NUREG/CR-2837
PNL-4327

PNL Technical Review of Pressurized Thermal Shock Issues

Prepared by L.T. Pedersen, W.J. Apley, S.H. Bian, L.J. Defferding, M.H. Morgenstern, P.J. Pelto, E.P. Simonen, F.A. Simonen, D.L. Stevens, T.T. Taylor

Pacific Northwest Laboratory
Operated by
Battelle Memorial Institute

Prepared for
U.S. Nuclear Regulatory Commission

NOTICE

Availability of Reference Materials Cited in NRC Publications

Most documents cited in NRC publications will be available from one of the following sources:

1. The NRC Public Document Room, 1717 H Street, N.W.
   Washington, DC 20555
2. The NRC/GPO Sales Program, U.S. Nuclear Regulatory Commission,
   Washington, DC 20555
3. The National Technical Information Service, Springfield, VA 22161

Although the listing that follows represents the majority of documents cited in NRC publications, it is not intended to be exhaustive.

Referenced documents available for inspection and copying for a fee from the NRC Public Document Room include NRC correspondence and internal NRC memoranda; NRC Office of Inspection and Enforcement bulletins, circulars, information notices, inspection and investigation notices; Licensee Event Reports; vendor reports and correspondence; Commission papers; and applicant and licensee documents and correspondence.

The following documents in the NUREG series are available for purchase from the NRC/GPO Sales Program: formal NRC staff and contractor reports, NRC-sponsored conference proceedings, and NRC booklets and brochures. Also available are Regulatory Guides, NRC regulations in the *Code of Federal Regulations*, and *Nuclear Regulatory Commission Issuances*.

Documents available from the National Technical Information Service include NUREG series reports and technical reports prepared by other federal agencies and reports prepared by the Atomic Energy Commission, forerunner agency to the Nuclear Regulatory Commission.

Documents available from public and special technical libraries include all open literature items, such as books, journal and periodical articles, and transactions. *Federal Register* notices, federal and state legislation, and congressional reports can usually be obtained from these libraries.

Documents such as theses, dissertations, foreign reports and translations, and non-NRC conference proceedings are available for purchase from the organization sponsoring the publication cited.

Single copies of NRC draft reports are available free upon written request to the Division of Technical Information and Document Control, U.S. Nuclear Regulatory Commission, Washington, DC 20555.

Copies of industry codes and standards used in a substantive manner in the NRC regulatory process are maintained at the NRC Library, 7920 Norfolk Avenue, Bethesda, Maryland, and are available there for reference use by the public. Codes and standards are usually copyrighted and may be purchased from the originating organization or, if they are American National Standards, from the American National Standards Institute, 1430 Broadway, New York, NY 10018.

GPO Printed copy price: $6.50

NUREG/CR-2837

PNL Technical Review of Pressurized Thermal Shock Issues

Manuscript Completed: June 1982
Date Published: July 1982

Prepared by
L.T. Pedersen, W.J. Apley, S.H. Bian, L.J. Defferding, M.H. Morgenstern,
P.J. Pelto, E.P. Simonen, F.A. Simonen, D.L. Stevens, T.T. Taylor

Pacific Northwest Laboratory
Richland, WA 99352

Prepared for
Division of Safety Technology
Office of Nuclear Reactor Regulation
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555
NRC FIN B2510

ABSTRACT

Pacific Northwest Laboratory (PNL) was asked to develop and recommend a regulatory position that the Nuclear Regulatory Commission (NRC) should adopt regarding the ability of reactor pressure vessels to withstand the effects of pressurized thermal shock (PTS). Licensees of eight pressurized water reactors provided NRC with estimates of remaining effective full power years before corrective actions would be required to prevent an unsafe operating condition. PNL reviewed these responses and the results of supporting research and concluded that none of the eight reactors would undergo vessel failure from a PTS event before several more years of operation. Operator actions, however, were often required to terminate a PTS event before it deteriorated to the point where failure could occur. Therefore, the near-term (less than one year) recommendation is to upgrade, on a site-specific basis, operational procedures, training, and control room instrumentation. Also, uniform criteria should be developed by NRC for use during future licensee analyses. Finally, it was recommended that NRC upgrade nondestructive inspection techniques used during vessel examinations and become more involved in the evaluation of annealing requirements.

EXECUTIVE SUMMARY

Pacific Northwest Laboratory (PNL) was asked by the Nuclear Regulatory Commission (NRC) to develop and recommend a near-term (<1 year) regulatory position that NRC should adopt to avoid or mitigate pressurized thermal shock (PTS) at nuclear power plants. The PNL technical staff and several independent consultants, who provided an overview of the program, evaluated what corrective actions, if any, must be taken before longer-term PTS generic resolution and acceptance criteria are established. Responses to NRC's request for information are still being received from licensees and owners groups. In this regard, the PNL review is limited to information available through May 1982.

The responses considered several classes of overcooling scenarios which could lead to a PTS event. For all scenarios, it was concluded that none of the eight reactors under review would undergo vessel failure should a PTS event occur before several more years of operation and, in most cases, before the end of reactor life. However, in many scenarios, operator actions were required to terminate the event before it deteriorated to a state where the conditions necessary for vessel failure were present. The NRC evaluation of PTS procedures and operator training at two of the eight plants indicated deficiencies in these areas. Therefore, it is recommended that procedures, training, and control room instrumentation be changed on a site-specific basis in the near- to long-term period.

In addition, the responses differed in terms of event conditions, assumptions, and acceptance criteria beyond what would be expected because of plant-specific situations. It is therefore recommended that uniform criteria be used to evaluate the effective full power years (EFPY) remaining before further corrective actions are required. Adopting these criteria may shorten the projected remaining EFPY under some PTS event scenarios, but it should not require additional corrective actions in the near-term.

To provide a data base on flaws, it is recommended that vessel inspections incorporate currently available, improved nondestructive inspection techniques; that a demonstration of inspection procedures be required; and that a standard method of reporting results of vessel inspections be developed. It is also recommended that an inspection be performed following a PTS event when analyses predict the potential for the initiation of a crack. The presence of a flaw in the highly irradiated vessel area is a necessary condition for crack propagation during a PTS event. However, due to the lack of definitive information, it is necessary to assume a conservatively large flaw during the fracture mechanics analyses. It is also recommended that the NRC more actively participate in evaluating vessel annealing. The NRC will need to consider the operational and safety questions concerning the vessel, piping components, supports, and other structural members. At this time, it would be appropriate for NRC to start drafting analyses requirements; methods for determining the new vessel material properties following annealing, inspection, and approval requirements; and any regulatory changes that may be necessary.

CONTENTS

FIGURES

TABLES

ABBREVIATIONS

ANSI	American National Standards Institute
ASME	American Society of Mechanical Engineers
CRT	cathode ray tube
DOT	Discrete Ordinate Transport
DPA	displacements per atom
ECCS	emergency core cooling system
EFPY	effective full power years
EOL	end of life
EPRI	Electric Power Research Institute
FAD	failure assessment diagram
FM	failure modes
HEDL	Hanford Engineering Development Laboratory
HFP	hot full power
HPI	high pressure injection
HSST	heavy section steel technology
HZP	hot zero power
INPO	Institute of Nuclear Power Operations
LER	Licensee Event Reports
LBLOCA	large-break loss-of-coolant accident
LOCA	loss-of-coolant accident
MAP	mitigating actions package
MSLB	main steam line break
NDE	nondestructive evaluation
NDT	nil-ductility transition
NRC	Nuclear Regulatory Commission
NSSS	Nuclear Steam Supply System
NTOL	near-term operating licensee
OMS	overpressure mitigating system
ORNL	Oak Ridge National Laboratory
P-T	pressure-temperature
PNL	Pacific Northwest Laboratory
PORV	Power (Pilot) Operated Relief Valve
PRA	probabilistic risk assessment
PTS	pressurized thermal shock
PV	pressure vessel
PWR	pressurized water reactor
PZR	pressurizer
RCP	reactor coolant pump
RCS	reactor coolant system
RFT	runaway feed transient
RT_{NDT}	nil-ductility transition reference temperature
SBLOCA	small-break loss-of-coolant accident
SG	steam generator
SI	safety injection
SIS	safety injection system
SLB	steam line break
SPDS	safety panel display system

ABBREVIATIONS (continued)

SRV	safety relief valve
SSLB	small steam line break
STA	shift technical assistant
t	thickness
T	temperature
TAP	task action plan
T-H	thermal-hydraulics
TBV	turbine bypass valve
TMI	Three-Mile Island
TTC	through-thickness crack
VISA	Vessel Integrity Simulation Analysis

ACKNOWLEDGMENTS

This report contains an account of the work sponsored by the Division of Safety Technology, Office of Nuclear Reactor Regulation of the U.S. Nuclear Regulatory Commission. This work was completed under the direction of Roy Woods and Marty Virgilio, project technical monitor and technical assistant, respectively, Generic Issues Branch of the Division of Safety Technology.

Contributions from many NRC staff members and research personnel performing work directed at resolving the pressurized thermal shock issue are acknowledged and credit is noted in the report, but not nearly to the appropriate extent.

The consultants who provided a broader overview of technical and nuclear safety issues are listed in Section 1.4. Their assistance and constructive comments within the short response times were informative and helpful.

Finally, the extraordinary efforts of many PNL staff members are acknowledged. Without their assistance, the difficult deadlines could not have been met. Special thanks are given to Peggy Snyder and Cathy Darby for typing and word processing, and to Carolynn Novich for editorial assistance.

1.0 INTRODUCTION

1.1 BACKGROUND

The pressure vessel of a nuclear plant is subjected to a pressurized thermal shock (PTS) when an extended cooling transient to the vessel wall is accompanied by system pressurization. Under these conditions, thermal and pressurization stresses on the internal surfaces of the vessel are additive. Moreover, these stresses are in tension and tend to open cracks located at or near the internal surfaces.

Nuclear plant pressure vessels are fabricated from ferritic steels. The internal surfaces of the vessels are clad with stainless steel weld to prevent metal corrosion processes. The vessels are designed to withstand normal heating and cooling transients for the life of the plant, which is usually 40 years at 80% operating efficiency or 32 effective full-power years (EFPY). A pressure vessel intended for 32 EFPY must be designed to maintain fracture toughness of the vessel material. An adequate level of fracture toughness provides assurance that small cracks will not propagate in a "brittle" manner as a result of stresses during an abnormal transient such as a PTS event. Failure in a brittle manner could fracture the vessel wall and lead to severe failure of the pressure boundary in the core area. In contrast, a ductile type of failure would be expected to result, at worst, in a through-vessel crack, which would leak but not result in a total loss of the pressure boundary.

In older nuclear plants, the pressure vessels were often fabricated with weld materials containing relatively high levels of copper, phosphorus, and nickel. These elements were later shown to result in greater irradiation damage to the vessel material than had been initially expected. Irradiation damage caused a shift in the fracture toughness curve to higher temperatures and, therefore, increased the remote possibility of a nonductile vessel failure.

Evaluating the failure probability of any nuclear pressure vessel is very complex. The evaluation must be plant-specific to allow for differences in material properties of the plant components, systems configuration, operating procedures, and dosimetry history. The plant control systems, component redundancy, operating history, and operator training and proficiency are important in determining the initiation, sequence, and timing of accident-type events and in evaluating the probability of mitigating operator actions. Finally, the thermal-hydraulic, material properties, and fracture mechanics analyses, using currently available codes, are used to determine the consequences of the events being analyzed.

The following conditions must be present during a PTS event before a significant nonductile failure probability would be expected:

- The nuclear plant pressure vessel must exhibit significant loss of fracture toughness through neutron irradiation.

- An overcooling transient must occur that would be of sufficient duration to cause a steep thermal gradient across the vessel wall and cooling to the low-toughness temperature range.

- A flaw must be present of sufficient size and be located at a critical beltline location where reduced fracture toughness and high thermal stress exist.

- A simultaneous high reactor coolant system pressure must be present.

In recent years a number of incidents have occurred that involved several, but not all, of the above conditions. The PTS issue is, therefore, being investigated in much greater detail by the NRC, the utility industry, and Nuclear Steam Supply System (NSSS) contractors.

1.2 OBJECTIVE OF STUDY

Pacific Northwest Laboratory is providing technical assistance to NRC to develop and recommend a regulatory position that NRC should adopt before the longer-term PTS program provides generic resolution and acceptance criteria. The near-term recommendations include any corrective actions required at the eight plants identified in the August 21, 1981 NRC letter.[1] The recommendations of this report are based on the review of information described in Section 1.3.

1.3 APPROACH

Eight pressurized water nuclear power plants (Ft. Calhoun, H. B. Robinson 2, San Onofre 1, Maine Yankee, Oconee 1, Turkey Point 4, Calvert Cliffs 1, and Three-Mile Island 1) have been identified for specific review of PTS event scenarios. These plants and the NSSS owners groups have supplied information in response to NRC requests.[2,3,4] The following sources of information were used by PNL to recommend NRC's near-term regulatory position.

1. Documentation by the licensees and owner groups to the NRC requests for information concerning the PTS issue.

2. Participation in reviewing current procedures, training, and operator responses to PTS events at selected plants as established by the NRC's PTS task force on procedure review.

3. Reviews of research work being performed in support of the PTS issue at NRC, national laboratories, industry, and other research institutes.

4. Reference documents which are pertinent to the PTS issue or technical areas important to this issue.

The report contained herein was completed using the above information and information from site visits, where appropriate, to establish the methodologies, procedures, sensitivities, and completeness of the various technical areas. This report has also been critiqued by a selected group of nationally known consultants within various technical areas of the program.

1.4 LIST OF STUDY PARTICIPANTS

The following staff members and consultants participated in the multidisciplinary study:

Pacific Northwest Laboratory

Program Management
L. T. Pedersen, Manager, Special Projects, Engineering Physics Department

Probabilities and Risk Assessments
P. J. Pelto, Senior Engineer, Systems Safety Analysis Section

Systems Operation
W. J. Apley, Technical Leader, Nuclear Systems, Energy Systems Engineering Section
L. J. Defferding, Senior Engineer, Energy System Engineering Section

Human Factors
M. H. Morgenstern, Technical Leader, Human Factors Engineering, Mechanical Design and Development Section

Thermal Hydraulics
S. H. Bian, Senior Engineer, Fluid and Thermal Engineering Section

Vessel Material Properties
E. P. Simonen, Technical Leader, Nuclear Metallurgy, Metallurgy Research Section

Fracture Mechanics
F. A. Simonen, Technical Leader, Structural Analysis, Structures and Mechanics Section

Nondestructive Evaluation
T. T. Taylor, Senior Engineer, Nondestructive Testing Section

Statistical Analyses
D. L. Stevens, Senior Engineer, Statistics Section

Consultants

Probabilities and Risk Assessment
Dr. Robert Budnitz, Future Resources Associates, Inc.

Human Factors
Dr. Julien Christensen, General Physics Corporation

Systems Operation
Mr. Reese J. Bursey, Nuclear Personnel Qualification, Inc.

Thermal Hydraulics
Dr. Donald S. Rowe, Rowe and Associates

Vessel Material Properties
Dr. Spencer H. Bush, Senior Staff Scientist, PNL

Fracture Mechanics
Prof. George T. Hahn, Vanderbilt University, Mechanical and Material Department

Statistical Analysis
Dr. Carl A. Bennett, Human Affairs Research Center, PNL

2.0 CONCLUSIONS AND RECOMMENDATIONS

The conclusions reached within the technical sections of this report support the continued operation of the eight plants under review. However, operator mitigating actions are required to reduce the probability that abnormal overcooling events will deteriorate into the pressurized thermal shock region of concern. Therefore, recommendations are made for corrective actions to procedures, training, and control room instrumentation on a near-term, intermediate, and long-term schedule.

The analyses provided by the licensees did not completely treat all aspects of the PTS issue. To provide acceptable, complete analyses, criteria were developed and are recommended for future PTS analyses. Finally, recommendations are made to upgrade nondestructive inspection techniques used to examine reactor vessel welds, and to improve the NRC VISA code.

The recommendations are of three types: corrective actions, regulatory revisions, and changes or additions to the PTS support program.

2.1 EVENT SCENARIOS

Conclusions

Event scenarios can be used to help predict the degree to which PTS threatens the continued safe operation of commercial nuclear facilities. Currently, the scenarios are generic in nature; therefore, unless it can be rigorously shown that the generic corrective actions needed to avoid or mitigate a PTS event lead to consistent improvement in individual plant safety, safety actions should be established on a plant-specific basis. A more detailed evaluation of event scenarios is provided in Chapter 3.0.

Recommendations

The following recommendations need to be taken to support mitigating actions for the four generic event scenarios. These recommendations need to be implemented to ensure that the plant operating staff (given the existing, design-basis control systems) have the training, equipment, and procedures necessary to preclude and/or mitigate potential PTS events. These recommendations discuss additional analyses required, proposed corrective actions, and methods to implement those actions.

1. Based on the studies that have been performed to date, severe PTS scenarios have an estimated frequency in the range of 10^{-3} to 10^{-6} for generic Babcock & Wilcox, Combustion Engineering, and Westinghouse plants. Additional work is necessary to support these values and should include a more systematic identification process for potential PTS scenarios, a more detailed consideration of operator error, and a consistent treatment of multiple failures and potential dependencies.

2. The time required for operator action plays a critical role in determining whether a potential PTS scenario may cause crack initiation. Therefore, it is essential that, as a part of the procedure upgrade implemented under Item I.C.1 of the Three Mile Island (TMI) task action plan, the following items be addressed:

   a. PTS-related procedures need to be audited using time-line charts to identify critical time constraints and ensure that they are clearly noted to the operator.

   b. Simulator and in-plant testing need to be conducted to establish reasonable generic estimates for operator response times for certain critical PTS evolutions (example: A percent of operators can perform evolution B within C minutes.)

   c. Licensee Event Reports (LERs) need to be reviewed to ascertain actual time-related information (including data on operator error) which can be used to supplement and validate information obtained in items a and/or b above.

3. Operator action has been identified as a major element in initiating, preventing, or mitigating PTS scenarios. Recommendations provided below address training and development of improved "tools" for the operator's use. Individual actions are prioritized into three categories: near-, intermediate-, and long-term. Recommended actions are either operator-oriented (including training), instrumentation-oriented, or procedure-oriented. Expanded discussion of each recommendation is contained in Chapter 3.0. Numbering corresponds to that used within Section 3.8.

   Near-Term Implementation (<1 yr)

   Procedure-oriented recommendations:

   1. improve general criteria
   2. require short-time reactor coolant system (RCS) cooldown rate limits
   3. improve RCS pressure control guidance
   4. improve RCS cooldown control guidance.

   Operator-oriented recommendations:

   1a. review overcooling events
   1b. provide PTS transient control classroom instruction (shift walk-through)
   1c. conduct PTS simulator training
   1d. conduct special transient-control simulator training
   1e. conduct nil-ductility transition (NDT) and PTS theory classroom training.

   Instrumentation-oriented recommendations:

   1. add temperature-based subcooling meter indication

Intermediate-Term Implementation (1-2 yr)

Procedure-oriented recommendations:
5.* review high pressure injection (HPI) termination pressure requirement
6.* establish post-transient "hold points"
7.* reduce saturation margin requirements.

Instrumentation-oriented recommendations:
2a. add instantaneous RCS cooldown rate monitor
2b. add integrated RCS cooldown rate monitor
3. improve RCS pressure readout.

Long-Term Implementation (>2 yr)

Procedure-oriented recommendations:
8.* incorporate variable saturation margin.

Instrumentation-oriented recommendations:
2c.*add RCS cooldown rate change indicator
4.* add NDT margin meter
5. improve steam generator level instrumentation
6.* add transient warning indicators.

4. Because nuclear facilities differ in terms of design and operating modes, each plant should be required to prepare a PTS mitigating actions package (MAP) which describes how that facility intends to implement the above listed recommendations (or technical justification for exceptions). The PTS MAPs should be reviewed and approved within an established time frame, and audits should be performed on a periodic basis to reaffirm continued facility attention to and action on the PTS issue.

2.2 THERMAL-HYDRAULICS

The thermal-hydraulic conditions in the reactor vessel downcomer provide the basic driving conditions for PTS. Driving conditions can be identified through the selection of specific accident scenarios and the use of analytical methods to calculate thermal-hydraulic conditions such as pressure, temperature, and heat transfer in the reactor vessel downcomer. The following conclusions and recommendations are from the thermal-hydraulic evaluation of PTS (Chapter 4.0).

---

* Require detailed analyses.
NOTE: Within the individual categories and classifications (e.g., instrumentation-oriented), recommended mitigating actions are listed in order of descending priority.

Conclusions

1. Transient Scenario Evaluations. Three major classes of transient scenarios were identified by the utility owners groups for PTS analysis: small-break LOCA (SBLOCA), main steam line break (MSLB), and small steam line break (SSLB).

   Even though the pressure and temperature behaviors for each scenario are plant dependent, a more critical factor in almost all cases reviewed is the operator action and the time allowed for the corrective actions for bringing the coolant conditions within the PTS safety regions. The sensitivity of the action time versus thermal-hydraulic behaviors were not addressed by the utilities. (See Chapter 3.0 for further discussion.)

   None of the utility responses addressed the issue of noncondensable gases. There are transient scenarios in which the primary system pressure could drop sufficiently to allow injection of emergency core cooling system (ECCS) water and nitrogen (noncondensable gas) from the ECCS accumulator. This is of concern because it affects the pressure-temperature relationship observed by the operator in the control room, and it could inhibit the re-establishment of natural circulation flow.

2. Analytical Methods. The analytical methods from the owners groups responses included three major factors in the thermal-hydraulic analysis: 1) system analysis to define bulk temperature and pressure in the vessel downcomer; 2) mixing analysis to define the effects of cold high pressure injection (HPI); and 3) the calculation of heat transfer at the vessel wall.

   - System Analysis. All the system codes used by the owners groups are one-dimensional codes based on homogeneous, two-phase flow. The codes also assume thermal equilibrium. The Combustion Engineering and Westinghouse models have provisions for relative phase velocity to account for the separation of steam and water in slowly moving two-phase flows. The Babcock & Wilcox model assumes equal phase velocity and would apply to single-phase flows or to conditions where homogeneous two-phase flow would exist. It would not apply to cases of vertical, low velocity, counter-current, steamwater flow where phase separation could occur. This could be especially important regarding natural circulation flow in the reactor loops or flow through the vent valves in Babcock & Wilcox plants. Vapor accumulation at high points in the system could stop or prevent natural circulation flow. None of the analytical models have provisions for considering noncondensable gas.

     All of the computer codes used for the system analysis have the ability to consider multiple loops. All cold-leg flows are assumed to fully mix in the downcomer, and the mixed temperature is used as part of the boundary condition for thermal stress analysis. For loop imbalances where one cold leg is at a lower temperature, this is an overly optimistic assumption. It would be more realistic to use the minimum cold-leg temperature for the vessel wall thermal stress analysis.

The ability of the system computer codes to calculate the proper system pressure depends upon their ability to calculate condensation and flashing phenomena. Condensation is of particular importance to system repressurization during HPI. Most computer codes can consider flashing; however, there are analytical model difficulties with condensation of steam against subcooled water. In the pressurizer, for example, condensation of steam occurs at the water surface during inflow of subcooled water. The ability to consider condensation more accurately is not code-specific, and is a weakness of current analytical models.

- Mixing Analysis. The utility owners groups responses included a variety of methods to define the effect of mixing the cold HPI water with the much warmer cold-leg flow. The basic result of all analyses was significant mixing in the cold leg and downcomer before reaching the critical welds. The assumption of large amounts of mixing in the cold leg downstream of the HPI and in the downcomer as predicted by different mixing models used by owners groups is reasonable as long as the loop flow is maintained (either through pumping or natural circulation). This phenomenon is basically supported by the CREARE 1/5-scale test results.[5,6] However, for the situation where the loop flow is not maintained, little mixing would be expected. This is not clearly addressed in the Babcock & Wilcox and Combustion Engineering submittals. The Westinghouse generic report[4a] mentioned that in the SBLOCA, no mixing is allowed when natural circulation is lost. In the Babcock & Wilcox case, credit was taken for the vent valve flow circulation through the downcomer, the core, and the vent valve. Under certain conditions where voids form in the core, the vent valve circulation may not be maintained. It is not clear whether the system code (CRAFT) used by Babcock & Wilcox could predict loss of vent flow. This is because of the restrictive nature of the homogeneous equilibrium model used in the code. In the MSLB, total mixing was assumed by all three NSSS groups. Again, this is reasonable as long as the loop circulation is maintained. Maintenance of loop circulation for MSLB in certain cases needs operator action. This was not clearly addressed in the submittals. In the small SLB, the probability of losing loop circulation is not very likely; therefore, the assumption of total mixing is acceptable.

  Both one- and two-dimensional mixing analysis codes were used by the owners groups. While two-dimensional analysis can provide more realistic assessments than one-dimensional analysis, caution must be used when assessing results. Multidimensional turbulence mixing models suffer from enhanced mixing caused by numerical diffusion, and they can miss some of the observed phenomena such as hydraulic jumps and secondary flows.

- Wall Heat Transfer. The heat transfer coefficient used by the owners groups had a wide range of values. Little supporting information was given other than a statement of what was done. Based on a discussion in Chapter 4.0, it is concluded that the heat transfer coefficient would be an insensitive parameter if it is large (nucleate boiling, forced convection) relative to the conductance of the vessel cladding. The heat transfer coefficient is most sensitive to the wall temperature gradient when the coefficient is at the mid-range value. At mid range, the heat transfer coefficient is of the same order of magnitude as the wall conductance (such as for free convection).

From the review of the submittals, Westinghouse[4a] and Babcock & Wilcox (Oconee)[3d] used Dittus-Boelter correlation for the forced convection heat transfer coefficient. It is judged to be adequate. As discussed above, the coefficients for this mode of heat transfer usually are so large that they do not contribute significantly to the temperature gradient. Combustion Engineering[4b] used a constant value of 300 Btu/hr ft^2°F. This may not be conservative for the initial phases of PTS transients when large amounts of heat transfer are expected.

For natural convection heat transfer, where the film coefficient is more sensitive, all three owners groups gave different values based on different correlations. Combustion Engineering, however, used a constant value (see Chapter 4.0).

Recommendations

1. The role and sensitivity of the operator to mitigate adverse thermal-hydraulic response needs to be more clearly determined.

2. Scenarios in both SBLOCA and steam line break (SLB) transients should include the cases where there is a breakdown of natural circulation. When natural circulation is lost, zero thermal mixing in the cold leg and downcomer should be used in the PTS analyses.

3. Selection of break sizes for the SBLOCA cases should be such that both loss of natural circulation and repressurization occur as early as possible during the transient.

4. In an imbalanced loop situation, the lowest temperature cold leg should be used as the bulk coolant temperature for local mixing analyses.

5. The hydrodynamic model inside the system should include phase separation capability (e.g., drift flux or two-fluid model) and thermal nonequilibrium to predict acceptable temperature, pressure, and flow.

6. For the calculation of forced convection heat transfer at the vessel wall in the downcomer, the Dittus-Boelter correlation is acceptable. For the natural convection heat transfer, the correlation based on Kato et al.[7] or equivalent correlation should be used as a criterion.

7. Experimental work on mixing, such as that at CREARE under EPRI sponsorship, should be continued to develop a more complete understanding of mixing within the cold leg and downcomer. Specific attention is needed for conditions of stagnant loop (and vent) flow. Attention should also be given to scaling the small-scale mixing data to full scale.

   Testing with different HPI configuration should be pursued further. The CREARE tests[6] showed that by keeping injection velocity high (through a smaller diameter HPI pipe), considerable turbulence mixing can be created. More extensive testing in different HPI pipe sizes and angles (including laterally inclined and multiple injections to promote swirling flow patterns) should be performed.

8. Because the assessment of and conclusions about PTS will depend heavily on the results of computer codes, continued development of analytical methods is recommended. Specific areas for attention include:

   - improved condensation modeling of liquid level interfaces (pressurizer, stratified hot leg) during pressurization
   - the breakdown and re-establishment of natural circulation (hot leg, vent flow) under low-velocity, two-phase conditions, and with provision for noncondensable gases
   - improvement and verification of multidimensional models for analysis of mixing in the cold leg and downcomer
   - improvement in modeling the thermal imbalance in transient situations when the loop temperatures are unsymmetrical.

These eight recommendations are not expected to significantly influence the final results in terms of the EFPY; therefore, further corrective actions will not have to be taken within the next one to two years. One possible exception is the effect of operator actions on the thermal-hydraulic results. Operator actions are addressed in Chapter 3.0.

2.3 MATERIALS PROPERTIES

Conclusions

The review of material properties of the critical welds in the eight plants considered in this report has demonstrated that concerns about severe embrittlement are justified and that the embrittlement concern is not due to excessive conservatisms. The following conclusions and recommendations are from the materials evaluation (Chapter 5.0) of PTS.

1. Dosimetry. Fluence uncertainties from dosimetry analyses have been reduced significantly in the last few years. Estimates of inner wall fluence are reliable to within accuracies of 10% to 30%. The through-wall damage analysis is currently being evaluated. Recent assessments suggest that damage through the vessel is greater than expected from fluence (E > 1 MeV) gradients. Revised estimates are based on the dependence of RT_{NDT} on fluence, flux, and spectra.

2. Initial RT_{NDT}. The initial nil-ductility transition reference temperature (RT_{NDT}) values used in the PTS evaluations are substantially above the mean. If the mean plus two sigma conservatism is judged as necessary, then the values used in the analyses are realistic. If better information becomes available, it is not expected that the revised initial RT_{NDT} could be lowered by more than 10° to 20°F.

3. Irradiated Properties. The substitution of the test reactor-based Regulatory Guide 1.99, Rev. 1, with the surveillance-based HEDL equation is justified. The surveillance data more realistically reflect irradiation behavior for pressure vessel neutron fluxes and spectra. Further refinements in the HEDL equation will result as more surveillance data become available. Prediction of damage saturation in high-copper/low-nickel welds is not justified based on the current data base.

4. Sensitivity Analyses. The PNL evaluation of uncertainties in the predicted embrittlement indicated that the typical uncertainty of a few hundredths of a percent of copper or a few tenths of a percent nickel result in an uncertainty of a few (e.g., 2 to 5 years) EFPY needed to achieve a given RT_{NDT}. Similarly, a 10°F uncertainty in assumed initial RT_{NDT} results in a 1 to 2 year EFPY uncertainty. Fluence uncertainties only slightly affect the uncertainty in RT_{NDT} due to the low fluence exponent of 0.27 in the HEDL equation. A fluence uncertainty of ±40% results in an RT_{NDT} uncertainty of only about 25°F. Establishing better estimates of fluences, weld chemistry, and initial toughness can postpone, for a few years, the concern for reaching a given RT_{NDT}, but the long-term, end-of-life embrittlement concern remains.

5. Fracture Toughness. The validity of using Charpy tests for estimating fracture toughness is supported by correlations between the temperature shift in the Charpy impact energy and the temperature shift in the fracture toughness. The current practice of using a lower-bound fracture resistance (K_{IR}) is justified due to the absence of an adequate data base to form a statistically based K_{IR}.

6. Control and Reduction of Embrittlement. The predicted as well as actual embrittlement of welds can be reduced by annealing the reactor vessel, reducing the irradiation flux to the vessel, or determining more precisely the weld chemistry. A one-time vessel anneal is not

effective unless the post-anneal irradiation exposure is short. One anneal performed in, say, June 1982 without subsequent irradiation flux reduction would not significantly reduce the end-of-life RT_{NDT} of any of the plants. One anneal plus a substantial (80%) reduction in flux would significantly reduce the end-of-life RT_{NDT} (e.g., a 135°F reduction for Fort Calhoun).

Two anneals performed before end-of-life result in significant reduction in RT_{NDT} even with no flux reduction. The RT_{NDT} of all plant welds in this evaluation can be maintained below 250°F to end-of-life fluences by annealing twice and reducing the flux by 80%. Furthermore, remote in-situ chemical analysis of welds could reduce the RT_{NDT} uncertainty of some high copper-high nickel welds for the near-term evaluation of vessel integrity.

Recommendations

1. Fluence estimates from Discrete Ordinate Transport (DOT) codes and surveillance dosimetry analysis are acceptable. The fluence estimates should be upgraded as additional dosimetry data are obtained.

2. Evaluations of through-wall radiation damage evaluations should be based on displacements per atom, and not a fluence ($E > 1$ MeV).

3. The dependence of embrittlement on damage rate and temperature should be more clearly defined for the long-term PTS evaluation.

4. The initial RT_{NDT} should be accepted according to the following order of credibility: testing archival material; discovering unreported, plant-specific test results; or testing welds that are similar to plant-specific welds.

5. The conservative estimates of the initial RT_{NDT} should be evaluated statistically using nonparametric tolerance limits (see Section 2.6).

6. The HEDL predictions of RT_{NDT} should be used to estimate the effect of copper, nickel, and fluence on embrittlement.

7. The irradiation shift in RT_{NDT} observed in the surveillance data and the HEDL predictions should be analyzed for confidence as a function of copper, nickel, and fluence. The statistical meaning of using a mean plus two sigma should be established for the long-term PTS evaluation.

8. The lower bound reference curve should be used to estimate fracture toughness (see Section 2.4).

9. The metallurgical reasons for variability in fracture resistance should be established. The reasons should provide mechanistic justification for defining realistic versus conservative fracture resistance criteria in the long-term PTS evaluation.

10. The feasibility of determining weld chemistry by remote access to the vessel exterior should be evaluated.

11. Acceptance criteria for predicting the annealing and reirradiation embrittlement of vessels should be established for the long-term PTS evaluation.

12. The validity of the HEDL curves for predicting reirradiation embrittlement should be determined. In particular, the dependence of embrittlement mechanisms on annealing, reirradiation, and flux reduction, should be clearly established in the long-term evaluation of PTS.

2.4 FRACTURE MECHANICS

Fracture mechanics analyses have been used in the utility owners group responses to predict whether fracture of an embrittled vessel is possible for a given overcooling transient. A conservative, but realistic, analysis fracture requires careful selection of inputs and assumptions for the analyses. The following are conclusions and recommendations from the fracture mechanics analyses (Chapter 6.0) of PTS.

Conclusions

1. Analytical Methods. Available fracture mechanics analysis methods for the PTS evaluations are at the mature state of development, and any near-term advances are likely to be insignificant relative to uncertainties in inputs for material properties and pressure-temperature histories for PTS events.

2. Vessel Tests. Fracture mechanics experiments underway at Oak Ridge National Laboratory (ORNL) should provide added confidence in PTS evaluations. However, the results of clad effects and crack propagation under PTS conditions will not likely be sufficiently timely or conclusive to permit less conservative assumptions to be used to address the plant-specific fracture concerns.

3. Crack Initiation. The linear elastic fracture mechanics methods used in the NSSS vendors calculations for crack initiation are similar except in detail, and should give conservative predictions for vessel integrity under PTS conditions.

4. Crack Arrest. A review of the crack arrest calculations showed a number of unconservative features. Recent data show that the American Society of Mechanical Engineers (ASME) K_{Ia} reference curve is unconservative, particularly for weldments. A revised arrest toughness curve is proposed (see Section 6.3).

5. Conservatisms and Safety Factors. A review of the conservatisms in the NSSS vendors fracture evaluations indicate that no "safety factors" are

used, and this practice is generally consistent with a narrow interpretation of the guidance given in the ASME code for emergency and faulted loads. The conservatisms of the analyses depend on the use of realistic upper bounds on postulated flaw size, fracture toughness reference curves, and predicted shift in RT_{NDT}. It is imperative that suitable allowance be made for vessel material variability when analyses are based on vessel-specific material properties from a limited sample of specimens.

6. Acceptance Criteria. Approaches that are more conservative than those used in the 150-day responses by the NSSS vendors in the application of warm prestress and also for the analysis of crack arrest in vessel integrity evaluations are recommended. A set of guidelines and acceptance criteria for fracture mechanics evaluations are proposed. Modest safety factors (consistent with ASME Code guidelines) are specified for conditions where crack arrest cannot be demonstrated. Acceptance criteria provide specific restrictions for warm prestress and crack arrest calculations. The proposed acceptance criteria will tend to encourage the use of flaw initiation as the acceptance criteria. Flaw initiation analyses of PTS events are more straightforward and well founded than are arrest analyses.

7. Probablistic Fracture Mechanics. In-house NRC probabilistic fracture mechanics calculations were reviewed. These results were found to be useful, and it is recommended that this work be continued by refining inputs, particularly those for flaw size probability distributions. Also, the credibility of the analyses could be enhanced by having the model inputs reviewed by knowledgeable workers in the field.

8. Estimated Failure Probability. Using the results of the NRC staffs' probabilistic fracture mechanics analyses, an estimate of the conservatism of deterministic fracture predictions based on the recommended acceptance criteria has been made. If these analyses ignore warm prestress, the deterministic predictions should correspond to a failure probability of about 10^{-5} given the occurrence of an overcooling transient with a specific pressure/temperature history. This estimate is subject to other uncertainties associated with the completeness of the data base, and with simplifications used in the fracture mechanics treatments.

9. Postulated Flaw Sizes. The NSSS vendor and utility responses lacked information on actual and probable flaw sizes, and did not address possible mechanisms of underclad cracking and the probability of detecting such cracks during in-service inspection. Such information would enhance the credibility of fracture mechanics evaluations.

10. RT_{NDT} Criteria. The implications of nil-ductility temperature criteria as an alternative to detailed fracture mechanics evaluations were addressed in this study. It was concluded that either criteria will lead to a RT_{NDT} limit for a specific vessel. This temperature will be dependent on the specific cooling transients possible for the plant of concern. A fixed and arbitrary limit on RT_{NDT} based on engineering judgment could be justified only on the basis of low confidence in the evaluations of event scenarios.

Recommendations

It is recommended that the following acceptance criteria be adopted for the evaluation of vessel integrity under PTS conditions:

1. The postulated flaw is to be at least 1.0 in. deep with a 6:1 length to depth ratio. Analyses of initiation and arrest should consider all possible flaws less than or equal to the postulated flaw.

2. Initiation is to be governed by the ASME K_{Ic} reference curve with an upper-shell toughness of 200 ksi $\sqrt{in.}$ For arrest calculations, the present ASME Code K_{Ia} reference curve should be adjusted to accommodate recent test data; a 50°F shift along the temperature scale is considered a suitable adjustment. An upper shelf of 200 ksi $\sqrt{in.}$ is to be used for K_{Ia}.

3. In general, credit for warm prestress effects should not be included for PTS events except in those cases in which system and operator constraints clearly prevent variation from the estimated pressure time transient. Warm prestress is to be applied only under decreasing crack-tip stress intensity factors (K) and never for conditions of increasing K. Warm prestress is to be applied only if crack arrest can be demonstrated using the criteria outlined under item 6 below.

4. Fracture toughness and RT_{NDT} shift are to be based on conservative bounding curves such as the the ASME reference curves and the NRC Regulatory Guide 1.99 shift curves. Future calculations should use the new HEDL shift curves, which are based on surveillance specimens with a two sigma statistical bound. If plant-specific surveillance specimen data are used, allowance should be made for statistical variations about mean levels, as indicated by small samples of specimens, and have a level of conservatism consistent with the accepted bounding curves.

5. The acceptance criteria should require no crack initiation. A safety factor on crack initiation should be used unless crack arrest can be demonstrated using the criteria of item 6 below. Suitable safety factors are:

   a) A factor of $\sqrt{2}$ applied to pressure and thermal stress intensity factors when used with the ASME K_{Ic} reference curve.

   b) An implied safety factor on initiation through the use of the revised K_{Ia} curve as recommended in item 2 above.

   c) Warm prestress may be used in the crack initiation analyses, but only with the limitations specified above in item 3. As such, crack arrest must be demonstrated, but the $\sqrt{2}$ safety factor on stress intensity factor may be omitted.

6. In crack arrest evaluations, the following criteria and guidelines should be followed:

a) Once flaw growth initiates, the flaw must be assumed to become a long axial or circumferential flaw.

b) The allowable depth for crack arrest must not exceed one half of the vessel wall thickness, unless detailed elastic-plastic analyses can justify that greater depths are acceptable. Vessel failure due to net section plastic collapse of the remaining ligament is to be precluded for the arrested crack depth.

c) The initiation condition for the arrest calculation must assume flaw sizes and K_Q-values (from those possible) that will produce the largest jump and not necessarily the earliest initiation.

d) It must be demonstrated that an arrested crack will not reinitiate for the existing pressure, temperature, and cooling rate limits for the vessel. The evaluation of initiation is to be in accordance with ASME Section III, Appendix G, except that the factor of 2.0 on the pressure-induced stress intensity factor (K_{IM}) may be reduced to a value of 1.0.

Except for the restrictions on warm prestress and the recommended safety factor, the proposed criteria for crack initiation are essentially those described in the owners group responses. The criteria on crack arrest are significantly more restrictive.

It was not possible in this study to evaluate the implications of the proposed acceptance criteria. It is believed that the crack arrest criteria will make it difficult to demonstrate arrest for borderline cases of crack initiation under PTS conditions. The initiation criteria, even with the recommended safety factors, will probably be much less restrictive on allowable EFPY than on the alternate arrest criteria. In effect, the proposed criteria should favor the more straightforward and more soundly based crack initiation analyses.

Use of the safety factor $\sqrt{2}$ on K_{Ic} (recommendation 5.a) is considerably less restrictive than the use of a modified K_{Ia} curve (recommendation 5.b). In terms of the example used in the sensitivity analyses (see Section 6.12), the $\sqrt{2}$ factor is roughly equivalent to a 30°F change in RT_{NDT}, or about 3 EFPY. The K_{Ia} approach is roughly equivalent to a 120°F change in RT_{NDT}.

It is recommended that calculations be performed in the near future to establish the impact of the proposed acceptance criteria on predictions of vessel integrity. The objective of these calculations should be to determine if specific vessels will not meet PTS requirements over the next two years. In PNL's judgment, a few vessels may be unacceptable for certain postulated transients. Vessels that currently have high RT_{NDT} values may not be acceptable for the Westinghouse small-break LOCA transient without warm prestress. Also, vessels may be unacceptable for the Combustion Engineering transients if RT_{NDT} is estimated using the proposed HEDL curve rather than the more optimistic approaches used by Combustion Engineering.

2.5 NONDESTRUCTIVE EVALUATION

Before a PTS event can produce significant nonductile failure, a flaw of sufficient size must exist in the beltline region of the vessel. Nondestructive evaluation (NDE) can help determine the integrity of a reactor vessel before and after a PTS event. The evaluation techniques characterize the flaws that exist in the vessel wall and ensure that flaws of concern do not exist in critical areas of the vessel. The ability to detect and characterize flaws can improve estimates for vessel-failure probability codes. The following conclusions and recommendations are from Chapter 7.0.

Conclusions

An evaluation of nondestructive techniques to detect underclad cracks is based on limited data from an ongoing NRC program. Our preliminary conclusions are:

1. It is possible to detect flaws at the clad/base-metal interface using special techniques that currently are being employed in Europe and demonstrated at PNL. Our initial estimate is that a significantly greater probability for detection exists for clad surfaces that are smooth or ground.

2. The current calibration requirements of ASME Section XI are neither adequate nor sensitive for detecting flaws at the clad/base-metal interface.

3. Regulatory Guide 1.150 should be revised to require a demonstration of the ability to detect flaws at the clad/base-metal interface.

Recommendations

1. Inspection procedures for the examination of weld volume in reactor pressure vessels should be required to include specialized techniques for examination of the reactor pressure vessel clad/base-metal interface.

2. Regulatory Guide 1.150 should be revised to require a demonstration of the ability to detect flaws at the clad/base-metal interface.

3. An inspection of a nuclear reactor vessel should be performed following a PTS event when the potential for the initiation of a crack exists.

2.6 STATISTICAL ANALYSES

The PTS literature does not indicate that a thorough statistical examination of the available data has ever been made. The lack of such an examination is evident in the seemingly indiscriminate pooling of data, in questionable distributional assumptions, and in the absence of consideration of the overall uncertainty structure. The Monte Carlo code, VISA, can provide valuable insight into the PTS issue. The following conclusions and recommendations are from the statistical analysis evaluation (Chapter 8.0) of PTS.

Conclusions

1. Various collections of data and/or models are available, but methods of data collection and analysis affect the interpretation and use of the information. Generally, it does not seem that a unified statistical examination of the data relevant to the PTS issue has been made.

2. A key requirement for the validity of a Monte Carlo approach is that the stochastic structure of the system be correctly modeled. This is a far more stringent requirement than merely putting an appropriate probability distribution on each input variable. The joint and collective properties of the uncertainties must be considered. This concern does not appear to have been adequately addressed.

3. The VISA code can be useful in investigating qualitative aspects of PTS. The use of VISA should be limited to doing sensitivity analyses and to comparing pressure/thermal transients.

4. The normal (Gaussian) distribution has been overused as a default statistical distribution. Confidence limits in the form of a mean plus two sigma are appropriate only for a normal distribution--but not all data follow a normal distribution.

Recommendations

Due to the varied sources of data, the several mathematical models in use, the time span over which the data were collected, and the various methods used to analyze the data, it is recommended that a coordinated statistical examination of the data relevant to PTS be made. The following are guidelines for the study:

1. Whenever possible, the data should be examined in their most elemental form (i.e., before aggregation, smoothing, or averaging).

2. Methods of data reduction (e.g., curve fittings) and aggregation should be reviewed.

3. Validity of normal theory confidence bounds should be evaluated and, when appropriate, alternative methods such as distribution-free tolerance limits should be used.

4. Stochastic relationships of variables affecting PTS should be determined. If sufficient data are not available, the study should identify the data needed.

3.0 EVENT SCENARIOS

Event scenarios can be used to help predict the degree to which PTS threatens the continued safe operation of commercial nuclear facilities. Scenarios must be based on a reliable data base or validation analyses that relate possible steady-state or transient plant operating conditions to the potential danger posed by exceeding the known PTS limits. Without this information, an adequate set of event scenarios cannot be developed, nor can viable recommendations for an interim position be made to the Commission.

This section provides a generic discussion in support of recommendations for actions that may be mandated to prevent or mitigate PTS events. A generic discussion of PTS scenarios is necessary because an infinite number of event scenarios can be constructed. The scenarios differ in terms of: initiating events; specific available plant equipment; potential, partial, or complete failure of instrumentation and control systems; and operator action or errors.

One must be realistic about event scenarios and the possibility of pressurized thermal shock in reactor vessels. The first question that must be answered is: "Are the operating limits that are established for a plant sufficiently conservative to guarantee (with minimum uncertainty) that as long as those limits are not violated at the plant during steady-state or transient maneuvers a PTS event will not occur?" The second question logically follows from the first: "Is the plant designed, maintained, and capable of being operated in such a manner that the operating limits are not violated during all reasonable scenarios?"

The remainder of this chapter will address both these questions. The validity of existing operating limits is also addressed in other chapters of the report. Here, the discussion of the first question will deal with how a scenario or nature of an event impairs the ability to determine whether existing plant limits are being violated. If current plant conditions render PTS indicators invalid, an operator who is not aware of the situation could inadvertently commit a PTS violation.

3.1 CLASSES OF EVENTS

Four generic classes of events can initiate a PTS event: overcooling transient with subsequent repressurization, overpressurization at low temperatures, localized cooling, and external cooling. Typical scenarios for these events are described below.

3.1.1. Overcooling Transient With Subsequent Repressurization

This scenario typically assumes the maximum uncontrolled cooldown rate and repressurization of the reactor coolant system (RCS). Overcooling events may include failure of the secondary feedwater control system, a rapid change in feedwater temperature, oversteaming, and/or cold-pocket RCS water injection.

Overcooling due to feedwater control problems can occur when too much cold feedwater is fed into the steam generator at a rate which overcools the plant. These events can occur because: 1) feedwater control valves can fail or stick open; 2) automatic steam generator water-level control systems can fail in the high mode (overfeed); 3) level indicators in the steam generator or other instruments that indicate to the individual or to the control system the feed rate, steam header pressure, and/or inventory in the steam generator may fail; 4) operators may under- or over-feed the steam generator when trying to control the system in manual; or 5) during recovery from a transient that resulted in a plant shutdown, the feedwater system may not be fine-tuned for small flow rates and will recover the design steam generator level too rapidly.

A rapid change in feedwater temperature may result when one or more feedwater heaters is lost, thereby lowering the temperature of the water entering the steam generator. This causes a reduction of temperature in the primary system.

Oversteaming can result from a steam line break, the continuous opening of a secondary relief valve, or failure (in the open mode) of a steam-demand control valve (e.g., turbine bypass valves, main turbine governor valves). In addition, if a reactor trip without a turbine trip occurred, a severe cooling transient could result. Following the severe cooldown transient, automatic repressurization to the HPI shutoff head or saturation pressure for the bulk RCS temperature could result in a challenge to vessel integrity. The amount of cooling within the vessel wall will be affected by the amount and temperature of the HPI water injected, the injection rate, the actual RCS recirculation rate during the injection, and the RCS fluid temperature change during repressurization. The reactor system may go solid following a cooldown accident. Caution must be exercised when dumping steam, when starting reactor coolant pumps and charging pumps, and when operating the pressurizer heater control during recovery.

Cold-pocket RCS water injection may result where partial hot leg RCS voiding has significantly reduced the natural circulation flow. Reflux boiling, in conjunction with high pressure injection (HPI), may be the only cooling mechanisms. If one (or more) reactor coolant pumps is started, a slug of cold RCS water that was in the RCS piping of the lower steam generator could be injected into the pressure vessel, where it would impinge on the beltline weld area.

3.1.2. Overpressurization at Low Temperatures

Overpressurization at low temperatures is largely precluded by requirements to install temporary, or modify the setpoints for existing, relief valves during low-temperature conditions. However, failures of the overpressure mitigating system (OMS) have occurred recently on at least two occasions (Turkey Point Unit 4 - November 1981; North Anna - May 1982). In the case of the Turkey Point failure, two separate transients resulted in overpressure conditions of 1100 and 750 psig at 110°F. These events exceeded the pressure limit of

480 psig at 110°F specified in Technical Specifications which prescribe the allowable pressure and temperature limits to prevent reactor vessel brittle fracture.[8] Such events, which are most likely to occur when the reactor coolant system is in a solid water condition, appear to lead to less severe consequences than other PTS scenarios. Since the event is the result of a low-temperature condition, the assumed thermal stresses in the vessel are not as significant, the amount of remaining residual heat in the core is smaller, and the induced thermal gradient across the pressure wall is lower.

3.1.3 Localized Cooling

Large-break loss-of-coolant accidents (LBLOCA) are not considered serious PTS initiators because the repressurization event is not as severe. Concern has been expressed, however, that a PTS might occur during a small-break loss-of-coolant-accident (SBLOCA). A localized-cooling event assumes that there is a simultaneous and extended loss of feedwater. Babcock & Wilcox[4c] and Combustion Engineering[4b] have described this phenomenon: with no feedwater available, the only way to cool the RCS is by injecting water (<100°F) from the HPI system. This cold HPI water is injected into nozzles located in the piping just in front of the inlet to the pressure vessel. As long as there is normal, natural circulation flow, then the cold HPI water can mix with the warmer water circulating in the loop before it is impinged on the beltline area. However, if the natural circulation flow is stopped because voids have formed in the primary hot leg piping, then the potential exists for the cold HPI water to impinge on the beltline without being preheated (assuming that no internal core flow pattern develops). The worst case would occur in the situation where the leak was big enough to cause void formation and a resultant loss of natural circulation flow, but small enough that the HPI system was able to allow for repressurization to near shutoff head. The result could be a very low reactor vessel wall temperature with a large force placed on it by the still-high RCS pressure.

Westinghouse further analyzed these phenomena and indicated that the most serious potential SBLOCA size would be between 0.5 in. and 1.5 in.[9] While other factors such as break location, safety injection flow rate, decay heat rate, and secondary pressure (steam dump and feedwater) affect the rate of cooldown following a LOCA, it is worth noting that the critical break size does include a stuck open PORV (1.4 in.).

3.1.4. External Cooling

It is possible that cold water could impinge or immerse the outside of the pressure vessel and cause a PTS event. For example, rupture of a non-RCS pipe in containment could flood the area adjacent to the pressure vessel. An event of this type did occur at the Indian Point #2 reactor. The pit surrounding the pressure vessel became filled with water. Such an occurrence does not become serious unless the induced thermal gradient changes rapidly. The gradient that would occur on the inner wall would be opposite of that normally encountered when the internal wall of the pressure vessel cools down faster than the external wall of the pressure vessel. In addition, the radiation damage is much lower in the outer region of the vessel wall. Therefore, it is unlikely that localized cold water impingement on critical regions along the wall of the pressure vessel would result in an aggravated situation that could lead to a PTS event.

3.2 INITIATING AND SEQUENCING EVENTS

The previous section described the methods by which the four classes of events could be initiated and described the sequence of occurrences that could lead to a potential PTS situation. In developing a set of actions that can be used to mitigate the PTS problem, it is essential that each of the following be considered:

1. methods that enable the plant to avoid scenarios which may lead to possible PTS conditions

2. methods that can assist the operator or control systems (if automatic) in escaping from a scenario that could result in PTS conditions

3. passive or active automatic protection systems that will preclude a PTS violation, irrespective of operator or control-system action, without compromising core cooling considerations

4. methods for controlling the plant and evaluating the seriousness of the situation if a PTS condition has occurred

5. methods for determining whether a PTS condition has occurred

6. relative gain in safety (i.e., do proposed modifications enhance safety in regard to the PTS issue while causing an unwarranted reduction in core cooling safety?).

3.3 OPERATIONAL CONSTRAINTS OF PLANT SYSTEMS

Individual plant systems differ, even though they may be of the same generic plant type. It would be inappropriate to mandate corrective actions for all plants based on operational or design deficiencies noted at a few facilities. Specific corrective actions that are needed to avoid or mitigate a PTS event should be established on a plant-specific basis. Therefore, unless it has been rigorously shown that a generic "fix" will result in a consistent improvement in assurance of plant safety, suggested corrective actions should be implemented only after they have been evaluated for the particular operating constraints of an individual plant. The safety actions should be implemented through plant-specific audits.

3.4 HUMAN FACTORS

Human factors reviews of nuclear power plant operations have traditionally been concerned with operator training, emergency procedures, control-room design, and training material. In the case of PTS, all four of these areas are of interest.

All eight plants have responded to the NRC stating that they have already trained or are in the process of training operators to be aware of PTS and to be aware of steps that operators can take to lessen the chance that a PTS event will occur.[2,3] Each plant has also stated that its emergency procedures have been revised to include PTS considerations and that it has instituted specific steps operators will take to prevent or at least lessen the risk of a PTS event. The plant responses did not address whether the human factors design of the control room would enhance or detract from the operator's ability to deal with PTS.

At this time, it is important to address the following positions regarding the human factors aspect of PTS:

1. Each plant's training program (i.e., lesson plans and other training material) should be reviewed to ensure that the information is technically correct and complete. The effectiveness of the training program should be evaluated by including PTS items in the licensing requirements, in written and oral exams, and in similar exercises. All personnel including licensed operators, shift technical assistants (STAs), and designated support staff should be retrained on PTS and core cooling. Retraining should include both classroom and simulator exercises. These personnel should be required to pass written and plant walk-through examinations. Training and examinations regarding PTS should become an integral part of the utilities requalification program. Periodic drills should be conducted, utilizing the shift-team approach, on probable transients that could challenge the PTS limits of the vessel.

2. Simulator training should be reviewed. This training program should include normal plant operation such as startup and shutdown, high probability transients, and equipment failures, as well as the design basis accidents. This training should also include post-transient analysis from available records to determine heat-up or cooldown rates. Transients could then be repeated with deliberate delays in operator responses so the effects could be evaluated. Both core cooling and PTS types of transients should be included.

3. Procedures should be reviewed to ensure that they are technically accurate, complete, and useable. The review should use methods similar to those developed for reviews of near-term operating license (NTOL) emergency action procedures under Item I.C.1 of the TMI task action plan (NUREG-0660 and NUREG-0737). Special emphasis should be given to procedure-, operator-, and instrumentation-oriented actions.

4. Criteria for training material should be established by the owners group working in conjunction with the Institute of Nuclear Power Operations (INPO). This approach will provide detailed, technical information and will help establish uniformity in PTS training.

3.5 PROBABILITIES

As discussed in Section 3.1, potential event scenarios leading to PTS include loss-of-coolant accidents, steam line breaks, and feedwater transients. These can be exacerbated by hardware or human failures. A first step in evaluating the significance of an overcooling transient sequence is to determine its estimated frequency of occurrence. This information can then be combined with the thermal-hydraulic and fracture mechanics analyses to determine the likelihood and consequences of a potential PTS event sequence. This section briefly summarizes and evaluates past and ongoing work that has been used to develop frequency estimates for PTS event sequences.

Given the general classes of PTS events (see Section 3.1), two approaches can be used to identify PTS event sequences: they can be postulated directly using engineering judgment, or they can be postulated by performing detailed logic modeling (such as event-tree/fault-tree models). The 150-day licensee responses and the owners group reports typically use the former, while ongoing research being conducted by ORNL for the NRC is performing more detailed logic modeling. These efforts will be discussed briefly in the following paragraphs.

The 150-day licensee responses and the owners group reports[3,4] typically postulated specific design-basis accidents and abnormal operational occurrences using conservative boundary conditions to enhance the overcooling events (minimum water temperatures and maximum feedwater flows were often assumed). The operator plays a key part in the transient sequences postulated. No effort was made to develop a comprehensive listing of PTS events representing a broad range of frequency and severity. Few, if any, probability estimates were given, and no vessel failures were calculated for the near-term. The Oconee submittal[3d] was an exception in that a broader range of PTS scenarios was postulated and frequency of occurrence estimates were made. Small-break LOCAs (both as initiators and resulting from transients) and severe overcooling events (excessive feedwater and insufficient steam pressure control) were analyzed using fault-tree models. Sequence frequencies were quantified using generic industry experience, Oconee experience, and the WASH-1400[10] data base. The estimated frequency for severe PTS events was in the range of 10^{-4} to 10^{-6} per year. The report states that when this value is combined with the conditional probability of vessel failure, PTS events are not a significant contributor to risk. Although the Oconee report is more detailed than the other licensee submittals, more information is needed before the 10^{-4} to 10^{-6} per year value can be accepted. This includes a more detailed consideration of operator error and a consistent treatment of multiple failures and potential dependencies.

The ongoing ORNL program was initiated in June 1981 to perform an independent study of PTS. The first phase of the study was an interim report,[11] which organized information about the PTS problem and major areas of uncertainty, to suggest means for filling knowledge gaps and to propose and evaluate mitigative measures. The report defined four general classes of transients: 1) large-break loss-of-coolant accident (LBLOCA), 2) small-break loss-of-coolant accident (SBLOCA), 3) main steam line break (MSLB), and 4) runaway feedwater transient (RFT). Oconee-1 was used as the reference plant. Detailed probability calculations were not performed. The following estimates were given for each initiating event: SBLOCA = 3×10^{-4}, LBLOCA = 1×10^{-4}, MSLB = 5×10^{-6}, RFT = 1. System/operator responses were not quantified. Conservative models were used to evaluate the classes of transients. In the case of MSLB and RFT, vessel failure resulted before the plant's normal end of life. An NRC staff review of this report[12] outlined the conservative assumptions used and proposed the following list of rough estimates for pressurized overcooling events for different reactor types. These estimates are preliminary and may have uncertainties of a factor of 10.

Estimated Frequency per Reactor Year

	Initiating Event	PTS Sequence
RFT	3×10^{-1} (B&W)	10^{-4} (B&W)
	6×10^{-2} (CE & W)	$<2\times10^{-5}$ (CE & W)
Large MSLB	1×10^{-4}	3×10^{-6}
SBLOCA	3×10^{-4}	1×10^{-5}
LBLOCA	1×10^{-4}	not stated
Rancho-Seco	3×10^{-1} (B&W)	10^{-3} (B&W)
	6×10^{-2} (CE & W)	10^{-4} (CE & W)

The current ORNL program is improving on the work described in the interim report. Efforts to identify and quantify the event sequence have been expanded to include detailed event-tree/fault-tree modeling, and the scope has been broadened to include Westinghouse and Combustion Engineering plants in addition to Babcock & Wilcox plants. To date, work has been performed only on Babcock & Wilcox plants to more rigorously define and quantify overcooling event sequences. The analytical approach being used is to:

- identify those systems which can create conditions suitable for PTS
- construct event trees which incorporate the identified systems for suitable event initiators
- use system functional dependence, engineering judgment, and thermal-hydraulic survey calculations to prune trees by reducing end-states and combining categories
- quantify frequency of remaining end-states

* select reduced set of event sequences for detailed thermal-hydraulic (T-H) and failure modes.

A combination of failure mode and effects analysis, and a fault tree analysis will be used to quantify the event sequences. Multiple failures and cascade failures will be included. This work is now in progress for Babcock & Wilcox plants. The work on Combustion Engineering and Westinghouse plants has not yet started.

The PTS event sequences that have been identified either have a relatively low probability of occurrence or a low consequence. However, no rigorous analysis has been performed to identify and quantify a set of potential PTS events for each of the eight plants of interest. The plants represent a range of scenarios from high probability, low consequence to low probability, high consequence. The ORNL program is heading in this direction, but results of the analyses will not be available for deciding the near-term regulatory position. Additional analyses which need to be performed are discussed in the Conclusions and Recommendations (Chapter 2.0) of this report.

3.6 RISK ASSESSMENT

Additional work is necessary to more rigorously define and quantify PTS event scenarios. Probabilistic risk assessment (PRA) can play an important role in helping to provide insight in these areas. Probalistic risk assessment methods can help identify sequences that challenge a reactor system and can pinpoint where hardware failures, operational conditions, or human errors contribute to an unfavorable end point for each particular class of sequence. As a result, PRA methods can guide remedial actions.

If enough information is available, PTS issues can be evaluated through a rigorous risk assessment approach. Risk assessment involves identifying event sequences, quantifying their probability of occurrence, performing the thermal-hydraulic and failure mechanics analyses on a probabilistic basis, and combining these probabilities to determine the frequency of pressure-vessel failure. Uncertainty bands could be established, and the calculated pressure vessel failure could be compared with some established, acceptable value. Portions of this approach can be implemented with the use of risk assessment tools, but a detailed risk assessment is not feasible at this stage. Risk assessment tools may assist in establishing a regulatory limit on RT_{NDT}. Instead of a strict RT_{NDT} requirement, it may be feasible to establish a list of design-basis transients for which reactors with a certain RT_{NDT} (e.g., 300°F) will have to demonstrate an acceptable probability of occurrence.

3.7 SENSITIVITY ANALYSES

Little effort to date has been spent on performing sensitivity studies on the frequency estimates for the PTS events identified. The Oconee submittal performed a simple uncertainty analysis and estimated 5% and 95% confidence levels. One major use of sensitivity analysis would be to examine the effect that corrective actions (such as those proposed in Section 3.8) would have on PTS event probability.

Operator action has been identified as a major contributor to PTS event sequences. The NRC has requested additional information from the licensees and owners groups to examine the sensitivity of the transient to the time assumed for operator action. Two types of PTS sequences may be postulated: 1) those sequences that would result in little or no serious challenge to safety unless there is operator error; and 2) those sequences that may provide a challenge to safety even though the operators respond correctly. This distinction is important when considering whether to adopt procedural and/or hardware corrective actions. Developing a comprehensive list of PTS scenarios will ensure that both of these types of sequences are considered. In addition, it ensures that corrective actions can be evaluated based on their effect on frequency of occurrence or potential consequences as determined by formal sensitivity analyses. These sensitivity analyses have not been performed to date but should play an important part in the long-term PTS program.

3.8 MITIGATING ACTIONS

This section summarizes actions that may be taken to mitigate the four generic event scenarios. These actions need to be implemented to ensure that the staff operating the plant (given the existing, design-basis control systems) have the training, equipment, and procedures necessary to preclude and/or mitigate potential PTS events. A description of the proposed actions is prioritized into near-term, intermediate-term, and long-term categories. Some of the recommended actions are operator-oriented (including training), some are instrumentation-oriented, and some are procedure-oriented.

The development of mitigating actions is exacerbated by the fact that some industry members and plant staff believe that:

a. The pressure vessel is over-designed with so many built-in conservatisms and safety factors that for a real PTS event to occur would require a much more serious plant upset than could possibly take place.

b. The whole field of PTS involves so many uncertainties that unnecessarily restrictive limits have been incorporated to compensate for the lack of precise technical information describing actual in-plant conditions that would result in a PTS event.

c. An emphasis on addressing the PTS issue will only result in compromising progress that has been made, post TMI, to ensure that inadequate core cooling does not occur.

d. Even if the current PTS predictions are correct, safety (prevention of violating operating limits) can be achieved through adequate operator training and procedures, with no plant modifications.

The mitigating actions suggested here will work effectively only if there is either a receptive attitude on the part of the entire industry when addressing the PTS issue, or if a comprehensive, detailed external audit program is implemented.

3.8.1. Procedure-Oriented Mitigating Actions

1. General criteria for procedures related to PTS should be established by the owners groups and INPO. Each facility should develop operating procedures to accomplish the following:[a]

   a. Instructions in the procedures should not lead operators to take actions that would violate nil-ductility (NDT) limits.

   b. Procedures should provide guidance on recovering from transient or accident conditions without violating NDT or saturation limits.

   c. Procedures should provide guidance on recognizing and recovering from PTS or near-PTS conditions. Instructions for how to control the plant should be based on where, relative to NDT and saturation curve limits, the operating point is located. Specific guidance must be given to the operator so that he or she may know what plant control actions should be taken subsequent to a PTS or suspected PTS event. Guidance should be given to specify the preferred temperature indicators (e.g., T_{hot} or core outlet thermocouples for core cooling and saturation limits, cold leg thermocouples for PTS, and wide-range thermocouples during natural circulation).

   d. PTS procedural guidance should have a technical basis (i.e., analyses of called-for action should be referenced).

   e. High-pressure injection and charging system operating instructions should reflect PTS concerns.

   f. Feedwater and/or auxiliary feedwater operating instructions should reflect PTS concerns.

   g. Clearly legible, current, and usable NDT and saturation curves should be available in the control room. Danger areas should be clearly identified.

   h. Procedures should include allowance for delay times in system response (e.g., loop transport time, thermal inertia, magnitude and direction of the cold slug, and cautions to help the operator control the major plant parameters affecting PTS).

(a) This list includes items that were developed by the NRC-H. B. Robinson Task Force.

2. As required by current procedures, cooldown rates are limited to "X" degrees per hour (and some include not to exceed "Y" degrees in a half hour). There should be additional guidance as to the cooldown rate to be observed (over a few minutes). Example: The cooldown rate shall not exceed 100°F in 1 hr, and not more than 10°F in 5 min. Doing so eliminates several problems:

   a. Controlling at a fixed short-time cooldown rate results in a much more stable and controlled shutdown.

   b. Periodic cooldown rates encourage "hunting." This can mean that, even within existing procedural guidelines, the plant can be subjected to short-term cooldown rates of 10°F/hr, then 200°F/hr, and then perhaps a heatup while the operator is reducing RCS temperature at "X" degrees per hour. The operator can end up establishing a cooldown rate that is excessive, and, lacking requirements to maintain a short-term rate, not realize that the plant has been placed in a severe transient condition that may lead to a PTS-initiating event.

3. Procedures that may result in pressure recovery need to be expanded to contain steps which clearly help the operator <u>after</u> the pressure decrease has been terminated. This will prevent the uncontrolled repressurization of the plant.

4. Procedures need to specify how to control cooldown rates, what to do when limits are exceeded (e.g., a plant transient occurs causing RCS temperature to drop 50°F in 10 min. What does the operator do? Stay at that temperature for a half hour before commencing a cooldown? Does he use T_{cold} for cooldown regarding NDT limits while monitoring T_{hot} for saturation? What if he has to use in-core thermocouples, can they be used with existing NDT curves?). Guidance on temperature control and monitoring instructions are required in all applicable procedures.

5. Current emergency procedures for HPI termination should be analyzed to determine the specified pressure at which the operator can secure safety injection while still maintaining adequate subcooling, heat sink, and pressurizer level requirements [this is especially important for those plants whose HPI system is capable of discharging against a very high shutoff head, up to and including the setpoints of the Power Operated Relief Valve (PORV) and the safeties]. This accomplishes two things: 1) it minimizes unnecessary cold water injection and, thus, overcooling transients, and 2) it minimizes potential resultant pressure on the inner vessel wall (i.e., the degree to which repressurization occurs).

6. Procedures should be written and analyses performed to establish post-accident or post-transient "hold points." This would establish the conditions following an event under which the operator knew that he was in a stable condition and would not feel obligated to put the plant through a subsequent transient to achieve another condition which only marginally

improved the safety margin. (Example: After a near-PTS event, it is not readily apparent that the best course of action is to initiate a plant cooldown at the maximum cooldown rate, thereby increasing the thermal gradient across the pressure vessel wall. It may be preferable, under certain conditions, to allow thermal equilibrium to be achieved, or even to heat up.)

7. The 50°F subcooling margin should be re-evaluated to determine if it can be lowered to provide more control margin for the plant operator. This would allow him to keep the plant farther from the NDT curve limits.

8. The minimum amount of subcooling required by procedure should be reviewed to determine if a single, fixed value is adequate or if the values should be allowed to vary with the RCS fluid temperature. While care should be taken to ensure that adequate protection against the violation of saturation limits is maintained, it is also clear that 50°F margins at 300°, 400°, 500°, and 600°F provide differing amounts of protection. Various alternative thermal margin specifications could be acceptable if properly analyzed, and would result in more control margin for the plant operator, especially at lower RCS temperatures. For example, specifications could require that a constant RCS stored energy margin exist between the operating point and the saturation margin--expressed in the form of a varying ΔT margin with RCS temperature).

3.8.2. Operator-Oriented Mitigating Actions

1. Each plant should require licensed operators to receive upgraded training in the following areas:[a]

   a. Review of previous overcooling events at their facility and review of applicable event summaries at other plants. Special emphasis should be placed on comparisons to similar facilities, and should include evaluation of:

      1. the event

      2. how the limit was challenged

      3. action taken to mitigate the event.

   b. Review all procedures and abnormal procedures that could challenge core and PTS limits, and outline the typical progress of key parameters until recovery is achieved. This exercise should consider an RCS with and without a steam bubble at locations other than the pressurizer. As a team, each shift crew should review their outlines and emphasize the operator response necessary to mitigate the transient. The review should include

(a) The list includes items developed by the NRC-H. B. Robinson PTS Task Force.

instrumentation and controls during the recovery phase, with a complete walk-through to the point where conditions have stabilized. Emphasis should focus on discussing alternatives for recovering from a PTS or near-PTS condition, and alternatives for minimizing RCS overcooling and subsequent repressurization, while still ensuring that core cooling is not jeopardized. The shift should provide plant management with feedback from questions or comments arising from this training.

c. Simulator training involving operation with scenarios resulting in the violation of or approach to PTS limits should be conducted. Particular attention should be given to the time required for the system to respond in order to determine the rate of operator response. However, criteria should be established first by the owners group or INPO, then reviewed by the plant's operations staff to determine if the simulator provides a reasonable model of the reactor. [Example: can the simulator demonstrate steam bubble(s) in the reactor coolant system (i.e., vessel head) during forced flow and natural circulation?]

d. Simulator training should be used to upgrade the operator's ability, during transient conditions, to monitor and control: RCS heatup rate, RCS pressure control, and steam generator water level. Practice should involve developing skills in establishing adequate increase and decrease rates, recognizing turning points (both minimum and maximum), and evaluating integrated parameters. Proposed procedure changes should be verified on a simulator before they are finalized.

e. Specified instruction should be given about NDT vessel limits for 1) normal modes of operation, and 2) transients and accidents. Instruction should particularly emphasize those events known to require operator response to prevent or mitigate PTS.

As described in Sections 3.4 and 3.5, the operator played a major role in initiating, preventing, or mitigating PTS scenarios. Preliminary work by the Westinghouse Owners Group has examined the effect of operator action during different time periods in response to selected PTS scenarios. For selected cases, reducing the operator response time from 60 to 20 min would result in a potential PTS scenario that would not cause crack initiation. As discussed in Section 4.2, the Combustion Engineering analysis indicated that for a SBLOCA, operator action taken to restart the feedwater flow at 30 min instead of 60 min can limit the transient to the P-T region-of-no-concern. A more detailed analysis needs to be performed on sensitivity of PTS scenarios to the time required for operator action and the potential for error.

Based on the incomplete understanding of potential PTS scenarios and the importance of operator actions, the near-term and intermediate recommendations for corrective action emphasize improvements in procedures and operator training.

3.8.3. Instrumentation-Oriented Mitigating Actions

1. The NRC - H. B. Robinson Task Force recommended an accelerated schedule for the inclusion of subcooling meter indication based on temperature (either in addition to or in place of existing pressure-based indication).

2. At all times, the following RCS cooldown rate indications should be made readily visible to the operator:

   a. instantaneous cooldown rate (over a few minutes)

   b. integrated cooldown rate (over last 1/2 and 1-hr period)

   c. cooldown rate change (i.e., the rate of temperature change, increase or decrease. This could be provided not as a quantitative value but as a light or color change.)

3. The pressure indicator readability of the reactor coolant system should be reviewed and, if not acceptable, be improved. Instruments should be reviewed to determine if they can be equipped with rate-change indicators that allow the operator to determine whether the pressure is decreasing or increasing. The instruments should also measure the relative rate of the increase or decrease. Current indications (e.g., strip charts) are difficult to interpret if the operator is attempting to determine when the point of pressure change has occurred. In addition, the chart speed is slow, and rapid changes are difficult to interpret during a real-time event.

4. A temperature/pressure instrument analogous to the saturation meter is needed to continually indicate the current temperature <u>and</u> pressure margin to the NDT limiting curves. A safety panel display system (SPDS) could be used for this indication.

5. Instruments indicating the water level in the steam generator should be improved. Current indicators such as strip charts or gages are difficult to note transients on, especially in regard to minimum or maximum-level turning points.

6. Information-only warnings (no new annunciators, just CRT or computer-printout displays) should be provided to inform the operator that:

   a. excessive cooldown rates exist

   b. excessive heatup rates exist

   c. excessive repressurization rate exists (alarm to be actuated only after a permissive is actuated by a low initial pressure signal)

d. excessive rates of increase in steam generator level exist (alarm to be actuated only after being enabled by a low initial steam generator level signal). An indication of steamflow-feedflow mismatch may also serve this purpose. (Note: this is a recommendation of NUREG-0667, "Final Report of the B&W Reactor Transient Task Force" (May 1980) and NRC DST Review (August 8, 1980).

3.8.4. Summary of Mitigating Action Recommendations

We recommend that the following actions be mandated to prevent or mitigate PTS events: (numbering corresponds to corresponding number under Section 3.8)

Near-Term Implementation (<1 yr)

Procedure-oriented recommendations:

1. improve general criteria
2. require short-time RCS cooldown rate limits
3. improve RCS pressure control guidance
4. improve RCS cooldown control guidance.

Operator-oriented recommendations:

- 1a. review overcooling events
- 1b. provide PTS transient control classroom instruction (shift walk-through)
- 1c. conduct PTS simulator training
- 1d. conduct special transient-control simulator training
- 1e. conduct NDT and PTS theory classroom training.

Instrumentation-oriented recommendations:

1. add temperature-based subcooling meter indication.

Intermediate-Term Implementation (1-2 yr)

Procedure-oriented recommendations:

- 5.* review HPI termination pressure requirement
- 6.* establish post-transient "hold points"
- 7.* reduce saturation margin requirements.

Instrumentation-oriented recommendations:

- 2a. add instantaneous RCS cooldown rate monitor
- 2b. add integrated RCS cooldown rate monitor
- 3. improve RCS pressure readout.

Long-Term Implementation (>2 yr)

Procedure-oriented recommendations:

- 8.* incorporate variable saturation margin.

Instrumentation-oriented recommendations:

2c.* add RCS cooldown rate change indicator
4.* add NDT margin meter
5. improve steam generator level instrumentation
6.* add transient warning indicators.

Because nuclear facilities differ in terms of design and operating modes, each plant should be required to prepare a PTS mitigating actions package (MAP) which describes how that facility intends to implement the above-listed recommendations (or technical justification for exceptions). The PTS MAP should be reviewed and approved within an established time frame, and audits should be performed on a periodic basis to reaffirm continued facility attention to and action on the PTS issue.

While it is clear that the recommended modifications will help preclude or mitigate potential PTS events, further work is required to quantify the "gain" in safety before credit can be given for improved margins (e.g., $RT_{NDT} + x^{\circ}F$). Based on the studies that have been performed to date, severe PTS scenarios have an estimated frequency in the range of 10^{-3} to 10^{-6} for generic Babcock & Wilcox, Combustion Engineering, and Westinghouse plants. Additional work is necessary to support these values and should include a more systematic identification process for potential PTS scenarios, a more detailed consideration of operator error, and a consistent treatment of multiple failures and potential dependencies.

In addition, time required for operator action plays a critical role in determining whether a potential PTS scenario may cause crack initiation. Therefore, it is essential that, as a part of the procedure upgrade implemented under Item I.C.1 of the TMI task action plan, the following items be addressed:

1. PTS-related procedures should be audited using time-line charts to identify critical time constraints and ensure that they are clearly noted to the operator.

2. Simulator and in-plant testing should be conducted to establish reasonable generic estimates for operator response times for certain critical PTS evolutions. (Example: X percent of operators can perform evolution B within C minutes.)

3. Licensee Event Reports (LERs) need to be reviewed to ascertain actual time-related information (including data on operator error) which can be used to supplement and validate information obtained in items 1 and/or 2 above.

---

*Require detailed analyses.
NOTE: Within the individual categories and classifications (e.g., instrument-oriented), recommended mitigating actions are listed in order of descending priority.

4.0 THERMAL HYDRAULICS

This section reviews the thermal-hydraulic phenomena that affect PTS. The plant transient scenarios that drive thermal-hydraulic response are evaluated, and the thermal-hydraulic analysis methods used to assess PTS are described. Recommendations are made for reducing thermal-hydraulic impacts resulting from PTS.

4.1 THERMAL-HYDRAULIC PHENOMENA AFFECTING PTS

Pressurized thermal shock is driven by the thermal-hydraulic conditions existing in the vicinity of critical welds in the pressure vessel of the reactor. Specifically, thermal-hydraulic phenomena establish the conditions of temperature and pressure that lead to thermal stresses. There are four principal thermal-hydraulic parameters that affect PTS:

- pressure in the reactor vessel
- cold-leg temperature
- thermal mixing in cold leg and downcomer
- surface heat transfer coefficients in downcomer.

These parameters are highly dependent on the plant response to transient or accident scenarios. The following sections describe the phenomena that affect these parameters.

4.1.1 Pressure

Pressure in the downcomer creates stresses in the pressure vessel walls. The pressure is driven by the plant response to actions of the control system or operators. Components of particular interest include primary system pumps, the feedwater system, the pressurizer, and emergency injection systems. The interrelated operation of the primary and secondary systems and their control systems define the pressure for specific scenarios.

4.1.2 Cold-Leg Temperature

Temperature reduction in the cold leg is the primary driving temperature for added thermal stresses in the pressure vessel wall. The reduced temperature cold-leg flow enters the vessel downcomer and cools the vessel walls as it flows to the lower plenum. Cooling of the wall causes added tensile stresses that could lead to internal cracking. Cold-leg temperature is highly dependent on transient scenarios. Transients that cause large reductions of cold-leg temperature lead to overcooling.

Although the temperature of the cold-leg flow can be reduced further by injecting colder water through the safety injection systems, the bulk of the cold-leg flow still provides the basic temperature that drives thermal stresses.

4.1.3 Thermal Mixing

Three mixing situations that can modify the water temperature in the cold leg and downcomer are: bulk mixing in downcomer, cold-leg injection mixing, and mixing in downcomer.

4.1.3.1 Bulk Mixing in the Downcomer

If all loops responded identically, all cold-leg temperatures would be identical, and the downcomer temperature would be uniform around the vessel. More realistically, however, one of the loops would be out of balance (colder) during a PTS transient. In that case, the flow from the colder loop could mix with the warmer flow from the other loops and result in some beneficial increase in temperature. The degree of benefit would depend on specifics of the downcomer flow pattern and other loop-flow conditions. If only negligible mixing occurred, then the lowest temperature cold-leg flow would drive the thermal stresses.

4.1.3.2 Cold-Leg Injection Mixing

Another source of cold water that could aggravate thermal shock is the injection of cold water from the emergency injection system. This water is normally near outside temperature and mixes with the much hotter water in the cold leg. Given natural circulation conditions, the typical flow ratio between cold-leg coolant and safety injection (SI) coolant is about five. The nature of this mixing depends on several factors: 1) the relative velocity of injected flows, 2) the turbulence levels, 3) density differences of the two flows, 4) the injection location, and 5) the specific geometry of the cold-leg piping.

Streams of relatively high velocity would have strong momentum interaction, high turbulence levels, and could be well mixed at the entrance to the downcomer. Low velocity injection in a slow moving (or stagnant) cold-leg flow would be affected by the density differences (buoyancy) of the two streams. Such flows would tend to stratify, and the colder fluid would move toward the bottom of the pipe. Superimposed thermal mixing would tend to reduce the temperature differences.

A variety of cold-leg flow patterns are possible (Figure 4.1). The most limiting case is low velocity injection in a stagnant cold leg where the injection flow moves along the bottom of the cold leg with little thermal mixing. The result would be the flow of the colder injection water down along the vessel wall. Some degree of flow mixing is realistic; heat transfer from the walls also heats the cold injection water. Bends in the cold-leg piping downstream of the injection point promote mixing by secondary flows.

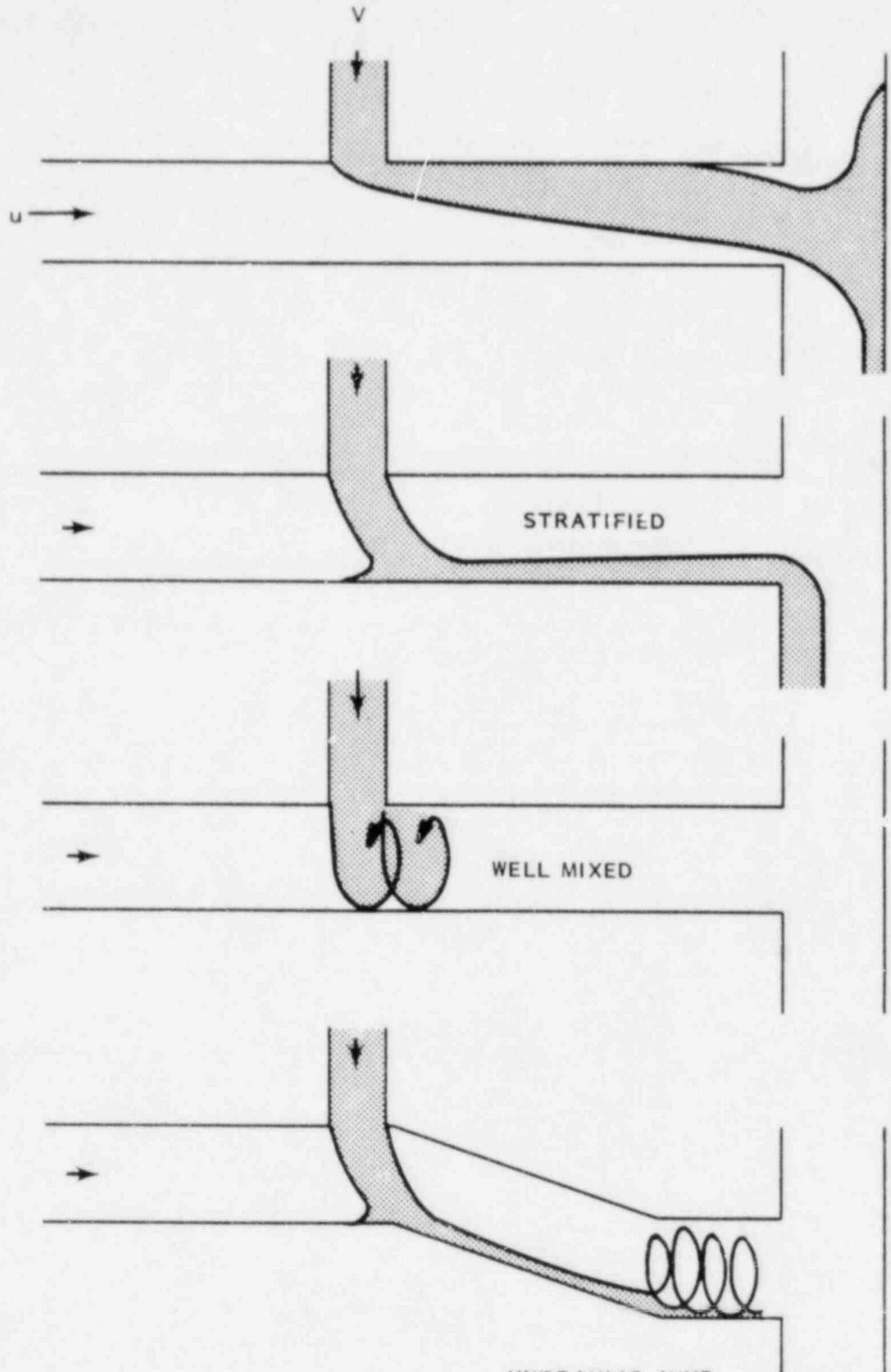

FIGURE 4.1. Four Examples of Cold Leg and Injection Flow Patterns (the long arrow implies high velocity)

4.1.3.3 Mixing in the Downcomer

If the cold water that is injected via the emergency system does not fully mix in the cold leg, additional mixing would occur in the downcomer. This mixing phenomenon is more localized than mixing that results from imbalanced loop flow and temperature. The situation of primary concern is the mixing of a stratified cold-leg flow where cold injected water flows along the bottom of the pipe. For low velocity cold-leg flow, the cold injected water would flow down along the vessel wall. This cold flow would be heated by the vessel wall and by mixing with the warmer flow in the remainder of the downcomer. Mixing could occur by turbulence and by buoyancy-driven circulation. Because welds of concern in the pressure vessel are located some distance below the cold legs, downcomer mixing could reduce the impact caused by emergency injection. High velocity cold-leg flows could be well mixed following the impact against the inner downcomer wall (core barrel).

Overall, thermal mixing superimposes a temperature correction on the most limiting cold-leg flow temperature. For well-mixed injection flows, the limiting cold-leg temperature would drive the thermal stresses. An additional correction could exist because of imperfect mixing.

4.1.4 Vessel Wall Heat Transfer Coefficient

The heat transfer coefficient on the surface of the vessel wall is an important parameter that affects the distribution of temperature in the wall. Both water temperature and the heat transfer coefficient control the rate of heat transfer from the wall. Heat transfer is closely related to the flow velocity in the downcomer. When the velocity is low, the heat transfer could be modified by buoyancy-driven flow redistribution or recirculation. This would occur as a result of fluid temperature differences in the water entering the downcomer or as a result of local heating of water close to the wall.

The magnitude of the wall heat transfer coefficient should also be compared with the conductance of the cladding on the inside walls of the pressure vessel. When the heat transfer of the fluid is high, the cladding heat transfer, not variations of fluid heat transfer, are controlling. When the fluid heat transfer coefficient is low, it is controlling and becomes a more sensitive parameter. The importance of the combined heat transfer coefficient and wall conductance can be determined by comparing them with the internal conductance of the vessel wall. When the internal conductance is high, the wall temperature gradients and thermal stresses could be moderated.

Although internal wall heat transfer is not the subject of the present discussion, it is worth pointing out that any parameter that affects wall heat transfer is a potentially important parameter.

4.2 UTILITY LICENCEES' THERMAL-HYDRAULIC ASSESSMENTS OF PTS

Utility licensees and owners groups were requested to submit an assessment of PTS to the NRC.[2,3,4] The following discussion reviews the thermal-hydraulic event scenarios, conservatisms, sensitivities, and analytical methods and codes used to assess a PTS event.

4.2.1 Plant Transients

The plant transients that could lead to potential PTS concerns can be caused by: 1) a leak/break in the primary system, 2) a leak/break in the secondary system, or 3) failure of the feedwater (FW) control system.

Actual scenarios include failure of pilot operated relieve valve (PORV), safety relieve valve (SRV), and turbine pypass valve (TBV); main steam line break; increase in feedwater flow (including failure of feedwater runback); and decrease in feedwater temperature.

These scenarios can be grouped into the following three types of transients:

1. small-break LOCA
2. steam line break (main SLB and small SLB)
3. excessive feedwater cooling.

Transients involving large-break LOCA are not included in the current PTS review.

The pressure and temperature behaviors for each of the three types of transients are very much plant-dependent; also they vary depending on what actions are taken by the operator and when the actions are taken. For a clearer understanding of the conditions used by the utilities and owners groups in their scenario analyses, comparison has been made among the three types of NSSS designs. The Babcock & Wilcox NSSS is based on Oconee-I design;[3d] the Westinghouse and Combustion Engineering NSSSs are generic types[4a,4b] with emphasis on H. B. Robinson 2 and Fort Calhoun, respectively.

The scenarios selected are small-break LOCA (SBLOCA), main steam line break (MSLB), and small steam line break (SSLB). Even though the feedwater transients have a higher probability of occurrence (see Chapter 3.0), they are not included in the comparison because the scenarios on feedwater transients discussed in the Oconee-I (B&W) 150-day report indicated they were less severe than the SLB cases in the secondary overcooling transients. Because the Westinghouse and Combustion Engineering plants have a larger thermal inertia in the secondary side of their steam generator, overcooling due to excessive feedwater flow would be a lesser concern than in a Babcock & Wilcox plant.

Comparison of the thermal-hydraulic analyses for SBLOCA by different utilities and owners groups is provided in Table 4.1. Figure 4.2 gives the pressure and temperature comparison. An approximate region (shaded region in the figure) is shown to indicate the relative margins of the transients to the pressure-temperature (P-T) combinations of a possible PTS concern. This region is based on a simplified and generalized fracture mechanics analysis using prescribed cooldown curves (see Figure 6.1). The cooling rate parameter (β) that corresponds to this region is 0.045. The RT_{NDT} is conservatively chosen to be 300°F (see Table 5.2). The final temperature (T_F) in Figure 6.1 is used as the downcomer temperature. Warm prestress is not reflected in this region.

For comparison, the Rancho-Seco overcooling data[4a] also are shown in Figure 4.2. The figure shows that the scenario based on Westinghouse analysis is of significant concern for resulting in a P-T combination that could lead to a PTS situation. This scenario assumed no local thermal mixing when the natural circulation was lost. Based on the information available to date on thermal mixing (see Section 4.3.2 for more discussion), this assumption is valid. It should be noted that the PTS region of concern in Figure 4.2 is just an indication of a possible PTS event. Because of the simplified procedure of establishing this region, it does not necessarily indicate an occurrence of pressure vessel wall crack initiation. It is used here as a reference for discussion on sensitivities and conservatisms.

The scenario based on Combustion Engineering analyses showed minimum margin to the PTS region of concern. The critical factors in all cases are the operator action and the time of action. For instance, in the Westinghouse scenario, if the operator throttles the HPI flow or restarts the RC pumps and/or controlls the feedwater systems, the natural circulation may be sustained longer, and the local thermal mixing may be enhanced. This could lead to the P-T curve not reaching the PTS region.

Factors influencing the thermal-hydraulic analysis of main steam line break transients are compared in Table 4.2. Figure 4.3 shows the calculated results based on the utility submittals. It can be seen that the P-T curves for all three different NSSS designs have small or no margins from the region of concern, and the crucial factor in keeping the margins positive is the actions taken by the operator. In Babcock & Wilcox and Westinghouse plants, a time length of 10 min is allowed for operator action, while for a Combustion Engineering plant it is 30 min. (See Chapter 3.0 for a review of the operator response times.)

All three analyses assumed total mixing at the downcomer. The uncertainty associated with local fluid mixing can easily cause the negative margins to increase (i.e., become more negative).

The assumptions and conditions used for the small SLB analyses are compared in Table 4.3 for three different NSSS groups. Figure 4.4 gives the P-T results. Generally speaking, the same comments as given in the SBLOCA and main SLB cases apply here; mainly, operator actions strongly influence the pressure/temperature behavior.

TABLE 4.1. Comparison of PTS Thermal-Hydraulic Analysis for Small-Break LOCA

Influencing Factors	B&W[a]	CE[b]	W[b]	Remarks
Break location	Pressurizer (safety valve)	PZR (PORV)	Hot leg and PORV composite	Pressurizer and hot-leg. breaks are conservative compared to cold-leg breaks
Sensitivity on break location	Not clear	Yes	Yes	
Break size (ft^2)	0.023	0 → 0.01	2 in. hot leg PORV composite	
Sensitivity analysis on size	Not clear	Yes	No	
SI flow	Throttled at 93 min	Max	Max	Oconee plant throttle has minimum effect on P-T curves
SI temperatures (F)	50	40	40	
Sensitivity of SI temperatures on EFPY	Unknown	Unknown	Unknown	W claims increase of 40°-80°F increases EFPY "a few years"
RCP trip at RX trip	Yes	Yes	Yes	
Mixing model	Turbulent jet mixing	Ellison and Turner's entrainment	Total mixing w/N.C.[d] No mixing w/out N.C.	
Effect of vent Valve Flow on Mixing	Strong	NA[c]	NA	

TABLE 4.1. (Contd)

Influencing Factors	B&W[a]	CE[b]	W[b]	Remarks
Decay heat	100% ANS	100%	100%	
Metal heating	Yes	No	Unknown	
Reverse S.G. heat transfer	Yes	Yes	Unknown	
System code	RETRAN02 MOD1	CE FLASH -4AS	NOTRUMP	
Mixing code	(e)	Mixup	VARR-II	
Operator action	Throttle HPI at 93 min	1. PORV opened at 10 min 2. FW restart at 30 min	Throttle Aux. feedwater to keep SG	1. Operator action on feedwater in CE plant is required to prevent core uncovery. 2. W operator action has little affect on P-T (increase primary temperature slightly).
Auxillary feedwater on/off	Off	Off	Throttled	

(a) Oconee-1.
(b) Generic plant.
(c) Not applicable.
(d) Natural circulation.
(e) Hand calculation based on turbulent jet mixing model.

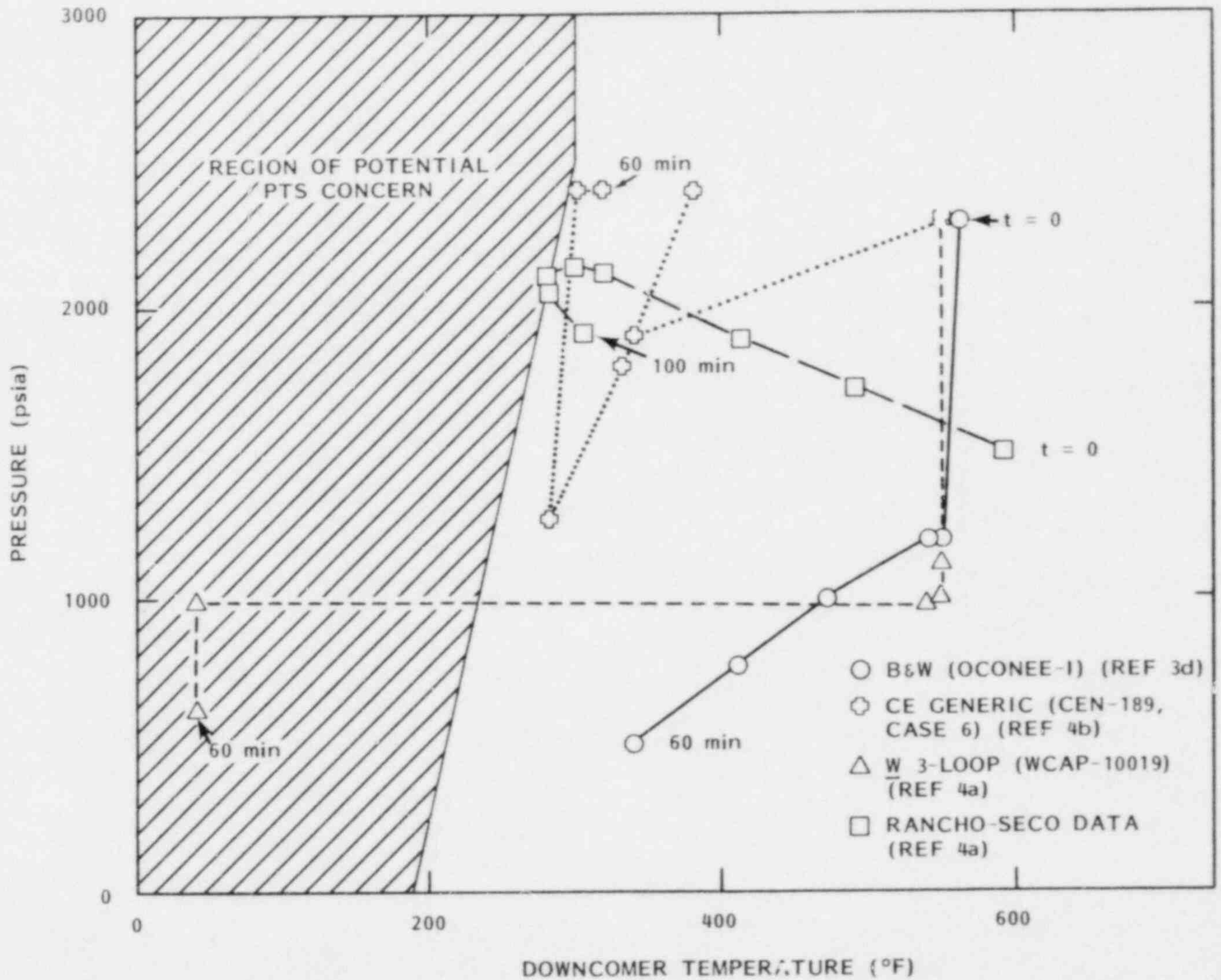

FIGURE 4.2. Pressure vs Downcomer Temperature for Small-Break LOCA

TABLE 4.2. Comparison of PTS Thermal-Hydraulic Analysis for Main Steam Line Break Accident

Influencing Factors	B&W[a]	CE[b]	W[b]	Remarks
Power	HFP	HZP	NA[c]	
Decay heat	50% ANS	0%	0% for temp. 100% for press.	
RCP	Trip on low RCS pressure	Trip on low PZR pressure	1. RCP on 2. RCP trip at t = 0	
Operator action	Isolate feed-water to affected SG in 5 min, start one RCP in each loop at 10 min	Trip RCP at 30 sec, throttle HPI to control pressure at 30 min	Terminate Aux FW to affected SG and HPI flow in 10 min	For B&W plant, if operator delays restarts of RCP, a prolonged loss of natural circulation could occur.
MFW isolation	Yes (at 5 min)	Yes[d]	Yes	
Aux FW isolation	NA	Yes[d]	Yes (at 10 min)	
HPI throttle	No	Yes	No (terminated at 10 min)	
Metal heating	Yes	NA	No for temp. Yes for press.	
Mixing	100%	100%	100%	
SG reverse transfer	Yes	Yes	No for temp. Yes for press.	
System Codes Used	RETRAN-02 MOD1	CEFLASH-4AS	LOFTRAN	
HPI temperature	50°F(implied)	NA	32°F	
Aux FW temperature	NA	NA	32°F	

(a) Oconee-1.
(b) Generic plant.
(c) Not applicable.
(d) Fort Calhoun Plant.

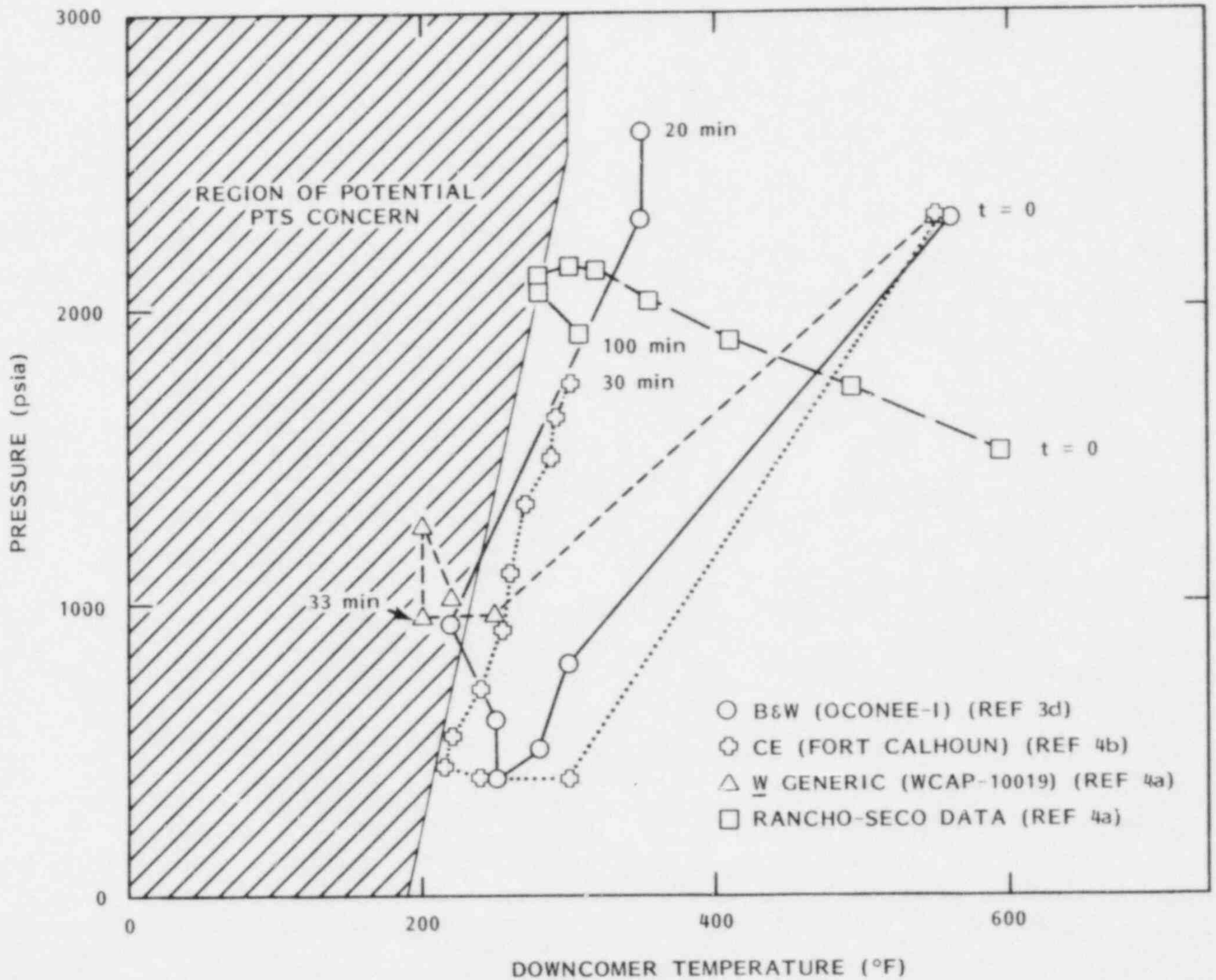

FIGURE 4.3. Pressure vs Downcomer Temperature for Main Steam Line Break

TABLE 4.3. Comparison of PTS Thermal-Hydraulic Analysis for Small Steam Line Break

Influencing Factors	B&W[a]	CE[b]	W[b]	Remarks
Location	Turbine bypass valves (2)	Atmospheric dump valve	Steam safety valve	It is believed that the use of TBV failure in the B&W case is due to the frequency of its fail-open in the observed abnormal events at Oconee
Power	HFP	HZP	HZP	
Decay heat	0%	0%	0%	
RCP Trip	No trip	Trip on low PZR pressure	NA[c]	For Oconee-1, RCP on/off is not sensitive
Operator action	Isolate EFWS at 20 min	Trip RCP at 10 min on low PZR pressure	No action	For Oconee-1, if operator does not isolate EFWS, system may cool down to conditions of T <250°F and P >1500°F
HPI throttle	No	Yes	NA	
MFW	No runback before trip at 39 sec	NA	NA	
Metal heating	Yes	NA	NA	
System code used	RETRAN-02 MOD1	CEFLASH-4AS	LOFTRAN	
Mixing	100%	100%	100%(implied)	

(a) Oconee-1.
(b) Generic plant.
(c) Not applicable.

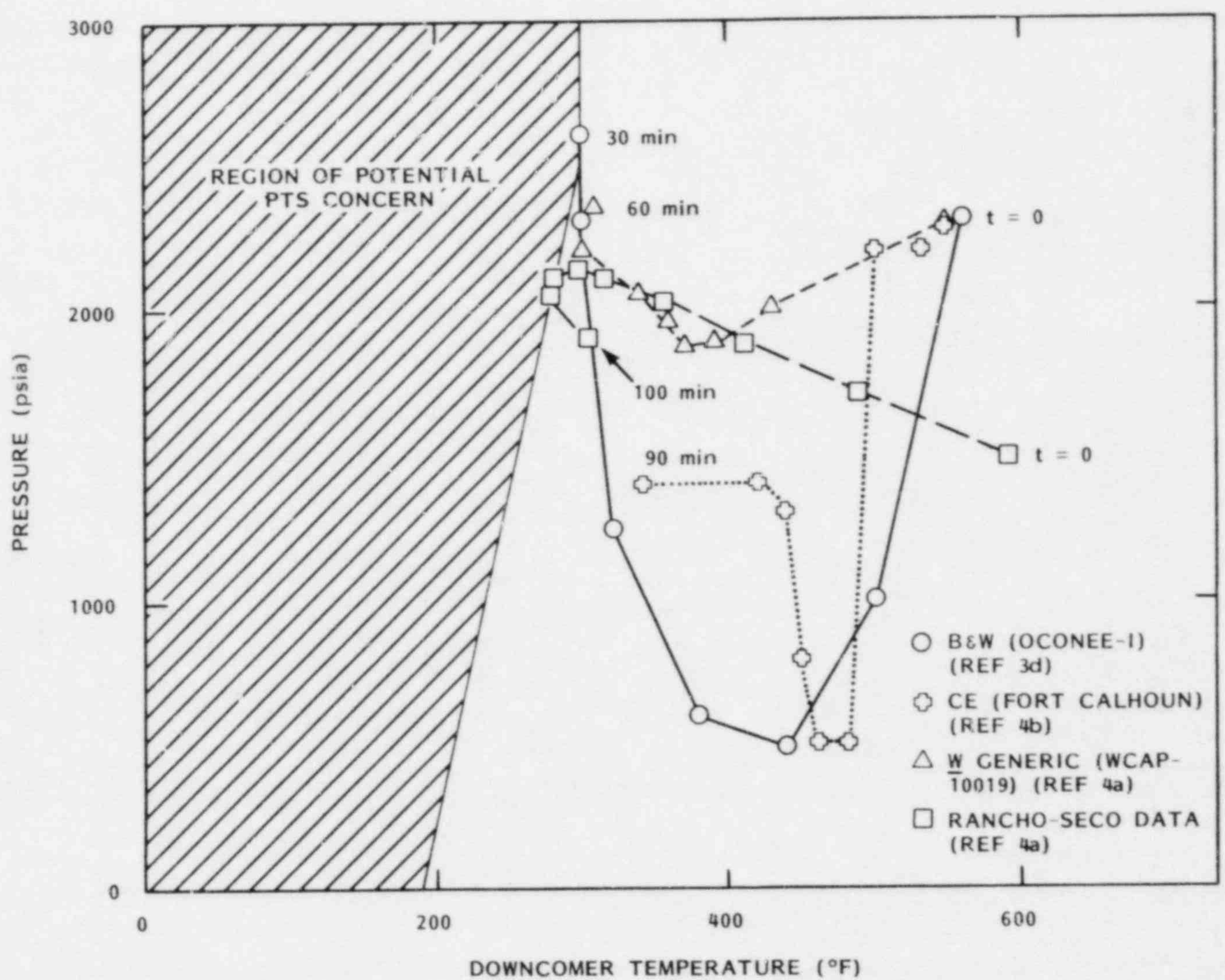

Figure 4.4. Pressure versus Downcomer Temperature for Small Steam Line Break

4.2.2 Conservatism and Sensitivity

This section describes the conservatism and sensitivity of the analyses presented in the 150-day responses of the utilities and in the NSSS generic reports. More knowledge on the sensitivity of key parameters and the conservatism on other assumptions could be gained in the future through the scenario studies and systems analysis underway at several national laboratories.

4.2.2.1 Small-Break LOCA (see Table 4.1)

It is reasonable to assume a small-break LOCA would occur in a hot leg or pressurizer location. In this case the maximum amount of cold leg and HPI water would reach the critical downcomer areas. In addition, the probability of a pressurizer leak is much higher than for other types of SBLOCA. Regarding the break size, the most conservative case is judged to be the one with the maximum break size which will start to lose natural circulation within the transient period of interest. In this case, because there is no natural circulation, the degree of local fluid mixing in the downcomer will be lower.

The sensitivity of break sizes has been addressed to a certain extent by Combustion Engineering. If combined with the probability of occurrence at the locations analyzed by the owners groups, the selection of break sizes seems to be reasonable. More studies on break-size sensitivity are needed.

The SI temperatures had been assumed at 50°F for Oconee-1 and 40°F for Westinghouse and Combustion Engineering. The safe operation of a vessel during a PTS event is sensitive to the downcomer fluid temperature next to the vessel wall. Therefore, the SI fluid temperature would be important under the loop condition where a low degree of mixing is expected (i.e., under a low loop flow rate). This aspect is not well addressed in the utilities' analyses.

The 150-day submittals did not address in detail the sensitivity of operator actions and the time frame for the actions. However, the Oconee submittal provided an analysis of the data from the abnormal events which occurred at the site and used the data as the basis for selecting these parameters. As mentioned in the previous subsection, operator actions have a rather large impact on the outcome of the thermal-hydraulic analysis. A more detailed analysis of the different scenarios of operator actions and the associated probabilities of occurrence should be performed.

4.2.2.2 Main Stream Line Break (see Table 4.2)

For Oconee-1, the transient starts at hot full power (HFP) condition, whereas for Combustion Engineering plants, it starts at hot zero power. These conditions are believed to be reasonable due to the different design of Babcock & Wilcox steam generator (once-through) from the U-tube design. In the latter case, the hot zero power condition would give the largest inventory of feedwater in the steam generators (SG), which leads to a maximum cooldown in an MSLB event. (It was not clear from the submittals what condition Westinghouse used).

The tripping of the reactor coolant pump (RCP) early in the transient is conservative because of the higher system pressure and lower downcomer temperatures (less mixing) caused by the tripping. Regarding the mixing of the SI fluid and cold-leg coolant, the assumption of total mixing used by all three NSSS groups is reasonable as long as the loop circulation is maintained. Maintenance of loop circulation for MSLB in certain conditions needs operator action; however, these actions were not clearly addressed in the submittals.

As mentioned previously, the time allowed for operator action for all three cases could be insufficient. More analyses are needed to give the sensitivity of the pressure-temperature behavior to the action time.

4.2.2.3 Small Steam Line Break (see Table 4.3)

The break locations of the small SLB transients are similar for all three plant designs. Assumptions that breaks occur in these locations are reasonable based on their probability of occurrence.

Sensitivity analysis on the break sizes of the small SLB was not well addressed. In the Oconee analysis,[3d] past experience on abnormal events was used as the basis for selecting turbine bypass valves as the break initiators. A more detailed analysis based on a systematic approach (for example, probabilistic risk analysis), should be conducted to identify the worst cases.

The assumption of total mixing in the case of small SLB is more acceptable because of the unlikely situation of losing natural circulation before sufficient time has passed (approximately one hour) for operator to take corrective action.

Again, sensitivity analysis of operator actions is not sufficient for all submittals. The analysis is needed to make certain that the type of operator actions that could lead to severe pressure-temperature combinations are not likely to occur.

4.3 ANALYTICAL METHODS

The utilities and the three owners groups used analytical methods to calculate pressure, temperature, and heat transfer at the inside wall of the pressure vessel. The analyses performed by the three groups followed the same basic approach and used a two-step procedure. The first step was a calculation of overall plant response (i.e., system analysis) to selected accident scenarios. The primary result was the pressure and bulk temperature in the cold leg and reactor downcomer. The second step of the thermal-hydraulic analysis considered the thermal mixing aspects of high pressure injection in the reactor cold leg. The following discussion presents a review of the analytical methods.

4.3.1 System Analysis

The system analysis was performed using one-dimensional computer codes to calculate the overall temperature and pressure for selected PTS accident scenarios. The specific codes that were used depended upon the specific accident scenario and the plant design. A summary of the codes used for the system analysis is provided in Table 4.4. A distinction can be made between the small-break LOCA and the overcooling accidents. Separate computer codes were used by each group for these two types of accidents.

Features of the codes used for the small-break LOCA are compared in Table 4.5. They are all one-dimensional codes based on a mixture representation of two-phase flow. They also assume thermal equilibrium. This means that liquid and vapor are at saturation temperature wherever two-phase flow exists. It also means that streams are fully mixed once they are combined, and that there is no inherent provision for temperature disparity from incomplete mixing.

All of the codes are used to consider forced and natural circulation of single- and two-phase flows. They are all based upon an equilibrium two-phase flow model with various levels of assumption regarding the relative velocity of liquid and vapor. The Combustion Engineering and Westinghouse models have provision for relative phase velocity to account for the separation of steam and water in slowly moving two-phase flows. The Babcock & Wilcox model assumes equal phase velocity and would apply to single-phase flows or to conditions where homogeneous two-phase flow would exist. It would not apply to cases of vertical, low velocity, counter-current, steam-water flow where phase separation could occur. The modeling of phase separation is important for accurately predicting natural circulation flow in the reactor loops or flow through the vent valves in Babcock & Wilcox plants. Vapor accumulation

TABLE 4.4. Computer Codes Used for System Analysis

Accident Type	Babcock & Wilcox	Combustion Engineering	Westinghouse
Small break LOCA	CRAFT	CEFLASH-4AS[a]	NOTRUMP
Main steam line break	RETRAN[b]	CEFLASH-4AS	LOFTRAN
Small steam line break	RETRAN	CEFLASH-4AS	LOFTRAN
Steam generator overfeed	RETRAN	(c)	(c)

(a) Applied to small break LOCA plus loss of feedwater.
(b) 02MOD0001.
(c) Not analyzed.

TABLE 4.5. Comparison of Computer Code Features for Small Break LOCA Analysis

Code Features	B&W CRAFT	CE CEFLASH-4AS	Westinghouse NO TRUMP
Primary system representation	6 nodes	not given	not given
Secondary system representation	1 node	not given	not given
Two-phase flow model			
mixture equations	yes	yes	yes
Relative velocity	no	yes[(a)]	yes[(a)]
Phase separation	no	yes	yes
Horizontal leg Counter-current flow	no	yes	yes
Thermal equilibrium	yes	yes	yes
Thermal nonequilibrium	no	no	no
Wall heat transfer	yes	yes	yes
Natural circulation	yes	yes	yes
Downcomer mixing	fully mixed	fully mixed	fully mixed

(a) Specified relative velocity (drift flux correlations).

at high points in the system could stop or prevent natural circulation flow. The re-establishment of natural circulation flow would require vapor condensation upon system repressurization.

None of the computer codes considers effects of noncondensable gases. For those transient scenarios where the pressure drops low enough and long enough, (nitrogen) noncondensable gas could enter the primary system via the emergency core cooling system (ECCS) accumulator. Should the system lose (or have lost) natural circulation, the noncondensable gas could prevent the re-establishment of natural circulation upon repressurization. The noncondensable gas also changes the pressuretemperature relationship from that of the steam table and could introduce an element of confusion to plant operators. Although the probability of introducing a noncondensable gas from the accumulator may be small, it is prudent to address the situation.

All of the computer codes used for the system analysis have the ability to consider multiple loops. Although not explicitly stated, it is understood that the PTS analysis used the minimum cold-leg temperature. For loop imbalances where one cold leg is at a lower temperature than the others, it would be overly optimistic to assume a fully mixed downcomer. The flows from each of the cold legs would have little opportunity to mix in the downcomer, and it would be more realistic to use the minimum cold-leg temperature to analyze the thermal stresses in the vessel wall. Any further temperature reduction from HPI would be superimposed on the cold leg having the minimum temperature. An accurate calculation of the bulk cold-leg temperature is fundamental to the analysis of PTS.

The ability of computer codes to calculate the proper system pressure depends upon the ability of the code to calculate condensation and flashing phenomena. Condensation is of particular importance because it can be important to the system repressurization during HPI. Most computer codes can consider flashing; however, there are still difficulties with analytically modeling condensation of steam against subcooled water. An example is the pressurizer where condensation of steam occurs at the water surface during inflow of subcooled water. Heat transfer from the pressurizer wall that affects this process. The ability to consider condensation more accurately is not code-specific, but is a weakness of current analytical models.

4.3.2 Mixing Analysis

The analyses submitted with the 150-day responses included the effect of mixing cold HPI with the warmer water in the reactor cold leg and the subsequent mixing along the vessel wall in the downcomer. The results of the mixing analysis were used together with the system results to estimate the temperature at the vessel wall.

4.3.2.1 Cold-Leg Mixing

A variety of methods were used by the utility groups to estimate the benefit of mixing. The features of the analytical models used for mixing in the cold leg are summarized in Table 4.6. The modeling ranged from no credit to a rather sophisticated two-dimensional representation that indicated substantial mixing. The model for the Combustion Engineering reactor accounted for a small degree of mixing.

4.3.2.2 Downcomer Mixing

The downcomer mixing models used in the analysis for the 150-day responses are compared in Table 4.7. All analytical models claimed credit for mixing in the downcomer. The models ranged from a two-dimensional analysis of the combined cold leg and downcomer to assuming a fully mixed downcomer flow based upon a high level of mixing calculated for the cold leg.

While two-dimensional analysis can provide more realistic assessments than one-dimensional analysis, caution must be used in assessing results. Multidimensional turbulence mixing models suffer from enhanced mixing caused by numerical diffusion, and they can miss some of the observed phenomena such as hydraulic jumps and secondary flows.

TABLE 4.6. Comparison of Cold-Leg Mixing Models at HPI Injection

Reactor Type	Model Description
B&W(1)	No credit for mixing in cold leg.
B&W(2)	See Table 4.7.
CE	a. One-dimensional falling plume with entrainment at injection point
	b. Horizontal gravity stratified flow without mixing downstream of injection
	c. No credit for pipe wall heating
W	a. Two-dimensional model of cold leg with injection
	b. VARR-II code with two-parameter turbulence model
	c. Momentum interchange of injection flow with cold-leg flow
	d. Buoyancy effects included

The analytical models used by owners groups assume a substantial amount of mixing of the HPI in the cold leg and downcomer. Their analyses indicate that the cold injection water does not contact the vessel wall. Instead, the vessel wall is closer to the temperature of the bulk downcomer flow. While the analytical methods are tentative and not fully verified for the current application, they appear to provide a reasonable estimate of the mixing benefit based upon the results from the CREARE 1/5-scale experiments described below.

4.3.2.3 CREARE Experimental Results

A series of 1/5-scale experiments were performed at CREARE[5,6] with the specific purpose of investigating mixing downstream of the HPI in the cold leg and downcomer. Density differences between the cold injected water and the warmer, cold-leg water was created by both temperature and salinity differences. Dye was used with the injected water to help visualize the flow patterns and mixing phenomena.

Two different geometric arrangements were used in the experiment. The first was for a geometry that approximated a Babcock & Wilcox plant. It had a sloped cold leg and had provision for vent-valve flow. The cold-leg flow was zero (stagnant) for all tests. This is judged to be a conservative assumption. The second geometric arrangement approximated a Combustion Engineering or Westinghouse design and considered nonzero cold-leg flows. The cold leg, vessel wall, and downcomer had thermocouples to measure temperatures.

TABLE 4.7. Comparison of Downcomer Mixing Models

Model	Description
Model A of B&W (2)	a. Turbulent jet mixes with hot vent flow in downcomer
	b. Mixing velocity and temperature based on Reichardt's fully developed jet
Model B of B&W (2)	a. Two-dimensional representation of cold leg, injection, and downcomer
	b. Used FLOW-2D code based on original SOLA-VOF.
	c. Donor/acceptor logic used to reduce numerical diffusion
	d. Turbulent kinematic viscosity used for diffusion of mass and momentum
	e. Substantial mixing is calculated for cold leg and downcomer; cold HPI injection does not reach vessel wall near welds.
CE	a. Temperature stratified flow at entrance to downcomer.
	b. Falling plume with entrainment in downcomer (extension of injection point model)
W	a. Fully mixed downcomer flow based on high level of mixing calculated for cold leg with natural circulation flow.

The Westinghouse and Combustion Engineering model experiments indicated that substantial mixing is possible in the cold leg, but this depends upon the relative magnitude of cold-leg flow, injection flow, injection velocity, and difference in density of the two streams. When the cold-leg velocity is high, there is very little tendency for buoyancy stratification and the flow is well mixed when it reaches the downcomer. When the cold-leg flow is slowly moving, there is substantial stratification, but there is still some mixing. When the injection velocity is high (small diameter nozzle), there is a high degree of mixing, even for slowly moving cold-leg flows. The tests also show that pipe bends can promote mixing by creating hydraulic jumps and secondary flows. The overall conclusion from the tests is that there is substantial benefit of mixing in the cold leg, especially when the cold-leg flow exceeds the injection flow by about a factor of 5. The mixing benefit decreases for lower flow ratios.

Figure 4.5 shows a plot of temperature at thermocouple location T7 versus HPI flow (expressed as Froude number) for the modelled Babcock & Wilcox test setup with bottom injection.[a] Temperature at T7 is the minimum temperature measured on the downcomer wall. The thermocouple is located 6.8 in. below the cold-leg center line. The temperature is well above the 70°F injection temperature for the conditions tested. At the highest injection flow tested, temperature at T7 is about midway between the 70°F injection temperature and the 150°F vent-flow temperature. Flow visualization photos using dye show substantial stratification in the cold leg, but with enhanced mixing (hydraulic jump and secondary flows) at the bend prior to entrance to the downcomer. The tests show that there is considerable mixing benefit available, even under conditions of zero loop flow. No data are available for reduced or zero vent flow.

A plot of T7 versus the ratio of loop flow to HPI flow is shown in Figure 4.6 for a cold-leg configuration similar to Combustion Engineering and Westinghouse plants. Thermocouple location T7 is on the downcomer wall 6.8 in. below the cold-leg center line and is also the location of minimum wall temperature. Figure 4.6 shows that T7 is only a few degrees lower than the simulated nominal loop temperature of 150°F for flow ratios greater than 10 and injection temperatures of 65°F. At a flow ratio of 2 (minimum tested), the wall at location T7 is about 120°F. Wall temperatures are higher below T7 because of additional mixing.

Based upon the CREARE experiments, claiming a substantial credit for mixing of cold HPI water in the cold leg and downcomer is justified. While uncertainties exist in the credit taken for mixing, they are judged to be less than the uncertainties in calculating the bulk cold temperature for many transient scenarios. This shifts the emphasis of PTS analysis to the determination of bulk cold-leg temperature and to the definition of those accident scenarios that lead to both severe overcooling and high pressure.

The justification for substantial mixing is based on experimental results with loop flow (Combustion Engineering and Westinghouse) and with vent flow (Babcock & Wilcox). For transient scenarios involving zero loop flow and/or vent flow, the mixing credit could be marginal. Additional experimental data for stagnant conditions would be useful.

Finally, it should be realized that the calculation of the bulk cold-leg temperature is still of fundamental importance to the PTS analysis. The effect of mixing the HPI injection with the cold-leg flow can impose an additional water temperature correction in the vicinity of critical vessel welds.

(a) Injection is on the side of the pipe in actual B&W plants.

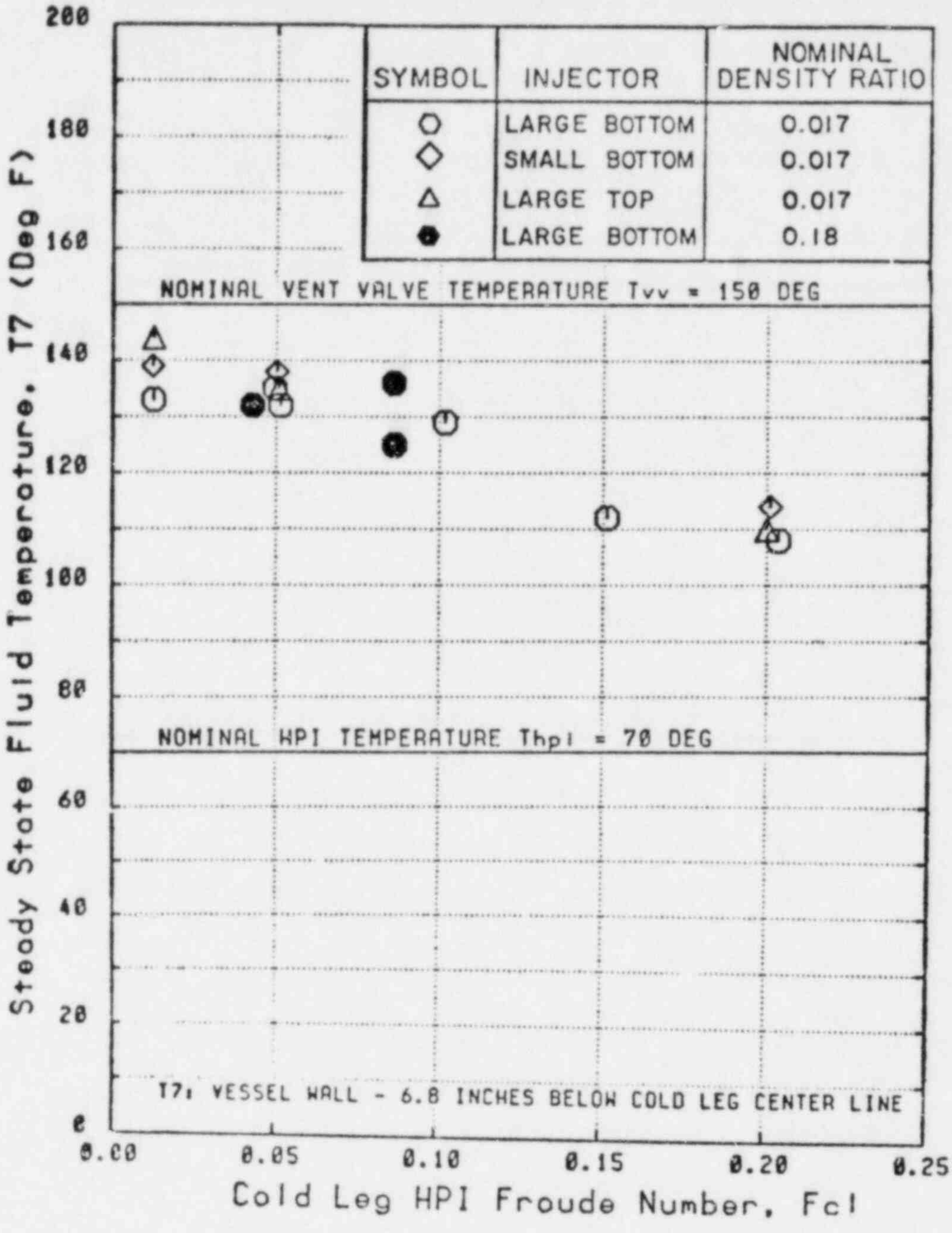

FIGURE 4.5. Downcomer Vessel Temperature T7 as a Function of Froude Number[5]

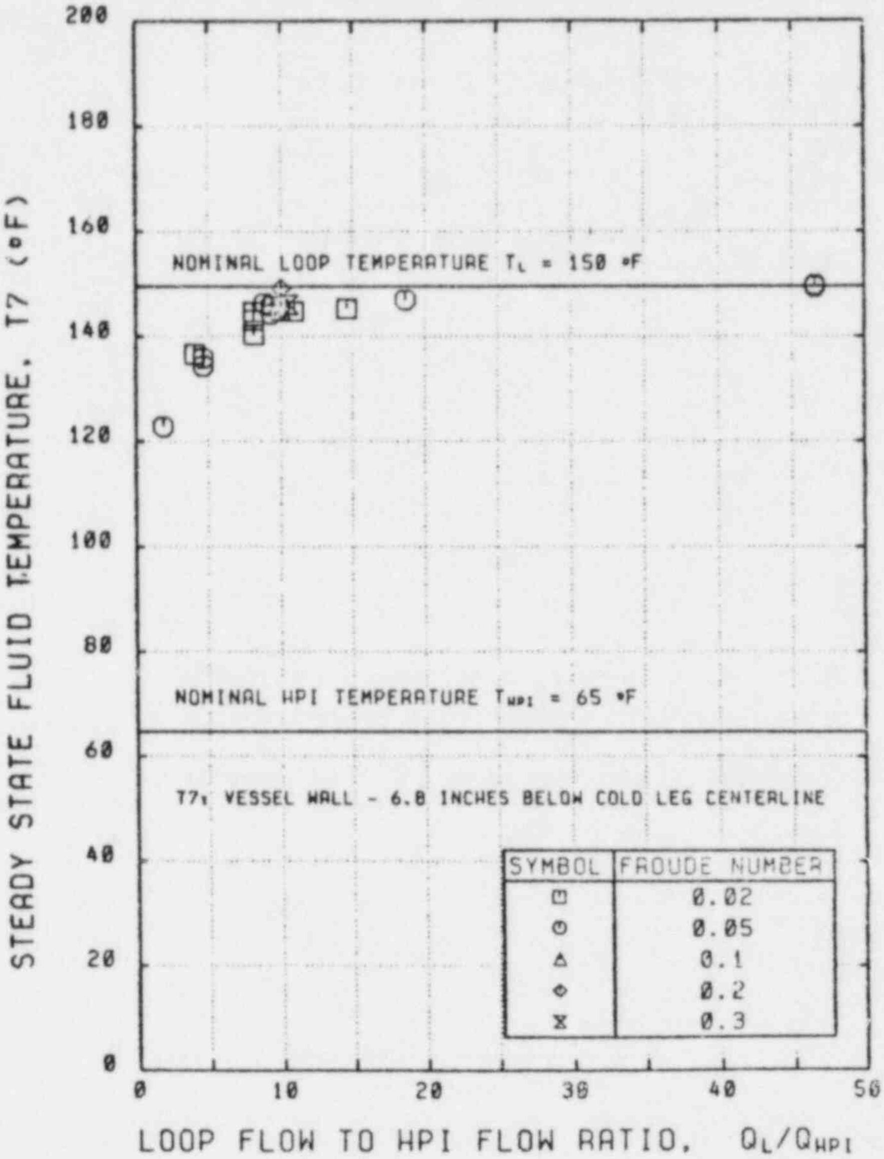

FIGURE 4.6. Measured Steady-State Temperature on Vessel Wall[6]

4.3.4 Heat Transfer at the Vessel Wall

4.3.4.1 Vessel Cladding and Fluid Heat Transfer

Thermal stresses in the reactor vessel wall are driven by the temperature distribution in the vessel wall. The two parameters that have the greatest effect on temperature distribution are the fluid temperature in the downcomer and the surface heat transfer coefficient.

The heat transfer modeling approach used in the 150-day responses is summarized in Table 4.8. The radial temperature distribution in the base metal is of primary importance. Some additional two and three-dimensional analysis is also indicated. The cladding was modeled as part of the surface heat transfer coefficient in the Babcock & Wilcox analysis model. Various heat transfer modes were considered, including an assumed constant value in the Combustion Engineering analysis on the basis that the vessel cladding is controlling.

The sensitivity of the temperature distributions to surface heat transfer can be assessed by comparing its magnitude to the cladding and base metal conductances. When the surface heat transfer coefficient is high, internal and cladding conductance are in control. Gradients are the largest in these cases. When the heat transfer coefficient is low relative to internal conductance, wall temperature gradients are typically flat, and the temperature changes uniformly with time.

TABLE 4.8. Comparison of Heat Transfer Models

	Babcock & Wilcox	Combustion Engineering	Westinghouse
Analytical model of wall	Two-dimensional (r,θ), finite element(a)	One-dimensional (r), finite element(d)	one-dimensional (r), finite difference
Analytical model of cladding	Part of surface heat transfer coefficient	One-dimensional (r) finite element	One-dimensional (r), finite difference
Modes of heat transfer at surface	Maximum of forced or free convection	Constant value	Film boiling nucleate boiling forced convection free convection
Range of heat transfer coefficient (Btu/hr ft^2°F)	50-1500(b) 3000(c)	300	not given
Vessell cladding contact conductance(e)	∞	∞	∞

(a) ANSYS code; some three-dimensional analyses also.
(b) Small-break LOCA; max (h_{forced}, h_{free}).
(c) Overcooling transients; h_{forced} is constant.
(d) MARC code; some two-dimensional analyses.
(e) Large conductance for weld deposited cladding.

The role of the cladding can be considered to a first approximation as part of the surface heat transfer coefficient. The overall heat transfer coefficient in series can be written as

$$h_{overall} = \frac{1}{\frac{1}{h_{surface}} + \left(\frac{\Delta x}{k}\right)_{cladding}}$$

For cladding on the order of 0.2 in. thick and k = 0.8 Btu/hr in.°F, the cladding conductance is 575 Btu/hr ft^2°F. For a wall thickness of 8.6 in. and k = 2.2 Btu/hr in.°F, the vessel wall conductance is 37 Btu/hr ft^2°F. When the surface heat transfer coefficient is large, as for nucleate boiling or forced convection, the cladding controls the heat transfer from the base metal. In that case uncertainties in the fluid heat transfer coefficient have a small effect. When the surface heat transfer coefficient is of the same order or smaller than the cladding conductance--as in free convection--it has a much more sensitive effect on the wall temperature. Furthermore, if surface heat transfer is reduced to be of the same order or smaller than the wall conductance, the surface heat transfer is very controlling. In this case, however, the major temperature gradient is at the fluid surface and the wall temperature gradients are minimal.

Thus, uncertainties of surface heat transfer coefficients in the midrange, as could exist for free convection, would have the greatest effect on wall temperatures. The combination of surface heat transfer coefficient and cladding conductances also raise the minimum temperatures of the base metal above the water temperature in the downcomer, and this benefit gets larger as the surface heat transfer coefficient gets smaller.

For the forced convection heat transfer film coefficients, Westinghouse(4a) and Babcock & Wilcox (Oconee)(3d) used the Dittus-Boelter correlation. This is a rather standard practice. As discussed previously, the coefficients for this mode of heat transfer usually are large, and they do not contribute significantly to the temperature gradient. Combustion Engineering(4b) used a constant value of 300 Btu/hr ft^2°F instead of a correlation for the transients analyzed in their generic report.(4b) This may not be conservative for the initial phase of the SBLOCA and for overcooling transients where large heat transfer is expected.

For the natural convection case, the value of surface heat transfer is more sensitive, as discussed previously. Comparisons were made between the correlations used by the three owners groups. Based on a condition of saturated water at 300°F (T_{bulk}), and a ΔT ($T_{surface} - T_{bulk}$) of 30°F, the coefficient at 140 in. below the cold-leg nozzle centerline is estimated to be 330 Btu/hr ft^2°F using the Westinghouse correlation,(4a) and 165 Btu/hr ft^2°F using the Babcock & Wilcox correlation.(3d) Combustion Engineering assumed a constant value of 300 Btu/hr ft^2°F. An independent evaluation using Kato et al. model,(7,13) which is based on a detailed derivation, yields a value of 264 Btu/hr ft^2°F. This correlation is expressed in the following form:

$$H = 0.138\, Gr_x^{0.36} (Pr^{0.175} - 0.55)\, K/L \qquad \text{for } Pr \approx 1$$

The notations in the correlation are standard.(13) It can be seen that under the typical natural circulation condition cited here, a heat transfer coefficient of around 300 seems to be reasonable. Using a different set of fluid conditions with T_{bulk} = 500°F and ΔT = 15°F yields 420 Btu/hr ft^2°F for W, 200 Btu/hr ft^2°F for Oconee, and 325 Btu/hr ft^2°F from the Kato model. From the above analysis, it is recommended that for free convection heat transfer, the Kato et al. correlation(7) should be used as a criterion. Other models for free convection should yield values equal to or larger than that of the Kato correlation.

4.3.5 Conclusions Regarding Analytical Methods and Experiments

1. The ability to calculate cold-leg pressure and temperature are fundamental to assessing PTS.

2. The use of existing NRC-approved methods for system analysis appear satisfactory; however, there are several items that warrant attention:

   a) effect of including loop temperature imbalances

   b) further verification of condensation modeling in components, especially the pressurizer

   c) breakdown and/or re-establishment of natural circulation for low velocity two-phase flows.

3. Experimental evidence at 1/5-scale indicates that there can be significant mixing benefits downstream of HPI in the cold leg except for very low flows.

4. Vessel wall heat transfer, and the resulting temperature gradient, are controlled by the cladding conductance and surface heat transfer. The surface heat transfer is most sensitive when it is in the free convection (natural circulation) mode.

4.4 DISCUSSION OF REMEDIAL ACTIONS IN THERMAL HYDRAULICS

There are several possible design and operator changes that could affect plant thermal-hydraulic response to PTS concerns. The following provide a list of items deserving consideration and a discussion about their advantages and drawbacks.

4.4.1 Heating the ECC Water

The motivation for heating the safety injection water is to reduce the thermal shock caused by the injection of cold water. Based upon the results of the CREARE 1/5-scale experiments, the cold water becomes well mixed by the time it reaches the critical weld regions of the downcomer when there is loop flow (or vent flow for Babcock & Wilcox plants). Preheating the injected water would offer only limited improvement when those conditions exist, and preheating would not be of significant benefit. Greater benefits would be possible if actions could be taken to limit the bulk cold-leg temperature for overcooling transients.

Under conditions of stagnant cold-leg flow (and vent flow), the mixing benefits could be substantially less. Should these conditions be judged credible, heating of the injection water could be of greater benefit.

No specific safety injection temperature requirement should be made unless benefits for the PTS issue can be proven. Through detailed analyses and surveys, the cost and design feasibility of maintaining the large tanks at higher temperatures can be determined. Such analyses could also be used to determine suitability of installed equipment (ratings, capacities, pump cooling, NPSH for pumps, Boron degradation, etc.) for higher temperature operation. Requiring a significant increase in safety injection water temperature would have a major impact on almost any effects, most of which would be negative. A more detailed analysis is needed to clearly establish the relative benefits of such an approach.

Heating the ECC water is not recommended as a near-term corrective action because it lacks clear benefits and may have possible negative effects. This should be left as a long-term study item.

4.4.2 Limiting Feed Water Flow

Some overcooling scenarios can be traced to feedwater flow in excess of those for normal plant operations. Flow restrictions placed in the feedwater system could limit the flow to reduce the overcooling. There are tradeoffs which must be analyzed further to determine the feasibility and benefits of this option. Modifications to plant design, which would automatically terminate or reduce either safety injection or auxiliary feedwater flow, should be considered with caution. Either approach would affect the operator's ability to safely control the plant, because it would take away cooling capability. Any recommendation to restrict flow in the feedwater system would be premature at this time.

4.4.3 ECC Injection Mixing Experiments

Experimental work on mixing, such as that at CREARE under EPRI sponsorship, should be continued to develop a more complete understanding of mixing within the cold leg and downcomer. Specific attention is needed for conditions of stagnant loop (and vent) flow. Attention should also be given to the scaling of the small-scale mixing data to full scale.

Testing with different HPI configurations should continue to be pursued. The CREARE tests[6] show that by keeping injection velocity high (through a smaller diameter HPI pipe), considerable turbulent mixing can be created.

More extensive testing in different HPI pipe sizes and angles (including laterally inclined and multiple injections to promote swirling flow patterns) should be investigated.

4.3.4 Development of Analytical Models

Since the assessment and conclusions for PTS will depend heavily on the results of computer codes, continued development of analytical models is recommended. Specific areas for attention include:

- improved condensation modeling for liquid level interfaces (pressurizer, stratified hot leg) during pressurization
- breakdown and re-establishment of natural circulation (hot leg, vent flow) under low-velocity, two-phase conditions
- improvement and verification of multidimensional models for analysis of mixing in cold leg and downcomer
- improvement in modeling the thermal imbalance in transient situations where loop temperatures are unsymmetrical, and modeling of loop flow mixing inside the lower plenum, core, and upper plenum of the vessel.

5.0 MATERIALS PROPERTIES OF IRRADIATED VESSELS

Pressure vessel steels exposed to neutron irradiation experience a degradation in fracture resistance. Ferritic steels have an intrinsically poor fracture resistance at low temperatures. The loss of ductility with decreasing temperature occurs as the nil-ductility transition temperature is approached. Below the transition temperature materials fail by unstable, brittle fracture, whereas above that temperature materials fail by stable, ductile fracture. Neutron irradiation causes the nil-ductility transition reference temperature (RT_{NDT}) to shift to higher temperatures. The shift can be large enough to endanger the integrity of the pressure vessel if the irradiation-shifted nil-ductility temperature is elevated above the service temperature of the vessel wall. Of particular concern is the fracture resistance of irradiation-sensitive welds.

Two factors aggravate the fracture resistance of irradiated vessel welds subjected to a PTS event. In some cases, aggravation occurs when the irradiation history of the reactor has resulted in significant elevation of the nil-ductility temperature. In other cases, aggravation occurs when PTS lowers the wall temperature, which thus lowers the fracture resistance of the vessel welds. Accurately predicting the fracture of a vessel weld requires estimating the vessel neutron exposure histories, welding procedures, and the irradiation sensitivities of welds as a function of chemistry. Furthermore, the radial dependence of neutron spectrum and flux in the wall must be evaluated to quantitatively determine the increasing fracture toughness through the wall.

This chapter describes the effects that irradiation and material characteristics have on the degraded fracture resistance of pressure vessel steels. Methods used by licensees and owners groups to predict fracture resistance, and the uncertainties inherent in these methods, are evaluated. Lastly, the state of knowledge is evaluated to indicate what information may become available in the future which would aid in evaluating the integrity of irradiated pressure vessels during a PTS event.

5.1 NEUTRON DOSIMETRY

Atomic displacements caused by neutron irradiation are the principal cause of degraded fracture toughness of nuclear pressure vessel steels. The degradation is directly related to the number of high-energy neutrons that penetrate the steel. Traditionally, the number of neutrons having an energy greater than 1 MeV has been used to characterize the irradiation exposure. Predicting the material properties of plant-specific reactor vessels requires an accurate knowledge of neutron exposures of metallurgical test specimens and an accurate knowledge of the neutron exposure of plant-specific pressure vessel components.

Methods used to irradiate and test metallurgical specimens and to estimate neutron exposure of vessel components result in uncertainties that affect the predicted reliability of vessels during a PTS event. Accurately defining the neutron irradiation environment requires knowledge of the neutron spectra, flux, and fluence, as well as the irradiation temperature. Irradiation of surveillance specimens provides the most reliable data base for predicting the irradiation properties of vessel components. Such data have the most credibility, because they most accurately represent the neutron environment inside a vessel wall. The plant-specific neutron spectra and fluxes are similar for surveillance irradiations and inner-wall vessel irradiations.

Methods used for vessel dosimetry are dependent on dosimetry analyses of surveillance capsules and on calculated neutron fluxes. Discrete Ordinate Transport (DOT) codes are used by the licensees and owners groups to map out the spatial dependence of neutron flux. The calculated fluxes are then compared with measured fluxes using flux monitors inserted in surveillance capsules. The DOT codes are considered to be accurate, but if wrong input values are assumed, the predicted fluxes can be inaccurate. When predicted fluxes are compared with measured fluxes, the values can agree to within 10% to 15%.(14) The uncertainty in peak fluence values provided by the licensees and owners groups is reasonable; the values for Combustion Engineering were within 30%, the values for Westinghouse were within 20%, and the values for Babcock & Wilcox were approximately 15%. The discrepancies in peak fluence values represent uncertainty in the predicted peak fluence ($E > 1$ MeV) at the inner surface of the steel vessel.

Additional uncertainty can exist in the predicted vessel properties because irradiation tests and vessel walls have different neutron spectra and fluxes. These differences are minimized when the properties of surveillance specimen are correlated to vessel properties. The correlation is possible because the neutron spectrum and flux of the surveillance location are similar to those found inside the vessel wall. When projecting properties through the thickness of the vessel wall, the spectrum and flux are degraded. The spectrum is shifted toward a lower average energy with many neutrons below 1 MeV contributing to irradiation damage.

To account for these lower energy neutrons, it has been recommended that displacements per atom (DPA) be used as a measure of irradiation exposure. The damage based on DPA is greater through the wall than would be predicted based on the $E > 1$ MeV assumption. Differences between the two exposure criteria as a function of distance through a vessel wall are given in Table 5.1.(15)

As radial distance increases, damage rates decrease. The lower damage rates may provide a greater opportunity for self annealing during irradiation. Hence, damage accumulates more slowly per DPA for positions deep in a vessel wall. This suggests a lesser damage in deep regions than would be expected if rate effects on damage efficiency were neglected when predicting radiation damage through a vessel wall. The effect of the damage rate efficiency can be estimated by comparing damage rates with thermal annealing rates. Combinations

TABLE 5.1. Change in DPA Damage as a Function of Position [Fluence (E > 1.0 MeV) is Constant]

Position	DPA from Neutrons (E < 1 MeV) / DPA from Neutrons (E > 1 MeV)
Surveillance	1.29
Inside surface of pressure vessel	1.48
1/4 thickness	1.73
1/2 thickness	2.18
3/4 thickness	2.71
Outside surface of PV pressure vessel	2.88

of irradiation temperature and irradiation flux that should produce an equivalent irradiation effect on embrittlement are plotted in Figure 5.1. The assumed dependence of flux on position is typical of a Westinghouse two-loop plant. The temperature/flux dependencies are shown for two annealing activation energies: 1.3 eV for vacancy diffusion, and 3.0 eV for iron self diffusion. Figure 5.1 indicates that damage produced to a given fluence at surveillance fluxes at 575°F equivalent to damage produced at 1/2 thickness fluxes at 525°F for a 1.3 eV annealing process. Because a difference of 50°F in irradiation temperature is known to affect the shift in RT_{NDT}, the flux differences shown in Figure 5.1 should affect the predicted shift in RT_{NDT} for a given fluence. Reasons for the existence or nonexistence of flux effects are not well understood.

Measurements of damage that are based on fluence greater than 1 MeV rather than on DPA can result in an unrealistic evaluation of fracture toughness through the vessel wall. The assumption of constant damage efficiency per DPA through the thickness of the wall is conservative. The low fluxes deep in the wall should result in less embrittlement damage per DPA than for the inner surface of the wall. Therefore, the damage deep in the wall is expected to be low because the fluence is low and because the damage efficiency per unit of fluence is also low. The dependence of damage efficiency on flux has not been quantitatively assessed.

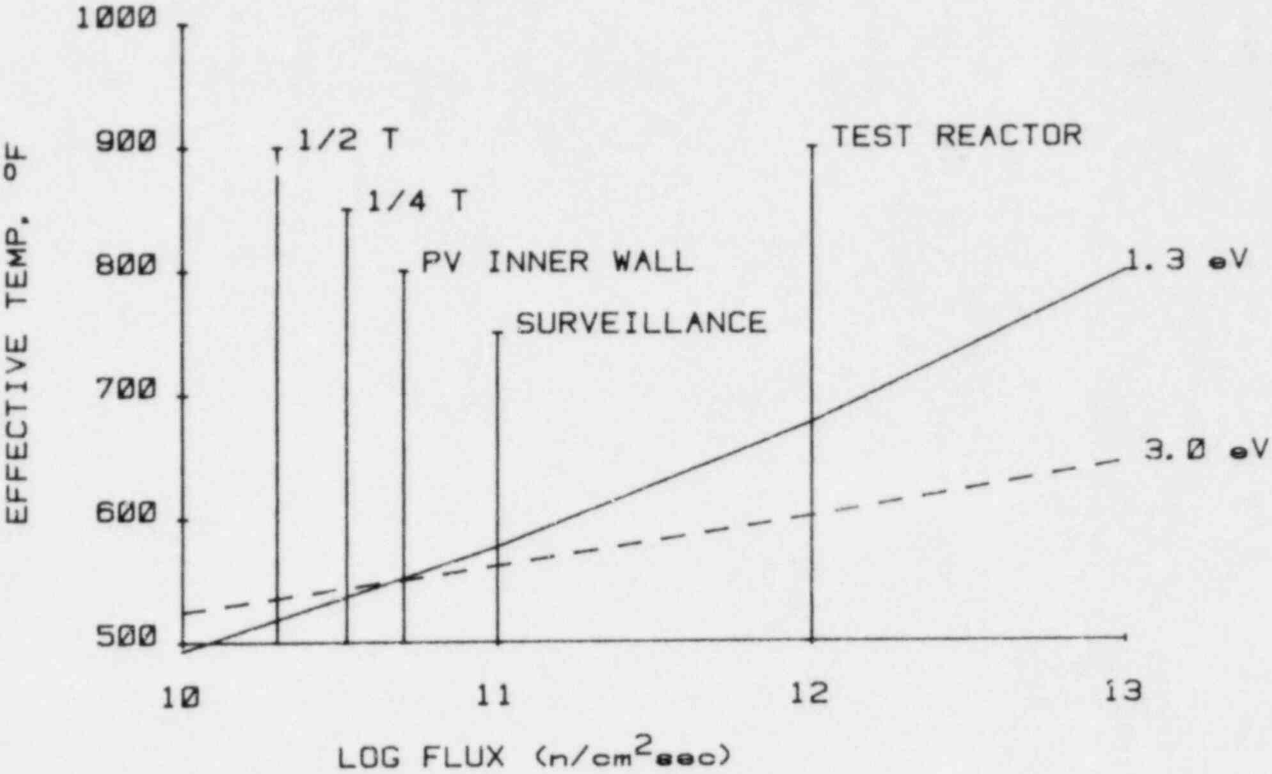

FIGURE 5.1. Effective Temperature to Produce Constant Damage for Variable Flux for Two Annealing Processes

With current knowledge, the inner wall fluence can be predicted with confidence. The uncertainty in predicted fluence has been reduced from over 300% in the early 1960s to the current uncertainties of 15% to 30% (Figure 5.2).(14) Uncertainty is shown as the effective uncertainty in the predicted ΔRT_{NDT} for a weld having the characteristics of the Fort Calhoun longitudinal weld. As illustrated in Figure 5.2, dramatic benefits have resulted from recent advances in the accuracy of dosimetry. However, future improvements in dosimetry accuracy can have only a minor impact on the PTS evaluation of the crack initiation resistance of near-surface flaws. Estimates of damage through the thickness of the vessel wall are expected to improve in the future as a result of the NRC heavy section steel technology (HSST) dosimetry program at the Hanford Engineering Development Laboratory (HEDL). These future assessments will not affect estimates of crack initiation during PTS, but they could affect the prediction of crack arrest during PTS and the prediction of crack propagation during subsequent PTS events.

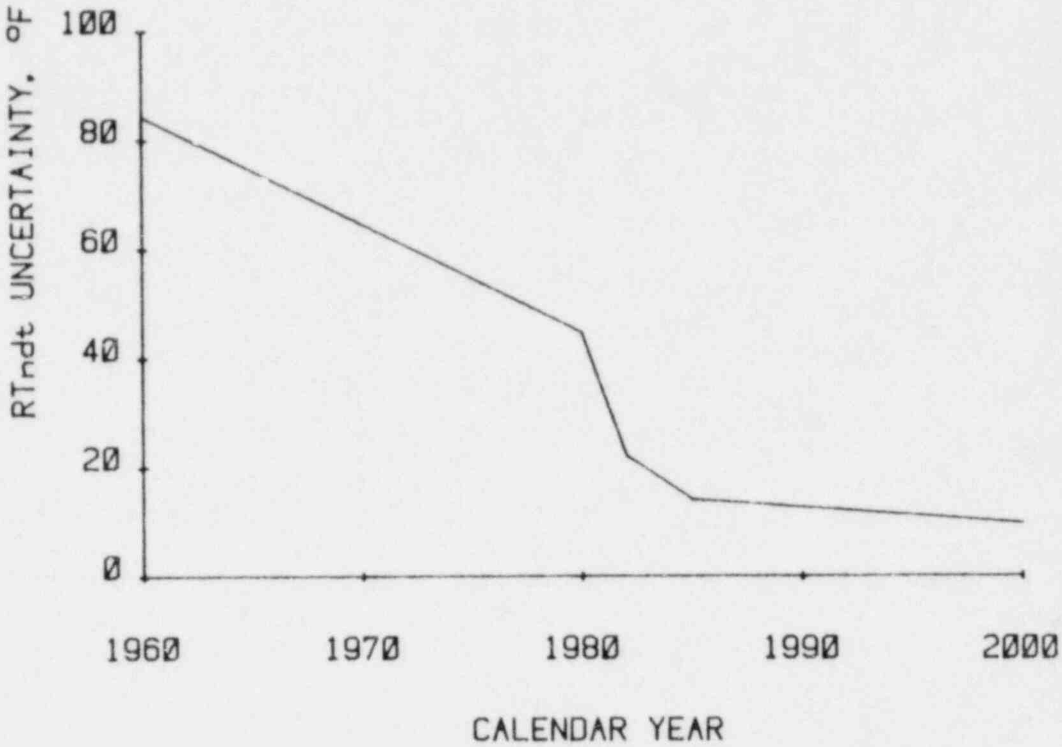

FIGURE 5.2. RT_{NDT} Uncertainty Due to Fluence Uncertainties as a Function of Calendar Year

5.2 INITIAL PROPERTIES

The irradiation-induced shift in the nil-ductility temperature (NDT) is not dependent on the initial RT_{NDT} of the material. This means that generic predictions, such as those described in Regulatory Guide 1.99, Rev. 1 and the HEDL curves, can be used to predict irradiation shifts of specific steels having different initial RT_{NDT} values.

During fabrication of the vessels considered in this PTS evaluation, the initial weld toughness was not a critical issue. Hence, the initial RT_{NDT}s of the welds were not completely characterized other than to assure that the RT_{NDT} was at least 60°F below the lowest service temperature. The use of these original estimates can result in conservatively high estimates of the initial RT_{NDT}.

The initial RT_{NDT} of welds in pressure vessels is a critical issue in this PTS evaluation because: 1) the final adjusted RT_{NDT} is directly proportional to the estimated initial RT_{NDT}, and 2) the weld properties are often impossible to document directly. When plant-specific, archival weld specimens are not available from which to fabricate metallurgical test specimens, it is necessary to test specimens from weld metal similar to the original plant vessel welds.

Combustion Engineering has developed a data base to statistically demonstrate the initial RT_{NDT} that is expected for submerged arc weldments using Linde 0091, 1092, and 124 flux. Testing of 82 weldments resulted in an average RT_{NDT} of -56°F with a standard deviation of 17°F. A mean plus two sigma estimate for the initial RT_{NDT} is -22°F for these welds. As noted in Chapter 8.0, the test data for these welds are not normally distributed. A more rigorous statistical analysis suggests that -10°F represents the confidence for the two sigma criterion.

The initial RT_{NDT} of the weld can be sensitive to the weld flux used.(16) Specifically, Linde 80 flux produces an initial RT_{NDT} approximately 40°F higher than other fluxes. Therefore, the initial RT_{NDT} of Linde 80 welds is expected to be about 40°F higher than that indicated in the Combustion Engineering generic study.

Estimates of the initial RT_{NDT} for each of the critical plant welds that were evaluated are shown in Table 5.2. These estimates were obtained from the NRC evaluation of the licensees responses. The weld descriptions and predicted RT_{NDT} are also shown in Table 5.2. The range of estimates in the initial RT_{NDT} can be large. The choice of value depends on the level of conservatism that is considered necessary for sound engineering judgment. Mean values cannot be considered realistic because there is significant variation in the measurement of the nil-ductility temperature.

Fracture-resistance test results are intrinsically random because crack propagation is sensitive to local inhomogeneities. Crack initiation and growth paths are not random, but rather follow specific paths dictated by local material characteristics (i.e., inclusions, grain boundaries, lattice defects, etc.). Therefore, mean values of initial RT_{NDT} and shifted RT_{NDT} are not reliable indicators of fracture resistance, and two sigma factors should be included to be confident that the assumed fracture resistance is in fact realistic for any crack in the vessel steel. The procedure of assuming a mean plus two sigma is a best estimate that a worst-crack/metallurgical property combination will not initiate. The expected confidence for the mean plus two sigma value may not exist if the data are not normally distributed. Confidence levels, as affected by distribution shape, are discussed in Chapter 8.0.

There is a definite need to know more accurately the initial RT_{NDT} of the welds in each particular plant. The confidence that can be placed in these estimates depends not only on metallurgical tests but also on the accurate documentation of welding technique, weld wire used, and weld flux used. The credibility of estimates can only be enhanced by performing more tests on archival material, by discovering previously unreported test results on weld specimens from each particular plant, or by evaluating properties of welds considered typical of the plant-specific weld. It is possible that in the future additional information could become available that could justify changing the current estimates. Expected changes will not exceed 10° to 20°F unless the conservative (mean plus two sigma) criterion is modified.

TABLE 5.2. Summary of Weld Properties and RT_{NDT} Predictions

Plant	Weld Location	RT^0_{NDT}, °F	Fluence, n/cm^2	Date	Cu, %	Ni, %	$RT_{NDT}^{(a)}$ Mean + 2σ
Turkey Pt. 4	Circum.	+20	1.10×10^{19}	9/30/81	0.30	0.57	265
Fort Calhoun	Long 2-410	-20	6.48×10^{18}	12/31/81	0.35	0.99	268
San Onofre 1	Long 7-860A	-20	2.75×10^{19}	10/31/81	0.35	0.20	278
Calvert Cliffs 1	Long 2-203	-20	7.05×10^{18}	12/31/81	0.30	0.99	267
Maine Yankee	Long 2-203	-20	4.73×10^{18}	12/31/81	0.36	0.99	251
Robinson 2	Long 2-273	-20	1.30×10^{19}	9/30/81?	0.34	0.20	218
	Circ 11-273	-20	1.24×10^{19}	(assumed)	0.34	0.50	253
Oconee 1	Long SA-1430	+20	2.27×10^{18}	10/01/81	0.31	0.55	183

(a) $RT_{NDT}\ (\text{HEDL}) = RT^0_{NDT} + (38 + 470{*}Cu + 350{*}Cu{*}Ni){*}\left(\frac{F}{1 \times 10^{19}}\right)^{0.27}$.

5.3 IRRADIATION PROPERTIES

The shift in the nil-ductility temperature due to neutron irradiation of pressure vessel steels is well known. The issue for the PTS evaluation is to quantify the irradiation shift as accurately as possible for specific vessel welds. Because specimens cannot be extracted from the irradiated vessels, it is necessary to project irradiation properties from irradiations of metallurgical test specimens. The irradiation environment and materials used for these metallurgical specimen irradiations must approximate, as much as possible, the materials and environment of the pressure vessel. Furthermore, irradiation tests must project the properties at some future date--in particular, to end of life or 32 EFPY.

The irradiation tests that were used to establish Regulatory Guide 1.99, Rev. 1 were performed primarily in test reactors at enhanced fluxes and in neutron spectra having average energies larger than those typical for pressure vessels. The rapid fluxes meant that fluences in end-of-life reactor vessels could be achieved in a few years or less. Surveillance specimen irradiations are performed to provide information on spectral and flux effects. These irradiations are performed near the vessel wall in each licensed reactor at a position that allows the specimen neutron spectra and flux to closely approximate the irradiation environment of the vessel wall.

The principal difference between earlier test specimen irradiations and the more recent surveillance specimen irradiations is evident when Regulatory Guide 1.99. Rev. 1 is compared with the HEDL curves. Both the guide and HEDL curves predict shift as a function of fluence. Regulatory Guide 1.99, Rev. 1 predicts a fluence exponent of one half for low fluences, whereas the HEDL curves predict a fluence exponent of about one third or less. Surveillance specimen irradiations, which are more realistic than test specimen irradiations, indicate a larger shift at low fluences, but the rate of increase in the shift becomes slower as the fluence increases. The lesser fluence exponent is expected for lower damage-rate irradiations. The lower rate provides more opportunity for annealing of damage to occur during the period of the irradiation.

The low fluence exponent determined from the HEDL analyses gives the effect of damage saturation with increasing fluence. The apparent saturation is particularly evident when data from surveillance specimens are compared with predictions from Regulatory Guide 1.99, Rev. 1. Data from Point Beach surveillance tests have suggested a saturation at higher fluences for high copper and low nickel steel. The uncertainty in the test data is, however, great enough to make the suggested saturation ambiguous.

Evidence from irradiation and annealing experiments indicates that two irradiation mechanisms lead to the embrittlement of steel. The first mechanism develops rapidly and anneals out rapidly. The second mechanism requires long-term irradiation and is resistant to annealing. The first mechanism may saturate over a range of fluence, and it may be what is seen in the Point Beach surveillance results. The saturation is probably temporary, but it may exist over a wide enough fluence range to result in significantly less damage accumulation in some pressure vessel steels.

Hanford Engineering and Development Laboratory has examined the surveillance test data base to determine if the fluence exponent depends on nickel concentration.(17) The statistical fit of the data that assumed an exponent dependent on nickel did not reduce the residual error in the fit as much as a fit that assumed an exponent independent of nickel concentration.

Evidence suggests that some departure from the typical fluence dependence may occur for specific steels (e.g., those low in nickel and high in copper). However, at this time the data are limited and do not justify assuming anything different than the trends established in the HEDL analysis.

Both Combustion Engineering and Westinghouse provided predictive curves in their responses for welds having a low nickel content (i.e., nickel less than 0.3 wt%). The Westinghouse prediction from WCAP-10019 is compared with the HEDL curve in Figure 5.3. The prediction presented by Westinghouse has a lesser fluence dependence and hence predicts a lesser shift at high fluences.

The HEDL analysis of the irradiation shifted RT_{NDT} demonstrates a one standard deviation of 24°F. The deviation is realistic even if the specific variability from material to material is reduced. Identical materials are expected to demonstrate variability similar to that observed in the HEDL analysis. Analyses shown by Hawthorne(18) indicate an uncertainty of 15°C (27°F) when correlating ΔT in a fracture toughness test as compared to ΔT as determined from Charpy V-notch tests of irradiated welds. This uncertainy is expected in the measurement of complex fracture properties.

Some variability in ΔT_{NDT} can be attributed to chemistry variations. Variation in copper content and nickel content can be significant. Such variation is expected for copper-coated electrodes because copper was not added for its alloying benefit, but rather it was added as a coating to aid in the welding process. A variable copper content in high-copper weld results from a variable coating thickness of the weld electrodes. The Combustion Engineering welds considered in this review were fabricated using an additional pure nickel weld wire. This procedure produced welds of uncertain nickel content. The uncertainty in predicted RT_{NDT} is the sum of uncertainties from metallurgical tests, material chemistry, and fluence.

The predictions of Regulatory Guide 1.99, Rev. 1 differ from those of HEDL because each used a different fluence exponent and only the HEDL predictions considered nickel concentration. At current fluences, the 1.99, Rev. 1 estimates exceed the HEDL estimates for the Westinghouse plants by as much as 57°F for the low nickel, high-fluence H. B. Robinson 2 longitudinal weld. At end-of-life (EOL), the difference is 37°F. The high-nickel, low-fluence Combustion Engineering welds have a lower Regulatory Guide 1.99, Rev. 1-predicted RT_{NDT} when compared to the HEDL-predicted RT_{NDT}. The difference for Maine Yankee is 38°F and 89°F at current and EOL fluences, respectively. The Babcock & Wilcox Oconee plant has a lower (by 23°F) 1.99, Rev. 1-predicted RT_{NDT} compared to the HEDL-predicted RT_{NDT} at current fluences, but a higher (by 33°F) 1.99, Rev. 1-predicted RT_{NDT} compared to the HEDL-predicted RT_{NDT} at EOL fluences.

As additional surveillance specimen tests are performed and evaluated, a more confident prediction of pressure vessel embrittlement should emerge. The surveillance-specimen data base to date is substantial, and it is expected that the current HEDL evaluation is realistic. Some reduction in the uncertainty may result from the addition of surveillance data for specific welds (e.g., those with a high copper, low nickel content).

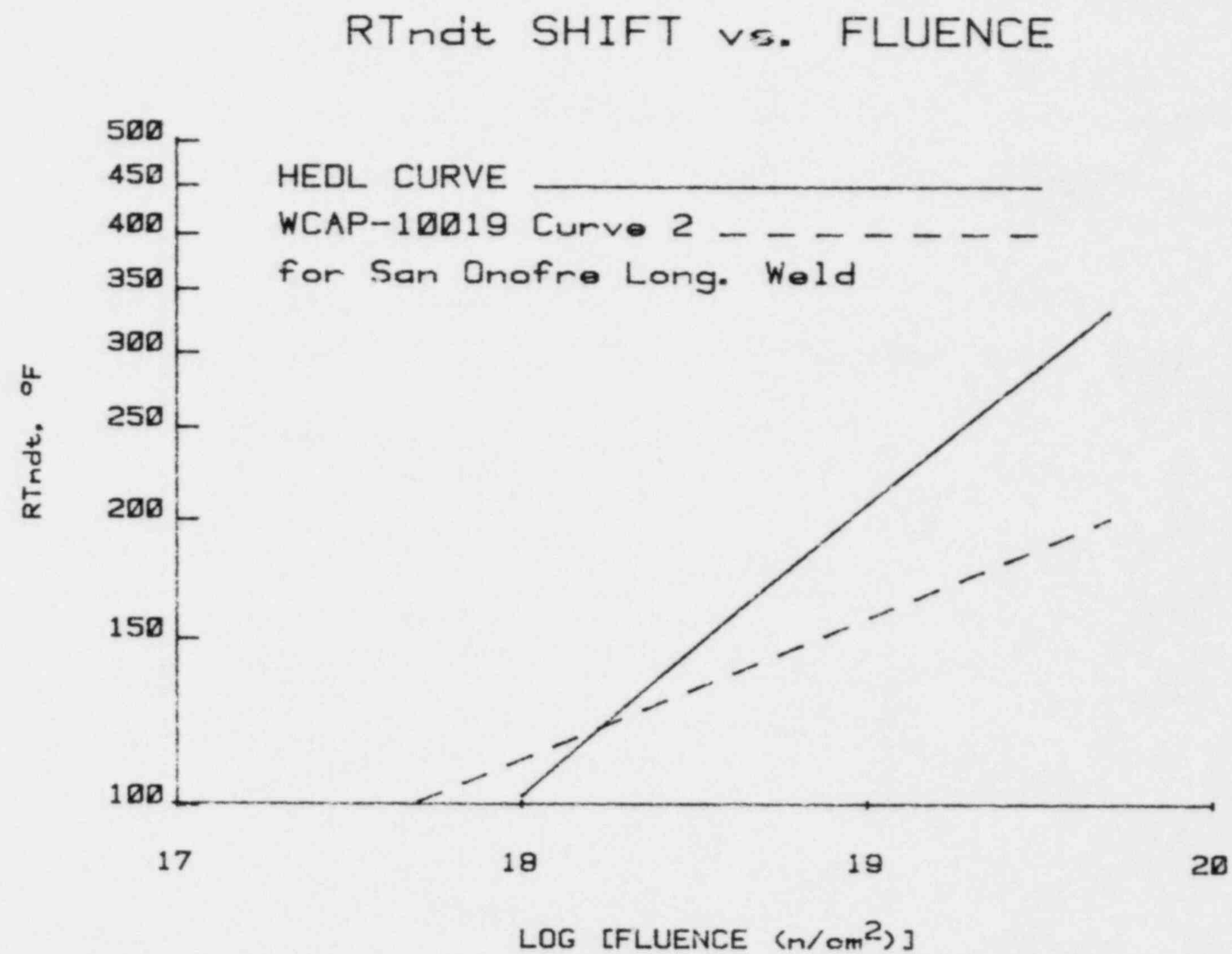

FIGURE 5.3. Comparison of HEDL and WCAP Predictions of RT_{NDT} for the Critical San Onofre Weld

5.4 SENSITIVITY ANALYSES

Uncertainty in the predicted shift in RT_{NDT} results from uncertainties in initial chemistry and initial RT_{NDT}. The dependence of the irradiation-shifted RT_{NDT} on copper content, nickel content, initial RT_{NDT}, and fluence is expressed in the HEDL equation shown in Table 5.2. The copper and nickel concentrations are expressed in weight %, and the fluence is expressed in units of n/cm^2 ($E > 1$ MeV).

The effect of the predicted shift on material parameters was analyzed using the method illustrated schematically in Figure 5.4. The adjusted RT_{NDT} was calculated for two conditions:

1. the conservative (mean plus two sigma) estimate based on compositions, initial RT_{NDT}, and fluences given in the 60- and 150-day responses

2. the revised estimate based on the same information as in the first condition except that one parameter in the equation was allowed to assume a revised value.

The first condition is represented by the solid line and the second condition is represented by the dashed line. The difference between the two predictions at the current conservative RT_{NDT} was calculated in terms of ΔEFPY as shown in Figure 5.4. Figures 5.5 through 5.7 indicate the plant-specific RT_{NDT} predictions as a function of fluence and represent the conservative estimates.

The dependence of the calculated EFPY uncertainty on copper and nickel concentrations is shown in Figures 5.8 and 5.9 for each of the plants. Sensitivities are shown for both the longitudinal and circumferential H. B. Robinson 2 welds. The detrimental effect of high copper content is obvious; a variation of only a few hundreths of a percent of copper results in an uncertainty of a few EFPY. A two-hundredths of a percent change in copper results in a 10° to 15°F change in the predicted RT_{NDT}. Results of nickel sensitivity analyses are shown in Figure 5.6. The analyses indicate that a variation of a few tenths of a percent of nickel results in an uncertainty of a few EFPY. A two-tenths of a percent change in nickel results in a 20° to 30°F change in the predicted RT_{NDT}.

The effect of assuming a lesser or greater initial RT_{NDT} on the calculated uncertainty in terms of EFPY is shown in Figure 5.10. A 20°F variation in initial RT_{NDT} results in a change of 2 to 3 EFPY. The sensitivities for the Combustion Engineering plants are less than those for the Westinghouse, Babcock & Wilcox Oconee plants.

SENSITIVITY ANALYSIS METHOD

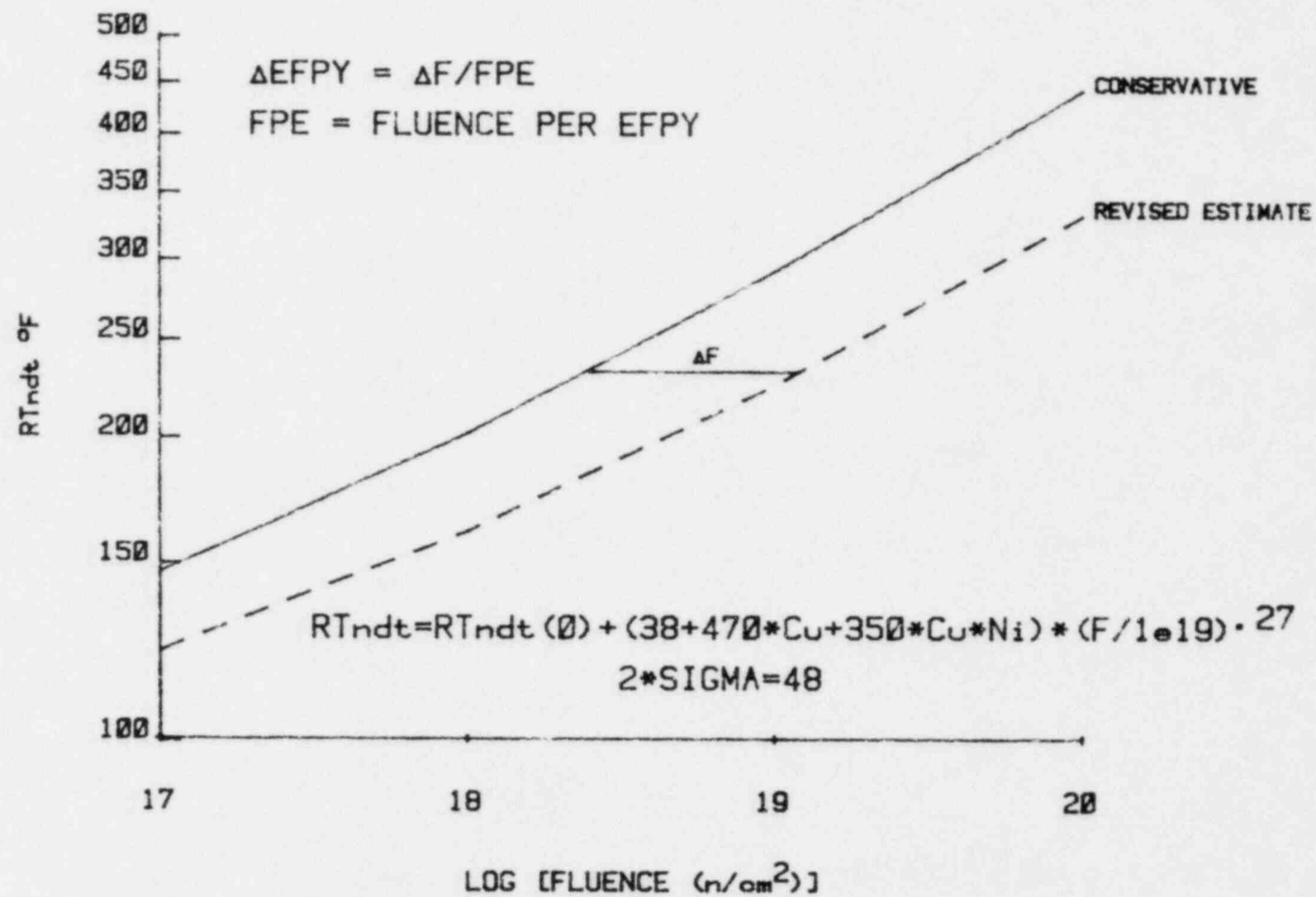

FIGURE 5.4. Schematic of Method for Calculating ΔEFPY

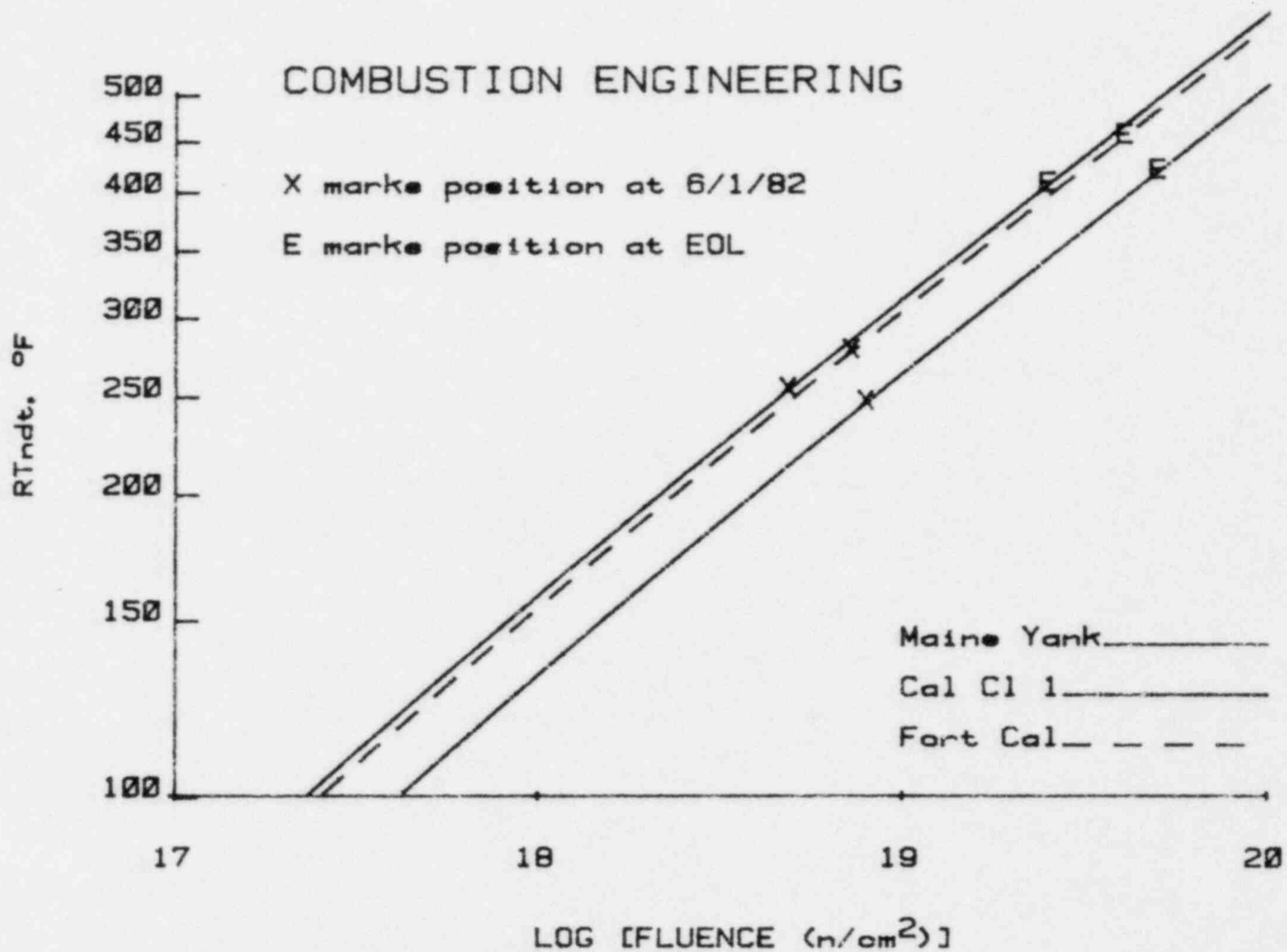

FIGURE 5.5. The Predicted RT_{NDT} of Combustion Engineering Plant Critical Welds

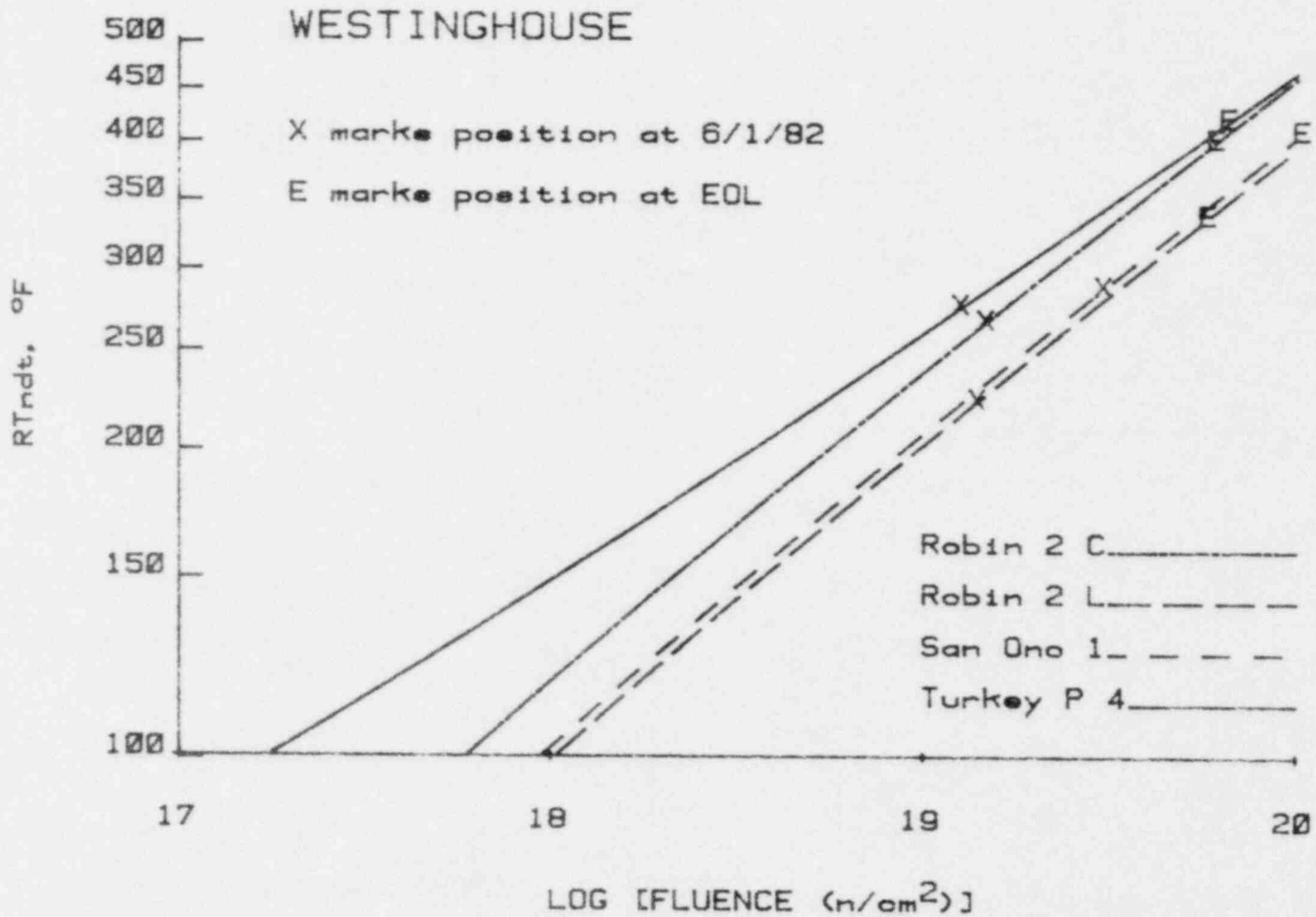

FIGURE 5.6. The Predicted RT_{NDT} of Westinghouse Plant Critical Welds

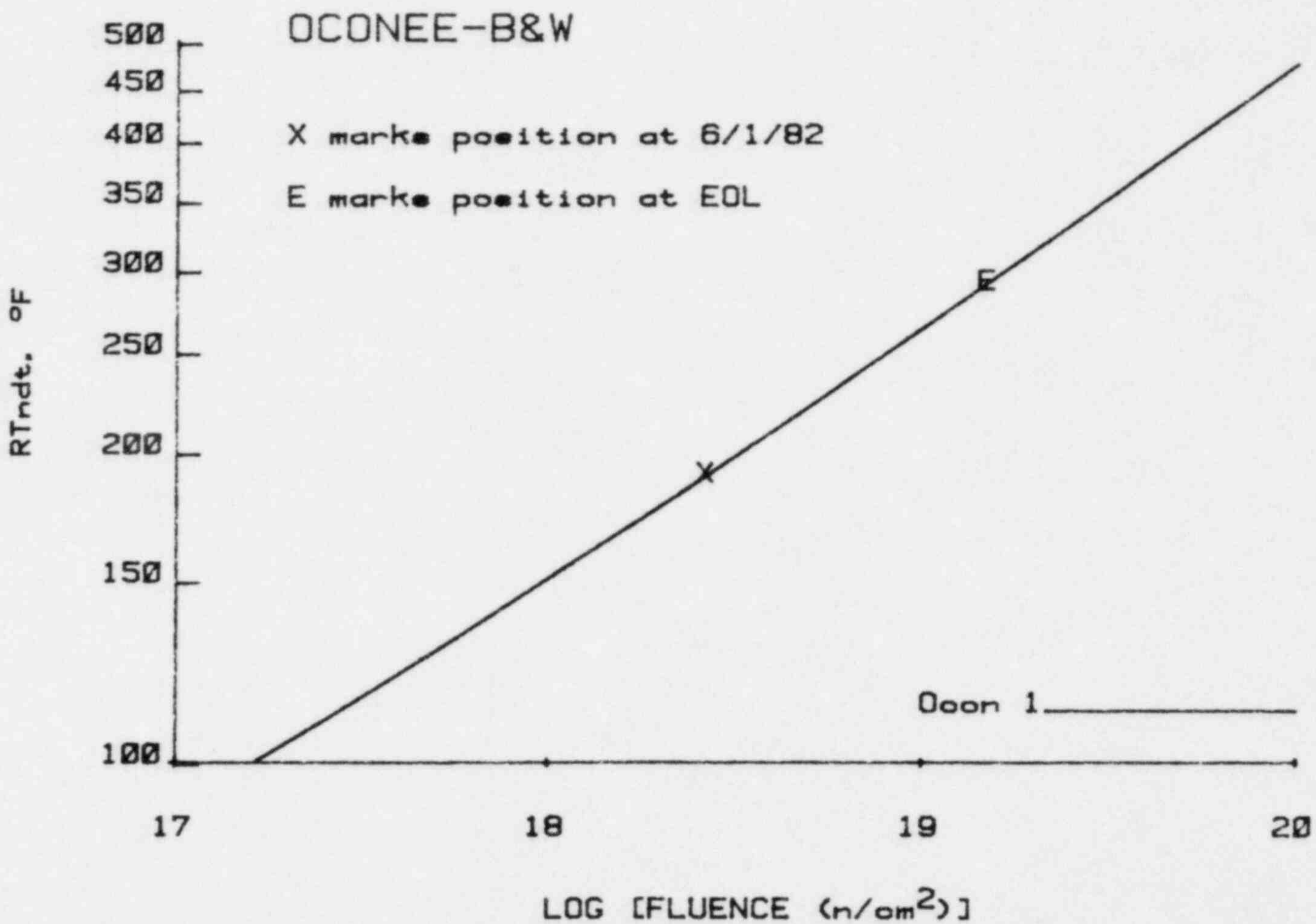

FIGURE 5.7. The Predicted RT_{NDT} of the Oconee Critical Weld

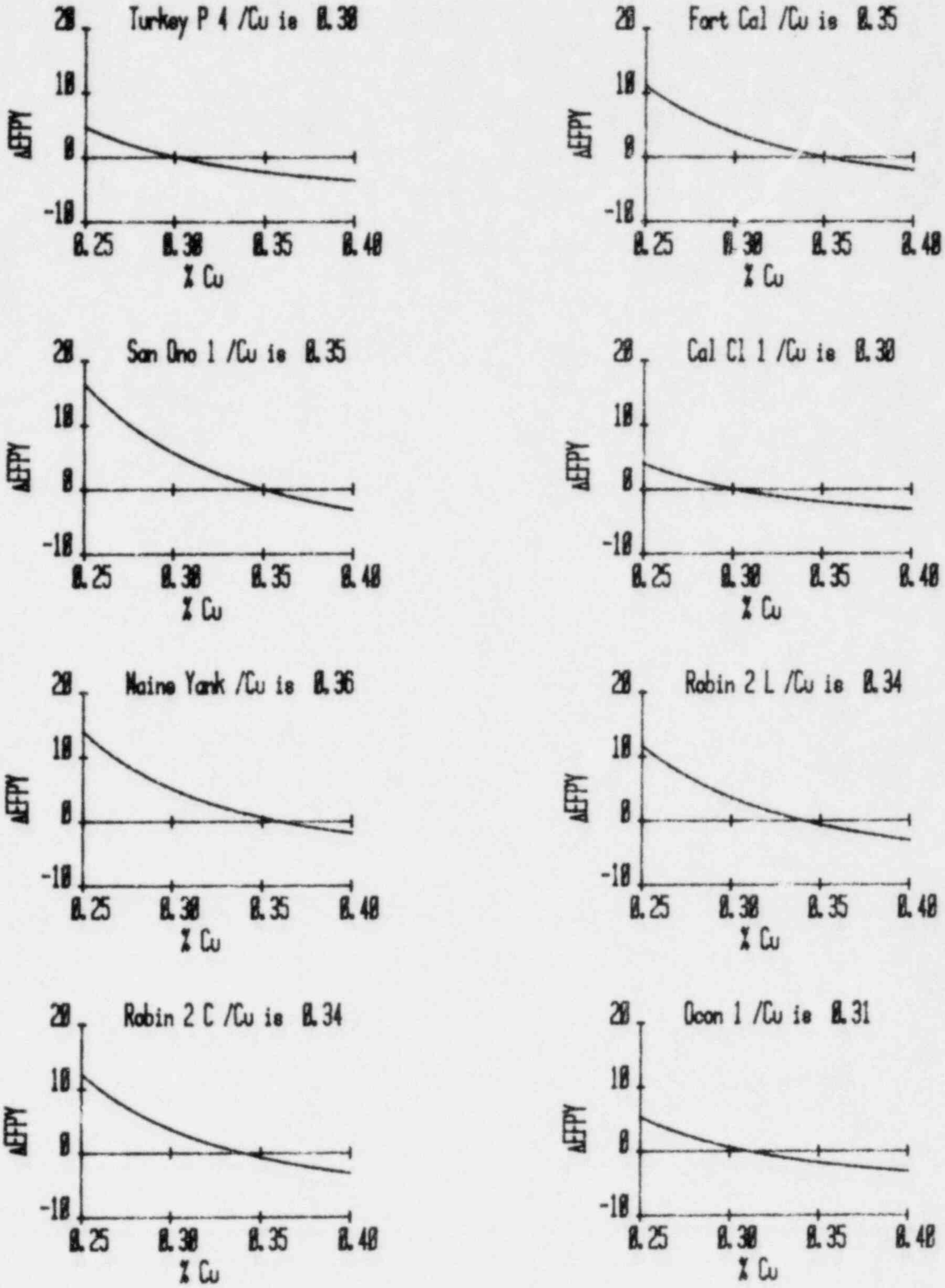

FIGURE 5.8. The Sensitivity of EFPY on Copper Content

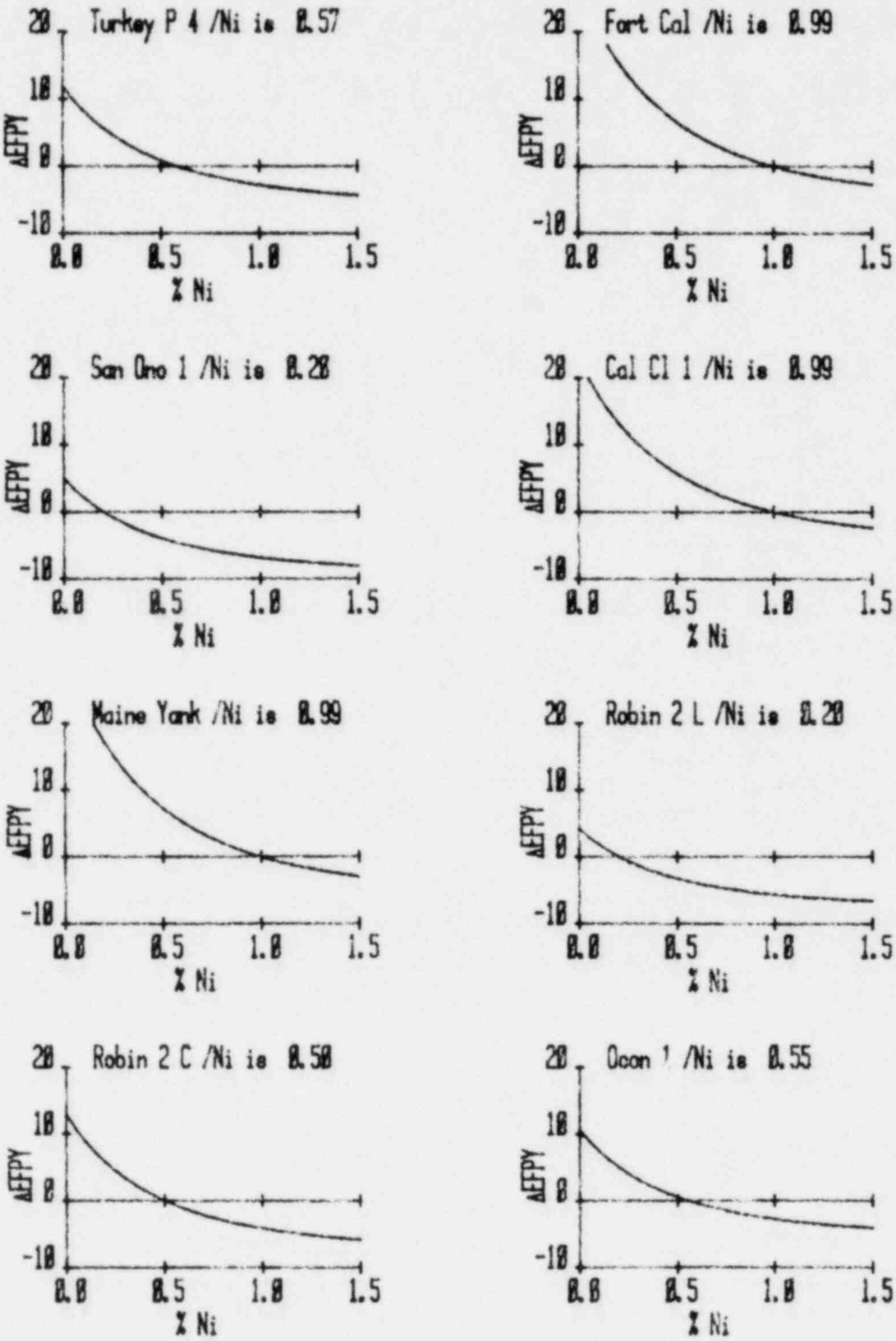

FIGURE 5.9. The Sensitivity of EFPY on Nickel Content

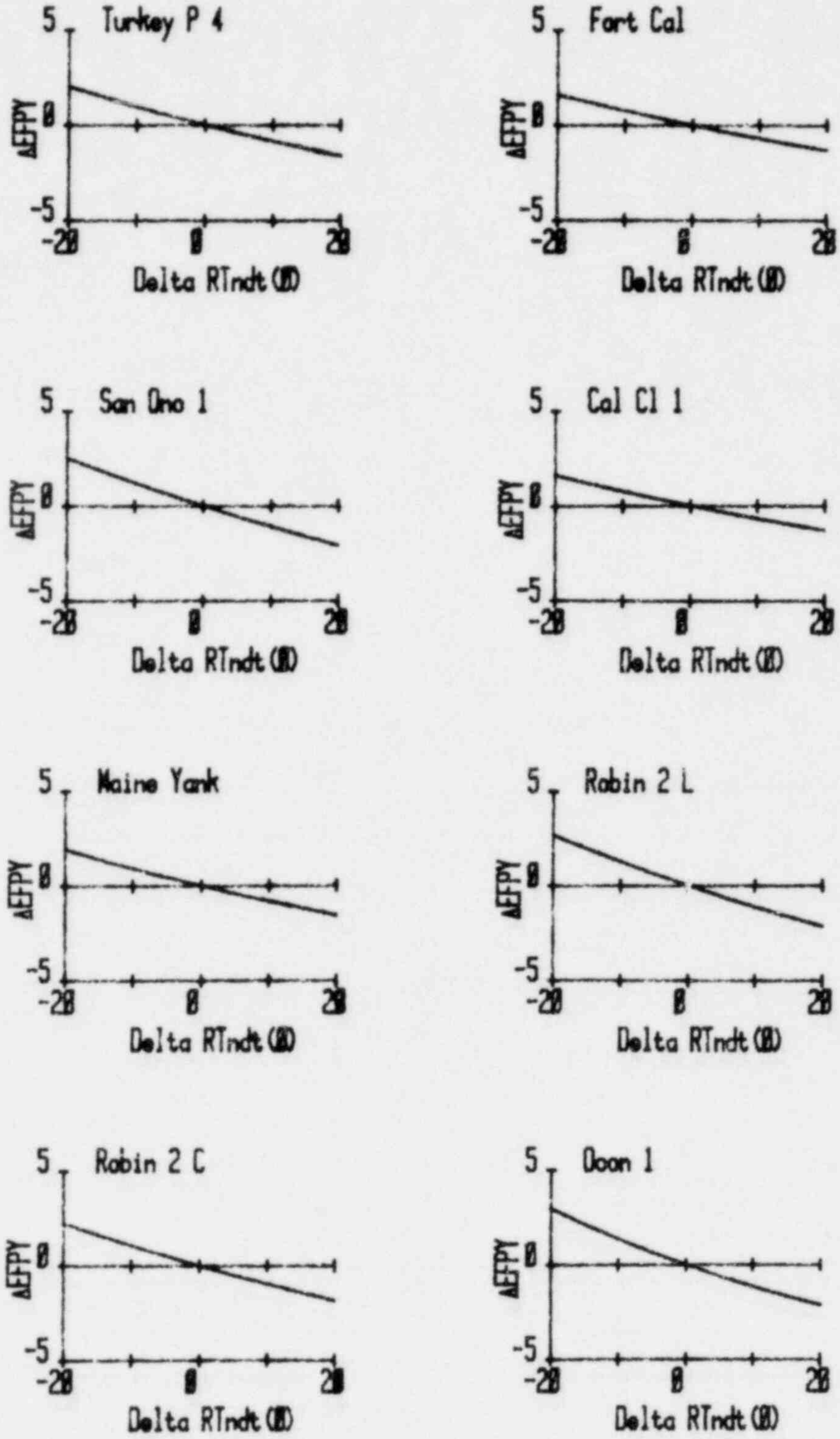

FIGURE 5.10. The Sensitivity of EFPY on Initial RT_{NDT}

A mean plus two sigma conservatism is assumed in the equation in Figure 5.4. If the entire conservatism is removed from the equation by assuming a mean value, then the predicted condition of the vessel integrity shifts by about 5 years. Therefore, the vessel integrity remains in question even if there is no conservatism in the RT_{NDT} prediction. The time to achieve the current RT_{NDT} is, however, delayed about 5 years.

The uncertainty in EFPY is inversely proportional to the uncertainty in fluence. Because the fluence exponent expressed in the equation in Figure 5.4 is low, the predicted RT_{NDT} is insensitive to fluence at high fluences. A 40% uncertainty in fluence implies only a 25°F uncertainty in the predicted RT_{NDT} for the plants evaluated in the 150-day responses.

5.5 FRACTURE TOUGHNESS

Both the nil-ductility transition temperature and the steel fracture toughness reference curves are used to evaluate the integrity of pressure vessels. Using the NDT concept to predict brittle fracture has the advantage of simplicity and historical credibility. The fracture toughness concept has the advantage of relating crack geometry, stress, and a material property to fracture. If a fracture toughness property is known, then calculations of fracture mechanics can be made to estimate vessel integrity for plant-specific accident scenarios.

Fracture toughness parameters indicate resistance to either crack initiation (K_{Ic}), crack arrest (K_{Ia}), or dynamic crack initiation (K_{Id}). The ASME code (Section III) recommends the use of crack resistance (K_{IR}), which is the lower bound curve drawn below K_{Ic}, K_{Ia}, and K_{Id}. A detailed discussion of these parameters is provided in Chapter 6.0. Measurement of these parameters requires the use of large specimens, which is impractical for irradiation testing. Therefore, small, Charpy V-notch specimens are used in surveillance capsules for testing the fracture resistance of steels in irradiated pressure vessel.

The predicted vessel-fracture resistance is dependent on a correlation between the more common Charpy V-notch irradiation tests and the more useful fracture toughness irradiation tests. Hawthorne has compared temperature shifts for irradiated welds from both types of tests and found exceptionally good agreement with an uncertainty of ±15°C (±27°F).[18] The uncertainty is similar in magnitude to uncertainties that can be expected for fracture testing of irradiated metals. An EPRI program has evaluated Charpy V-notch and fracture toughness data from identical materials having identical irradiation histories. The results of the EPRI program strengthen the confidence in the Charpy V-notch/fracture toughness correlation.

The lower-bound K_{IR} curve currently used could be replaced with a conservative, statistically based K_{IR} curve. The lower-bound method does not allow for an evaluation of conservatism in terms of probability. A mean K_{IR} (statisticallly based) is compared with the lower-bound K_{IR} (ASME code) in Figure 5.11. The conservatism in terms of temperature difference is about 50°F for temperatures near or above the nil-ductility temperature. For a given fracture toughness, the temperature that is predicted from mean value assumptions can be almost 100°F lower than the temperature predicted from conservative value assumptions. This difference can be explained by two temperature uncertainties. The first uncertainty is evident in Figure 5.11, which demonstrates a 50°F difference between the mean K_{IR} and the ASME lower-bound K_{IR}. The second uncertainty is the two sigma factor of 48°F that is included in the HEDL prediction of RT_{NDT}. A 50°F uncertainty in RT_{NDT} is typically equivalent to a 5 to 10 EFPY uncertainty. Therefore, a more rigorous statistical treatment of K_{IR} could result in extending the time to achieve a given critical RT_{NDT} by only a few years for the plants in this PTS evaluation. The validity of various K_{IR} values is assessed in Chapter 6.0.

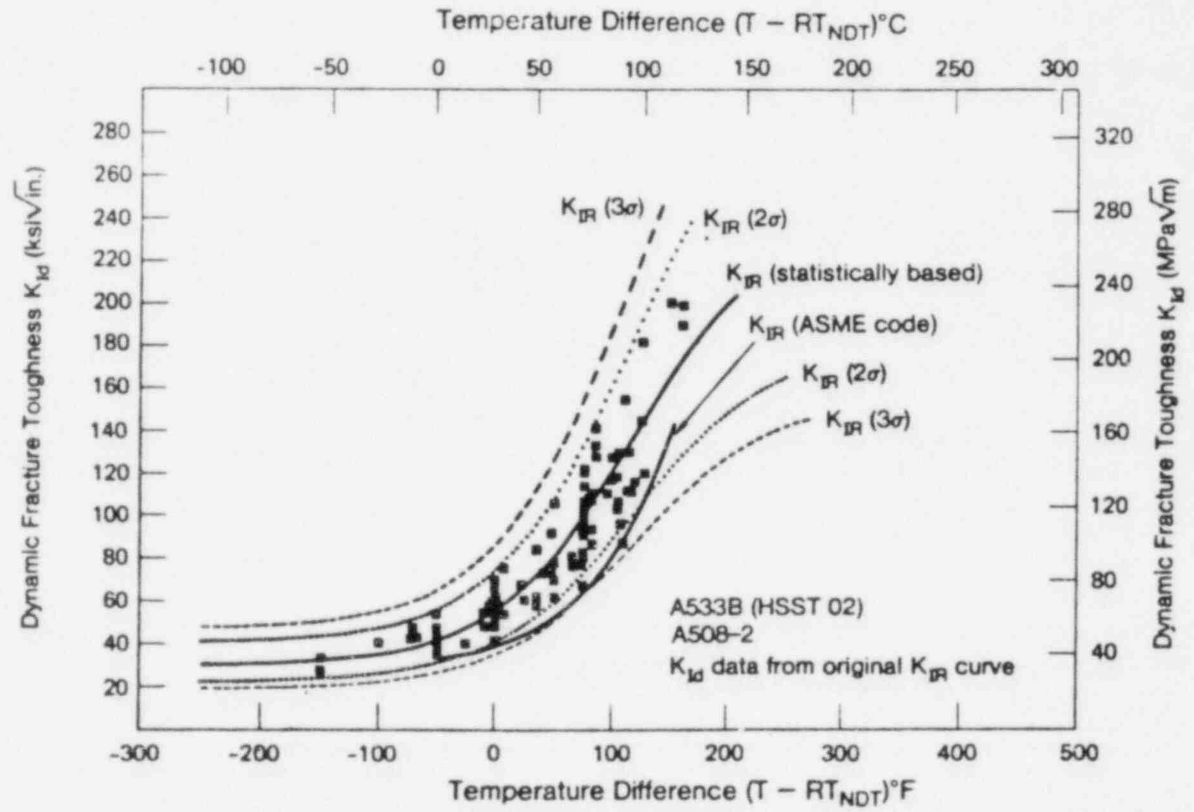

FIGURE 5.11. Comparison of Fracture Roughness for Statistically Based K_{IR} and the ASME Code Lower-Bound K_{IR}

5.6 CONTROL AND REDUCTION OF EMBRITTLEMENT

The uncertainty in vessel integrity can be reduced by reversing or controlling the irradiation shift in RT_{NDT}, or by more fully characterizing the vessel materials. The irradiation shift in RT_{NDT} can be controlled by annealing the vessel or by reducing the irradiation flux to the vessel. Furthermore, the predicted shift for a given fluence is dependent on the assumed material composition and on the assumed initial RT_{NDT}. Therefore, the predicted RT_{NDT} can be realistically reduced if a reduction in the assumed copper content, nickel content, or initial RT_{NDT} can be justified.

Annealing of the reactor vessel is the only method currently used that can restore some of the original, unirradiated vessel toughness. Fuel management schemes can reduce the rate of future embrittlement but cannot reduce embrittlement caused by previous irradiation. Studies of pressure vessel annealing indicate that irradiation damage can be erased and that the fracture resistance can be restored.

In particular, Hawthorne[18] has annealed irradiated vessel welds and then reirradiated them (Figure 5.12). The damage that accumulated rapidly at low fluences was easily annealed, whereas the damage that accumulated slowly at higher fluences was resistant to annealing. For multiple annealing cycles, the percentage of recovery observed for each anneal decreased for each additional anneal. From these data, the minimum RT_{NDT} after each anneal was observed to follow a fluence dependence that paralleled the fluence dependence expected for continuous (without anneals) irradiation at high fluences. Upon reirradiation, the rate of embrittlement appeared to follow the embrittlement rate of the post-anneal RT_{NDT} (i.e., it follows the low fluence rates, not the high fluence rates). These effects need to be evaluated further.

The response expected during irradiation, anneal, and reirradiation cycles can be calculated from the equation in Figure 5.4. The dependence of EOL RT_{NDT} on annealing and reirradiation parameters is shown in Figures 5.13 and 5.14. The independent variable is the percentage of recovery that is obtained from the anneal. A family of curves is shown for each plant to illustrate the benefit of fuel management schemes that reduce the irradiation flux to the vessel. The figures show reductions in flux by 0%, 40%, and 80%. The point at which there is zero recovery and zero flux reduction corresponds to the expected EOL RT_{NDT} if no corrective actions are taken to reduce embrittlement. Figure 5.13 indicates the benefit from one anneal performed on June 1, 1982. Figure 5.14 indicates the benefit from two anneal cycles, the first on June 1, 1982, and the second midway between June 1, 1982, and the plant end-of-life EFPY.

A single anneal (Figure 5.13) is less beneficial than a double anneal (Figure 5.14) because the plants are currently at a relatively low fluence. The reirradiation that would occur after June 1, 1982, is about three-fourths of the plant lifetime fluence. As seen in Figures 5.5 through 5.7, the RT_{NDT} at three-fourths of the EOL fluence is only slightly less than the RT_{NDT} EOL fluence. To demonstrate the maximum benefit on EOL RT_{NDT}, the anneal should be delayed to a time of about one-half the EOL fluence instead of the one-fourth EOL fluence that exists on June 1, 1982.

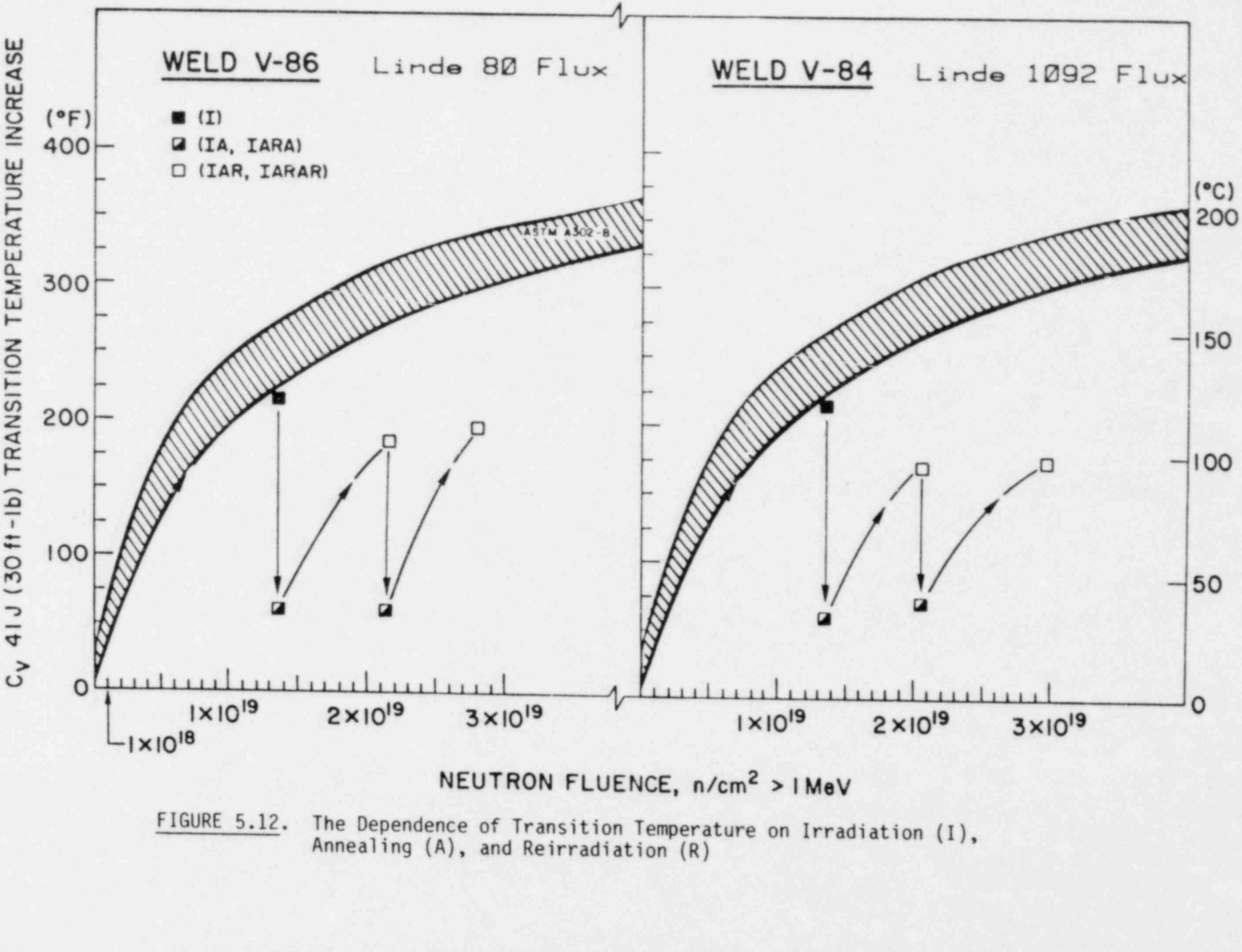

FIGURE 5.12. The Dependence of Transition Temperature on Irradiation (I), Annealing (A), and Reirradiation (R)

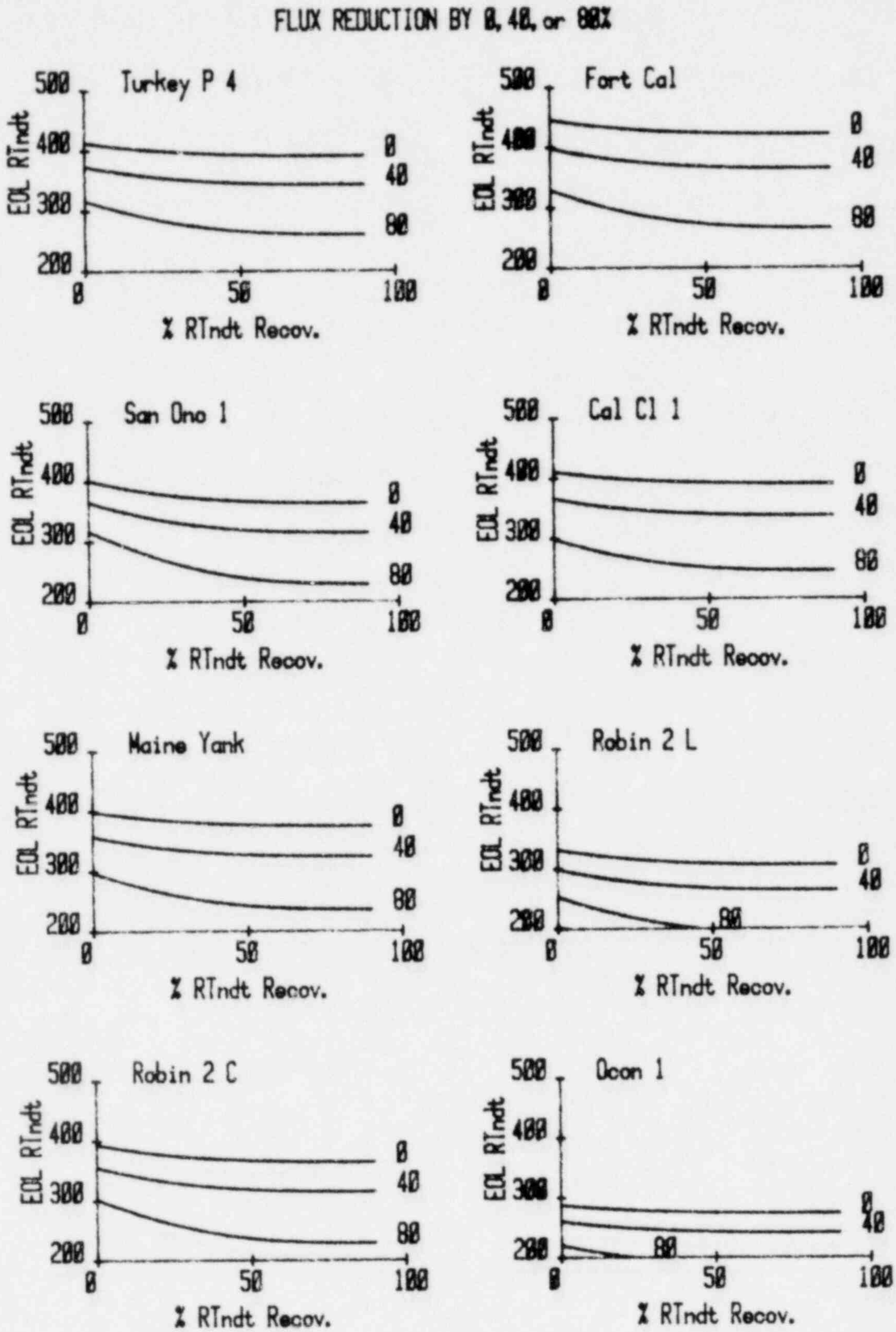

FIGURE 5.13. The Predicted EOL RT_{NDT} as a Function of % Recovery from One Anneal and Flux Reduction by 0, 40, or 80%

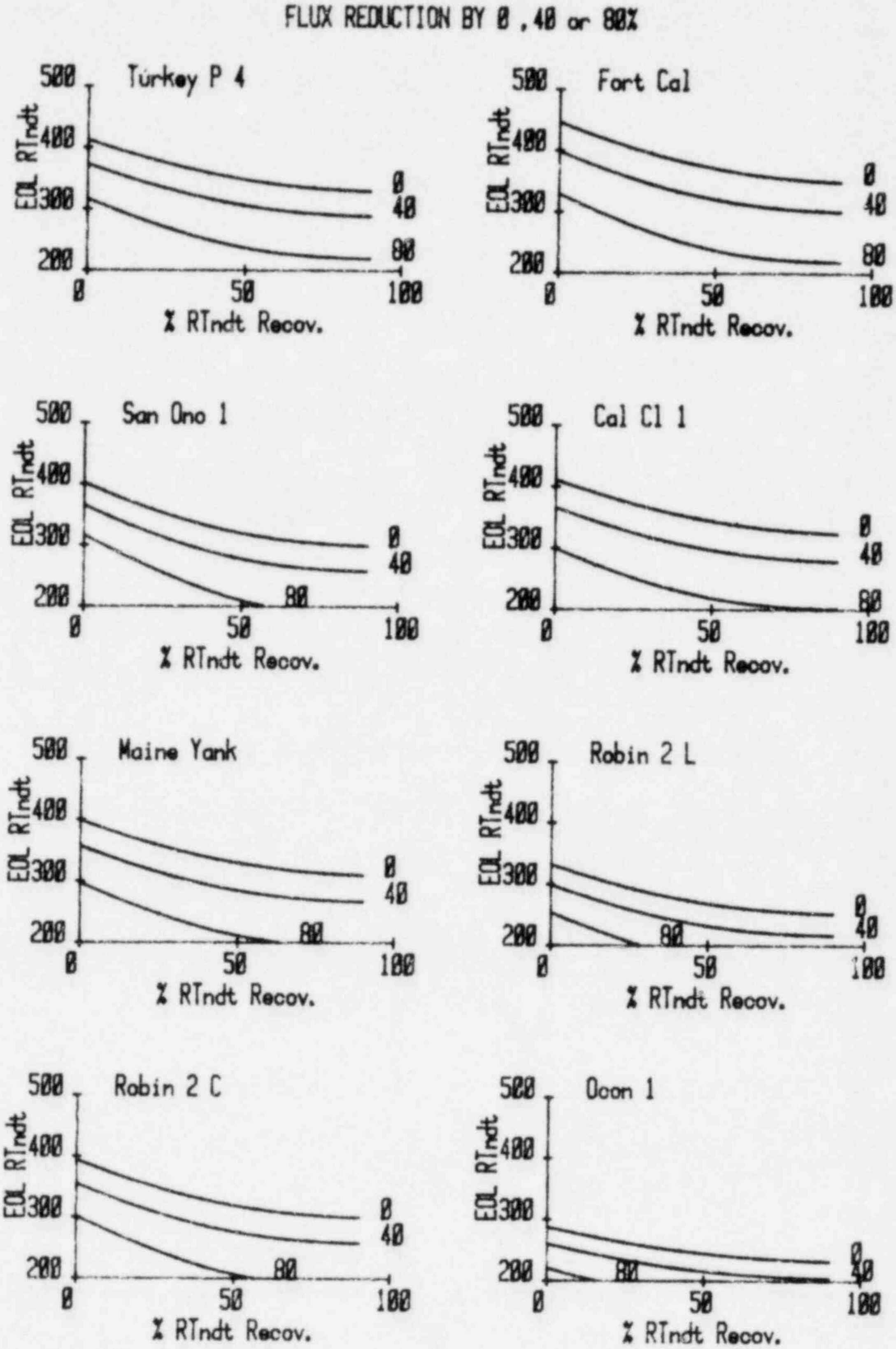

FIGURE 5.14. The Predicted EOL RT_{NDT} as a Function of % Recovery from Two Anneals and Flux Reduction by 0, 40, or 80%

Multiple annealing cycles demonstrate a much more significant reduction in EOL RT_{NDT} than is predicted for a single anneal. The significant benefit that is evident in Figure 5.14 occurs for the double anneal because a shorter reirradiation period follows each anneal. The reirradiation fluence that follows the second anneal is about one-third the EOL fluence, whereas the reirradiation fluence that follows the single anneal (Figure 5.13) is about three-fourths of the EOL fluence.

The effect of reducing the flux by fuel management schemes is clearly beneficial. As a rule, a 10 to 15 degree reduction in EOL RT_{NDT} is expected for each 20% reduction in flux. Multiple annealing cycles and flux reductions are needed to obtain and maintain a low RT_{NDT} (e.g., 200°F).

The calculated rates of reembrittlement used in Figures 5.13 and 5.14 are conservative because they assume a rapid rate consistent with low fluence embrittlement rates. Actual rates of reembrittlement could be much less. The data on annealing and reembrittlement, however, are not extensive enough to justify lesser rates of reembrittlement than assumed in these calculations.

Accurate characterization of critical weld chemistry can reduce the necessary conservatism in the material analysis. Nondestructive, in-situ chemical analysis is a promising technique for evaluating copper and nickel concentrations of welds. Two techniques for in-situ chemical analysis that might be used to evaluate welds include x-ray fluorescence and laser excitation measurements on the vessel exterior. Pacific Northwest Laboratory considered analyzing neutron activation of copper and nickel in irradiated vessels, but concluded that the activation technique is unacceptable.

The x-ray fluorescence technique would use a ^{109}Cd source and a SiLi detector to measure impurities to a 10% accuracy in a 5R gamma environment. A PNL mock-up bench test and experimental analyses indicate that the experiment can be designed to yield an adequate signal-to-noise ratio and resolution needed to measure copper and nickel contents of welds at the outside surface of the vessel. The bench test compared the spectra with and without an external irradiation source to simulate the influence of the vessel gamma irradiation flux. The spectra was from a steel disc with a thin sheet of zinc in front of it to simulate the presence of either copper or nickel in steel. The concentrations measured were representative of the expected percentages of elements, but the signal-to-noise ratio in this test was less than required for practical application of the technique in the field. Realistic increases in the signal-to-noise ratio can be obtained by optimizing the source strength, count rate, collimation, detector area, and resolution.

The laser excitation technique would use a light guide to channel a laser beam to a small area of the vessel exterior to vaporize a minute volume of weld material. The spectrum emitted by the vapor would be channeled in a light guide to a detector for optical analysis. The technique has the required merits of remote, in-situ, nondestructive chemical analysis. A bench test has not been performed to demonstrate the technical feasibility of the laser technique.

The benefit that could be realized from better estimates of weld chemistry is evident (see Figure 5.9). At Fort Calhoun, for example, a copper concentration of 0.31% implies an RT_{NDT} 38°F less than that predicted for the assumed 0.35% copper concentration. The corresponding EFPY increase is about 4 years. A nickel concentration of 0.6% implies an RT_{NDT} 45°F less than that predicted for the assumed 0.99% nickel concentration. The corresponding EFPY increase is about 5 years. Similar benefits are calculated for other plants that have welds containing a high copper-nickel concentration.

6.0 FRACTURE MECHANICS

In this chapter, the fracture mechanics and stress analyses provided by the the NSSS Users Groups, the NRC staff, and the NRC contractors are reviewed. Except for plant-specific inputs, calculations could be performed mostly on a generic basis. Since the methods applied to the 150-day responses differed only in detail, the PNL review primarily evaluated the acceptance criteria for embrittled vessels and assessed the conservatisms in the analyses and fracture mechanics input data.

6.1 ANALYSES METHODS AND CODES

The computer codes and the methods used by the NSSS vendors for the owners group calculations[2,3] are summarized in Table 6.1. The table also includes the OCA-I code developed by ORNL for the NRC[19] and the VISA code developed in-house at NRC for deterministic and probabilistic evaluations.[20,21] The individual assumptions and inputs used for fracture mechanics analyses and the PNL concerns are discussed in the following sections. Only the methods of the generic analyses used by the NSSS vendors will be reviewed, because not enough information was available to review fracture mechanics analyses on a plant-specific basis.

The information that was reviewed was contained in the references cited in the previous paragraph. In addition, supplemental information on the ORNL and NRC analyses was obtained during meetings. On many points, the vendors analyses were not described in adequate detail; therefore, Table 6.1 was completed, in part, on the basis of information implied by statements in the reports.

The foremost trend evident in Table 6.1 is the uniformity among the analyses performed by the independent organizations. This same uniform tendency is evident even in the format used to present the results. The lack of major differences can be attributed to the level of maturity of the theory and to the fact that the fracture mechanics approaches were based, in large measure, on portions of the ASME Boiler and Pressure Vessel Code that address fracture mechanics evaluations.

As indicated in Table 6.1, the different organizations have elected to use different computer codes to perform the actual calculations. The simple cylindrical geometry of the vessel beltline region facilitates the use of linear elastic fracture mechanics to predict crack initiation. One dimensional heat conduction/stress analysis models and linear elastic fracture mechanics models are adequate to treat the thermal shock situation. There is no reason to believe that the computer codes used in the NSSS owners group analyses are not suitably documented and verified. Nevertheless, additional documentation and detailed reports on benchmark-type calculations would be appropriate to further enhance NRC's confidence in the validity of the calculations.

TABLE 6.1. Summary of PTS Fracture Mechanics Analyses

Assumptions and Inputs	Westinghouse	Combustion Engineering	Babcock & Wilcox	ORNL-OCA-I Code	NRC Code
Fracture Mechanics Theory	Linear elastic	Linear elastic	Linear elastic	Linear elastic	Linear elastic
Stress Intensity Factor Solution	Published cylinder solutions 3-D surface flaws; no clad effects	MARC FE code numerical solution; clad effects included	BIGIF code K solutions and/or? Section XI-Appendix A and WRC175 solution	Weight function method validated against finite solutions	Influence coefficient method of Heliot, Labbens & Tanon
Flaw Shape	Two flaw model; 6:1 elleptical shallow surface flaw increasing to "long" flaw for deep flaws	Long axial flaw through clad	6:1 elliptical surface flaw. No increase in aspect ratio as flaw grows in depth	Long axial flow through clad. Only axial flaws	Long surface flaw (axial or circumferential)
Stress Analysis	Equations from classical elasticity theory	MARC code finite element model	B&W code PCRIT based on AMSE Section XI	Equations from classical elasticity	Closed form elasticity solution
Clad Effects	Included in heat transfer but ignored in stress and fracture solution	Clad included in heat transfer, stress analysis and fracture solutions	Clad included in heat transfer. Contribution of clad to thermal stress included. Clad omitted in analysis of pressure stress	Clad included in heat transfer but ignored in stress and fracture solutions. (OCA-II includes clad effects.)	Clad included in heat transfer, and in stress and fracture solution
Warm Prestress	Yes. Included if needed to show safe condition for worst cases	Yes. Included if needed to show safe conditition for worst cases	Yes, if both pressure and temperature stress are decreasing and arrest criteria met	Warm prestress included as option	Warm prestress not included for generic work. Considered if P-T transient is well defined
Acceptance Criteria	No initiation of flaws less than 1.0 deep which do not arrest within 3/4 of wall	None stated	No initiation unless arrest occurs within 1/4 wall. With warm prestress arrest must occur within 1/2 wall	Output provided without acceptance criteria. User interprets results and imposes his criteria	Arrest on or below upper shelf. Preferably no initiation

TABLE 6.1. (Continued)

Assumptions and Inputs	Westinghouse	Combustion Engineering	Babcock & Wilcox	ORNL-OCA-I Code	NRC Code
Shift in RT_{NDT}	Reg. Guide 1.99 except where less conservatism is justified by surveillance data	Reg. Guide 1.99 with adjustment for low nickel. Plate material based solely on 1.99	Reg. Guide 1.99 (for Oconee)	Reg. Guide 1.99	HEDL mean curve based on surveillance specimens for probabilistic analyses; mean plus 2σ for deterministic analyses
Initial RT_{NDT}	No discussion in W Generic report	Plant specified data when available. CE does not accept conservatism of MTEB 5-2 position of RT_{NDT} >10°F	Based on Oconee-1 capsule specimens. Otherwise based on BAW-10046A Rev 1. All Oconee welds taken at +20°F	Provided as user input.	User specified
Toughness Curves	ASME reference curves with 200 ksi $\sqrt{in.}$ upper shelf	ASME reference curves with 200 ksi $\sqrt{in.}$ upper shelf	ASME reference curves with 200 ksi $\sqrt{in.}$ upper shelf	ASME reference curves as default. Option to specify upper shelf. User may specify alternate to ASME curves analyses	Mean toughness curves based on ORNL data for probabilistic analyses, ASME for deterministic analyses
Heat Transfer Model		MARC finite element solution with clad modeled and film coefficient prescribed	3-D (ANSYS code) heat transfer of vessel wall. Clad metal treated as contribution to film coefficient	Numerical solution including clad effect. Film coefficient is user input	Closed form conduction solution for polynominal or exponential temperature history. Clad included with film coefficient
Flaw Depth	1.0 in. or less	Not discussed	Depth not discussed. Probably 1/4 wall or less	Not specified. Results given as function of flaw depth	Specified as statistical distribution of flaw depths. Flaw depth of 15% or less assumed in deterministic analyses.
Radiation Fluence Variation with r and θ	Worst case peak values except plant specific surface values at ID where data was available	Detailed r and θ variation with weld specific values	Specific for each weld with radial variation utilized	Fluence variation through wall modeled	Fluence variation through wall modeled
Initial Weld Chemistry	Worst case if data lacking. Otherwise data from vendors reports and from similar vessels	Surveillance specimen historical trends, and worst case where no data is available	Cu, and P not specific for Oconee vessel, but representative of B&W vessels	Cu and P specified by user	See RT_{NDT} shift curve

6.2 EVENT SCENARIO EVALUATIONS

In PNL's short-term effort described in this report, trends of published analyses were used to guide assessments of the fracture implications of alternate event scenarios. No independent fracture mechanics calculations were performed.

Alternate event scenarios were screened using information obtained from NRC staff members (see Figure 6.1). The trends predicted in Figure 6.1 were reviewed and found to be consistent with specific analyses that were performed using detailed fracture mechanics simulations.

The most significant conclusion to be drawn from Figure 6.1 is that RT_{NDT} at the inside surface of the vessel must not be greater than the relevant cooldown temperature if a flawed vessel is to safely sustain the full operating pressure during a rapid cooldown. However, if the rapid cooldown occurs with only minimal pressure, then RT_{NDT} of the radiation-embrittled vessel can exceed the final coolant temperature by as much as 100°F. It should be emphasized that the trends predicted in Figure 6.1 are for accident conditions in which the acceptable failure probability is higher than would be acceptable for routine vessel cooldown.

6.3 FRACTURE INITIATION, PROPAGATION, AND ARREST

In a relatively conservative scenario, a small, pre-existing underclad crack will be subjected to great tensile thermal stress as well as to more modest levels of pressure-induced stress during the rapid cooling. As the vessel wall cools through the ductile-brittle transition temperature range, the initiation fracture toughness of the material is exceeded and the flaw grows into the wall of the vessel, which has a gradient of decreasing stress and increasing toughness. Next, the crack breaks through the clad, at which time the crack would (in the presence of an embrittled stainless steel clad) tend also to extend in length. The crack would continue to extend in length and depth unless the stress intensity factor at the deepest point of crack penetration into the vessel wall satisfied conditions for crack arrest. For the deep cracks in a vessel that is under substantial internal pressure, it is possible, if not probable, that arrest will not occur and that the vessel will rupture.

Analyses of crack initiation, propagation, and arrest were reviewed to establish if they address the above scenario in a conservative manner. All the analyses conservatively assumed that the flaw initially extended through the clad. Also, all the analyses conservatively assumed that the initial flaw was long; some analyses assumed that the length was infinite. The initial flaw postulated by Westinghouse and Babcock & Wilcox had a length-to-depth ratio of 6:1, which is consistent with guidelines in ASME Section III, Appendix G. All analyses predicted crack growth when the applied stress intensity exceeded the ASME code lower-bound K_{Ic} values. All the initiation models are considered to be acceptable and conservative, provided that the warm prestress effect is not taken to suppress initiation. Concerns with warm prestress are discussed

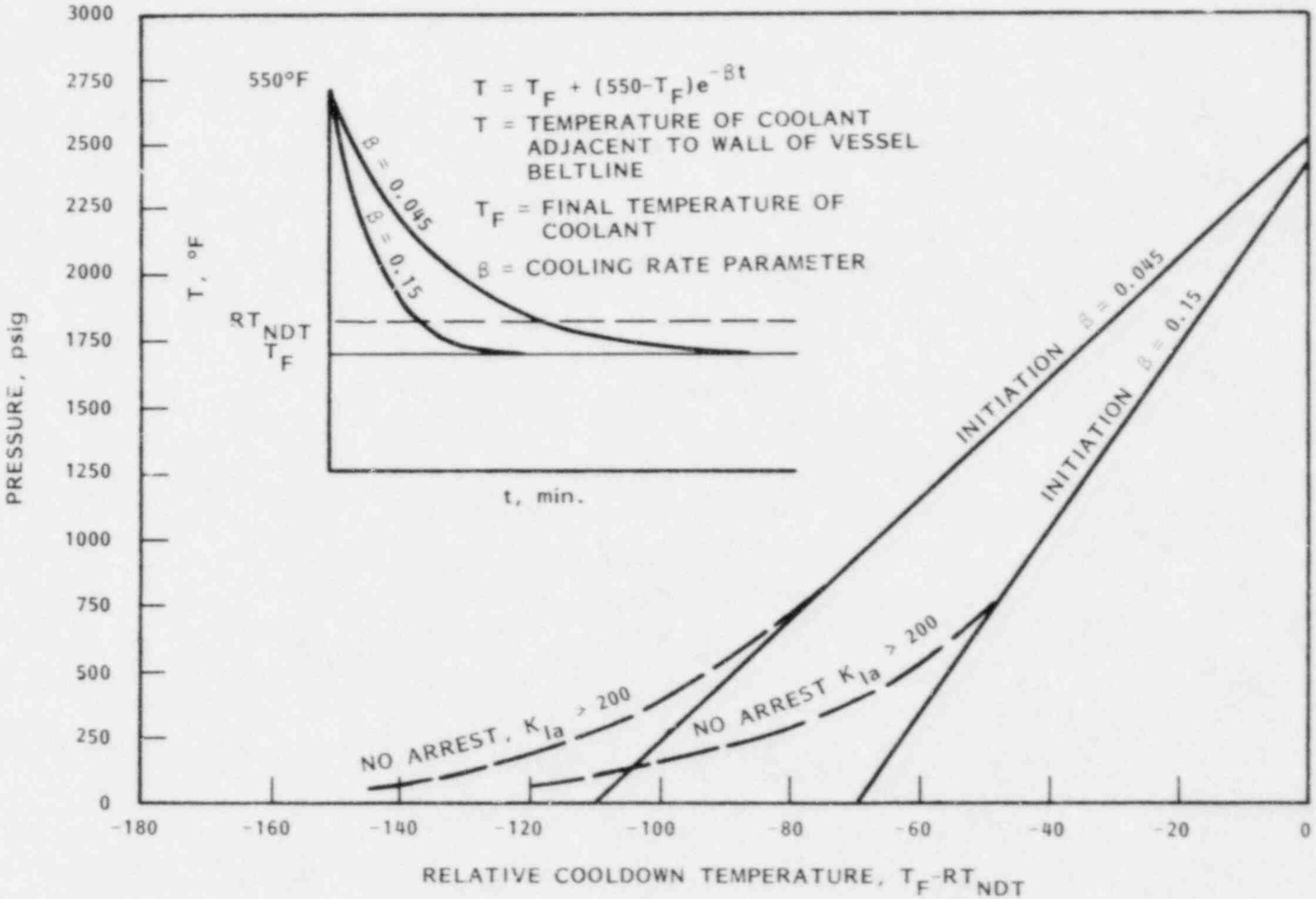

FIGURE 6.1. Diagram Used to Evaluate the Implications of Alternate Event Scenarios (obtained from NRC Staff)

later in this chapter. Also discussed are recommendations for engineering safety factors for the crack-initiation predictions.

Except for the Babcock & Wilcox model, all analyses assumed that the flaw was initially of infinite length or that it increased in length during propagation. The Westinghouse analysis used a particularly elegant "two-flaw concept" to predict the crack propagation in both depth and length in a conservative, but realistic, manner. The Babcock & Wilcox analysis is not entirely acceptable because it assumes that the flaw maintains the 6:1 aspect ratio as it grows in depth, and thus the flaw is not permitted to grow to an infinite length. This deficiency in the Babcock & Wilcox model is not particularly serious because their analyses of propagation and arrest are otherwise relatively conservative in requiring that the initiated flaw be arrested within 1/4 of the vessel wall.

In contrast to the uniformly conservative analyses of crack initiation, many aspects of all the crack-arrest analyses are questionable, particularly for flaws that penetrate deep into the vessel wall. An acceptance criterion, which used arrest at 3/4 of the wall thickness (t) for pressurized thermal shock, was used by Westinghouse and Combustion Engineering. Concerns about the unconservatism of this criterion are addressed in Section 6.11. Also, the ASME code curve for arrest toughness may be unconservative, as documented below.

The pressurized thermal shock analyses used linear elastic fracture mechanics to deal with crack arrest on the upper shelf. For deep cracks in vessels under pressure, the crack-tip plastic zone size can extend sufficiently so that the far boundary can have an effect on crack growth behavior. Furthermore, at depths of 3/4 t, with pressures at a significant fraction of the design pressure, the net ligament could fail by plastic instability. One cannot assume that elastic fracture mechanics is valid under these conditions.

The NSSS owners group analyses of crack arrest may be adequate for pure thermal shock conditions where the crack-tip stress intensity factors decrease rapidly for very deep cracks. However, for conditions of pressurized thermal shock, it is recommended that the linear elastic methods be restricted to flaws no more than, say, 1/2 of the vessel wall. Furthermore, the analyses should ensure that stresses on the unbroken ligament of deeply flawed vessels do not exceed the limit load criteria.

It should be stated that sufficient detail was not available to determine if the plant-specific vessel integrity analyses did, in fact, predict any cases of arrest of deep cracks in vessels under significant pressure. The concerns raised here are directed to a potential unconservatism that could lead to erroneous conclusions about vessel integrity.

In regard to arrest of relatively shallow flaws, it is believed that the fracture mechanics analyses that were submitted to NRC are conservative. The once exception is the definition of the ASME code K_{Ia} curve in the critical transition temperature range. This report presents recent data for welds that fall below the ASME code lower-bound K_{Ia} curve. The lower bound on upper-shelf toughness used in the submittals was 200 ksi $\sqrt{in.}$, which is, no doubt, conservative. Tests for arrest on the upper shelf should show that measured arrest toughness on the upper shelf is actually greater than initiation toughness. This reversal of the relative values of arrest and initiation toughness from that for the transition temperature range can be explained. The reversal arises from the change from a stress-controlled brittle fracture to a strain-controlled ductile fracture; from the rate dependence of plastic flow resistance; and from the rising nature of the R-curve.

A concern not addressed in the NSSS owners group reports is a disturbing trend noted by ORNL in HSST thermal shock tests. Crack initiation in these vessel tests was governed by an apparent initiation fracture toughness that corresponded to the low range rather than to the expected mean of small-specimen fracture toughness data. Their explanation is that the small specimens did not stress as large a sample of material or that the specimens were not sufficiently thick to develop fully the plane-strain plastic constraint at the crack tip to give the worst-case conditions for brittle fracture. The long crack fronts of the HSST vessel tests would enhance both the sample size and plastic constraint effects. The ORNL observations probably have minimal significance for predicting the initiation of the growth of relatively shallow and short underclad cracks. Such cracks have relatively short crack fronts. For crack arrest, the implications are more significant because the concern is with flaws that have grown significantly in length. The ORNL tests indicate that the lower-bound toughness curves in the ASME code are not as conservative as the test specimen data would imply. That is, the behavior of vessels may lie closer to the ASME code lower-bound toughness curve than to the mean toughness curve. In this regard, the behavior observed in the ORNL vessel tests was, in fact, somewhat above the ASME code lower-bound curve, but was well below the mean of specimen data used by ORNL to predict the vessel behavior before the thermal shock tests.

Revising the RT_{NDT} shift curves, which are based on the HEDL analysis of surveillance capsule data, may influence evaluations of crack arrest. Compared to current Regulatory Guide 1.99 curves, the revised curves have lower slopes and predict enhanced radiation damage for lower fluences. These shift curves will thus predict a more uniform distribution of RT_{NDT} through a vessel wall. Also the enhanced toughness within the outer wall will not be as great as some previous predictions. Therefore, predictions of crack arrest based on the older shift curves may be overly optimistic. It is recommended that the sensitivity of the arrest curves (as provided, for example, by the ORNL OCA-I Code) to the change in shift curves be evaluated. It is expected that the critical flaw depths for initiation and arrest will tend to be driven further apart.

Recent data from K_{Ia} tests suggest that the present ASME code K_{Ia} reference curve may not be conservative. In this regard, crack arrest calculations are most sensitive to the position of the K_{Ia} reference curve near the upper end of the transition curve at temperatures $100°F < T-RT_{NDT} < 200°F$. Virtually no recent K_{Ia} measurements have been performed above $T-RT_{NDT} = 140°F$, and very few reliable measurements are available in the range $T-RT_{NDT} > 100°F$.

The sharp upturn of the K_{Ia}-reference toughness curve at $T-RT_{NDT} >$ 100°F is based on a limited number of K_{Id} (rapidly loaded stationary crack toughness) measurements, not on K_{Ia} measurements (see Figure 6.2). The loading rates in K_{Id} tests are substantially less (factor of perhaps 10^4) than the loading rates associated with the arrest of a running crack in K_{Ia} tests. There probably is a close connection between K_{Id} and K_{Ia}, but K_{Id} values cannot be viewed as reliable measures of K_{Ia}.

More recent K_{Ia} measurements are summarized in Figure 6.3 and provide little support for a sharp upturn. This means that there is a possibility that the K_{Ia}-reference toughness curve significantly overstates K_{Ia} values for unirradiated A533B and A508 steel (plate and forgings) for temperatures $T-RT_{NDT} > 100°F$.

The K_{Ia} values for an unirradiated submerged arc weldment (see Figure 6.3) are well below the levels for A533 and A508. This figure illustrates that the existing K_{Ia}-reference toughness curve overstates the K_{Ia} values for the weldment at 100°F. The nonconservatism at the K_{Ia}- reference curve at the upper end of the transition, as suggested in Figure 6.3, could be equivalent to a roughly 50°F underestimation of the RT_{NDT}. This underestimation would have a substantial impact on EFPY.

Other data on K_{Ia} have been reported in a Japanese document (unidentified) which was translated for ORNL. The data were from the ESSO-type of test on two heats of A533B plate. All data for K_{Ia} at temperatures up to 160°F above RT_{NDT} were greater than the ASME K_{Ia} reference curve. Details of the tests were not reported in the Japanese publication. However, the results are believed to be questionable, due to the test technique used to measure K_{Ia}.

More efforts should be made to measure K_{Ia} values in the range $150°F < RT_{NDT} < 250°F$. In the meantime, a more conservative K_{Ia}-reference toughness curve should be included in the crack-arrest calculations. As an ad-hoc measure for PTS calculations, it is recommended that the present K_{Ia} reference curve be shifted 50°F. This will be consistent with the data trends at higher temperatures shown in Figure 6.3. Any overconservatism at lower temperatures will be of no consequence, since only the upper temperature range has any bearing on crack arrest under PTS conditions. A long-range revision of the ASME K_{Ia} reference curve should be based on a review of available data by a qualified task group.

6.4 NIL-DUCTILITY TEMPERATURE CRITERIA

The nil-ductility temperature approach of Pellini[22] is a simplified engineering guide to the prevention of brittle fracture, and can be used in place of detailed fracture mechanics analyses. This approach defines a minimum service temperature (T_{min}) for low strength ferritic steels ($\sigma_y \geq 50$ ksi) and is most commonly stated as follows:

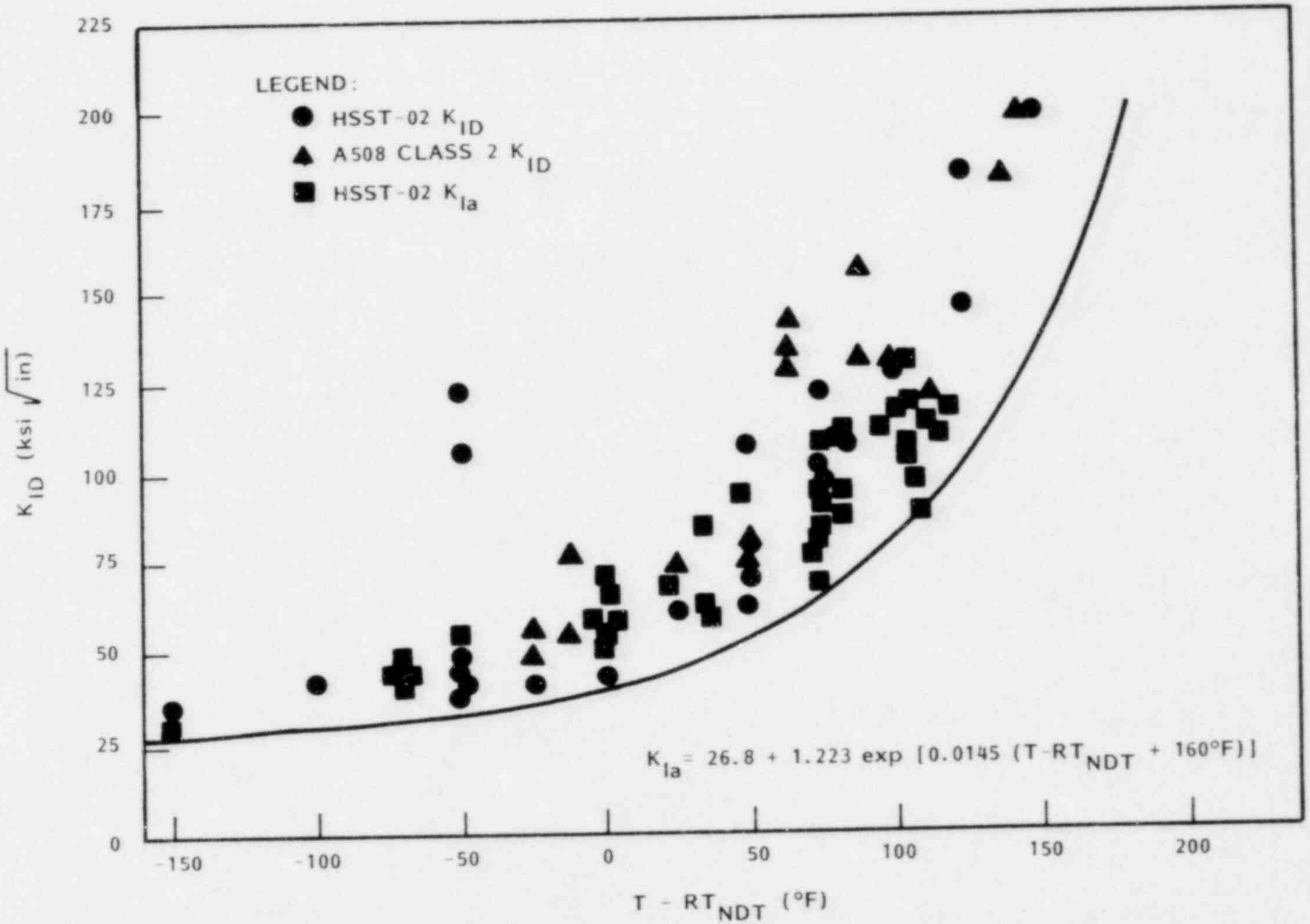

FIGURE 6.2. K_{Ia} Reference Toughness Curve with Supporting Data(23)

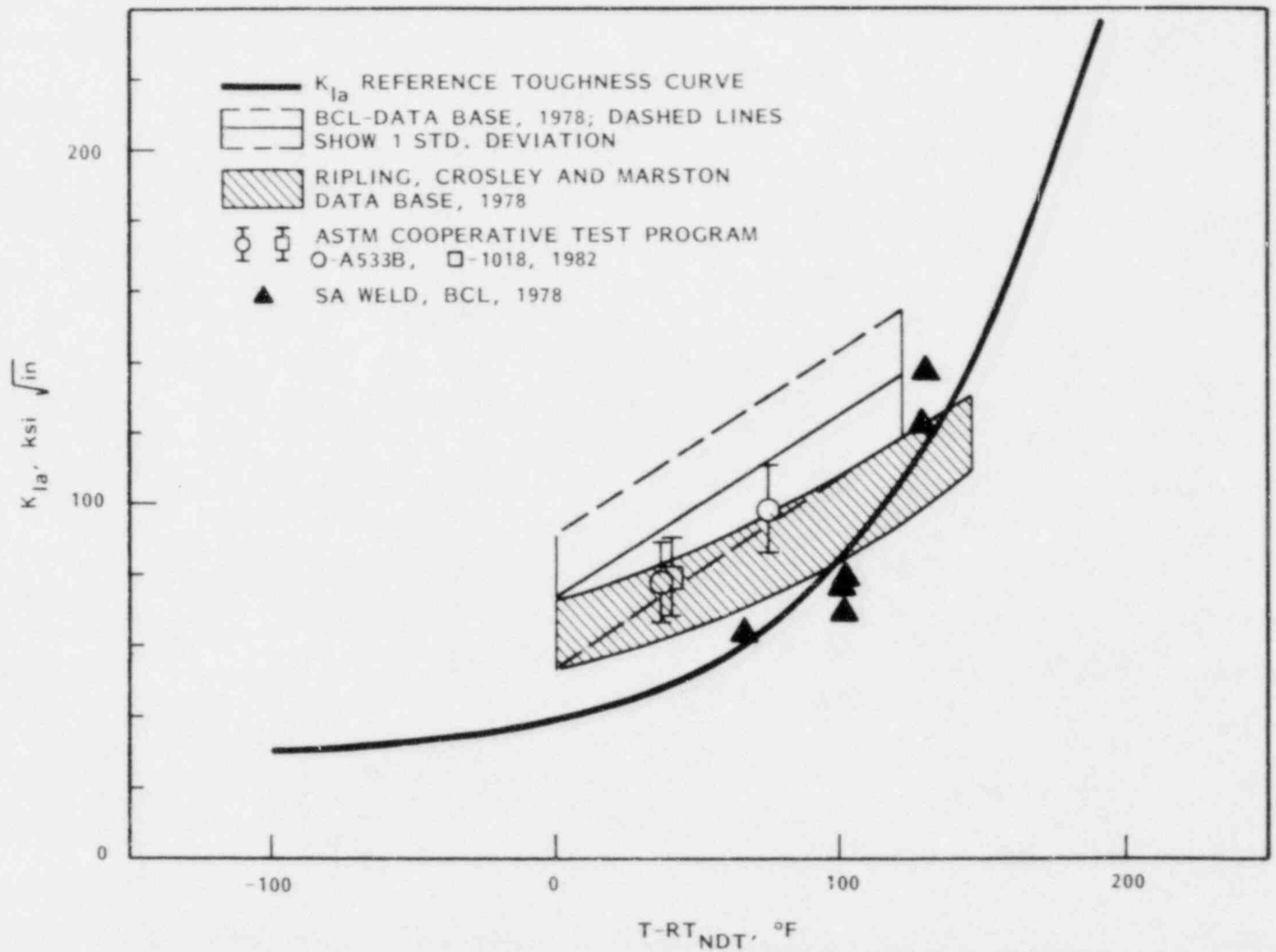

FIGURE 6.3. Summary of Recent K_{Ia} Measurements on A533B Steel

$$T_{min} \geq RT_{NDT}, \quad \text{if} \quad \sigma < 5 \text{ to } 8 \text{ ksi}$$

$$\geq RT_{NDT} + 30°F, \quad \text{if} \quad \sigma < 25 \text{ ksi}$$

$$\geq RT_{NDT} + 60°F, \quad \text{if} \quad \sigma < 50 \text{ ksi}$$

$$\geq RT_{NDT} + 120°F, \quad \text{To ensure upper shelf ductile failure at ultimate strength}$$

These criteria are largely empirically based and are supported by documented cases of in-service brittle failure. The approach does not require detailed stress or fracture mechanics analyses, and it is a useful guide when load and stresses are not well defined, or when detailed analyses are not practical.

The nil-ductility approach has also been developed to include the effects of thick section sizes such as those present in reactor vessels. Results published by Pellini(24) are shown in Figure 6.4, and their implications are indicated in Table 6.2. For the arrest of a large through-thickness crack (TTC), the criteria are essentially as stated above. However, for the initiation of a small crack, which is essentially the situation of concern in PTS events, the thick-section criteria is considerably less restrictive. In the case of high levels of applied stress in the yield stress range, one may operate at a temperature only slightly above NDT. This conclusion is not inconsistent with detailed fracture mechanics results generalized in Figure 6.2 (see Section 6.2).

Provisions of the ASME Pressure Vessel Code and NRC criteria have often been based on simple limits related to RT_{NDT}. For example, ASME Section III requires that preservice proof tests be performed at a minimum temperature of RT_{NDT} + 60°F as a safeguard against catastrophic fracture. Similarly, Regulatory Guide 1.99 requires that vessels be designed for thermal anneal if RT_{NDT} is expected to exceed 200°F before end-of-life. It is stated or implied that a conservative shifted value of RT_{NDT} at the 1/4 wall location after radiation exposure is RT_{NDT} = 200°F. Furthermore, the background to Regulatory Guide 1.99 states that the intent is to allow a 200°F decrease in coolant temperature from the operating temperature to account for system transients. This would ensure upper-shelf toughness during transients that result in cooling to 350°F.

There are a number of difficulties in applying RT_{NDT} criteria to the PTS problem. These include identifiying the cooling transients that would give the appropriate minimum service temperature, and establishing the required margin above the RT_{NDT}. Simplistic application of the Pellini(22) criteria would suggest that $T_{min} \geq RT_{NDT} + 120°F$ for the severe stresses associated with thermal shock. For the Rancho-Seco transient, which involved cooling to about 280°F, the required maximum allowable RT_{NDT} would be a very conservative value of 160°F. However, an irradiated vessel will have a significant through-wall gradient in toughness (RT_{NDT} varies through the wall), and during the cooling there will be a gradient in temperature through the wall. Both factors have a bearing on the appropriate maximum value of the inside surface RT_{NDT} required to avoid brittle fracture during a thermal shock transient.

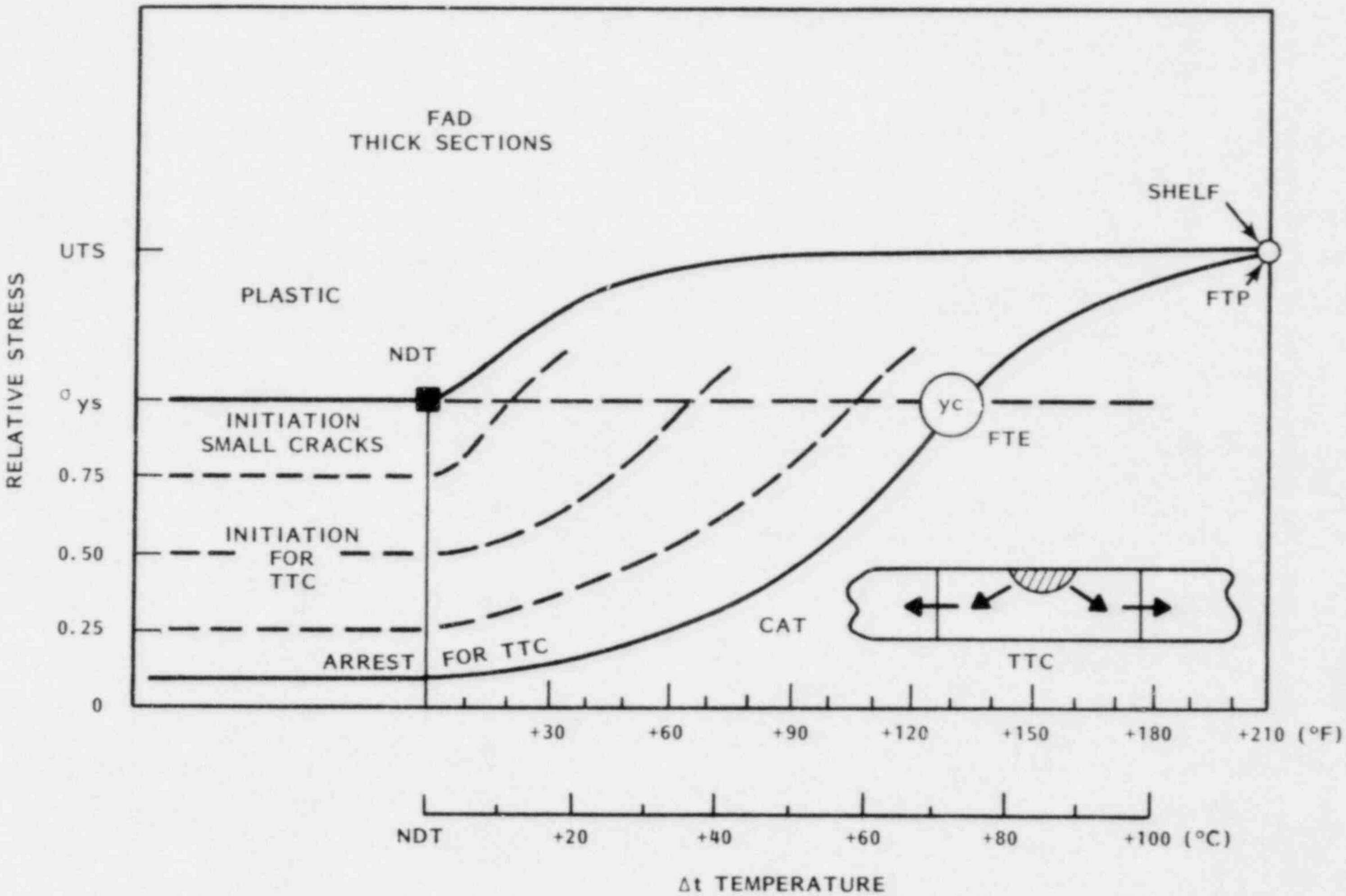

FIGURE 6.4. Failure Assessment Diagram (FAD) Expansion to Include the Effects of Very Large Section Size

TABLE 6.2. Implications of Failure Analysis Diagram[24] for Thick Sections

Applied σ/σ_{ys}	Stress ksi[a]	Relative Temperature to Arrest a TTC[b]	Relative Temperature for Preventing the Onset of Crack Extension: Small Crack	Relative Temperature for Preventing the Onset of Crack Extension: Large Crack
1	60	NDT + 130°F	NDT + 20°F	NDT + 60° to 110°F
0.75	45	NDT + 120°F	NDT + 0°F	NDT + 50° to 90°F
0.50	30	NDT + 100°F	NDT + 0°F	NDT + 0° to 60°F
0.75	15	NDT + 60°F	NDT + 0°F	NDT + 0°F
~0.1	5-8	NDT + 0°F	NDT + 0°F	NDT + 0°F

(a) for σ_{ys} = 60 ksi.
(b) TTC = through-thickness crack.

The intent of detailed fracture mechanics analyses should be to establish appropriate limits of RT_{NDT} in a more realistic manner than is possible with the Pellini criteria,[22] which are based on experience outside the area of reactor vessel integrity. Nevertheless, the limiting RT_{NDT}'s that are based on detailed fracture mechanics analyses should be consistent with or be viewed as refinements of the proven empirical criteria. The danger in applying detailed fracture mechanics is that unconservative inputs to the analyses can lead to unconservative RT_{NDT} limits. For example, by assuming nonconservative transients or very small flaw sizes, one could erroneously predict the safe operation of vessels with very low toughness corresponding to excessively high values of RT_{NDT}.

6.5 CONSERVATISMS IN ANALYSES

It is desirable that fracture evaluations be conservative enough to ensure a suitably low probability of failure without being excessively conservative. Excessive conservatisms can result in unnecessary operating restrictions. Sources of conservatisms and engineering safety factors, and the appropriate levels for these factors, will be described in this section.

In this analysis of fracture mechanics, the pressure and temperature histories used as input are assumed to be realistic. It is left to the overall risk analysis to assign suitable probabilities to the occurrence of the loadings. In the fracture mechanics analyses, no additional safety factors have been applied in the 150-day responses to the pressure temperature transients, nor does the ASME code or NRC require such factors on loads.

There is no requirement to overestimate stress in the stress analyses nor are the calculations of crack-tip stress intensity factors in the fracture mechanics analyses required to be anything other than as accurate as possible. The vessel beltline geometry, applied loadings, and postulated flaws are sufficiently simple to allow sufficiently accurate solutions for elastic stress intensity factors. It is believed that these solutions for the PTS scenarios have accuracies on the order of 10%.

The conservatisms present in the PTS fracture analyses are from two sources, namely, the fracture toughness estimates and the size and shape estimates of the postulated flaws. Fracture toughnesses in the various PTS analyses were based on the reference toughness curves in ASME Section XI. The code toughness curves were derived as lower bounds on data, but the curves contain no arbitrary safety factor. In fact, the curves do not extend below actual fracture toughness data points. This implies that there is a finite probability that a given vessel has a toughness at some location equal to or even less than the ASME bounding curve. Furthermore, as discussed above, recent data indicate that the current ASME K_{Ia} curve may not always be a lower bound, particularly for weld material.

Closely related to the reference toughness curves are the estimates of initial RT_{NDT} and the radiation-induced shift in RT_{NDT}. There are unreliabilities associated with the use of RT_{NDT} as the sole parameter for characterizing the toughness of ferritic steels in the transition temperature range. The current ASME characterization method assumes that the shape of the fracture toughness curve is constant for all applicable materials. For nontypical heats, one cannot rely on the curve to always give a lower bound. Nevertheless, RT_{NDT} provides a useful approximation for characterizing toughness for large shifts in transition temperature produced by irradiation.

Characteristics of radiation-induced shift are discussed in detail in Chapter 5.0. It should be noted here, however, that certain licensee analyses may have used vessel-specific properties based on the initial RT_{NDT} specimens and shift as measured from surveillance specimens. These fracture mechanics analyses could, in fact, be based on shift values more representative of mean rather than upper-bound values. As such, there is no conservatism in the estimated shift implied in these vessel-specific analyses. However, for the older vessels of concern in the 50- and 150-day responses, intial RT_{NDT} and the shift in RT_{NDT} have been estimated, in most cases, on the basis of NRC criteria. These criteria were developed as bounding curves. Again, these bounds are not unreasonably conservative because in isolated cases, actual surveillance data can be shown to fall in line with bounding curves (e.g., those given in Regulatory Guide 1.99).

Considerations of flaw size are discussed in Section 6.9. The postulated flaw sizes in the PTS analyses are intentionally conservative. The flaw sizes are the only input to the analyses that are not supported by data, and this does not allow the realism or bounding nature of the input to be evaluated. In the PTS situation, postulated flaw depth is not a good, direct measure of conservatism. Under thermal shock, there is an increase in fracture toughness and a decrease in thermal stress with depth into the vessel wall. This makes predictions of crack initiation relatively insensitive to flaw depth. Accordingly, PTS evaluations by the licensees have considered the consequences of flaws smaller than the maximum postulated size. With regard to crack arrest, small flaws can be even more dangerous than large flaws. Small flaws will initiate at higher stress levels and will propagate to a greater depth before arrest.

In summary, the fracture mechanics analyses and each of the inputs except flaw size have no arbitrary safety factors. Nevertheless, the bounding nature of the inputs, when combined, tend to provide a safety margin. In other words, the probability is low for the postulated flaw to be present at a location of lower-bound toughness that also experiences an upper-bound shift in RT_{NDT}. The estimates of these probabilities have been performed by J. Strosnider of the NRC staff using the VISA code based on Monte Carlo simulations. An evaluation of these analyses is addressed in Chapter 8.0.

6.6 SAFETY FACTOR CONSIDERATIONS

Any conservatisms of the PTS fracture mechanics predictions in the licensee responses are not based on arbitrary safety factors. An exception is safety factors that may be implied by the size of postulated flaws. The safety factors prescribed by the ASME code are evaluated in this section. Also evaluated is whether or not the lack of explicit safety factors in the licensee responses is consistent with code requirements.

Guidelines for fracture mechanics evaluations are given both in ASME Section III, Appendix G and in ASME Section XI, Appendix A. In many respects, both sections prescribe similar methodologies. The evaluations for the PTS situation would fall most likely under rules in Section III, which describe pressure-temperature and toughness requirements. Section XI addresses the consequences of known flaws detected during in-service inspection.

For normal (Level A) and upset (Level B) loads, Appendix G requires a factor of 2.0 on the stress intensity factor for pressure-induced stress, but it does not require a safety factor on thermal stress. The evaluation is based on the K_{IR} fracture toughness (i.e., the arrest toughness at the point of deepest crack penetration). Using arrest toughness rather than the higher initiation toughness can be viewed as an implied safety factor or conservatism. The postulated flaw in Appendix G is a 3/2 wall long by 1/4-wall deep (6:1 aspect ratio).

Pressurized thermal shock would be classified in Appendix G with Level C and D loads (emergency and faulted, respectively). The code gives little guidance for fracture mechanics evaluations, but it does recommend that the principles prescribed for Level A and B loads be used in the evaluations. Also, the defect sizes, material toughness, and loadings should be justified on an individual case basis. No specific safety factors are suggested, and in this sense, the licensee fracture evaluations for PTS do not fall outside code requirements.

Section XI provides somewhat more specific guidelines for flaw evaluations. For normal and upset loads, a factor of 10 on flaw size is specified, or as an alternative, a factor of $\sqrt{10} \approx 3$ can be applied to fracture toughness. A further conservatism for normal and upset loads is that the arrest fracture toughness be used in the evaluation. Thus, a net safety factor greater than three for fracture initiation is required when comparing applied K to the lower-bound toughness curves for initiation.

Criteria in Section XI are less conservative for low-probability emergency and faulted conditions such as PTS than they are for normal and upset loads. Predictions of crack initiation are based on the K_{Ic} initiation curve rather than the K_{Ia} arrest curve. A safety factor of 2.0 on critical flaw size is specified, but as an alternative, a safety factor of $\sqrt{2}$ can be imposed on the ASME code reference K_{Ic} curve. Under emergency and faulted conditions, K_{Ic} can be used if analyses show that crack arrest will occur within 3/4 of the vessel wall; otherwise, the initiation prediction is to be based on the more conservative arrest toughness curve.

In summary, the safety factors in the ASME code for fracture evaluations are quite small for emergency and faulted loads. The PTS fracture evaluations reviewed by PNL do not include safety factors, and this is consistent with the ASME code only if one assumes that the postulated flaw sizes used in the PTS calculations have a factor of two applied to flaw depth, and that the acceptance criteria include some demonstration of crack arrest capability.

It is recommended that specific safety factors be applied in PTS fracture mechanics evaluations to provide recognition for possible unreliability in analytical treatments and input parameters. The safety factor should be modest and consistent with traditional ASME code factors for emergency and faulted conditions. Considering that fracture behavior is somewhat insensitive to flaw size under thermal shock conditions, it is recommended that the safety factor be applied to the stress intensity factor rather than to flaw size. Accordingly, the following options for safety factor are suggested:

a. No safety factor on crack initiation is required on the basis of the ASME K_{Ic} reference curve, provided that crack arrest can be demonstrated. However, the arrest analysis must be based on the conservative criteria outlined in Section 6.11 (i.e., revised K_{Ia} reference curve, arrest within 1/2 wall, etc.).

b. An implied safety factor on crack initiation can be imposed by using the K_{Ia} toughness curve as the criterion for initiation. The revision of the ASME K_{Ia} curve should be used. No demonstration of crack arrest is then required.

c. A safety factor of $\sqrt{2}$ on the crack initiation can be imposed on the basis of the ASME K_{Ic} reference curve. No demonstration of crack arrest is then required.

6.7 PROBABILISTIC ASSESSMENTS

Recent calculations by Strosnider at NRC have addressed vessel integrity under pressurized thermal shock using probabilistic methods[21] that use a computer code described by Gamble and Strosnider.[20] The methods described in these reports were reviewed on April 15, 1982, by Strosnider and members of the PNL Pressurized Thermal Shock Team. Details of the methodology will not be discussed here. Rather, the probabilistic analyses will be used to interpret the deterministic predictions of the NSSS users group analyses. The intent will be to assess the conservatism of the deterministic analyses as a certain level of (low) failure probability.

The Strosnider probabilistic analyses were found to closely parallel deterministic methods such as those used in the ORNL OCA-I code. (In fact, an extension of the OCA-I code is being developed to include a probabilistic failure prediction.) Treatment of the pressure and temperature transient is treated in a purely deterministic manner, as are the predictions of the corresponding vessel stresses and crack-tip stress intensity factors. Only the flaw size and fracture toughness are treated in a probabilistic manner.

Uncertainties in estimating fracture toughness of an irradiated vessel and uncertainties related to estimating RT_{NDT} are addressed through statistical distributions. The mean curves for estimating toughness and shift in RT_{NDT} were essentially the mean curve counterparts of the upper-bound ASME code fracture toughness curves and the HEDL surveillance-based shift curves. Uncertainties in copper content and fluence levels are also simulated by statistical distributions. In effect, the analyses account for the possibilities of the inputs having upper-bound-type values, but does not assume, as in deterministic analyses, that all inputs always have upper-bound values for a given overcooling accident.

Although the probabilistic predictions have been considered to be mainly qualitatively correct and useful for sensitivity calculations, it is believed that the analyses also show semiquantitative trends. In this context, predictions from the deterministic OCA-I code[25] and the probabilistic VISA code[21] were compared. The comparison involved a postulated vessel with a high level of radiation embrittlement subjected to the conditions of the Rancho-Seco transient. The specific parameters were:

Rancho-Seco transient

0.35% Cu

$RT_{NDT_0} = 20^\circ F$

No warm prestress

The OCA-I code predicts the following conditions for crack initiation:

Fluence (at surface) = 10.0×10^{18} n/cm^2

RT_{NDT} (at surface) = 300°F (from Regulatory Guide 1.99)

Initial depth of flaw = 1.0 in.

Time = 70 min into transient (coolant temperature of 280°F and pressure of 2000 psi)

In contrast, the VISA code predicts a failure probability of about 10^{-5} at a fluence of 10.0×10^{18} n/cm^2. The estimated probability of having a flaw of the required 1.0-in. depth is approximately 10^{-2}. Given the existence of this flaw in the critical weld, the probability of failure is approximately 10^{-3}. The 10^{-2} flaw probability is based on data for all types of flaws in vessel welds, and not specifically for underclad cracks (which are of greatest concern to PTS). A probability of 10^{-2} is, no doubt, conservative for vessels found to be free of detectable flaws after an effective in-service inspection as described in Chapter 7.0 of this report.

Comparison of deterministic (OCA-I) and probabilistic (VISA) predictions indicates that the conservative inputs to the deterministic analyses imply a low probability of failure. It should be stated that the predicted probability of failure increases rapidly with an increase in RT_{NDT}. An increase in RT_{NDT} from 300° to 360°F implies an increase in predicted failure probability from about 10^{-5} to about 10^{-3}.

It is believed that probabilistic analyses are a useful method for evaluating the level of conservatism in deterministic analyses. It is recommended that the existing base of preliminary probabilistic predictions be refined and expanded to increase their credibility and usefulness. The main need is to improve the estimates of mean and variance for the input variables. The greatest limitation at present is the lack of a good basis for the probability of occurrence and size distributions for near-surface flaws in vessels. The most effective near-term improvements in the probabilistic analyses would be achieved through a formal or informal peer review, which would establish a consensus on the judgmental-type inputs to the model.

6.8 WARM PRESTRESS

All three NSSS vendors used the concept of warm prestress. However, for plant-specific analysis, it was possible to show that for most postulated accidents, vessel failure did not occur even when no credit was taken for warm prestress. Only as a last resort was warm prestress included in the analyses.

A large and consistent body of empirical evidence indicates that crack growth does not initiate under conditions of decreasing crack-tip stress intensity factors (K). The evidence for the warm prestress effect under increasing K at levels below a previous K-maximum is believed to be less convincing. In this regard, the responses claimed credit for warm prestress only for situations of decreasing K.

Reservations regarding effects of warm prestress focus on the requirement of a decreasing crack-tip K value. This condition can be readily violated by any small but rapid increase in system pressure, which would produce a sharp increase in K at the crack-tip. Plant records have shown that pressure fluctuations occur during cooling transients, and these functions could have negated assumptions regarding conditions of decreasing K from a decaying thermal stress field. Pacific Northwest Laboratory's review has also encountered scenarios where the K level continues to decrease late in the transient; the decrease occurs only because predicted thermal stresses decay more rapidly than does the predicted increase in stress due to repressurization. In certain cases, the K level decreases only slowly and never falls much below its peak value. In such situations small changes in the pressure/temperature history would negate the supposed benefit of warm prestress.

We recommend that warm prestress be included in vessel integrity evaluations only if it can be clearly and convincingly shown that an increasing K field cannot exist during the critical portion of the transient. We do not question laboratory demonstrations of warm prestress. The HSST vessel thermal shock tests conducted by ORNL have exhibited warm prestress effects. The concept has proven valuable in explaining the outcome of controlled laboratory experiments, but it should be used with great caution in conservative engineering safety evaluations where the detailed conditions of loading are subject to considerable uncertainty. Furthermore, claiming benefit from warm prestress could be viewed as inconsistent with other engineering approximations in the fracture mechanics evaluations. For example, fracture mechanics evaluations typically neglect possible harmful effects of residual stresses in vessel welds.

6.9 FLAW-SIZE CONSIDERATIONS

A critical input to the fracture mechanics evaluations is the postulated flaw size. In this regard, analyses in the licensee responses are vague and provide little or no justification for the upper-bound flaw sizes used in their fracture mechanics analyses.

In establishing pressure temperature limits for normal and upset loads, Appendix G of ASME Section III specifies a 1/4 wall flaw of 6:1 length-to-depth ratio. Appendix G would cover PTS as emergency and faulted loads but gives little guidance as to the postulated flaw size. The Appendix does, however, state that postulated flaw size is to be justified.

The Westinghouse owners group report[4a] specifically states that they used a 1-in.-deep flaw of 6:1 ratio. They justified this with statements regarding flaw detection capabilities for in-service inspection. The other owners group reports make no statement about postulated flaw depths. One can infer that Babcock & Wilcox considers flaws of 1/4 wall or less, because their acceptance criteria call for arrest at 1/4 wall. The Combustion Engineering owners group report states that "no initiation is permitted." This probability means that they postulated the existence of any flaw that might initiate. In the context of the "football curve," this means that the initiation zone is required to be of zero size, or that the initiation zone lies to the right of the warm prestress line.

None of the reports makes any mention of actual known cracks in vessels or even discusses possible mechanisms of cracking or characteristics of possible cracks. Reports on ORNL and EPRI studies on the PTS problem have alluded to underclad cracking mechanisms and factors that can contribute or preclude such cracks in specific vessels. Information of this type, along with definite information on crack detection capabilities, could provide a basis for postulating worst-case flaw sizes in the critical welds of concern.

A 1-in.-deep flaw, as postulated by Westinghouse, could be justified on the basis of an effective inspection procedure (see Section 6.1). Assuming that a flaw of about two clad thicknesses (about 0.5 in.) can be detected, the 1-in. depth would provide a margin of two relative to flaw depth as specified in the ASME Section XI rules for evaluation of known flaws under emergency and faulted loads. A 1-in. flaw would be about 1/8 wall and would be consistent in being somewhat less conservative than 1/4 wall Section III, Appendix G, flaw for normal and upset loads.

6.10 CLAD EFFECTS

The treatment of clad effects was fairly uniform in the various analyses. The benefit of clad is included as an impedence to heat transfer to the base metal of the vessel wall. Structurally, the cladding has been ignored or, if included (as in the Combustion Engineering fracture model), has been modeled in a manner such to enhance crack growth.

The presence of clad will enhance the probability of the existence of a crack in critical welds. Marston[16] discusses factors leading to underclad cracking (e.g., metallurgical and welding variables) for weld-deposited clad. Notably, none of the licensees' responses addressed the potential for underclad cracks in their specific vessels. Clad will also enhance the existence of flaws because cladding interferes with an effective ultrasonic inspection. Difficulties of detecting underclad cracks are addressed in Chapter 7.0.

The effects of clad on crack propagation under thermal shock conditions has been considered in the HSST program at ORNL. Their HSST work on this topic was reviewed by PNL during a visit to ORNL on April 20, 1982. To date, only scoping-type calculations have been performed, but an accelerated experimental program is underway. Results are scheduled to be available in June 1982.

It is quite possible that underclad cracks in the base metal of the vessel will not extend into the clad during a PTS event. Under thermal shock, the clad will tend to restrain the opening of cracks and, hence, more severe transients and greater embrittlement would be required for vessel failure. Additionally, the ORNL tests now in progress are expected to demonstrate that a tough clad material will "pin" the ends of a surface crack and prevent it from growing circumferentially or longitudinally in a vessel. Such an effect would greatly enhance the possibility of arresting initiated cracks after a limited amount of growth in the depthwise direction. However, the toughness of clad material, particularly after irradiation, is not currently known. Until ORNL data on irradiated clad are available, credit for favorable clad effects on crack initiation and arrest is not justified for conservative vessel integrity evaluations. None of the licensee's evaluations have taken such credit for clad.

6.11 ACCEPTANCE CRITERIA

The discussion of the NSSS owners group responses showed clear differences in acceptance criteria and, therefore, in levels of conservatism regarding acceptable vessel performance in a PTS event. The differences include postulated initial flaw sizes, crack initiation criteria, and allowable flaw depths for crack arrest. The ASME code fails to define specific criteria in Section III, Appendix G, for emergency and faulted loads. Therefore, it is necessary to define minimum acceptance criteria as well as ground rules for fracture mechanics calculations if NRC is to evaluate plant-specific responses on a uniform basis.

A number of alternative criteria for evaluating vessel integrity have been proposed by PNL. For a detailed list of the criteria, the reader is referred to the Conclusions and Recommendations (Chapter 2.0) of this report.

Unconservatisms in the crack arrest analyses in the 150-day responses were outlined in Section 6.3. Further justification for the proposed crack-arrest criteria are stated in the following paragraphs.

The response in the Babcock & Wilcox owners group report (3d) proposed arrest at 1/4 wall, which is more conservative than that proposed here, but the additional conservatism is justified because Babcock & Wilcox did not consider the crack arrest for a long flaw but rather for a 6:1 (length:depth) flaw. Westinghouse and Combustion Engineering proposed a criterion of arrest of a long flaw at 3/4 wall. This criterion was considered unacceptable for three reasons:

1. Arrest at a depth of 3/4 wall would be such that vessel failure in a ductile manner would be of concerrn if the operating pressure should be imposed late in the transient or after termination of the transient during post-accident recovery. These conditions of pressurization have not been addressed by the vendors. The 3/4-wall flaw depth for arrest is that given in ASME Section XI, Appendix A, for evaluation of flaws detected during in-service inspection. This flaw size is believed to have been included in the code in the context of vessel thermal shock in the event of a large-break LOCA.

2. In the large-break LOCA system, repressurization is physically impossible. On the other hand, pressurization in a PTS event is not only possible, but is desirable to maintain pressure at specific levels. Also, in a large-break LOCA, plant operation at pressure cannot and will not occur for an extended time period; thus, only safe shutdown of the plant is of concern. Furthermore, the safety implications of a through-wall flaw is different for an unpressurized vessel. Because there would not be a large release of energy as could occur in a PTS scenario, a leak rather than a break would be the possible result.

3. Plant personnel may have no indication that a large flaw has been created in the vessel wall during a PTS event. Only a detailed and effective in-service inspection could detect flaws that were initiated and arrested in a PTS event. Accordingly, item "d" of criteria 6 (see Section 2.4) requires a minimum level of acceptability of the arrested flaw under normal (as opposed to accident) conditions. This criterion is proposed because normal heatup and cooldown transients will subject the vessel to temperatures well below RT_{NDT}.

Item 6c (Section 2.4) addresses the selection of an initiation assumption to ensure that the crack jump is not underestimated in the arrest calculations. This criteria is in need of further detailed development. The Westinghouse owners group report has an approach that is consistent with PNL's recommendation. Specifically, the Westinghouse approach identified the maximum flaw that could initiate $[a_{c(max)}]$ for cases where warm prestress was applied.

6.12 SENSITIVITY ANALYSES

Fracture mechanics analyses were performed to establish the sensitivity of the fracture predictions to the inputs and assumptions. These calculations were performed for a high copper and nickel weld in a postulated vessel with relatively high fluence and high initial RT_{NDT}. Results reported at ORNL for the Rancho-Seco transient using the OCA-I code[26] formed the basis of the present estimates. Results for the sensitivity analyses are given in Table 6.3. A definition of the baseline conditions is also provided.

In the calculations listed in Table 6.3, each of the factors was changed independently to perturb the conditions of the ORNL baseline analyses. The ORNL analysis predicted the initiation of fracture for a 1.0-in.-deep flaw at a fluence of about 1.0×10^{19} n/cm^2 when the benefit of warm prestress was neglected. For the high-fluence vessel considered here, this fracture condition would occur at 6.6 EFPY of operation. The effect of changing each factor is expressed in Table 6.3 as an increase or decrease in EFPY before encountering a critical condition for fracture if the vessel were to encounter conditions similar to those of the

TABLE 6.3. Sensitivity of Fracture Mechanics Analysis to Input Parameters and Assumptions (for a postulated vessel with severe radiation embrittlement subjected to conditions of the Rancho-Seco transient)

Condition Factor Changed from Baseline Case	EFPY Yr	Impact on Critical Flaw Growth Increase/Decrease in EFPY, yr	 Increase/Decrease in RT_{NDT}, °F
Baseline conditions[a]	6.6	0	0
Cu, 0.25% vs. 0.35%	11.3	+4.7	-47
Warm prestress	16.5	+9.9	-99
Arrest at 1/2 wall	6.6	0	0
Arrest at 3/4 wall	10.0	+3.4	-34
Underclad flaw vs. surface flaw	10.1	+3.5	-35
Residual Stress (ksi) +20	4.1	-2.5	+25
+10	5.6	-1.0	+10
-10	8.8	+2.2	-22
-20	13.6	+7.0	-70
Increase in coolant temperature at vessel wall (°F) +10	7.6	+1.0	-10
+20	8.6	+2.0	-20
+50	11.6	+5.0	-50
+100	16.6	+10.0	-100
0.5-in. flaw vs. 1.0-in. flaw	9.9	+3.3	-33

(a) Baseline conditions:
- Rancho-Seco transient
- 0.35% Cu, High Nickel
- No warm prestress: no crack arrest
- Fluence = 1.5×10^{18} n/cm^2 per EFPY
- Initial RT_{NDT} = 20°F
- Increase in RT_{NDT} = 10°F EFPY
- Conditions for crack initiation
  Fluence = 1.0×10^{19} n/cm
  Flaw depth = 1.0 in.

Rancho-Seco transient. The equivalent change in peak inside surface RT_{NDT} at the time of the transient is also tabulated.

Effects on fracture were estimated using three alternative approaches. First, the collection of curves given in the ORNL report allowed the effects of copper, warm prestress, crack arrest, and flaw depth to be read directly from the curves. Effects of changes in coolant temperature were converted directly into an increase in RT_{NDT} on the basis of the assumed rate of increase of 10°F per EFPY at 6.6 EFPY. Finally, the effects of increasing the crack-tip stress intensity factor for a given flaw depth (due to residual stress or modeling the flaw as an underclad or surface crack) was estimated from the ASME lower-bound K_{Ic} toughness curve. Data in the ORNL report indicated that fracture was predicted for the baseline when a K_I of 90 ksi $\sqrt{in.}$ was achieved. Changes in K were first expressed as an equivalent change in RT_{NDT}, as given by the ASME reference toughness curve, and then related to EFPY.

Reductions in copper-content--from 0.35% to 0.25%--nearly doubled the predicted life of the vessel, giving about a 5-year increase in allowable EFPY.

Of the assumptions made in the analyses, the inclusion of warm prestress had the greatest impact on vessel life, giving nearly a 10-year increase in EFPY and more than doubled the predicted vessel life. Allowing flaw initiation, with the restriction that arrest occur at a crack depth of 1/2 of the vessel wall, gave no increase in vessel life. Only by allowing the arrested flaw depth to increase to 3/4 of the vessel wall was an enhancement in predicted life of about 3 EFPY attained.

An estimate of the decrease in the crack-tip stress intensity factor was made for an underclad flaw. The estimate was then compared to an estimate for a flaw of the same depth but which extended to the surface of the vessel. This is a less conservative, but perhaps more realistic, analysis of a flaw that might actually exist in a vessel. The estimate of the behavior of the true underclad flaw increased the predicted vessel life from 3.6 EFPY to about 10 EFPY. A similar increase in vessel life was estimated when flaw depth (1.0 in.) was decreased by a factor of two (to 0.5 in.).

Uncertainties in the actual level of stress in the vessel were evaluated by postulating the existence of residual stresses of ±10 ksi and ±20 ksi. In this regard, no information was available on residual stress in vessel welds. A 10 ksi stress is believed to be a realistic bound on the stress remaining after stress relief of a welded vessel.

The postulated tensile residual stresses gave a modest decrease of 1 to 2 EFPY in vessel life. Compressive residual stresses gave a more dramatic increase in predicted vessel life. In this regard one would expect that favorable levels of very high, compressive residual stress could totally cancel the peak tensile thermal stresses developed during the cooling transient. If K values during the transient were less than the K_{Ic} toughness on the lower shelf, the analyses could not predict fracture even for very high fluence levels that correspond to an unlimited period of EFPY of operation.

7.0 NONDESTRUCTIVE EVALUATION

The ability of nondestructive evaluation (NDE) to detect and characterize flaws in pressure vessel material provides an opportunity to decrease the probability of failure from a PTS event. Nondestructive evaluation techniques that were developed in Europe can be used to detect and characterize flaws near the clad surface of the vessel. In addition, the techniques can be used to assess the integrity of the inner surface of the vessel.

This chapter provides a preliminary evaluation of NDE techniques that can be used to detect underclad cracks. The evaluation will consider 1) current techniques for detecting small flaws near the clad of the vessel surface, and 2) current code requirements and the state-of-practice for near-surface flaw detection.

7.1 CURRENT NONDESTRUCTIVE EVALUATION TECHNIQUES

Currently, a specialized inspection technique developed in Germany and France is generally accepted as providing optimum detection results.[26,27,28,29] The detection technique requires that 70° refracted compressional waves be focused just beneath the clad surface of the vessel (Figure 7.1).

A preliminary evaluation of this inspection technique was made by PNL for NRC.[a] The evaluation was a "blind" test involving PNL staff members. Flaw amplitude response and crack detection measurements were made to determine an approximate signal-to-noise ratio.

The internal, "blind" test was conducted using a pressurizer dropout that contained nine actual underclad cracks generated by a thermal fatigue process. The cracks were oriented both parallel and perpendicular to the direction of the cladding. The cracks ranged in depth from 0.25 to 0.75 in. through the wall. Although none of the three operators had prior knowledge of crack location, each operator detected every crack.

Measurements of flaw amplitude response from each of the cracks (labeled A through I) in the pressurizer dropout were compared with the amplitude response from a 3-mm, flat-bottom reference reflector (Table 7.1). The flaw amplitude response was measured from two directions (180° apart) as would be done during actual field tests. Figure 7.2 shows the response of the cracks in the pressurizer dropout as a function of baseline noise measured on inspection instruments. The noise level is highest at the clad/base-metal

(a) Evaluation of the techniques was undertaken as part of an existing NRC program, Integration of Fracture Mechanics and Nondestructive Testing. The NRC technical contact is Joe Muscara.

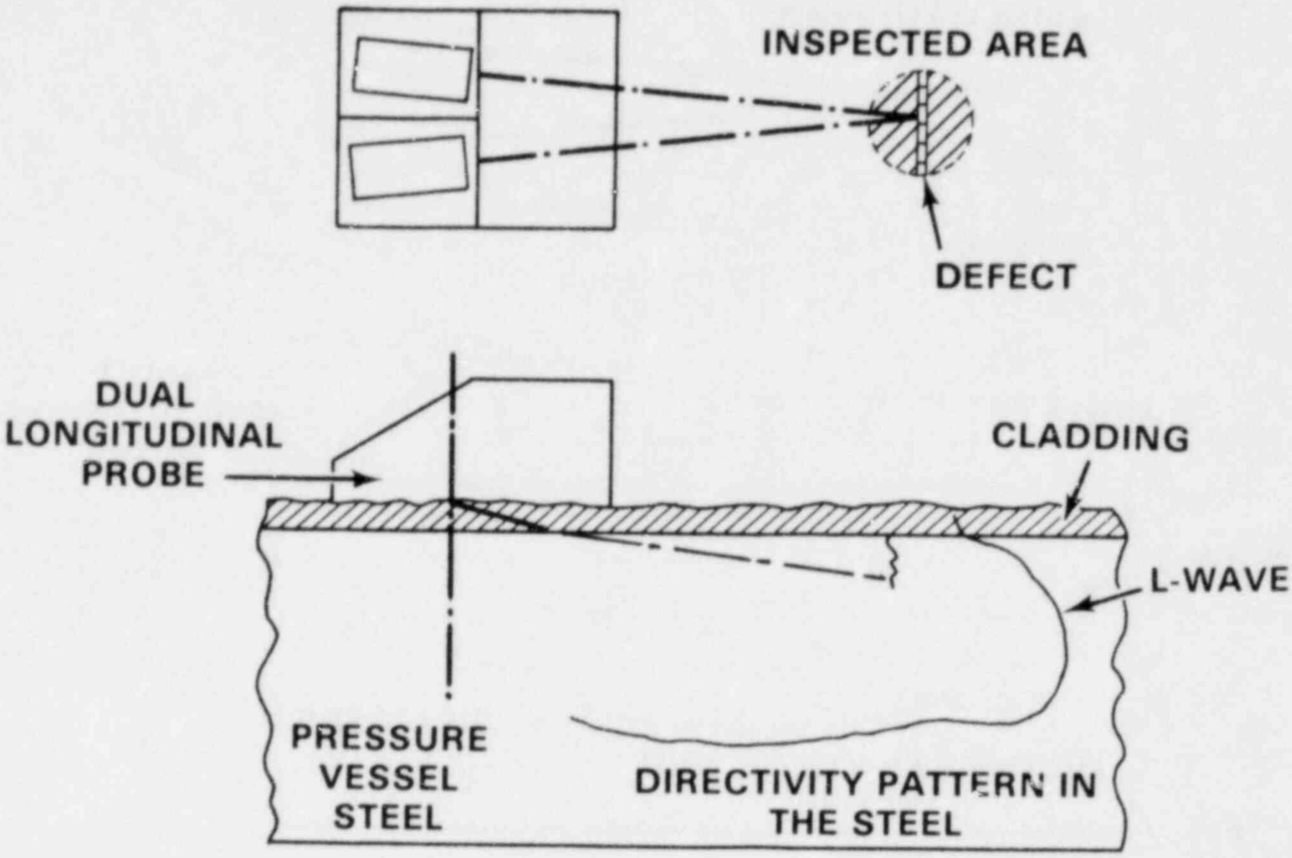

FIGURE 7.1. Dual Probe for Underclad Crack Detection

TABLE 7.1. Sensitivity Standard: 3-mm Flat-Bottom Reference Reflector

Flaw	Flaw Depth Through Wall, in.	Reference Reflector Response Direction A	Reference Reflector Response Direction B
A	0.5 in.	+3 dB	+6 dB
B	0.5 in.	+5 dB	+5 dB
C	0.25 in.	+6 dB	+10 dB
D	0.5 in.	+3 dB	+5 dB
E	0.25 in.	+14 dB	+5 dB
F	0.15 in.	+4 dB	+8 dB
G	0.5 in.	+1 dB	+2 dB
H	0.75 in.	+6 dB	+12 dB
I	0.75 in.	+9 dB	+1 dB

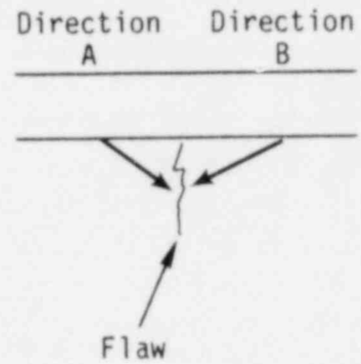

TABLE 7.2. Ranking of Flaw Detectability Based on Type of Clad, Type of Finish, and Direction of Flaw

Detectability	Flaw Direction with Respect to Clad	Finish	Clad
Very High	Perpendicular	Smooth	Strip or Multiwire
Very High	Parallel	Smooth	Strip or Multiwire
Moderate	Perpendicular	Snooth	Manual
Moderate	Perpendicular	Unground	Strip or MW
Moderate	Parallel	Unground	Strip
Moderate	Parallel	Unground	MW
Low	Parallel	Smooth	Manual
Low	Perpendicular	Unground	Manual
Low	Parallel	Unground	Manual

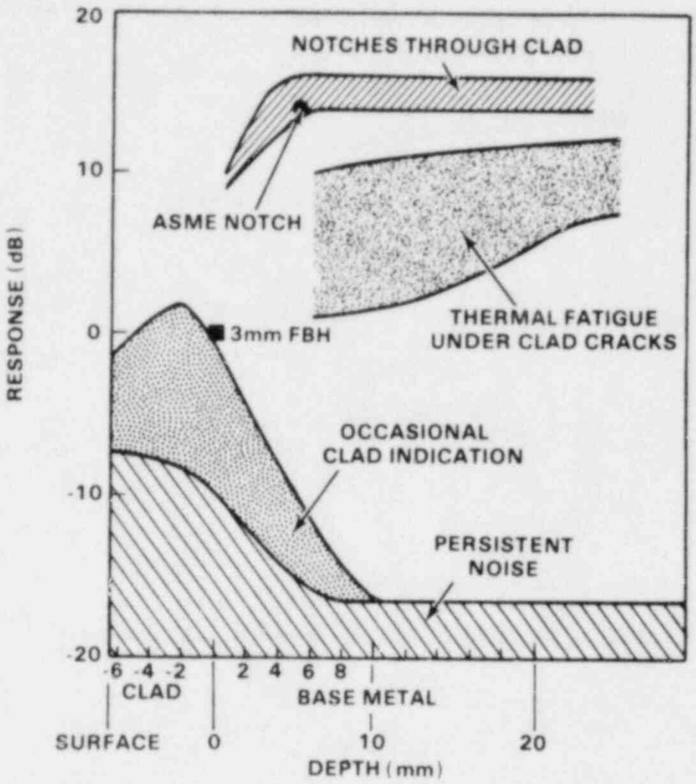

FIGURE 7.2. Flaw Response from Pressurizer Dropout

interface and decreases with depth through the wall. However, even at the clad/base-metal interface, the signal-to-noise ratio was very good.

Although the blind test and flaw amplitude data are limited, they can be used to estimate the relative detectability of underclad cracks (see Table 7.2). The probability of detecting underclad cracks is ranked in Table 7.2 with respect to: 1) the orientation of the flaw in relation to the direction of clad application, 2) the type of cladding finish used, and 3) the type of cladding process used.

7.2 CODE REQUIREMENTS AND NONDESTRUCTLVE STATE-OF-PRACTICE TECHNIQUES

The Federal Code of Regulation (10 CFR 50.55a) references ASME Section XI, which defines examination requirements for reactor vessel welds. The volume of weld that must be examined includes the inner-clad/base-metal surface. The inspection-system calibration requirements of Section XI, however, are not sensitive to near-surface cracks.

Until recently, the standard practice for preservice and in-service inspection teams was to intentionally gate out the clad and 1 in. of the base metal interface because inspection techniques generated sound interference at the clad surface during reactor vessel inspection. Regulatory Guide 1.150 is the first attempt to emphasize the importance of inspecting the clad/base metal interface. The guide, however, does not specify the size of defect that must be detected at the inner surface. As a result of the current state-of-practice techniques, and because Regulatory Guide 1.150 has only recently been applied,[(a)] there is little basis to say that underclad cracks do or do not exist in vessels.

7.3 PRELIMINARY EVALUATION OF NONDESTRUCTIVE UNDERCLAD CRACK DETECTION

Because a limited data base was used in our study, we emphasize that this is a preliminary evaluation of NDE underclad crack detection. Through our evaluation we have concluded that:

1. It is possible to detect flaws at the clad/base-metal interface using special techniques that currently are being employed in Europe and demonstrated at PNL. Our initial estimate is that the probability of detection will be very high for clad surfaces that are smooth or ground.

2. The current calibration requirements of Section XI are neither adequate nor sensitive for detecting flaws at the clad/base-metal interface.

3. Regulatory Guide 1.150 should be revised to require a demonstration of ability to detect flaws at the clad/base-metal interface.

(a) The implementation date of Regulatory Guide 1.150 for operating reactors was July 15, 1981. For plants under construction the date was January 15, 1981.

8.0 STATISTICAL ANALYSIS

The major effort of the statistical review of PTS was an evaluation of a computer code used to calculate probability of reactor failure due to PTS. However, because any code is limited by the quality of its input, some effort was made to evaluate the quantity and quality of data available for input determination.

8.1 VISA CODE EVALUATION

VISA (for Vessel Integrity Simulation Analysis) is an extension of the code used in NUREG-0778.[20] The study described in NUREG-0778 used Monte Carlo techniques to assess failures rate for PWR pressure vessels during 1) normal operation, 2) pressure/temperature transients during startup and shutdown, and 3) pressure transients during full-power operation.

The VISA code developed by Jack Strosnider of NRC[21], uses Monte Carlo simulation to estimate the failure probability of a reactor pressure vessel subject to specified PTS transients.

The VISA code extends the code used in NUREG-0778 in three major directions:

1. The temperature/pressure transient is an input, so that the code can accommodate arbitrary time histories of temperature and pressure.
2. The fracture mechanics portion of the code has been expanded to consider crack propagation and potential arrest as well as crack initiation.
3. The additional parameters (i.e., flaw size, copper content, initial RT_{NDT}, K_{Ic}, K_{Ia}, fluence and ΔRT_{NDT}) are treated as random variables.

8.2 LOGIC DESCRIPTION OF VISA CODE

The VISA code can be divided into two portions: the first carries out a deterministic fracture analysis for a specified pressure/temperature transient. The stress field and the temperature as a function of time and depth into the vessel wall are calculated. The fluence as a function of depth is calculated using an exponential attentuation model. The shift in reference temperature, ΔRT_{NDT}, is calculated following Regulatory Guide 1.99, Rev. 1. The values for K_{Ic} and K_{Ia}, (crack initiation and arrest toughnesses, respectively) are selected according to Section XI of the ASME code. This deterministic portion of the VISA code compares well with the OCA-1 code (see Section 6.7).

The Monte Carlo portion of VISA estimates failure probability of a reactor pressure vessel subjected to a specified pressure/temperature transient. As in any Monte Carlo study, the code is iterative. In a single pass through the code, values of the random variables are selected from specified probability distributions. Once these values are selected, the calculations are deterministic. Each iteration of the code results in one of three outcomes: 1) no crack initiation, 2) crack initiation followed by arrest, 3) pressure vessel failure. With a large number of iterations (~500,000) the ratio of the number of iterations resulting in failure to the total number of iterations is an estimate of the failure probability.

For each iteration of the simulation, values of fluence, flaw size, and copper content are selected from their respective distributions. The RT_{NDT} at the inner wall is calculated as a function of fluence and copper content. With these values fixed for the iteration, the code steps through the time history of the transient. For each time step, the stress intensity at the crack depth is taken from the deterministic portion of the code. A value of K_{Ic} is simulated to determine fracture initiation. If initiation does not occur, the simulation moves to the next time step. If initiation does occur, the crack is extended 1/4 in., and the crack arrest toughness (K_{Ia}) is simulated. If arrest occurs, the simulation moves to the next time step; if not, the crack is extended another 1/4 in. and a new value of K_{Ia} is simulated. This process is continued until either the vessel fails or the duration of the transient is reached.

8.3 DISCUSSION

The validity of the results of any Monte Carlo simulation depends on several general areas of concern. The first is the technical quality of the deterministic aspect of the code. If the mathematical model (conditional on all the random variables being fixed) does not accurately represent the physical situation, then random perturbation is not going to provide useful insight. In the present case, the most important mathematical models are the heat transfer and thermal stress algorithms and fracture mechanics models. Although a detailed review of the methods used in the code was not conducted, the agreement of VISA and OCA-1 can be regarded as a strong indication that the basic mathematical models are satisfactory.

A second area of concern is that the input probability distributions are reasonable representations of the uncertainty, or random scatter, of the variables being treated as random. Probability distributions can be difficult to determine because 1) few data are available, or 2) the available data may not be directly applicable. In general, a conscientious effort has been made to use the best available information to derive uncertainty distributions. Particular distributions are discussed in Sections 8.4 and 8.5.

A third requirement for validity is that the stochastic structure of the model as a system reflect physical reality. It is not sufficient for each random variable, considered by itself, to have the correct distribution. Any stochastic dependencies that exist among the random variables need to be accounted for in the simulation model.

A fourth topic that should be addressed is the suitability of the random number generator used in the simulation. Most random number generators in use are based on a multiplicative congruential algorithm. Although mulitiplicative congruential random number generators can be quite good, some constants, when used as multipliers, can introduce inadvertent stochastic structure into the simulation. The suitability of the random number generator should be evaluated in light of its intended application.

8.4 MATERIAL PROPERTIES VARIABLES

The major material properties variables that are relevant to the PTS issue are those that describe heat transfer and thermal stress and the nil-ductility reference temperature (RT_{NDT}). Since neutron embrittlement is expressed as a shift in RT_{NDT}, this variable becomes paramount in the evaluation of failure probability in terms of EFPY.

Combustion Engineering[4b] claims that the typical initial RT_{NDT} for submerged arc weldments using Linde 0091, 1092, and 123 flux is -60°F. Data on 82 submerged arc weldments are presented to support this claim. The data have a mean value of -56°F with a standard deviation of 17°F, and the claim is made that 97.5% of the population would fall below the mean plus two sigma upper limit. This analysis has a number of deficiencies. First, the data base consists of 1) weldments made using Linde 0091 and 124 fluxes, and 2) some surveillance welds and extra-low copper welds, both made with unspecified flux. The composite distribution was tested for normality using the ANSI standard D' test. The test was significant at the 0.02 level. Thus, the confidence limit statement made by Combustion Engineering is not valid. Moreover, the data appear to be skewed toward the right (i.e., toward higher values). A skewness test confirmed that the data were significantly skewed. This implies that the distribution tails off towards the high end, so that there are more high values than one would expect from a normal distribution. Finally, there is an indication of differences related to flux. A Mann-Whitney test was used to compare the welds made with the Linde 0091 flux to those made with the Linde 124 flux. The test was significant at the 0.05 level.

Combustion Engineering used the weld data in an illustrative and confirmatory manner; therefore, their incorrect statistical analysis does not affect the conservatism of their calculations. However, this particular data set provides a concise example of the dangers of a naive statistical analysis. A proper analysis must consider both the source of the data and the intended application of the results.

Regulatory Guide 1.99 provides curves relating shift in reference temperature (ΔRT_{NDT}) as a function of neutron fluence. Hanford Engineering Development Laboratory (HEDL) has also provided some less conservative curves that account for the effect of nickel and copper. The HEDL curves (see Chapter 5.0) were developed using surveillance specimen data. This work is a potential source for deriving an uncertainty distribution for use in VISA.

The nil-ductility reference temperature is given by an expression of the form

$$RT_{NDT} = RT_{NDT}(0) + \Delta RT_{NDT}$$

where ΔRT_{NDT} is a function of fluence, copper content, and nickel content. The total uncertainty in RT_{NDT} depends on uncertainty in the assumed mathematical model and on uncertainty in the variables $RT_{NDT}(0)$, fluence, copper content, and nickel content. The mathematical model above has two major aspects which affect its error structure. The first assumption is that ΔRT_{NDT} is additive and independent of initial RT_{NDT}. The second assumption is that the structural relationship between fluence, copper, and nickel has a particular form.

The uncertainty of each variable is discussed in Chapter 5.0. The model uncertainty is implicitly included as one of the components of the standard deviation of 22°F around the HEDL mean curve. However, it is not known what other components of uncertainty are included in the 22°F. Thus, although the individual uncertainties are reasonably well characterized, their interrelationship is not.

To further illustrate, the fluence uncertainty (see Chapter 5.0) is considered to be in the range of 10% to 30%. The HEDL mean curve was derived by fitting ΔRT_{NDT} observations to fluence, copper content, and nickel content. Assessment of the overall uncertainty on ΔRT_{NDT} depends on whether the values of fluence, copper, and nickel were precisely known or were subject to uncertainty as discussed in Chapter 5.0. In the latter case, some portion of the individual uncertainties is subsumed into the standard deviation around the mean; in the case of precisely known values, the individual uncertainties and the variation around the mean are separate and distinct.

8.5 FRACTURE MECHANICS VARIABLES

Crack propagation for a given stress field depends on three variables: the initial crack size, the crack initiation toughness (K_{Ic}), and the crack arrest toughness (K_{Ia}). Conservative estimates of K_{Ic} and K_{Ia} as functions of RT_{NDT} are available from Section XI of the ASME code. For the VISA code, mean curves were developed in-house by NRC.

A major barrier to the use of VISA for estimating the probability of reactor failure is the lack of data on the number and depth of near-surface, underclad cracks. The flaw distribution is taken from NUREG-0258[30], and is referred to as the OCTAVIA flaw distribution. The OCTAVIA flaw distribution was based on operational information and discussions with metallurgical personnel, and thus appears to be quite arbitrary and without a firm tie to data. Although this distribution may be useful in parametric or sensitivity studies, it is of little help in assessing a realistic probability of failure.

Until a satisfactory estimate of a flaw distribution is available, it is recommeded that VISA be used to develop estimates of the conditional failure probability given that a flaw of a specified size exists. The most likely source of data on relevant flaw sizes is NDE in-service inspection of reactor pressure vessels. There are inspection techniques that should be able to locate relevant flaws with high probability, although there is insufficient data from carefully designed and conducted trials to provide a statistically valid estimate of the probability (see Chapter 7.0).

8.6 PROBABILITIES OF REACTOR FAILURE

The VISA code has the potential for being useful in the resolution of the PTS issue. However, the results of VISA need to be interpreted circumspectly and applied with an understanding of their limitations. In addition, the estimated probability of failure is realistic only insofar as both the deterministic and only insofar as stochastic models are a faithful representation of reality, and the statistical distributions on the input variables are realistic. Finally, VISA treats the pressure/thermal transient as given. A complete evaluation of failure probability would require an estimate of the probability of the transient.

As noted in Section 8.3, the use of probabilistic calculations always requires judgment as to whether or not the stochastic elements are being correctly modeled. Judgment is also required when determining what degree of model validity is required to provide reliable, quantitative insight with respect to the failure phenomena involved. In the present case one must balance the possible over-conservatism of the bounding processes used in the deterministic calculations against the fact that a reliable stochastic model of crack initiation, growth, and arrest and the factors which govern these processes is difficult to formulate at the present state of understanding of the technical issues. While it is important to recognize this difficulty, relying on essentially deterministic calculations does not make the problem go away, it only fails to display its existence and denys even the qualitative insight gained through the modeling process. While recognizing the limitations on the quantitative validity of current calculations, efforts to construct improved models for probabilistic calculations should be continued.

In many cases, data exist which could be used to provide an estimated probability distribution for use by VISA. The present practice has been to use the data to estimate a mean and variance and then to assume a normal distribution. This practice has the potential for not sampling the tails of a non-normal distribution frequently enough. A non-normal distribution can result from aggregation of data from several sources, among other reasons. An example is discussed in Section 8.4. This problem could be overcome by using a distribution-free method (e.g., nonparametric tolerance intervals or extreme value theory) to define probability distributions.

The quality of the statistical distributions depends not only on data on individual variables, but also on the joint properties of the random variables. At this point most of the potential stochastic dependencies among the random variables in the VISA code cannot be evaluated, primarily because insufficient data are available. However, scientific judgement can be used to define variables where the likelihood of dependence is high. Some important variables that are likely to be interdependent are K_{Ic}, the crack initiation toughness, and K_{Ia}, the crack arrest toughness. The material and environmental characteristics that cause the deviation of K_{Ic} from nominal are likely also to cause the deviation of K_{Ia} from nominal in an analogous manner. Thus, it is likely that a section of material with a lower-than-nominal K_{Ic} also has a lower-than-nominal K_{Ia}. If the dependency is not allowed for in the simulation, the simulation will result in too frequent crack arrest.

Another set of variables, with a potential dependence, describe the chemical composition of the welds. The dependence could arise from several mechanisms. For example, chemical content is related to the type of welding rod used. A second mechanism that may produce stochastic dependency is the span of time over which the pressure vessels were fabricated. During this time, welding techniques evolved new types of welding rods came into use, and older ones were abandoned. Also, differences in chemical composition may be related to manufacturers.

The potential exists for answering some of these concerns through a thorough statistical review of currently available data and the methods used to collect and analyze the data. For instance, there are chemical data from representative and surveillance welds, and an analysis of this data along with a review of the history of welding techniques could resolve any potential stochastic dependence of chemical composition. In Section 8.4 it was pointed out that data reduction techniques can affect uncertainty structure. This could be resolved by a review of the source of the data used to develop a curve.

In light of these limitations, the most appropriate use of the VISA code would be as a tool for doing sensitivity analyses and for comparing pressure/thermal transients resulting from various event scenarios. Used in this mode, and coupled with reliable flaw-size information, VISA would provide a powerful tool for gaining insight into reactor pressure vessel failure due to PTS.

9.0 REFERENCES

1. Eisenhut, D. G., NRC, to Licensees. Letter dated August 21, 1981. "Pressurized Thermal Shock to Reactor Pressure Vessels." (Request for 60- and 150-day responses from Ft. Calhoun, Robinson 2, San Onofre 1, Maine Yankee, Oconee 1, Turkey Point 4, Calvert Cliffs 1, and Three Mile Island 1 Nuclear Power Plants.)

2. Licensee 60-Day Responses to NRC (see Reference 1), and Subsequent Questions Prior to 150-Day Responses:

   a. Jones, W. C., Omaha Public Power District, Fort Calhoun Station, dated October 20, 1981.

   b. Utley, E. E., Carolina Power & Light Company, H. B. Robinson Unit 2, dated October 26, 1981.

   c. Randazza, J. B., Maine Yankee Atomic Power Company, Maine Yankee, dated November 2, 1981.

   d. Novak, T. M., Duke Power Company, Oconee Nuclear Station Unit, dated October 20, 1981.

   e. Uhrig, R. E., Florida Power & Light Company, Turkey Point Unit 4, dated October 23, 1981.

   f. Poindexter, C. H., Baltimore Gas and Electric, Calvert Cliffs Nuclear Power Plant Unit 1, dated October 20, 1981.

   g. Moody, W. C., Southern California Edison Company, San Onofre Nuclear Generating Station Unit 1, dated November 4, 1981.

   h. Hukill, H. D., Metropolitan Edison Company, Three Mile Island Nuclear Station Unit 1, dated November 2, 1981.

3. Licensee 150-Responses to NRC:

   a. Jones, W. C., Omaha Public Power District, Fort Calhoun Station, dated January 18, 1982.

      Omaha Public Power District, Fort Calhoun Station, CEN-189, Appendix A, dated December 1981.

   b. Utley, E. E., Carolina Power & Light Company, H. B. Robinson Unit 2, dated January 25, 1982.

c. Randazza, J. B., Maine Yankee Atomic Power Company, Maine Yankee, dated January 21, 1981.

Maine Yankee Atomic Power Company, Maine Yankee, CEN-189, Appendix C, dated December 1981.

d. Parker, W. O., Duke Power Company, Oconee Nuclear Station Unit 1, DPC-RS-1001, dated January 1982.

Transmittal letter, Duke Power Company, Oconee Nuclear Station Unit 1, w/Attachment 1, dated January 15, 1982.

e. Uhrig, R. E., Florida Power & Light Company, Turkey Point Unit 4, dated January 21, 1982.

f. Lundvall, A. E., Baltimore Gas and Electric, Calvert Cliffs Nuclear Power Plant Unit 1, dated January 28, 1982.

Baltimore Gas and Electric, Calvert Cliffs Nuclear Power Plant Unit 1, dated January 21, 1982.

Baltimore Gas and Electric, Calvert Cliffs Nuclear Power Plant Unit 1, CEN-189, Appendix B, dated December, 1981.

g. Baskin, K. P., Southern California Edison Company, San Onofre Nuclear Generating Station Unit 1, dated January 25, 1982.

4. Owners Groups Responses to NRC Letter (see Reference 1):

a. Westinghouse Owners Group, Summary Report on Reactor Vessel Integrity for Westinghouse Operating Plants, WCAP-10019, dated December 1981.

Letter from G. S. Vissig, NRC, to Licensees Represented by Westinghouse Owners Group. "Summary of Meeting...," March 8, 1982.

b. Combustion Engineering Owners Group, CEN-189, dated December 1981.

Letter from G. S. Vissig, NRC, to Licensees Represented by Combustion Engineering Owners Group. "Summary of March 3, 1981 Meeting...," March 12, 1982.

c. Babcock & Wilcox Nuclear Power Group. December 1980. Reactor Vessel Brittle Fracture Analyses During Small Break LOCA Events with Extended Loss of Feedwater. BAW-1628. Babcock & Wilcox, Lynchburg, Virginia.

5. Block, J. A., et al. March 1982. Fluid and Thermal Mixing in a Model Cold Leg and Downcomer With Vent Valve Flow. EPRI NP-2227, Electric Power Research Institute, Palo Alto, California.

6. Block, J. A., et al. April 1982. Fluid and Thermal Mixing in a Model Cold Leg and Downcomer with Loop Flow. EPRI NP-2312, Electric Power Research Institute, Palo Alto, California.

7. Kato, H., N. Nishiwaki and M. Hirata. 1968. "On the Turbulent Heat Trans fer by Free Convection from a Vertical Plate," International Journal of Heat Mass Transfer, Vol. 11, pp. 1117-1125. Pergamon Press, New York.

8. IE Notice No. 82-17; "Overpressurization of Reactor Coolant System," June 1982. SSINS No. 6835, Division of Engineering and Quality Assurance, Office of Inspection and Enforcement, U.S. Nuclear Regulatory Commission, Washington, D.C.

9. Letter from O. D. Kingsley, Chairman Westinghouse Owners Group, to H. R. Denton, NRC. Enclosure Title: "Summary of Evaluations Related to Reactor Vessel Integrity," dated May 28, 1982.

10. U.S. Nuclear Regulatory Commission. 1975. Reactor Safety Study: An Assessment of Accident Risk in U.S. Commercial Power Plants. WASH-1400, NUREG 75-014, U.S. Nuclear Regulatory Commission, Washington, D.C.*

11. Kryter, R. C., et al. 1981. Evaluation of Pressurized Thermal Shock. NUREG/CR-2083, ORNL/TM-8072, Oak Ridge National Laboratory, Oak Ridge, Tennessee.*

12. Letter from W. J. Dirks, NRC, to NRC Commissioners. "Staff Review of ORNL Report on PTS," October 30, 1981.

13. Eckert, E. R. G. and R. M. Drake. 1972. Analysis of Heat and Mass Trans fer. McGraw-Hill Book Co., New York.

14. McElroy, W. N., A. I. Davis and R. Gold. 1981. Surveillance Dosimetry of Operating Power Plants. HEDL-SA-2546, Hanford Engineering Developmental Laboratory, Richland, Washington.

15. McElroy, W. N., et al. 1979. LWR Pressure Vessel Surveillance Dosimetry Improvement Program--1979 Annual Report. NUREG/CR-12-1, Hanford Engineering Development Laboratory, Richland, Washington.*

16. Marston, T., W. Sun and B. Chexal. 1981. "EPRI Pressurized Thermal Shock Program, Status and Review." Paper presented at the Ninth Water Reactor Safety Review Meeting, October 28, 1981, Gaithersburg, Maryland.

17. Guthrie, G. L. and W. N. McElroy. 1981. LWR Pressure Vessel Irradiation Surveillance Dosimetry, Quarterly Progress Report, October-December 1980. NUREG/CR-1241, Vol. 4, Hanford Engineering Development Laboratory, Richland, Washington.*

18. Hawthorne, J. R. 1982. Status of Knowledge of Radiation Embrittlement in USA Reactor Pressure Vessel Steels. NUREG/CR-2511, NRL Memo Rpf 4737 R5, Naval Research Laboratory, Washington, D.C.

19. Letter from R. C. Kryter, ORNL, to Distribution. "Agenda for Program Review Meeting with Steve Hanauer, Bob Bernero, Carl Johnson and Roy Woods on February 3, 1982."

20. Gamble, R. M. and J. Strosnider, Jr. 1981. An Assessment of the Failure Rate for the Beltline Region of PWR Pressure Vessels During Normal Operation and Certain Transient Conditions. NUREG-0778, U.S. Nuclear Regulatory Commission, Washington, D.C.*

21. Letter from J. Stosnider, NRC, to Distribution. "Distribution of March 5, 1982, RPV Failure Probability Study Status Report," March 24, 1982.

22. Pellini, W. S. 1963. NRL Report 5920. U.S. Naval Research Laboratory, Washington, D.C.

23. Jones, R. L., T. U. Marston, S. W. Tagart, D. M. Norris and R. E. Nickell. "Applications of Fatigue and Fracture Tolerant Design Concepts in the Nuclear Power Industry." Paper presented at the ASTM Symposium on Design or Fatigue and Fracture Resistant Structures, November 10-11, 1980, Bal Harbour, Florida.*

24. Pellini, W. S. 1976. Principals of Structural Integrity Technology. Office of Naval Research, Arlington, Virginia.

25. Letter from R. D. Cheverton, ORNL, to M. Vagins, NRC. "Parametric Analysis of Rancho Seco Overcooling Accident," March 3, 1981.

26. de Raad, J. A., G. Engl and H. Bergh. 1981. "Inside Ultrasonic Inspection of Innernozzle Radius Corners of Nuclear Pressure Vessels--Contact and Immersion Technique." Paper presented at the Fourth International Conference on NDE, May 1981, Lindau, Germany.

27. Launay, J. P., J. C. Lecomte, P. Martin and A. Thomas. 1981. "Nondestructive Evaluation of Underclad Defects." Paper presented at the Fourth International Conference on NDS, May 1981, Landau, Germany..

28. Becker, F. L. "Near Surface Crack Detection in Nuclear Pressure Vessels." Paper presented at the Fifth International Conference on NDE, May 10-13, 1982, San Diego, California.

29. Gruber, G. J. "Near Surface Detection and Sizing of Unclad Cracks in Nuclear Cracks in Nuclear Reactor Vessels by Ultrasonic Multiple-Beams Technique." Paper presented at the Fifth International Conference on NDE, May 10-13, 1982, San Diego, California.

30. Vesely, W. E., E. K. Lynn and F. F. Goldberg. 1978. The OCTAVIA Computer Code: PWR Reactor Pressure Vessel Failure Probabilities Due to Operationally Caused Pressure Transients." NUREG-0258. U.S. Nuclear Regulatory Commission Report, Washington, D.C.

* Available for purchase from the NRC/GPO Sales Program, U.S. Nuclear Regulatory Commission, Washington, DC 20555; and/or the National Technical Information Service, Springfield, VA 22161.

10.0 BIBLIOGRAPHY

Post-150-Day Response: Request for Additional Information.

a. Omaho Public Power District, Fort Calhoun Station, meeting of March 3, 1982.

b. Carolina Power & Light Company, H. B. Robinson Unit 2, meeting of February 24, 1982, dated March 16, 1982.

c. Main Yankee Atomic Power Company, Maine Yankee, meeting of March 3, 1982.

d. Memorandum from NRC to Duke Power Company, Oconee Nuclear Station Unit 1, "Forthcoming Meeting with Duke Power Company Concerning '150' Responses on the Pressurized Themal Shock Issue (PTS)," dated March 5, 1982.

   Duke Power Company, Oconee Nuclear Station Unit 1, "Meeting Summary: NRC --Duke Power Company Meeting Concerning 150-Day Response of Pressurize Thermal Shock (PTS)," March 24, 1982 dated March 29, 1982.

e. Florida Power & Light Company, Turkey Point Unit 4, dated March 16, 1982.

f. Baltimore Gas and Electric, Calvert Cliffs Nuclear Power Plant Unit 1, meeting of March 3, 1982.

g. Southern California Edison Company, San Onofre Nuclear Generating Station Unit 1, meeting of February 24, 1982

h. Westinghouse Owners Group, "Summary of Meeting with Westinghouse Owners Group, Southern Edison Company, Carolina Power & Light Company, and Florida Power & Light Company Concerning the Pressurized Thermal Shock Issue," March 8, 1982.

   Westinghouse Owners Group, meeting of February 24, 1982, dated March 16, 1982.

Memorandum from T. J. Walker, NRC, to S. S. Pawlicki. "Minutes of PWR Owner's Groups Meeting with NRC on March 31, 1981," dated April 20, 1982.

Memorandum from D. G. Eisenhut, NRC, to H. R. Denton and E. G. Case. "Preliminary Assessment of Thermal Shock to PWR Reactor Pressure Vessels," April 1, 1982.

Wiggington, D., NRC. "Summary of Meeting Held on April 29, 1981, with the PRW Owners Groups to Discuss Thermal Shock to Reactor Pressure Vessels," May 1, 1981.

Letter from R. W. Jurgensen, W Owners Group, to D. G. Eisenhut, NRC. "An Assessment of Westinghouse PWR Vessel Integrity for Severe Thermal Shock Conditions," May 14, 1981.

Letter from K. P. Baskin, C-E Owners Group, to D. G. Eisenhut, NRC. "Reactor Vessel Pressurized Thermal Shock," not dated.

Letter from J. J. Mattimoe, B&W Group, to H. Denton, NRC. "Letter Report on Reactor Vessel Brittle Fracture Concerns in B&W Operating Plants," May 12, 1981.

Letter from G. S. Vissing, NRC, to All Licensees. "Summary of Meeting with CE Owners Group on October 7, 1981 Concerning Pressurized Thermal Shock to Reactor Pressure Vessels (RPV)," October 21, 1981.

Memorandum from W. J. Dircks to NRC Commissioners. SECY-81-286A. "Pressurized Thermal Shock of Pressure Vessels," October 9, 1981.

W. J. Dircks to NRC Commissioners, "Status Report on Pressurized Thermal Shock," September 8, 1981.

Memorandum from W. J. Dircks to NRC Commissioners. SECY-81-687. "Designation of Pressurized Thermal Shock as an Unresolved Safety Issue," December 8, 1981.

Hawthorne, J. R. 1979. Survey of Postirradiation Heat Treatment as a Means to Mitigate Radiation Embrittlement of Reactor Vessel Steels. NUREG/CR-0486, NRL Report 8287, Naval Research Laboratory, Washington, D.C.*

Hawthorne, J. R. 1979. Significance of Copper, Phosphorous, and Sulfur Content to Radiation Sensitivity and Postirradiation Heat Treatment of A302-B Steel. NUREG/CR-0327, NRL Report 8624, Naval Research Laboratory, Washington, D.C.*

Letter from G. D. Whitman, ORNL, to M. Vagins, NRC. "Evaluation of HSST Intermediate Vessel Pressurized-Thermal-Shock Experiment," September 15, 1981.

Kryter, R. C., et al., ORNL. 1981. Evaluation of Pressurized Thermal Shock. NUREG/CR-2083, ORNL/TM-8072, Oak Ridge National Laboratory, Oak Ridge, Tennessee.*

Iskander, S. K., R. D. Cheveton and D. G. Ball. 1981. OCA-1, A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients. NUREG/CR-2113, ORNL/NUREG-84, Oak Ridge National Laboratory, Oak Ridge, Tennessee.*

Vesely, W. E., E. K. Lynn and F. F. Goldberg. "Reliability Problems of Reactor Pressure Components." In Proceedings of a Symposium on Application of Reliability Technology to Nuclear Plants, October 10-13, 1977, International Atomic Energy Agency, Vienna.

Letter from G. R. Mazetis, Robinson PTS Task Force, to H. L. Thompson, DHFS. "Robinson 2 Short-Term Task Force on Pressurized Thermal Shock (PTS)," April 1982.

Hoge, K. G. 1979. Evaluation of the Integrity of SEP Reactor Vessels. NUREG-0569, U.S. Nuclear Regulatory Commission, Washington, D.C.*

U.S. Nuclear Regulatory Commission. 1979. Effects of Residual Elements on Predicted Radiation Damage to Reactor Vessel Materials. Regulatory Guide 1.99, Revision 1, U.S. Nuclear Regulatory Commission, Washington, D.C.*

Nickell, R. E., D. M. Norris, S. W. Tegart, Jr. and T. U. Marston. 1981. Structural Mechanics Program: Progress in 1980. EPRI NP-1969-SR, Electric Power Research Institute, Palo Alto, California.

Jones, R. L., T. U. Marston, S. T. Oldberg and K. E. Stahlkopf. 1979. Pressure Boundary Technology Program: Progress 1974 through 1978. EPRI-NP-1103-SR, Electric Power Research Institute, Palo Alto, California.

Letter from G. S. Vissing, NRC, to All Licensees Represented by CE Owners Group. "Summary of Meeting with CE Owners Group, Omaha Public Power District, Baltimore Gas and Electric Company and Maine Yankee Atomic Power Company Concerning the PTS Issue," March 12, 1982.

---

* Available for purchase from the NRC/GPO Sales Program, U.S. Nuclear Regulatory Commission, Washington, DC 10555; and/or the National Technical Information Service, Springfield, VA 22161.

NRC FORM 335 (7-77)

U.S. NUCLEAR REGULATORY COMMISSION

BIBLIOGRAPHIC DATA SHEET

1. REPORT NUMBER (Assigned by DDC)
NUREG/CR-2837
PNL-4327

4. TITLE AND SUBTITLE (Add Volume No., if appropriate)
PNL TECHNICAL REVIEW OF PRESSURIZED THERMAL SHOCK ISSUES

2. (Leave blank)

3. RECIPIENT'S ACCESSION NO.

7. AUTHOR(S)
L.T. Pedersen, W.J. Apley, S.H. Bian, L.J. Defferding, M.H. Morgenstern, P.J. Pelto, F.P. Simonen, D.L. Stevens, T.T. Taylor,

5. DATE REPORT COMPLETED
MONTH June | YEAR 1982

DATE REPORT ISSUED
MONTH July | YEAR 1982

9. PERFORMING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code)
Pacific Northwest Laboratory
Richland, Washington 99352

6. (Leave blank)

8. (Leave blank)

12. SPONSORING ORGANIZATION NAME AND MAILING ADDRESS (Include Zip Code)
Division of Safety Technology
Office of Nuclear Reactor Regulation
U.S. Nuclear Regulatory Commission
Washington, DC 20555

10. PROJECT/TASK/WORK UNIT NO.

11. CONTRACT NO.
B 2510

13. TYPE OF REPORT
Technical Report

PERIOD COVERED (Inclusive dates)

15. SUPPLEMENTARY NOTES

14. (Leave blank)

16. ABSTRACT (200 words or less) Pacific Northwest Laboratory (PNL) was asked to develop and recommend a regulatory position that the Nuclear Regulatory Commission (NRC) should adopt regarding the ability of reactor pressure vessels to withstand the effects of pressurized thermal shock (PTS). Licensees of eight pressurized water reactors provided NRC with estimates of remaining effective full power years before corrective actions would be required to prevent an unsafe operating condition. PNL reviewed these responses and the results of supporting research and concluded that none of the eight reactors would undergo vessel failures from a PTS event before several more years of operation. Operator actions, however, were often required to terminate a PTS event before it deteriorated to the point where failure could occur. Therefore, the near-term (less than one year) recommendation is to upgrade, on a site-specific basis, operational procedures, training, and control room instrumentation. Also, uniform criteria should be developed by NRC for use during future licensee analyses. Finally, it was recommended that NRC upgrade nondestructive inspection techniques used during vessel examinations and become more involved in the evaluation of annealing requirements.

17. KEY WORDS AND DOCUMENT ANALYSIS
Pressurized Thermal Shock
Unresolved Safety Issue A-49

17a. DESCRIPTORS

17b. IDENTIFIERS/OPEN-ENDED TERMS

18. AVAILABILITY STATEMENT
Unlimited

19. SECURITY CLASS (This report)
Unclassified

21. NO. OF PAGES

20. SECURITY CLASS (This page)
Unclassified

22. PRICE
$

NRC FORM 335 (7-77)

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

OFFICIAL BUSINESS
PENALTY FOR PRIVATE USE, $300

FOURTH CLASS MAIL
POSTAGE & FEES PAID
USNRC
WASH. D.C.
PERMIT No. G 67

120555078877 1 AN9XA1121314
US NRC
ADM DIV OF TIDC
POLICY & PUBLICATIONS MGT BR
PDR NUREG COPY
LA 212
WASHINGTON DC 20555