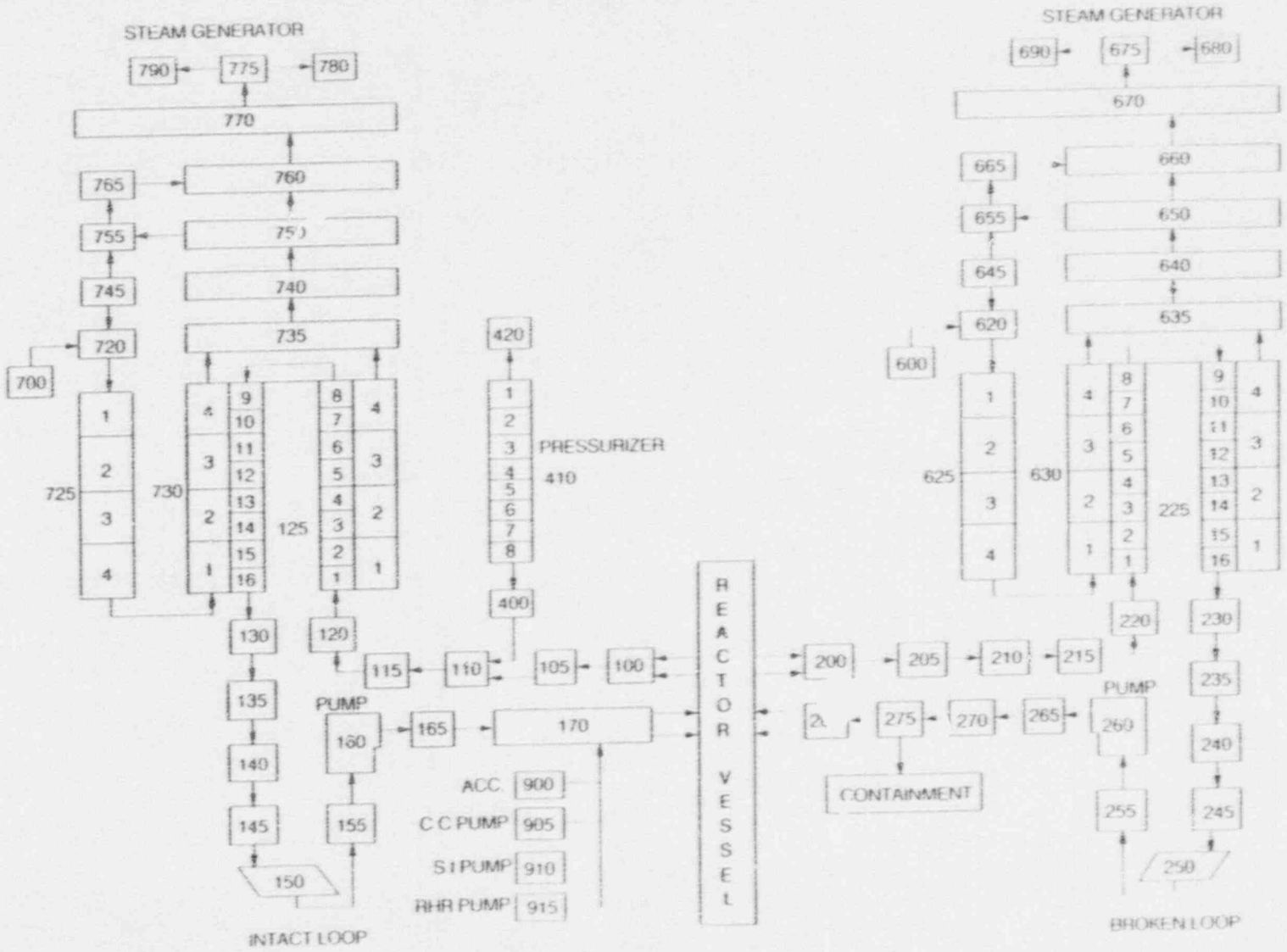


FIGURE 2a. RELAP5/MOD2 SBLOCA NODING FOR R.C LOOPS



FIGUPE 2b RELAP5/MOD2 SBLOCA NODING FOR REACTOR VESSEL