SMUD
SACRAMENTO MUNICIPAL UTILITY DISTRICT □ 6201 S Street, Box 15830, Sacramento, California 95813; (916) 452-3211

January 16, 1981

Director of Nuclear Reactor Regulation
Attention: Mr. Robert W. Reid, Chief
Operating Reactors, Branch 4
U. S. Nuclear Regulatory Commission
Washington, D.C. 20555

Docket 50-312
Rancho Seco Nuclear Generating
Station, Unit No. 1
TMI Action Plan, NUREG-0737

Dear Mr. Reid:

On December 15, 1980, the Sacramento Municipal Utility District provided commitments for the implementation of each item in NUREG-0737, "Clarification of TMI Action Plan Requirements". Several of these commitments involved the submittal of information by January 16, 1981. This letter provides that information along with information on other items for which a submittal has been requested.

The following items listed below are provided as attachments to this letter:

Item	Title	Description of Submittal	Attachment
I.A.1.1.4	Shift Technical Advisor	Description of current STA training program and long-term program.	1
I.C.1.2 & 3	Short-term accident and procedures review	Description of Schedule for submittal of documents and implementation of guidelines.	2
II.B.3.2	Post accident sampling	Descrip of system.	3
II.B.4	Training for mitigating core damage	Description of program.	4
II.E.4.2.5	Containment Isolation Dependability	Justification of containment isolation pressure setpoint.	5
II.E.4.2.6	Containment Isolation Dependability	Description of purge valve operating restrictions.	6

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Item	Title	Description of Submittal	Attachment
II.F.2.3	Instrumentation for the detection of inadequate core cooling	Discussion of District's position.	7
II.K.2.13	Thermal-mechanical report	Report on evaluation.	8
II.K.3.2 & 7	PORV failures and opening probability	Report on evaluation.	9
II.K.3.3	Reporting SV & RV failures and challenges	District commitment.	10
II.K.3.17	ECC system outages	Report on ECCS outages.	11
III.A.2	Emergency preparedness	Discusssion of emergency response plans.	12
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Sincerely,

John J. Mattimoe

John J. Mattimoe
Assistant General Manager
and Chief Engineer

Attachments

ATTACHMENT 1

I.A.1.1.4 Shift Technical Advisor

The District has utilized the INPO Guideline, "Nuclear Power Plant Shift Technical Advisor - Recommendations for Position Description, Qualifications, Education and Training", to train the permanent STA's.

The present INPO guideline requires STA's to complete the training as stated in Section 6 and to participate as an STA in training, that is OJT, for a period of three (3) months on the plant for which the individual will perform STA duties. All District permanent STA's have completed the training required by Section 6, but will not have performed the OJT for a period of three (3) months.

Until the three (3) month OJT period is complete, the District will continue the On Call Technical Advisor (OCTA) program which was instituted during the interim STA assignments. All OCTA's are management personnel of the Rancho Seco Station and hold ucrrent SLO licenses. The duty OCTA is readily available to the Shift Supervisor or STA throug the use of a radio receiver commonly called a "beeper" and a vehicle which contains a mobile transmitter on the same frequency as the radio in the Rancho Seco Control Room.

It is the District's intent to have all STA's obtain a Senior License. The schedule for the permanent STA's, who commence their duties on 1/1/81, is to obtain a License Operator license in the fall of 1981, and a Senior License Operator license by the end of 1982. They will, therefore, enter the Rancho Seco License Operator Requalification Training Program which will ensure they retain their ability to perform STA functions.

The District will continue to utilize the INPO Guideline, "Nuclear Power Plant Shift Technical Advisor - Recommendations for Position Description, Qualifications, Education and Training", for future STA replacements. It will continue to be the District's intent to have all replacement STA's obtain a Senior License Operator license.

The District's intention is to eventually phase out the STA positions. The Shift Supervisor's training will be upgraded to include a course of study in appropriate engineering and scientific subjects equal to 60 credit hours of college level subjects. The control room design is presently being reviewed per NUREG-0660, Task 1. D. This review will be utilized to upgrade the control room man/machine interface. When both above items are complete, the STA position will be eliminated.

ATTACHMENT 2

I.C.1.2 & 3 Short Term Accident and Procedures Review

The Abnormal Transient Operating Guidelines (ATOG) Program of the B&W Owners' Group was discussed with the NRC Staff on December 16, 1980. The draft ANO-1 operator guidelines which had been provided to the Staff are considered to be representative of the guidelines which are in preparation for Rancho Seco. In order to facilitate confirmation by the Staff that all plant specific guidelines are essentially identical, the District will provide the draft Rancho Seco guidelines when available. Resolution of outstanding items related to the ATOG program will be handled on an Owners' Group basis.

ATTACHMENT 3

II.B.3.2 Postaccident Sampling

The system being designed for installation at Rancho Seco is in conformance with the clarification provided in NUREG-0737, so details are not provided here. As discussed in our letter of December 15, 1980, hydrogen and chloride analysis will not be provided.

ATTACHMENT 4

II.B.4 Training for Mitigating Core Damage

The INPO is developing training guidelines for recognizing and mitigating the consequences of core damage. The District understands that an approved training guideline will be issued soon. Upon receipt of the approved training guidelines, the District will develop a training program based on those INPO guidelines with the intent of commencing training on or about April 1, 1981.

ATTACHMENT 5

II.E.4.2.5 Containment Isolation Dependability

The staff position presented in NUREG-0737 is that the containment pressure setpoint for initiation of containment isolation for non-essential penetrations must be reduced to the minimum compatible with normal operating conditions. The restrictions on purging imposed by Item II.E.4.2.6 has created pressures in the containment higher than previously experienced at Rancho Seco. At the present time, the Rancho Seco containment is pressurized to approximately one psig. It is expected that an extended period of operation would lead to even greater pressures. The Rancho Seco Technical Specification limit for the containment pressure setpoint is four psig maximum (the set point is somewhat below this to allow for instrument drift).

The existing set point was established to provide for prompt isolation with either a loss of coolant accident or steam line break accident inside the containment. The set point provides for initiation of emergency core cooling systems, in addition to containment isolation. Lowering this set point, would lead to not only spurious isolation signals, but increased actuation of the emergency core cooling systems. We feel that the expected operating pressures of between one and two psig and the existing set point below four psig provides the minimum acceptable margin to prevent spurious actuations of these systems. Since the majority of nonessential system isolation valves are closed during reactor operation, the major effect of spurious actuation would be on the emergency core cooling system, and would achieve little improvement in containment isolation.

ATTACHMENT 6

II.E.4.2.6 Containment Isolation Dependability

The District committed to this item on December 15, 1980, by reference to an earlier letter dated January 18, 1980. This commitment was limited to maintaining the containment purge valves closed until operability criteria are satisfied. In addition, the District commits to verifying the valve to be closed at least every 31 days beginning February 1, 1981. It should be noted that this date coincides with the beginning of an outage during which valve closure will not be required.

ATTACHMENT 7

II.F.2.3 Instrumentation for the Detection of Inadequate Core Cooling

As discussed in our letter of December 15, 1980, the District's position on the addition of primary system level indication has been presented in letters dated March 5, 1980, August 28, 1980, and October 29, 1980. We still feel that instrumentation does not exist which satisfies your requirements, or that is necessary and desireable for the detection of inadequate core cooling. However, as part of our commitment to follow the development of technology in this area, the District is participating as a member of the B&W Owners' Group, in an effort to develop a design for hot leg level measurement. This effort will include the design of a narrow range hot leg level differential pressure measurement. The design developed will be a state of the art differential pressure detection system, which will achieve the best accuracy and discrimination available. We do wish to reiterate, however, that no conclusions have been made on the desirability of such a system.

ATTACHMENT 8

II.K.2.13 Thermal-Mechanical Report

The following report, "Thermal-Mechanical Report - Effect of HPI on Vessel Integrity for Small Break LOCA Event with Extended Loss of Feedwater," BAW-1648, November, 1980, was prepared on a generic basis for the B&W Owners' Group and is provided in response to this request. The report contains several conservatisms. The analysis assumes a total loss of all feedwater which is unrealistic in view of the auxiliary feedwater system operating history at Rancho Seco, and proposed improvements to this system. In addition, high pressure injection flow is assumed to be 40°F in this evaluation. The warm climate at Rancho Seco provides nominal temperatures much higher than this.

Based on the results of this evaluation, we feel that operation of Rancho Seco can continue in a safe manner. Further evaluations of the thermal-mechanical conditions will require the use of more realistic assumptions specific to Rancho Seco.

BAW-1648

November 1980

THERMAL-MECHANICAL REPORT — EFFECT OF HPI ON VESSEL INTEGRITY FOR SMALL BREAK LOCA EVENT WITH EXTENDED LOSS OF FEEDWATER

Applicable to

Babcock & Wilcox 177-Fuel Assembly
Nuclear Steam Systems

dupe of
8101050350
110pp

BABCOCK & WILCOX
Nuclear Power Group
Nuclear Power Generation Division
P. O. Box 1260
Lynchburg, Virginia 24505

Babcock & Wilcox

Babcock & Wilcox
Nuclear Power Group
Nuclear Power Generation Division
Lynchburg, Virginia

Report BAW-1648

November 1980

Thermal-Mechanical Report — Effect of HPI on Vessel Integrity for Small Break LOCA Event With Extended Loss of Feedwater

Key Words: Brittle Fracture, Small Break, Reactor Vessel Downcomer, High Pressure Injection

ABSTRACT

This report has been prepared to address issues raised in a letter from D. F. Ross of the U. S. Nuclear Regulatory Commission to J. H. Taylor of Babcock & Wilcox. The letter, dated July 12, 1979, is entitled "Information Request on Reactor Vessel Brittle Fracture." The investigation reported herein addresses the possibility of exceeding the fracture mechanics acceptance criteria of the reactor vessel in a nuclear steam system caused by excessive cooling by high-pressure injection flow (without reactor coolant loop flow) during small breaks (or total loss of feedwater events where the operator opens the power-operated relief valve) where the reactor coolant pressure is kept relatively high owing to choked flow out the small break (or open PORV).

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1. INTRODUCTION AND DISCUSSION

The investigations described in this report were performed to evalu e the concern of brittle fracture during recovery from a small LOCA with extended loss of feedwater in response to NRC's information request dated July 12, 1979.[2] This report describes the analyses performed and the results obtained.

1.1. The Brittle Fracture Concern

The concern associated with brittle fracture during a small LOCA with the required assumption of extended loss of feedwater can best be understood through the use of simple schematics of the system. Figure 1-1 shows the reactor coolant flows within the reactor coolant system during normal operation.

Figure 1-2 shows the system flows a few minutes after a small break typical of a stuck-open power-operated relief valve (PORV). During this phase, the reactor coolant system (RCS) pressure and temperature are dropping and RCS flow is decreasing as a result of an operator requirement to trip the RC pumps on ESFAS actuation. The temperature in the RCS is higher than in the secondary side of the steam generator; thus, the generators assist in removing heat and circulating the RCS coolant. Assuming no feedwater is available, the RCS must be cooled by injecting coolant from the high-pressure injection (HPI) system. The warm RCS loop water is well mixed with the cold HPI flow as it passes the HPI nozzle; thus, relatively warm water enters the vessel and downcomer.

If the transient proceeds unhindered, RCS temperature and pressure continue to fall and steam voids will form in the system. The rate of temperature and pressure decrease and the volume of voids is primarily a function of the break size. At this point, natural circulation loop flow can cease. For the larger small breaks assuming an extended loss of feedwater and no forced RC flow, loss of natural circulation could occur in 8 to 15 minutes and is partly the result of voids at the top of the hot leg and partly the result of heat removal capability by the steam generators. Loss of steam generator heat removal occurs when the RCS temperature falls below the secondary system temperature.

When loop flow ceases, the cold HPI water will begin to cool the cold leg and subsequently flow into the reactor vessel (RV) downcomer. There it will mix with the warm vent valve return flow as shown in Figure 1-3. The system will stay in this condition, with the downcomer fluid temperature gradually decreasing as decay heat decreases, until reactor coolant loop flow is initiated. Reactor coolant pumps are normally restarted once 50F subcooled conditions are re-established around the entire loop. No credit is taken for this in the analysis. Instead, it is assumed that reactor coolant pumps are not started. If RC pressures remain high enough and there is insufficient vent valve flow or mixing, the RV wall temperatures may decrease to the point where cracks in the RV could initiate if flaws exist in the RV metal.

The potential for brittle fracture of the reactor vessel is dependent upon RV material properties, flaw size from which the brittle fracture initiates, temperature, and stress. The main components of stress are usually the RC pressure and vessel thermal gradients due to cooling. Transients that exhibit high vessel stress at a low RV temperature must be evaluated to ensure that the fracture mechanics acceptance criteria will not be violated during the transient considering the vessel irradiated material properties and postulated flaw sizes.

1.2. Investigations

The analyses presented in this report can be divided into four areas:

1. LOCA analysis.
2. Reactor vessel downcomer mixing.
3. Reactor vessel cooldown analysis.
4. Linear elastic fracture mechanics.

The LOCA analyses provided all the information necessary for performing the linear elastic fracture mechanics (LEFM) analyses except the RV downcomer temperature next to the RV wall and the RV wall temperature versus time. The LOCA analyses determined the RV downcomer temperature assuming complete mixing of the HPI and vent valve flows entering the downcomer. The extent to which mixing occurs is uncertain; therefore, various RV downcomer mixing calculations were made using different assumptions. Next, temperature gradients in the RV versus time are determined.

Finally, LEFM analyses were conducted for different LOCA events assuming different amounts of downcomer mixing. The analyses performed are summarized below.

The combination of LOCA, mixing, vessel cooldown, and LEFM analyses which will be emphasized in this report are summarized as cases 1 through 4 in Table 1-1.

1.2.1. LOCA Analyses

Several LOCA analyses were performed. Three breaks (0.007, 0.015, and 0.023 ft^2) were analyzed assuming no feedwater to the steam generators. They were located at the top of the pressurizer. The analyses were performed both with and without operator action to throttle back the HPI flow. For the three cases with operator action (cases 2-4, Table 1-1), the assumed action was to reduce HPI flow when the core outlet temperature reached 100F subcooled and then maintain approximately 100F subcooling at the core outlet. The primary purpose of the LOCA analyses was to determine the HPI flow rate, vent valve flow rate and temperature, RCS pressure, and RV downcomer temperature. The 0.007-ft^2 pressurizer break with no operator action (case 1, Table 1-1) was analyzed in detail using the CRAFT computer code[1] for 10 hours real time in response to the NRC-requested analysis.[2] The other LOCA analyses used the CRAFT code only during the blowdown stage of the transient. After the RCS refilled with water a steady-state analysis was performed to determine the reactor vessel conditions. The steady-state analytical method was benchmarked against the CRAFT analysis for the 0.007-ft^2 pressurizer break with no operator action. The LOCA analyses are described in detail in section 2 of this report.

1.2.2. RV Downcomer Temperature Evaluation

Fracture mechanics analyses require calculation of the RV wall temperature, which in turn depends on the downcomer fluid temperature. It is expected that there will be significant mixing, both in the cold leg piping in the area of HPI injection and in the downcomer, but because of the complex geometry of the downcomer region, quantifying this effect represents the principal uncertainty in the investigation. For this reason, very conservative bounding calculations were also performed. The analyses performed to evaluate potential mixing are discussed in section 3.

1.2.3. RV Cooldown Analyses

Once the downcomer fluid temperature at the vessel wall is determined, the temperature gradients versus time through the wall must be determined. The reactor vessel cooldown analyses are described in detail in section 4.

1.2.4. Linear Elastic Fracture Mechanics Analyses

Linear elastic fracture mechanics analyses were performed for each of the break cases and for a range of mixing assumptions. These analyses are described in section 5.

1.3. Assumptions and Conservatisms

Assumptions are used throughout this report as necessary to address the items contained in the Staff's July 12, 1979 information request. Three fundamental assumptions which have been made to address the requested information are (1) an extended loss of all feedwater, (2) subsequent extended loss of both forced and natural circulation, and (3) combining worst case plant parameters in order to perform a generic analysis which conservatively envelops the operating B&W plants.

Since the issuance of the Staff request, programs have been undertaken or completed which significantly reduce the potential of these situations occurring. Extensive upgrades underway to increase the reliability of the emergency feedwater systems decrease the probability of ever experiencing an extended loss of feedwater and the need to cool the core via the HPI system.

In addition, as mentioned above, current instructions to plant operators call for restarting reactor coolant pumps once 50F subcooled fluid conditions are re-established throughout the system. This is also ignored in the analysis.

The main conservatisms and assumptions used throughout this report are summarized below.

1. All feedwater is lost for an extended period of time.
2. All reactor coolant pump forced flow is lost for an extended period of time.
3. Core flow into the downcomer is assumed to pass through four vent valves rather than the eight valves existing on all but one plant. This reduces the amount of warm water entering the downcomer.

4. A hypothetical maximum HPI flow capacity is assumed over the entire RCS pressure range analyzed. No single plant can achieve this hypothetical capacity over the entire pressure range.

   This assumption affects all the analyses, including those which assume operator action to throttle HPI, since the initial reactor vessel cool-down prior to achieving 100F subcooling at the core outlet is maximized, resulting in increased thermal stresses during the transient.

5. The MIX2 mixing analyses (section 3) assume little HPI-vent valve flow mixing in the downcomer. The HPI flow was assumed to enter the downcomer and essentially stream down the RV wall and mix with the vent valve flow, which is assumed to be circumferentially distributed.

6. In addition to HPI flow mixing with the hot water coming from the vent valves, several other mechanisms are available for heating the HPI flow

   a. Upstream mixing in the cold leg piping.
   b. Heating by the reactor vessel walls.
   c. HPI pump energy.
   d. Heating by the cold leg piping.

   These effects, however, were conservatively ignored in all the analyses. The heat available from items a and b above is expected to be significant. The hotter fluid from the vent valves is expected to travel into the cold leg piping beyond the 2 feet which were modeled. The vent valve flow will mix with and heat the HPI fluid in the cold leg before it enters the downcomer. This is the gravity effect discussed in section 3. The reactor vessel wall also will provide heat to the downcomer fluid. However, a more important feature of this heating is the inherent tendency to reduce the wall to fluid heat transfer. This is because the fluid next to the vessel wall is heated up locally. The buoyancy force due to the density gradient tends to oppose the downward flow and as a result, the velocity of the fluid near the vessel wall could be slowed or even reversed. This mixed convection phenomenon would tend to reduce heat transfer from the vessel wall into the fluid and maintain the vessel wall temperature higher than predicted by these calculations.

7. A worst-case HPI fluid temperature of 40F is assumed.

8. Linear elastic fracture mechanics (LEFM) methods were used in the brittle fracture analysis. No credit was taken for warm prestressing.

Table 1-1. Analysis Summary - Cases 1 Through 4

	Case 1	Case 2	Case 3	Case 4
LOCA analysis	0.007-ft^2 pressurizer break, no HPI throttling	0.007-ft^2 pressurizer break with HPI throttling	0.023-ft^3 pressurizer break with HPI throttling	0.023-ft^2 pressurizer break with HPI throttling
Mixing analysis	Complete, perfect mixing (CRAFT)	Distributed vent valve flow, streaming HPI flow (MIX2)	Distributed vent valve flow, streaming HPI flow (MIX2)	No mixing
Reactor vessel cooldown analysis	Constant heat transfer (BEFRAM)	More detailed analysis (see section 4)	More detailed analysis (see section 4)	More detailed analysis (see section 4)
LEFM (fracture analysis)	LEFM at 6 EFPY	LEFM at 3.8 EFPY	LEFM at 3.8 and 4.8 EFPY	LEFM at 3.8 EFPY

Figure 1-1. RCS Flow During Normal System Operation

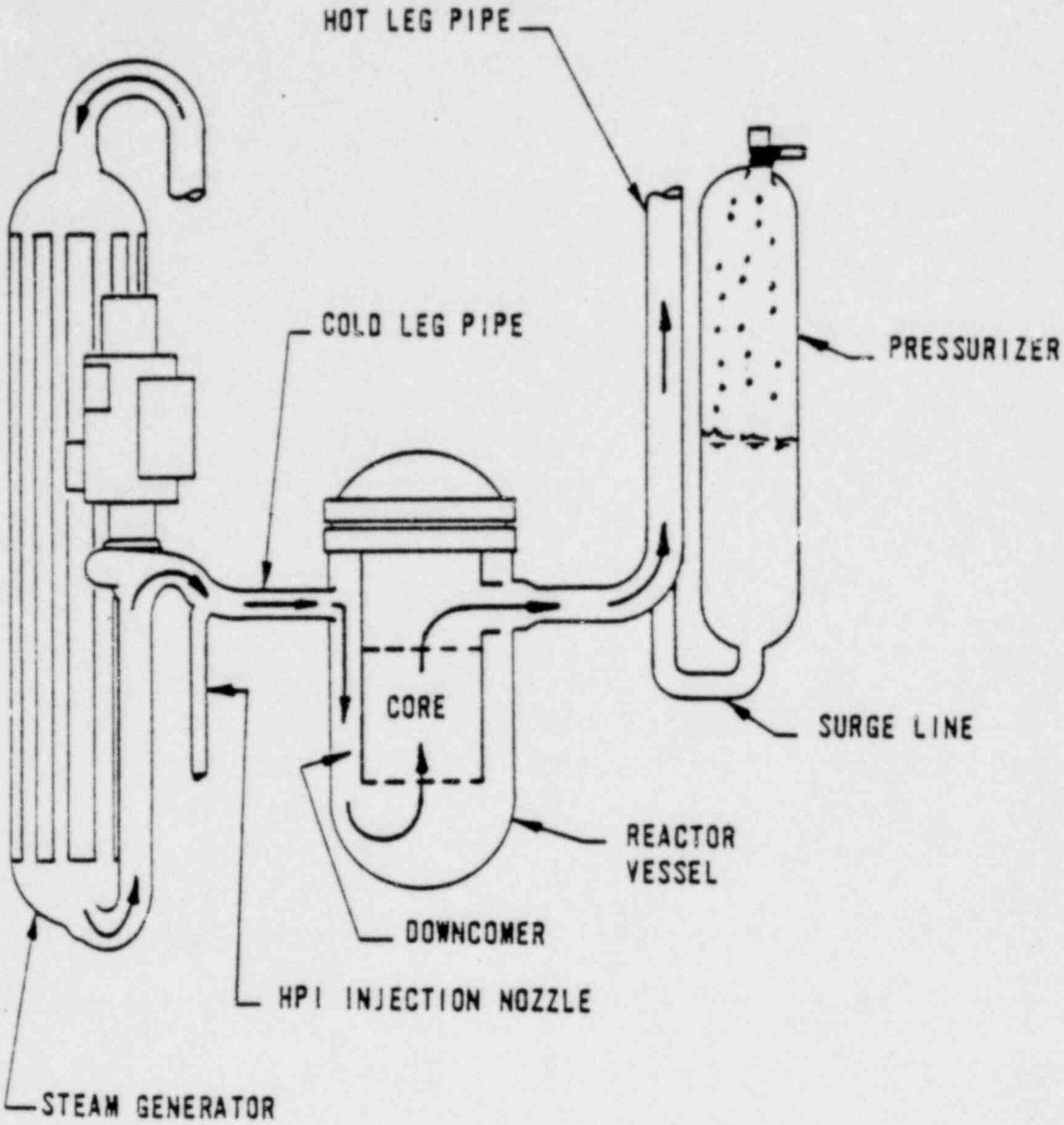


Figure 1-2. RCS Flow During Total Loss of Feedwater Event With a Small Break

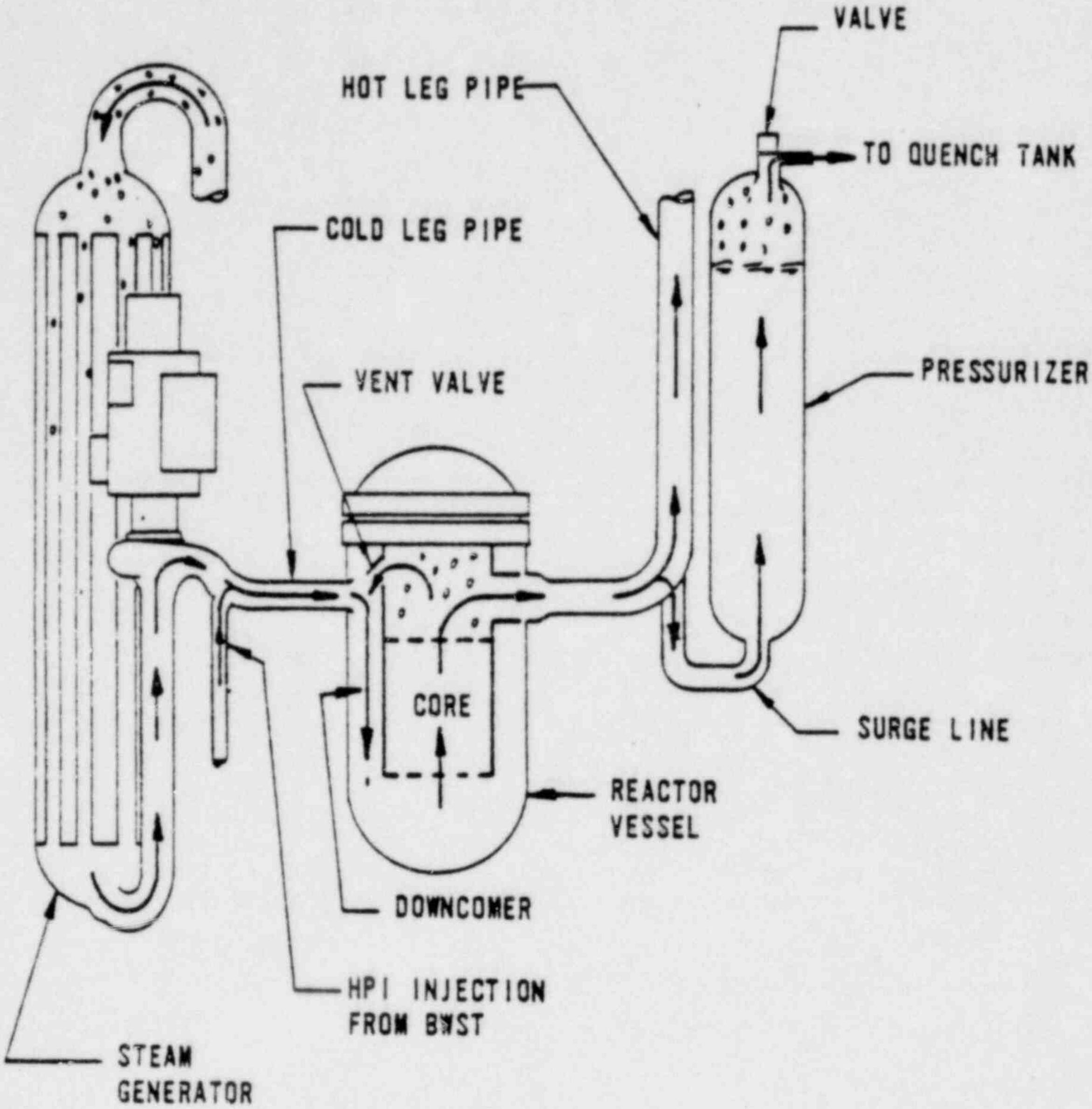
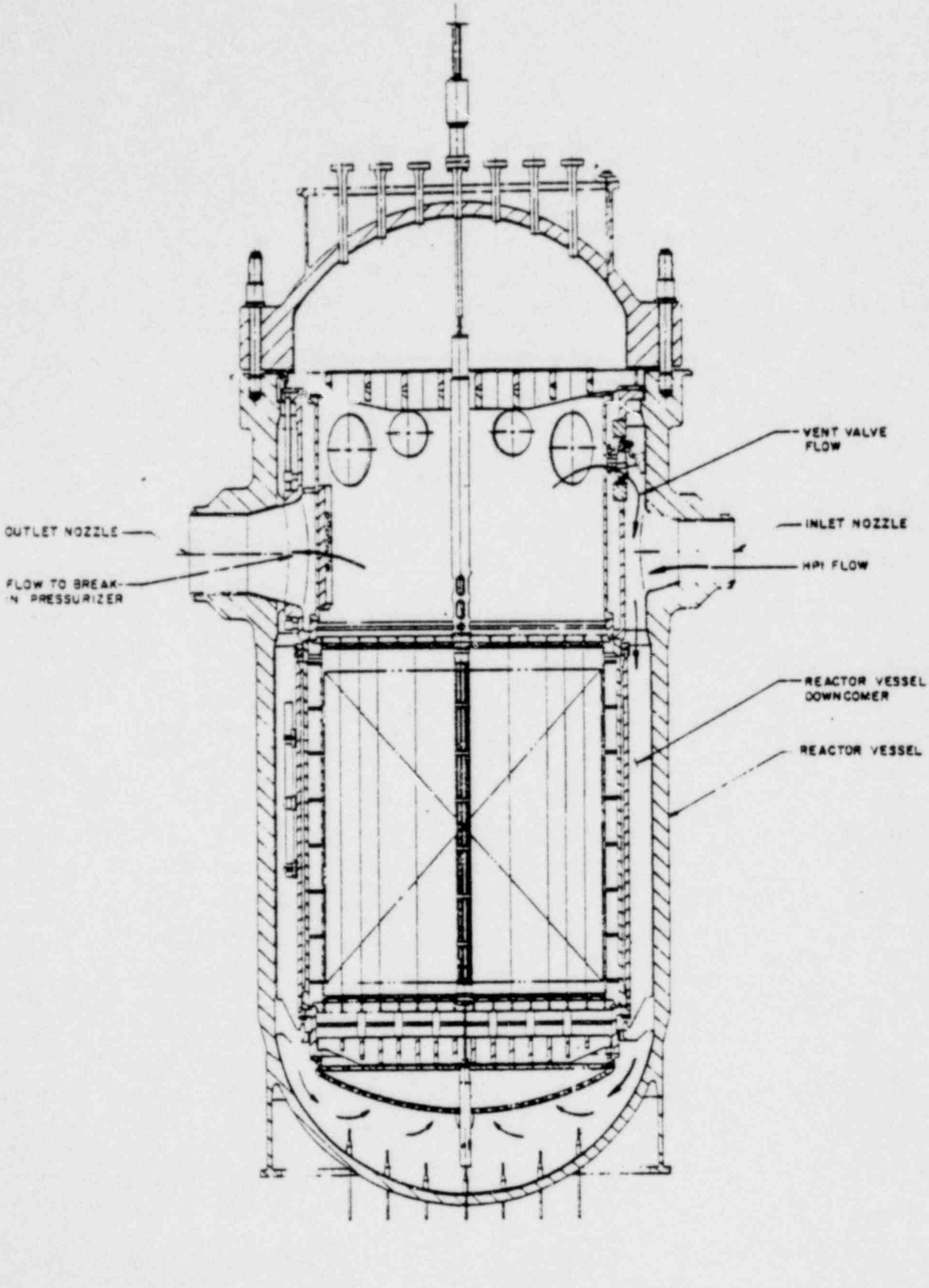


Figure 1-3. Reactor Vessel Flow During Small Break



2. SMALL BREAK ANALYSIS

In order to bound the brittle fracture concern, a number of evaluations and small-break LOCA analyses were performed; these included (1) evaluating worst-case inputs for the analyses, (2) running one break size out 10 hours, (3) analyzing a spectrum of breaks using worst-case HPI flow to help define the worst-case break size, and (4) analyzing the spectrum of breaks assuming operator action to throttle HPI based on subcooling at the core outlet.

2.1. Evaluation of Worst-Case Parameters

The most limiting transient is considered to be the one that produces a system pressure coming closest to the maximum allowable pressure as determined by a linear elastic fracture mechanics analysis (LEFM). The maximum allowable pressure is a function of three parameters — temperature, rate of temperature change, and material properties. Of these three parameters, only temperature and rate of temperature change are affected by the transient variables of HPI flow, break location, and break size. Therefore, an investigation was undertaken to define the worst-case parameters (HPI flow, break location, and break size) for use in ECCS/brittle fracture analyses. The results of the investigations are provided below.

2.1.1. HPI Flow Effect

HPI flow rate has a significant impact on RCS pressure and downcomer temperature. When HPI flow increases, the RV downcomer water temperature decreases and the RCS pressure increases. Both of these changes increase the potential for reactor vessel brittle failure. Therefore, the worst condition from the standpoint of brittle fracture mechanics is the condition of maximum HPI flow into the RCS. Except where operator action is explicitly modeled, the analyses assumed the maximum HPI system flow allowed by the piping configuration with three HPI pumps operating at pressures above ~1500 psig and the flow from two HPI pumps and two makeup pumps (as on Davis-Besse) for RCS pressures below ~1500 psig (see Figure 2-1 for the pump head curves used in the analyses).

This means that the unthrottled analysis (case 1, Table 1-1) encompasses the maximum HPI flow injection capability of all 177-FA plants. This also means that for the throttled analyses (cases 2-4, Table 1-1) the initial cooldown prior to achieving 100F subcooling at the core outlet and beginning to throttle HPI is maximized. This is conservative with respect to the thermal shock concern.

2.1.2. Break Location Effect

A small unmitigated LOCA with prolonged total loss of feedwater and RC pump trip will eventually result in a loss of primary loop circulation. Under this condition, mixing of cold HPI water with the water in the RV downcomer is primarily dependent on the capability of the vent valves to provide circulation of hot water into the downcomer.

For cold leg breaks, the hot water leaving the core flows (1) through the hot leg, steam generator, and broken cold leg to the break, and (2) through the vent valve, downcomer, and broken cold leg to the break. The latter path has the least flow resistance and thus allows a large portion of the hot water to enter the downcomer for mixing. Furthermore, the diversion of HPI water to the break reduces the total amount of HPI water entering the downcomer. For hot leg or pressurizer breaks, more HPI water is available to enter the downcomer. In addition, less vent valve flow occurs in a hot leg or pressurizer break, thus decreasing the amount of hot water available for downcomer mixing. As a result, the most severe downcomer conditions will result from a break in the hot leg or pressurizer. The analyses that follow use a pressurizer break to evaluate system conditions.

2.1.3. Break Size Effect

Because of the combination of parameters that influence brittle fracture susceptibility, the worst break size cannot be determined a priori. As the break size increases, the downcomer temperature decreases due to a lower system pressure and increased HPI flow. However, the lower system pressure tends to offset the effect of the lower temperature. In addition, the initial cooldown rate increases as the break size increases. Because these effects tend to offset each other, a spectrum of breaks was analyzed to show the effect of the break size. Three pressurizer break sizes — 0.007, 0.015, and 0.023 ft^2 — were analyzed. The 0.007-ft^2 corresponds to the PORV orifice area, the

0.023-ft^2 break corresponds to that of the safety valve, and 0.015-ft^2 is an intermediate-size break. The bounding break sizes were chosen by the following logic:

1. The 0.007-ft^2 break was the smallest break size considered for this investigation because the operator is instructed by procedures to open the PORV during a loss-of-feedwater event.
2. Break sizes larger than 0.023 ft^2 result in more rapid depressurization to pressures at which the LPI system provides makeup (with little or no repressurization). The transient response of these larger breaks is similar to that of the large break LOCA being considered under NRC Task Action Plan A-11. Therefore, 0.023-ft^2 is the largest break size that was considered in this investigation.

While the discussion above indicates that break sizes can be adequately bounded, the worst break with respect to thermal shock cannot be defined a priori because of the interation of HPI flow, pressure, and temperature on the brittle failure concern. Therefore, the PORV case was chosen for the detailed 10-hour CRAFT run since it represents the most probable event.

2.2. LOCA Analyses Without Operator Action to Throttle HPI Flow

2.2.1. 0.007-ft^2 Pressurizer Break (Case 1, Table 1-1)

A 10-hour CRAFT analysis was conducted to determine the system response for an extended total-loss-of-feedwater accident.[1] The break size chosen was 0.007 ft^2 at the top of the pressurizer. This corresponds to an open PORV, which is the most likely small break to accompany an assumed total-loss-of-feedwater event.

2.2.1.1. Model Development

In response to the NRC request to provide an analysis of the thermal-mechanical conditions in the vessel for 10 hours, an eight-node CRAFT model, as shown in Figure 2-2, was developed to determine the thermal-hydraulic conditions in the vessel.[2] This model was compared with the 22-node model developed for the small break LOCA analysis. A comparison of the results from both models (as shown in Figures 2-3 and 2-4) shows that the simplified model can adequately predict the thermal-hydraulic conditions in the vessel. A sudden drop in the downcomer temperature is caused by the initiation of HPI. The system noding is described below.

Node 1: Reactor vessel downcomer and lower plenum.

Node 2: Reactor core and upper plenum.

Node 3: Cold legs between RC pumps and reactor vessel.

Node 4: Hot legs.

Node 5: Primary side of steam generators and cold legs between steam generators and RC pumps.

Node 6: Secondary side of steam generators.

Node 7: Pressurizer.

Node 8: Containment.

The CRAFT code assumes homogeneous mixing of the liquids in a node and determines its thermodynamic conditions based on the thermal equilibrium between the steam and liquid phases. This assumption will result in complete mixing of the cold and hot fluids entering the downcomer region from the cold legs and vent valves, so the downcomer node temperatures calculated by CRAFT are mixed mean temperatures.

2.2.1.2. Assumptions Used for 0.007-ft^2 Break

The 0.007-ft^2 pressurizer break size (PORV throat area), without operator action to throttle HPI flow, was chosen for the 10-hour CRAFT analysis since operator guidelines call for opening the PORV and HPI injection if all feedwater is lost. The following key assumptions were made in this analysis:

1. Reactor and RC pumps trip at time zero. Mixing of cold HPI water with the hot fluid in the RCS is minimized when no flow circulation around the primary loop is assumed, i.e., RC pump trip and loss of steam generator heat removal capability.

2. Loss of main and emergency feedwater is assumed to occur simultaneously at time zero.

3. PORV is opened at 20 minutes by operator action.

4. HPI is actuated at 20 minutes — HPI system flow without operator action is assumed as described in section 2.1.1. This HPI flow and the coldest BWST temperature (40F) will promote a colder downcomer temperature.

5. Four vent valves are modeled. The vent valve flow enhances mixing. The most severe case would be no vent valve flow since this produces lower downcomer temperatures. However, the system is self-compensating with

respect to vent valve flow; decreasing the vent valve flow promotes a higher ΔP across the vent valve, which will increase flow through the valve. Four vent valves were modeled although all plants except Davis-Besse have eight.

6. A Moody discharge model with a discharge coefficient of 0.75 was used to calculate flow through the open PORV. This valve is based on the normalization of the Moody choked flow to the design steam flow through the PORV.
7. Initial operating power level is 102% of 2772 MWt.
8. Decay heat is based on 1.2 times the ANS standard.

Modeling of the piping and quench tank downstream of the PORV was treated as a part of the containment since flow choking always occurs at the PORV.

For purposes of this analysis, the HPI has been injected directly into the downcomer at the inlet elevation. HPI flow into the cold leg pipe volume (node 3) between the reactor vessel and the RC pumps will reduce the node temperature below the lower bound of the steam table used in CRAFT. In order to avoid this difficulty, the HPI flow was injected directly into the downcomer.

The vent valve flow area is based on four vent valves in a fully open position. As the differential pressure across the vent valve falls below 0.25 psi, the valve opening angle decreases. The flow reduction due to the partial opening of the vent valve is accomplished by increasing the flow resistance in accordance with the ΔP across the vent valve as shown in Table 2-2.

2.2.1.3. PORV Relief Line Choking Evaluation

An evaluation was performed to determine whether choking flow ever occurs downstream of the PORV. If choked flow occurs in the downstream piping, mass accumulation and pressure buildup in the pipe will result. This may create an unchoking condition of the PORV. The upper and lower boundary conditions in the pressurizer were used to examine the flow characteristics in the downstream piping to demonstrate that downstream choking will not occur. The calculations are provided below.

1. Upper Bound Condition

The Moody choked flow through the PORV is calculated for an upstream condition of P_o = 2500 psia and h_o - 731.7 Btu/lbm as follows:

Mass flux $G_{Moody} = 11,369\ lbm/ft^2\text{-}s$,

Throat pressure $P_t = 1500$ psia,

Exit quality $x = 15\%$,

Flow through PORV $W_{porv} = AxG_{Moody} = 0.007 \times 11,369 = 97.6\ lbm/s$.

Using the throat pressure and the exit quality, the enthalpy of the mixture is 695.4 Btu/lbm. The choked flow in the downstream pipe is calculated using the PORV exit condition, i.e., $P_o = 1500$ psia and $h_o = 695.4$ Btu/lbm:

Mass flux $G_{Moody} = 7543\ lbm/s\text{-}ft^2$

Throat pressure $P_t = 885$ psia,

Flow rate $W_{pipe} = AxG_{Moody} = 0.051 \times 7543 = 384.7\ lbm/s$.

2. Lower Bound Condition

With pressurizer pressure $P_o = 1400$ psia and enthalpy $h_o = 598.8$ Btu/lbm, the choked flows through the PORV and the downstream pipe are determined similarly:

$W_{porv} = 0.007 \times 9064 = 63.4\ lbm/s$,

$P_t = 830$ psia,

$x = 5\%$,

$h_o = 549.2$ Btu/lbm.

The choked flow in the downstream pipe for $P = 280$ psia and $h = 549.2$ Btu/lbm is

$W_{pipe} = 0.051 \times 6383.1 = 325\ lbm/s$.

The Moody discharge model was used to calculate steam and saturated water flow through the PORV. The orifice equation was used to calculate subcooled water flow. The pressure in the quench tank was assumed to reach equilibrium with the containment within 20 minutes; the maximum pressure drop between the PORV and the quench tank will be 200 psi.

The calculations above indicate that the flow in the downstream pipe is always greater than that through the PORV. Therefore, choked flow will occur only through the PORV during the transient.

2.2.1.4. Results

The sequence of events for the 0.007-ft^2 case is tabulated in Table 2-1. The main events can be summarized as follows:

Sequence of events	Time, s
• Reactor trip, RC pump trip, turbine trip, and loss of all feedwater.	0.0
• Secondary side boils dry.	420.0
• RCS repressurizes and exceeds safety valve setpoint pressure of 2515 psia, and safety valves open.	780.0
• PORV is open and HPI is initiated (operator action).	1,201.0
• Loop flow essentially stops.	2,300.0
• HPI flow matches leak flow, and system reaches a subcooled state at approximately 1500 psia.	5,300.0
• End of analysis.	36,000.0

Following reactor trip, the steam generator provided sufficient cooling and the system depressurized. The system repressurized and exceeded the safety valve setpoint pressure after steam generator cooling was lost at 420 seconds. The loop flow continued until approximately 2300 seconds into the transient. Loop flow was maintained because both the hot and cold legs were filled with water during this period and the density gradient between the cold and hot legs was enough to maintain the loop circulation. Figures 2-5 through 2-8 show the liquid levels in cold legs, hot legs, pressurizer, and reactor vessel. The fluid temperature plots, shown in Figures 2-9 through 2-11, indicate that the primary system reached a subcooled state at approximately 5300 seconds. The flow rates and qualities as a function of time for the core exit, PORV, vent valve, and HPI are provided in Figures 2-12 through 2-18. Limited steam flows were observed during the early part of the transient. The water inventory in the primary system is presented in Figure 2-19, and the pressure in the core as a function of time is shown in Figure 2-20. The system pressure stabilized at 1500 psia. The vent valve flow continued for the entire 10 hours. Pressurizer fluid/metal temperature, upper head metal temperature, and cold leg water temperature as a function of time are presented in Figures 2-21 through 2-23.

2.2.1.5. Conservatisms

1. Because this analysis is generic, maximum achievable HPI flow was considered. As indicated in section 2.2, this involved the combination of two different HPI systems.
2. No operator action was assumed to reduce the HPI flow.
3. HPI flow was assumed to be directly and totally injected into the downcomer. If the actual piping configuration were modeled whereby the HPI is directed into the cold leg pipe, a fraction of the total HPI flow injected tends to flow backward through the steam generator resulting in less HPI flow into the downcomer and a less severe temperature degradation.
4. Four vent valves are also used in the analysis to envelop the Davis-Besse raised-loop plant. The lowered-loop plant with eight vent valves will have vent valve flow equal to or greater than that of the raised-loop plant. One of the key factors affecting downcomer temperature is the amount of hot water flowing through the vent valves. Greater vent valve flow results in a warmer downcomer temperature.

2.2.2. 0.015- and 0.023-ft^2 Pressurizer Breaks

In addition to the 0.007-ft^2 pressurizer break, 0.015- and 0.023-ft^2 breaks were analyzed to determine the effect of break size on the reactor vessel downcomer temperature and system pressure. These additional analyses were not as comprehensive as the 0.007-ft^2 analysis. They used simpler calculational methods and only determined the RV conditions.

2.2.2.1. Analytical Method

The eight-node CRAFT model described in section 3.1 was used for the initial blowdown analysis. The 0.015- and 0.023-ft^2 breaks were run for 20 and 45 minutes, respectively. The subsequent transient calculations, performed using a steady-state code, are provided below.

Under the steady-state assumption, the rate of change of mass (M_{RCS}) and energy (E_{RCS}) in the RCS is zero:

$$\frac{dM_{RCS}}{dt} = \frac{dE_{RCS}}{dt} = 0. \tag{1}$$

Then the core outlet enthalpy is calculated:

$$h_h = \frac{q}{W_{HPI}} + h_{HPI} \tag{2}$$

where

h_h = core outlet enthalpy, Btu/lbm,
h_{HPI} = enthalpy of HPI water, Btu/lbm,
W_{HPI} = HPI flow, lbm/s,
q = decay heat, Btu/s.

The vent valve flow is determined by performing an energy balance in the downcomer region:

$$W_{vv} = W_{HPI} \frac{h_c - h_{HPI}}{h_h - h_c} \tag{3}$$

where

h_c = core inlet enthalpy, Btu/lbm,
W_{vv} = vent valve flow, lbm/s.

The vent valve flow, which can also be calculated from the elevation pressure drop across the vent valve, is given by

$$W_{vv} = \frac{288 \times g_c \rho_h A_{vv}^2 \Delta P}{K_{vv}} = 96.26\ A_{vv} \frac{\rho_h \times \Delta P}{K_{vv}}\ . \tag{4}$$

where

ρ_h = core outlet density, lbm/ft^3,
A_{vv} = vent valve flow area, ft^2,
K_{vv} = loss coefficient,
ΔP = elevation pressure drop, psi
= $Hx(\rho_c - \rho_h)/144$,
ρ_c = core inlet density, lbm/ft^3,
H = elevation head, ft.

Assuming a system pressure and a downcomer water temperature T_c, the vent valve flow W_{vv} can be calculated using equations 3 and 4. The downcomer water temperature is determined by iterating on the assumed T_c until equations 3 and 4 predict the same vent valve flow. A benchmark study was performed for the stuck-open PORV case (0.007-ft^2 break) assuming a system pressure of 1500 psia. The results indicate that the steady-state code predicts the downcomer temperature approximately 9% above the CRAFT prediction, as shown in Figure 2-24. The

9% deviation in the downcomer temperature was used as an adjustment factor for the two breaks analyzed.

2.2.2.2. Assumptions Used for 0.015- and 0.023-ft^2 Breaks

The assumptions used in the CRAFT model are the same as those described in section 2.2.1 except for the following:

1. The break was initiated at time zero. The 0.015- and 0.023-ft^2 small break transients assumed that a break occurred simultaneously with the loss of feedwater. The 0.007-ft^2 break transient assumed that the break occurred 20 minutes after losing feedwater flow by opening the PORV.
2. HPI was initiated by an ESFAS setpoint of 1365 psia with a 35-second delay. The 0.007-ft^2 break transient did not assume an initiating break as do the 0.015- and 0.023-ft^2 small break transients, and HPI was assumed to be operator-initiated at 20 minutes for the 0.007-ft^2 break.
3. A discharge coefficient of 1.0 was applied to the Moody discharge model instead of 0.75 because these are considered simple breaks that do not have the complex flow geometry of the PORV. The Moody correlation was used for both saturated and subcooled water.

The steady-state analysis was performed using a constant system pressure based on HPI flow equal to leak flow for a given break size. The steady-state system pressures were calculated as 1000 and 600 psia for the 0.015- and 0.023-ft^2 breaks, respectively.

2.2.2.3. Results

Table 2-1 provides the sequence of events for the 0.015- and 0.023-ft^2 breaks. The downcomer temperature transients predicted by CRAFT and steady-state codes for these breaks are provided in Figure 2-25. The loop circulation ceased early in the transient as shown in Figure 2-26 because of the loss of steam generator cooling and the RCS voiding.

2.2.2.4. Conservatisms

The maximum HPI flow indicated above was used for both the CRAFT and steady-state calculations. No operator action was taken to reduce the HPI flow. The HPI water temperature was assumed to be 40F.

2.3. LOCA Analyses With Operator Action to Throttle HPI Flow (Cases 2-4, Table 1-1)

Fracture mechanics analyses performed on the data from the breaks without operator action to throttle HPI flow analyzed above using the techniques of section 5 of this report produce undesirable results after several hours when HPI is not throttled. As a result, the 0.007-, 0.015-, and 0.023-ft^2 breaks were analyzed again, now assuming that the operator started throttling back the HPI flow rate when the core outlet temperature reached 100F subcooled. (HPI flow under these conditions is independent of the number of HPI pumps operating.) The operator then maintained the core outlet temperature at 100F subcooled for the remainder of the transient. Maintaining this subcooling margin results in higher downcomer temperatures due to the reduced HPI flow rates. Throttling also results in reduced downcomer pressures. Both of these effects of throttling are beneficial with respect to the thermal shock concern.

2.3.1. Assumptions Used

The 100F subcooled conditions were used as the basis for a steady-state calculation to determine the downcomer pressure and temperature. The following assumptions were made:

1. The HPI water temperature is 40F.
2. The system is in a steady-state condition; i.e., HPI flow is equal to leak flow.
3. Leak flow is based on the Moody correlation with a discharge coefficient of 1.0.
4. The core outlet temperature is maintained at 100F subcooled.
5. Decay heat is based on 1.2 times ANS standard.
6. Pressurizer water temperature is equal to core outlet temperature.

2.3.2. Analytical Methods

The results of the eight-node CRAFT analyses, as described in section 2.2.2.1, were used to determine the RCS conditions until the core outlet became 100F subcooled. Operator action to reduce HPI flow to maintain 100F subcooling is assumed at this time. The remainder of the transient conditions are calculated using a steady-state analysis as described below.

In a steady-state condition, the relationship of HPI flow to leak flow is defined as

$$W_L = W_{HPI}\left(\frac{\nu_{HPI}}{\nu_L}\right) \qquad (5)$$

where

W_L = leak flow. lbm/s,

W_{HPI} = HPI flow, lbm/s,

ν_{HPI} = specific volume of HPI, ft^3/lbm,

ν_L = specific volume of core outlet water, ft^3/lbm.

Assuming a system pressure and 100F subcooling, the leak flow can be calculated by the Moody correlation. Equation 6 is used to determine the HPI flow (W_{HPI}) required to maintain 100F subcooling. Equation 5 is used as a convergence criterion for determining the system pressure. If W_{HPI} and W_L fail to satisfy the equation, the system pressure is readjusted until the criterion is satisfied. Once the system pressure is determined, then the equations (3 and 4) in 2.2.2.1 are used in the same manner to determine the vent valve flow.

$$q = W_{HPI}(h_h - h_{HPI}) \qquad (6)$$

where

q = decay heat rate, Btu/s,

h_h = enthalpy of core outlet water based on the 100F subcooled state, Btu/lbm,

h_{HPI} = enthalpy of HPI water, Btu/lbm,

W_{HPI} = HPI flow rate, lbm/s.

2.3.3. Results

The mixed downcomer temperature was calculated to be equal to the saturation temperature minus 150F. This value is based on the assumption of 100F subcooled at the core outlet plus 50F core ΔT. The downcomer temperature and pressure plots are shown in Figures 2-27 and 2-28 for the 0.007-, 0.015-, and 0.023-ft^2 breaks. The vent valve and HPI flows and vent valve fluid temperature versus time (to 3 hours) for the 0.007- and 0.023-ft^2 breaks are shown in Figures 2-29 through 2-31. The HPI flow and the vent valve flow and temperature are used for the mixing and reactor vessel temperature analyses as described in sections 3 and 4. Fracture mechanics analyses performed on this data are discussed in section 5 of this report.

Table 2-1. Transient Sequence of Events

	$0.007\ ft^2$ (cases 1, 2), time-minutes	$0.015\ ft^2$, time-minutes	$0.023\ ft^2$ (cases 3, 4), time-minutes
Reactor, turbine, and feedwater trip	0	0	0
Reactor coolant pump trip	0	0	0
LOCA initiated	20	0	0
Reach saturation at core outlet	Never	2	2
HPI initiated	20	3	3
Regain subcooled state at core outlet	NA	7	9
Loss of natural circulation	40	13	9
Achieve 50F subcooled at core outlet	55	19	17
Achieve 100F subcooled at core outlet	77	--	30

Table 2-2. Vent Valve Opening Vs Resistance

ΔP across vent valve, psi	Avg opening angle, degrees	Resistance factor,[a] K
≥ 0.25	21	4.2
$0.25 > \Delta P \geq 0.2$	17.5	5.0
$0.20 > \Delta P \geq 0.16$	12.0	8.8
$0.16 > \Delta P \geq 0.12$	8.13	16.0
$0.12 > \Delta P \geq 0.08$	4.13	46.0
$0.08 > \Delta P$	0	∞

Figure 2-1. HPI Flow Rate Vs RCS Pressure

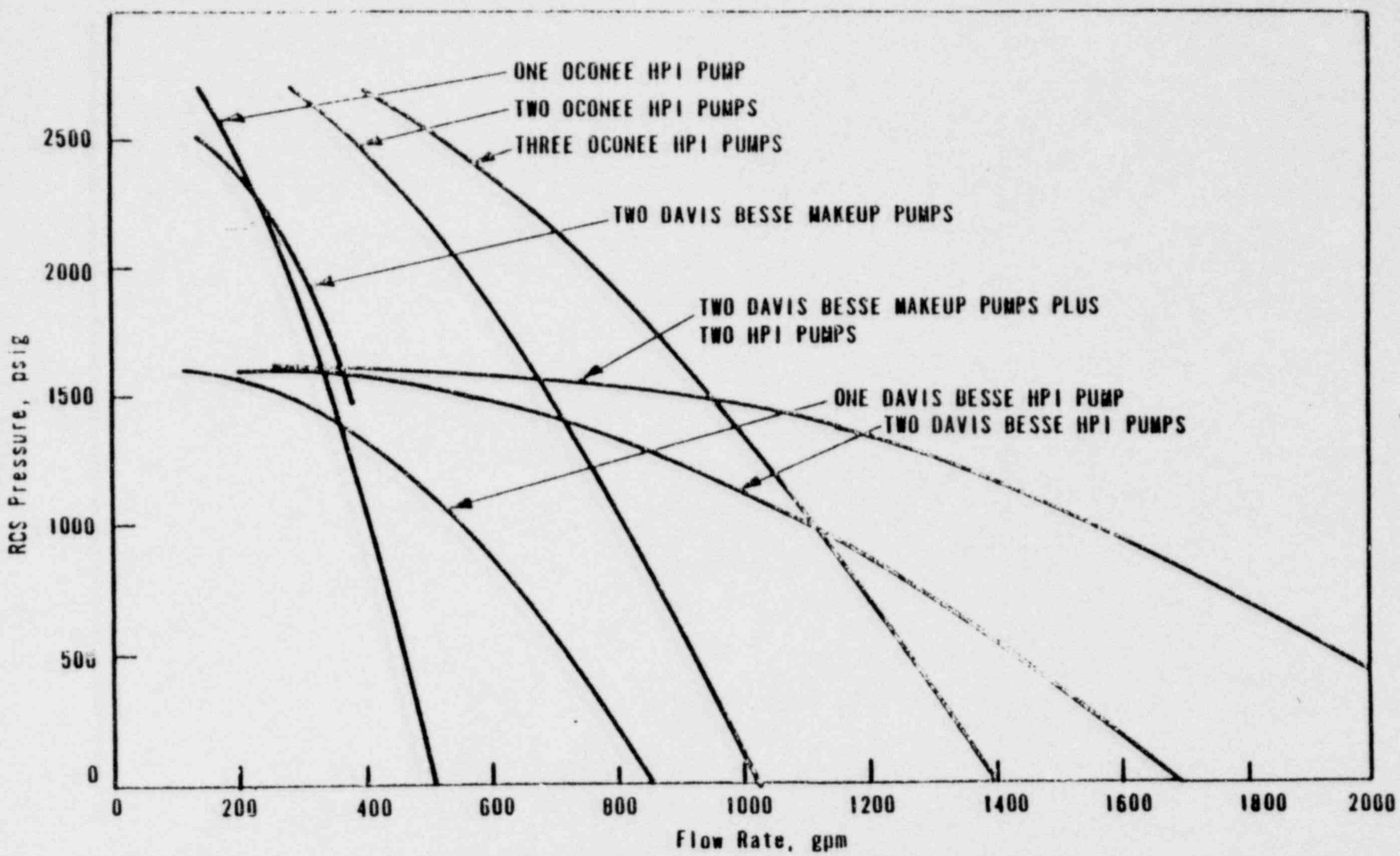
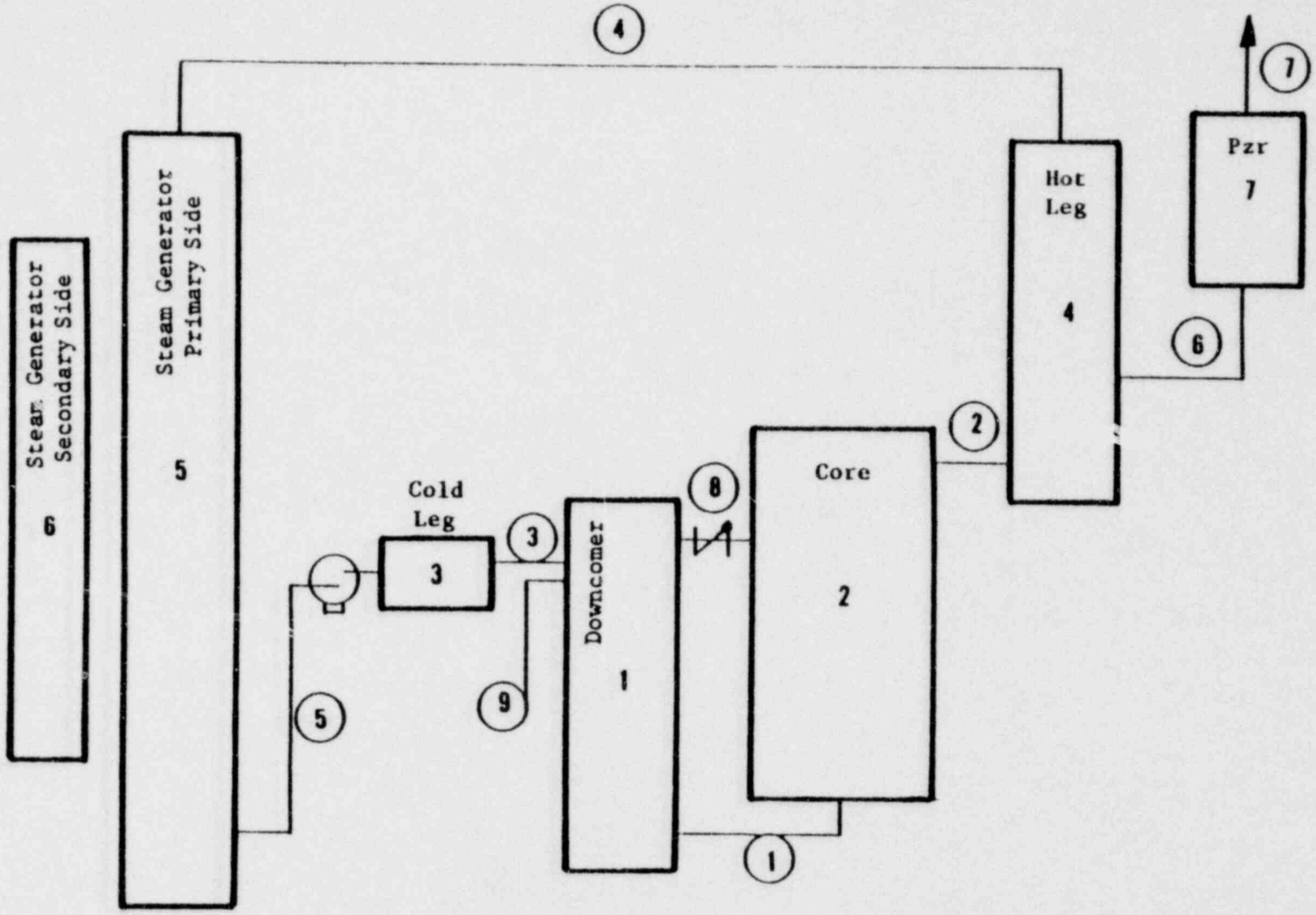


Figure 2-2. CRAFT Noding Scheme, Eight-Node Model of RCS



NODE 8 IS CONTAINMENT NODE

Figure 2-3. Downcomer Temperature Vs Time, Comparison of Multinode and Eight-Node CRAFT Models, Stuck-Open PORV, Two HPI Pumps, AFW @ 40 s

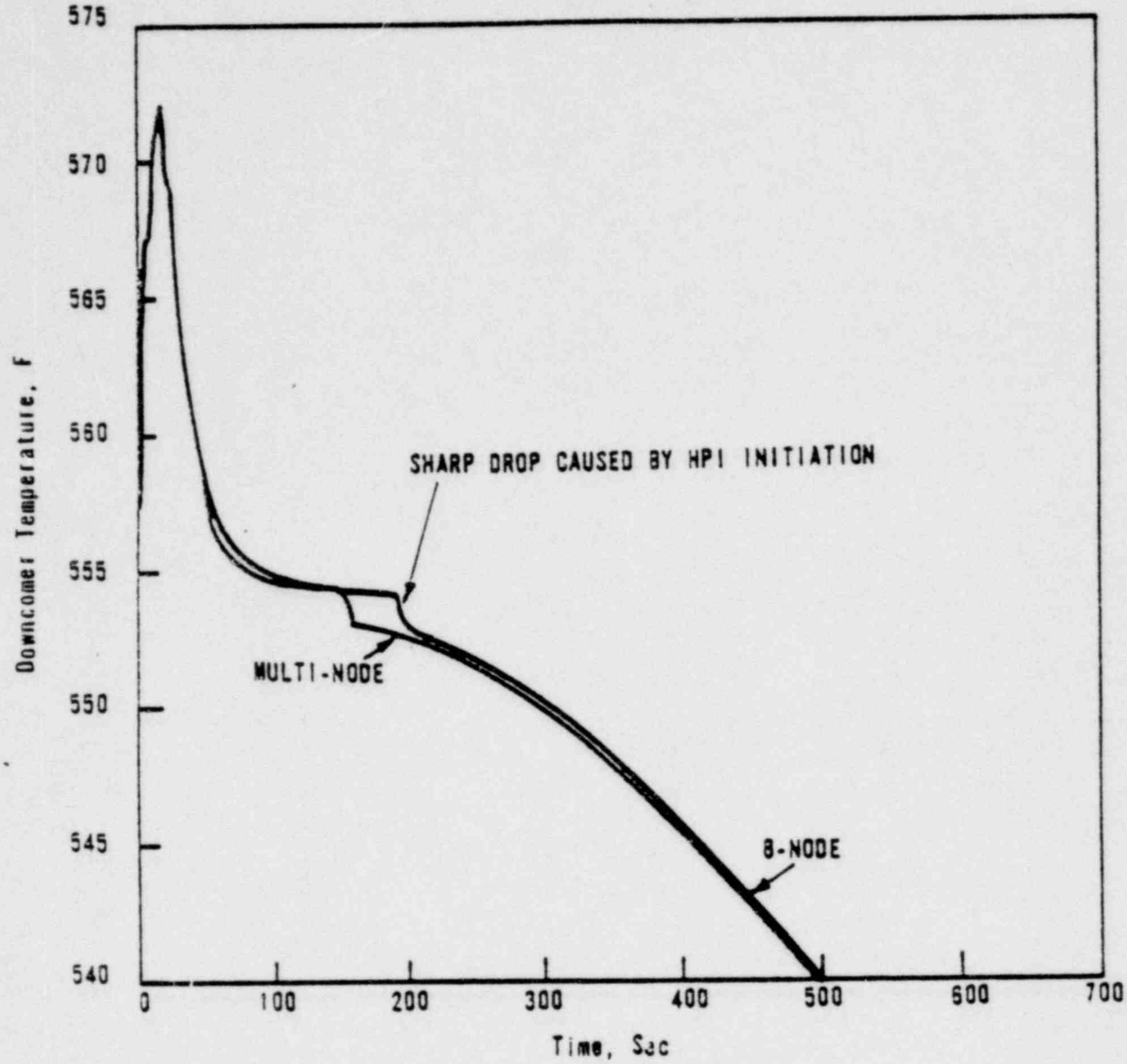


Figure 2-4. Downcomer Pressure Vs Time — Comparison of M̈ulti-node and Eight-Node CRAFT Models. Stuck-Open PORV, Two HPI Pumps, AFW @ 40 s

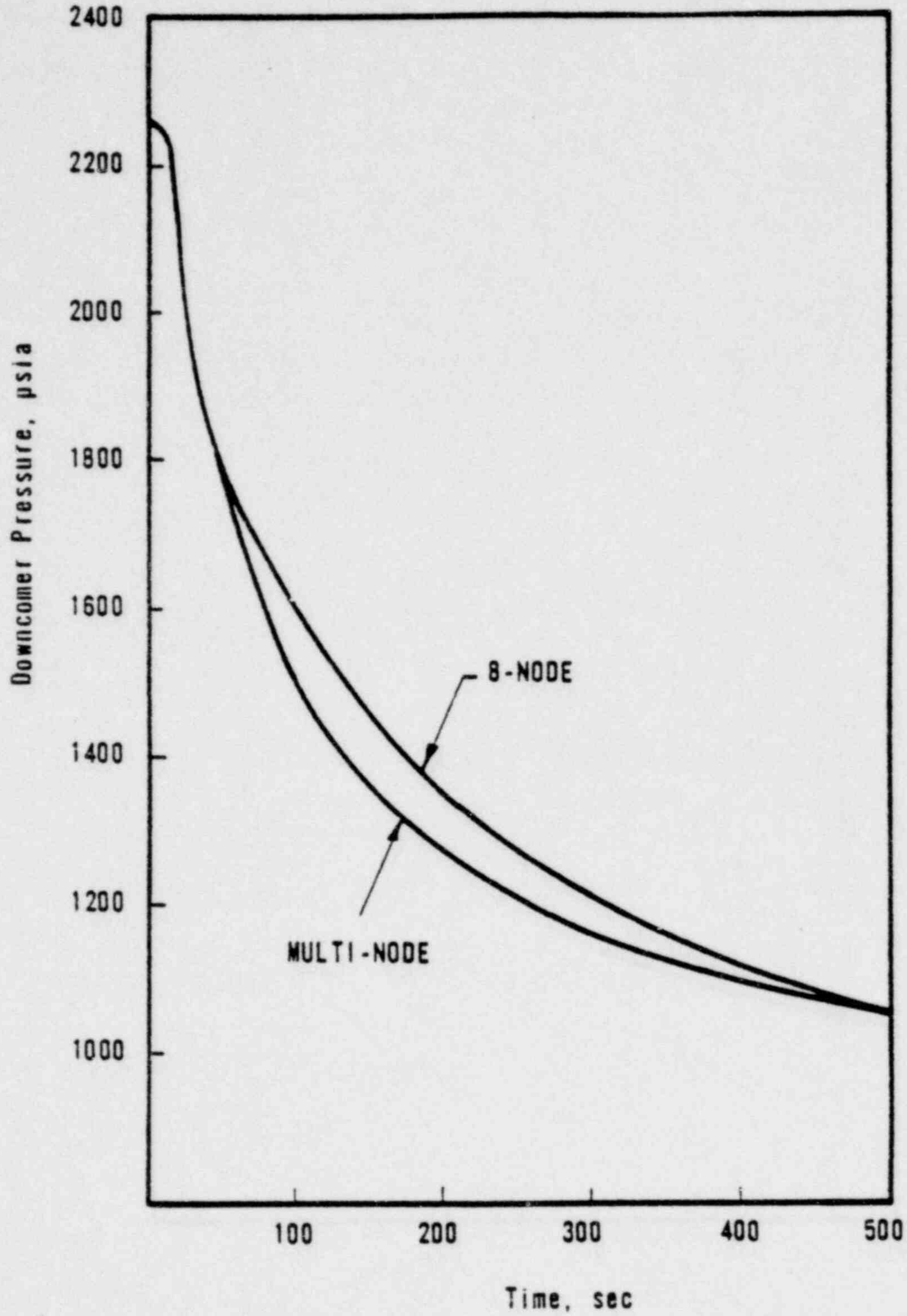


Figure 2-5. Cold Leg Level 0.007-ft^2 Pressurizer Break Without HPI Throttling, Node 3

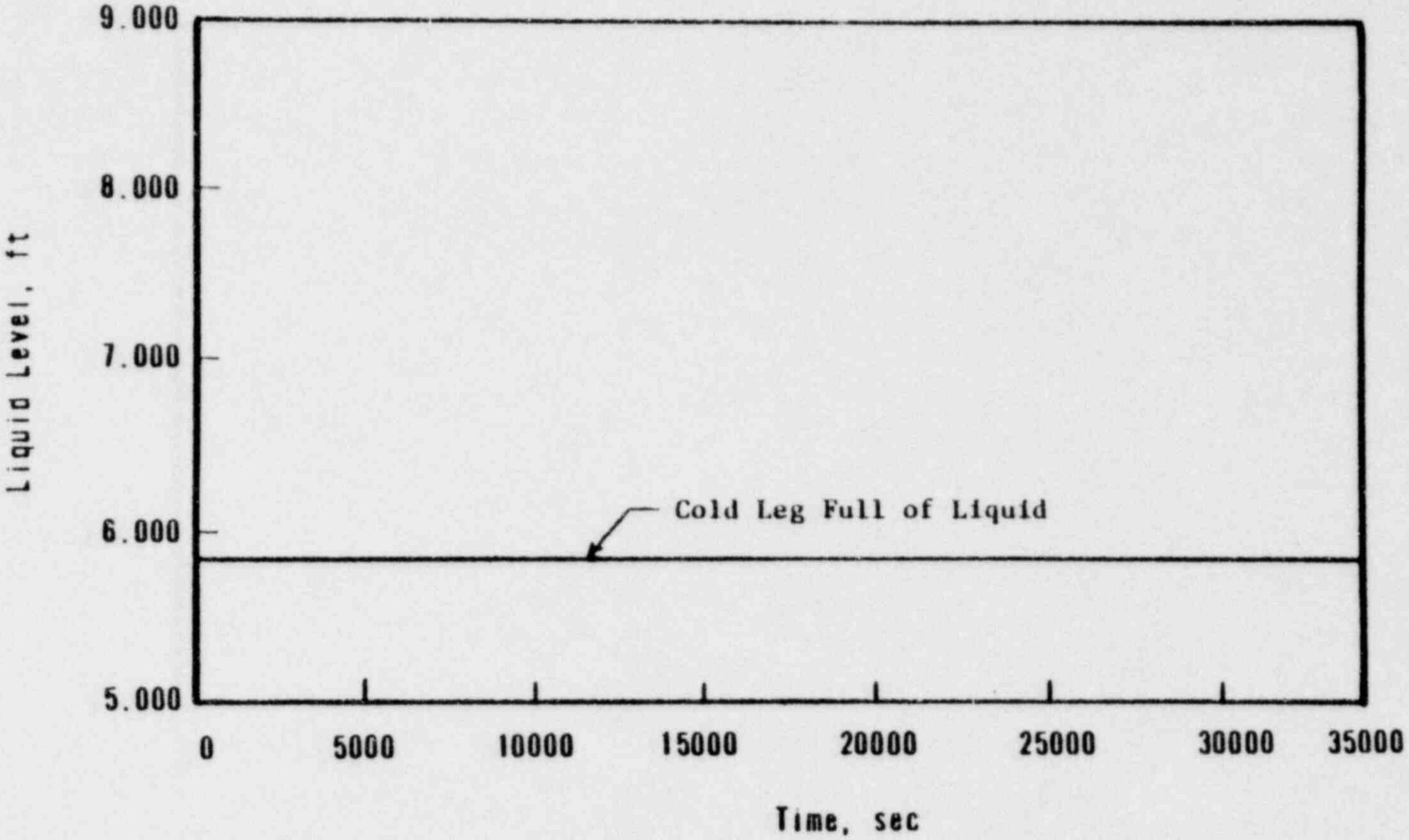


Figure 2-6. Hot Leg Level, 0.007-ft^2 Break Without HPI Throttling, Node 4

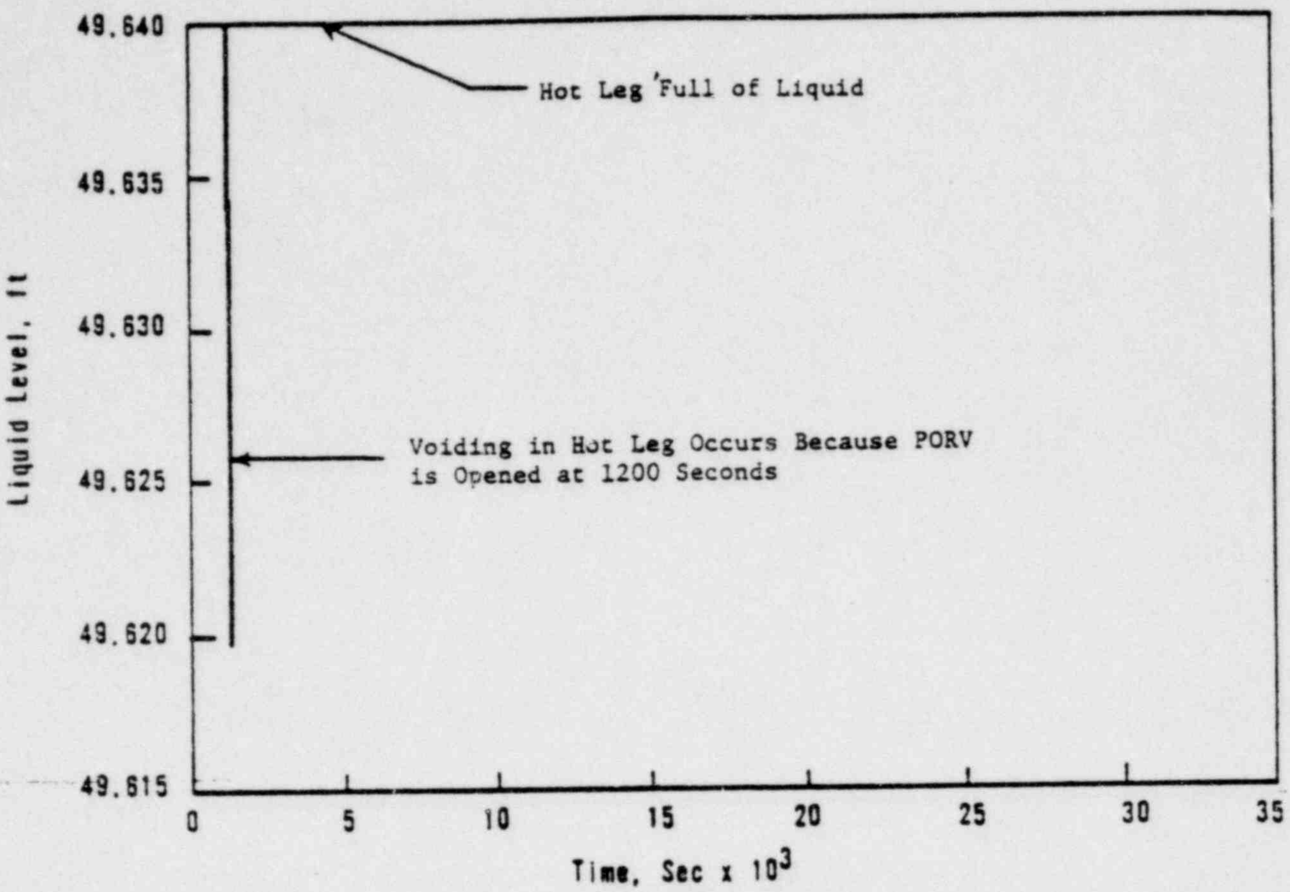


Figure 2-7. Pressurizer Level 0.007-ft^2 Pressurizer Break Without HPI Throttling, Node 7

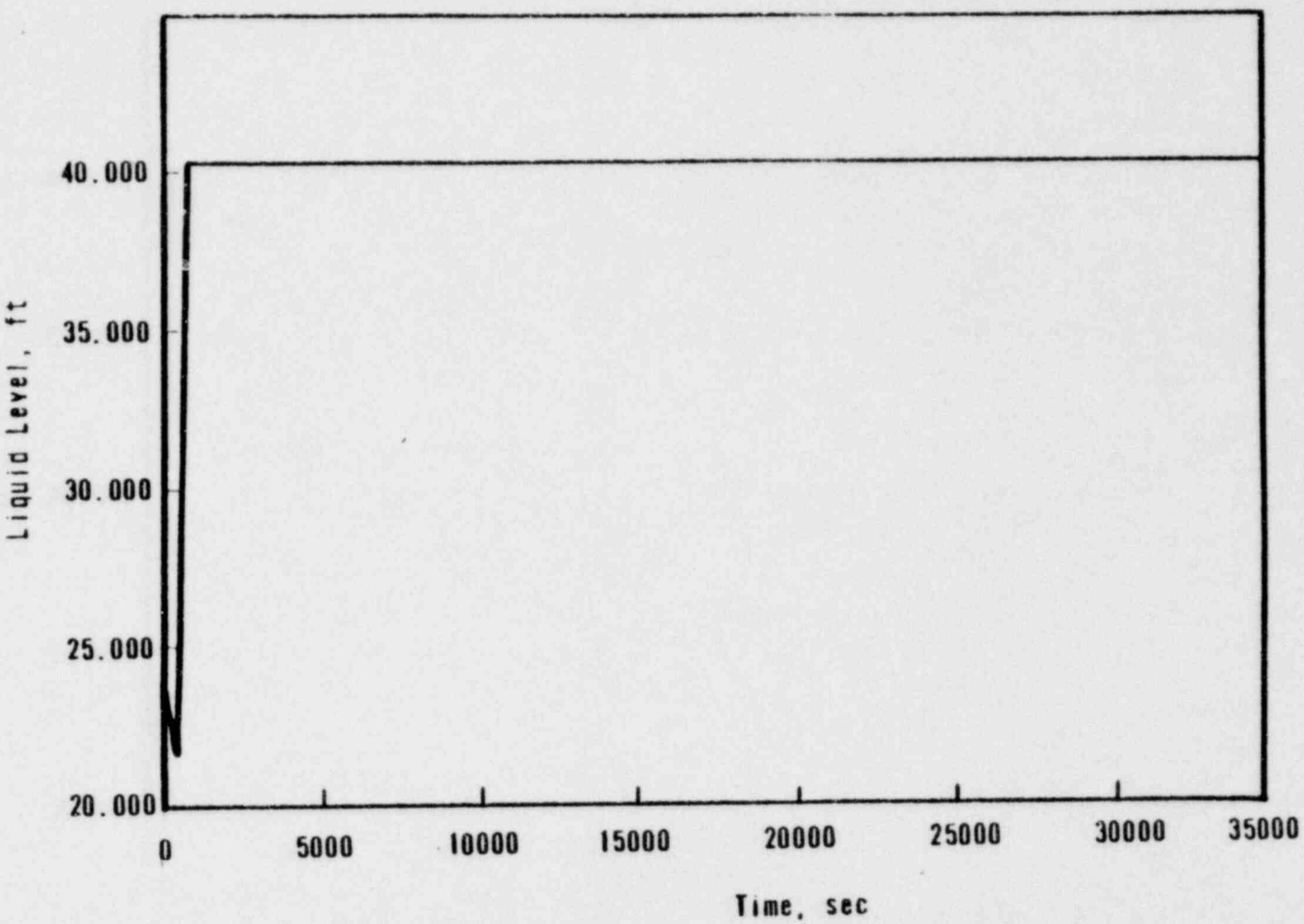


Figure 2-8. RV Liquid Level 0.007-ft^2 Pressurizer Break Without HPI Throttling

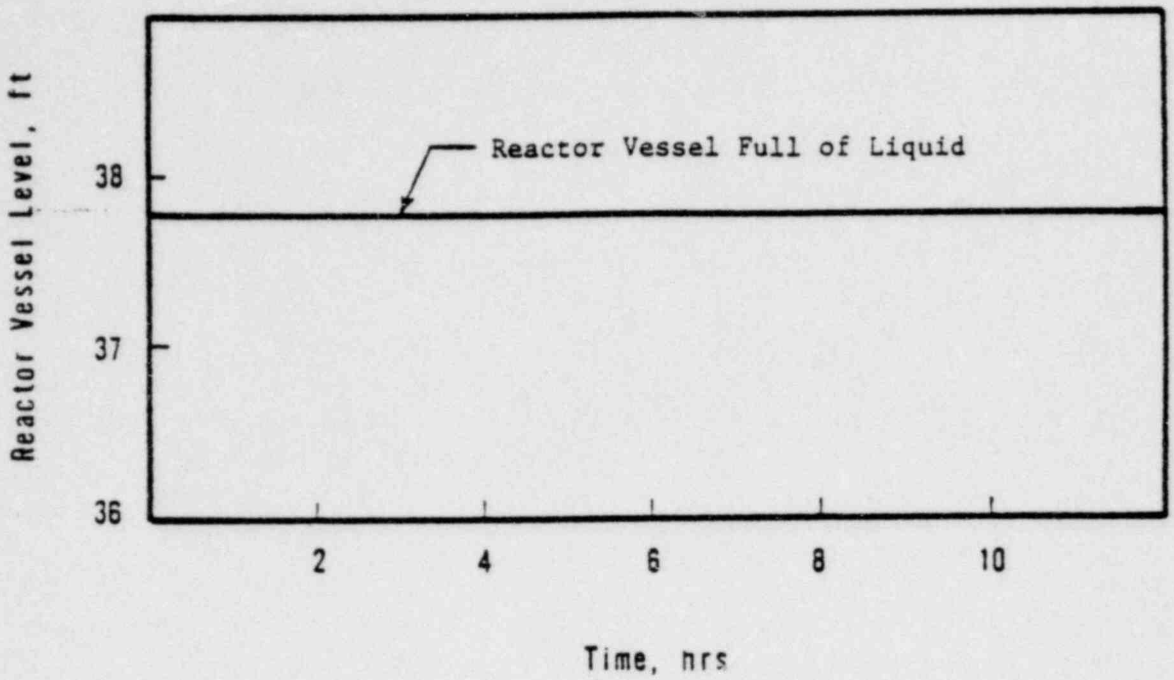


Figure 2-9. Core Outlet Temperature Vs Time, 0.007-ft² Pressurizer Break Without HPI Throttling, Node 2

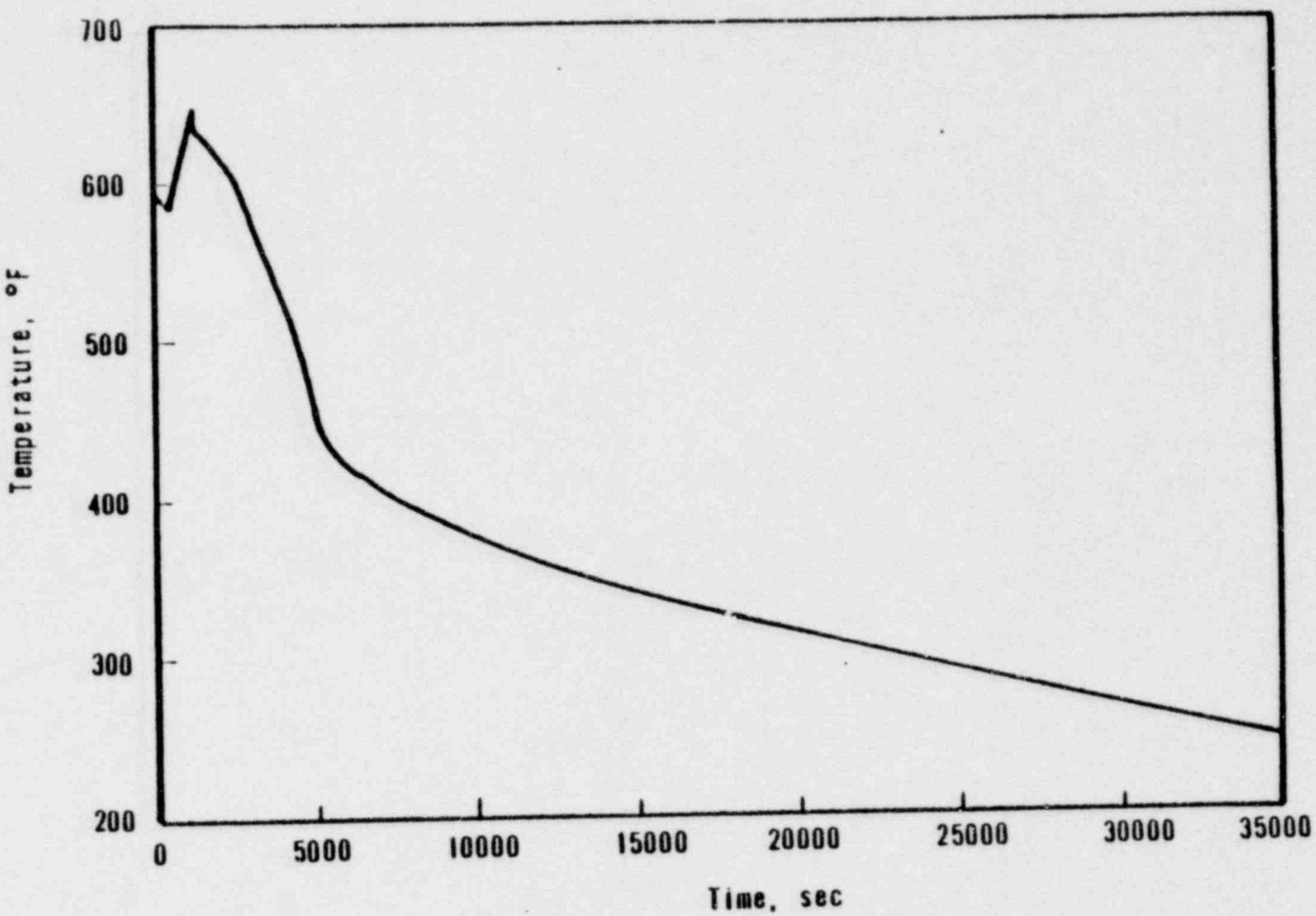


Figure 2-10. Downcomer Temperature Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Node 1

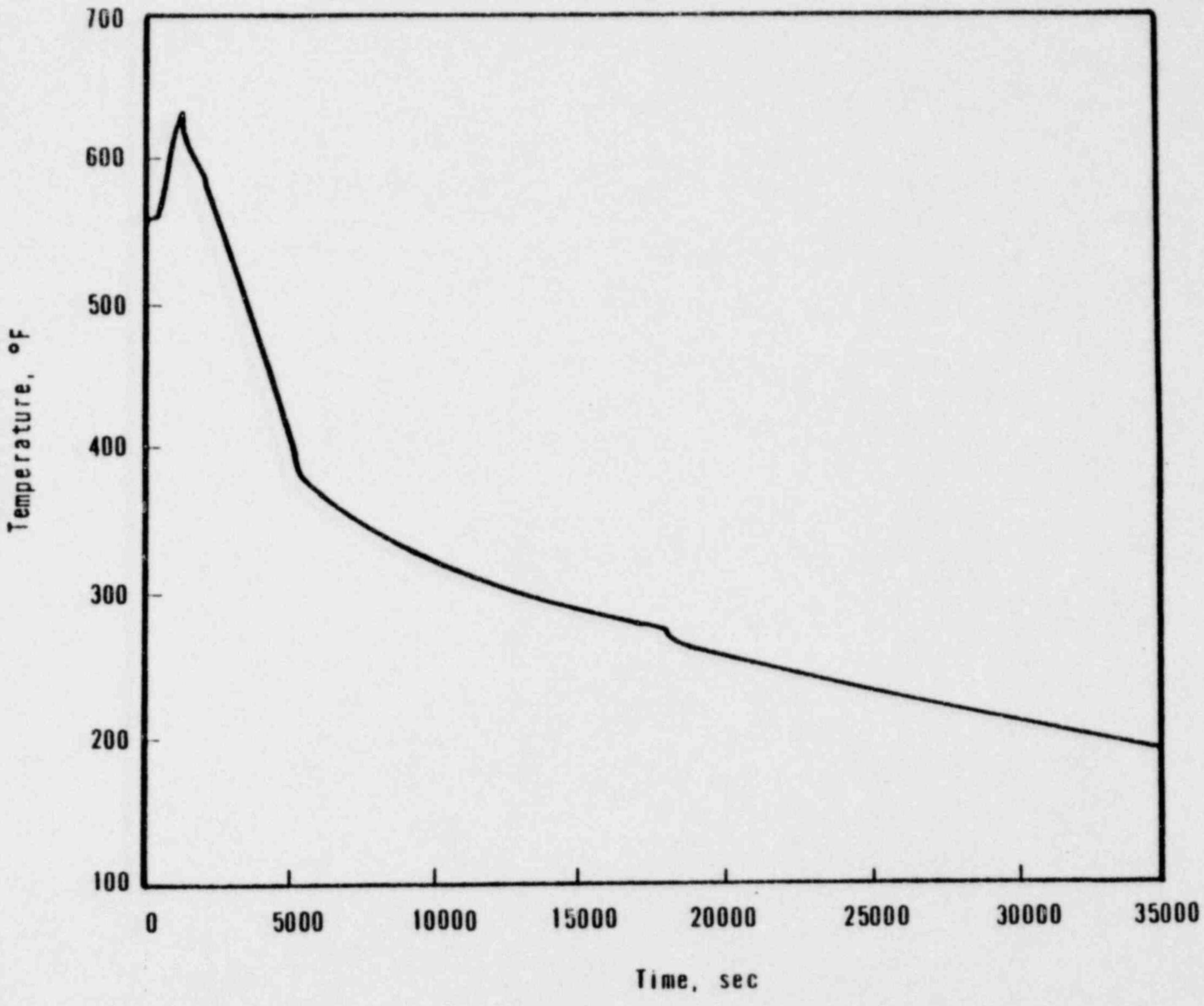


Figure 2-11. Hot Leg Temperature Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Node 4

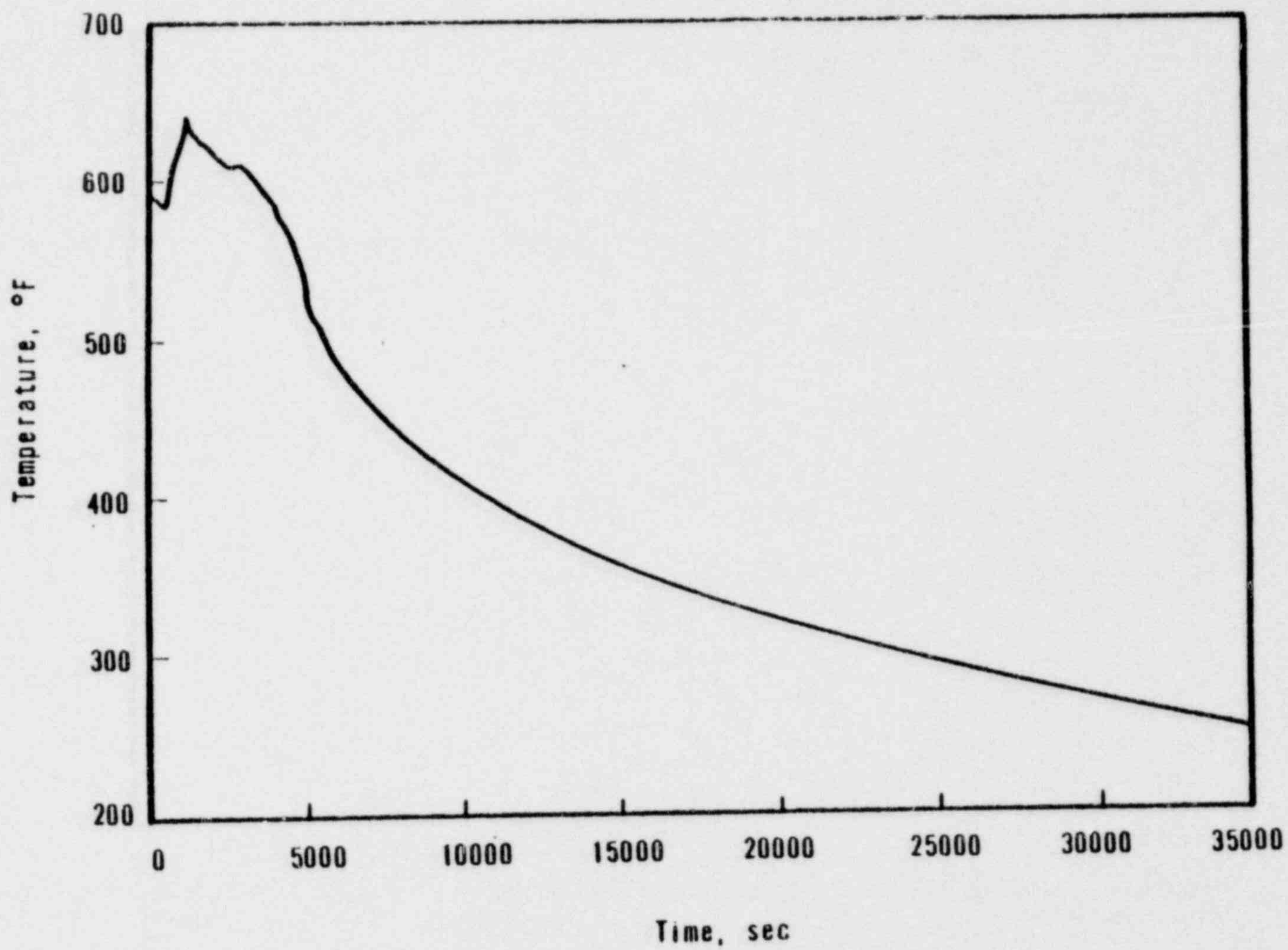


Figure 2-12. Core Exit Flow Vs Time - 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 2

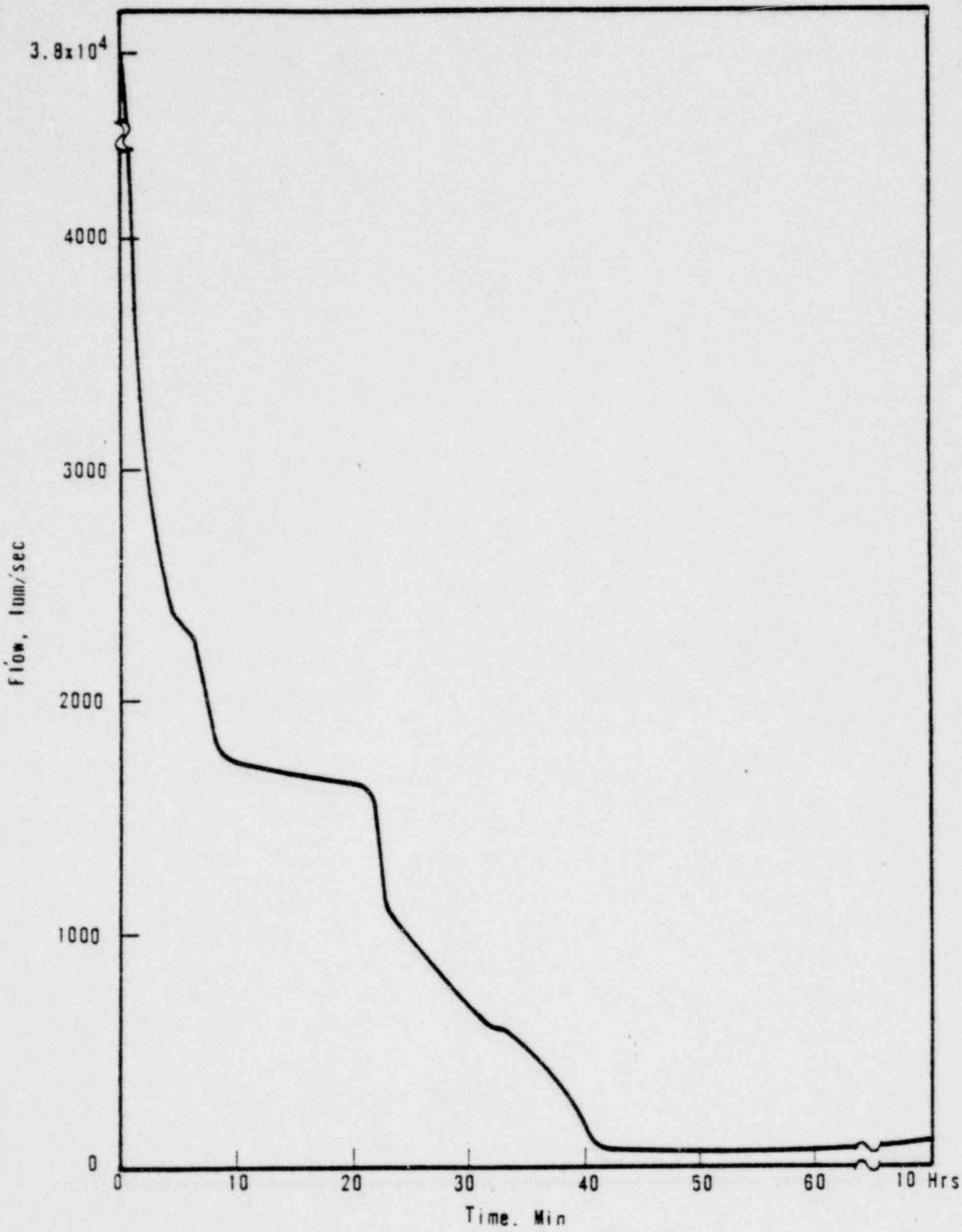


Figure 2-13. Leak Path Flow Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 7

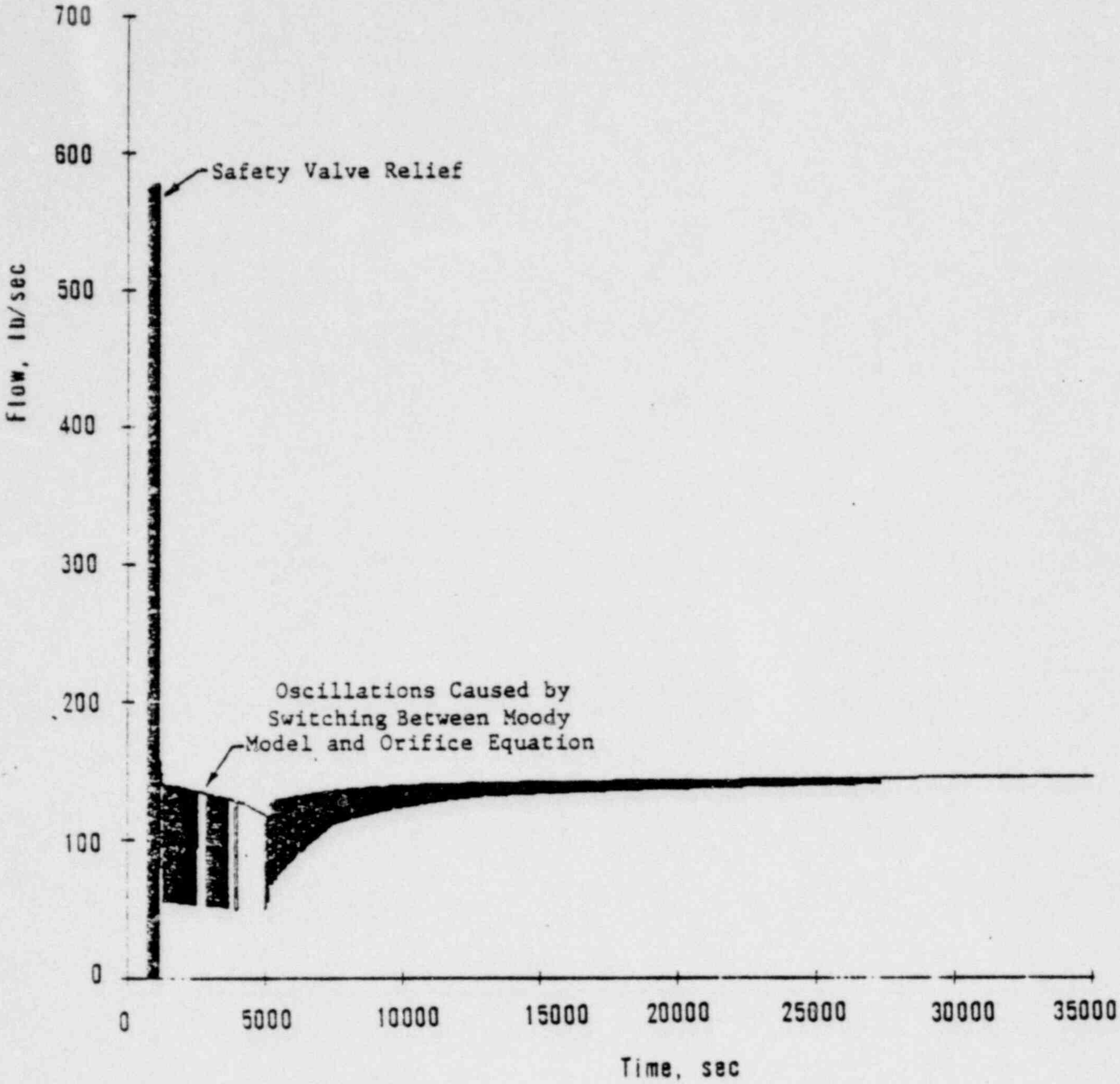


Figure 2-14. Vent Valve Flow Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 8

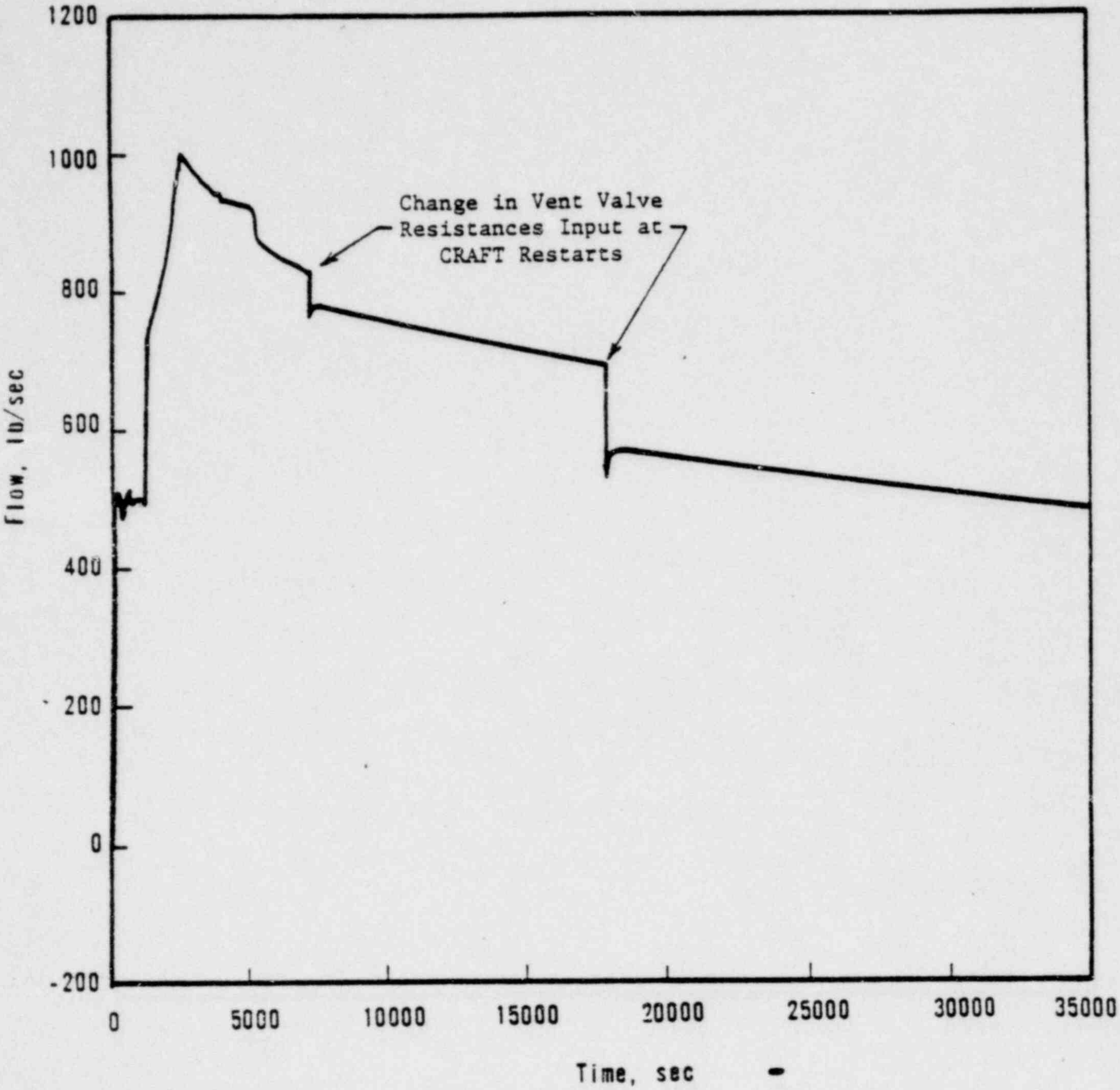


Figure 2-15. HPI Flow Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 9

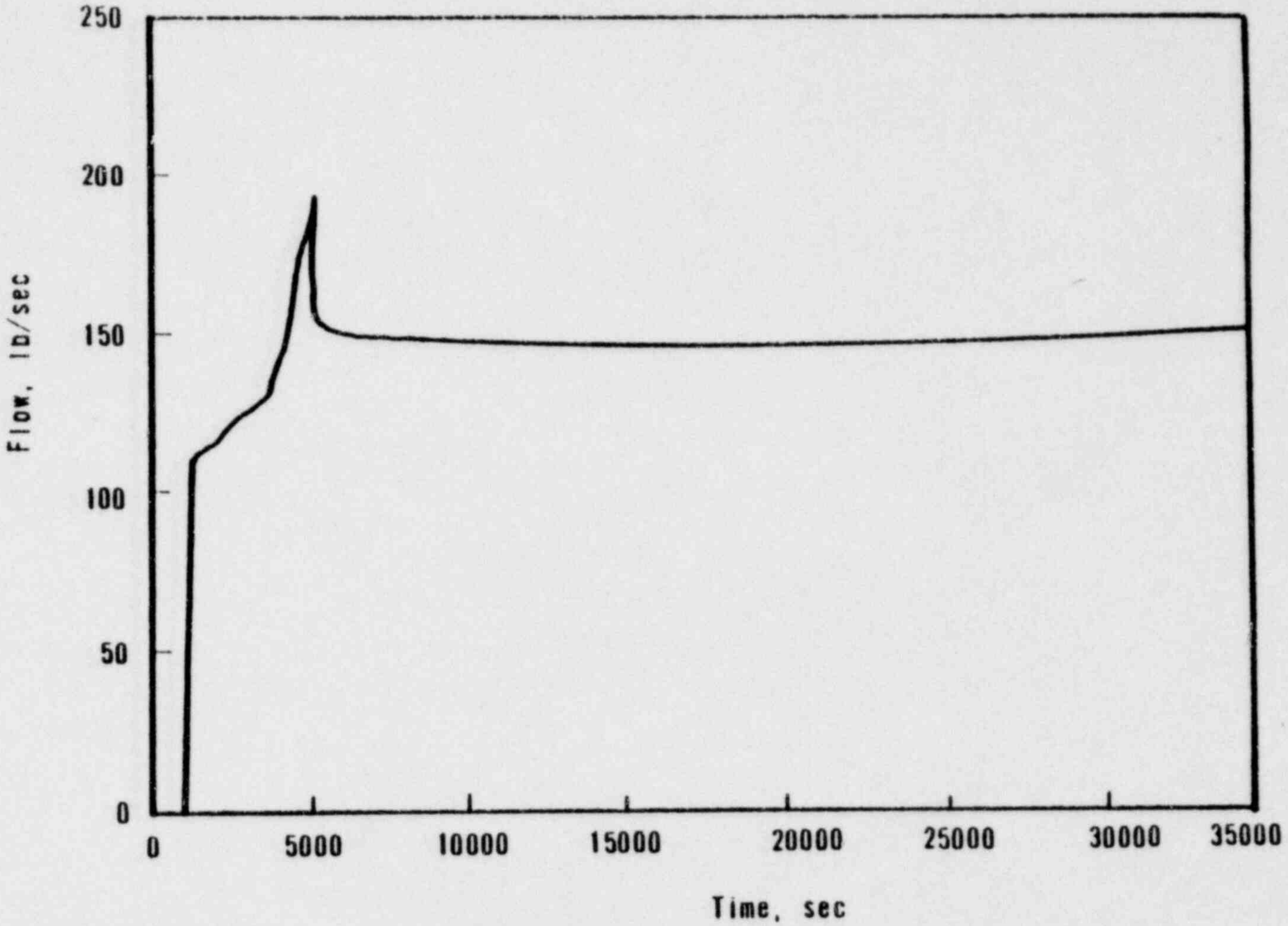


Figure 2-16. Core Outlet Quality, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 2

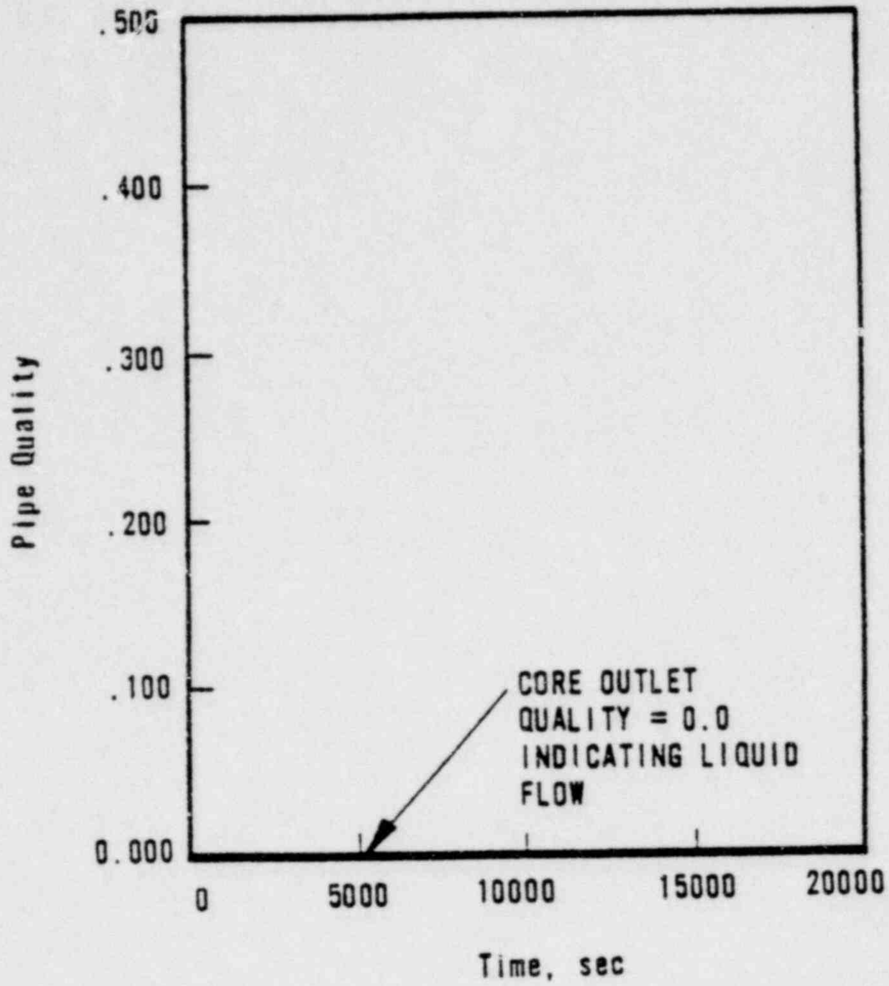


Figure 2-17. Leak Path Quality (PORV), 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 7

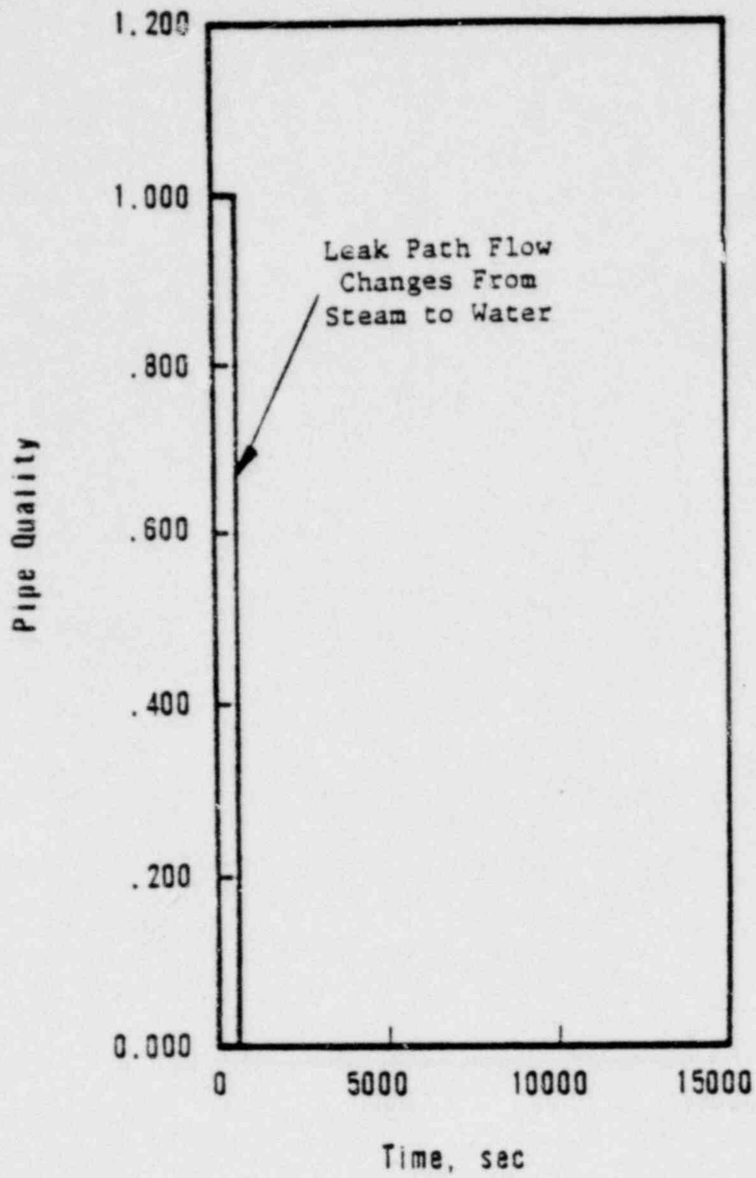


Figure 2-18. Vent Valve Quality, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Path 8

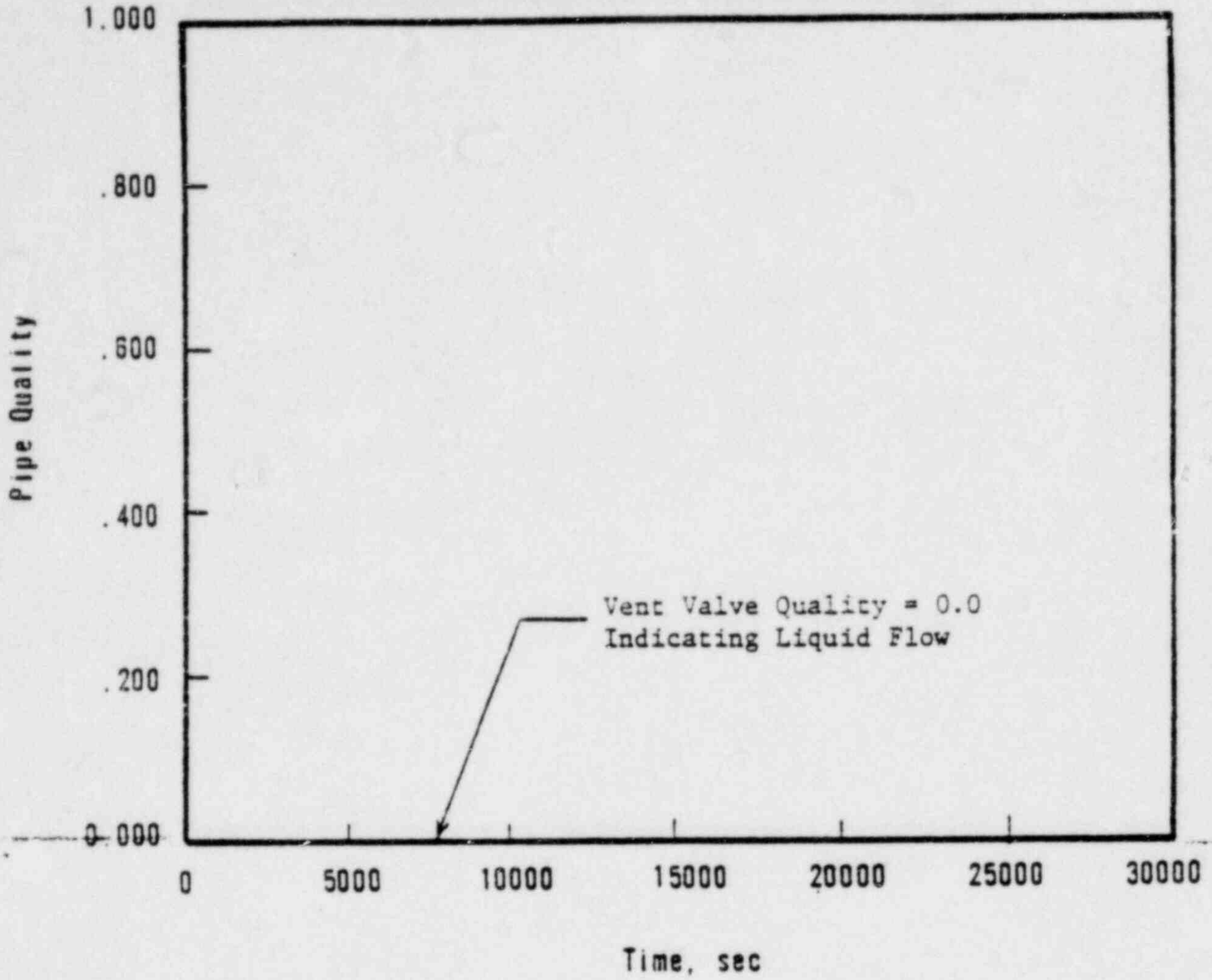


Figure 2-19. Primary System Inventory, 0.007-ft^2 Pressurizer Break Without HPI Throttling

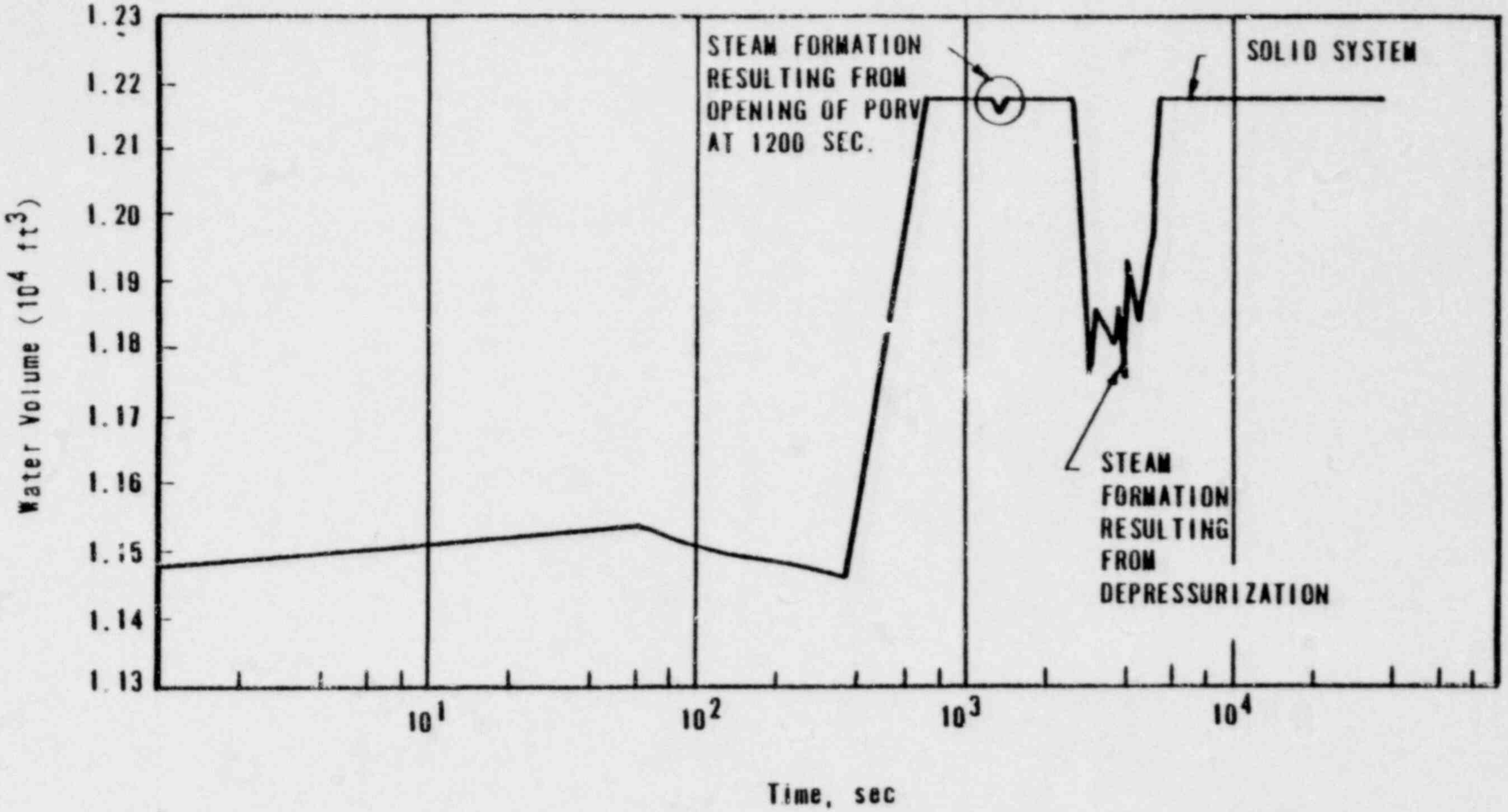


Figure 2-20. Core Pressure Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling, Node 2

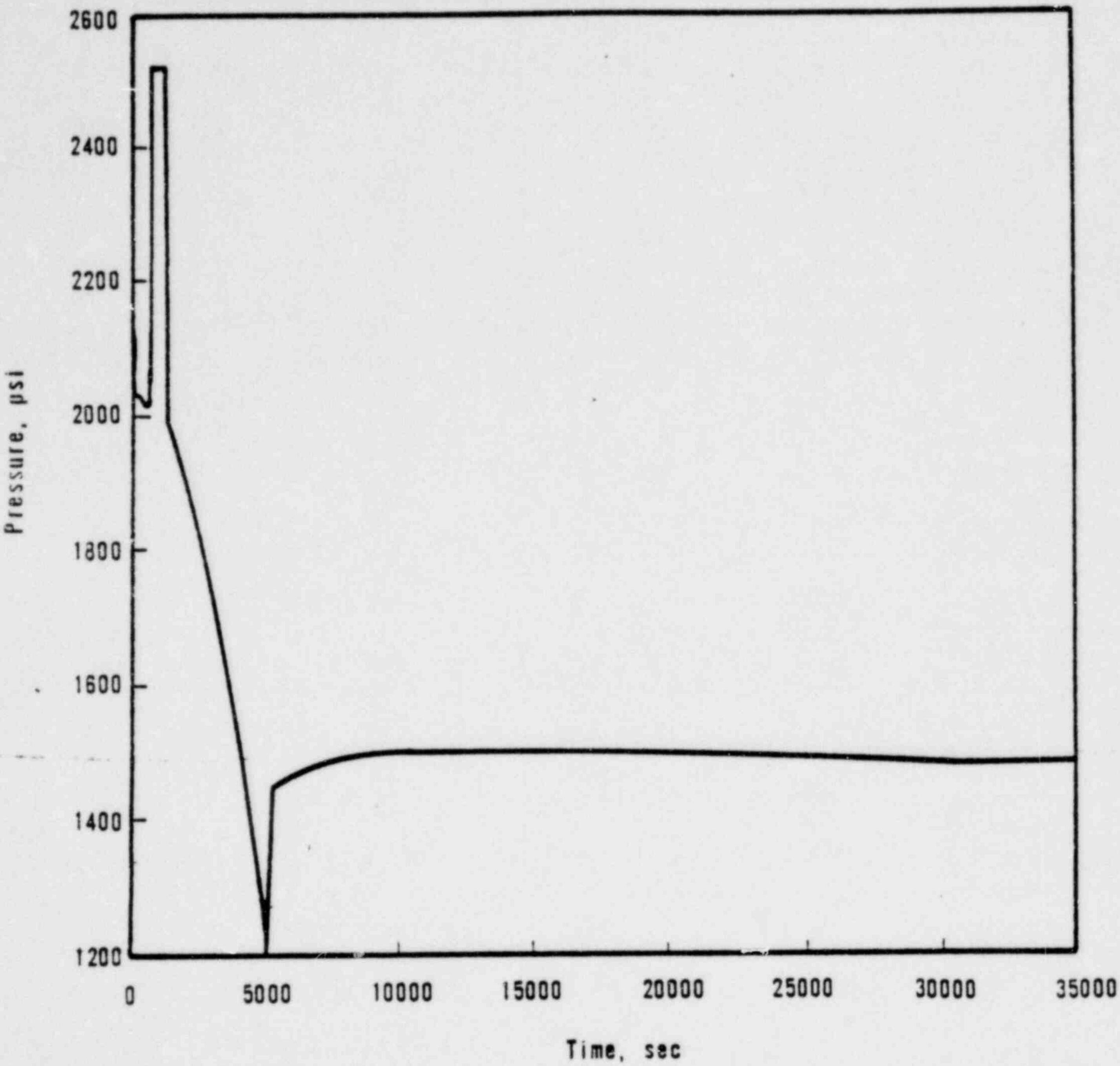


Figure 2-21. Pressurizer Fluid and Metal Temperature Vs Time, 0.007-ft^2 Break Without HPI Throttling

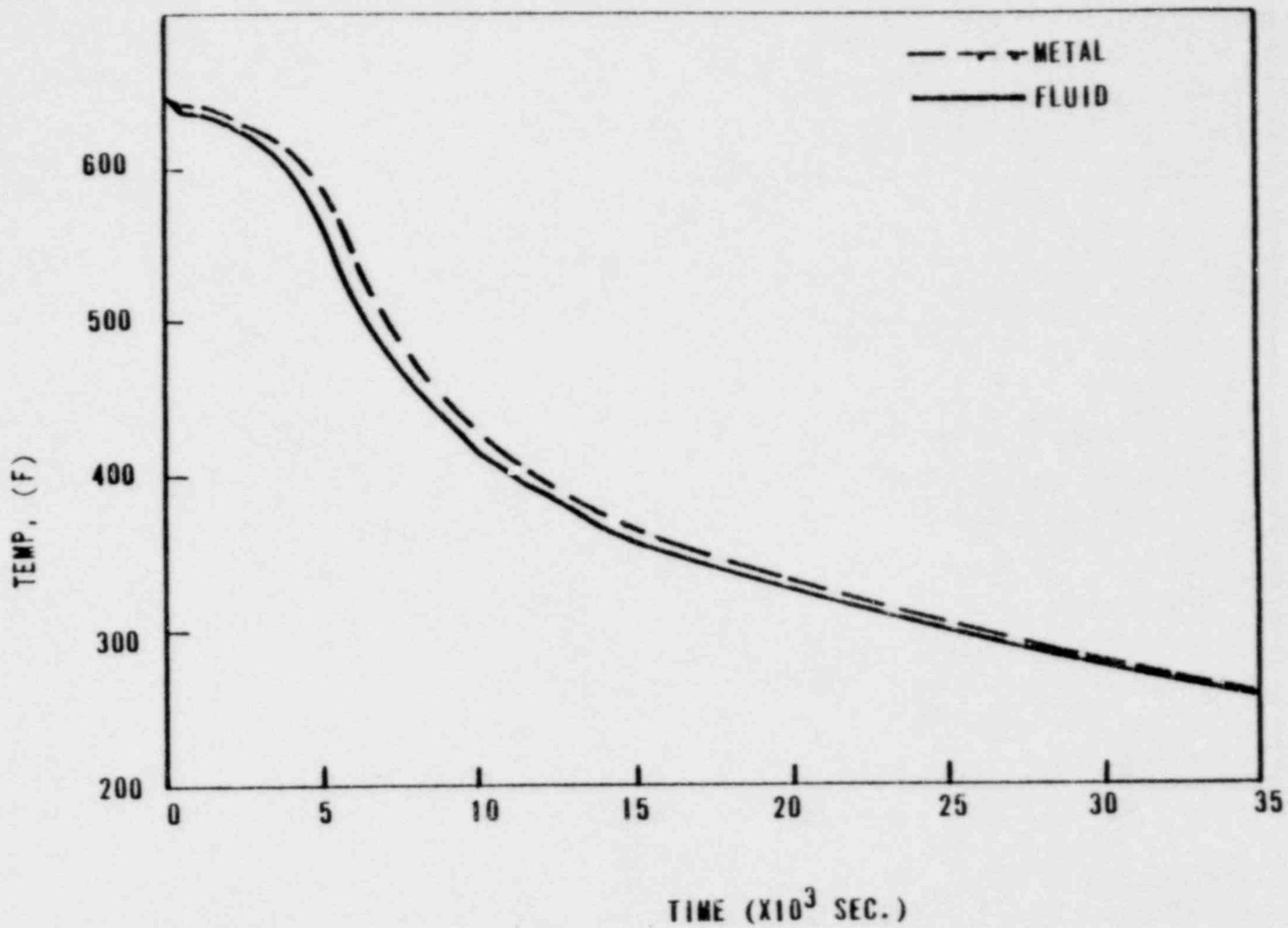


Figure 2-22. Upper Head Metal Temperature Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling

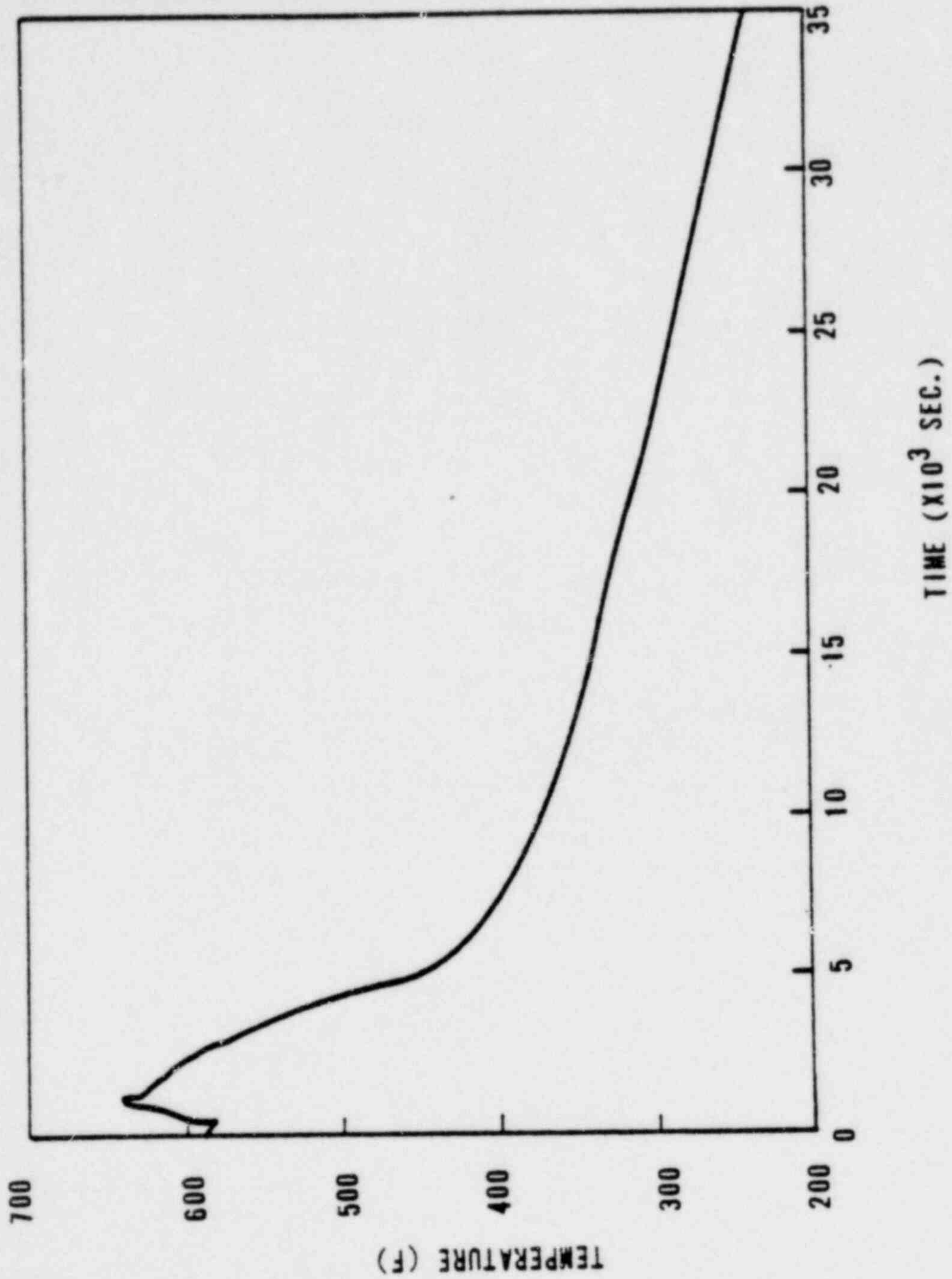


Figure 2-23. Cold Leg Water Temperature Vs Time, 0.007-ft^2 Pressurizer Break Without HPI Throttling

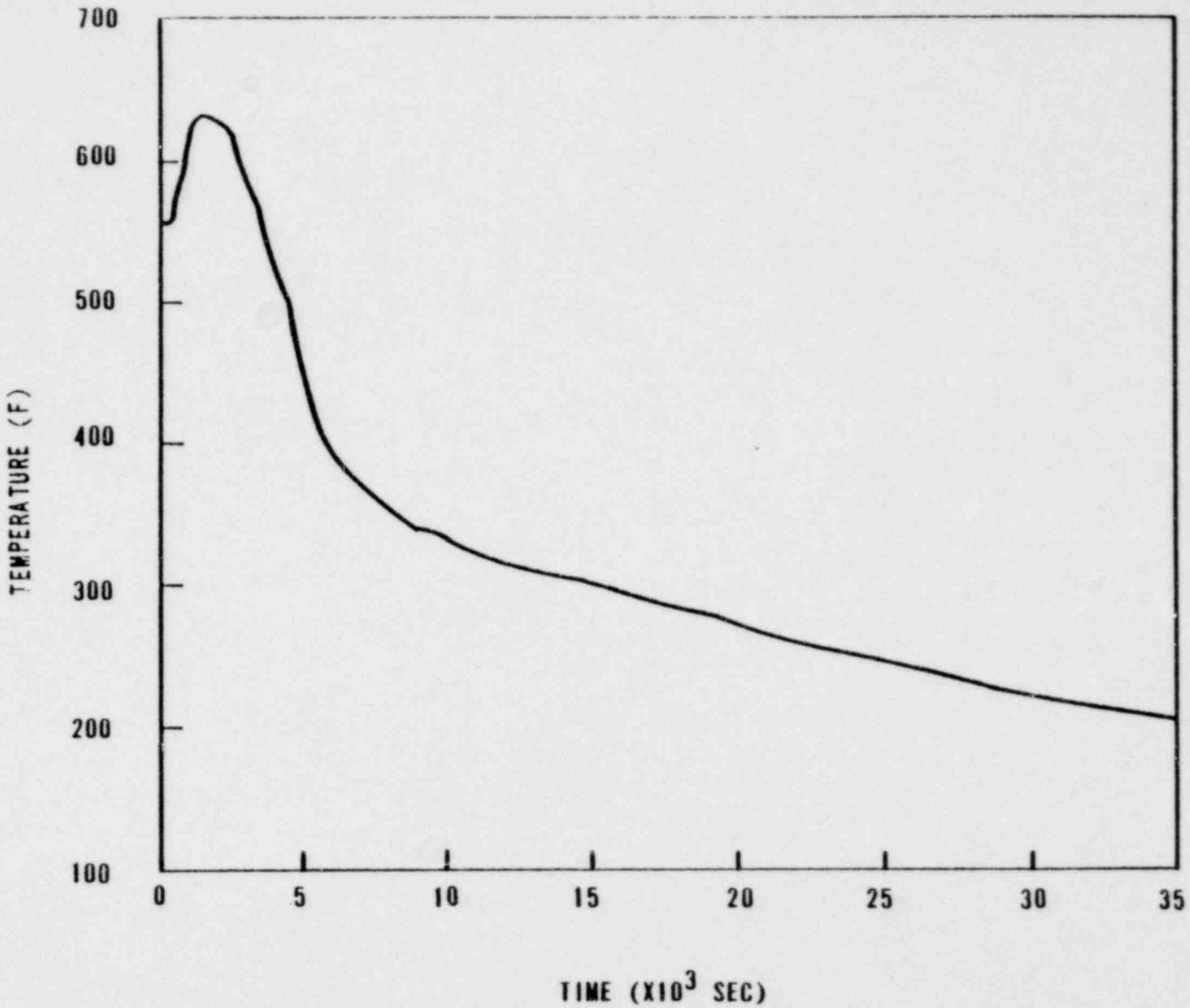


Figure 2-24. Downcomer Temperature Vs Time at 1500 psia, Comparison of Eight-Node CRAFT to Semi-Steady-State Analysis Method

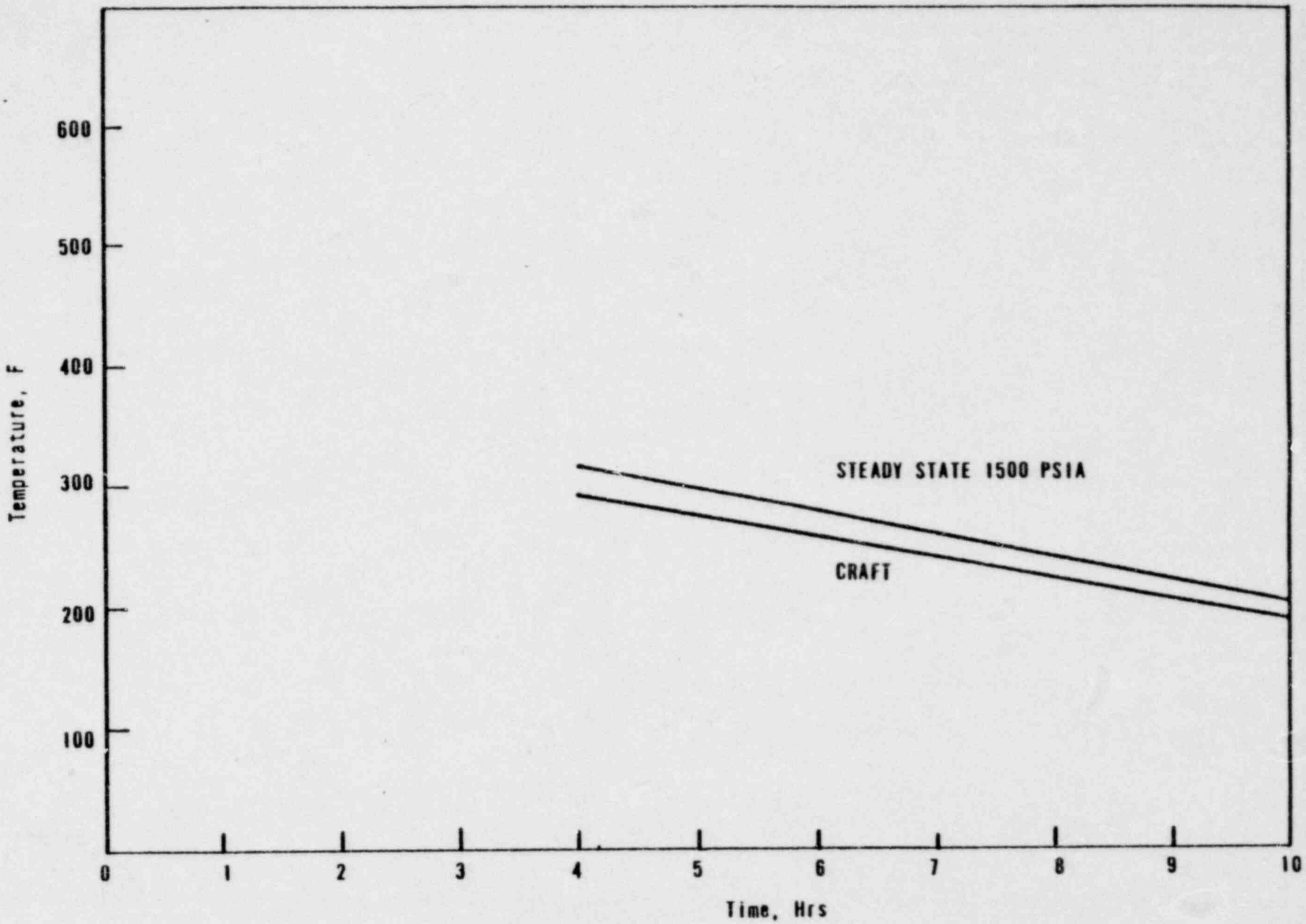


Figure 2-25. Downcomer Temperature Vs Time, 0.015- and 0.023-ft^2 Pressurizer Breaks Without HPI Throttling — CRAFT Semi-Steady-State Analysis

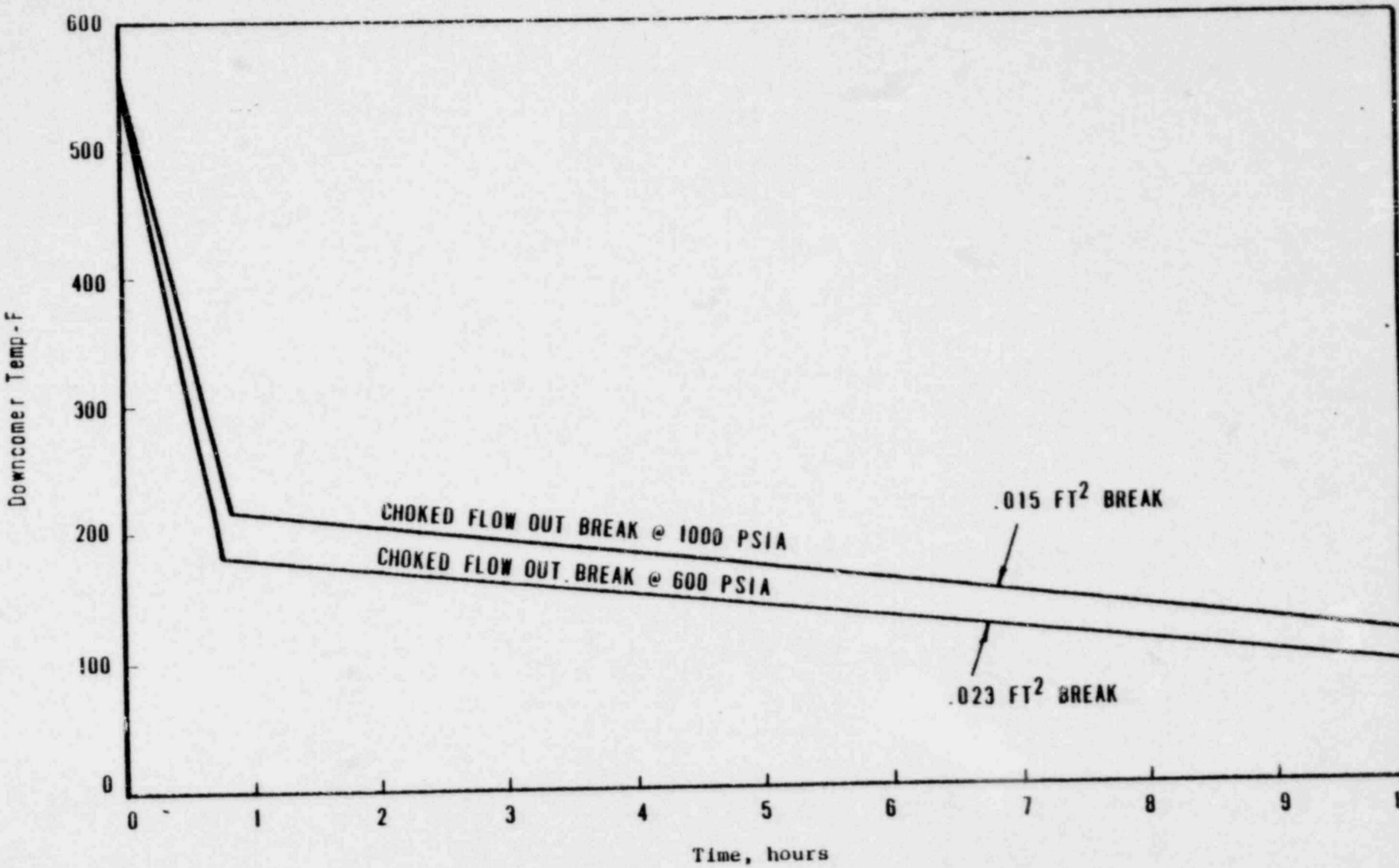


Figure 2-26. RC Loop Flow Vs Time, 0.015- and 0.023-ft² Pressurizer Breaks Without HPI Throttling, Flow Path 3

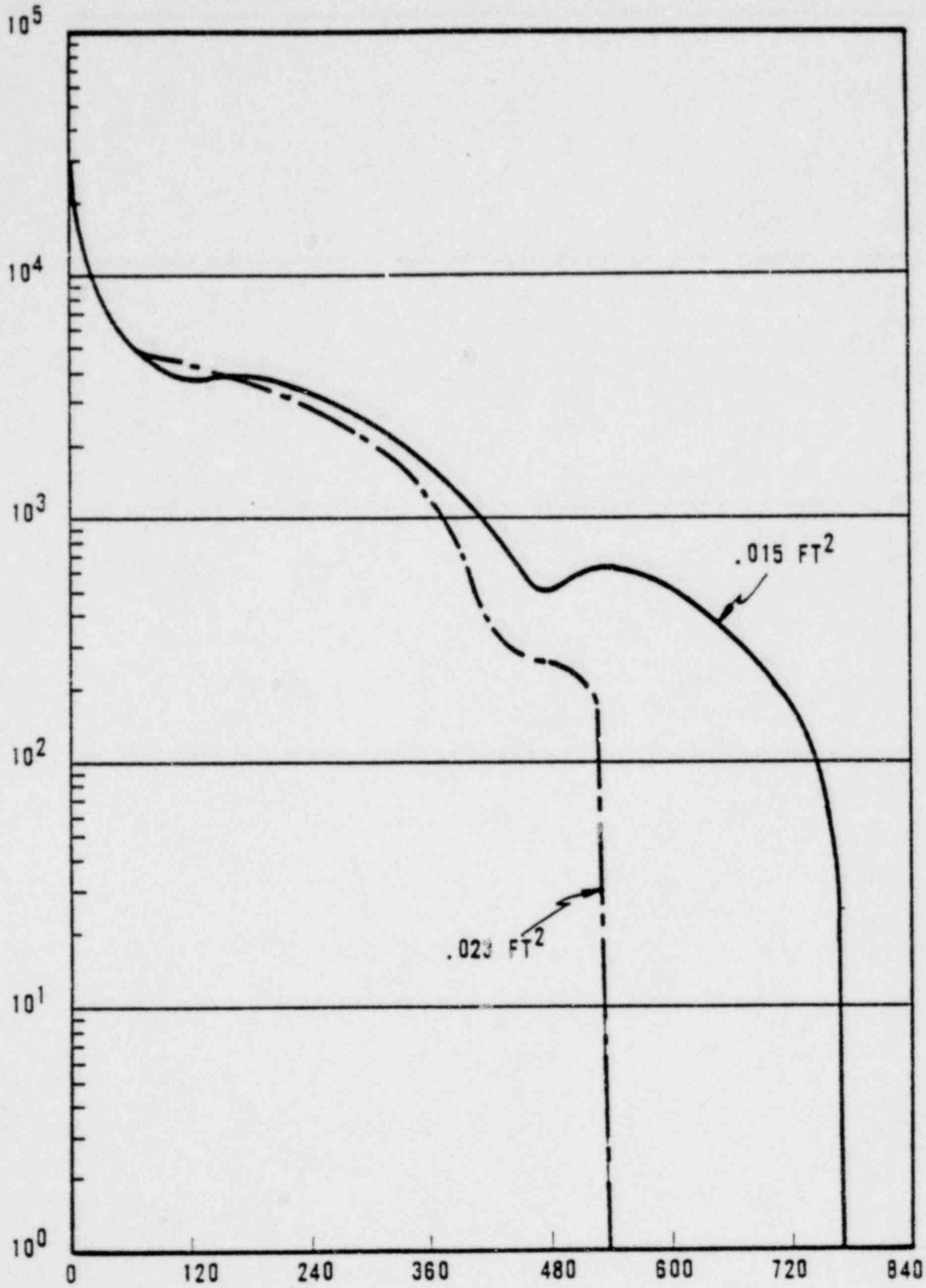


Figure 2-27. Downcomer Temperature Vs Time, 0.007-, 0.015-, and 0.023-ft^2 Pressurizer Breaks With HPI Throttling at 100F Subcooled Core Outlet Assuming Moody Discharge Flow for 100F Subcooled Water

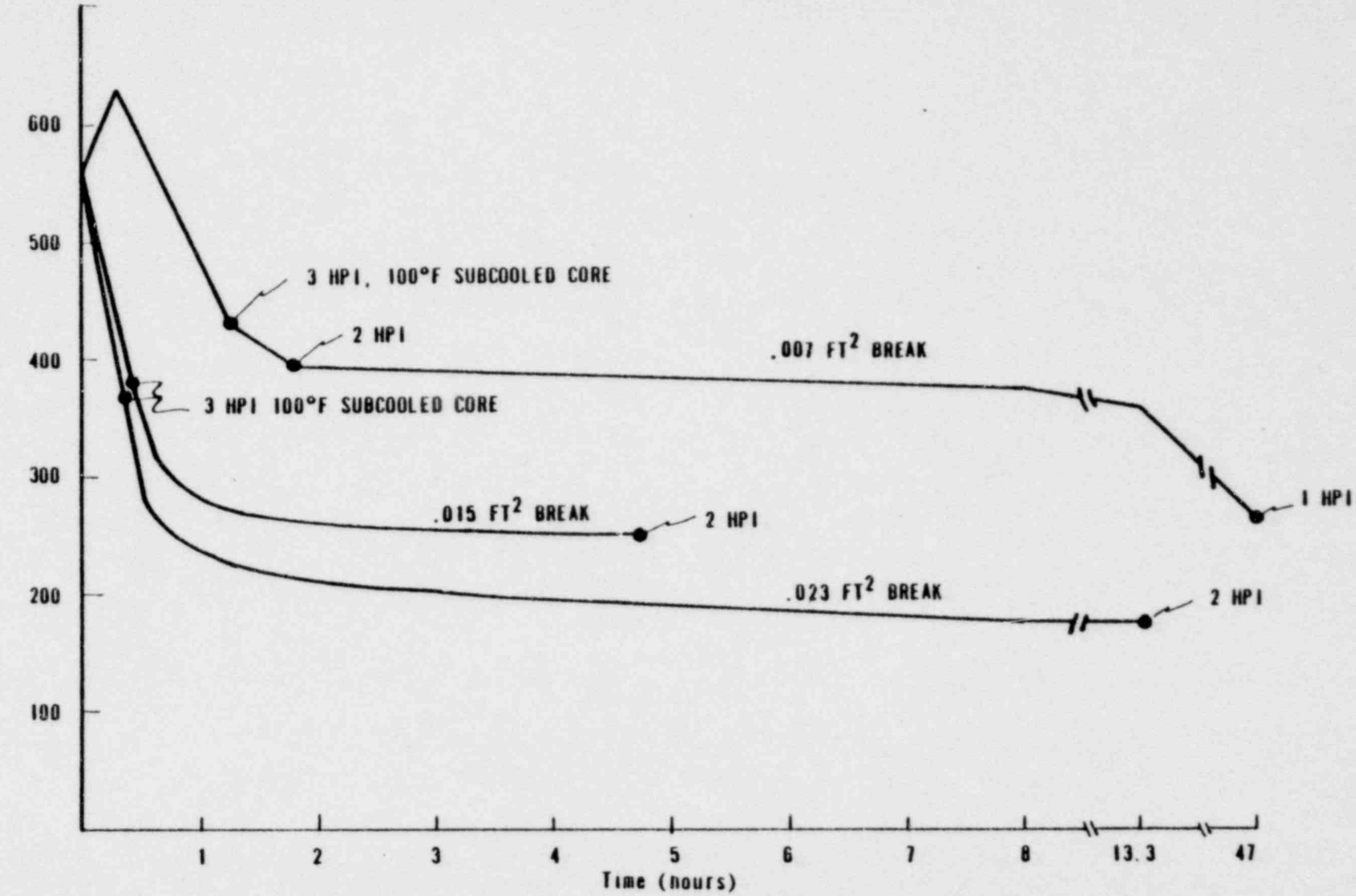


Figure 2-28. RV Pressure Vs Time, 0.007-, 0.015-, and 0.023-ft^2 Pressurizer Breaks With HPI Throttling at 100F Subcooled Core Outlet Assuming Moody Discharge Flow for 100F Subcooled Water

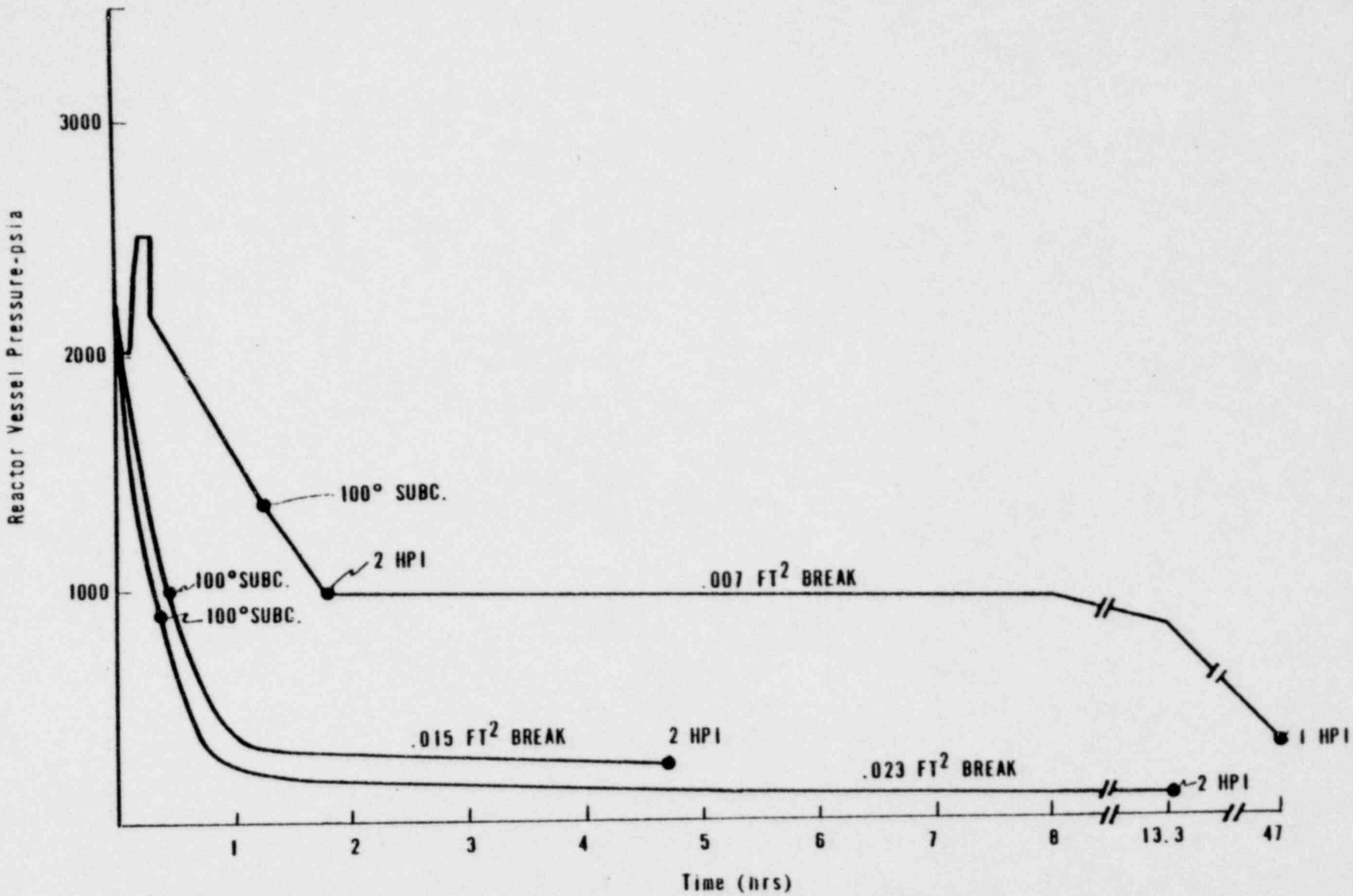


Figure 2-29. HPI Flow Vs Time, 0.007- and 0.023-ft^2 Pressurizer Break With Operator Action

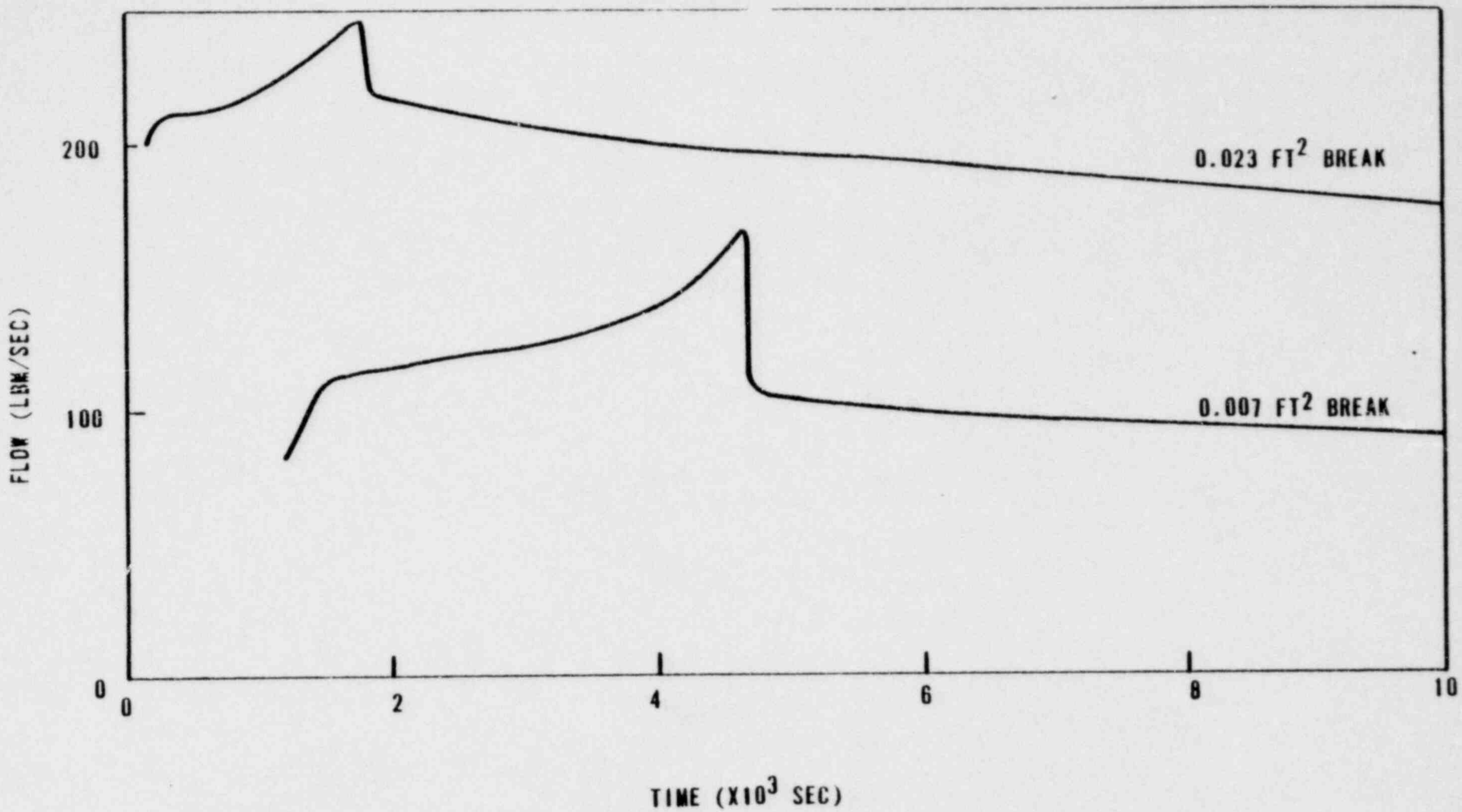


Figure 2-30. Vent Valve Flow Vs Time, 0.007- and 0.023-ft^2 Pressurizer Break With Operator Action

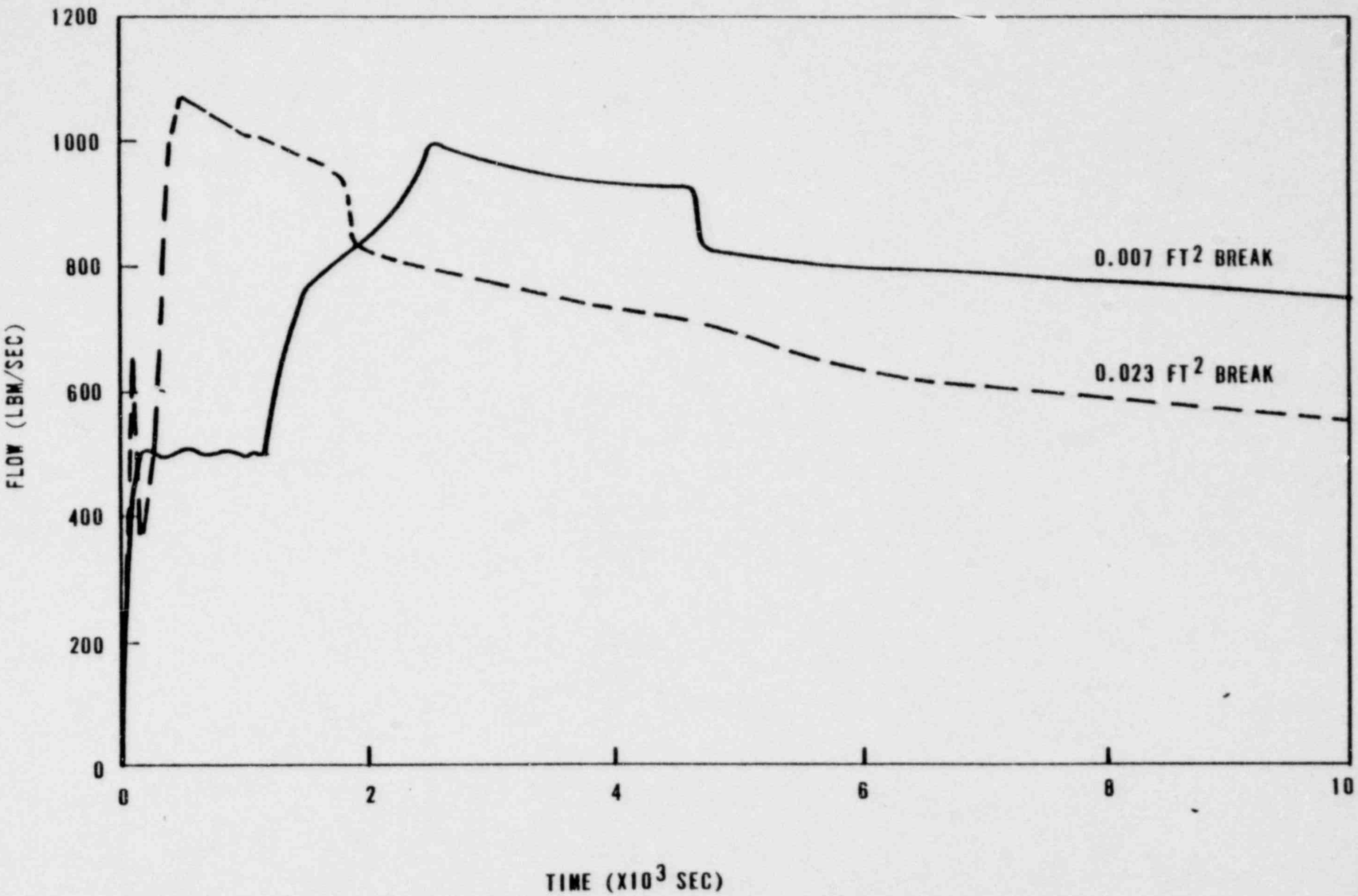
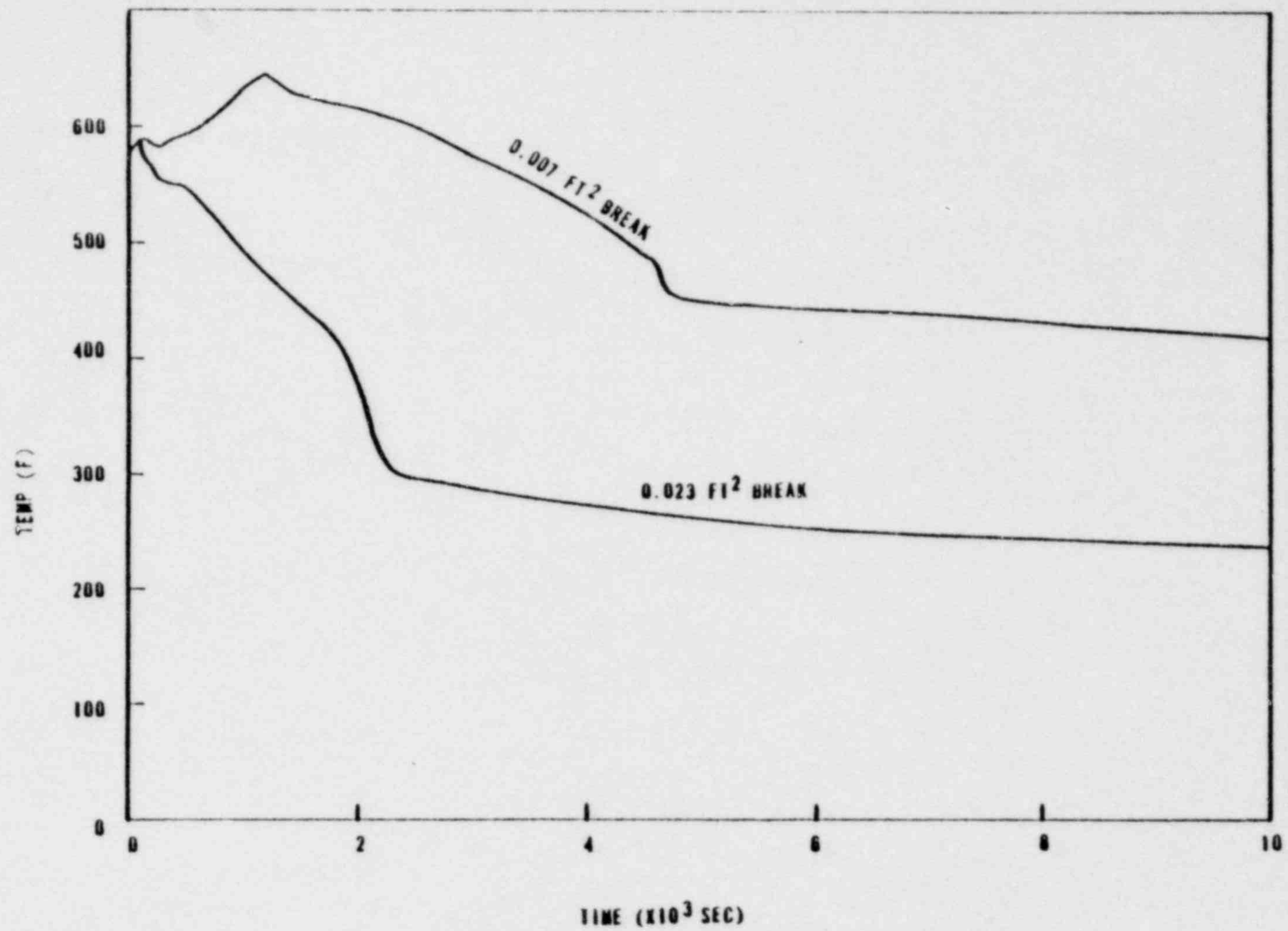


Figure 2-31. Vent Valve Fluid Temperature Vs Time, 0.007- and 0.023-ft^2 Pressurizer Break With Operator Action



3. REACTOR VESSEL DOWNCOMER MIXING

The fracture toughness of the RV is a function of the temperature of the downcomer fluid next to the RV wall. Therefore, to derive the RV fracture toughness, the downcomer fluid mixing must be evaluated to determine the fluid temperature next to the RV wall. The various mixing models used in the analyses are presented below.

3.1. CRAFT Mixing (Case 1, Table 1-1)

The LOCA analyses described in section 2 determine mixed mean downcomer temperature. One node (node 1 in Figure 2-2) is used in the CRAFT model for the RV downcomer; it calculates the RV downcomer temperature assuming complete mixing of all fluids in the downcomer. For the extended loss-of-feedwater transients, the fluids entering the downcomer are the cold leg loop flow (only at the very beginning of the event), the HPI flow, and the vent valve flow. After the loop flow stops, only the HPI and vent valve flows remain. The velocities of the flows are very low (less than 1 fps in the cold leg and downcomer); consequently, complete mixing in the downcomer as determined by CRAFT may not occur.

3.2. MIX2 Mixing (Cases 2 and 3, Table 1-1)

Because the vent valve flow offers the most benefit with regard to downcomer water heatup, an effort was made to analytically predict the mixing. MIX2, a two-dimensional code under development at B&W, was used to model the region of the downcomer where mixing takes place. MIX2 solves the continuity, momentum, and energy equations in both space and time for single-phase, compressible water flow. The solution method employed is the implicit-continuous-Eulerian technique. The gravity effect is also included in the analysis to handle natural circulation or mixed convection problems. The inputs to MIX2 are the system pressure, inlet flow velocities, and temperatures. The turbulence exchange is modeled by an effective viscosity model. The outputs of MIX2 are the local velocity and temperature fields of the domain being analyzed.

3.2.1. Analysis Assumptions

Breaks of various sizes, both with and without operator action, were analyzed. The 0.023-ft^2 break with operator action throttle the HPI to maintain the core outlet 100F subcooled (case 3) is given herein as an example case because loss of natural circulation occurs faster for this break than for the 0.007- or 0.015-ft^2 breaks (~540 seconds). As such, it would be the controlling break for minimum cooldown times for small breaks, which require heating of the HPI flow by hotter vent valve water.

The downcomer and part of the cold leg are modeled by a 18×12 grid system. HPI flow is modeled by a uniform stream going from right to left, and the vent valve flow is modeled by the stream coming from the top (as shown in Figure 3-1). The two streams of different temperatures are mixed in the downcomer region. It must be pointed out that MIX2 is a two-dimensional code, so assumptions must be made to account for the flow distribution in the circumferential direction, i.e., how the HPI and downcomer flows spread out in the downcomer annulus.

An obvious assumption that can be made is that both the HPI water and the vent valve flow spread out quickly and distribute uniformly in the downcomer annulus. Calculational results indicated that the fluid temperature in the downcomer obtained from this analysis is generally quite high, and it provides an optimistic estimate for the vessel wall temperature. Actual conditions may or may not approach this model. On the other hand, we can assume that the HPI water does not spread around the annulus, while the vent valve flow distributes uniformly. This is a much more conservative assumption, and the calculational results given herein as an example and used in cases 2 and 3, Table 1-1 use that assumption.

These calculations were performed with and without accounting for gravity effects. It is also noted that because of modeling limitations, the mixing boundary condition used 40F water in the cold leg approximately 2 feet from the downcomer. If the length of cold leg pipe from the downcomer to the HPI injection point had been modeled, additional mixing would probably occur. The locations of the HPI nozzles on the cold leg pipes and their distance from the downcomer are shown for lowered- and raised-loop plants in Figures 3-2 and 3-3, respectively.

3.2.2. Analysis Performed

The following parameters were used for the mixing analysis:

Number of cells in X-direction	18
Number of cells in Y-direction	12
δX, ft	0.139
δY, ft	0.467
Turbulent diffusivity ν_T, 10^{-4} ft^2/s	1.0
HPI temperature, F	40

System pressure, HPI and vent valve flow velocities and temperatures were obtained from the analysis described in section 2.

The results of the MIX2 calculations are fluid temperature profiles in the downcomer immediately below the nozzle. Figure 3-4 illustrates typical downcomer fluid temperature profiles both with and without gravity effects. It can be seen that the case with the gravity effect has a more moderate temperature gradient in the downcomer. This is because the gravity effect tends to cause the HPI flow to settle and flow along the bottom of the cold leg, creating space for the hotter vent valve flow to come in to the upper part of the cold leg pipes. This phenomenon can be illustrated by the two schematic flow maps appearing as Figures 3-5 and 3-6. As a result of the gravity effect, considerable mixing takes place in the cold leg and the stratification effect is less pronounced.

The resulting downcomer fluid temperature at the vessel wall directly beneath the cold leg nozzle for the 0.023-ft^2 break with operator action (case 3, Table 1-1) is shown in Figure 3-7.

3.2.3. Summary

Based on the results of the mixing analysis, it is concluded that mixing will occur and that the HPI water will be heated by the hotter vent valve water. Quantification of the mixing benefit is more difficult. If the circumferential distribution assumptions of the previous section are accepted (uniform vent valve flow distribution, concentrated HPI), then the analyses show the mixing phenomenon could provide as much as 150F of heatup based upon 540F vent valve water. In addition, the heatup could be greater than 150F if the existing 17 feet of cold leg between the HPI injection point and the downcomer were used in the analysis rather than 2 feet. However, the uncertainty in the circumferential distribution of flow and in the analytical predictions (both computational and modeling) makes the exact benefit difficult to determine.

3.3. Bounding Analyses (Case 4, Table 1-1)

Because of the uncertainty in the degree of HPI-vent valve fluid mixing actually taking place in the downcomer bounding analyses were performed. These analyses assumed essentially no mixing. They are discussed more fully in sections 4 and 5.

Figure 3-1. Numerical Model of MIX2 Analysis

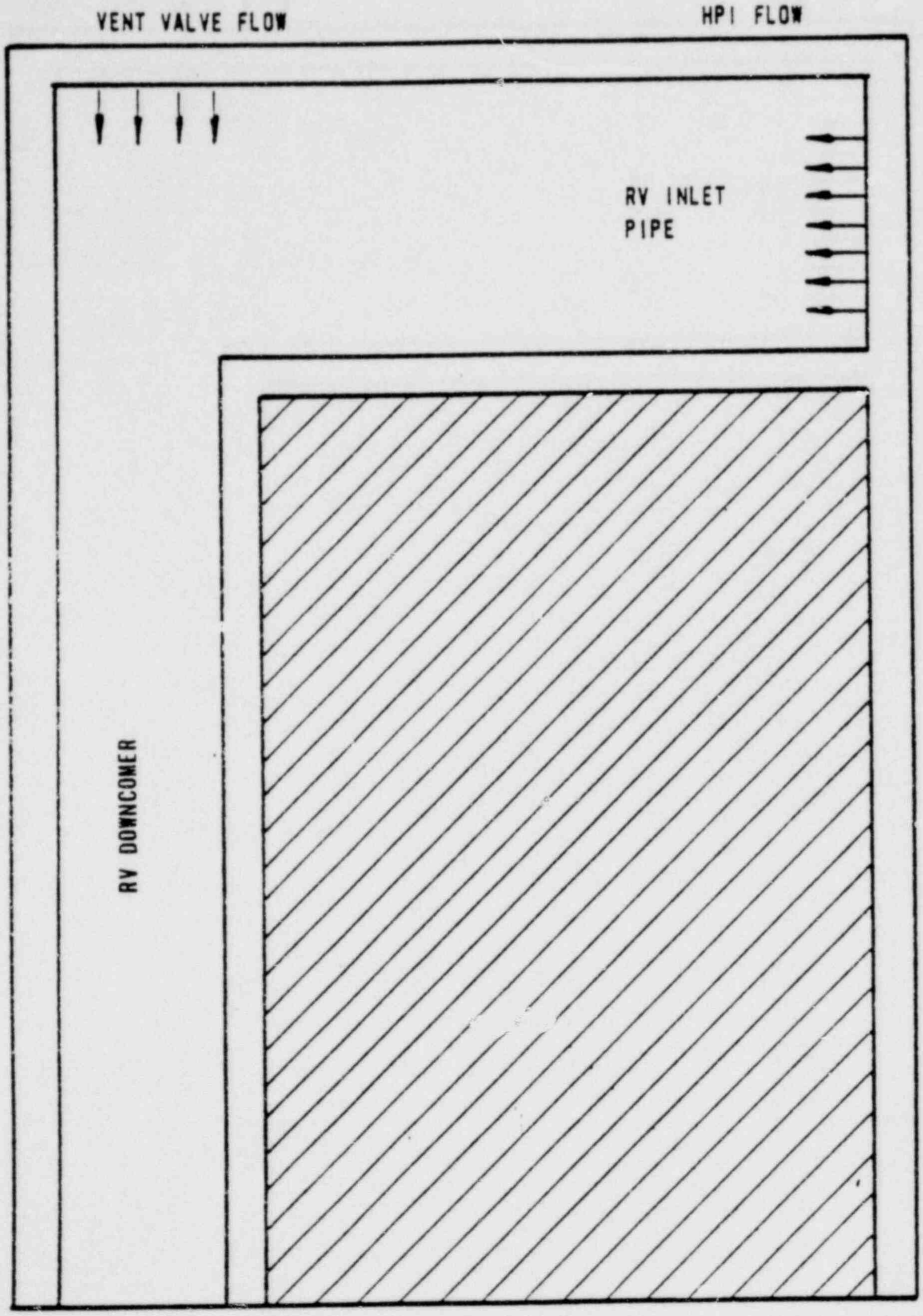


Figure 3-2. Locations of HPI Nozzles (One on Each Cold Leg Pipe) — Lowered-Loop 177-FA Plants

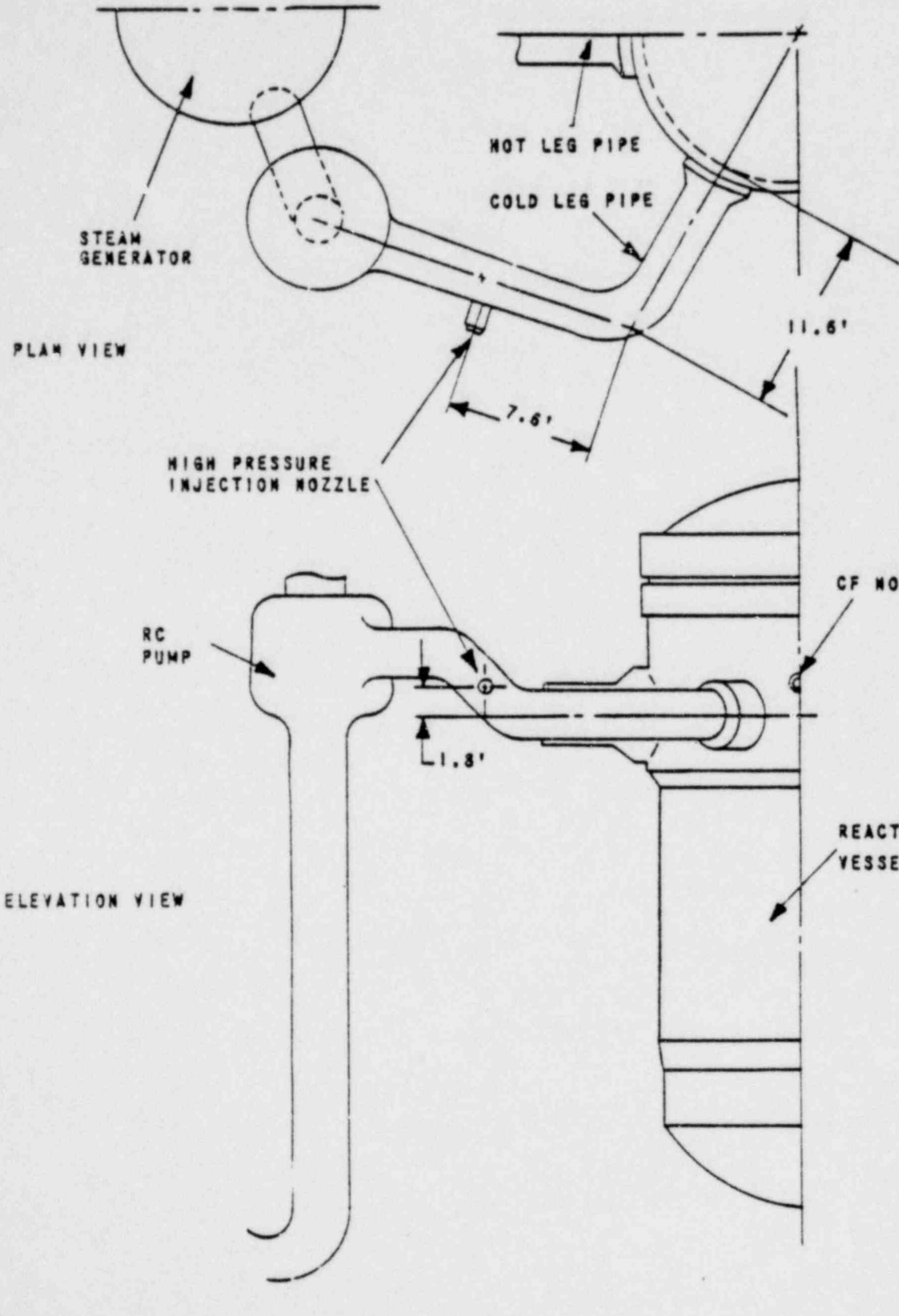


Figure 3-3. Locations of HPI Nozzles (One on Each Cold Leg Pipe) — Raised-Loop 177-FA Plants

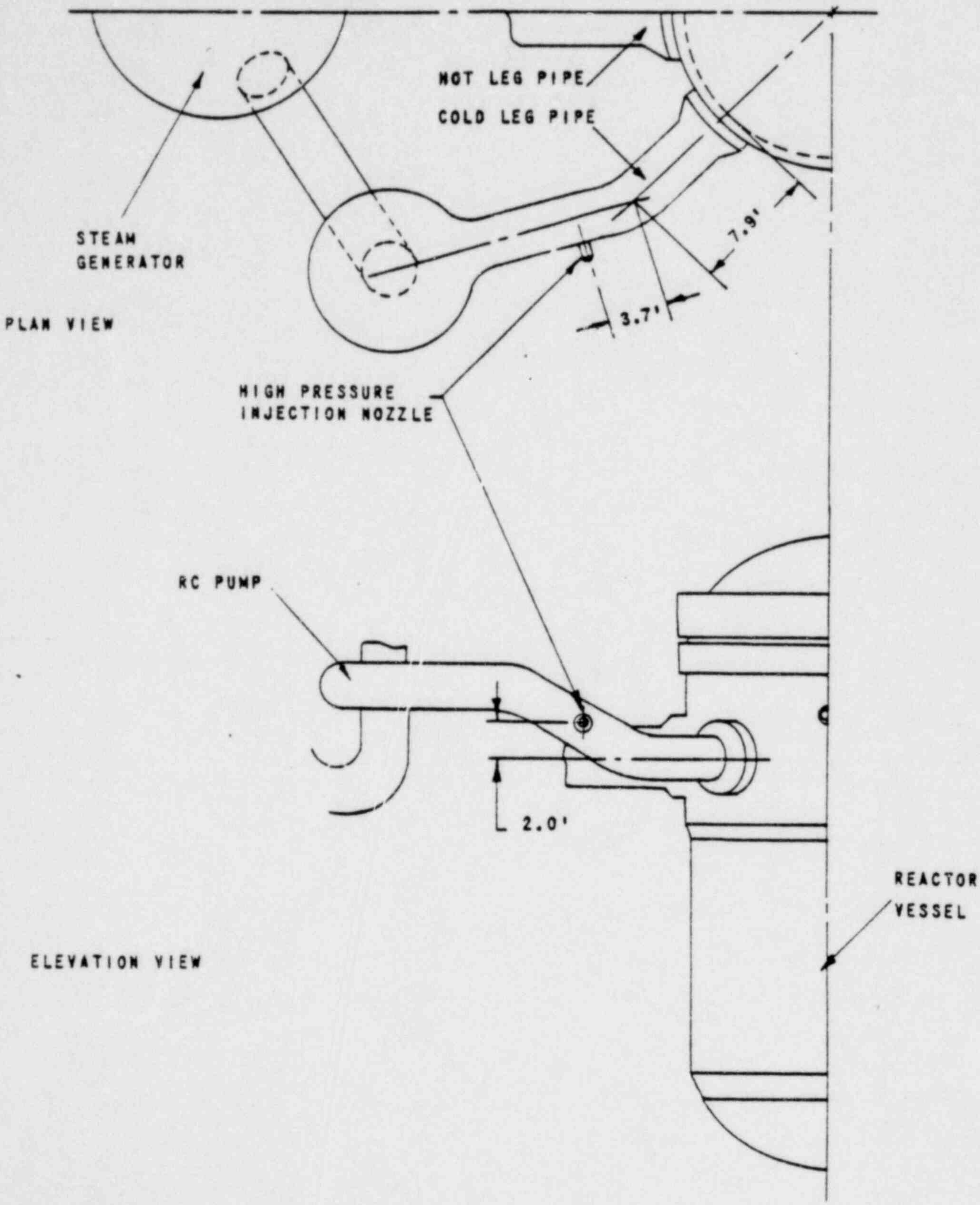


Figure 3-4. Downcomer Fluid Temperature Profiles — HPI Temperature 40F, VV Temperature 540F, $(\dot{M}_{HPI}/\dot{M}_{VV}) \sim 1.5$

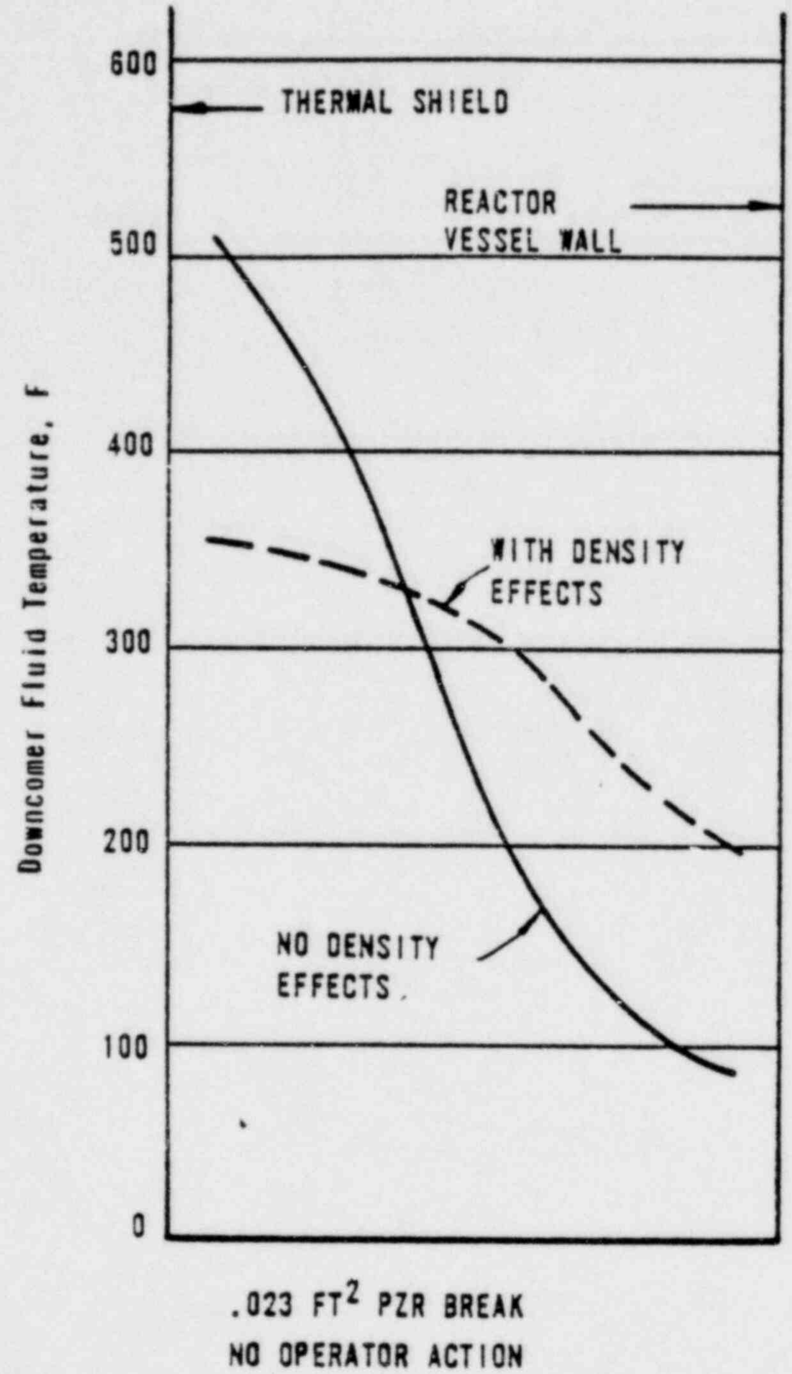


Figure 3-5. Downcomer/Cold Leg Velocities Without Density Effects

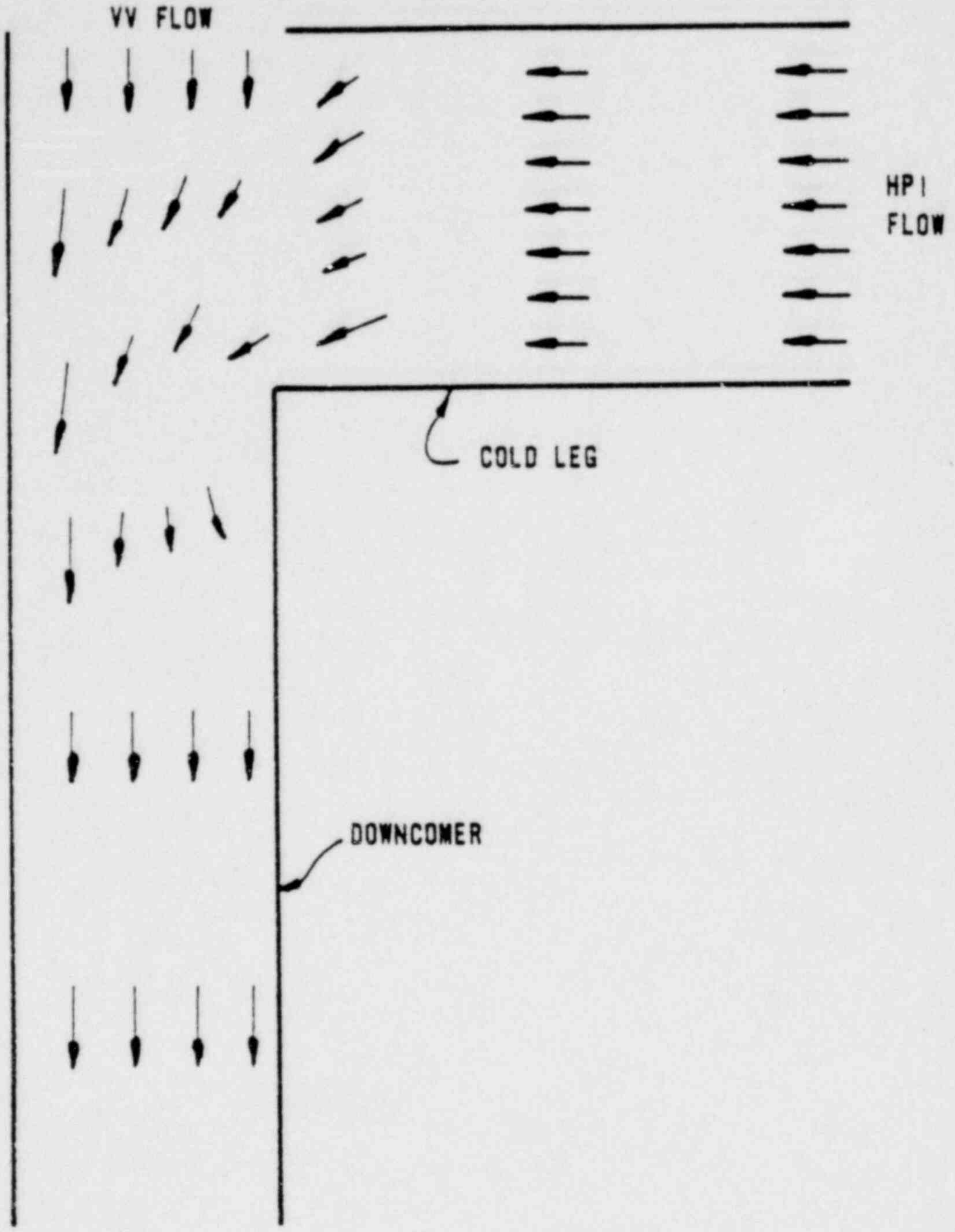


Figure 3-6. Downcomer/Cold Leg Velocities
With Density Effects

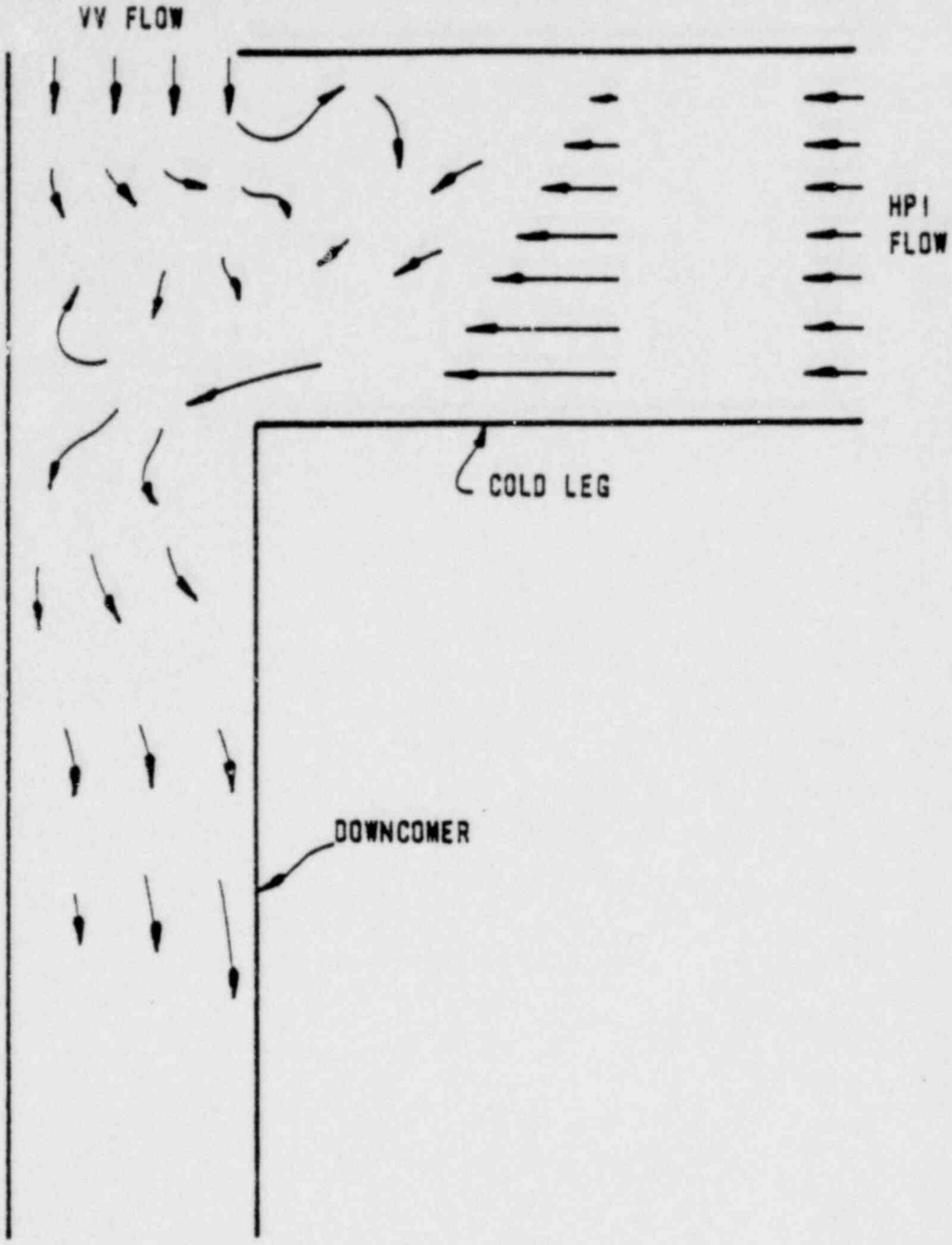
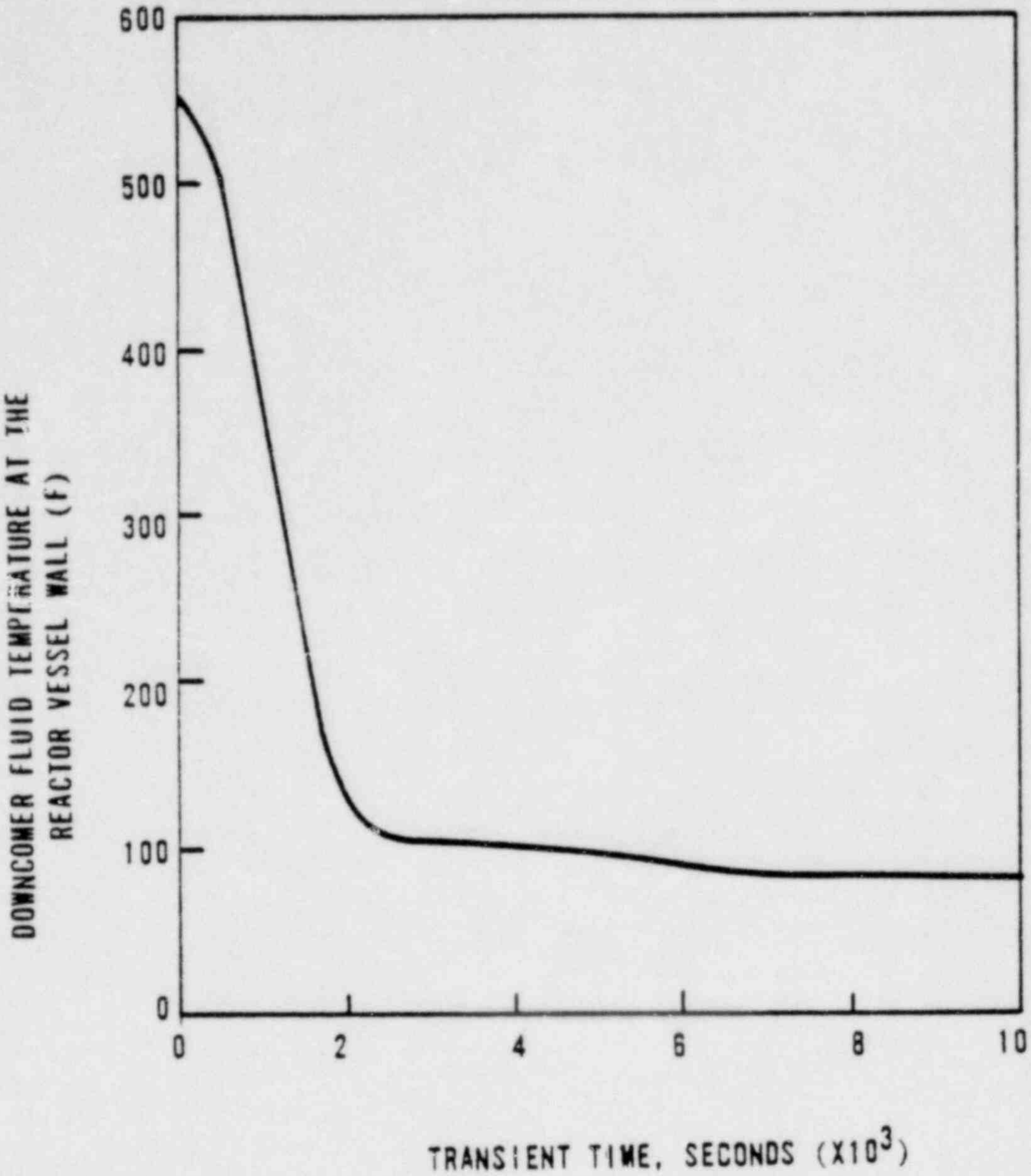


Figure 3-7. Downcomer Fluid Temperature at the RV Wall, 0.023-ft^2 Pressurizer Break With Operator Action, MIX2 Results



4. REACTOR VESSEL COOLDOWN ANALYSES

Once the downcomer fluid temperature next to the reactor vessel wall is determined using the methods described in section 3, the temperature profile in the reactor vessel versus time must be calculated. These reactor vessel cooldown analyses are described in this section.

4.1. 0.007-ft^2 Pressurizer Break Without HPI Throttling (Case 1, Table 1-1)

The initial analysis performed to evaluate the thermal shock concern was the 0.007-ft^2 pressurizer break without HPI throttling (case 1). The downcomer fluid temperature used in this analysis was the CRAFT determined mixed mean temperature, as described in section 3.1. The reactor vessel cooldown analysis performed for case 1 employed the BEFRAN[3] computer code. This analysis uses a one-dimensional, cylindrical heat transfer calculation which assumes a constant film coefficient. Subsequent reactor cooldown analyses (cases 2-4) used a more detailed calculation, as described below.

4.2. 0.007- and 0.023-ft^2 Pressurizer Breaks With Operator Action to Throttle HPI Flow (Cases 2-4, Table 1-1)

As mentioned in section 2.3 and described in detail in section 5, fracture mechanics analyses performed on the 0.007-ft^2 break without operator action to throttle HPI flow produce unacceptable results after several hours. As a result, analyses which included operator action were performed (cases 2-4, Table 1-1). The reactor vessel cooldown analyses performed on these cases is described herein. The downcomer fluid temperatures versus time used as input to cases 2 and 3 were the MIX2 results described in section 3.2. The downcomer fluid temperature versus time used in case 4 represents an extremely conservative bounding analysis. Except for the differences in the downcomer fluid temperature used as input, the reactor vessel cooldown analyses performed for cases 2-4 employ the same techniques. The bounding analysis (case 4) is described in detail in this section.

4.2.1. Bounding Reactor Vessel Cooldown Analyses (Case 4, Table 1-1)

As previously discussed, uncertainties exist in determining the extent of water mixing in the reactor vessel downcomer; therefore, to bound the overall concern, some conservative analyses were conducted. Four analyses assuming no HPI-vent valve flow mixing were performed to determine the RV thermal gradients versus time. These situations could be perceived as HPI flowing into the RV downcomer and then streaming down along the RV wall with no mixing with vent valve flow. For these analyses complete mixing was assumed in the RV downcomer while RC loop flow existed. Once the RC loop flow stopped, the RV downcomer temperature was rapidly dropped over 50 seconds to the assumed HPI temperature and then sustained there (Figure 4-1). In one analysis, the final downcomer temperature was assumed to be 40F, which corresponds to the minimum BWST temperature. In the other three analyses, the final downcomer temperatures were 90, 120, and 150F, which reflect various amounts of mixing.

Reactor vessel temperatures during a 0.023-ft^2 break with operator action were calculated. Fluid conditions were taken from the analyses described in section 2.3. Wall surface heat transfer is obtained from the larger of (turbulent) forced and free convection heat transfer. The transient one-dimensional wall energy transfer problem was solved using the explicit Euler technique. Wall temperatures and temperature gradients are obtained for nominal conditions, for varying injection temperatures, and (by separate analysis) with allowances for azimuthal conduction in the wall.

4.2.1.1. Assumptions

1. Fluid Heating — After loop flow stagnates, the downcomer bulk fluid temperature is set arbitrarily to temperatures of 40, 90, 120, or 150F.

2. Initial Temperatures — Gamma and neutron flux attenuation in the wall are used to set the initial temperature distribution in the reactor vessel.

3. Vessel Outer Surface — The outer surface of the RV is assumed to be perfectly insulated.

4. Film Heat Transfer — Film heat transfer variations in opposing mixed convection are ignored. The film heat transfer coefficient (HTC) is set to the larger of the (pure, turbulent) forced and free HTCs.

4.2.1.2. Conditions

1. Geometry — The vessel wall is 8.4375-inch SA-508 class 2 steel, clad on the inner face with 0.1875-inch stainless steel; the inside diameter is 170.625 inches. The inner boundary of the downcomer is the thermal shield, with a 151-inch outer diameter. The heated length is 15 feet (but is of no consequence here, without fluid heating).

2. Initial Conditions — Initial conditions are those of an operating 177-FA plant at full power. Coolant flow is 131 mlbm/h at 555.4F and 2200 psia. Flux attenuation in the vessel wall generates 24 kBtu/h-ft[4] at the inner surface, attenuating approximately exponentially with wall depth (with a linear attenuation coefficient of 8.4/ft). Film heat transfer is by forced convection, and the total temperature rise across the vessel wall is 17F.

4.2.1.3. Analyses

1. Fluid Conditions — Fluid conditions are used as input except that loop flow from CRAFT is added to injection flow after HPI initiation at 140 seconds, and HPI flow is multiplied by 4.5 to account for fluid streaming after loop flow stagnates at T=540 seconds (the ratio of downcomer circumference to inlet nozzle diameter is 4.5).

2. Film Heat Transfer — In opposing flow, with forced convection downward along a vertical heated wall, heat transfer may differ from either pure forced or pure free convection. In laminar flow, opposing heat transfer is usually degraded from pure (forced or free) convection, but this influence in turbulent flow is unknown. Thus, the film heat transfer herein is estimated by evaluating the pure forced convection HTC and selecting the larger of the two, Figure 4-2. The forced convection HTC is as follows[4]:

$$H_{forced} = 0.023\ K/D\ Re^{0.8}\ Pr^{0.43}\ (Btu/h\text{-}ft^2\text{-}F)$$

where

K = fluid thermal conductivity, Btu/h-ft-F,

D = hydraulic diameter, ft ($D \sim 2W$ where W = downcomer width),

Re = Reynolds number, the ratio of inertial to viscous forces, $Re = VD/\nu$.

V = fluid velocity, fps,

ν = kinematic viscosity, ft^2/s,

Pr = Prandtl number, the ratio of storage to conduction energy transfer, $Pr = C_p \mu/K$,

C_p = specific heat, Btu/lbm-F,

μ = dynamic viscosity, lbm/h-ft.

Because H_{forced} is proportional to $V^{0.8}$, it decreases abruptly as loop flow stagnates (Figure 4-2).

The free (or natural) convective HTC is[5]

$$H_{free} = 0.094\ K/L\ (Gr\ Pr)^{1/3}$$

where

L = heated length, ft,

Gr = Grashof number, the ratio of buoyant to inertial force, $Gr = g\beta\Delta TL^3/\nu^2$,

g = gravitational acceleration, 32 ft/s^2,

β = fluid thermal expansivity, $\beta = 1/\rho\ \partial\rho/\partial T$ (1/F),

ΔT = governing temperature difference, wall to fluid, F.

Notice that (turbulent) H_{free} is apparently independent of heated length. Also, as H_{forced} decreases, the wall-to-fluid temperature difference increases, as does H_{free} (Figure 4-2).

3. Vessel Wall Heat Transfer — The energy equation in the wall:

$$\rho C_p \frac{\partial T}{\partial t} = K\left[\frac{\partial^2 T}{\partial r^2} + \frac{1}{r}\frac{\partial T}{\partial r}\right],$$

with

$T = T(r,t)$ and boundary conditions (inner surface convection), $K\ \partial T/\partial r\ (r = r_i) = H[T(r = r_i) - T_{bulk}]$, and (insulated outer surface) $\partial T/\partial r\ (r = r_o) = 0$,

is solved by discretization in space and application of the Euler explicit method to solve the approximate system of ordinary differential equations. Temperature-dependent properties are employed,[6,7] as are the HTC modeling techniques previously described.

The solution of the energy equation is verified by comparing it to the slab approximation (valid for large inner radius):

$$\rho C_p \frac{\partial T}{\partial t} = K \frac{\partial^2 T}{\partial x^2}$$

The adequacy of the temporal incrementation is verified by doubling the number of time steps and comparing the standard and refined solutions. Spatial and temporal increments of $\Delta r = 1/8$ inch and $\Delta T = 1/10$ second are used.

4.2.1.4. Results

The resultant film HTC changes from forced to free at 7 minutes into the transient (Figure 4-2). Thus, free convection governs the bulk of the transient and ranges from $h \cong 200$ Btu/h-ft^2-F at t=10 minutes to $h \cong 100$ at t=1 hour, decreasing with wall surface temperature. Wall temperature profiles respond to downcomer temperatures (Figure 4-3); as loop flow stagnates, the downcomer temperature approaches the injection temperature, and wall surface heat transfer increases markedly. By t=10 minutes, the inner surface temperature approaches that of the injected fluid, but the outer wall temperatures have barely changed.

The assumed downcomer temperature was varied to assess its impact. As expected, the resultant wall temperature profiles are less sloped with raised downcomer temperatures, especially at later transient times (Figure 4-3).

Tangential conduction was investigated using FELCON, a transient, two-dimensional, finite element conduction code.[8] The wall temperature response without tangential conduction was obtained by setting the entire inner surface to 40F at t > 0. Tangential conduction effects were then introduced by setting 2 feet of the inner surface to 40F, while retaining the remaining 8 feet at 550F, and extracting the wall temperature profiles in the cooled region. As with increased HPI temperatures, tangential conduction decreases the wall temperature gradient, particularly at later transient times (Figure 4-4).

As discussed earlier, these results are very conservative and present a bounding case to the thermal shock question. The results of the fracture mechanics analysis for these cases are discussed in section 5.

Figure 4-1. Downcomer Fluid Temperature, 0.023-ft^2 Break (Typical Case 4, Table 1-1)

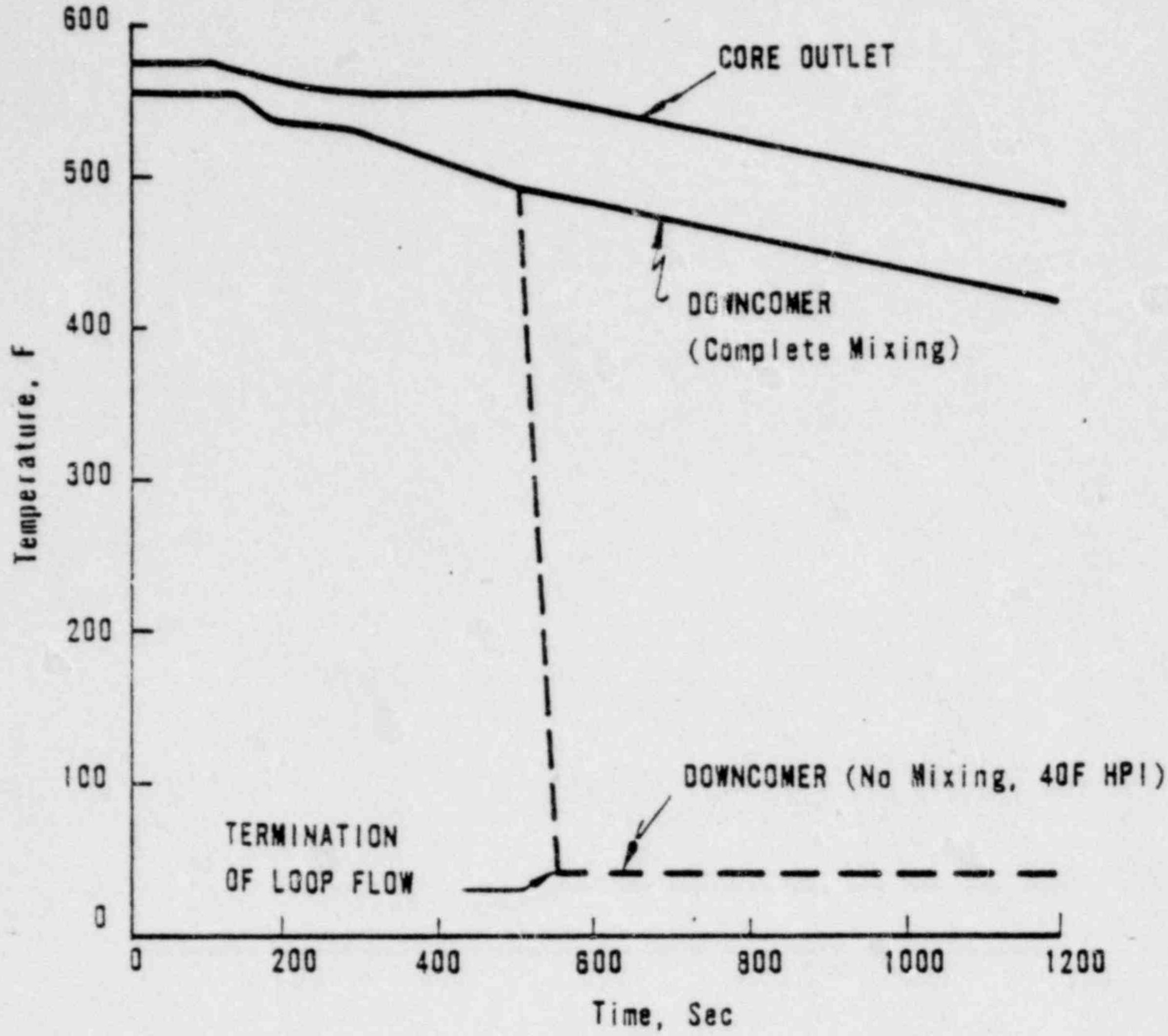


Figure 4-2. Heat Transfer Coefficient Vs Time
(Typical Case 4, Table 1-1)

WALL CONVECTIVE HEAT TRANSFER COEFFICIENT (h)
VERSUS TRANSIENT TIME.

$$h_{forced} = 0.023 \frac{K}{D} Re_D{}^{0.8} Pr^{0.4}$$

$$h_{free} = 0.094 \frac{K}{L} (Gr_L\ Pr)^{1/3}$$

.023 FT^2 PZR BREAK

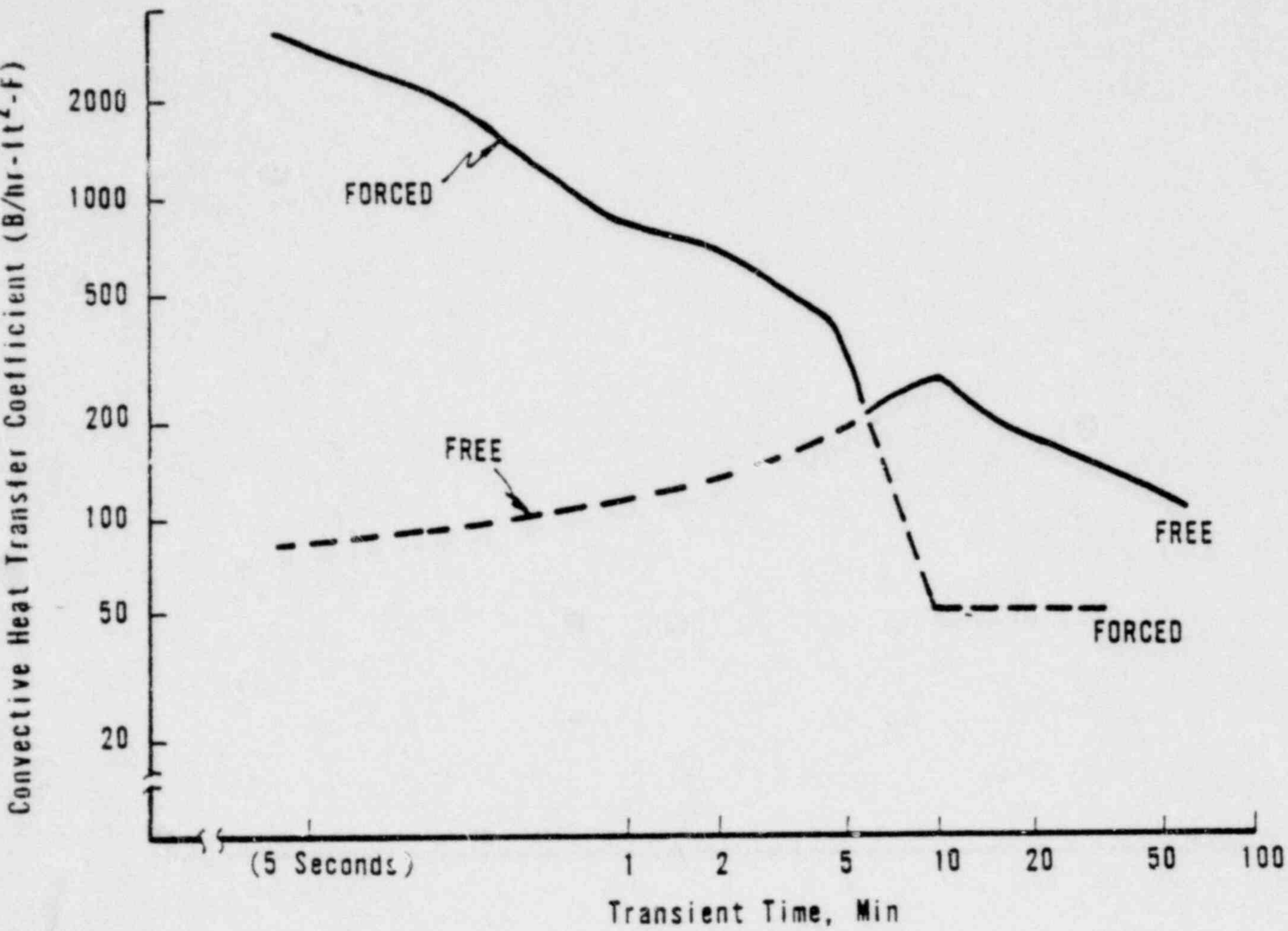


Figure 4-3. Transient Wall Temperature Profiles, 0.023-ft^2 Pressurizer Break (Typical Case 4, Table 1-1)

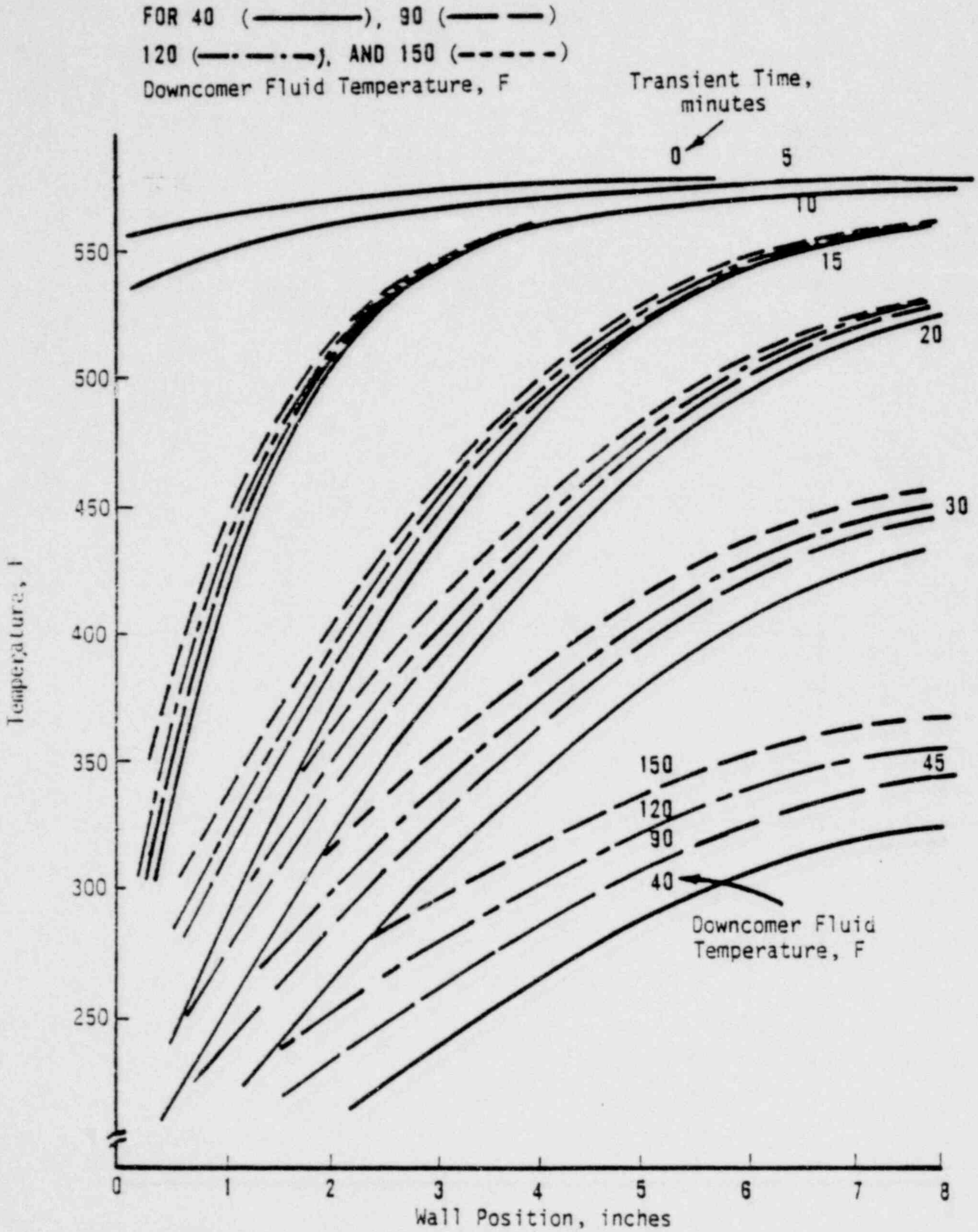
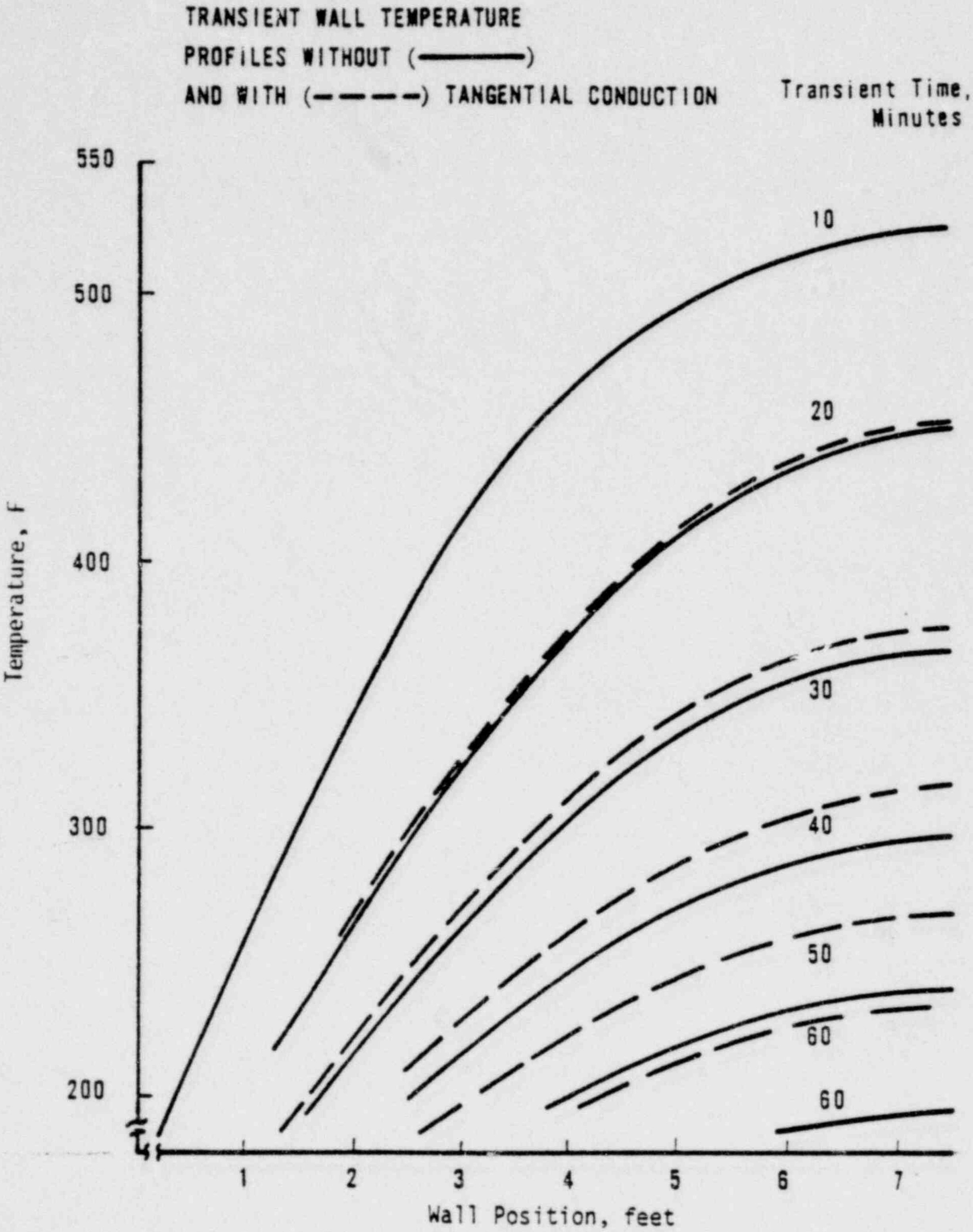


Figure 4-4. Tangential Conduction



5. FRACTURE MECHANICS ANALYSES

The transient cases summarized in Table 1-1 have been evaluated to determine whether crack initiation is predicted. Subsequent crack arrest and the possiblity of warm prestressing are also evaluated.

5.1. Methodology

The linear elastic fracture mechanics (LEFM) analytical technique has been used to evaluate both types of transients discussed above. The validity of LEFM for predicting crack initiation and arrest has been demonstrated by the thermal shock experiments conducted in the HSST Program at the Oak Ridge National Laboratory.

5.1.1. Thermal Stress Intensity Factors

The thermal stresses due to the temperature gradient through the thickness of the vessel are computed from the following general relationship:

$$\sigma_r = \frac{\alpha E}{1 - \nu}\frac{1}{r^2}\left(\frac{r^2 - a^2}{b^2 - a^2}\int_b^a Trdr - \int_a^r Trdr\right),$$

$$\sigma_\theta = \frac{\alpha E}{1 - \nu}\frac{1}{r^2}\left(\frac{r^2 + a^2}{b^2 - a^2}\int_a^b Trdr + \int_a^r Trdr - Tr^2\right),$$

$$\sigma_z = \frac{\alpha E}{1 - \nu}\left(\frac{2}{b^2 - a^2}\int_a^b Trdr - T\right)$$

where

α = coefficient of thermal expansion,

E = elastic modulus,

ν = Poisson's ratio,

$\sigma_r, \sigma_\theta, \sigma_z$ = stress components,

r, θ, z = reactor vessel axes.

The temperature distribution through the wall can be assumed to be approximately parabolic. The stress equations are evaluated by assuming the following general representation of the temperature distribution through the vessel.

$$T = A(b - r)^2$$

where

T = temperature,

r = vessel radial direction.

A, b = constants.

The stress intensity factors are then computed by the following generalized relationship[11]:

$$K_I = \sqrt{\pi c}\left(N_0 F_1 + \frac{2c}{\pi} N_1 F_2 + \frac{c}{2} N_2 F_3 + \frac{4}{3}\frac{a^3}{\pi} N_3 F_4\right)$$

where

c = crack depth, radially,

N_0, N_1, N_2, N_3 = coefficients of crack opening stress polynomial,

F_1, F_2, F_3, F_4 = geometry magnification factors depending on crack depth.

5.1.2. Pressure Stress Intensity Factors

The components of stress in the RV beltline region due to pressure are computed as follows:

$$\sigma_r = Pi \text{ at } r = a,$$

$$\sigma_z = \frac{a^2 Pi}{b^2 - a^2},$$

$$\sigma_\theta = \frac{a^2 Pi}{b^2 - a^2}\left(1 + \frac{b^2}{r^2}\right)$$

where

$\sigma_r, \sigma_z, \sigma_\theta$ = radial, axial, and hoop stresses,

r, z, θ = reactor vessel coordinate axes.

The stress intensity factors are computed from the same generalized relationship presented in section 5.1.1.

5.1.3. Welding Residual Stress Intensity Factors

The residual stresses due to welding are computed on the basis of the evaluation of residual stresses in heavy weldments conducted by Feril, Juhl, and

Miller.[9] The residual stress distribution through the vessel thickness is given below for three types of weld geometry shown in Figure 5-1.

Longitudinal welds: $\sigma(x)/S_y = 0.12 - 0.36x + 0.18x^2$

Circumferential welds:

Type 1: $\sigma(x)/S_y = -0.06 + 0.18x^2$ (Single V)

Type 2: $\sigma(x)/S_y = 0.12 - 0.72x + 0.72x^2$

where

$\sigma(x)$ = residual stress distribution with directions as shown in Figure 5-1,

S_y = yield stress,

$x = a/t$ (t - thickness), $o \leq a \leq t$.

The stress intensity factors are computed from the generalized relationship presented in section 5.1.1.

5.1.4. Material Fracture Toughness Data

The material fracture toughness data were obtained from the reference curves of the ASME Boiler & Pressure Vessel Code, Section XI, Appendix A.[10] Figure A-4200-1 gives lower bound static crack initiation toughness, K_{IC}, and crack arrest toughness, K_{IA}, as functions of metal temperature and material reference temperature, RT_{NDT}.

The material reference temperature, RT_{NDT}, is adjusted to account for irradiation embrittlement effects. The amount of adjustment to be added to the initial reference temperature is computed from USNRC Regulatory Guide 1.99. The adjustment is a function of the material's weight percent of copper and phosphorus and the accumulated neutron fluence, n/cm^2. The peak neutron fluence for the beltline region on the vessel inner surface is adjusted to account for specific weld locations axially and circumferentially. The neutron fluence attenuation through the vessel thickness is also taken into account.

Taking these factors into consideration, the controlling material was found to be the longitudinal weld seam, WF-70, in the lower shell of the Rancho Seco vessel. The properties of this material have been used as the base case for which all results have been quantified. The applicability of the results to reactor vessels other than that of the Rancho Seco plant and with lower irradiation levels is discussed in section 5.4.

Case 1 presented below (section 5.3.2.1) was analyzed using base material properties (Rancho Seco) and an accumulated neutron fluence corresponding to 6.0 EFPY* as a basis. (As of November 3, 1980, Oconee 1, the lead B&W plant, had accumulated 4.5 EFPY. Rancho Seco had accumulated 3.2 EFPY as of the same date.) The peak neutron fluence for the beltline region on the vessel inner surface corresponding to 6.0 EFPY is 4.0×10^{18} n/cm^2. The computed adjusted reference temperature on the inner surface was 246F.

Cases 2 (section 5.3.2.2) and 4 (section 5.3.2.3) presented below were performed using base material properties (Rancho Seco) at an accumulated neutron fluence corresponding to 3.8 EFPY. The corresponding vessel beltline inner surface peak neutron fluence and computed adjusted reference temperature were 2.5×10^{18} n/cm^2 and 200F, respectively.

Case 3 (section 5.3.2.2) presented below was analyzed using base material properties at both the 3.8 EFPY irradiation values of Cases 2 and 4 and using values corresponding to 4.8 EFPY. The vessel beltline inner surface peak neutron fluence and computed adjusted reference temperature corresponding to 4.8 EFPY were 3.1×10^{18} n/cm^2 and 220F, respectively.

5.2. Flaw Parameter Assumptions

The reactor vessels in question have not operated long enough to have been subjected to an inservice inspection. Based on the shop inspections and the ASME Section XI baseline inspections, there is no evidence of flaws in any of these vessels. However, in order to perform the fracture mechanics analysis, the existence, location, orientation, and size of flaws were assumed. Surface flaws with the major axis oriented longitudinally and the minor axis oriented radially were postulated in the controlling weld metal. While the critical flaw size in the radial direction was a product of the fracture mechanics analysis, the aspect ratio for the initial flaw was assumed to be 6:1 as recommended by Section III, Appendix G of the ASME Code, and the aspect ratio for arrest and subsequent initiations was assumed to be infinitely long. This assumption is consistent with the crack propagation results from the thermal shock experiments conducted in the HSST Program.

- - - - -

*EFPY: effective full-power year.

Since the "critical" flaw size was unknown, a spectrum of sizes ranging from 0.21 to 3.0 inches with several aspect ratios were evaluated. Results showed that for the slower temperature changes (lesser degree of thermal shock — case 1, Table 1-1) deep flaws were critical but did not become limiting until several hours into the transient. However, for the fast transients (severe thermal shock — cases 2-4, Table 1-1) the shallow flaws were critical because they became "cold" at much higher stresses early in the transient. For the specific transients shown in Figures 5-2, 5-3, and 5-4 the 0.5-inch flaw provided the smallest pressure margins between allowable and actual transient pressure.

5.3. Results

5.3.1. Fracture Mechanics Evaluation Criteria

The transients evaluated here are considered to be accident conditions. Therefore, vessel integrity must be maintained to facilitate safe reactor shutdown. Crack initiation can be allowed provided the cracks can be arrested. The criterion for precluding crack initiation is as follows:

$$K_{IT} + K_{IP} + K_{IW} < K_{IC} \quad \text{at flaw size } a_1,$$

and cracks are arrested provided

$$K_{IT} + K_{IP} + K_{IW} < K_{IA} \quad \text{at flaw size } a_2$$

where

K_{IT} = applied stress intensity factor due to thermals,
K_{IP} = applied stress intensity factor due to pressure,
K_{IW} = applied stress intensity factor due to residual stresses,
K_{IC} = static crack initiation toughness,
K_{IA} = crack arrest toughness.

The existence and applicability of warm prestressing is also evaluated. Warm prestressing exists provided crack initiation does not occur prior to or at the maximum applied load. If the load decreases continuously from the point of maximum load, subsequent predictions of crack initiation by LEFM are conservative for the following reasons:

1. The introduction of compressive residual stresses at the crack tip due to unloading.
2. Work-hardening in the plastic zone around the crack tip.
3. Blunting of the crack tip by plastic flow.

5.3.2. LEFM Results

As detailed in other sections of this report, transient cases were analyzed for break sizes of 0.007 ft^2 (stuck-open PORV), 0.015 ft^2, and 0.023 ft^2 (stuck-open safety relief valve). The LEFM results for the 0.007- and 0.023-ft^2 cases are presented in this section. The 0.015-ft^2 results are bounded by these cases. These transients were analyzed using three conditions of mixing. The first case (case 1, Table 1-1) is complete, perfect mixing which uses the downcomer reactor coolant temperature transient directly from CRAFT. The second mixing condition (cases 2 and 3, Table 1-1) employed MIX2 results of vent valve/HPI mixing (section 3). This second mixing condition represents an intermediate model as compared to complete mixing in CRAFT and the third mixing condition used, no mixing (case 4).

5.3.2.1. 0.007-ft^2 Pressurizer Break Without HPI Throttling, Complete Mixing (Case 1, Table 1-1)

These transients are considered to be acceptable because crack initiation is not predicted before several hours into the event. However, a warm prestressing situation clearly exists. The operator should take action to depressurize the plant (throttle HPI) since the applied K exceeding K_{IC} cannot be tolerated indefinitely. Again, these results are applicable to the base case (Rancho Seco) at 6 EFPY.

On the basis of this analysis, it was decided that subsequent analyses would be performed assuming operator action to throttle HPI (cases 2-4).

5.3.2.2. 0.007- and 0.023-ft^2 Pressurizer Break With HPI Throttling, MIX2 Mixing (Cases 2 and 3, Table 1-1)

The allowable and actual transient pressure curves are shown in Figure 5-2 for the analyses corresponding to 3.8 and 4.8 EFPY. Again, these data represent the base case (Rancho Seco) analysis using 40F HPI temperature. Only the data for the 0.023-ft^2 break (case 3, Table 1-1) is illustrated since it was shown to be the worst transient; the downcomer temperature transient being more severe than that of the 0.007-ft^2 break (case 2, Table 1-1). Clearly, actual pressures remain below allowable, indicating no brittle fracture concern exists since crack initiation is not predicted. Again, a warm prestressing situation clearly exists.

5.3.2.3. 0.023-ft^2 Pressurizer Break With HPI Throttling, No Mixing (Case 4, Table 1-1)

In order to evaluate the required amount of water mixing in the downcomer, a series of thermal shock transients using hypothetical, worst-case downcomer conditions was analyzed. These temperature transients are 550-40, 550-90, 550-120, and 550-150F; the results of the 550-90F transient are shown, along with actual system pressure in Figure 5-3. Again, the results shown are for the base case (Rancho Seco) at 3.8 EFPY. The actual system pressure remains below allowable, indicating no brittle fracture concern exists since crack initiation is not predicted. Again, a warm prestressing condition clearly exists.

The 550-40F transient resulted in actual system pressure exceeding allowable at about 25 minutes into the transient. Crack propagation without arrest would be predicted under these hypothetical conditions.

The mixing required to heat 40° BWST water to 90°F at the RV wall in the downcomer during the critical times in the transient is slightly less than that predicted by the MIX2 model, which uses concentrated HPI flowing down the reactor vessel wall as a model (section 3.2). As previously described, Figure 3-7 shows the downcomer temperature results of the vent valve/HPI mixing predicted by MIX2 for the 0.023-ft^2 pressurizer break with HPI throttling. Figure 3-7 indicates that the 40F HPI fluid is warmed to approximately 90 to 100F at the RV wall by the mixing. A comparison of the allowable pressures in Figure 5-2 which assumes mixing and assumes 3.8 EFPY irradiation with Figure 5-3 which shows the results of the bounding analysis using 90° BWST fluid shows that there is another effect besides the no mixing assumption which results in lower allowable pressures for the 550-90F bounding analysis. This other hypothetical assumption is that the downcomer temperature is dropped from 550 to 90F over 50 seconds in the bounding analysis whereas if mixing occurs (Figure 3-7), this drop takes place over approximately 1 hour. Therefore, if the bounding analysis indicates 90F results are acceptable, then lesser temperatures (i.e., lesser mixing) would be acceptable using the mixing assumption.

In addition, assuming 40F HPI fluid temperature in these analyses is conservative. Borated water storage tank temperatures must be maintained between 40 and 90F during operation. Therefore, it may be possible to vary the BWST temperature within that range to assist in mitigating the thermal shock concern.

For breaks smaller than 0.023-ft^2, the actual transient pressures are somewhat higher. However, it takes longer before loop flow would completely stop. Therefore, the times at which the critical pressure for crack initiation without arrest exceeds the actual pressure are longer for breaks smaller than the 0.023-ft^2 break.

5.4. Applicability of Base Case (Rancho Seco)

The limiting welds with respect to brittle failure of the reactor vessel are longitudinal welds. This is true since for circumferentially oriented flaws in circumferential welds the allowable pressure would be twice that for a comparable longitudinal weld due to the differences in stress normal to the flaw orientation. Also, for longitudinal flaws in circumferential welds the base metal has substantially lower RT_{NDT} thus higher toughness which prevents the flaw aspect ratio from becoming large. (Allowable pressures for flaws with a 1:1 or 2:1 aspect ratio are higher than flaws with a 6:1 aspect ratio.) Because of these inherent differences between flaws oriented in longitudinal and circumferential welds, the base analysis is only applicable to plants with longitudinal weld seams. As indicated in Table 5-1, these plants are Oconee 1, TMI-1, TMI-2, Crystal River 3, Arkansas Nuclear One (ANO-1), and Rancho Seco. The potential for cold water at the weld location would be most likely to exist only on plants with welds under or near cold leg nozzles. The locations of the longitudinal welds with respect to the cold leg nozzles are shown in Figures 5-5 through 5-10. In addition, the locations and dimensions of core flood nozzles, vent valves, and hot and cold leg pipes and nozzles are provided in Figures 5-11 and 5-12. As can be seen from these figures, the only plants with longitudinal welds under or near the cold leg nozzles are Oconee 1, ANO-1, and Rancho Seco. Welds for the other plants would be subjected to substantially higher water temperatures. Hence, the base analysis is very conservative for the other units.

The results of a bounding (no mixing, case 4) analysis using Oconee 1 material properties and an accumulated neutron fluence corresponding to 4.9 EFPY as a basis (Oconee 1 irradiation as of November 3, 1980 was 4.5 EFPY) produced acceptable results for all of the assumed BWST temperatures, including 40F. Figure 5-4 shows these results for the 550-40 and 550-90F transients.

Similar analyses showed even greater improvement for ANO-1. Clearly, Rancho Seco — which has the least irradiation — has the most restrictive allowable pressures.

In summary, significant variations in weld material, weld types, and irradiation times exist between plants, thus making the bounding analyses very conservative for some plants.

5.5. Conservatisms

It is felt that the fracture mechanics analysis described above has a number of inherent conservatisms. Without elaboration or quantification, these conservatisms are listed below.

1. Flaw size, shape, orientations, and location.
2. K_{IC} and K_{IA} lower bound toughness curves.
3. Adjusted RT_{NDT} from upper bound of Regulatory Guide 1.99.
4. Applicability of LEFM to stresses above yield as in the case of severe thermal shock with pressure.
5. No credit for warm prestressing.

Table 5-1. Comparison of Reactor Vessel Materials[a]

		Adjustment RT_{NDT}, F
Controlling Longitudinal Welds		
Oconee 1 (5.998 EFPY)	SA-1493	184[b]
Oconee 2 (5.528 EFPY)	NA[c]	NA
Oconee 3 (5.393 EFPY)	NA	NA
TMI-1 (5.516 EFPY)	SA-1526	198
TMI-2	SA-1493	160
Crystal River 3	WF-18/8	144
ANO-1	WF-18	160
Rancho Seco	WF-70	222[b]
Davis-Besse 1	NA	NA
Midland 1	NA	NA
Midland 2	BAB-243	39[d,e]
Controlling Circumferential Welds		
Oconee 1	SA-1229	171
Oconee 2	WF-25	223
Oconee 3	WF-67	173
TMI-1	WF-25	220
TMI-2	WF-193	158
Crystal River 3	WF-70	184
ANO-1	WF-112	195
Rancho Seco	WF-154	187
Davis-Besse 1	WF-233	128
Midland 1	WF-70	151
Midland 2	BAB-243	39[d]

(a) Reference: January 1, 1980 plus 2 EFPY adjustment to RT_{NDT} from Regulatory Guide 1.99.

(b) These longitudinal welds are in the upper shell and underneath an inlet nozzle.

(c) NA: not applicable.

(d) The material listed is upper shell material, which is controlling over the weld material.

(e) Not a longitudinal weld.

Figure 5-1. Types of Weld Orientations

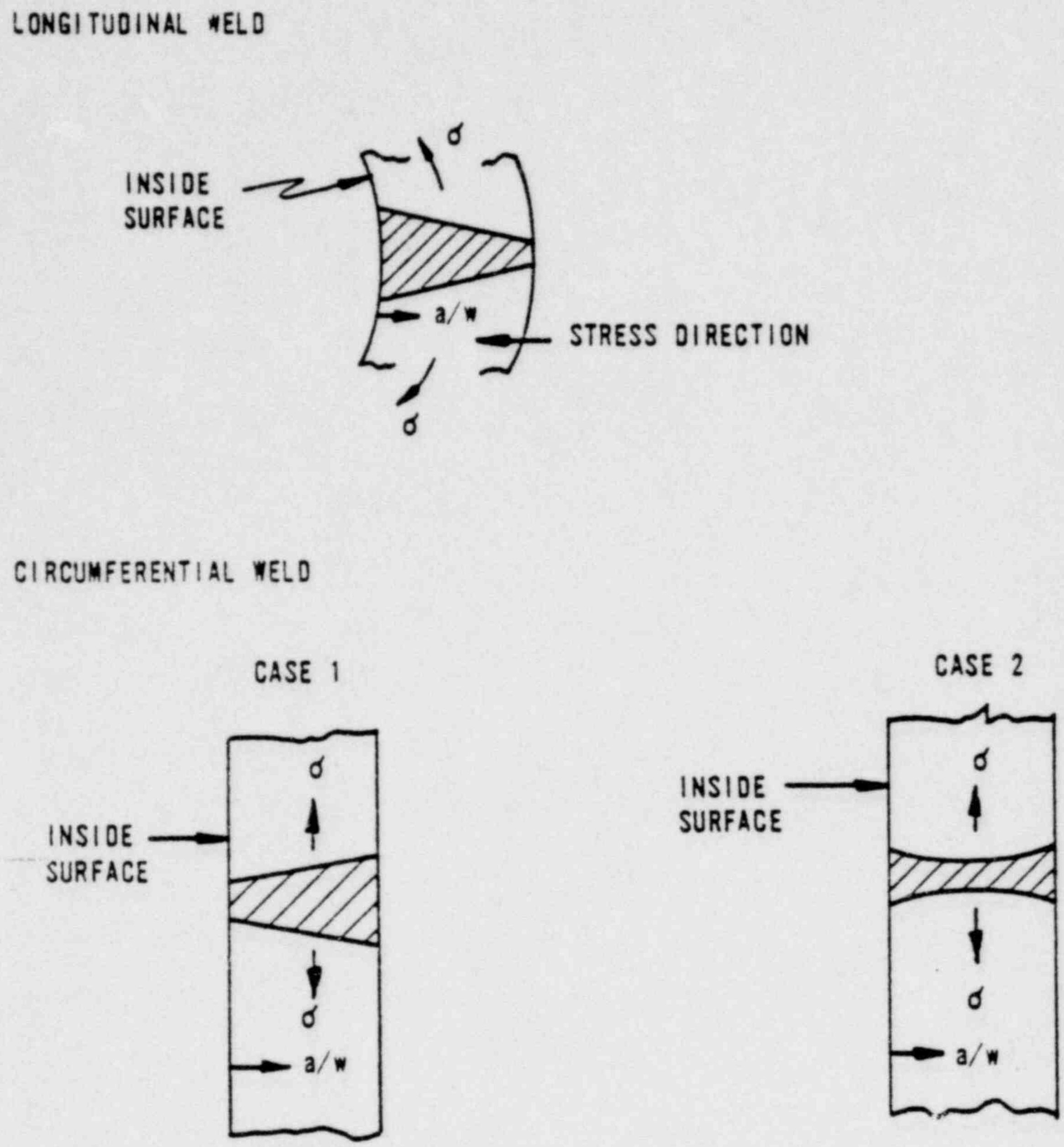


Figure 5-2. Allowable and Actual Pressures Vs Time, 0.023-ft^2 Pressurizer Break With Operator Action, Rancho Seco, 40F BWST, MIX2 (Case 3, Table 1-1)

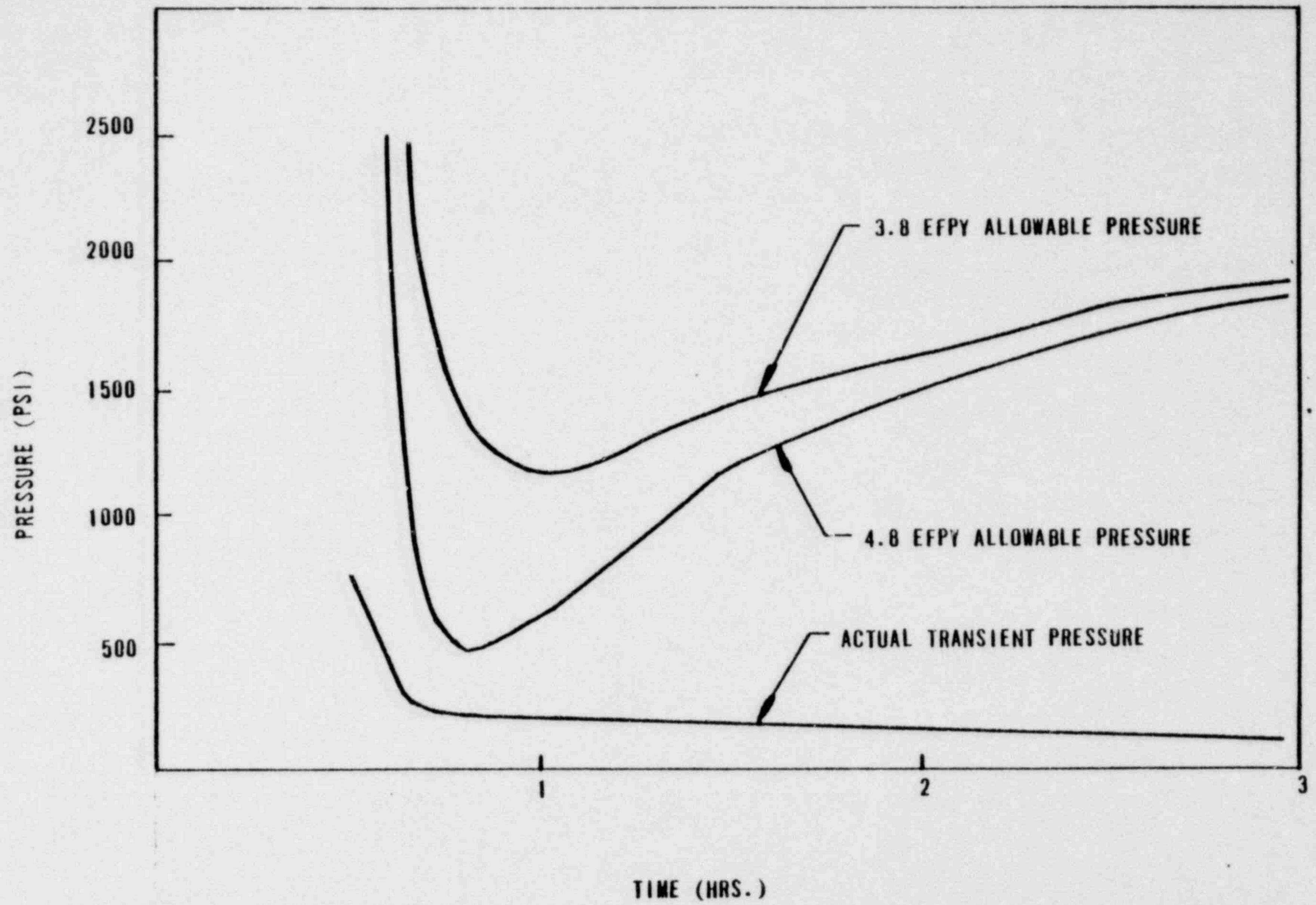


Figure 5-3. Allowable and Actual Pressure Vs Time, 0.023-ft^2 Pressurizer Break With Operator Action, Rancho Seco, 550-90F Transient, Bounding Analysis, 3.8 EFPY (Case 4, Table 1-1)

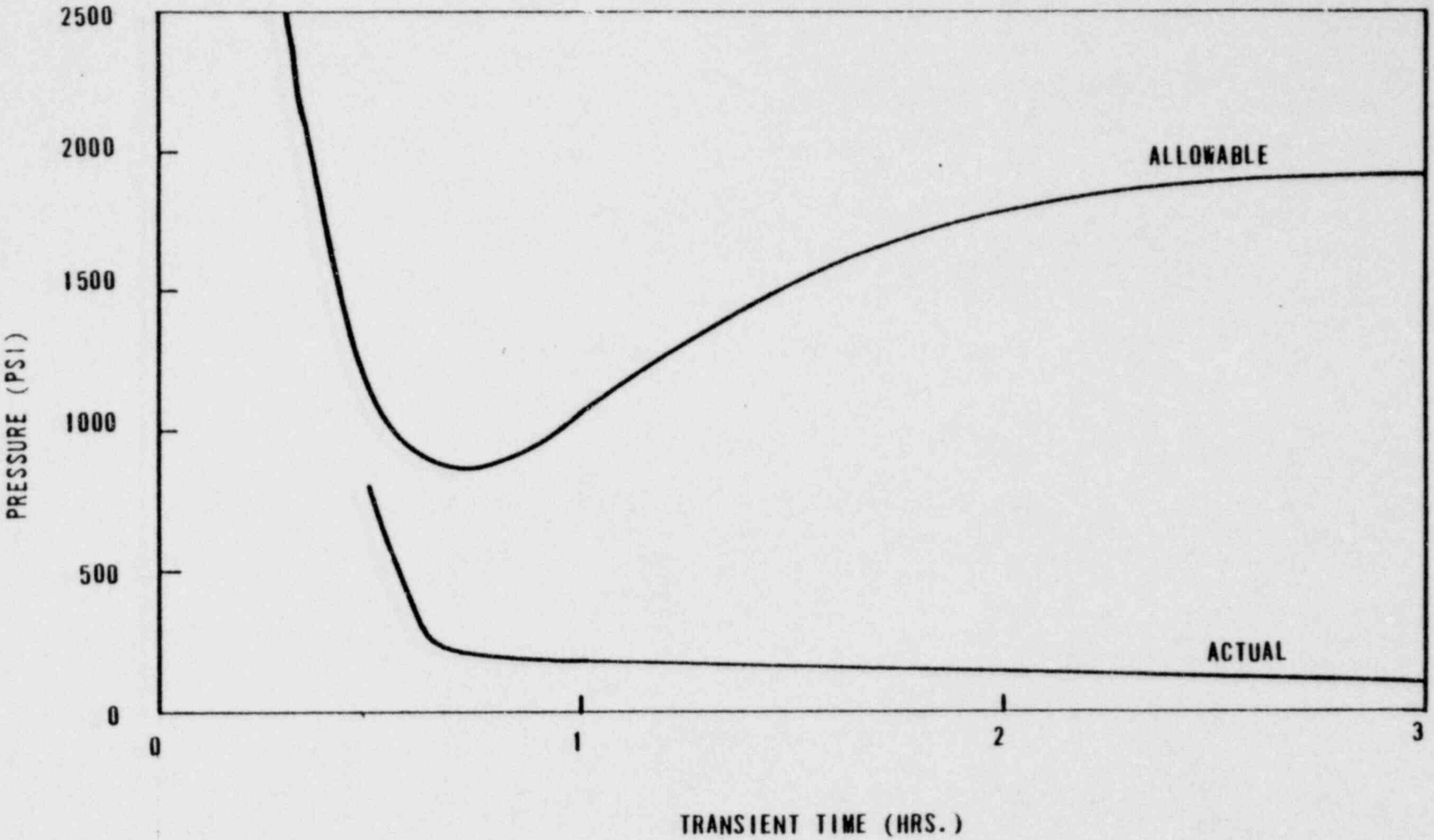


Figure 5-4. Allowable and Actual Pressure Vs Time, 0.023-ft^2 Pressurizer Break With Operator Action, Oconee 1, 550-90 and 550-40F Transient, Bounding Analysis, 4.9 EFPY (Case 4, Table 1-1)

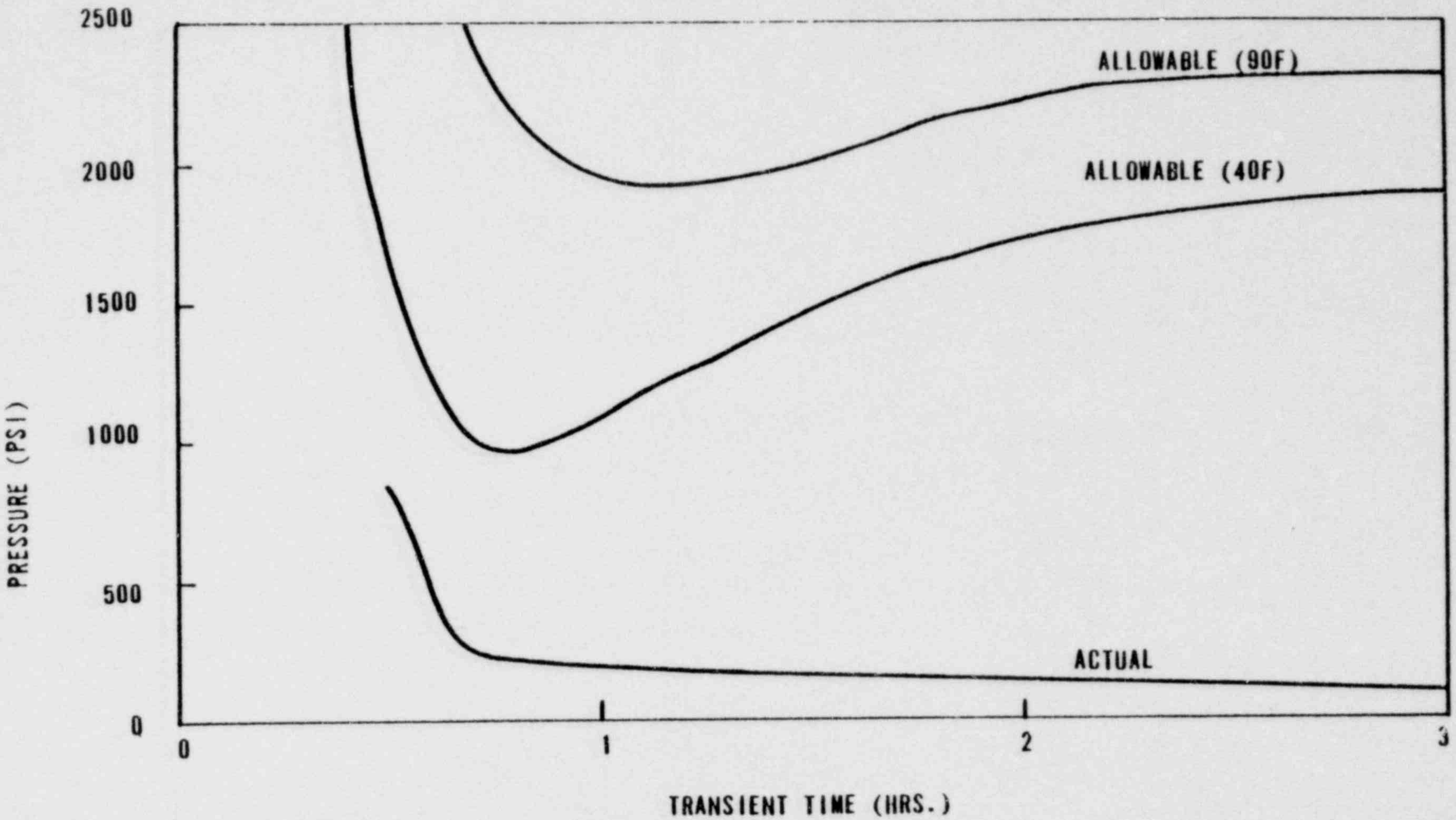


Figure 5-5. Oconee 1 Inside Surface Reactor Vessel — Weld Locations of Interest

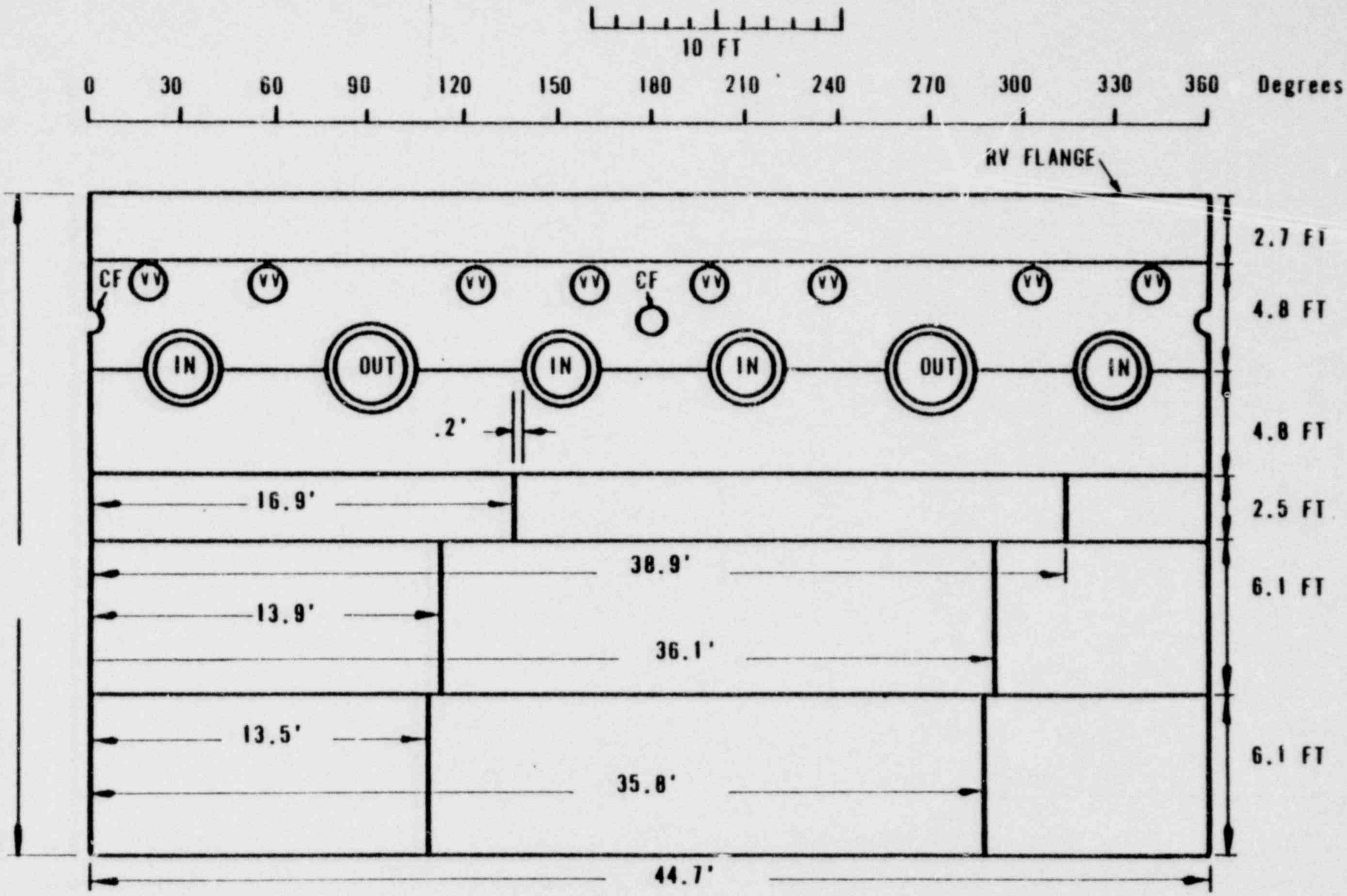


Figure 5-6. TMI-1 Inside Surface Reactor Vessel — Weld Locations of Interest

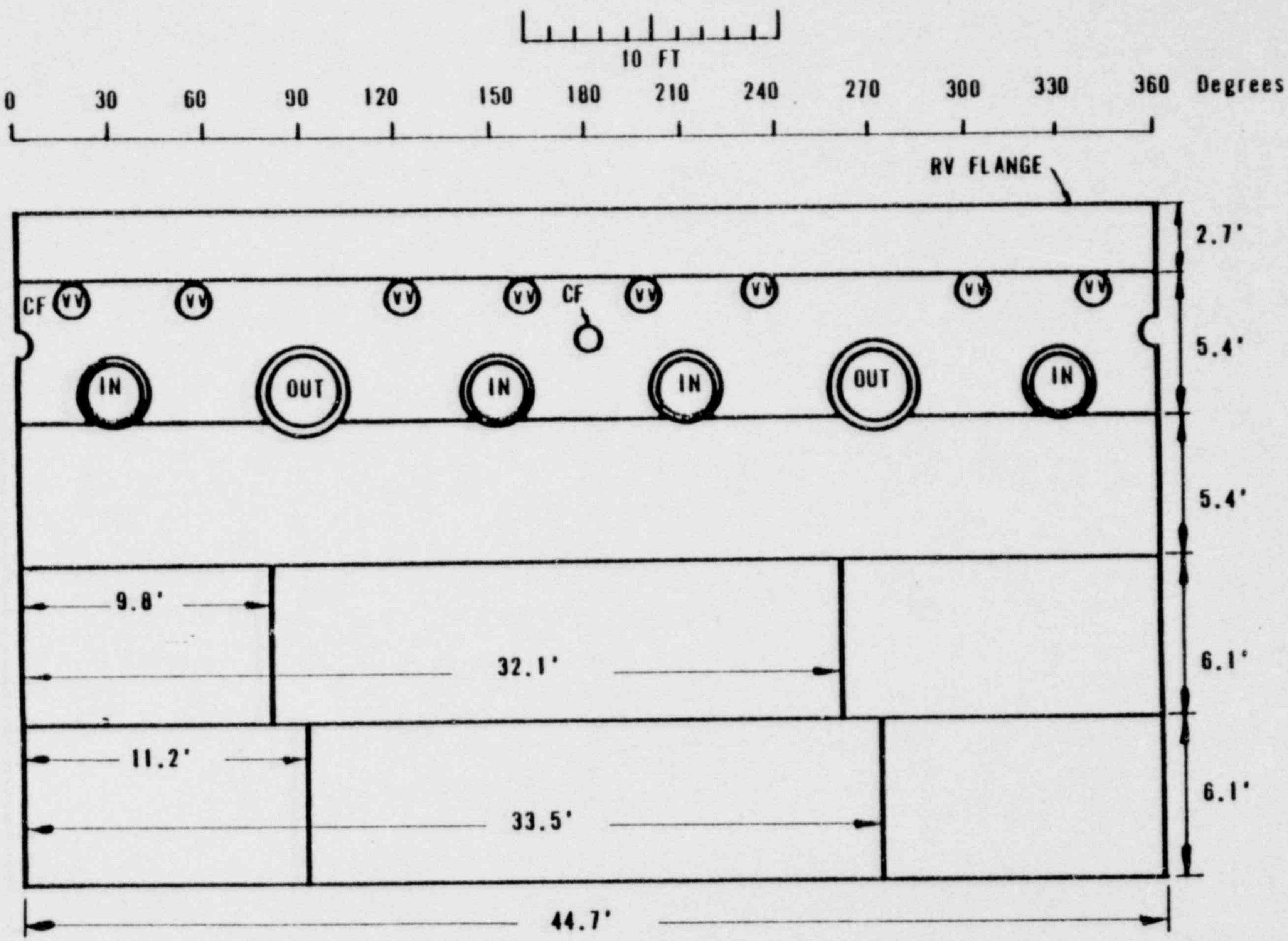


Figure 5-7. TMI-2 Inside Surface Reactor Vessel — Weld Locations of Interest

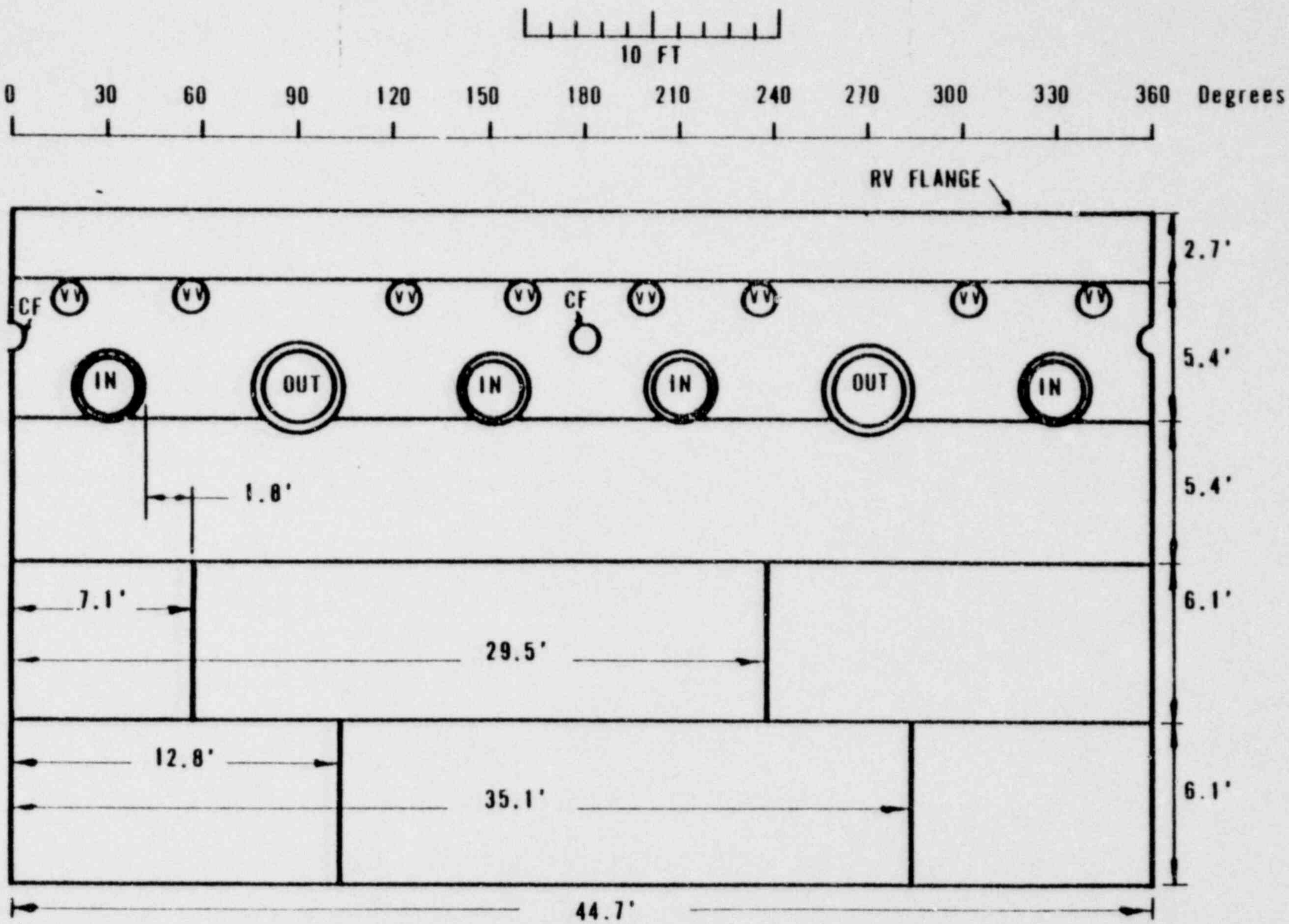


Figure 5-8. Crystal River 3 Inside Surface Reactor Vessel — Weld Locations of Interest

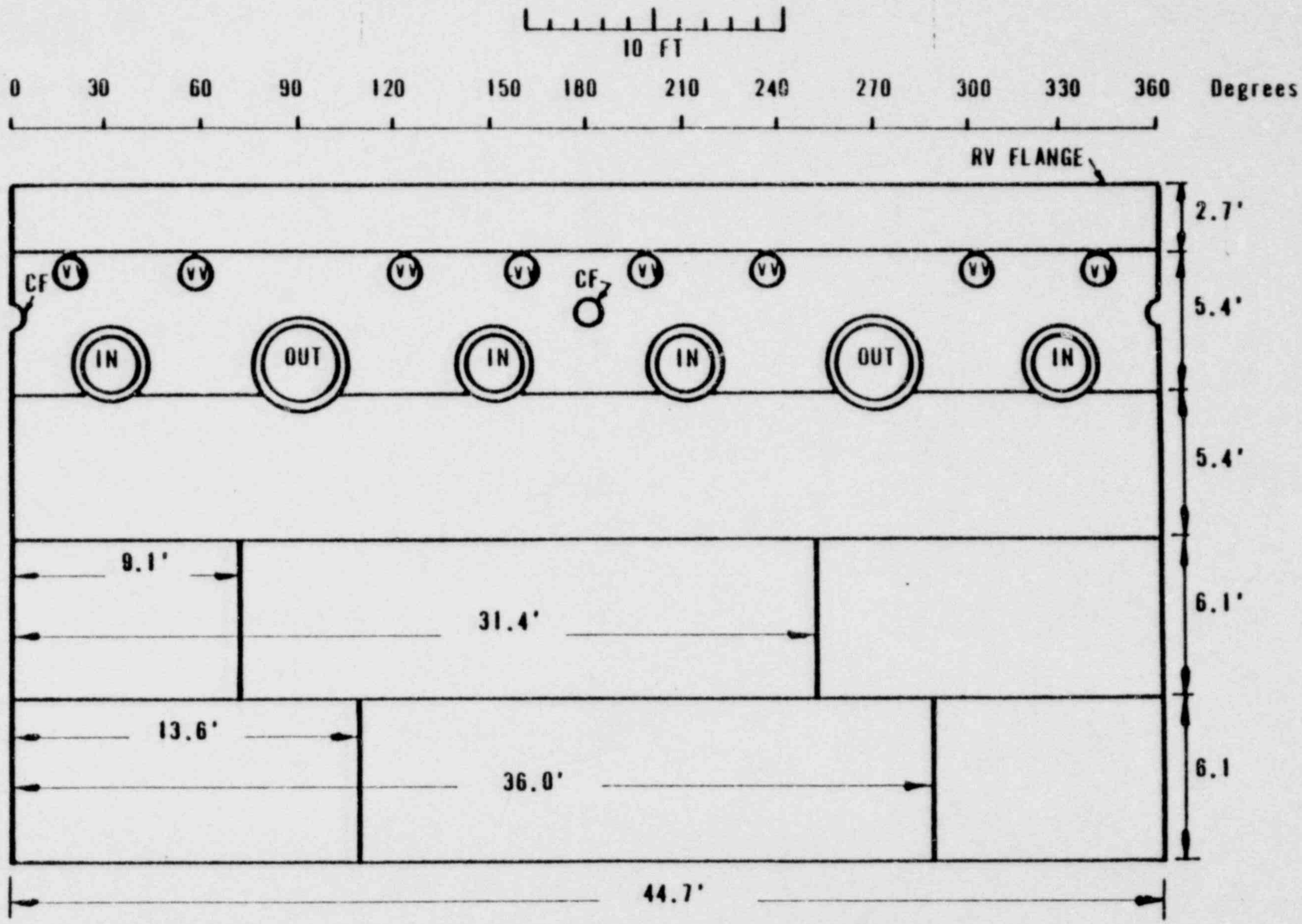


Figure 5-9. ANO-1 Inside Surface Reactor Vessel - Weld Locations of Interest

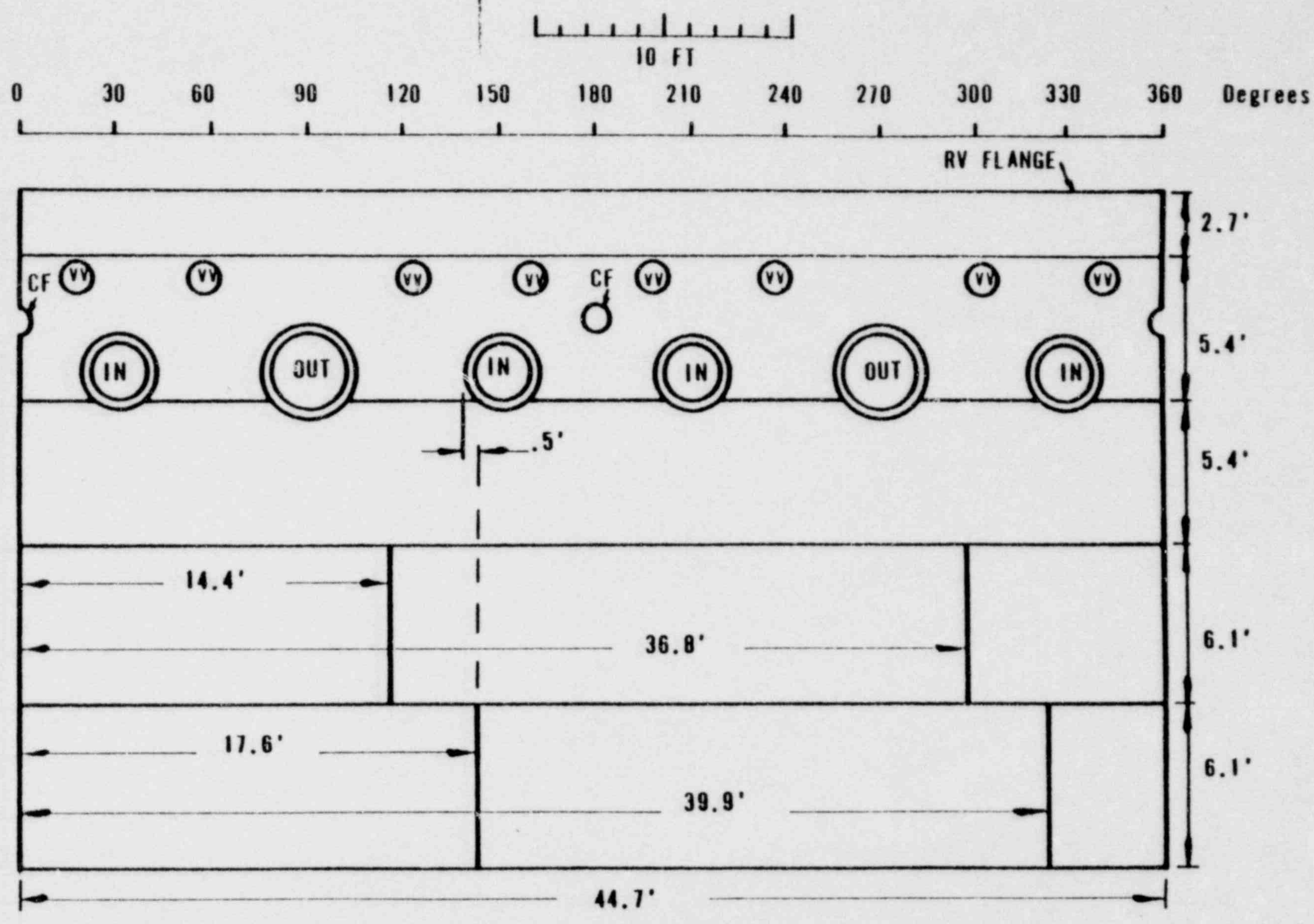


Figure 5-10. Rancho Seco Inside Surface Reactor Vessel — Weld Locations of Interest

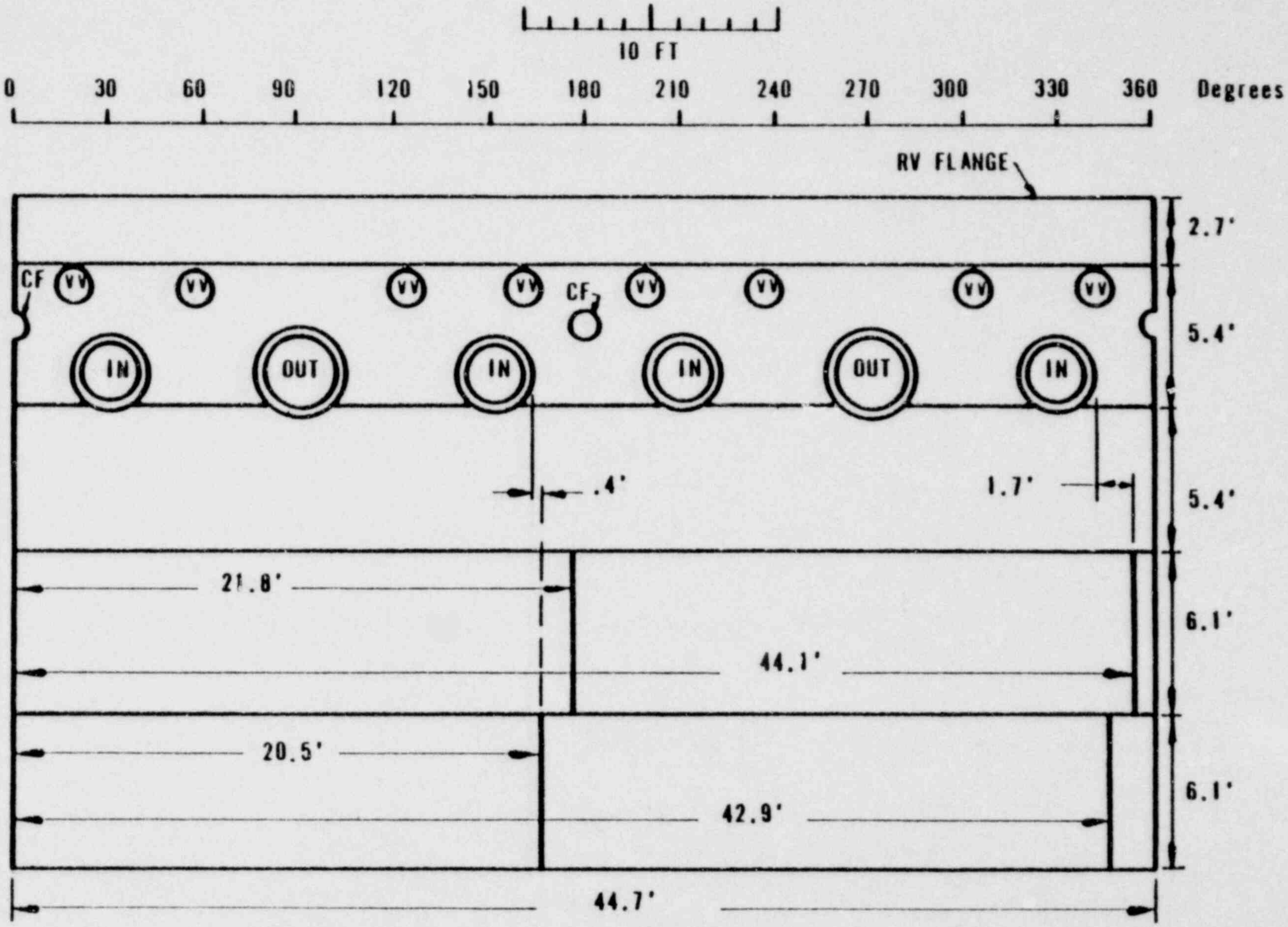


Figure 5-11. Reactor Vessel Nozzle Locations — Inside Surface, Typical 177-FA Lowered-Loop Plant

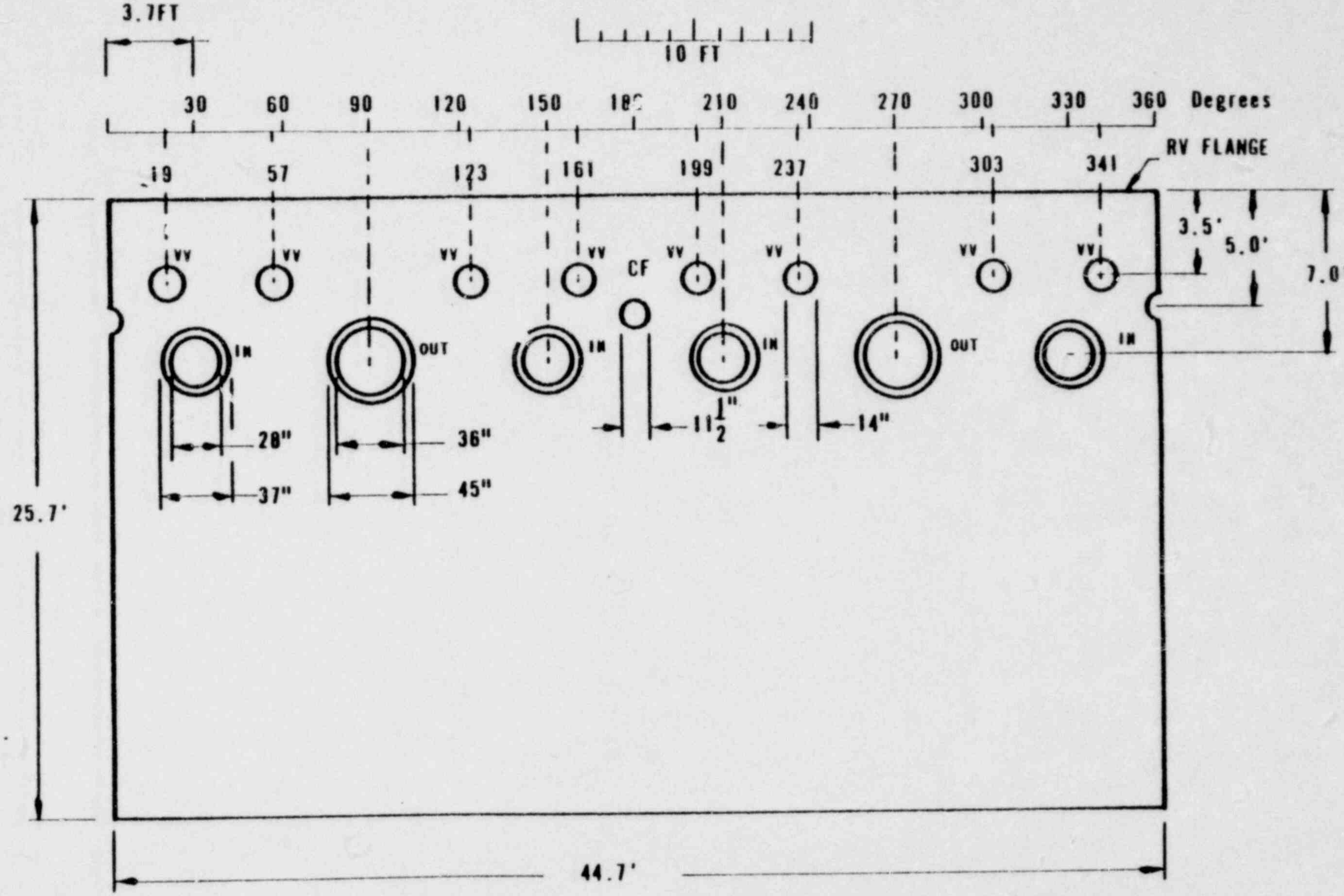
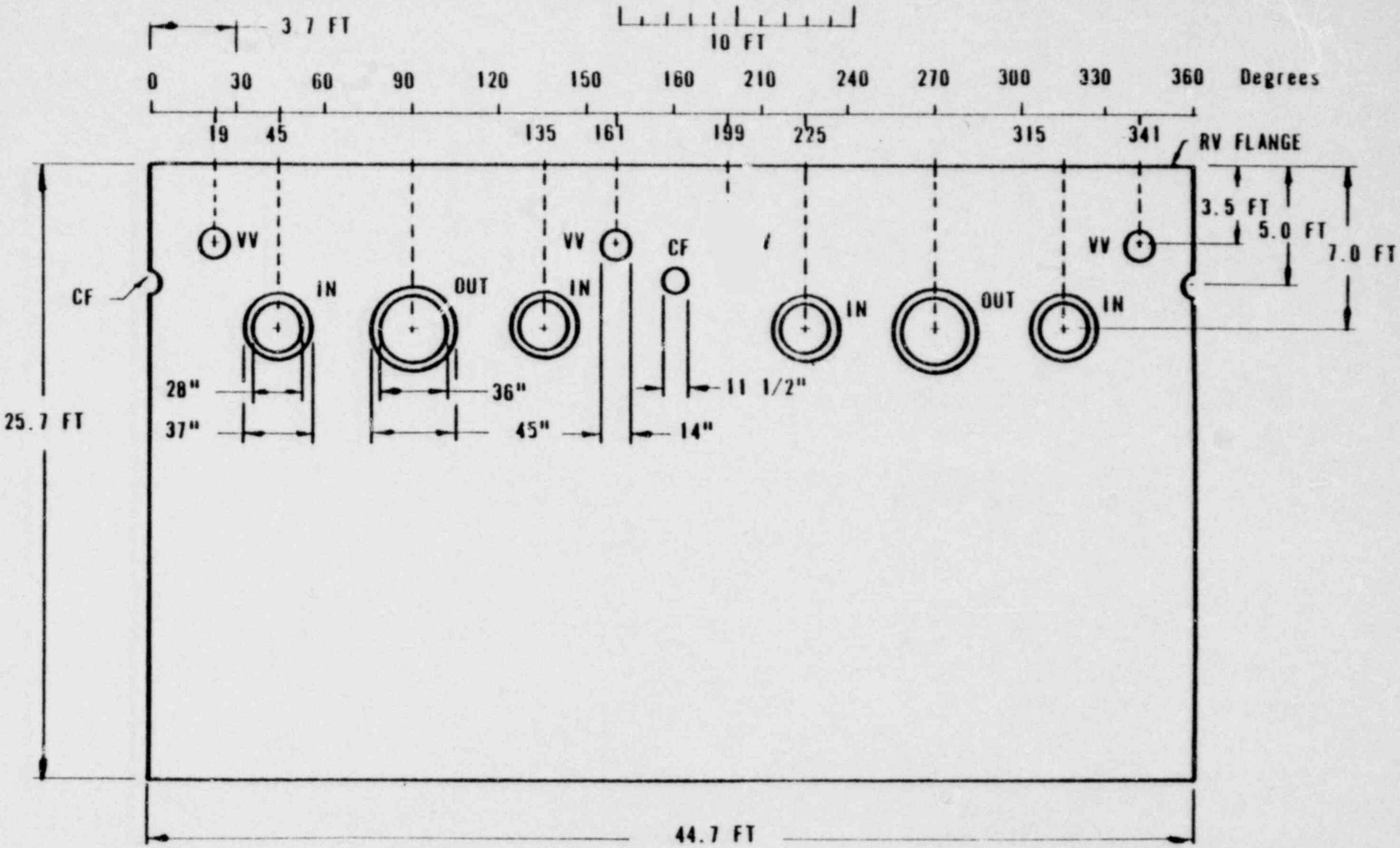


Figure 5-12. Reactor Vessel Nozzle locations — Inside Surface, Typical 177-FA Raised-Loop Plant (Davis-Besse 1)



6. SUMMARY AND CONCLUSIONS

The investigations and analyses described in this report were performed to evaluate the concern of brittle fracture following a small LOCA, assuming an extended loss of feedwater and an extended loss of forced reactor coolant system flow. This is in response to the NRC's information request of July 12, 1979.

6.1. Analyses Performed

LOCA analyses were performed for break sizes of 0.007-, 0.015-, and 0.023-ft^2 assuming no feedwater to the steam generators. Each break size was analyzed assuming (1) no operator action and (2) the operator throttles HPI flow to maintain approximately a 100F subcooling margin at the core outlet. The purpose of the LOCA analyses was to determine the HPI flow rate, vent valve flow rate and temperature, and RCS pressure.

A very conservative evaluation was developed to define the worst case bounding downcomer temperature conditions used for the LEFM analyses. The bounding case assumed the HPI flowing into the downcomer flows down along the RV wall without spreading out circumferentially and with no mixing with the vent valve fluid. Resulting downcomer fluid bulk temperatures of 40, 90, 120, and 150F were assumed for inputs to the worst case LEFM calculations (case 4, Table 1-1).

Other, more realistic downcomer analyses were performed which included gravity effects, circumferential distribution of the vent valve flow, and mixing with the concentrated HPI flow stream (cases 2 and 3, Table 1-1).

Linear elastic fracture mechanics analyses were performed for each break size, with and without operator action to throttle HPI flow, for different mixing assumptions.

6.2. Conclusions

The analyses reported in other sections of this report result in the following conclusions.

6.2.1. General Conclusions

1. Vent valve flow occurs for the entire duration of the small break LOCA transients analyzed (Figure 2-30).
2. As long as forced flow exists (reactor coolant pumps are operating), the incoming HPI water will mix with water returning from the steam generators and no reactor vessel brittle fracture concern exists.
3. In the case in which no loop flow is present, vessel downcomer local wall temperatures will depend on the interaction of many factors including the flow rate and temperature of the HPI water, the flow rate and temperature of the vent valve return flow, and the degree of mixing between them.
4. Welds likely to experience the most rapid cooldown are those vertically below the cold leg nozzles into which HPI water is injected. The limiting welds with respect to brittle failure of the reactor vessel are longitudinal welds. Only in the Oconee 1, ANO-1, and Rancho Seco reactor vessels do longitudinal welds exist under or near the cold leg nozzles. Hence, the analysis of the longitudinal welds on Oconee 1, ANO-1, and Rancho Seco is conservative for the other welds (other operating B&W units).
5. The controlling RV material (weld and base material) with the lowest RT_{NDT} was found to be the longitudinal weld seam, WF-70 in the lower shell of the Rancho Seco vessel. The properties of this material are used as the base case for generic enveloping analyses of all reactor vessels.
6. Throttling of HPI to reduce system pressures will be required to help alleviate the brittle fracture concern.

6.2.2. Specific Conclusions

As of November 3, 1980, Oconee 1, the lead B&W plant, had accumulated 4.5 EFPY. Rancho Seco had accumulated 3.2 EFPY. The following conclusions are burnup and mixing-model dependent. As discussed throughout this report, all of the analyses leading to these conclusions are performed using input assumptions which conservatively envelop all B&W operating plants.

1. Results of a base case (Rancho Seco) analysis corresponding to 6 EFPY and assuming complete HPI/vent valve fluid mixing (case 1, Table 1-1) are considered acceptable because crack initiation is not predicted before several hours into the event. The operator should take action to depressurize the plant (throttle HPI).

2. Base case (Rancho Seco) analyses at 3.8 and 4.8 EFPY which assume operator action to throttle HPI to maintain approximately 100F subcooled conditions at the core outlet and which assume HPI/vent valve fluid mixing as determined using MIX2 (section 3) show acceptable results (Figure 5-2). No brittle fracture concern is indicated since crack initiation is not predicted (cases 2 and 3, Table 1-1). The calculated margins between actual and allowable pressure indicate operation for some time beyond the 4.8 EFPY actually analyzed would be acceptable. A worst-case (40F) HPI temperature was assumed.

3. Base case (Rancho Seco) analyses at 3.8 EFPY which assume operator action to throttle 40F HPI to maintain approximately 100F subcooled conditions at the core outlet and which assume no HPI/vent valve mixing following loss of natural circulation (case 4, Table 1-1) result in allowable pressures being exceeded at about 25 minutes into the transient. Crack initiation without arrest would be predicted for this hypothetical case. (The no HPI/vent valve mixing assumption means the RV downcomer fluid temperature at the RV wall changed from 550 to the HPI temperature of 40F in 30 seconds when RC loop flow (natural circulation) stopped.) Analysis of the same transient assuming 90F HPI water gives acceptable results (Figure 5-3).

   The same transient using 40F HPI water (worst case) was analyzed using Oconee 1 data at 4.9 EFPY. Oconee 1 is the second most limiting reactor vessel. The results (Figure 5-4) are acceptable indicating no brittle fracture concern for all reactor vessels except Rancho Seco using any BWST temperature including the worst case (40F). Crack initiation is not predicted. Similar analyses using ANO-1 data indicated even greater margins. Again, the calculated margins between actual and allowable pressure indicate operation for some time beyond the irradiation actually analyzed would be acceptable.

4. State-of-the-art methods do not presently support highly accurate analytical predictions of the three-dimensional fluid mixing in the downcomer. Until the amount of mixing between HPI fluid and vent valve fluid is better defined, the exact amount of margin, the length of time that margin exists, or the adequacy of operation action to eliminate any brittle fracture concern cannot be rigorously determined. Certainly the analyses reported herein are in many respects conservative with what the actual situation is expected to be. The major arguments supporting this are

   - An extended total loss of feedwater or extended loss of loop flow is unrealistic.
   - Some mixing and heating of the HPI will occur.
   - Warm prestressing benefits are present.

7. REFERENCES

[1] CRAFT — Fortran Program for Digital Simulation of a Multinode Reactor Plant During LOCA, BAW-10092, Babcock & Wilcox, Lynchburg, Virginia, April 1975.

[2] D. F. Ross (NRC) to J. H. Taylor (B&W), letter, "Information Request on Reactor Vessel Brittle Fracture," July 12, 1979.

[3] P. K. Nair, S. B. Skaar, and D. E. Smith, BEFRAN-1 Beltline Fracture Analysis, NPGD-TM-498, Babcock & Wilcox, Lynchburg, Virginia, May 1979

[4] F. W. Dittus and L. M. K. Boelter, University Publications in Engineering, Vol 2 (1930), p 443.

[5] G. C. Vliet and D. C. Ross, "Turbulent Natural Convection ...," ASME J. Heat Transfer, November 1975, pp 549–555.

[6] Nuclear Systems Material Handbook, Vol 1 (1978), TID-26666, Hanford Engineering Development Laboratory, Richland, Washington (1978).

[7] ASME Boiler and Pressure Vessel Code, Section III, Appendixes (1977).

[8] D. T. Buchanan and Y. H. Hsii, FELCON — General Purpose Program for Solving Thermal Conduction Problems, NPGD-TM-268, Rev. 1, Babcock & Wilcox, Lynchburg, Virginia, February 1979.

[9] D. A. Ferril, P. B. Juhl, and D. R. Miller, "Measurement of Residual Stresses in a Heavy Weldment," Welding Journal, WRC Supplement, Vol 45, No. 11, November 1966.

[10] ASME Boiler & Pressure Vessel Code, Section XI, Appendix A.

[11] P. K. Nair, Accident Transients Fracture Analysis for 177-FA Reactor Vessel Beltline Region, BAW-1605, Babcock & Wilcox, Lynchburg, Virginia, January 1980.

ATTACHMENT 9

II.K.3.2 & 7 <u>PORV Failures and Opening Probability</u>

The following report, "Report on Power Operated Relief Valve Opening Probability and Justification for Present System and Setpoints", Document No. 12-1122779, December, 1980, has been prepared as a generic report for the B&W Owners' Group. The analysis provided in this report shows that an automatic block valve closure system is not necessary, as indicated previously. The District is considering such a modification, however, as an overall plant improvement, which will allow a return to the original reactor pressure trip and PORV opening set points, together with an elimination of a reactor trip upon a turbine trip. The design for any such modification will be submitted in time for NRC review prior to implementation.

REPORT ON POWER-OPERATED RELIEF VALVE

OPENING PROBABILITY AND JUSTIFICATION FOR

PRESENT SYSTEM AND SETPOINTS

- Submitted to Satisfy Requirements of

NUREG-0737, Items II.K.3.2 and II.K.3.7

Document No. 12-1122779

December 1980

REPORT ON POWER-OPERATED RELIEF VALVE

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NUREG-0737, Items II.K.3.2 and II.K.3.7

Document No. 12-1122779

December 1980

Table of Contents

1.0 INTRODUCTION AND SUMMARY

NUREG-0737, "Clarification of TMI Action Plan Requirements," November 1980, required that a report be submitted which provides the information identified in Items II.K.3.2 and II.K.3.7. Specifically, NUREG-0737 requested the following information/justifications:

1. II.K.3.2
   - o Compile operational data regarding pressurizer safety valves to determine safety valve failure rates
   - o Perform a probability analysis to determine whether the modifications already implemented have reduced the probability of a small break LOCA due to a stuck-open PORV or safety valve a sufficient amount to satisfy the criterion ($<10^{-3}$ per reactor year), or whether the automatic PORV isolation system specified in Task Item II.K.3.1 is necessary.

2. II.K.3.7
   - o Perform an analysis to assure that the frequency of PORV openings is less than 5% of the total number of overpressure transients.

This report is submitted in compliance with NUREG-0737 and demonstrates that the requirements of NUREG-0737 are met with the existing Power-Operated Relief Valve (PORV), Safety Valve and High Pressure Trip Setpoints and that no automatic isolation system is required.

2.1 Evaluation of PORV Opening Probability During an Overpressure Transient

An evaluation of the probability of PORV opening has been performed. Two separate analyses have been performed. The first is an analytical estimate, the second is an analysis based upon operating experience.

2.1.1 PORV Opening Probability Based Upon Analyses

A series of calculations have been completed using best estimate numbers to estimate the probability of PORV opening. Wherever possible, these calculations were based on operating plant data in an attempt to provide realistic estimates for the analyzed events. The following paragraphs summarize the results and calculational basis for the analysis.

The probability of the PORV lifting during a loss of feedwater (LOFW) or turbine trip is approximately 3.9×10^{-6}/Rx-Yr for plants with a PORV setpoint of 2450 psig and 3.9×10^{-3}/Rx-Yr for plants with a PORV setpoint of 2400 psig. The latter setpoint is presently applicable only to Davis-Besse 1. These probabilities are based on the assumptions that the high pressure trip setpoint is 2300 psig with a standard deviation of 1.4 psi and that the actual setpoint at which reactor trip occurs is a random variable which is normally distributed. The small standard deviation is based on the fact that the PORV and RPS actuation points are not completely independent; i.e., they share a common source; i.e., sensor and instrument string. Thus, these parts of the string errors are perfectly correlated and cancel one another in the analysis. Other parts of the relevant string error are not correlated and it is upon these that the 1.4 psi standard deviations are based. In a similar fashion, the actual opening setpoint of the PORV is also assumed to be a random variable with a normal distribution. The assumption of normality for the actuation of either the high pressure trip or the PORV is just an assumption; no data is available to justify or deny the validity. The RCS pressure rise above the RPS high pressure trip setpoint (hence referred to as "pressure rollover") during a LOFW or turbine trip was determined by a combination of plant data and engineering analysis. Pressure rollover data from the operating plants (Table 2.1-1) was compiled from available data. However, these data points represent situations in which the PORV could open, thus decreasing the amount of pressure overshoot. Therefore, it was necessary to correct for the PORV opening, since we are interested in the situation in which it remains closed. This was an accomplished by benchmarking the CADD code to a transient in which the PORV was isolated. After satisfactory duplication of this transient, the code was rerun modeling proper functioning of the PORV. The resulting pressure correction to the rollover data was 17.4 psi. The rollover data itself was tested and is statistically acceptable as normally distributed. It has a mean of 9.2 and a standard deviation of 27.52 psi. The presence of negative values in this data set indicates that the RPS trip setpoints have frequently been set low. Since the data reflects actual operating experience, the use of the negative values can be justified in the analysis.

Using the above data and assumptions, a Monte Carlo simulation of the relation

PORV - RPS - EXCESS - BIAS = SAMPLE

was conducted. The terms in the above relation are defined as follows:

PORV - PORV setpoint, a normally distributed random variable

RPS - High pressure trip setpoint, also a normally distributed random variable

EXCESS - Pressure follover, a randomly distributed normal variable

BIAS - A constant (17.4 psi) defined by analysis which compensates the rollover data for the fact that the PORV will remain closed.

Six thousand sample values of the above alogrithm expression were calculated using the SAMPLE code. A negative value of the above expression implies the PORV opens. In the computer trials, no negative values in 6000 instances were observed.

It was then assumed that the random variables described above are independent in the probabilistic sense, so an analytic approach was applied. The sum or difference of several independent normal distributions is also a normal distribution with mean equal to the algebraic sum of the means and standard deviation equal to the square root of the sum of variances. In this case, the mean is

$$2450 - 2300 - 9.23 - 17.4 = 123.37 \text{ (except DB-1, } = 73.37)$$

and standard deviation is

$$\sqrt{(1.4)^2 + (1.4)^2 + (27.52)^2} = 27.59 \text{ (for DB-1,} = 27.59)$$

The probability that the PORV will open during an overpressure transient is $3.9X10^{-6}$/Rx-Yr (for DB-1 this value is $3.9X10^{-3}$/Rx-Yr). The statistics show that we can be 99% confident that at least 99.99% of all LOFW and turbine trip high pressure transients will not open the PORV for the PORV set at 2450 psig. For a setpoint of 2400 psig, the statistics indicate a 99% confidence that more than 99.4% of the overpressure transients will not result in opening the PORV.

2.1.2 PORV Opening Probability Based Upon Operational Data

NUREG-0667, "Final Report of the B&W Reactor Transient Response Task Force," contained a listing of reactor trips (148) with PORV actuations prior to the TMI-2 accident. Since the accident at TMI-2 approximately 59 trips have occurred on B&W designed plants. Approximately 42 of these trips would have lifted the PORV with the old setpoints. Of the 190 trips that would have lifted the PORV with old setpoints, three of these events would have lifted the PORV with the new setpoints. In addition the modifications that have been made to the plants since those transients would have precluded PORV actuation given the same initiating events on those plants and the new setpoints. Based on these data, it is estimated that the present PORV opening probability is less than 1.6% for an overpressure transient, which is less than the 5% requirement stated in II.K.3.7 of NUREG-0737.

TABLE 2.1-1

PRESSURE ROLLOVER DATA

Trip #	Power, %	Peak Pressure, psig	Rollover, psig
1	95	2355	0
2	90	2385	+30
3	25	2400	+45
4	20	2385	+30
5	90	2390	+40
6	32	2345	-10
7	40	2360	+5
8	40	2352	-5
9	92	2375	+20
10	15	2365	+10
11	35	2400	+45
12	13	2370	+15
13	14	2355	0
14	38	2380	+25
15	98	2410	+55
16	72	2400	+45
17	100	2340	-15
18	100	2340	-15
19	100	2390	+35
20	100	2330	-25
21	98	2325	-30
22	15	2355	0
23	9	2370	+15
24	30	2345	-10
25	99	2350	-5
26	16	2295	-60

2.2 Evaluation of PORV and Safety Valve Reliability

2.2.1 Safety Valve Failure Rate History

There have been three cases where pressurizer safety valves were lifted on B&W plants. None of these cases resulted in failure of the safety valve to reseat. Because of the few data points, no estimate was made of the safety valve failure rates.

2.2.2 Evaluation of Small Break LOCA Probabilities/Need for PORV Isolation System

The contribution to the probability of a SB LOCA from an open PORV was estimated by two methods. The first was an analysis effort, the second was based strictly upon operational data. The results are discussed below:

2.2.2.1 Small Break LOCA Probability Calculations

The probability of a stuck open PORV is the product of the probability of being demanded open times the probability of failing open on demand. The raising of the PORV setpoint has reduced the number of demands and thus the probability of being in the stuck open state. The point estimate for PORV SB LOCA probably (variation not estimated) is calculated to be **5.04×10^{-4}** per reactor year which complies with II.K.3.2 requirement that the probability of stuck open PORV SB LOCA does not significantly impact the probability of SB LOCA from all causes (1×10^{-3} per reactor year). The initiators of PORV actuations have been grouped into five categories along the associated frequency of each category. Details on how the values are calculated are contained in Table 2.2.2-1.

1. PORV opening on overpressure transient	3.9×10^{-6}/Rx-Yr
2. PORV opening on transient with delayed aux. feed	1.4×10^{-3}/Rx-Yr
3. PORV opening on operator action under ATOG guidelines	1.54×10^{-2}/Rx-Yr x 1 demand (offsite power available) x 10^{-3} offsite power loss/event x 23 demands (offsite power lost)
4. PORV opening due to instrumentation control faults	5×10^{-3}/Rx-Yr
5. PORV opening from additional consideration from II.K.3.7	1.8×10^{-3}/Rx-Yr
TOTALS	2.40×10^{-2}/Rx-Yr 2.61×10^{-2}/Rx-Yr(DB)

This total is then multiplied by the probability of the PORV sticking open on demand.

Note that all plants except Davis Besse (Crosby PORV) have Dresser valves; however, the entire B&W operating plant experience was used to arrive at a generic PORV sticking open probability as follows: There have been ten stuck open PORV events, five of which could be classified as mechanical failure of the PORV (the other five were basically installation errors). Using all these five failures in determination of future frequency is considered conservative since two of the failures (OC-3,6/13/75 and CR-3, 11/75) were rectified by design changes, another (TMI-2, 3/28/79) cause is unknown. OC-2, 11/6/73 could be considered as a burn-in failure and the

DB-1, 10/13/77 event is a Crosby valve. Using five failures in 250 demands results in a value of 2×10^{-2} to fail to reclose on demand. This value is considered conservative not only due to the inclusion of all five failures but also the number of demands is probably much higher than 250. There have been 148 documented PORV openings on reactor trips; however, there is not a listing of PORV demands when the reactor did not trip (e.g., ICS runback) nor is consideration given to transients that could have actuated the PORV numerous times during an event. The value of 250 demands is conservatively used here. An analysis was also performed to include values for other than mechanical failure that keep the PORV open. The results of this analysis is summed with the mechanical contributor (2×10^{-2}/d) to arrive at the value for failure to reclose on demand (2.1×10^{-2}/d).

Probability of PORV small break LOCA equals:

$$(2.4 \times 10^{-2})\ (2.1 \times 10^{-2}/d) = 5.04 \times 10^{-4}/\text{Rx-Yr}$$

$$(2.61 \times 10^{-2})\ (2.1 \times 10^{-2}/d) = 5.48 \times 10^{-4}/\text{Rx-Yr (DB)}$$

2.2.2.2 Small Break LOCA Probability Based Upon Operational Data

As discussed in Section 2.1.2, there have been three events which with the revised setpoints would have actuated the PORV. However, the plants have been reconfigured (e.g., upgrades on aux. feedwater, control circuitry of PORV, NNI power sources, AC power sources) so as to reduce the probability of these PORV actuations. Conservatively estimating that one event could occur in the 45 years of B&W plant operation, yields a probability of occurrence of 2.22×10^{-2}/Rx-Yr.

The previous section gave a PORV failure probability of $2.1x10^{-2}/d$.

Therefore the probability of a PORV small break LOCA equals:

$(2.22x10^{-2}d/Rx\text{-}Yr)(2.1x10^{-2}/d) = 4.7x10^{-4}/Rx\text{-}Yr$

which is less than the $1.0x10^{-3}/Rx\text{-}Yr$ criterion.

3.0 CONCLUSION

Both the analytical prediction and the estimate based on historical data result in a value of $4.7 \times 10^{-4}/Rx\text{-}Yr$ for a stuck open PORV from all causes. Th s value meets the specification given in II.K.3.2 Note that no credit has been assigned for the operator closing the block valve given an open PORV. Analytical predictions (given proper auxiliary feedwater response) result in a value less than .01% of PORV openings for overpressure transients (taking into account the most limiting non-anticipatory trips) and historical data shows the frequency to be less than 1.6% which satisfies the criter'on (less than 5%) specified in II.K.3.7.

Since the requirements of II.K.3.2 and II.K.3.7 are met with the current PORV configuration and set point it is not necessary to address the requirement for an automatic block valve closure system per II.K.3.1.

Table 2.2.2-1

1. The probability of a PORV opening on an overpressure transient from Section 2.1.1

for plants with PORV setpoint of 2450	$3/9 \times 10^{-6}$/Rx-Yr
for plants with PORV setpoint of 2400 (DB)	3.9×10^{-3}/Rx-Yr

2. The PORV opening probability in a transient with delayed aux. feed

A value of 1.0 was assigned for PORV opening probability if aux. feedwater was not supplied. A value of 1.4×10^{-3}/Rx-Yr for loss of all feedwater was referenced from a B&W calculation which used average unavailability as calculated in the generic aux. feedwater reliability studies (BAW-1584) in conjunction with generic EPRI data on loss of main feedwater frequency and loss of offsite power frequency.

On completion of the ongoing aux. feedwater reliability analysis (AP&L, SMUD, FPC) more specific values can be applied to those plants.

3. The PORV opening probability on operator action under ATOG guidelines

There are 3 events that call for operator opening of the PORV: a) Loss of All Feedwater. This contribution is already counted in 2 above; b) Small LOCA. Not applicable to

Table 2.2.2-1 (Cont'd)

this calculation since the plant is already in a small LOCA; c) Steam Generator Tube Rupture (considered smaller than small LOCA as defined in II.K.3.2 so argument of b) does not hold): The demand on the PORV given a tube rupture varies depending on whether offsite power is available or lost. If offsite power (Reactor Coolant Pumps) is available, only one PORV opening is required, whereas in the loss of offsite power scenario as many as 23 PORV openings are required.

The value calculated assumes that the probability of Steam Generator Tube Rupture considered with a LOOP event is small (no causal effect of LOOP or Steam Generator Tube Rupture) and therefore, the WASH-1400 of 1×10^{-3} for a LOOP given a reactor trip is used in the calculations. There have not been any tube ruptures in the cumulative B&W experience, due to the limited number of years experience. A Chi-square 50% confidence value with 0 failures is rather high (1.54×10^{-2} Rx-Yr).

Table 2.2.2-1 (Cont'd)

1.54×10^{-2}/Rx-Yr x 1 demand	Offsite Power	1.54×10^{-2}/Rx-Yr
1.54×10^{-2}/Rx-Yr $\times 10^{-3}$ Offsite Power/ event x 23 demands	Offsite Power Loss	1.54×10^{-4}/Rx-Yr
		1.57×10^{-2}/Rx-Yr

In the final calculation of probability to reclose, it should be noted that no adverse effects of the 23 demands in the loss of offsite power case on PORV operability is assumed.

4. <u>PORV opening due to instrumentation control faults</u>

   This has been estimated at 5×10^{-3}/ reactor year. This value assumes that power supply faults and other control deficiencies have been corrected by each utility.

5. <u>PORV opening probability from additional considerations from II.K.3.7</u>

   There are overcooling transients that initiate HPI and operator failure to throttle or terminate flow before the PORV setpoint is reached. There have been 8 overcooling transients that initiated

Table 2.2.2-1 (Cont'd)

HPI in 392 reactor trips. The current frequency of reactor trips is 6 trips/ Rx-Yr per plant. In this event sequence, the operator has approximately 4 minutes from time of HPI initiation until PORV setpoint is reached. The operator failure rate to terminate or throttle HPI flow is based on having ATOG in place (1.5×10^{-2}/d - based on NUREG-CR-1278 with moderately high stress). The overall probability of this sequence is therefore estimated to be 6 trips/Rx-Yr x 8/392 overcooling events/trip x 1.5×10^{-2} = 1.8×10^{-3} Rx-Yr

N.A. for DB

TOTALS 2.25×10^{-2}/Rx-Yr

2.46×10^{-2}/Rx-Yr (DB)

Note that these values are dominated by the conservative analysis of steam generator tube rupture. Analytical studies could be performed to obtain a more realistic value. Also note that the calculation for category 4 did not include operator or maintenance induced faults, such as the DB event of 10/27/80.

ATTACHMENT 10

II.K.3.3 Reporting SV and RV Failures and Challenges

The District commits to report all failures of the PORV or safety valves to reclose. All challenges will be included in annual reports.

ATTACHMENT 11

II.K.3.17 ECC System Outages

The following listing of emergency core cooling system outages was derived from a operating review of the following systems:

1. High pressure injection (HPI)

2. Decay heat removal (DHR/LPI)

3. Core flood system

4. Emergency power supply (diesel generators)

The review covered 1976, 1977, 1978, 1979, and 1980. The review was limited to those periods when the reactor was at power operation and the system outage resulted in implementing a time limit for continued reactor operation in compliance with the limiting conditions for operation in the Rancho Seco technical specifications.

ECC OUTAGES

1977

Equipment: "B" HPI System
Reason: Improper valve line-up
Date: November 11 through 12
Duration: 18.3 hours

1976

Equipment: "A" diesel generator
Reason: Faulty speed control switch
Date: December 6 through 8
Duration: 50.75 hours

1978

Equipment: "A" diesel generator
Reason: F.O. filter gasket leak
Date: October 4
Duration: 50 minutes

Equipment: "A" diesel generator
Reason: Failed fuse in voltage regulator circuit
Date: October 24
Duration: 6.75 hours

1979

Equipment: DHR pump P-261B (LPI)
Reason: Leaking shaft seal
Date: July 19 through July 20
Duration: 33.5 hours

Equipment: "A" diesel generator
Reason: Failure to start
Date: Novmeber 9
Duration: 5.3 hours

Equipment: "A" HPI system
Reason: Utilized "A" HPI to supply M.U. and seal injection while repairs were being performed on M.U. system
Date: December 6 through 8
Duration: 49.25 hours

Equipment: "B" HPI pump
Reason: Improper valve line-up
Date: December 17 through January 9, 1980
Duration: 534.25 hours (LER 79-24)

Note - July 20 through July 23 both DHR systems (LPI) were declared inoperable due to I&E Bulletin 79-02 criteria for anchor bolts - RX shutdown approximately 50.5 hours.

1980

Equipment: "B" HPI System
Reasor I&E Bulletin 79-02 analysis (pipe support design)
Date: January 2 through 5
Duration: 70.25 hours (Note - Rx was shutdown after 48 hours)

Equipment: Both DHR systems (LPI)
Reason: I&E Bulletin 79-02 analysis (pipe support design)
Date: January 4 through 5
Duration: 21.25 (Rx shutdown due to both systems being O.O.S.)

Equipment: M.U. tank isolation valve (affected "A" HPI)
Reason: Improper valve line-up
Date: January 9 through 10
Duration: 42 hours

Equipment: "A" DHR (LPI)
Reason: I&E Bulletin 79-14 review/analysis (pipe supports)
Date: July 3
Duration: 5.5 hours

Equipment: "B" diesel generator
Reason: Failure to start
Date: December 19 through 22
Duration: 91.3 hours

ATTACHMENT 12

III.A.2 Emergency Preparedness

On January 2, 1981, the District submitted a letter describing the required information on station and offsite emergency plans. Meteorological functions are discussed in the Emergency Plan in Sections 6.4.6 and 7.6.1. Appendix A to the Emergency Plan provides a list of the Emergency Plan Procedures. Procedure 9 (Offsite Dose Calculations) will describe how the meteorological data will be used to calculate the offsite radiation exposures. The information will be communicated to the necessary response groups via the communications network as described in Section 7.5.4. The counties within the 10-mile EPZ will have full representation at the EOF. It will not be necessary to transmit the entire information package to the respective county EOC's.

ATTACHMENT 13

III.D.3.4.1 Control-Room Habitability

The following report describes the review made of the Rancho Seco control room. The results of this evaluation and modifications the District presently intends to make are presented.

III.D.3.4 CONTROL ROOM HABITABILITY

Table of Contents

1. Introduction

The NRC letter of May 7, 1980, to all operating reactor licensees (ref. 8.1) imposes five additional TMI-2 related requirements. These five items are a part of NUREG-0660 "NRC Action Plan", May 1980. Task III.D.3.4 of NUREG-0660, Control Room Habitability, requires that the licensee assure that control room operators will be adequately protected against the effects of an accidental release of toxic and radioactive gases, and that the nuclear power plant can be safely operated or shutdown under design basis accident conditions (Criterion 19, "Control Room", of Appendix A, "General Design Criteria for Nuclear Power Plants", to 10 CFR 50). All facilities that have not been reviewed for conformance with Standard Review Plan (SRP--NUREG-75/087) Section 2.2.1-2.2.2, "Identification of Potential Hazards in Site Vicinity"; 2.2.3, "Evaluation of Potential Accidents", and 6.4, "Habitability Systems", shall perform the necessary evaluations and recommend appropriate modifications to meet control room habitability requirements.

To comply with the NRC letter, the control room was evaluated in three areas:

1. The control room design was reviewed for conformance to the Standard Review Plan and to identify necessary modifications.

2. Control room concentrations from postulated accidental release of toxic gases were evaluated. Potential sources within five miles from the plant site were considered.

3. Control room operator radiation exposure from airborne radioactive materials and direct radiation resulting from a design basis accident was evaluated.

The evaluation results and recommended modifications are presented in this report.

2. Summary

The Rancho Seco Nuclear Generating Station 1 control room is evaluated for conformance to current control room habitability requirements in Section 3. Areas of nonconformance were identified, and appropriate modifications are recommended in Section 4.

2.1 Control Room Modifications

The recommended control room habitability requires the following modifications:

2.1.1 The present non-redundant (train "B") emergency control room heating, ventilation and air conditioning (HVAC) system must be augmented with a redundant system (train "A").

2.1.2 The existing normal HVAC ducting within the control room must be upgraded to seismic Class I for distribution of emergency HVAC train "A" cooling.

2.1.3 The existing train "B" emergency HVAC must be upgraded to include filtered recirculation and additional cooling requirements.

2.1.4 The control room must automatically isolate on detection of high chlorine or ammonia concentrations.

2.2 Accidental Release of Toxic Chemicals

Potential sources of toxic chemicals within a 5 mile vicinity of the plant are evaluated in section 5. One offsite chemical and 11 onsite chemicals were considered for toxic gas contamination of the control room. The evaluation indicated that detection of an onsite chlorine or offsite ammonia accident requires automatic control room isolation and immediate donning of self-contained personal air breathing apparatus.

2.3 Radiation Exposure

Direct and airborne radiation exposures to the control room operators were evaluated in section 6. The direct and airborne exposures were found acceptable.

3. Evaluation of Control Room Habitability

The present and the recommended control room designs were evaluated for conformance with current habitability requirements. Table 3-1 lists the primary documents reviewed. Secondary documents referenced by these documents were evaluated as necessary.

The present and the recommended control room designs are compared in Table 3-2 to the acceptance criteria of the Table 3-1 documents. The recommended design will meet all habitability requirements.

Table 3-1. Documents Evaluated for Control Room Habitability

SRP 2.2.1-2.2.2 Identification of Potential Hazards in Site Vicinity
SRP 2.2.3 Evaluation of Potential Accidents
SRP 6.4 Habitability Systems
Regulatory Guide 1.78 Assumptions for Evaluating the Habitability of a Nuclear Power Plant Control Room During a Postulated Hazardous Chemical Release
Regulatory Guide 1.95 Protection of Nuclear Power Plant Control Room Operators Against an Accidental Chlorine Release

Table 3-2. Evaluation of Control Room for Regulatory Conformance

Item	Design: Present	Design: Recommended	Comment/Recommendation
SRP 2.2.1-2.2.2			
II.1	yes	yes	
II.2, II.3	no	yes	Offsite toxic chemical survey was not reported. This review satisfies the requirement. Ref. section 5.
SRP 2.2.3			
II	- N.A. -		Statistical evaluations not used.
SRP 6.4			
II.1: III.1.a	no	yes	Include a critical document reference file in the control room.
.b	yes	yes	
.c	yes	yes	
.d	yes	yes	
II.2 & III.2	no	yes	Include a 5 day supply of food and water for 5 people plus a medical kit in the control room.
II.3: III.3.a	N.A.	N.A.	Present design is type III.3.a(1). Recommended design will be type III.3.a(3).
III.3.b	no	yes	Present design did not consider control room in leakage.
III.3.c	no	yes	Present design was acceptable for operating license (issued in 1975). Recommended HVAC will meet current "single failure" requirements.
III.3.d(1)	yes	N.A.	Present design did not consider control room inleakage. Ref. re-analysis in section 6.2.
:			
III.3.d(3)	N.A.	yes	Recommended design will be acceptable. Ref. section 6.2.

Table 3-2. Evaluation of Control Room for Regulatory Conformance (Continued)

Item	Design Present	Design Recommended	Comment/Recommendation
II.3.a	yes	yes	
II.3.b	yes	yes	(Ref. comment for III.3.c, above)
II.3.c(2)	No	yes	Present: Emergency pressurization is 0.43 vol/hr. Ability to pressurize to 1/8" W.G. should be verified.
II.4	no	yes	Add self contained breathing apparatus for at least 5 men. A six hour bottled air supply should be available with unlimited offsite replenishment.
II.5	No	yes	Present design was acceptable for operating license. Present design will meet requirements of SRP 6.5.1 and Regulatory Guide 1.52.
II.6.a	yes	yes	
II.7	yes	yes	
II.8.a	yes	yes	
II.8.b	no	yes	The control room was not designed for large chlorine spills. The recommended design has a chlorine detector near the chlorine building. Ref. section 5.
R.G. 1.78	no	yes	See comment for SRP 6.4, II.8.b.
R.G. 1.95	no	yes	See comment for SRP 6.4, III.8.b.

4. Recommended Control Room Modifications

4.1 Non-system Modifications

The non-system modifications that were identified in Table 3-2 are summarized in Table 4.1-1.

4.2 System Modification

The control room HVAC system modifications are presented in the following five sections. The design basis of the recommended control room design is presented in section 4.2.1. The control room envelope is presented in section 4.2.2. Components that will be used in the recommended design are described in section 4.2.3. The design exfiltration and infiltration analysis procedures are presented in section 4.2.4, and changes to system operational procedures are presented in section 4.2.5.

4.2.1 Design Bases of the Recommended Control Room HVAC

A. The habitability systems will provide coverage for the control room envelope identified in Figure 4.2-1.

B. The control room emergency ventilation and air conditioning system will mantain the control room atmosphere within conditions suitable for prolonged occupancy throughout the duration of any design basis accident.

C. The control room emergency ventilation and air conditioning system will maintain suitable environment for sustained occupancy of at least 5 persons.

D. Food, water, medical supplies, and sanitary facilities will be provided for sustained control room occupancy.

E. The radiation exposure of control room personnel will not exceed the limits set by 10CFR50, Appendix A, General Design Criterion 19.

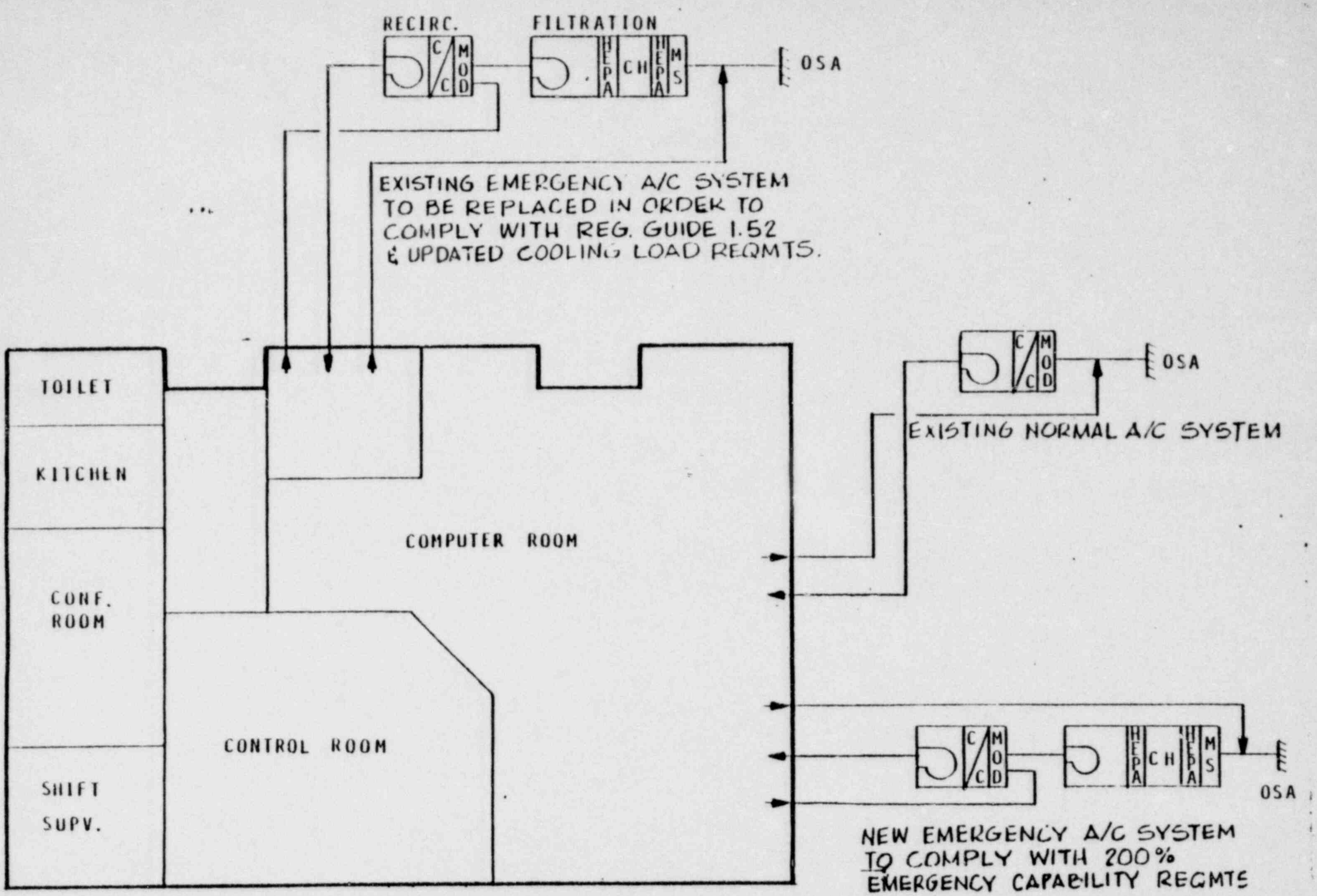
F. The habitability systems will provide the capability to detect and protect control room personnel from smoke and noxious gases.

G. Respiratory, eye, and skin protection will be provided for emergency use within areas of the control room envelope.

H. The control room emergency ventilation and air conditioning system will be capable of automatic and manual transfer from its normal operating mode to the emergency or isolation modes.

Table 4.1-1. Non-System Modifications

Requirement	Comment
SRP 6.4	
II.1: III.1.a	A critical document reference file should be available to the operators without leaving the control room.
III.2	A medical kit and a 5 day supply of food and water for 5 men should be available to the operators without leaving the control room.
II.3.c(2)	Verify ability to pressurize the control room to 1/8" W.G.
II.4	Self contained breathing apparatus for at least 5 men should be available to the operators without leaving the control room. A six hour bottled air supply should be available with unlimited offsite replenishment. Appropriate procedures should be developed. Note Regulatory Guide 1.95, section C.4.c and C.4.d.
R.G. 1.95	Administrative controls on the control room doors to limit infiltration during postulated toxic gas accidents.
Ref. 8.1	A potassium-iodide drug supply should be in the control room.



S.M.U.D.
RANCHO SECO NUCLEAR STA
CONTROL RM ENVELOPE

FIG. 4.2-1

Table 4.2-1 System Modifications

Requirement	Comment/Recommendation
SRP 6.4	
II.3: III.3.a	Present design is "isolation with recirculation with filtered pressurization." Recommended design will be "isolation with filtered recirculation and filtered pressurization."
III.3.b	Present design did not consider infiltration. Recommended design considers 10 cfm pressurized infiltration and max. 110 cfm infiltration when isolated.
II.3: III.3.c and II.3.b	Present design has a single control room emergency HVAC (train "B"). Two trains (recommended trains "A" and "B") are currently required to meet single failure criterion.
II.3.c (2)	Present emergency HVAC pressurizes control room with 400 cfm. Each train of the recommended design will pressurize the control room with max. 1000 cfm. Additional cfm is required for additional penetrations and additional design margin.
II.5	Present (train "B") emergency HVAC is not in accordance with SRP 6.5.1 and Regulatory Guide 1.52. The present emergency HVAC will be replaced with a system that is in accordance with the requirements.

I. A single active failure of a component of the control room emergency ventilation system, assuming a loss of offsite power, will not impair the ability of the system to comply with design bases A, B, C, E, F, and H, listed above.

J. The control room emergency HVAC will be designed to remain functional during and after a design basis earthquake.

K. All air ducts and their supports required by the emergency HVAC will be Seismic class I.

L. All air ducts and supports not required by the emergency HVAC will be evaluated for impact on seismic class I systems.

4.2.2 Definition of the Control Room Envelope

The areas, equipment, and materials to which the control room operator could require access during an emergency are shown in Figure 4.2-1.

The volume of the emergency zone served by the HVAC system in the emergency mode or the isolation mode is 55,300 cubic feet.

4.2.3 Component Description

The control room emergency HVAC will be provided by two 100% capacity trains as shown in fig. 4.2-1. The trains will be designed in accordance with Standard Review Plan section 6.5.1 "ESF Atmosphere Cleanup Systems" and Regulatory Guide 1.52 (ref. 8.7). Each train will include an emergency air conditioning unit, an outside air filtration unit and a physically separated outside air intake.
The ventilation filtration supply unit will contain a prefilter, two HEPA filter banks, an activated charcoal adsorber, a heating coil, a fan, and associated ductwork. The emergency recirculation type air conditioning unit will contain a prefilter, a fan and a cooling coil. Automatic dampers will be provided for system isolation purposes.

A. Heater

In order to maximize carbon adsorber efficiency, an electric heating coil will be provided in the outside air filter unit to lower the relative humidity of the incoming air to 70% or less.

B. HEPA Filters

HEPA filter banks will be upstream and downstream of the associated carbon adsorber. The HEPA filters will be designed and tested in accordance with Regulatory Guide 1.52 and ERDA 76.21 (ref. 8.2).

C. Carbon Adsorbers

The carbon adsorbers for the outside air filtration units will be tray type "2 inch" filters designed and tested in accordance with Regulatory Guide 1.52.

D. Emergency Recirculation Train Fans

The emergency recirculation trains fans will be Seismic Class I and are capable of delivering the design required ft^3/min flowrate with all filters at their maximum anticipated pressure drop.

E. Control Room Access Doors

To minimize inleakage, the control room access doors have air-tight seals and will be equipped with self-closing devices that shut the doors automatically.

F. Radiation Detectors

Redundant radiation detectors are installed in the control room normal supply air duct.

The remainder of the system; i.e., supply/recirculation fans, exhaust fans, ducting, dampers, etc., will be components that function during normal and emergency operation.

4.2.4 Leak Tightness

The exfiltration and infiltration analyses will be performed using the methods and assumptions given in NAA-SR-10100 (ref. 8.3), the ASHRAE Handbook of Fundamentals-1977 Edition (ref. 8.4), and Regulatory Guide 1.78.

4.2.5 System Operational Procedures

The control room HVAC operation in the normal mode is unchanged.

The control room HVAC operation in the emergency mode is unchanged excepting the independent operation of two emergency HVAC trains, i.e. the present train "B" and the recommended train "A".

The control room HVAC operation in the isolation mode is unchanged except the independent operation of the two emergency HVAC trains and the addition of automatic isolation and alarm on detection of high chlorine or ammonia concentration. Toxic gas detector placement, setpoints, response times and control room operator procedures are presented in section 5.4.

5. Evaluation of Control Room Habitability after Postulated Toxic Gas Accidents

The evaluation of postulated toxic gas accidents proceeded as follows. In section 5.1, potential offsite toxic gas accidents are identified. In section 5.2, potential onsite toxic gas accidents are identified. In section 5.3, postulated toxic chemical accidents are evaluated for potential control room concentrations. Finally, section 5.4 discusses the design requirements to mitigate the consequences of two accidents that can lead to excessive control room concentrations.

5.1 Identification of Offsite Chemicals

Toxic chemicals within a 5-mile vicinity of the plant were evaluated for potential gas concentrations in the control room. Within the vicinity (Fig. 5.1-1) there are only two transportation routes: a Southern Pacific Railroad line and California State Highway 104 (Twin Cities Road). There are no military facilities or large chemical concerns.

5.1.1 Rail

The Southern Pacific Railroad (SPRR) line by the plant connects Stockton-Sacramento lines (more than 6 miles west of the plant) to a terminus in Ione (about 10 miles east of the plant).

Considering the types of shipments on the line, the railroad concluded that toxic chemicals are not transported by the plant.

5.1.2 Roads

The cities of Sacramento, Stockton and Ione form a road transportation triangle shown to scale in figure 5.1-2. There are no other major transportation routes within the triangle. Because there is no industry within the 5 mile vicinity of the plant, companies in the vicinity of Ione were evaluated for use of toxic chemicals that could be transported by the plant.

The companies that were investigated for transportation of toxic chemicals are listed in table 5.1-1. The table also indicates the results of the investigation. At a minimum, each company was asked (i) Do they transport toxic chemicals by the plant, (if yes, then they were questioned as to quantity and frequency) and (ii) Could they identify any other company that might use toxic chemicals and might transport by the plant. (Each of the investigated companies seemed to take a substantial interest in the safe operation of their nuclear neighbor.)

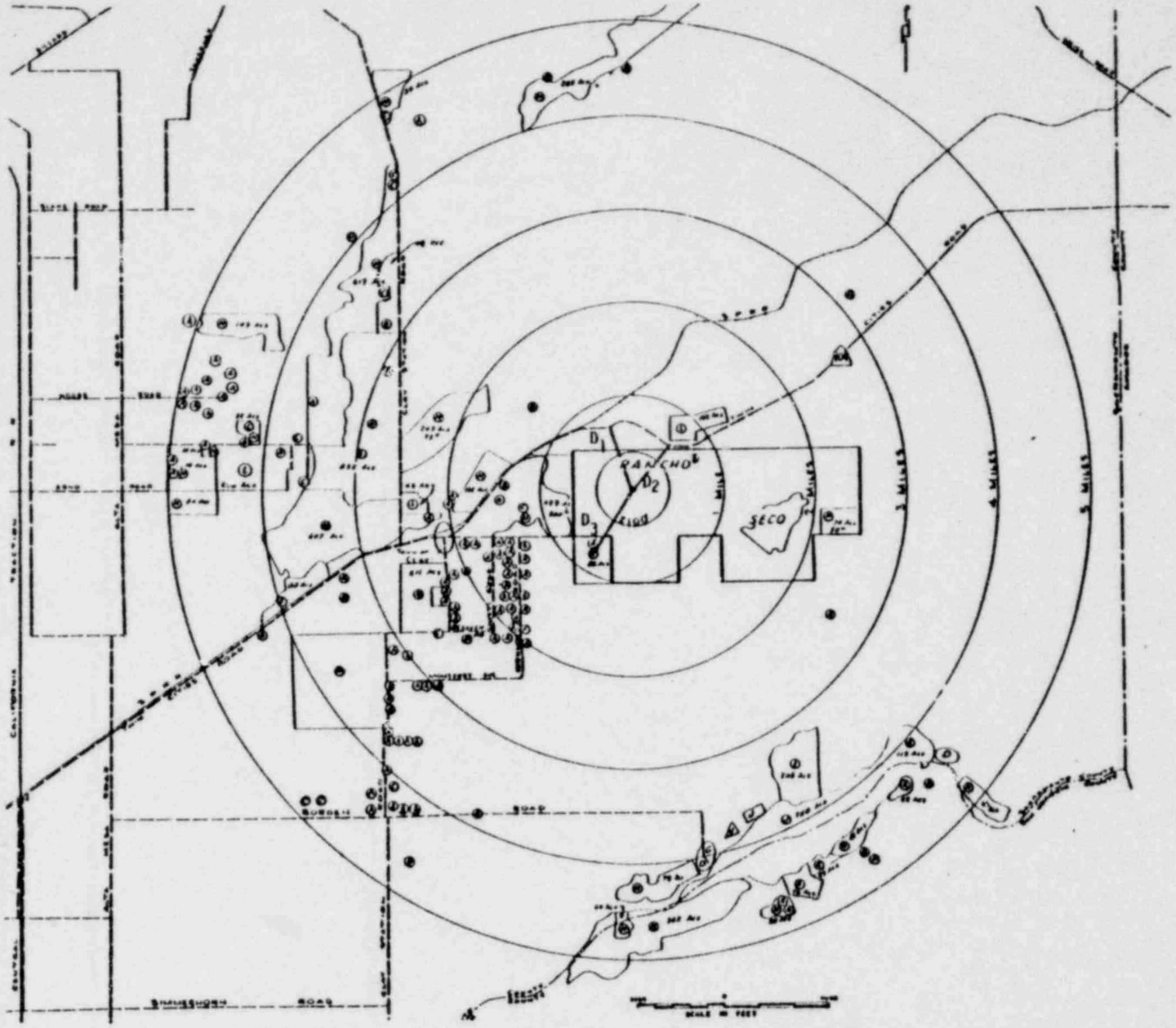
John Taylor Fertilizer indicated that they transport 1000 gallon tanks of 82% ammonia at irregular intervals to the various agricultural concerns around the plant. As a rule, the company indicated that the ammonia would be used within hours of delivery. To conservatively calculate the potential consequences of an ammonia road accident, the ammonia was assumed to be pure (100%) ammonia, and the accident was assumed at the closest offsite location serviced by Taylor (D_3 in figure 5.1-1).

Table 5.1-1

Companies Surveyed for Hazardous Chemicals Used or Transported Within 5 Miles of the Plant

Company/Location	Hazardous Chemicals
American Lignite Products (209) 274-2407 South of Buena Vista	None
Bendix (209) 223-1660 (Includes Amador Central Railroad) Ione	None
Owens-Illinois (209) 274-2424 Highway 124	None
Interpace (209) 274-2471 Highway 124	None
Zenith Clay (209) 274-2453 Highway 124	None
Preston School (209) 274-2421 State of California, Dept. of Youth Authority	None
*Simplot (209) 457-2387 Lodi	None
*John Taylor Fertilizer (916) 776-2113 Sacramento	82% ammonia in 1000 gal tanks

*Largest chemical distributors in the area.



LEGEND

A 1 HOUSE
B BARN (S)
C 1 HOUSE & 1 BARN
D 1 HOUSE & BARN (S)
E FEED MIXING PLANT
F HORSE PADDOCK
G FEED MILL
H IRRIGATED PASTURE
I TOMATOES
J TOMATOES, BEANS, & VARIOUS CROPS
K ALFALFA
L CLOVER SEED
M OATS
N BEANS
* HEADS OF BEEF
O ORCHARD.

TOWN OF CLAY (7 HOUSES)
ALL ACREAGE IS WITHIN THE 5-MILE RADIUS

Clostest Approach

D_1 4500 Ft. S.P.R.R.
D_2 3200 Ft. Hwy 104
D_3 2900 Ft. Feed mixing plant

FIGURE 5.1-1
CURRENT LAND USE WITHIN A 5 MILE RADIUS

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11-9-71

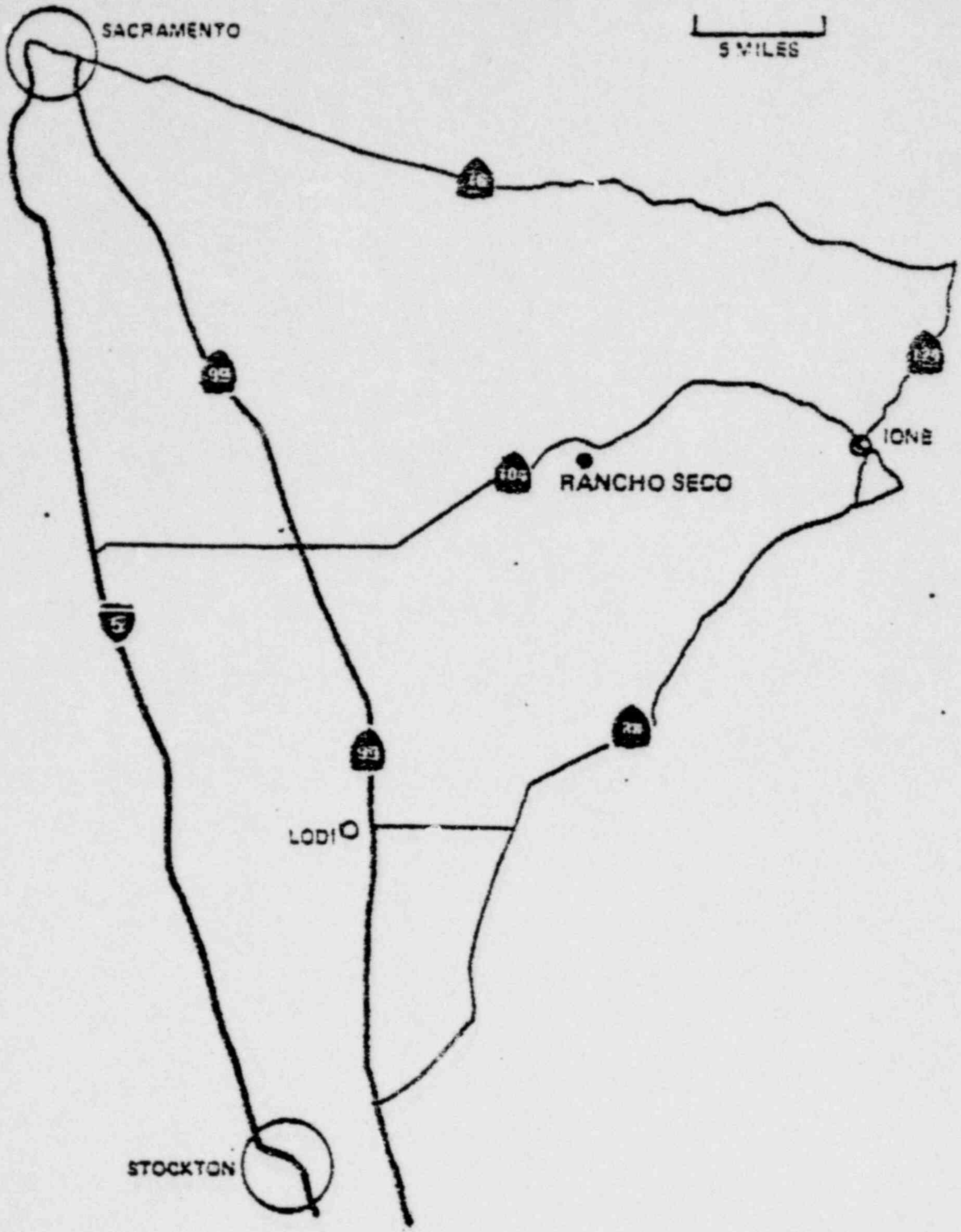

Figure 5.1-2

MAJOR TRANSPORTATION ROUTES NEAR THE PLANT. COMPANIES NEAR IONE PREFERENTIALLY USE HWYS 16 AND 88 OVER HWY 104.

The other companies indicated that they preferentially use highways 16 and 88 rather than highway 104.

5.2 Identification of Onsite Chemicals

Onsite toxic chemicals were identified by a site survey, review of the FSAR, review of plant general arrangement and plot plan drawings, review of the Plant Equipment List and review of the Plant System Descriptions. Twelve toxic chemicals were identified. Their location, distance from the control room and quantity are identified in table 5.2-1 and figures 5.2-1 and 5.2-2.

Caustic soda (sodium hydroxide), the last chemical in Table 5.2-1, was eliminated from further analysis. Caustic soda does not generate gases and its vapor pressure at the elevated temperature of 739°C is 1mm Hg, consequently at near ambient temperatures the vapor evolution is negligible.

5.3 Toxic Gas Concentrations in the Control Room after Postulated Accidents

The verified Bechtel computer program TOXGAS was used to evaluate toxic gas concentrations in the control room. The model is consistent with NUREG-0570 (ref. 8.5), Regulatory Guides 1.78 and 1.95. Accidents were postulated to occur with the "short term" meteorology used to answer AEC question 9.17 in the FSAR. Higher wind speeds were evaluated when the higher windspeed increased the accident severity.

The control room toxic gas concentrations for the one chemical in table 5.1-1 (ref. section 5.1.2) and the first 11 chemicals of table 5.2-1 are compared to the intent of Regulatory Guide 1.78 in table 5.3-1. Only two chemicals require protective action: Ammonia (offsite) and chlorine (onsite). Procedures for postulated accidents with the two chemicals are presented in section 5.4.

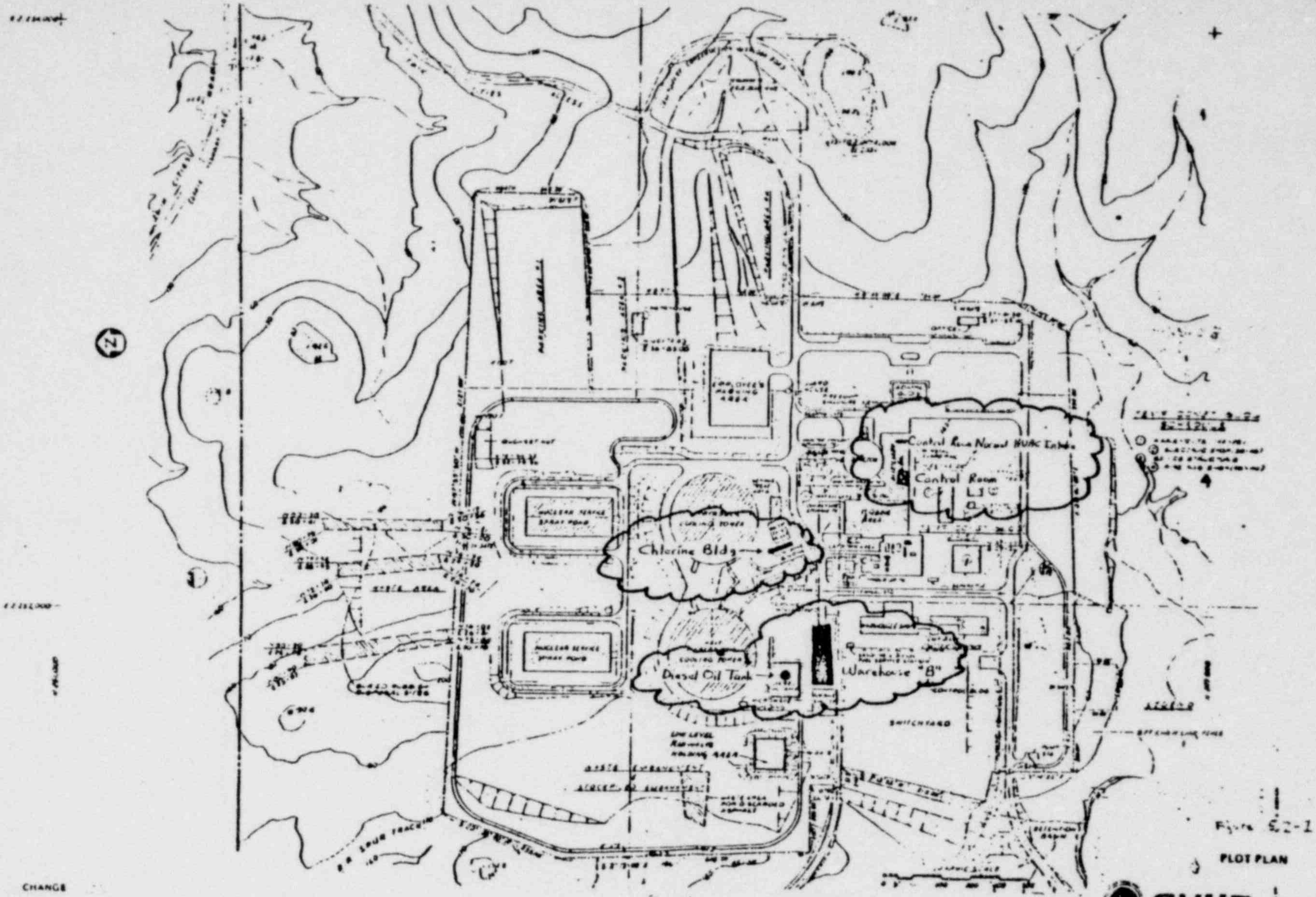

Figure 5.2-1

PLOT PLAN

SMUD

SACRAMENTO MUNICIPAL UTILITY DISTRICT

Amendment 29

CHANGE

SMUD PSAR – 161
6-6-72

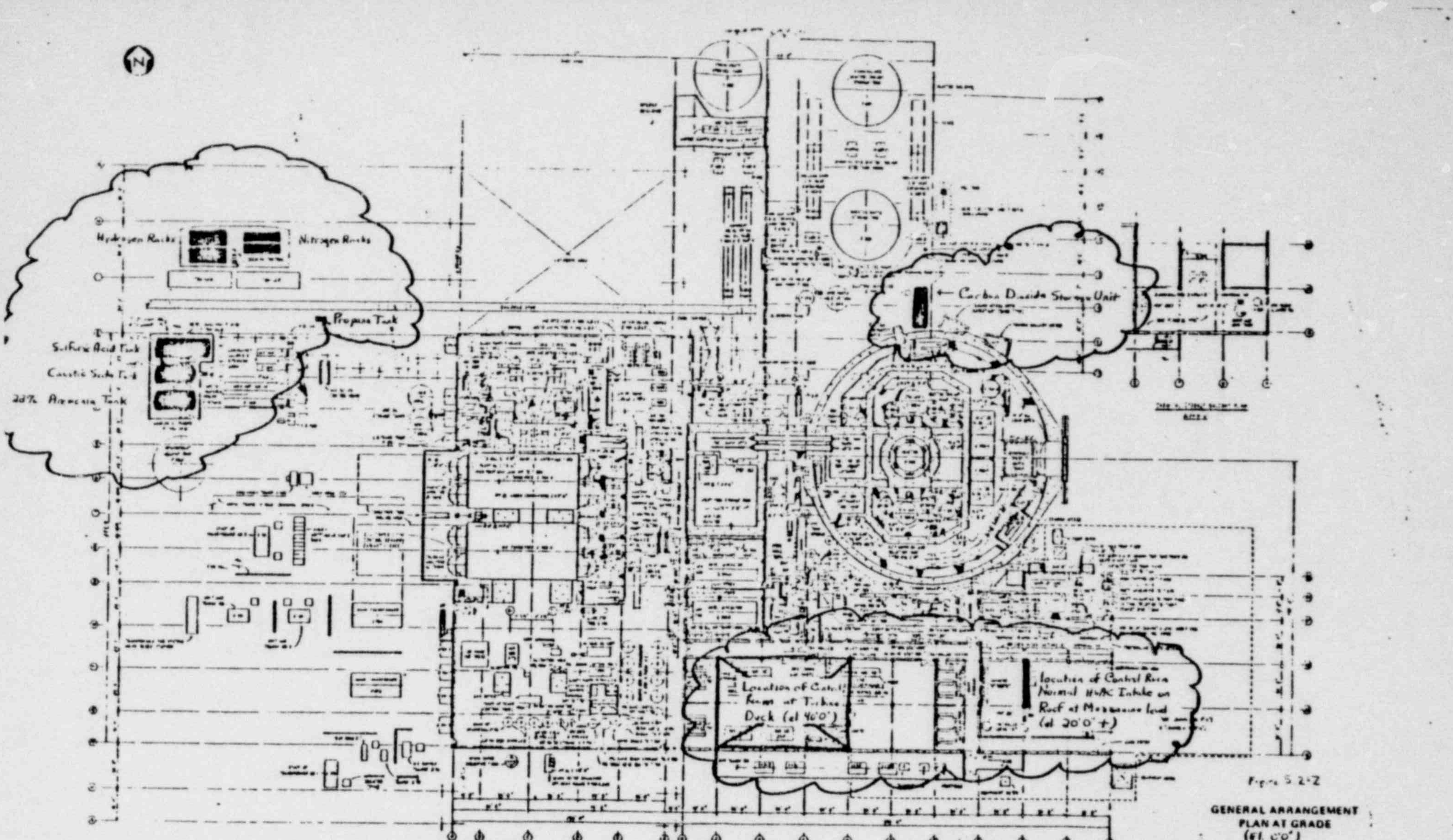

Figure 5.2-2

GENERAL ARRANGEMENT
PLAN AT GRADE
(el. 0'0")

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Amendment 29

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SMUD FSAR - 164
1-30-75

Table 5.2-1 Onsite Toxic Chemicals

Chemical	Quantity	Distance to Normal CR Intake	Location/Comments
Ammonia (28%)	12000 gal	500 ft	Tank V-745, west of auxiliary boiler
Carbon Dioxide	7.5 tons	200 ft	Tank V-998, CO_2 building
Chlorine	1 ton tanks	550 ft	Tanks V-754A thru C, Chlorine Building
Diesel Oil	200,000 gal	900 ft	Tank T-897, south of W. Cooling Tower
Hydrazine (35%)	55 gal drum	800 ft	Warehouse "B"
Hydrogen	30000 scf per 4 tanks	550 ft	Tanks V-920, North of auxiliary boiler
Hydrogen Peroxide	55 gal drum	800 ft	Warehouse "B"
Freon 113	55 gal drum	800 ft	Warehouse "B"
Nitrogen	30000 scf per 5 tanks	550 ft	Tanks V-925, North of auxiliary boiler
Propane	115 gal	450 ft	Tank V-935, North of auxiliary boiler
Sulfuric acid	16000 gal	500 ft	Tank T-743, South of W. Cooling Tower
Caustic Soda	11000 gal	500 ft	Tank T-741, South of W. Cooling Tower

Table 5.3-1. Evaluation of Hazardous Gas Concentrations in the Control Room After Postulated Accidents and Without Protective Action

Item	Vessel Contents	TLV	Objectives of SRP 6.4 met?
1	28% Anhydrous Ammonia	25[4]	Yes[1]
2	Carbon Dioxide	5000[4], 20000[5]	Yes[2]
3	Chlorine	1[4], 4[5], 15[6]	**No**[3]
4	Diesel Oil	1[4]	Yes[1]
5	35% Hydrazine	0.1[4], 1.0[5]	Yes[2]
6	Hydrogen	143000[4]	Yes[1]
7	Hydrogen Peroxide	1[4]	Yes[1]
8	Freon 113	1000[4]	Yes[1]
9	Nitrogen	143000[4]	Yes[1]
10	Propane	143000[4]	Yes[1]
11	Sulfuric Acid	0.25[4]	Yes[1]
12	Ammonia (Offsite)	25[4], 55[5], 100[2]	**No**[3]

1. Threshold limit value for an acute 8 hour exposure is not exceeded.

2. Threshold limit value for an acute 8 hour exposure is temporarily exceeded, but TLN for a 1 hour exposure is not exceeded.

3. Threshold limit value for 2 minute exposure is exceeded.

4. Threshold limit value for continuous, 8 hour exposure (ppm).

5. Threshold limit value for a 1 hour exposure (ppm).

6. Threshold limit value for a 2 minute exposure (ppm).

5.4 Design Requirements to Mitigate Consequences of Postulated Toxic Gas Accidents

Of the two postulated toxic chemical accidents that lead to excessive control room concentrations, the chlorine accident is the more restrictive. Control room design alternatives for the chlorine accident are evaluated in section 5.4.1. The design recommended in section 5.4.1.3 is then evaluated for the postulated ammonia accident in section 5.4.2. Procedures common to all toxic chemical accidents are presented in Section 5.4.3. The present design is evaluated in Section 5.4.4.

5.4.1 Postulated Rupture of a One Ton Chlorine Tank in the Chlorine Building

Design alternatives for postulated toxic gas accidents that lead to excessive control room concentrations are presented in Standard Review Plan Section 6.4 subsection III.5.b and Regulatory Guides 1.78 and 1.95. Three limiting designs were evaluated. In Section 5.4.1.1, designs that do not require operators to don personal air breathing apparatus are evaluated. In Section 5.4.1.2, designs without remote chlorine detectors are evaluated. In Section 5.4.1.3, the recommended design is evaluated.

5.4.1.1 Designs not Requiring Air Breathing Apparatus

Rapid detection and control room isolation with minimal infiltration can limit toxic gas concentrations to acceptable levels. To limit the maximum chlorine concentration to 5 ppm, the control room infiltration must be below 5 cfm (0.005 air changes per hour). Consequently, this design alternative is not recommended.

5.4.1.2 Detection at the Outside Air Intake

Rapid chlorine detection at the control room normal HVAC (outside) air intake with control room isolation and the operator use of air breathing apparatus is an acceptable design if the operators have at least two minutes to put on the apparatus. With detection at the air intake and immediate control room isolation, the maximum tolerable control room infiltration is 30 cfm (0.03 air exchanges per hour). Consequently, this design is not recommended.

5.4.1.3 Recommended design: Detection at the Chlorine Building

By chlorine accident detection at the chlorine building, the operators can take the precautionary action of donning personal air breathing apparatus at least two minutes before control room concentrations exceed 15 ppm. Limiting control room isolation parameters for the recommended design are presented in Table 5.4-1.

The chlorine detector location or setpoint may be altered within limits to avoid unnecessary "Spurious" activations. Postulated accidents that can lead to excessive control room concentrations require source (and intermediate) concentrations in excess of 15 ppm. Therefore, a detector

Table 5.4-1. Recommended Control Room Isolation Parameters

	Postulated Accident	
	Chlorine	Ammonia
Detector location (Separate Train "A" and Train "B")	Chlorine Building (Ref. Section 5.4)	Control Room normal HVAC outside air intake
Detector Setpoint	1 ppm	25 ppm
Maximum isolation delay delay time (includes detector response time and control room isolation time)	>20 sec	>30 sec
Maximum Control Room infiltration (including 10 cfm for doors)	>110 cfm[1] = 0.11 air exchangers per hour	
Minimum time for operators to don air breathing apparatus	>2 min	>10 min
Objectives of SRP 6.4 met	Yes	Yes

1 Set by chlorine accident.

setpoint much higher than the recommended 1 ppm will not compromise control room habitability. The recommended setpoint provides indirect protection for all plant personnel. Neither does the detector have to be in the chlorine building. The primary purpose of remote detection is to provide an early alarm for operators to don breathing apparatus. The limiting chlorine accident has a 1.5 m/sec (3 mph) windspeed, for which control room isolation can be delayed by more than 100 seconds, but detection (and operator warning) must not be delayed by more than 20+ seconds after the accident. Consequently, the chlorine detector may be placed as much as 15m from the chlorine building (in the direction of the control room). The recommended placement provides indirect protection for all plant personnel.

5.4.2 Postulated Rupture of a 1000 gal offsite Ammonia Container

Automatic detection and control room isolation is required for an offsite ammonia accident because the control room concentration of ammonia can increase to 100 ppm in less than the two minutes after the odor is detectable by the operators. With ammonia detection at the air intake and control room infiltration of under 110 cfm (ref. Table 5.4-1) the operators have many minutes to put on air breathing apparatus. The potential chlorine accident clearly requires operators to immediately don air breathing apparatus. The potential ammonia accident is such that many minutes are available to evaluate the accident before air breathing apparatus is required. Procedures recognizing the time difference and the odor threshold of 47 ppm may be developed. The recommended procedure is immediate (within 2 minutes) operator use of air breathing apparatus.

5.4.3 Procedures for Toxic Chemical Accidents

Regulatory Guide 1.78 Section C.15 requires that "Emergency procedures to be initiated in the event of a hazardous chemical release within or near the station should be written." Minimum procedure contents are presented in that section and Section C.6 of Regulatory Guide 1.95.

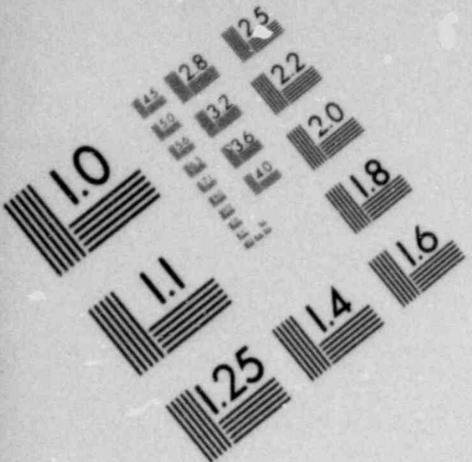
5.4.4 Present Control Room Design

Because the present normal control room HVAC intake flow is less than that for the recommended design (1280 cfm versus approximately 1600 cfm), the maximum control room concentration of toxic chemicals will be smaller at any given time. The evaluation of Section 5.3 is therefore a conservative calculation for the present design.

The present design does not have automatic isolation on detection of high ammonia or chlorine concentration. Therefore the design is acceptable only for accidents less severe than postulated rupture of a tank. Control room isolation after operator detection of a chlorine odor is presented in the FSAR response to AEC question 9A.44. An identical procedure is appropriate for ammonia accidents.

IMAGE EVALUATION
TEST TARGET (MT-3)

6″



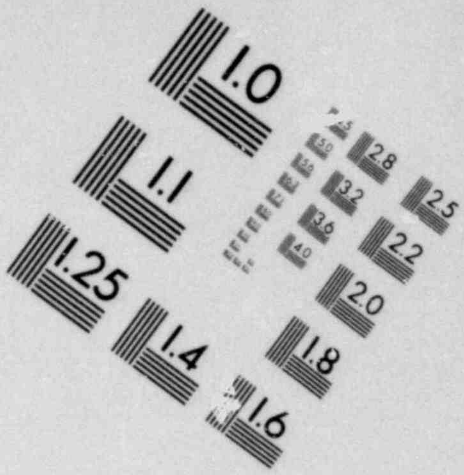
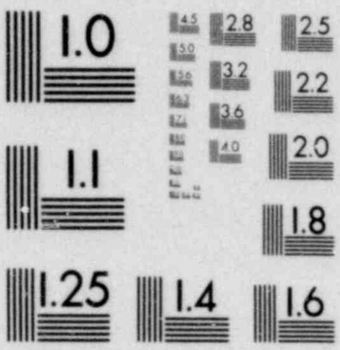
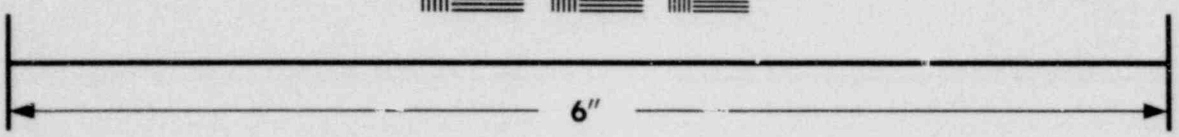
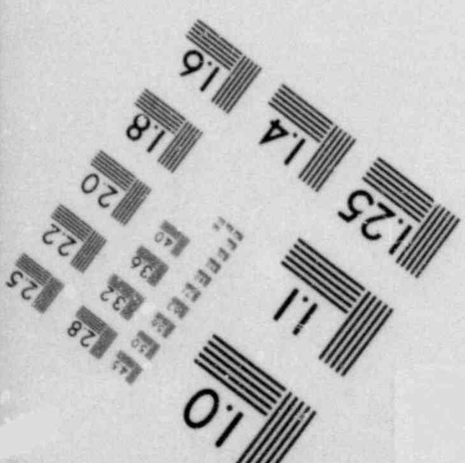
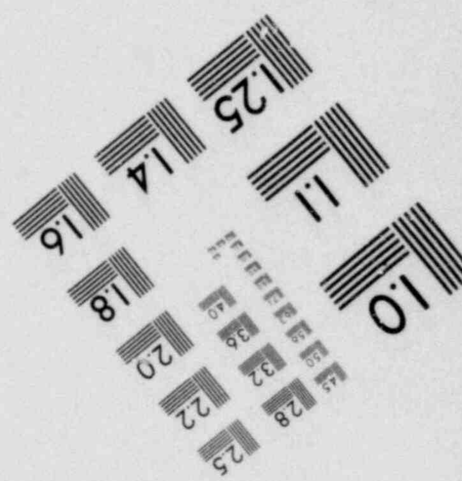


IMAGE EVALUATION TEST TARGET (MT-3)









6. Analysis of Control Room Operator Radiation Exposure from a Design Basis Accident

6.1 Direct Radiation

Direct radiation in the control room was evaluated in reference 8.6. The evaluation resulted in post accident radiation zone maps and curves of normalized dose rate versus time. The pages from that report that were used in this evaluation are included as appendix A.

The radiation zone map at the turbine deck (el. 40'0") shows the control room console area and entryway is in radiation zone A (less than 15 mr/hr), and the adjoining support instrumentation area is in zone B (15 to 100 mr/hr). The control room kitchen, bathroom, conference room and supervisor's office are also in zone A.

A realistic estimate of the maximum operator dose would include consideration of operator relative occupancy factors in zones A and B. In this evaluation, the conservative assumption was used that the entire control room is in zone B and that the dose rate is the upper limit within the zone of 100 mr/hr.

The integrated operator dose with radiation source A and C is then 0.73 rem and 1.4 rem, respectively. An assumed relative zone A occupancy factor of 50 percent would reduce the maximum operator dose to 0.42 rem and 0.81 rem, respectively.

6.2 Airborne Radioactive Materials

In the interregnum between the FSAR amendment 20 calculations of the control room operator dose from airborne radioactive materials and the present evaluation the regulatory assumptions on control room (unfiltered) infiltration have become better defined. The effect of an assumed 10 cfm infiltration (Ref. Table 4.2.1) and the full capacity for control room pressurization is evaluated in section 6.2.2 for the current design. Then the recommended control room design was evaluated. The results of all three calculations and the relevant FSAR calculation are summarized in Table 6.2-1.

6.2.1 Comparison to the FSAR

Thyroid and whole body doses were presented in the FSAR response to AEC Question 9.17. For comparison, the doses are included as the first line of Table 6.2-1.

Table 6.2-1. Control Room Operator Radiation Exposure from Airborne Radioactive Materials

Dose Type	Thyroid (rem)	Wholebody (rem)
6.2.1 FSAR Response (Amendment 20)	1.79	0.184
6.2.2 Re-evaluated current design	11	0.30
6.2.3 Recommended design	4	0.36
Maximum Permissible Dose 10 CFR 50, Ap. A, G.D.C. 19 and SRP 6.4 Section II.8.a	30	5

6.2.2 Re-evaluation of Current Control Room Design

Standard Review Plan Section 6.4 requires inclusion of a minimum 10 cfm infiltration to account for backflow during opening and closing of control room doors. The control room dose was recalculated using the full 400 cfm pressurization flow with 10 cfm infiltration. The second line of Table 6.2-1 shows that the operator dose is less than the allowable operator exposure.

6.2.3 Recommended Control Room Design

The recommended control room design differs from the current design by having a larger (maximum) design pressurization flow (approximately 1000 cfm versus 400 cfm) and the addition of approximately 1000 cfm of filtered recirculation. The combined effect of the larger inflow and added recirculation is a reduced thyroid dose and small changes in the wholebody dose (3rd line of Table 6.2-1).

Table 7-1. Information Requested by the NRC for Control Room Habitability Evaluation

1. Control Room Mode of Operation, i.e., pressurization and filter recirculation for radiological accident isolation or chlorine release

2. Control Room Characteristics

   a. air volume of control room
   b. control room emergency zone (control room, critical files, kitchen, washroom, computer room, etc.)
   c. control room ventilation system schematic with normal and emergency air flow rates
   d. infiltration leakage rate
   e. HEPA filter and charcoal absorber efficiencies
   f. closest distance between containment and air intake
   g. layout of control room, air intakes, containment building, and chlorine or other chemical storage facility with dimensions
   h. control room shielding including radiation streaming from penetrations, doors, ducts, stairways, etc.
   i. automatic isolation capability-damper closing time, damper leakage and area
   j. chlorine detectors or toxic gas (local or remote)
   k. self-contained breathing apparatus availability (number)
   l. bottled air supply (hours supply)
   m. emergency food and potable water supply (how many days and how many people)
   n. control room personnel capacity (normal and emergency)
   o. potassium iodide drug supply

3. On-site storage of chlorine and other hazardous chemicals

   a. total amount and size of container
   b. closest distance from control room air intake

4. Off-site manufacturing, storage or transportation facilities of hazardous chemicals

   a. identify facilities within a five-mile radius
   b. distance from control room
   c. quantity of hazardous chemicals in one container
   d. frequency of hazardous chemical transportation traffic (truck, rail, and barge)

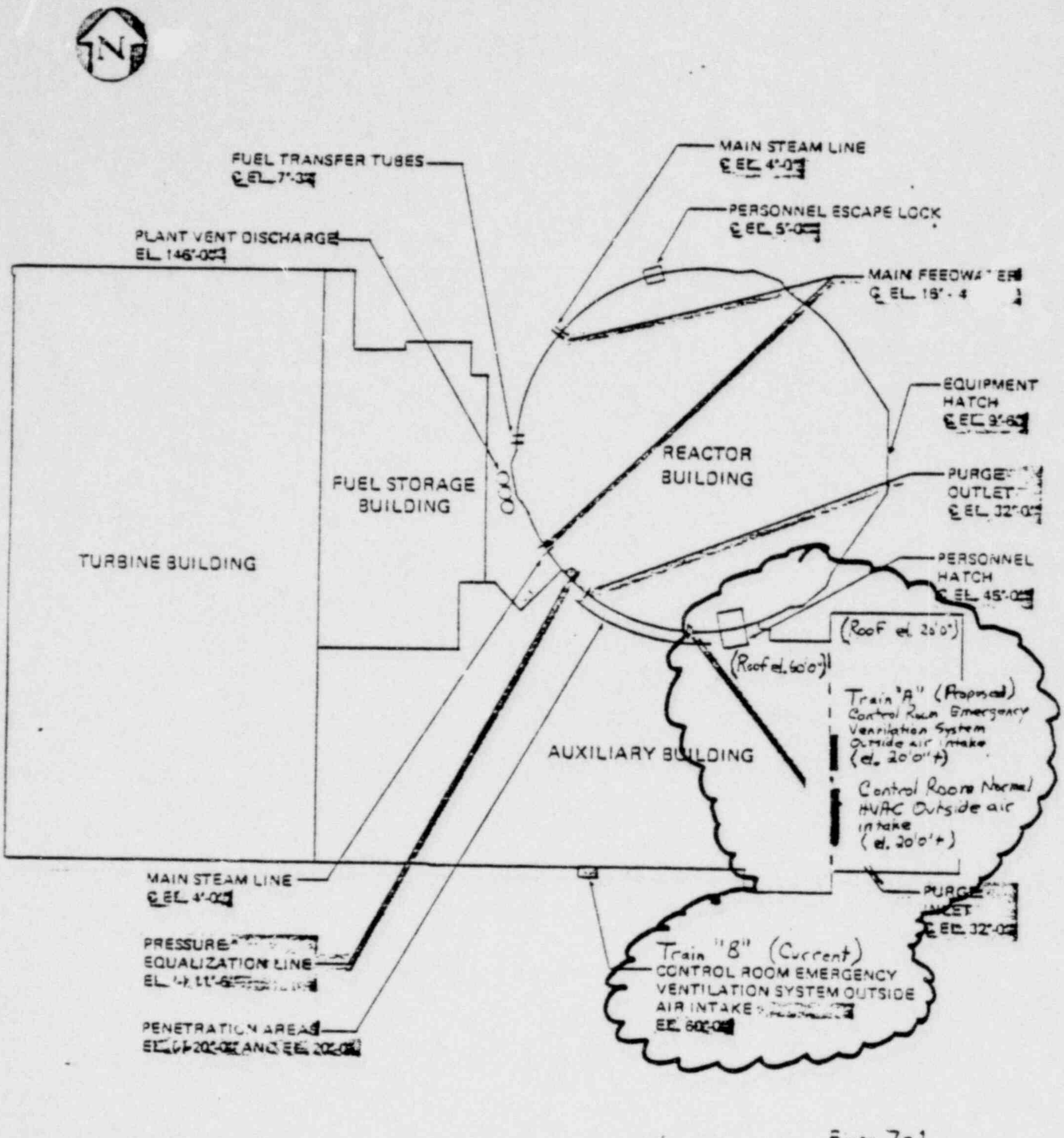


Figure 7-1
RELATIONSHIP OF REACTOR BUILDING LEAKAGE POINTS TO THE CONTROL ROOM EMERGENCY AIR INTAKE

CHANGE

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3-17-73

SACRAMENTO MUNICIPAL UTILITY DISTRICT

Table 7-2. Response to Information Requested by the NRC for Control Room Habitability Evaluation

Current Design		Recommended Design
1. Isolation and recirculation with filtered pressurization		Isolation, filtered recirculation and filtered pressurization.
2a. Control room volume: 55,300 ft^3		(Same)
b. Normal control room and control room isolation envelope are the same		
c. Normal flow		
intake fan	1280 cfm	1600 cfm*
recirculation fan	12800 cfm	16000 cfm*
Emergency flow		
intake fan	400 cfm	1000 cfm*
recirculation fan	5000 cfm	16000 cfm*
		Filtered recirculation - 1000 cfm*
d. Infiltration (pressurized)	10 cfm	(Same)
Infiltration (isolated)	-	<110 cfm*
e. Filter efficiencies (Ref. Regulatory Guide 1.52)		
Particulates	95%	95%
Charcoal - Elemental, organic iodine	99%	99%
Ref. Figs. 5.2-2 and Fig. 7-1		(Same)
g. Ref. Figs. 5.2-1, 5.2-2 and 7-1		(Same)
h. Ref. 8.6 (Also excerpted in Appendix A)		(Same)
i. See FSAR Table 9A-1 Closing time <10 sec		(comparable)

Table 7-2. Response to Information Requested by the NRC for Control Room Habitability Evaluation (Continued)

Current Design	Recommended Design
j. None	Ref. sections 5.3 and 5.4
k. None (4 available in Chem Lab)	5 in control room
l. None	Ref. Table 4.1-1
m. None	Ref. Table 4.1-1
n. At least 5	At least 5
o. None (Available in decontamination locker)	Ref. Table 4.1-1

*Maximum values used in the analysis.

8. References

8.1 _____, NRC Letter to All Operating Reactor Licensees, 7 May 1980.

8.2 ERDA 76-21, "Nuclear Air Cleaning Handbook," Oak Ridge National Laboratory, C. A. Burchsted, J. B. Kahn, and A. B. Fuller, March 31, 1976.

8.3 NAA-SR-10100, "Conventional Buildings for Reactor Containment," AEC Research and Development Report, June 1965.

8.4 _____, "Handbook of Fundamentals", ASHRAE, 1977 edition.

8.5 NUREG-0570, "Toxic Vapor Concentrations in the Control Room Following a Postulated Accidental Release", NRG J. Wing, 1979.

8.6 Letter from S.M.U.D. to R. W. Reid, USNRC, April 11, 1980. Relevant portions excepted as appendix A.

8.7 Regulatory Guide 1.52,"