



ARKANSAS POWER & LIGHT COMPANY
 POST OFFICE BOX 551 LITTLE ROCK, ARKANSAS 72203 (501) 371-4000

October 31, 1979

1-109-25

Director of Nuclear Reactor Regulation
 ATTN: Mr. R. W. Reid, Chief
 Operating Reactor Branch #4
 U. S. Nuclear Regulatory Commission
 Washington, D. C. 20555

Subject: Arkansas Nuclear One-Unit 1
 Docket No. 50-313
 License No. DPR-51
 Outstanding Items Related to
 B&W Small Break Analysis
 (File: 1510.1)

Gentlemen:

Arkansas Power & Light Company herein provides information requested in Dr. D. F. Ross' letter of August 21, 1979. The following items required in Attachment A to Enclosure 1 of the Ross letter are attached:

- | | | |
|--------------|--|--------------|
| Item 1B | Relief and Safety Valve
Flow Model Justification. | Attachment 1 |
| Item 2A | OTSG 3-Mode Model
Justification. | Attachment 2 |
| Item 3A - 3D | Noncondensable Gases
in the Primary System. | Attachment 3 |
| Item 4 | CRAFT-2 Simulation of TMI-2
Accident. | Attachment 4 |

Items 3A-3D are provided as interim responses at this time. These interim responses will be incorporated into a final package and submitted to you by November 15, 1979.

The remaining items required by Attachment A to Enclosure 1 of the Ross letter will be submitted per the following schedule.

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Mr. R. W. Reid

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October 31, 1979

Item 1A	CRAFT Analyses with sequential AFW Flow	12/11/79
Item 2B	Repressurization to PORV Set-point with PORV sticking open	
	Qualitative Assessment	11/15/79
	Detailed Analysis	2/11/80

Items 5 and 6, the semiscale small break experiment prediction and the LOFT small break prediction respectively, are being forwarded to you directly from B&W.

Very truly yours,

John M. Giff
David C. Trimble
Manager, Licensing

DCT/DMG/ew

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RESPONSE TO QUESTION 1B OF ATTACHMENT A TO D.F. ROSS
(NRC) LETTER DATED 8/21/79

Question

- 1B. Provide justification of relief and safety valve flow models used in the CRAFT2 code.

RESPONSE

The CRAFT2 code, which is documented in topical report BAW-10092, Rev. 2¹, does not have any special models for prediction of the fluid discharge through the relief and safety valves. Rather, they are modeled as leak paths from the pressurizer control volume to the containment. Thus, the Bernoulli (orifice) equation is used for subcooled discharge, while the Moody correlation is used for saturated steam or two-phase discharge. These models are the same as those used in B&W's ECCS Evaluation Model.² Since little information exists on the flowrate through pressurizer valves for subcooled or two-phase fluid conditions, it is impossible to ascertain the accuracy of this modeling technique. Since pressurizer leaks are inherently less severe than the breaks in the cold leg pump discharge piping analyzed to demonstrate compliance to 10 CFR 50.46, a truly realistic model for the discharge rates is not necessary. However, the modeling technique utilized is expected to reasonably approximate the discharge rates and their subsequent effect on the RCS.

System response to relief valve actuation have been analyzed and submitted to the Staff in Section 6 of the May 7, 1979³, report. The cases specifically analyzed were:

1. A loss of main feedwater accident which results in actuation and a subsequent sticking open of the pressurizer relief valve was addressed. Offsite power was assumed to remain available and only one HPI train was used for emergency core cooling. This analysis is similar to the TMI-2 event that occurred on March 28, 1979, and demonstrated that, if one HPI pump remained available, no core uncover would have occurred.
2. A stuck open PORV assuming a loss of offsite power and only one HPI train available was analyzed. Results of this evaluation demonstrated that core uncover would also not occur.

An additional analysis of the effect of a pressurizer break which supplemented those presented in reference 3, was provided to the Staff in a letter from J.H. Taylor (B&W) to R.J. Mattson (NRC) dated May 12, 1979⁴. That analysis examined the effect of the stuck-open PORV case, Case 2 above, except the auxiliary feedwater system was assumed inoperable. The results of that evaluation showed that, even without auxiliary feedwater, one HPI pump can handle the accident provided that realistic decay heat valves are utilized. In all of these evaluations, the PORV was modeled via a leak path representation in the CRAFT2 code. The orifice area of the PORV was modeled as the leak area (1.05 in.²) and a discharge coefficient of 1.0 was utilized.

The method for modeling the PORV described above does result in a predicted steam flowrate, at the valve rated pressure, which is in excess of the design (rated) flowrate. An alternative modeling approach is to use a discharge coefficient (C_D) which, at the valve rated pressure, would yield the valve rated flowrate. For the 177-FA plants, this is a C_D of approximately 0.85. For the first two cases described above, this modeling approach would result in a slower system depressurization and a slower discharge of the RCS inventory. Thus, the use of a $C_D = 1.0$ used in previous evaluations results in a conservative assessment of the transient. For the third case, the use of a smaller C_D would result in a larger repressurization following the loss of the SG as a heat sink and the change in the discharge from steam to two-phase flow. However, use of a C_D of 0.85 would result in an inventory loss less than that calculated in reference 4 and no core uncover would occur.

Besides the cases involving actuation of the pressurizer relief valves, analyses were performed for a total loss of SG heat sink and are provided in references 5 and 6. In those evaluations, the pressurizer safety valves were exercised. To model these valves, the leak path representation was used with the leak path opening and closing at the opening setpoint of the valve. The valve area and C_D was chosen such that the rated flowrate for the valve would be simulated at the valve rated pressure. Because of the large relief capacity of the valve, the system pressure oscillated within a few psi of the valve setpoint and the valve was exercised intermittently. Thus, any discrepancies between the modeled

and the actual relief capacity of the pressurizer safety valve is not expected to significantly alter the system response.

While there is little information available on the discharge rates through the pressurizer valves, it is also important to note the breaks in the pressurizer are bounded by breaks in the cold leg pump discharge piping. Pump discharge breaks are analyzed to show conformance of the ECCS to meet the criteria of 10 CFR 50.46. The reason that cold leg breaks bound breaks in the pressurizer was discussed in detail in reference 3. Therefore, it is not necessary to simulate the actual relief capacities of the pressurizer valves in order to demonstrate the ability of the ECCS to mitigate the consequences of a loss of RCS inventory through the valves within the criteria of 10 CFR 50.46.

REFERENCES

- ¹ BAW-10092, Rev. 2, "CRAFT2 - FORTRAN Program for Digital Simulation of a Multinode Reactor Plant During LOCA," R.A. Hedrick, J.J. Cudlin, and R.C. Foltz, April 1975.
- ² BAW-10104, Rev. 3, "B&W's ECCS Evaluation Model," B.M. Dun, et al., August 1977.
- ³ Letter J.H. Taylor (B&W) to R.J. Mattson, May 7, 1979, "Evaluation of Transient Behavior and Small Reactor Coolant System Breaks in the 177-FA Plant."
- ⁴ Letter J.H. Taylor (B&W) to R.J. Mattson, May 12, 1979.
- ⁵ Letter from R.B. Davis to 177 Owner's Group, Subject: "Complete Loss of Feedwater Transient," September 11, 1979.
- ⁶ Letter from R.B. Davis to Mr. C.R. Domeck, Subject: "Complete Loss of Feedwater Transient on Davis-Besse," September 11, 1979.

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Question

- 2A. Provide justification that the 3 node steam generator model used in the CRAFT2 analysis of small breaks is adequate for the prediction of steam generator heat transfer.

RESPONSE

The B&W ECCS Evaluation Model¹ for small breaks utilizes a three-node representation, in the CRAFT2 simulation, for the prediction of steam generator heat transfer following a small break. Two of the nodes, stacked vertically, are used to model the primary side of the once through steam generator (OTSG). The upper node includes the hot leg piping, from the center on the 180° U-bend at the top of the vertical section of the hot leg to the SG upper head, the upper head of the SG, and the upper one-half of the tube region. The lower node simulates the lower one-half of the tube region. The third node is used to model the secondary side of the OTSG. To evaluate the suitability of this modeling technique, the unique characteristics of the OTSG and its effects on the small break transient must be examined. As is shown later, for small breaks evaluated with the auxiliary feedwater system operable, heat removal via the SG is not necessary for the worst case breaks, i.e., those that result in core uncover, in order to successfully mitigate the transient. For the smaller breaks, heat removal via the SG is necessary. The three-node representation utilized appropriately models the heat transfer characteristics of the OTSG. For the smaller breaks, heat removal via the steam generator is necessary and the heat transfer characteristics of the OTSG must be appropriately considered. Although the 3-node SG model does not rigorously account for the heat transfer process that will occur, it does provide a reasonable representation of the effects of these heat transfer processes in the OTSG. Since these smaller breaks exhibit large margin to core uncover, the CRAFT2 SG model is adequate for demonstrating compliance to 10 CFR 50.46.

In performing small break evaluations, the CRAFT2² code is used to predict the hydrodynamic response of the primary system including the effect

of SG heat transfer during the transient. The option 2 SC model, which is explained in detail in Section 2.6 of topical report B/W-10092, Rev. 2, is utilized to predict heat flow in the SG. The calculation progresses basically as follows:

1. Based upon the initial steady-state heat transfer characteristics of the OTSG and the initial primary and secondary fluid temperatures, an overall UA for each region of the SG is calculated.
2. The calculated steady-state UA can be modified by user-specified input options. These include an input multiplier table versus time, multiplied based on the primary side control volume mixture height during the transient, and a multiplier for reverse heat transfer, i.e., heat flow from the secondary to the primary side of the SG.
3. Using the modified UA and the calculated primary and secondary side control volume temperatures, the amount of heat transferred is calculated.

In performing the small leak calculations for demonstrating compliance to 10 CFR 50.46 for the operating B&W plants, no input multiplier versus time is utilized, nor is the modification based on primary side mixture level used. However, a multiplier for reverse heat transfer of 0.1 is utilized. This multiplier and its basis is explained in the ECCS evaluation model topical report¹ and is utilized to reflect the change in heat transfer regime on the secondary side of the SG for reverse heat flow.

The OTSG design of the B&W designed operating NSSs allows use of a simplistic model for calculation of SG performance during a small LOCA transient. With the loss-of-offsite power, assumed in design calculations for small breaks, and the subsequent loss of main feedwater, the auxiliary feedwater system is actuated and will become operable in approximately 40 seconds and control the secondary side level. The auxiliary feedwater enters the SG very high, approximately 2 feet below the upper SG tube sheet, and is sprayed onto the tube bundles. Thus, heat transfer will occur in the upper portion of the SG independent of the actual level in the SG. The introduction of auxiliary feedwater to the SG has two effects on the small LOCA transient. First, it raises the thermal center in the SG during the natural circulation phase of the accident which

results in a continuation of circulation through the RCS, for some period of time, even while inventory is lost from the primary system. Later in the transient, after sufficient inventory has been lost from the system, circulation will be interrupted and the auxiliary feedwater, for a certain range of small breaks, will condense steam on the primary side of the SG; thereby maintaining the primary system pressure near the secondary side pressure. The analytical approach utilized for the small break evaluation is consistent with this performance of the auxiliary feedwater system.

It should be noted that between the time that circulation through the loops is lost and the time that the primary side SG level has dropped to the point where condensation heat transfer will occur, system repressurization can occur as heat removal via the SG will be lost. This phenomena occurs only for the very small sized small breaks in which the SG heat removal is necessary. If simulation of this repressurization phenomena of the very small breaks is desired, an additional node would be needed in the small break model in order to separate the hot leg and SG upper plenum volumes from the tube region. This will allow steam to accumulate in the upper regions of the RCS without being affected by heat removal that occurs in the steam generator. In the analyses presented in reference 5 for these smaller sized breaks, a model which included the additional node was utilized and showed that the repressurization phenomena does not result in core uncovering.

It is also important to note the role of the SG on the small break transient in order to evaluate the appropriateness of the SG model utilized in small break evaluations. Licensing calculations for the operating B&W units have previously been submitted to the Staff in references 3 and 4. These evaluations have shown that the worst case small breaks, i.e., breaks which result in core uncovering, occur for breaks in excess of 0.05 ft². As demonstrated in the May 7, 1979 report⁵, SG heat removal is not necessary for breaks of this size. For smaller breaks, SG heat removal is necessary as the break alone is not sufficient to remove enough fluid volume and energy to depressurize the RCS. However, as demonstrated in reference 5, these breaks are of no consequence as the SG heat removal and the slower discharge rate for these breaks easily prevents core uncovering.

As demonstrated, the SG model utilized in the small break evaluations for the operating plants appropriately accounts for the effect of the spatial heat removal processes that will occur in the OTSG during a small break. It was also shown that the SG performance is not important for the worse case small breaks. Thus, the CRAFT2 SG model is adequate for demonstrating compliance of the ECCS to 10 CFR 50.46.

REFERENCES

- 1 "B&W's ECCS Evaluation Model," BAW-10104, Rev. 3, Babcock & Wilcox, August 1977.
- 2 R.A. Hedrick, J.J. Cudlin, and R.C. Foltz, "CRAFT2 - Fortran Program for Digital Simulation of a Multinode Reactor Plant During Loss-of-Coolant," BAW-10092, Rev. 2, Babcock & Wilcox, April 1975.
- 3 Letter, J.H. Taylor (B&W) to S.A. Varga (NRC), July 18, 1979.
- 4 "Multinode Analysis of Small Breaks for B&W's 177-Fuel Assembly Nuclear Plants With Raised Loop Arrangement and Internals Vent Valves," BAW-10075A, Rev. 1, Babcock & Wilcox, March 1976.
- 5 "Evaluation of Transient Behavior and Small Reactor Coolant System Breaks in the 177-Fuel Assembly Plant," Babcock & Wilcox, transmitted via letter from J.H. Taylor to R.J. Mattson, dated May 7, 1979.

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Questions

- 3A. Provide the sources of noncondensable gases in the primary system.
- 3B. Discuss the effect of noncondensable gases on: (1) condensation heat transfer, (2) system pressure calculations, and (3) natural circulation flow.

Response

There are two possible ways in which the release of noncondensable gases in the primary system could interfere with the condensation heat transfer processes which occur in the steam generator during small loss of coolant accidents. If noncondensable gases filled the U bend at the top of the hot leg, then water vapor would have to diffuse through the noncondensable gases before they could be condensed in the steam generator. This would be a very slow process and would effectively inhibit natural circulation. Lesser amounts of noncondensibles would reduce the heat transfer by condensation because the vapor would have to diffuse through the noncondensibles to get to the condensate on the tubes.

Table 1 lists the potential sources and amounts of noncondensable gases for a 177 fuel assembly plant. However, most of these gases would not be released for small break transients. Appendix K evaluations performed for the 177FA plants demonstrate that cladding temperatures remain low and no cladding rupture nor metal water reaction occur. Thus, these sources can be neglected. Also, the steam generator is a heat sink only if primary system pressure is above ≈ 1050 psia. Therefore, gases present in the core flooding tank can be neglected in addressing the effect of noncondensable on SG condensation. The only sources of noncondensibles which might separate in the RCS are the gases dissolved in the coolant, the gases in the pressurizer, gases in the makeup and borated water storage tank and gases released from an allowed 1% failed fuel in the core. Thus, the maximum amount of noncondensable gases in the system, assuming all gas comes out of solution, no noncondensibles are lost through the break flow, that there was one percent failed fuel, and the injection of 6.4×10^4 lbm from the makeup tank and BWST (typical of ≈ 1500 sec of HPI), would be:

Dissolved in coolant	563 scf
In pressurizer	166
Fission gas	2
Fuel rod fill gas	11
MU tank	24
BWST	14
Total	780 scf

This gas would occupy a volume of 22.4 ft^3 at a pressure of 1050 psia, the lowest pressure condition in the primary system for which condensation heat removal will occur. It should be noted that the assumed integrated injection flow does not have a significant effect on the total volume of noncondensibles which might be present in the primary system. Since the volume required to completely fill the U-bend in the hot leg is 125 ft^3 , the noncondensable gases will not impede the flow of vapor to the steam generator.

The heat transfer during condensation is made up of the sensible heat transferred through the diffusion layer and the latent heat released due to condensation of the vapor reaching the interface (see Figure 1). The model of Colburn and Hougen(1) gives the following equation for the heat transfer in the vapor phase:

$$\phi = hg(Tg_o - Tg_i) + Kg Mg h_{fg}(Pg_o - Pg_i) \quad (1)$$

where

ϕ = condensation heat flux, btu/hr ft²

hg = heat transfer coefficient for vapor layer, Btu/hr ft² °F

Tg_o = bulk temperature, °F

Tg_i = temperature of interface, °F

Kg = mass transfer coefficient, $\frac{\text{lb mole}}{\text{lb}_f \text{ hr}}$

Mg = molecular weight, lbm/lb mole

h_{fg} = latent heat of vaporization, Btu/lbm

Pg_o = partial pressure of vapor at bulk conditions, $\frac{\text{lb}_f}{\text{ft}^2}$

Pg_i = partial pressure of vapor at the interface,

$$\frac{\text{lb}_f}{\text{ft}^2}$$

$$Kg = \frac{1.02 D}{z R T} \left(\frac{g z^3 \rho \left(\frac{\rho_o}{\rho_i} - 1 \right)}{\mu D} \right)^{.373} \quad p/\text{pam}$$

D = diffusion coefficient, ft²/hr

z = height

R = gas constant, $1545 \frac{\text{lb}_f \text{ ft}}{\text{lb mole } ^\circ\text{R}}$

T = absolute temperature at bulk conditions, °R

g = acceleration of gravity, ft/hr²

ρ = density, lbm/ft³

ρ_o = density at bulk conditions, lbm/ft³

ρ_i = density at interface conditions, lbm/ft³

μ = viscosity, lbm/hr ft

$$\text{pam} = \frac{p_{ai} - p_{ao}}{\ln \frac{p_{ai}}{p_{ao}}}$$

p_{ai} = partial pressure of gas at interface, $\frac{\text{lb}}{\text{ft}^2}$

p_{ao} = partial pressure of gas at bulk conditions, $\frac{\text{lb}}{\text{ft}^2}$

For the application to OTSG condensing heat transfer during small break transients, the term $hg(T_{g0} - T_{gi})$ can conservatively be neglected since the vapor velocities would be very low. Thus,

$$\phi = K_g M_g h_{fg}(P_{g0} - P_{gi}). \quad (2)$$

The heat transfer with noncondensable gases present is obtained by iteration. An interface temperature T_{gi} is assumed, which fixes P_{gi} , and the heat transfer across the liquid condensate film is computed from

$$\phi = h_f(T_{gi} - T_w) \quad (3)$$

where

$$h_f = .943 \left(\frac{\rho_f(\rho_f - \rho_g)g h_{fg} k_f^3}{\mu_f z(T_{gi} - T_w)} \right)^{1/4}$$

ρ_f = density of fluid, lbm/ft³

ρ_g = density of vapor, lbm/ft³

k_f = thermal conductivity of fluid, Btu/hr ft °F

T_w = wall temperature, °F

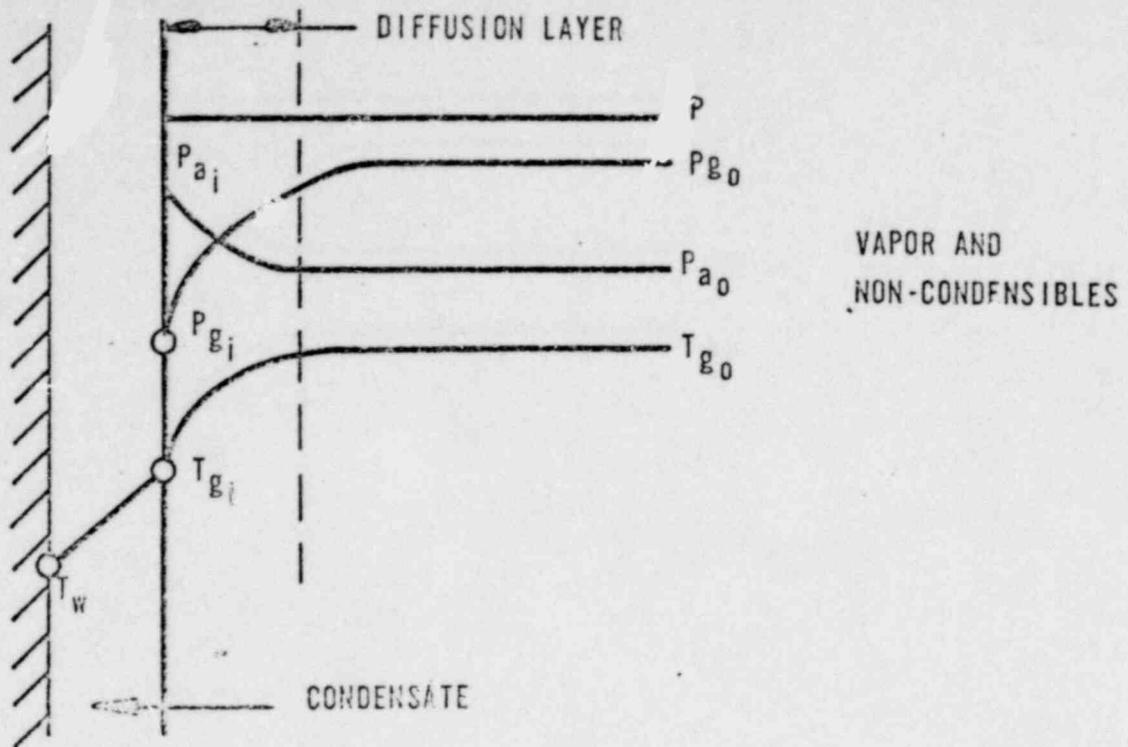
The partial pressure of the gas at the bulk conditions can be calculated from the mole fraction of noncondensable gases. When the heat flux computed from equation 2 matches that computed by equation 3, the proper interface temperature has been found.

The impact of noncondensibles on the condensation heat transfer process during a small break was examined for the 0.04 ft² and 0.01 ft² cold leg breaks analyzed for the 177-FA plants. The breaks utilize the SG for heat removal for a significant portion of the transient. Hand calculations were performed, using the theory presented above, to ascertain the effect of non-condensibles on the transient.

The amount of noncondensable gases, assuming that all gases come out of solution, would be 2.61 moles. The effect of these gases is to raise the pressure and primary temperature to obtain the same heat transfer. Assuming that the noncondensibles accumulated only within the steam generator upper plenums and the steam generator tubes, the system pressure increase, due to noncondensibles, would only be 25 psi, for a 0.04 ft² break, and 40 psi, for a 0.01 ft² break. It should be noted that this effect is predominantly due to the inclusion of the partial pressure of the noncondensibles, which is 24 psi for the 0.04 ft² break and 34 psi for the 0.01 ft² break, in the total system pressure. These calculations represent the maximum impact as they were computed at the time of maximum condensation heat flux for the respective cases.

As shown, the influence of noncondensibles does not significantly effect the condensation heat transfer process. The estimates made are conservative in that they assumed all the gas is located in the steam generators (none is in the top of the reactor vessel or pressurizer) and no gases escape through the break. Thus, the presence of noncondensable gases in the system should not significantly affect the small break transient.

FIGURE 1



- P = TOTAL PRESSURE
 P_{a_i} = PARTIAL PRESSURE OF GAS AT INTERFACE, lb_f/ft^2
 P_{a_0} = PARTIAL PRESSURE OF GAS AT BULK CONDITIONS, lb_f/ft^2
 P_{g_i} = PARTIAL PRESSURE OF VAPOR AT INTERFACE, lb_f/ft^2
 P_{g_0} = PARTIAL PRESSURE OF VAPOR AT BULK CONDITIONS, lb_f/ft^2
 T_w = WALL TEMPERATURE, °F
 T_{g_i} = TEMPERATURE AT INTERFACE, °F
 T_{g_0} = BULK TEMPERATURE, °F

REFERENCE: 1) COLBURN, A.P. AND HOUGEN, D.A., "DESIGN OF COOLER CONDENSERS FOR MIXTURES OF VAPORS WITH NONCONDENSING GASES", IND. ENG. CHEM. 26(11), 1934.

TABLE 1

SOURCES OF NONCONDENSIBLES - 177 FA PLANT

Source	Gas	Total available				1% FAILED FUEL				1% METAL-WATER REACTION			
		Total Volume scf	Individual Gas Volumes scf	Total Mass lb.	Individual Gas Masses lb.	Total Vol. scf	Ind. Gas Vol. scf	Total Mass lb.	Individual Gas Masses lb.	Total Vol. scf	Ind. Gas Vol. scf	Total Mass lb.	Individual Gas Masses lb.
Dissolved in reactor coolant	H ₂ & N ₂	560	H ₂ - 205 N ₂ - 158	14	H ₂ - 1.7 N ₂ - 12.3								
Pressurizer steam space	H ₂ & N ₂	136	H ₂ - 65 N ₂ - 71	5.9	H ₂ - 0.4 N ₂ - 5.5								
Pressurizer water space	H ₂ & N ₂	30	H ₂ - 20 N ₂ - 10	0.91	H ₂ - 0.11 N ₂ - 0.8								
Fission gases in core	Kr & Xe	186	Kr - 20 Xe - 166	65.5	Kr - 4.8 Xe - 60.7	1.9	Kr - 0.2 Xe - 1.7	0.66	Kr - 0.05 Xe - 0.61	1.9			
Fuel rod fill gas	He & some N ₂ & O ₂	1133	He - 1092 N ₂ - 32 O ₂ - 9	14.8	He - 11.5 N ₂ - 2.8 O ₂ - 0.3	11.3	He - 10.9 N ₂ - 0.3 O ₂ - 0.1	0.16	He - 0.12 N ₂ - 0.03 O ₂ - 0.01	11.3			
Metal water reaction (100%)	H ₂	416,500	-	2320	-					4165	-	23.2	-
MU tank gas space	H ₂ & N ₂	726	H ₂ - 421 N ₂ - 305	26.1	H ₂ - 2.3 N ₂ - 23.8								
MU tank water space	H ₂ & N ₂	24	H ₂ - 16 N ₂ - 8	0.71	H ₂ - 0.09 N ₂ - 0.62								
BWST	Air (N ₂ & O ₂)	1383	N ₂ - 902 O ₂ - 481	121.2	N ₂ - 70.3 O ₂ - 50.9								
CF tank gas space (two tanks)	N ₂	26,248	-	2047	-								
CF tank water space (two tanks)	N ₂	964	-	75	-								

Assumptions

- RCS contains 40 std. cc H₂/Kg water & 20 std. cc N₂/Kg water, with water volume = 10,690 ft³ at 583F and 2200 psia.
- Pressurizer water contains 40 std. cc H₂/Kg water & 20 std. cc N₂/Kg water with Henry's Law relation between water space and steam space at 650F. Water volume = 825 ft³ and steam volume = 716 ft³
- Fission gases based on inventory in core at 292 EFPD.
- Fuel rod gas based on each rod containing 0.0375 gmol He, 0.0011 gmol N₂ and 0.00029 gmol/O₂.
- Metal-water reaction based on 52,000 lb. Zr cladding.
- MU tank values based on tank containing 200 ft³ gas space and 400 ft³ water space at 120F with the water containing 40 std. cc H₂/Kg and 20 std. cc N₂/Kg with Henry's Law relationship between gases in water and in gas space.
- BWST contains 450,000 gallons of water saturated with air, i.e., 15 std. cc N₂/Kg and 8 std. cc O₂/Kg.
- Each CF tank contains 1040 ft³ water and 370 ft³ gas space with 600 psig N₂ at 120F with Henry's Law relation between water and gas.
- Values for 1% failed fuel based on Xe and Kr fission product inventory and fuel rod fill gas (He) in 1% of fuel rods being released to coolant.
- Values for 1% metal-water reaction based on gases in Item 9 above and H₂ released from 1% of Zr cladding (520 lb.) reacting with coolant.

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QUESTION 3C: Describe any operator actions and/or emergency procedures necessary to preclude introduction of significant quantities of non-condensable gases into the primary system.

RESPONSE: Present provisions of the small break emergency procedures preclude the generation of significant quantities of non-condensable gases by preventing core uncovering. Therefore, no revision to these actions is necessary.

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Question 3D: Describe operator actions to be taken in the event of a significant accumulation of non-condensable gases in the primary system.

Response:

The review and development of operator actions to be taken in the event of a significant accumulation of non-condensable gases in the primary system is incomplete pending finalization of small break impact studies. Preliminary investigations are centered upon (1) review of present provisions within the small break emergency procedures (e.g., restart of one or more RC pumps and/or remote manual operation of the PORV) which may be effective in minimizing the impact on system response, and (2) use of present plant features (e.g., pressurizer spray and venting and/or letdown to the makeup tank) to degass the reactor coolant in the long term following small breaks where a return to natural circulation is possible.

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Attachment 4
CRAFT2 SIMULATION OF THE MARCH 28, 1978
TMI-2 TRANSIENT

I. INTRODUCTION

In the May 7, 1979 "Blue Book" reports¹, a CRAFT2 simulation of the first hour of the TMI-2 transient was presented. That analysis has since been modified and updated to include more recent estimates of the net make-up to the RCS during the event. This report presents the results of the latest B&W CRAFT2 simulation of the TMI-2 event and covers approximately the first 2 hours and 20 minutes of the transient.

The small break ECCS evaluation model, which is described in topical report BAW-10104² and the July 18, 1978 letter report³, was used, with some "best estimate" modifications, for the simulation. Actual TMI-2 data were combined with available information about the operator actions to determine estimates of the HPI and AFW injection times and flow rates.

The simulator results (described in detail in the "Results" section) show all the trends and very good comparisons to the actual plant data of system pressure, temperature and pressurizer level. The analysis also predicts the time for the start of core uncover which is in reasonable agreement with the NSAC-1⁴ report. Thus, the CRAFT2 code is shown to benchmark very well versus the TMI-2 data and is suitable for the performances of small break evaluations.

2. METHOD OF ANALYSIS

The CRAFT2 code which is documented in topical report BAW-10092⁵, was used to simulate the TMI-2 reactor coolant system hydrodynamics. The model uses one node for the reactor building, two nodes for the secondary system, and 23 nodes to simulate the reactor coolant system, including four nodes for the pressurizer. A schematic diagram of the model is shown in Figure 1. The analytical model used for this simulation is basically the same as B&W's ECCS evaluation model. However, certain input assumptions which differ from the evaluation model approach, were made in order to obtain a "best estimate" simulation. These assumptions are described below:

- a. The initial core power level used in the model was 102% of 2772. However, following reactor trip, the fission product decay heat was adjusted to 98% power operation. The decay heat curve utilized

is 100% , instead of 120% required by Appendix K to 10 CFR 50, of the NRS 5.1 decay heat curve.

- b. A loss of the main feedwater pumps, which is the initiating transient, was assumed at time zero. In order to account for potential draining of secondary side fluid from the steam generator downcomer into the tube region, a main feedwater coast-down of 10 seconds was utilized.
- c. A turbine trip coincident with the loss of main feedwater was assumed. This results in the steam generator pressure being controlled by a combination of the turbine bypass valves, the atmospheric dump valves and the main steam safety valves. For the first 90 minutes, the turbine bypass valves control the secondary side pressure. In the simulation, these valves were set at 1025 psig.
- d. The CRAFT2 input was adjusted to open the pilot-operated relief valve (PORV) at 8 seconds. This opening time had to be input, and the open valve simulated, since CRAFT2 code does not have models for the pressure relief systems of the RCS. Preliminary TMI-2 data was used to determine the PORV opening time. Present TMI-2 scenarios⁴ indicate that the PORV actually opened at 3 seconds. As will be shown in the results section, if the CRAFT2 code had an explicit PORV model, it would have predicted the opening at 3 seconds.
- e. The reactor scram was chosen to occur at 10 seconds based on preliminary TMI-2 data. Since the CRAFT2 code does not have provisions for a reactor trip on high pressure, this had to be simulated based on time.
- f. The leak area utilized for the PORV is 1.05 in.^2 and represents the orifice area of the valve. The Moody critical discharge correlation was utilized to predict the fluid lost through the PORV. For the first $4\frac{1}{2}$ minutes of the simulation, a discharge coefficient (C_D) of (.8) was used. For the remainder of the evaluation a C_D of 1.0 was employed.

- g. Actuation of the High Pressure Injection System (HPI) was based on ESFAS signal of 1615 psia. This resulted in the actuation of the 2 HPI systems at 1 minute and 45 seconds into the transient, as opposed to 2 minutes and 2 seconds which was the make-up flow initiation time at TMI-2. Between 275 and 6100 seconds, the HPI flow was assumed to be throttled by the operator to an average flow of ≈ 34 gpm. This value is based on preliminary assessment on the net makeup flow to the R/S. No explicit modeling of letdown was used, only net flow was simulated. After 6100 seconds, an average net makeup (HPI) of 42 gpm was utilized.
- h. A four-node pressurizer model was used in the evaluation in order to reduce instantaneous artificial condensation in the pressurizer. This phenomenon, which occurs when the subcooled reactor coolant fluid mixes with two-phase pressurizer fluid, results from the equilibrium model limitations of the code. This model is necessary only to predict the response of the RCS during the initial phase of the loss of main feedwater event. Also, the pressurizer surge line resistance was updated to reflect more realistically the TMI-2 surge line.
- i. Steam Generator Modeling - The steam generator model was modified to account for the following phenomena:
1. The overall heat transfer coefficient (between primary and secondary) was assumed to ramp to zero in one minute to account for the delayed auxiliary feedwater injection.
 2. Full heat transfer coefficient was reinstated at 500 seconds to account for the auxiliary feedwater injection after 8 minutes.
 3. Auxiliary feedwater was initiated at 500 seconds with half of the design AFW flow capacity and with the SG level controlled to 3 feet. With the reactor coolant pumps on, AFW is controlled by the ICS to 3 feet.
 4. Steam Generator B was assumed to be isolated at 1 hour and 41 minutes based upon preliminary TMI-2 data. This was simulated by setting the heat transfer coefficient across the B steam generator to zero.

5. The auxiliary feedwater control level was manually raised to 50% on the operating range at 1 hour and 45 minutes into the transient due to the loss of the RC pumps.
6. The main steam safety valves were modeled to open at 5400 seconds, and the feedwater flow was increased at 6100 seconds. This was done to simulate the steam generator A depressurization following the operator's attempt to increase feedwater flow to steam generator A at about 1 hour and 34 minutes.
- j. The RC pumps in the B loop were tripped at 4400 seconds; the A loop RC pumps were tripped at 6060 seconds. These values are consistent with the TMI-2 data.

Table 1 provides a comparison of the assumed times for various system actuations and operator actions to the NSAC scenario. As shown, the values utilized are reasonable compared to the actual performance during the TMI-2 transient.

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3. RESULTS

3.1 System Pressure

Figures 2 and 3 compare the reactor coolant system pressure calculated by CRAFT2 to the TMI-2 data. Following the loss of main feedwater, the pressure in the RCS rose sharply due to the decreased heat removal across the SG. As shown by Figure 2, the CRAFT2 prediction overpredicts the pressure during this phase of the accident due to the delayed opening of the PORV 3 seconds in the transient versus 8 seconds for the CRAFT2 simulation, and the delayed reactor trip, 8 seconds for the transient versus 10 seconds for the simulation. If the CRAFT code had an explicit model for the PORV, opening of the valve would have been consistent with the data and a better comparison would have been obtained. After the reactor tripped, the RC pressure decreased. The calculated pressure drops below the actual data after 20 seconds. This is apparently caused by the 10 second main feedwater coastdown employed in the simulation overpredicting the drainage of secondary downcomer fluid to the SG. After the SG dries out, approximately one minute, the difference between the prediction and the data decreases.

Approximately 5 minutes into the transient, the fluid in the hot leg flashed due to the depressurization of the RCS and the system pressure increased. As indicated on Figure 3, the CRAFT2 code properly predicts the system repressurization time, but overpredicts the actual pressure. The overprediction of system pressure is probably caused by the assumed net makeup to the RCS during this time period. Although the HPI was throttled to a net makeup of 34 gpm during this time interval in the simulation, between 4:58 and 6:58, the NSAC scenario of events indicate that the letdown flow was in excess of 160 gpm. Thus, it is quite probable that there was a decrease in inventory in the RCS due to the high letdown over this time period.

At 8 minutes and 18 seconds, auxiliary feedwater flow was readmitted to the SG and primary system pressure decreased (Figure 3) to approximately 1100 psig and was maintained at that value up to approximately one hour and 20 minutes.

As shown by Figure 3, the CRAFT2 prediction is greater over this period by about 100 psi. The coolant pressure was measured in the hot leg during the accident; the predicted system pressure shown is the core pressure. The actual predicted hot leg pressure is about 60 psi lower than the predicted core pressure. Also, the pressure in the secondary side was held in the CRAFT2

simulation at 1025 psig, while the measured value was 1000 psig, resulting in an additional deviation. Thus, the CRAFT2 prediction reasonably follows the transient behavior over this period when the deviations are considered. It should also be noted that the primary system pressure during this phase of the transient is basically controlled by the SG. The CRAFT2 prediction did not demonstrate fluctuations in system pressure during this period as the secondary pressure of the SG is assumed to be regulated at 1025 psig. The plant data shows that the secondary side SG pressure was not held constant over this period, but fluctuated.

At one hour and 34 minutes, the RCS pressure dropped due to an apparent attempt by the operator to increase feedwater to the A SG. The analysis attempted to simulate the depressurization effect of the increased auxiliary feedwater flow by opening the relief valves at 5400 seconds and increasing the auxiliary feedwater flow at 6100 seconds. This modeling technique was utilized as little information is available on the actual auxiliary feedwater flow delivered to the SG during this period. As shown by Figure 3, this resulted in an underprediction of the primary system pressure until 7500 seconds and an overprediction for the remainder of the transient analyzed.

3.2 Pressurizer Level

A comparison of the CRAFT2 predicted pressurizer level to the TMI-2 data is provided in Figure 4. As shown, there are two pressurizer level predictions given in the figure. The first, entitled mixture level - CRAFT, is the calculated mixture level within the pressurizer. The second, entitled instrumentation reading - CRAFT, is the calculated liquid level that would be "seen" within the pressurizer level taps and is directly comparable to the TMI-2 data.

The initial pressurizer response and comparison to the loss of main feedwater event (first 4 minutes of the transient) is not easily discernable in Figure 4. It was, however, discussed in the May 7, 1979, report. During this phase of the accident, the pressurizer level responded in a similar manner as the system pressure. Also, the comparison of the predicted to the actual response of the pressurizer level is similar. That is, the rise in pressurizer level during the first 10 seconds is overpredicted and the pressurizer level after reactor trip is underpredicted. The reasons for this are the same as those discussed previously in section 3.1.

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The significant aspect of this comparison is the predicted mixture level response to the predicted instrument reading response during the transient. As shown by Figure 4, the predicted instrument response and the measured response are in good agreement throughout the simulation. However, as shown by the figure, although the instrument reading is on scale for portions of the first 101 minutes of the transient, the actual predicted mixture level after 6 minutes is at the top of the pressurizer. Thus, a two-phase mixture exited through the valve during this entire period. After 101 minutes, only steam was entering the pressurizer through the surge line (note that the RC pumps have been tripped), and the pressurizer mixture had reached a sufficient void fraction to allow for phase separation at the top of the mixture and only steam started to flow out.

3.3 System Flow

Figure 5 shows a comparison of the predicted and transient loop flows. As shown, the predicted flow rates do not match well with the actual data. This disagreement is caused by two factors. First, loop flow was measured by Gentillis tubes, which are calibrated based on single phase flow. Their actual performance during two-phase flow is unknown. Secondly, performance of the RC pumps with two-phase flow is not well understood. In performing the evaluation, a two-phase pump degradation multiplier based on the semiscale pump tests was utilized. This multiplier results in a sharp decrease in pump head once any significant voiding is calculated at the pump inlet. As shown, at 55 minutes, the loop flow sharply decreased due to this effect. Although the agreement is not excellent, the pump flow calculation does not appear to have significantly affected the simulation.

3.4 Hot and Cold Leg Temperatures

Figures 6 and 7 show a comparison of the predicted versus actual response of the hot and cold leg temperature measurements during the transient. After 5 minutes and up to the time the core started to uncover, the RCS was saturated, and the fluid temperature comparison has the same deviations previously discussed in section 3.1.

After the core starts to uncover, which occurs at approximately 110 minutes, the hot leg temperature measurement indicated superheated steam (Figure 6). However, the CRAFT2 prediction does not exhibit this behavior. This is due to the one-node representation of the core and the equilibrium assumption of the

CRAFT code. As long as fluid is predicted to remain within the core node, regardless of the actual amount of core uncover, the one-node representation calculates the exiting steam temperature to be saturated. However, the actual physical process results in saturated steam at the top of the core mixture level. This steam superheats as it receives energy from the uncovered portion of the fuel pins. A multinode representation of the core would be necessary to predict the hot leg temperature response during this period.

3.5 System Void Fraction

The average system void fraction evolution for the primary system, excluding the pressurizer, is shown on Figure 9. Due to the continued loss of RCS inventory through the PORV and the inadequate net makeup to the RCS, the system void fraction increases almost linearly from 10 until 101 minutes into the transient. At 101 minutes all the RC pumps have been tripped. At this time, the RCS liquid inventory is distributed as follows; the RV is filled to slightly above the top of the core; the loop seal in the B loop is full; the A loop has very little inventory. During the subsequent 30 minutes, the RV inventory is boiled-off and the steam is condensed by the A loop steam generator. Because of the lowered loop design, this inventory remains trapped within the A loop pump suction piping and the steam generator. During this period of time, the core becomes uncovered. Thus, since the process is a redistribution of water within the RCS with the only fluid loss being steam vented through the PORV, the system average void fraction does not change significantly.

3.6 Core Mixture Level

The calculated core mixture level for the transient is given in Figure 8. As shown, no core uncover was calculated while the RC pumps were operating. However, closely following the termination of the RC pump flow, the level in the core decreased. Core uncover was calculated to start occurring at 105 minutes into the transient. This compares reasonably well with the NSAC prediction of approximately 103 minutes. Thus, the calculated loss rate through the PORV and the net makeup to the RCS must be in reasonable agreement with the actual behavior during the TMI-2 incident.

As shown by Figure 8, the core was predicted to totally uncover. However, this result occurs due to the insufficient spatial detail in the core region. The simulation assumes that all core heat is removed and deposited in the fluid. This results in an overprediction of the core boil-off once the core is uncovered. A more detailed core model is necessary in order to predict how much core heat is deposited in the liquid region for subsequent boil-off of the

core liquid and how much energy is used to superheat the steam. However, since the simulation was basically made to determine how the core uncover occurred, the refined core model was deemed unnecessary.

4. CONCLUSIONS

As demonstrated, the CRAFT2 code simulation predicts reasonably well the system behavior during the first 2 hours and 20 minutes of the TMI-2 transient. In fact, the core uncover time is predicted within a few minutes of the inferred core level response given in the NSAC report. Therefore, it is apparent that the net makeup to the RCS was very low (approximately 34 gpm) over this period which resulted in uncover of the core and subsequent core damage. Also, it is shown that the CRAFT2 code is able to predict the system hydrodynamics during a small LOCA and is suitable for licensing calculations.

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- 3 Letter J.H. Taylor (B&W) to S.A. Varga (NRC), July 18, 1978.
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- 5 R.A. Hedrick, J.J. Cudlin, and R.C. Foltz, "CRAFT2 - FORTRAN Program for Digital Simulation of a Multinode Reactor Plant During Loss-of-Coolant," BAW-10092, Rev. 2, Babcock & Wilcox, April 1975.

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Table 1. Comparison of CRAFT2 Assumption
to NSAC Scenario

Event	Time, hrs: min: sec	
	NSAC	CRAFT2
Loss of feedwater flow/turbine trip	0:0	0:0
PORV opens	0:03	0:08
Reactor trip	0:08	0:10
HPIs actuated	2:02	1:45
HPI throttled	4:38	4:35
Auxiliary feedwater block valves opened	8:18	8:20
Reactor coolant pump 2B stopped	1:13:29	1:13:33
Reactor coolant pump 1B stopped	1:13:42	1:13:33
Steam generator B isolated	1:42:00	1:41:40
SG A level raised to 50% on operate range	1:40:00	1:41:40
Reactor coolant pump 2A stopped	1:40:37	1:41:00
Reactor coolant pump 1A stopped	1:40:45	1:41:00

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FIGURE 1

CRAFT Noding Diagram for TMI-2 Transient

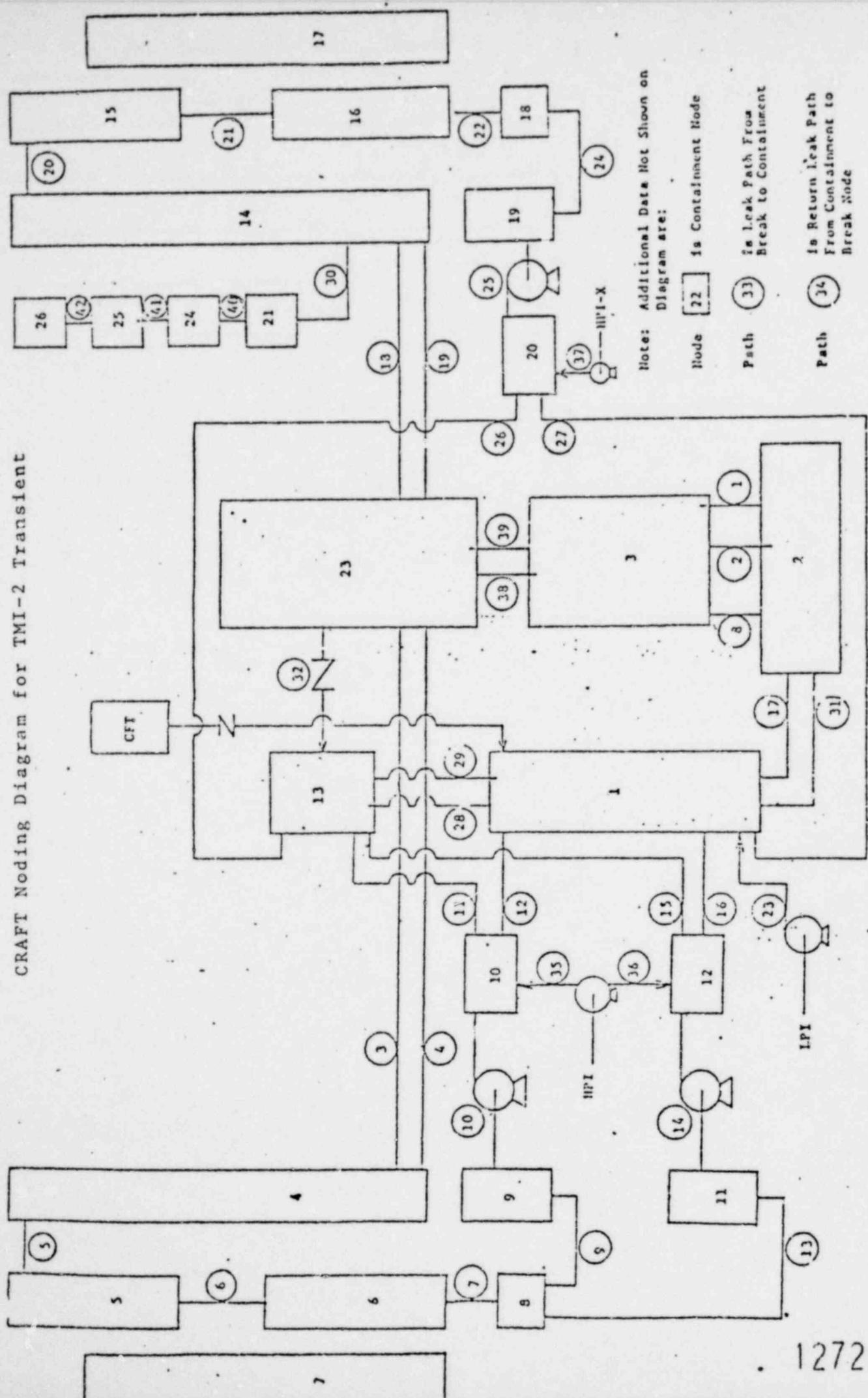


FIGURE 2 SYSTEM PRESSURE VS TIME

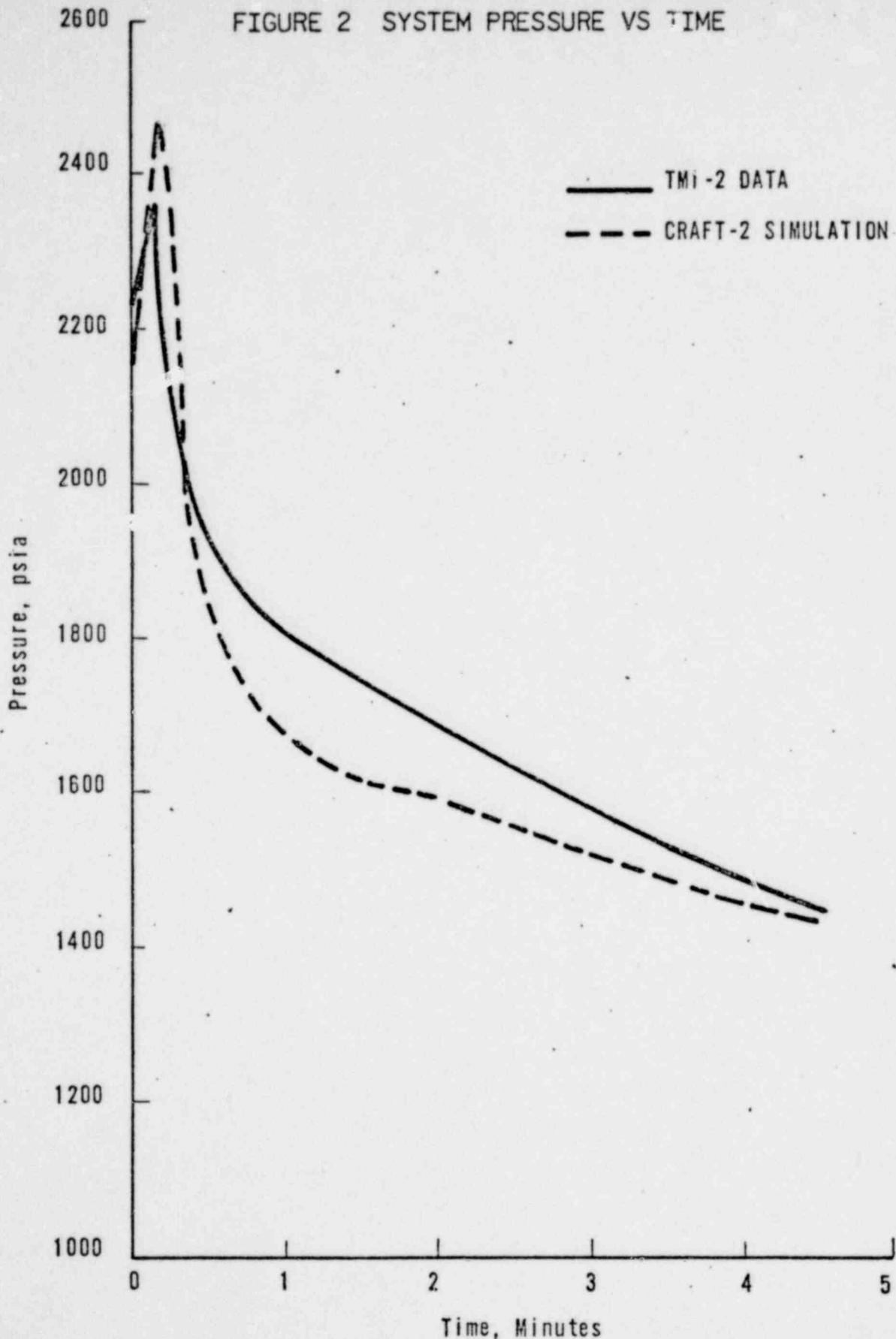
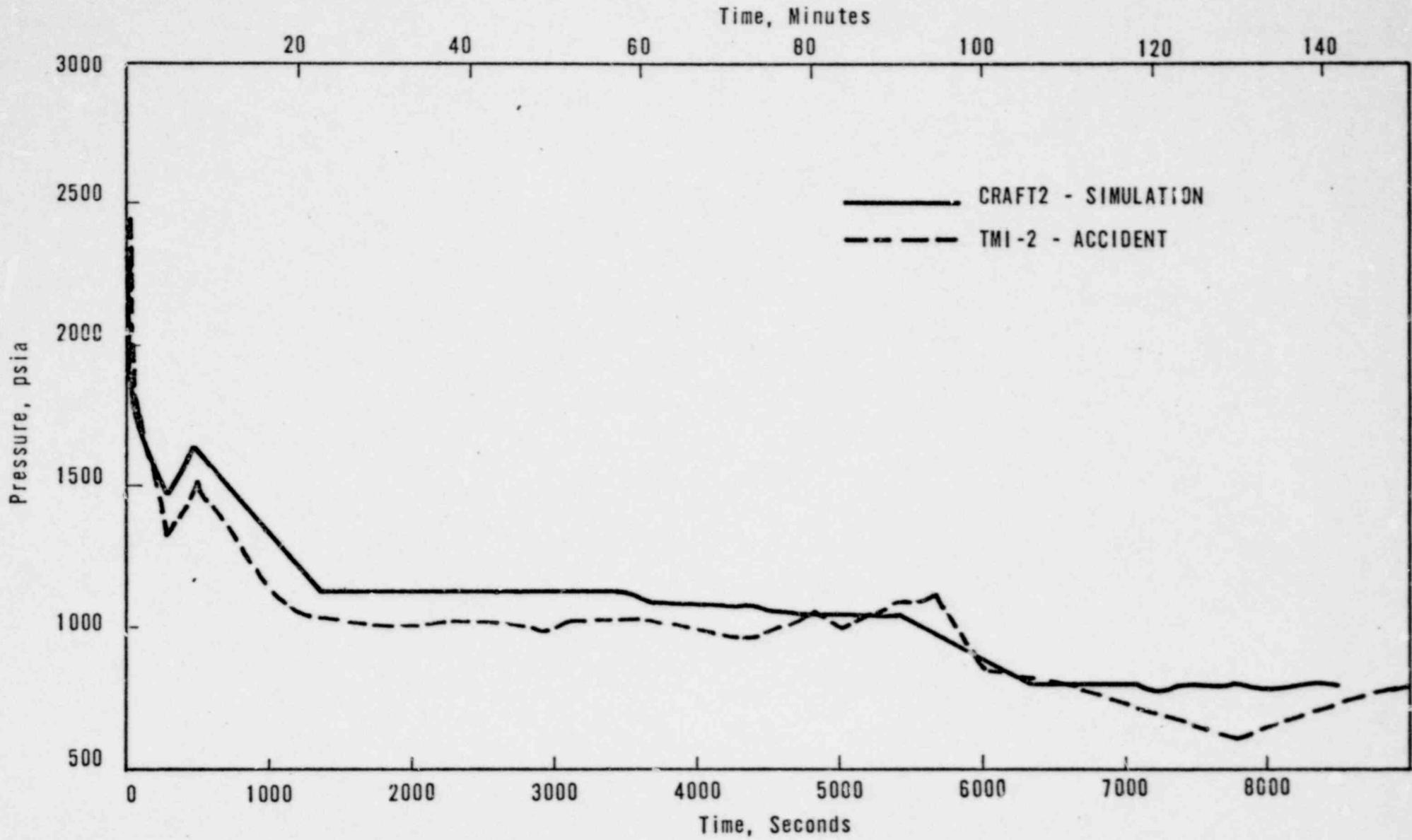


FIGURE 3 REACTOR COOLANT PRESSURE



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FIGURE 4 PRESSURIZER LEVEL

--- MIXTURE LEVEL - CRAFT
— INSTRUMENTATION READING - CRAFT
- · - · - TMI-2 PRESSURIZER LEVEL

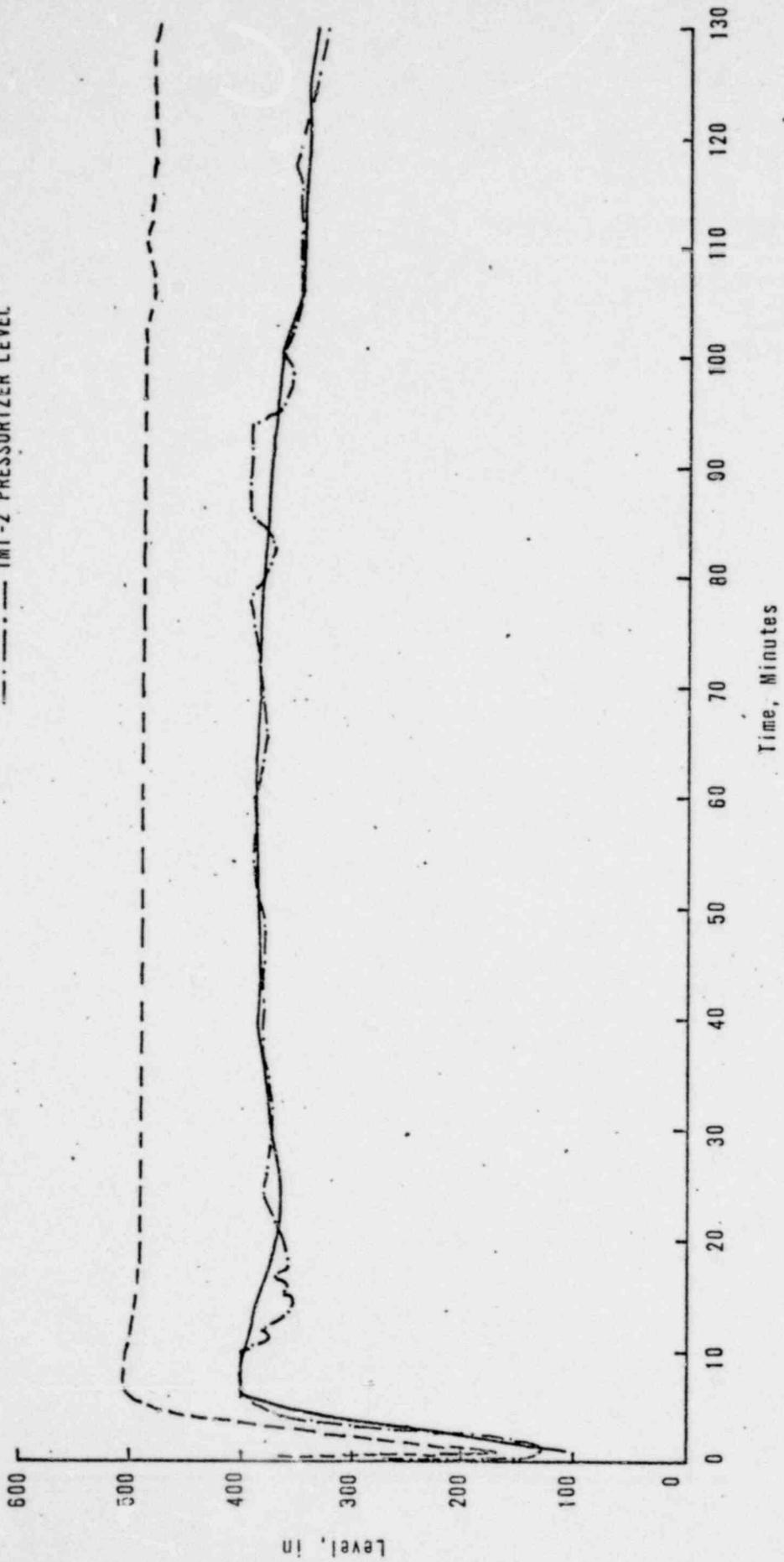


FIGURE 5. HOT LEG FLOW

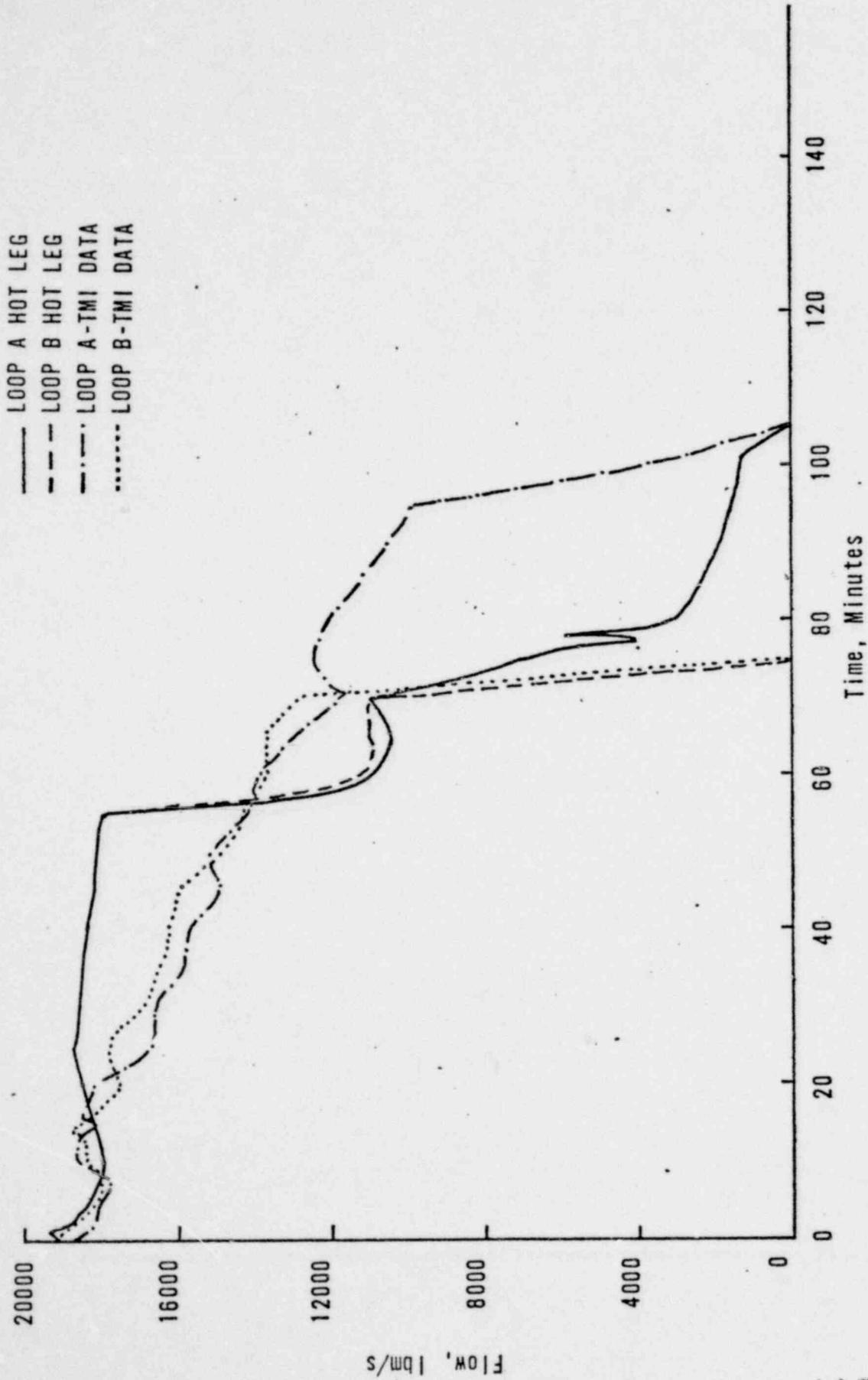
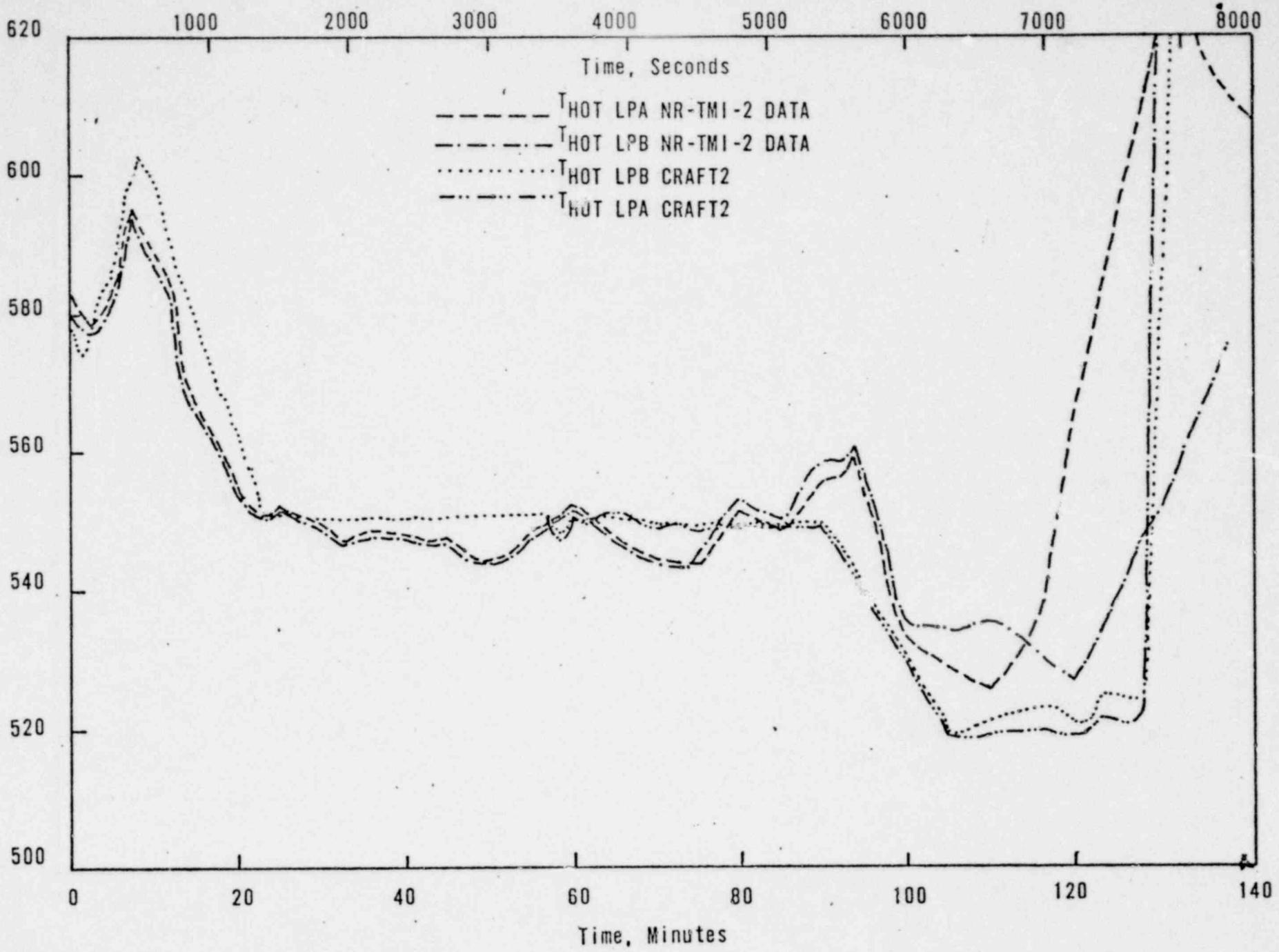
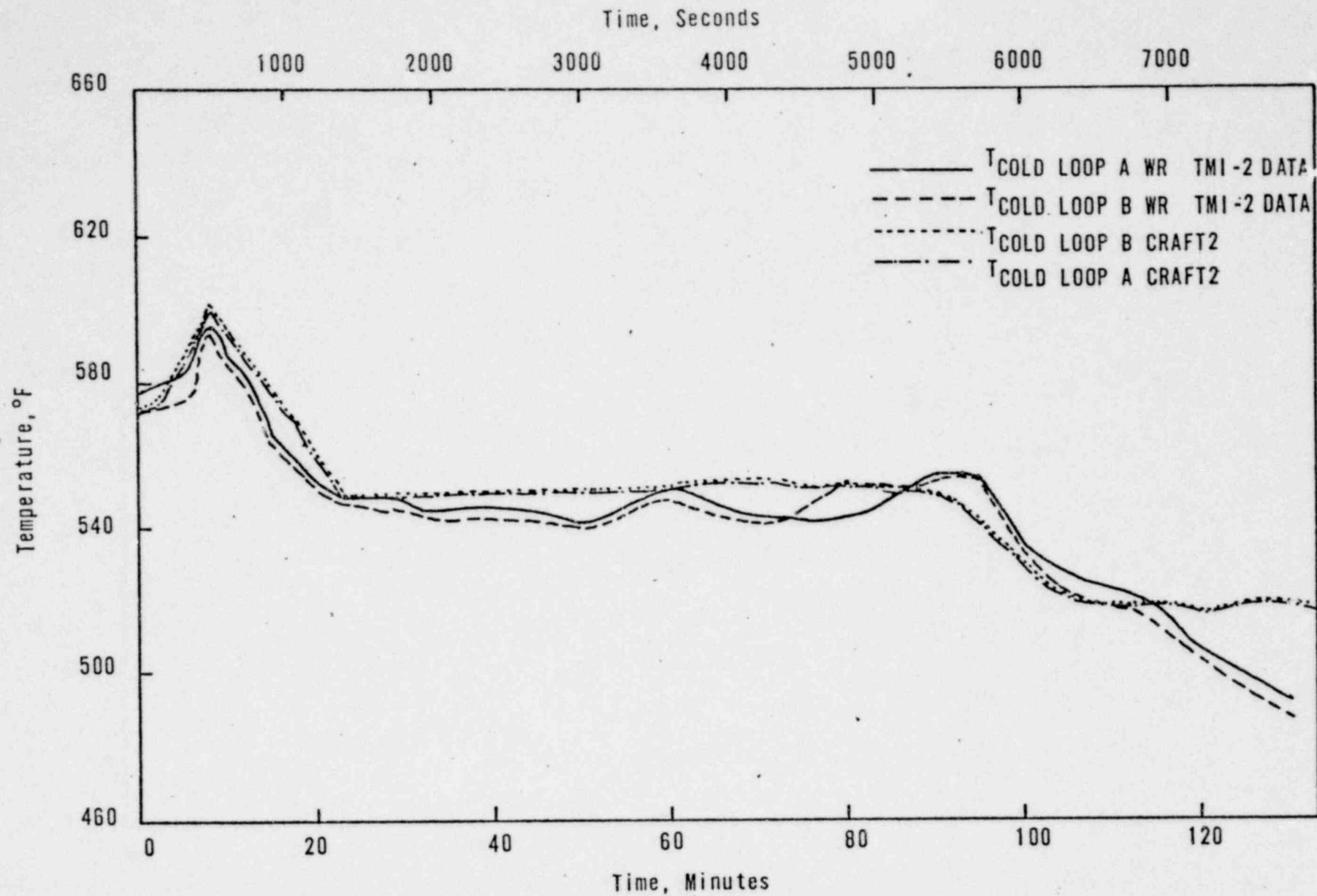


FIGURE 6 HOT LEG TEMPERATURE



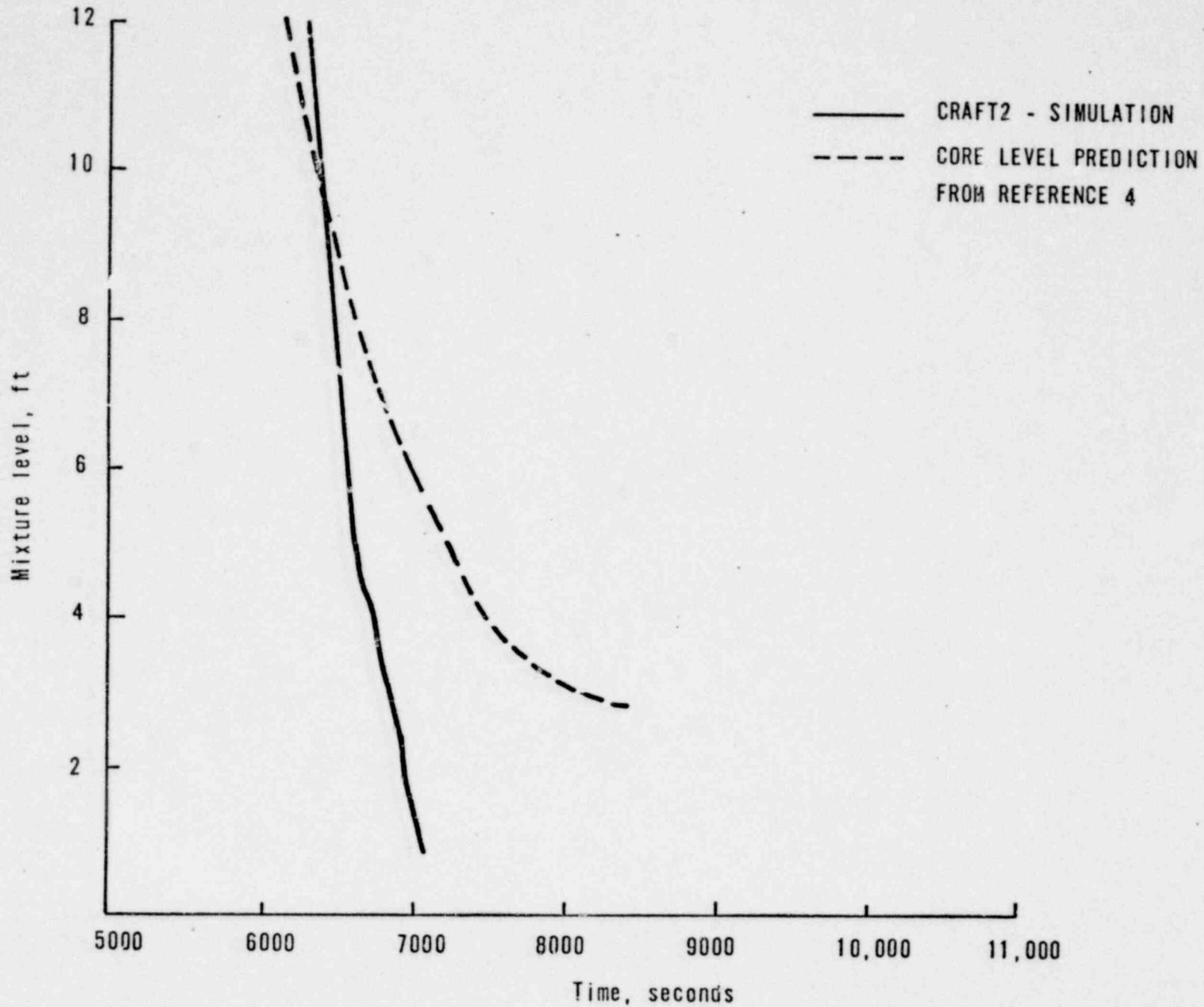
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FIGURE 7 COLD LEG TEMPERATURE



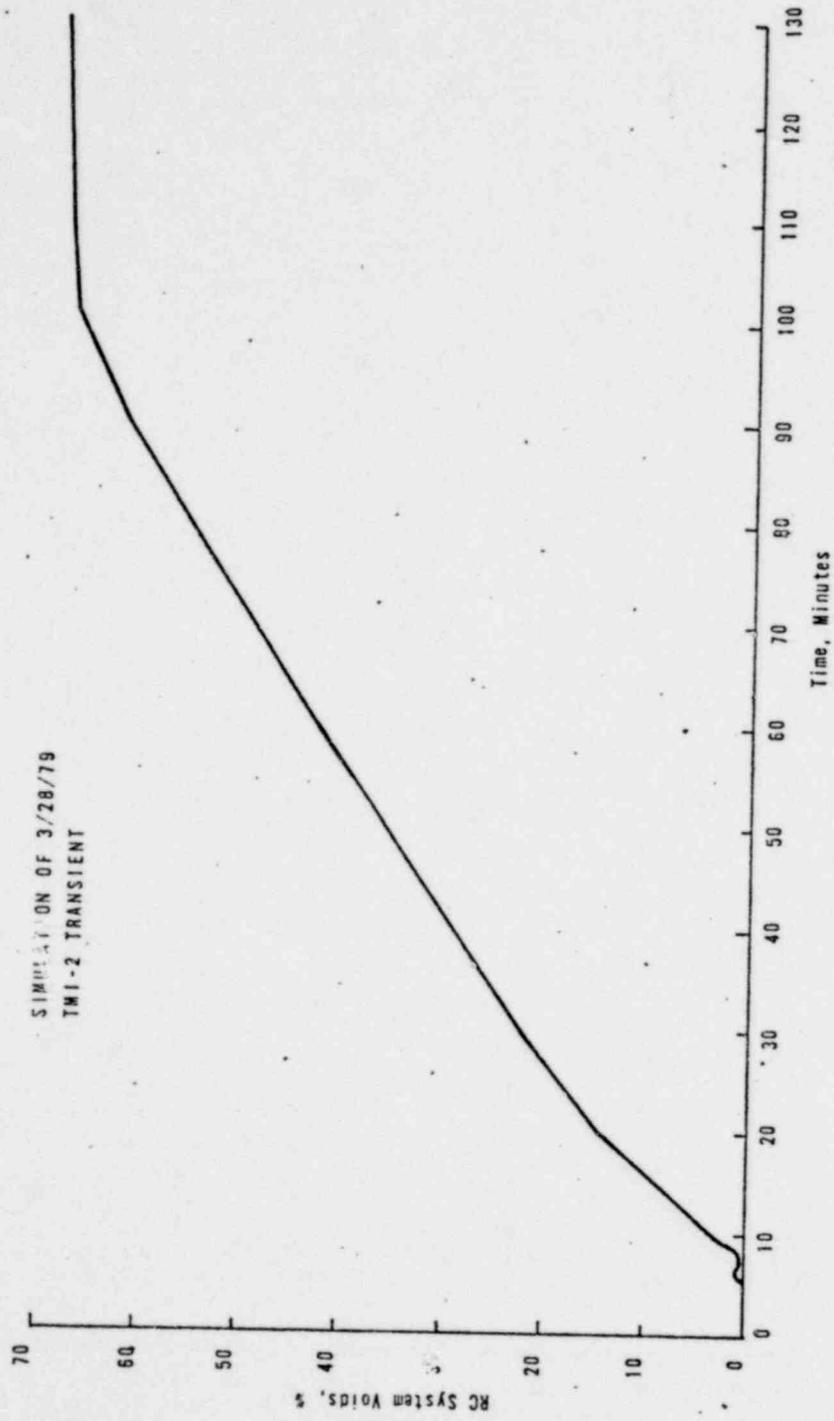
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FIGURE 8 CORE MIXTURE LEVEL



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FIGURE 9 SYSTEM VOIDS VS. TIME USING BEST ESTIMATE HPI FLOWS



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