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COMPARISON OF THERMAL-HYDRAULIC RESPONSE OF
LOFT AND A LARGE PRESSURIZED WATER
REACTOR TO LOCA CONDITIONS

Aerojet Nuclear Company

NATIONAL REACTOR TESTING STATION

Idaho Falls, Idaho — 83401

APRIL - 1972

PREPARED FOR THE

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IDAHO OPERATIONS OFFICE UNDER CONTRACT AT(10-1)-1375

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ANCR-1053

Reactor Technology
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COMPARISON OF THERMAL-HYDRAULIC RESPONSE OF
LOFT AND A LARGE PRESSURIZED WATER
REACTOR TO LOCA CONDITIONS

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AEROJET NUCLEAR COMPANY

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ABSTRACT

An analytical study was performed to determine how closely the thermal-hydraulic response of the Loss-of-Fluid Test (LOFT) pressurized water reactor [55 MW(t)] with a 5.5-foot-long core compares with that of a large, four-loop, pressurized water reactor [3000 MW(t)] with a 12-foot-long core under loss-of-coolant accident conditions. The analysis employed the thermal-hydraulic computer codes RELAP3 and THETA1-B to determine the hydraulic response of the primary systems during blowdown and the thermal responses of the cores during blowdown. An additional analysis was performed to assess the core thermal response during ECC reflooding. On the basis of the results of the analyses, the conclusion was reached that a 5.5-foot core operating in the LOFT system can produce thermal-hydraulic behavior representative of that calculated to occur in a four-loop large PWR with a 12-foot core during a double-ended inlet pipe break. The inlet pipe break analyzed is generally considered to result in the highest core temperatures for large PWR's.

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COMPARISON OF THERMAL-HYDRAULIC RESPONSE OF LOFT AND A LARGE PRESSURIZED WATER REACTOR TO LOCA CONDITIONS

I. INTRODUCTION

This report documents the results of an analytical study in which the thermal-hydraulic response of the Loss-of-Fluid Test (LOFT) system was compared with that of a typical large four-loop pressurized water reactor (PWR) system. The purpose of the study was to determine how closely the calculated thermal-hydraulic response of the LOFT system incorporating the currently planned 5.5-foot core compared with that of a typical large PWR with a 12-foot core. The analysis included the hydraulic response of the primary system fluid and the thermal response of the core cladding during the decompression and emergency core cooling (ECC) phases of a loss-of-coolant accident (LOCA) resulting from an offset shear break of the inlet (cold leg) recirculating pipe to the reactor vessel. The thermal and hydraulic responses of the reactor system are considered to be the most significant phenomena that occur during the LOCA. The 100% inlet piping break is considered by the reactor manufacturers, the Atomic Energy Commission, and Aerojet Nuclear Company to result in the most severe temperature rise in the reactor core. Thus, this condition is considered to produce the maximum credible accident, and is the one which is used to assess the ultimate safety of a PWR.

The LOFT system analyzed is considered the ideal LOFT system in that the core was assumed to operate in a primary system whose fluid volume was proportioned on a region-to-region basis to the thermal power in the same manner as in a large PWR. The assumed proportioning of the fluid volumes to the thermal power is representative of that in the current LOFT reactor design.

The LOFT facility is designed to exhibit all of the phenomena and their interactions presently recognized in the LOCA sequence for a large central station power plant. The events of the accident are to be coupled in the proper sequence and are to occur in the proper period of time. In addition, the core thermal events (as reflected by the fuel cladding temperature history) occurring in the LOFT core during the experiment are expected to approximate the core thermal events that would occur in the core of a large PWR during an LOCA.

In order to represent adequately the thermal behavior of a large PWR with the LOFT 5.5-foot core, the LOFT system was designed to operate with the same coolant enthalpy rise as does the large PWR. This enthalpy rise, however, results in a lower mass flux in the shorter LOFT core (1.15×10^6 lb_m/ft²-hr in LOFT as compared to about 2.5×10^6 lb_m-ft²-hr in a large PWR). This lower mass flux necessitated an investigation of the critical heat flux (CHF) ratio for the LOFT core to determine whether an adequate margin existed for safe operations at steady state conditions. The thermal-hydraulic analysis and the CHF investigation indicated that the response of the LOFT 5.5-foot core adequately represents the response of a large PWR under LOCA conditions, and that the CHF ratio is probably adequate for safe operation.

The results of the comparative thermal-hydraulic analysis are given in the body of the report, and the methods and codes used in the comparative analysis and supporting studies are given in the appendices. The analysis method and the results of the investigation of the CHF ratio are presented in Appendix A. The decompression portion of the thermal-hydraulic analysis was performed by coupling two computer codes; the codes, and the manner in which the two reactor systems were segmented for the calculations, are described in Appendices B and C. The methods used for the core thermal response analysis during the reflooding portion of the LOCA are presented in Appendix D.

II. RESULTS AND DISCUSSIONS

Analyses were performed to determine the comparative thermal-hydraulic responses of the LOFT reactor system and a large PWR system to LOCA conditions. The analyses include the hydraulic responses of the reactor systems during blowdown and the thermal responses of the reactor cores during blowdown and ECC reflooding. Discussion of the results of these analyses is presented in this section.

1. HYDRAULIC RESULTS

The digital computer code RELAP3^[1] was used for calculating the hydraulic responses of the LOFT and large PWR systems to LOCA conditions prior to core reflooding. Descriptions of the models for the LOFT and large PWR systems used in RELAP3 are provided in detail in Appendix B. The particular version of RELAP3 used for the calculations was RELAP3/MOD 036, which includes a calculation of two-phase frictional pressure drops. Input conditions used for the RELAP3 calculations, in addition to those presented in Appendix B, are as follows:

	<u>Large PWR</u>	<u>LOFT</u>
Hot leg pressure (psia)	2250	2250
Hot leg temperature (°F)	610	610
Cold leg temperature (°F)	545	545
Core flow (lb/sec)	35,138	566.6
Bypass flow (lb/sec)	1,665.5	30.6
Break condition	Instantaneous 100% double-ended break of inlet piping	
Discharge coefficient	0.6	0.6
Time delay before initiation of injection systems (sec)	25	25
Bubble rise models	Phase-separation model in upper plenum, accumulators, and containment with a slope parameter, C_o , of 0.8 and bubble escape velocity, v , of 3 ft/sec. Homogeneous models ($C_o = 0$, $v = 0$) used for all other volumes	
Scram curve	0\$ from rupture to 15 sec, linear 0\$ to -30\$ from 15 to 20 sec, -30\$ from 20 sec on	
Pump shutdown	At rupture	At rupture
Pump Coastdown (sec)	1000	1000

	<u>Large PWR</u>	<u>LOFT</u>
Accumulator operation	Initiation at 600 psig	
Secondary system operating temperature (°F)	515	515
Water void coefficient	-0.0556 \$/% void	-0.268 \$/% void

The results selected from the RELAP3 predictions for comparison of the hydraulic response of the two systems are the depressurization histories, the frictional pressure drops across the PWR blowdown-loop pump and the LOFT pump simulator, the mass flux from the breaks, and the mass flux histories of the cores[a]. All results are for the 100% inlet piping break.

Figure 1 depicts the pressure in the lower plenum with respect to time after rupture for both the large PWR and LOFT. Comparison of the data in Figure 1 indicates good depressurization correspondence between the two systems.

Fig. 1 Comparison of pressure in lower plenums of LOFT and large PWR.

The frictional pressure drops developed during blowdown across the LOFT simulated pump junction (orifice) and the PWR pump junction are shown in Figure 2. The simulation of the pump resistance in the LOFT broken loop is to be accomplished with an orifice. Exact simulation of the PWR flow resistance with this orifice is not possible. The resistance of the pump in the PWR broken

[a] All results were obtained from RELAP3 Computer Runs LOFT-3,1,5 and PWR1-1,2,4A.

Fig. 2 Comparison of frictional pressure drop across blowdown pump junction for LOFT and large PWR.

loop is calculated in RELAP3 to be a function of pump coastdown. These pressure drops shown in Figure 2, as expected, are markedly different for the first five seconds during which time coastdown of PWR pump has significant influence, but compare favorably for the remainder of the blowdown.

Figure 3 shows the comparative results of the mass flux from the hot and cold legs of the double-ended inlet pipe rupture. These results indicate that the mass ejection rates for the two systems are similar, and the division of flow between the hot and cold legs predicted for the four-loop, large PWR is provided by LOFT and its blowdown loop. A slightly higher flow for the hot leg of the large PWR during the first five seconds was calculated and is attributed to the lower pump friction pressure drop in the broken loop of the large PWR during this period of time. This lower pressure drop allows more fluid to exit from the upper plenum.

The core mass flux can significantly affect the core thermal response. Comparable behavior of the core mass flux during blowdown for both systems provides increased assurance that the core thermal response for both systems will also be similar. The mass flux to the cores is presented in Figure 4. Both the LOFT and large-PWR mass flux curves show an early momentary flow reversal and then a return to the normal positive flow direction up through the core resulting from continued pump coastdown operation in the unbroken loop. The larger PWR flow for the first five seconds of the transient coincides with the larger mass efflux through the hot leg of the broken pipe. This larger flow is probably a result of the lower pump friction of the operating PWR pump compared with that of the constant resistance of the orifice simulating the pump in the LOFT broken loop. These calculations show that, after cavitation of the pumps in the unbroken loops, the flow is essentially zero at about 8 seconds

Fig. 3 Comparison of mass flux from the blowdown junctions in LOFT and large PWR.

Fig. 4 Comparison of mass flux to core in LOFT and large PWR.

for LOFT and at about 11 seconds for the large PWR. A negative flow then occurs as the system pressure reaches 600 psig. The negative flow is caused by the injection of low temperature accumulator coolant which depresses the pressure in the lower plenum. After 15 seconds, the core mass flux for the two systems is very similar; that is, it is slightly negative and then approaches zero.

2. THERMAL RESULTS

The cladding temperature behavior at the core hotspot is an indicator used to determine the correlation of all phenomena calculated for LOCA conditions for the LOFT and large-PWR systems. The thermal response of the LOFT and PWR cores during blowdown was determined from THETA1-B/MOD 005 calculations employing RELAP3 results as input. A description of the THETA1-B code and the analytical modeling used for the analysis is provided in Appendix C. During core reflooding, the core flooding rates were calculated from analysis of a resistance network for steam flow. Analyses of the fuel rod temperature during reflooding are performed by an analog-hybrid simulation of a fuel rod. The heat transfer coefficient correlation applied to the rod model is based on PWR-FLECHT data. Details on modeling for these analyses are provided in Appendix D.

In addition to providing hydraulic results required as input to THETA1-B, RELAP3 produced the normalized shutdown core power curves for LOFT and the four-loop large PWR, as shown in Figure 5. These curves show that the LOFT core, at beginning of life, shuts down much more rapidly than does the four-loop large PWR. This more rapid shutdown of LOFT is due to the smaller LOFT core and the resultant larger negative void coefficient at the beginning of core life. Integration of the curves results in a difference of approximately 1.2 full-power seconds in the shutdown power. These curves were used in the analysis of the hot-spot cladding temperature behaviors for LOFT and the PWR.

The maximum fuel cladding temperatures during the blowdown for the 100% inlet break are shown in Figure 6. The discontinuities in the cladding temperature curves occurring within the first 30 seconds are coincidental with the changes in the curves for the core mass flux. The initial early core flow reversal causes early departure from nucleate boiling with a rapid increase in cladding temperature. The rate of temperature rise then is decreased as the pumps in the unbroken loop cause the flow to return to the normal direction up through the core. As the flows decrease to zero, the cladding temperatures reach maximums of about 2100°F at 11 seconds for LOFT and at 14 seconds for the PWR. Soon after these temperature maximums are reached the accumulators start to inject coolant into the system, and the mass flux becomes negative as steam condensation occurs in the lower plenum and downcomer as a result of injection of low-temperature ECC. The increase in mass flux reduces the cladding temperature several hundred degrees. The shutdown power curves indicate that the four-loop, large PWR core temperature should be higher than those of LOFT; however, on the average, the calculations show a slightly higher hot-spot cladding temperature for LOFT. This apparent anomaly is the result of a lower mass flux through the LOFT core during the first five seconds of the

Fig. 5 Shutdown power for LOFT and the four-loop, large PWR for beginning-of-life conditions. transient probably resulting from a greater frictional pressure drop in the simulated pump in the LOFT broken loop during this period and a resultant higher hot leg flow rate in the LOFT broken loop.

The maximum fuel cladding temperatures attained during the reflooding of the cores are also shown in Figure 6. Calculations for the reflooding period of the LOCA were started at the time in the transient at which the inventory of water in the downcomer and lower plenum was sufficient to fill the lower plenum and reach the bottom of the core. The reflooding rate within the core of each reactor was calculated by a computer routine that balances the driving head provided by the height of water in the downcomer and the back-pressure of the generated steam as it flows upward through the core, through the blowdown and operating loops, and out the break (Appendix D).

For the LOFT 5.5-foot core, the reflooding interval began at 32.5 seconds in the transient. The hot-spot cladding temperature at this time in the THETA1-B computation is 1750°F. During the reflooding of the core, the cladding temperature attained a maximum of 1900°F at about 16 seconds after reflooding begins, or about 48.5 seconds from the time of the break.

The reflooding of the large PWR core began at 35 seconds in the transient at which time the THETA1-B computation shows the hot-spot cladding temperature is 1670°F. The peak cladding temperature during reflooding is 1780°F and occurs after about 16 seconds of reflooding, or about 51 seconds after initiation of the transient.

Fig. 6 Comparison of maximum cladding temperatures for LOFT and large PWR.

As indicated in Figure 6, a discontinuity in cladding temperature occurs for both cores at the time of reflooding. This discontinuity results from changing from a heat transfer coefficient calculated by THETA1-B to a coefficient calculated by a correlation derived from PWR-FLECHT data. The PWR-FLECHT correlation results in a very low heat transfer coefficient being calculated at the onset of flooding; whereas, just prior to flooding, THETA1-B calculates a greater heat transfer coefficient based on the existing flowing steam. When flooding starts (that is, when water reaches the bottom of the core) existing steam flow is sealed off, and the lower heat transfer calculated by the PWR-FLECHT correlation is considered valid.

A transition in the heat transfer regime and quenching of the fuel pin occurs after the cladding surface temperature decreases to about 1000°F. Rapid quenching at the hotspot is observed for both cores about 95 seconds after reflooding starts.

III. CONCLUSIONS

The conclusion reached on the basis of the results of the analyses described herein is that a 5.5-foot core operating in the LOFT system can produce a thermal-hydraulic response representative of that calculated to occur from a double-ended inlet break in a four-loop PWR with a 12-foot core. Other pipe break conditions to be tested in the LOFT system are expected to be representative of the corresponding conditions in a PWR. The important thermal-hydraulic characteristics for the two plants exhibit similar behavior over the entire accident sequence.

IV. REFERENCE

1. W. H. Rettig et al, RELAP3 -- A Computer Program for Reactor Blowdown Analyses, IN-1321 (June 1970).

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APPENDIX A

STEADY STATE CHF STUDY

An analysis was performed to determine whether the steady state critical heat flux (CHF) ratio for the LOFT 5.5-foot core with the proposed low flow (nominal low flow of 1.15×10^6 lb_m/ft²-hr through the core) would permit safe operation at steady state conditions. The low flow, which results from designing the LOFT core such that the coolant enthalpy rise would be the same as in a large PWR, will exist at the time the loss-of-coolant experiment is initiated. The results of the analysis indicate that a critical heat flux is not likely to occur under steady state conditions prior to initiation of the loss-of-coolant experiment.

Presented in this appendix is the analysis of the CHF ratio conducted using the Westinghouse Electric Corporation (W-3) [A-1, A-2] and the Babcock & Wilcox Company (B&W-2) [A-3] correlations and both the HAMBO [A-4] and COBRA [A-5] codes.

L. S. Tong has stated that a minimum critical heat flux ratio of 1.30 is typical for a reactor core under operational conditions. This CHF ratio, which was obtained using the W-3 correlation and the THINC code, is a generally accepted value and is used to determine the acceptability of the proposed LOFT core coolant flow rate. The 1.30 CHF ratio is discussed further in Section A-4.

1. ENGINEERING AND PHYSICS FACTORS INVOLVED IN A CHF ANALYSIS

An analysis to determine the CHF ratio should account for the following factors:

- (1) Hot Channel Enthalpy Rise Factor. This factor includes the following subfactors: (a) a geometrical subfactor accounting for the variation of pellet diameter, density, and enrichment, fuel rod diameter, rod pitch, and rod bowing; (b) an inlet flow maldistribution subfactor based on the maldistribution of water into the hot channel from the lower plenum; (c) a flow redistribution subfactor accounting for the reduction in flow in the hot channel due to localized or bulk boiling; and (d) a flow mixing subfactor that accounts for mixing between the various subchannels in a fuel assembly and also between adjacent assemblies. This mixing reduces the enthalpy rise in the hot channel resulting from local power peaking or unfavorable mechanical tolerances.

A hot-channel enthalpy rise factor was not used in this analysis because the multichannel HAMBO and COBRA codes which account for flow redistribution and mixing between coolant subchannels were used. However, HAMBO and COBRA have limited subchannel boundaries and, therefore, cannot handle the gross flow redistribution and mixing that occurs between different regions in the core.

Inlet flow maldistribution was not accounted for in HAMBO nor COBRA but is not considered to be a problem because the most critical region for CHF occurs a considerable distance (about three feet) from the core inlet. The geometrical subfactor was not considered because information on mechanical and fuel enrichment tolerances was not available. This subfactor should be included in the analysis at some future date.

- (2) Engineering Hot Channel Local Heat Flux Factor (F_{qE}). This factor is determined by statistically combining the tolerances for the fuel diameter, density, enrichment, and the fuel rod diameter, pitch, and bowing. The tolerances are normally distributed and, therefore, are handled statistically. A value of 1.04 was used for this analysis.
- (3) Maximum Core Overpower Factor. This factor includes the effect of calorimetric, drift, and setpoint errors. A value of 1.07 was used for this analysis.
- (4) Variation of Primary Coolant System Pressure. The maximum pressure instrument error variation is assumed to be ± 45 psi from the nominal design operating pressure. A value of ± 45 psi was used in the analysis.
- (5) The Maximum Variation in the Steady State Temperature from the Reference Temperature. This factor includes temperature deadband and instrument errors and for this analysis was assumed to be 4°F from the nominal temperature of the fluid at the inlet of the lower plenum.
- (6) Variation of Primary Coolant System Flow. The maximum flow variation is assumed to be $\pm 3\%$ of the nominal design flow. A value of 97% of the design flow was used for the analysis.
- (7) Nuclear Power Factor. This factor is the nuclear power peak-to-average value determined from the radial and axial power distributions. A value of 2.61 (1.59 radial, 1.64 axial) was used for this analysis.

The preceding factors (with the exception of hot channel enthalpy rise factor) were used to determine the CHF ratio.

2. PARAMETERS

The minimum CHF ratios were calculated through use of the following nominal design parameters:

- (1) Core Power (100%) -- 55.1 MW(t)
- (2) Core Operating Pressure -- 2273 psia
- (3) Low Core Flow -- $1.15 \times 10^6 \text{ lb}_m/\text{ft}^2\text{-hr}$
- (4) Core Coolant Flow Area -- 1.78 ft^2

- (5) Core Outlet Bulk Temperature -- 613°F
- (6) Core Inlet Bulk Temperature -- 545°F
- (7) Maximum Hot Rod Power at 100% Core Power -- 19.0 kW/ft
- (8) Axial Peak-to-Average Power -- 1.64
- (9) Radial Peak-to-Average Power -- 1.59
- (10) Overall Peak-to-Average Power -- 2.61
- (11) Fuel Rod Pitch -- 0.563 in.
- (12) Fuel Rod OD -- 0.422 in.
- (13) Fuel Pellet OD -- 0.3669 in.
- (14) Diametral Gap -- 0.0065 in.
- (15) Cladding Thickness -- 0.0243 in.

The area enclosed by dotted lines in Figure A-1 is the core region which was used in the analysis. Figure A-2 shows this region in greater detail and denotes the pin powers used. The peak-to-average radial power factor was 1.59 and the peak-to-average axial power factor was 1.64. The axial power profile is shown in Figure A-3.

Fig. A-1 LOFT central fuel assembly.

Pin Powers Normalized to Pin Peak Power

Fig. A-2 Thermal-hydraulic model used for HAMBO and COBRA.

Fig. A-3 LOFT axial power profile used in CHF ratio study.

3. RESULTS OF CHF RATIO ANALYSIS

Results of the analysis to determine the CHF ratio for the LOFT 5.5-foot core for a low flow of 1.15×10^6 lb_m/ft²-hr are presented in Table A-I. The W-3 and B&W-2 correlations and the HAMBO and COBRA codes were used. The design parameters and assumed input parameters and conditions were essentially the same for both codes and were presented in the previous sections.

The spacer effect factor used in the W-3 correlation is

$$SF = 1.0 + 0.03 (G/10^6) \left(\frac{TDC}{0.019} \right)^{0.35}$$

where

SF = spacer factor

G = mass flux (lb_m/ft²-hr)

TDC = mixing coefficient (0.060 in this analysis).

The use of this spacer factor increased the CHF ratio by approximately 5%.

TABLE A-I

SUMMARY OF CHF ANALYSIS FOR LOFT 5.5-FOOT CORE^[a]

<u>Code</u>	<u>Correlation</u>	<u>CHF Ratio</u>
COBRA	W-3 ^[b]	1.33
COBRA	B&W-2	1.59
HAMBO	W-3	1.22
HAMBO	B&W-2	1.49

[a] Analysis performed for a pressure of 2228 psi, nominal core ΔT of 68°F, coolant flux of 1.15×10^6 lb_m/ft²-hr, and an inlet coolant temperature of 549°F.

[b] The W-3 correlation includes a nonuniform heat flux factor as well as an equation for determining the effect of fuel rod spacers.

4. DISCUSSION OF CHF, INCLUDING THE DESIRABLE CHF RATIO AND THE COMPUTER PROGRAMS USED TO DETERMINE CHF

The term critical heat flux defines the situation wherein the heat transfer from the cladding to the coolant suddenly decreases resulting in a large, rapid increase in the cladding temperature. The CHF ratio is the ratio of the calculated CHF at a cladding node divided by the steady state heat flux calculated to exist at that node.

CHF is considered to be a local phenomenon rather than a bundle-average phenomenon. Therefore, the coolant quality and flow rate are quantities which are desirable to know for the subchannels adjacent to the fuel rods which produce the most power and are, therefore, most likely to achieve a critical heat flux.

Tong reports that for a rod bundle with mixing vane grids and having a CHF ratio of 1.3 (using the W-3 correlation), the probability of reaching CHF based on its (the W-3 correlation) standard deviation is 0.05% (which is negligible) [A-2]. This probability is also based on the fact that the Westinghouse Electric Corporation THINC-II subchannel code was used in conjunction with the W-3 correlation.

HAMBO and COBRA are computer codes that employ a subchannel model for the steady state hydraulic analysis of a rod cluster assembly cooled by fluid. In the subchannel model, the cross-sectional area available for coolant flow is subdivided into a number of parallel interacting subchannels. The equations of mass, energy, and momentum conservation for the subchannels

are solved to give the radial and axial variation in enthalpy and mass velocity. The CHF conditions are then assessed by use of appropriate CHF correlations for each of the subchannels.

In theory, the COBRA code is nearly the same as the HAMBO code, but both codes require the use of experimental data to calculate the extent of mixing and flow between subchannels. In addition, each code allows the user some liberty in choosing input such as the mixing coefficients, single- and two-phase friction coefficients, slip ratios, and subcooled nucleate boiling void fraction. Consequently, both codes may not necessarily predict the same answers. Yet the answers predicted by both codes (Table A-I) should be, and are, reasonably close. Indications are, however, that the mixing model used in HAMBO is more accurate than that used in COBRA (for example, in HAMBO, mixing between subchannels is reduced if a net flow occurs between subchannels; whereas, COBRA makes no provision for the effect of flow on mixing).

Use of multichannel codes, such as HAMBO and COBRA, may produce some difficulties for conditions wherein bulk boiling occurs. Under conditions for which bulk boiling is prevalent, preferential flow of the gas phase (steam) occurs into the more open channels [A-6]. Reference A-6 states that, "It has been shown that a subchannel code typical of the present generation codes (COBRA) is unable to predict the trends in the two-phase data regardless of the magnitude of eddy diffusivity mixing, thus indicating a fundamental problem in the thermal-hydraulic modeling". This shortcoming in the HAMBO and COBRA codes is well recognized. Both HAMBO and COBRA assume a homogeneous mixture of liquid and gas during bulk boiling and do not consider the true case wherein the liquid tends to remain adjacent to the fuel cladding and a steam and liquid mixture occupies the remainder of the coolant channel.

The LOFT core analysis using HAMBO and COBRA, however, indicates that bulk boiling will not occur. Therefore, even if HAMBO and COBRA are inaccurate during bulk boiling the analysis for LOFT would not be affected. The analysis does indicate that subcooled boiling takes place in the upper half of the core, but since the life of bubbles formed during subcooled boiling is quite short, this condition will probably not lead to extensive migration of the steam bubbles to the more open channels.

5. CONCLUSIONS

For the purposes of this analysis and for preliminary design purposes, a steady state core mass flux of $1.15 \times 10^6 \text{ lb}_m/\text{ft}^2\text{-hr}$ appears appropriate for the LOFT core described in Section 2, Parameters, of this appendix. As shown in Table A-I, this mass flux gives a CHF ratio varying from 1.22 from HAMBO and the W-3 correlation to 1.59 from COBRA and the B&W-2 correlation.

Indications are that the B&W-2 correlation is more nearly accurate than the Westinghouse Electric Corporation W-3 correlation when evaluated against rod bundle data; that is, it predicts the experimental results better for average channel conditions. Since in this application the correlations have been used

with subchannel analysis methods that have not been verified, the results should be considered preliminary. Experimental verification of the analysis under LOFT conditions is required to confirm the results. The CHF ratios in Table A-I are generally above the accepted minimum value of 1.30. The later inclusion of fuel and rod geometry effects and fuel enrichment effects are not expected to substantially reduce the CHF ratios. On the basis of the results presented, the proposed steady state mass cooling flux of 1.15×10^6 lb_m/ft²-hr is not expected to result in CHF prior to initiation of the loss-of-coolant experiment.

6. REFERENCES

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- A-2. L. S. Tong, "Critical Heat Fluxes in Rod Bundles", Two-Phase Flow and Heat Transfer in Rod Bundles, Annual Winter Meeting, ASME (November 1969) pp 31-46.
- A-3. J. S. Gellerstedt et al, "Correlation of Critical Heat Flux in a Bundle Cooled by Pressurized Water", Two-Phase Flow and Heat Transfer in Rod Bundles, Annual Winter Meeting, ASME (November 1969) pp 31-46.
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APPENDIX B
RELAP3 MODELS

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APPENDIX B

RELAP3 MODELS

The RELAP3 computer code^[B-1] was used to determine the hydraulic response of the primary system fluid in the LOFT and large PWR reactor systems during the decompression portion of an LOCA resulting from an offset shear break in the inlet piping. The RELAP3 model for a typical large, four-loop PWR system is described, along with the corresponding RELAP3 analytical model of the proposed LOFT system having a 5.5-foot core. The RELAP3 output from the reactor systems models was used to provide system coolant conditions for input to THETA1-B^[B-2], the hot channel heat transfer code used to predict thermal response of the hot fuel pin. RELAP3 output was also used in the re-flooding analysis.

1. ANALYSIS OF A TYPICAL LARGE PWR SYSTEM

The version of the RELAP3 code used to simulate a large PWR system, designated MOD 036, is dimensioned for up to 200 control volumes. The model of a typical large, four-loop PWR system undergoing an offset shear break in the inlet piping of one of the four loops is shown in Figure B-1. Volume locations are given in Table B-I. In this model, the three unbroken loops are lumped into a single loop. At 0.005 second, valves are opened at the break junctions and a valve between the two volumes which simulate the inlet piping is closed to prevent crossflow. The effect of such modeling with valves is to simulate the leaks into the containment volume caused by an offset shear break of the inlet piping occurring at time 0.005 second. The minimum flow areas at the break junctions were calculated using a discharge coefficient of 0.6.

Since nodalization studies^[B-3] have indicated that at least five active volumes should be used to model a steam generator, each steam generator in the RELAP3 model has five heat transfer regions. Although the nodalization studies also have indicated that five or more control volumes should be used to model the core to obtain best accuracy, only three control volumes were used because very little loss in accuracy, and large saving in required computation time, resulted. The axial power profile used in modeling the core had a peak-to-average ratio of 1.73, and the assumed beginning-of-life power distribution was skewed toward the bottom of the core. This axial power profile resulted in RELAP3 power fractions of 0.4644, 0.3079, and 0.2277 for the lower, center, and upper core regions, respectively.

Table B-II presents a brief summary of the most important system parameters for a RELAP3 model of the typical large PWR system undergoing the offset shear break in the inlet piping. A standard equation for logarithmic-mean temperature difference, assuming a constant heat transfer coefficient and constant specific heats, was used to obtain both the temperature and power fraction distribution throughout the steam generators.

Fig. B-1 RELAP3 model for offset shear break of the inlet piping of a typical four-loop, large PWR system.

TABLE B-I

RELAP3 MODEL OF TYPICAL FOUR-LOOP PWR SYSTEM

<u>Volume</u>	
1	Head
2	Upper Plenum
3	Outlet Piping
4	Steam Generator Hot Plenum
5	Steam Generator Active Volume
6	Steam Generator Active Volume
7	Steam Generator Active Volume
8	Steam Generator Active Volume
9	Steam Generator Active Volume
10	Steam Generator Cold Plenum
11	Pump Inlet Piping
12	Inlet Piping
13	Upper Downcomer
14	Lower Downcomer
15	Lower Plenum
16	Core Bypass
17	Lower Core
18	Middle Core
19	Upper Core
20	Outlet Piping
21	Steam Generator Hot Plenum
22	Steam Generator Active Volume
23	Steam Generator Active Volume
24	Steam Generator Active Volume
25	Steam Generator Active Volume
26	Steam Generator Active Volume
27	Steam Generator Cold Plenum
28	Pump Inlet Piping
29	Pump Outlet Piping
30	Inlet Piping
31	Pressurizer
32	Accumulator
33	Accumulator
34	Containment

TABLE B-II

SUMMARY OF RELAP3 INPUT DATA FOR LARGE PWR MODEL

<u>Volume</u>	<u>Volume (ft³)</u>	<u>Initial Pressure (psia)</u>	<u>Bubble Model Index</u>
1	498.0	2250.7	0
2	1461.0	2253.2	1
3	237.0	2250.0	0
4	510.2	2245.8	0
5	477.0	2237.4	0
6	477.0	2227.2	0
7	297.0	2218.8	0
8	477.0	2216.5	0
9	477.0	2216.0	0
10	510.2	2216.4	0
11	504.0	2217.8	0
12	381.2	2305.7	0
13	320.2	2299.1	0
14	400.8	2295.6	0
15	1046.0	2295.3	0
16	292.0	2272.9	0
17	211.3	2282.5	0
18	211.4	2272.9	0
19	211.3	2263.4	0
20	79.0	2250.0	0
21	170.3	2245.8	0
22	159.0	2237.4	0
23	159.0	2227.2	0
24	99.0	2218.8	0
25	159.0	2216.5	0
26	159.0	2216.0	0
27	170.3	2216.4	0
28	168.1	2217.8	0
29	84.6	2306.3	0
30	42.5	2305.1	0
31	1801.3	2243.2	1
32	4162.5	614.7	1
33	1387.5	614.7	1
34	1,980,000	14.7	1

Bubble Model Index

<u>Index</u>	<u>Slope Parameter</u>	<u>Bubble Velocity (ft/sec)</u>
0	0	0
1	0.8	3.0

TABLE B-II (Contd.)

SUMMARY OF RELAP3 INPUT DATA FOR LARGE PWR MODEL

Junction	From Volume	To Volume	Initial Flow (lb/sec)	Minimum Flow Area (ft ²)	Junction Inertia (ft ⁻¹)
1	1	2	60	0.76	0.10
2	2	3	27,646	13.76	0.68
3	3	4	27,646	13.76	0.65
4	4	5	27,646	32.04	0.26
5	5	6	27,646	32.04	0.48
6	6	7	27,646	32.04	0.53
7	7	8	27,646	32.04	0.53
8	8	9	27,646	32.04	0.48
9	9	10	27,646	32.04	0.26
10	10	11		15.73	0.10
11	11	12	27,646	0	2.26
12	12	13	27,646	12.38	1.38
13	13	14	36,861	26.72	0.42
14	14	15	36,861	26.72	0.33
15	15	16	1,666	3.22	0.29
16	15	17	35,138	52.80	0.09
17	17	18	35,138	52.80	0.08
18	18	19	35,138	52.80	0.08
19	19	2	35,138	52.80	0.09
20	16	2	1,666	3.22	0.30
21	13	1	60	0.17	0.22
22	2	20	9,215	4.59	1.93
23	20	21	9,215	4.59	1.96
24	21	22	9,215	10.68	0.79
25	22	23	9,215	10.68	1.43
26	23	24	9,215	10.68	1.15
27	24	25	9,215	10.68	1.15
28	25	26	9,215	10.68	1.43
29	26	27	9,215	10.68	0.79
30	27	28	9,215	5.24	3.14
31	28	29	9,215	0	3.06
32	29	30	9,215	4.13	1.54
33	30	13	9,215	4.13	1.09
34	0	3	0	0.50	0
35	0	3	0	0.50	0
36	0	3	0	0.50	0
37	32	12	0	0	50.70
38	0	20	0	0.50	0
39	0	20	0	0.50	0
40	0	29	0	0.50	0

TABLE B-II (Contd.)

SUMMARY OF RELAP3 INPUT DATA FOR LARGE PWR MODEL

<u>Junction</u>	<u>From Volume</u>	<u>To Volume</u>	<u>Initial Flow (lb/sec)</u>	<u>Minimum Flow Area (ft²)</u>	<u>Junction Inertia (ft-l)</u>
41	33	29	0	0	152.00
42	31	3	0	0.72	73.00
43	29	34	0	2.47	2.49
44	30	34	0	2.47	1.24

2. DISCUSSION OF RELAP3 MODELS USED
FOR THE PROPOSED LOFT SYSTEM

The version of the RELAP3 code used for the analyses of the proposed LOFT system, designated MOD 036, is dimensioned for up to 200 control volumes. The RELAP3 model of the proposed LOFT system undergoing an offset shear break in the inlet piping of one of the loops is shown in Figure B-2. Volume locations are given in Table B-III.

In the proposed LOFT system, the volumes used for the unbroken loop are about three times as large as the corresponding volumes in the broken loop. Thus, the unbroken loop simulates the lumped loop used to represent the three unbroken loops of a typical, four-loop, large PWR system. The use of five active heat transfer volumes to model the steam generator and three volumes to model the core corresponds to modeling practices used for the typical large PWR system. The proposed LOFT system, however, does not have an active steam generator in the broken loop, and the "dummy" steam generator used to obtain pressure drops comparable to those of a large PWR was modeled by two control volumes. In general, the RELAP3 modeling of the proposed LOFT system corresponded to practices used in RELAP3 modeling of the typical large PWR system.

Table B-IV briefly summarizes the most important system parameters for the RELAP3 model of the proposed LOFT system having a 5.5-foot core undergoing an offset shear break of the inlet piping. The axial power profile used in modeling the 5.5-foot LOFT core had a peak-to-average power ratio of 1.65 [a]. The axial power profile resembled a chopped cosine curve skewed slightly toward the bottom of the core. The resulting RELAP3 power fractions for the lower, center, and upper core regions were 0.3407, 0.4830, and 0.1763, respectively.

[a] This peak-to-average power ratio varies slightly from that used in other appendices. This ratio is dependent on the number of axial nodes used in describing the axial power profile in the core.

Fig. B-2 RELAP3 model for offset shear break of the inlet piping of the proposed LOFT system.

TABLE B-III

RELAP3 MODEL OF LOFT SYSTEM

<u>Volume</u>	
1	Upper Plenum
2	Outlet Piping
3	Steam Generator Hot Plenum
4	Steam Generator Active Volume
5	Steam Generator Active Volume
6	Steam Generator Active Volume
7	Steam Generator Active Volume
8	Steam Generator Active Volume
9	Steam Generator Cold Plenum
10	Pump Inlet Piping
11	Inlet Piping
12	Downcomer
13	Lower Plenum
14	Core Bypass
15	Lower Core
16	Middle Core
17	Upper Core
18	Outlet Piping
19	Dummy Steam Generator Volume
20	Dummy Steam Generator Volume
21	Pump Inlet Piping
22	Pump Outlet Piping
23	Inlet Piping
24	Containment
25	Pressurizer
26 and 27	Accumulators

The temperature and power fraction distributions throughout the steam generator were calculated by using a standard equation for logarithmic-mean temperature difference and assuming a constant heat transfer coefficient and constant specific heats.

TABLE B-IV

SUMMARY OF RELAP3 INPUT DATA FOR OFFSET SHEAR INLET BREAK
OF PROPOSED LOFT SYSTEM HAVING A 5.5-FOOT CORE

<u>Volume</u>	<u>Volume (ft³)</u>	<u>Initial Pressure (psia)</u>	<u>Bubble Model Index</u>
1	34.20	2252.0	1
2	5.83	2250.0	0
3	8.15	2248.1	0
4	7.65	2241.7	0
5	7.65	2239.4	0
6	5.70	2237.6	0
7	7.65	2238.1	0
8	7.65	2238.9	0
9	8.15	2235.0	0
10	12.38	2235.7	0
11	9.39	2265.2	0
12	16.90	2264.0	0
13	16.50	2264.5	0
14	4.30	2258.6	0
15	4.30	2261.2	0
16	4.30	2258.5	0
17	4.30	2255.9	0
18	1.95	2251.2	0
19	8.75	2249.0	0
20	8.75	2249.0	0
21	4.13	2251.9	0
22	2.08	2251.2	0
23	1.04	2261.7	0
24	31,984.00	14.7	1
25	31.67	2248.1	1
26	79.39	614.7	1
27	27.54	614.7	1

Bubble Model Index

<u>Index</u>	<u>Slope Parameter</u>	<u>Bubble Velocity (ft/sec)</u>
0	0	0
1	0.8	3.0

TABLE B-IV (Contd.)

SUMMARY OF RELAP3 INPUT DATA FOR OFFSET SHEAR INLET BREAK
OF PROPOSED LOFT SYSTEM HAVING A 5.5-FOOT CORE

Junction	From Volume	To Volume	Initial Flow (lb/sec)	Minimum Flow Area (ft ²)	Junction Inertia (ft-l)
1	1	2	597	0.249	49.50
2	2	3	597	0.249	48.17
3	3	4	597	1.530	2.18
4	4	5	597	1.530	3.27
5	5	6	597	1.530	2.84
6	6	7	597	1.530	2.84
7	7	8	597	1.530	3.27
8	8	9	597	1.530	2.18
9	9	10	597	0.285	76.65
10	10	11	597	0.224	170.35
11	11	12	597	0.224	100.20
12	12	13	597	1.140	6.70
13	13	14	31	0.108	3.91
14	13	15	567	1.770	0.72
15	15	16	567	1.770	1.04
16	16	17	567	1.770	1.04
17	14	1	31	0.108	5.60
18	17	1	567	1.770	2.41
19	1	18	0	0.083	143.70
20	18	19	0	0.083	154.93
21	19	20	0	0.510	26.26
22	20	21	0	0.095	383.00
23	21	22	0	0.075	556.80
24	22	24	0	0.045	186.80
25	23	12	0	0.075	99.90
26	23	24	0	0.045	93.40
27	25	2	0	0.013	4025.00
28	26	11	0	0	2800.00
29	27	22	0	0	8370.00
30	0	2	0	0.500	0
31	0	18	0	0.500	0
32	0	2	0	0.250	0
33	0	18	0	0.250	0
34	0	11	0	0.250	0
35	0	22	0	0.250	0

3. REFERENCES

- B-1. W. H. Rettig, G. A. Jayne, K. V. Moore, C. E. Slater, M. L. Uptmor, RELAP3 -- A Computer Program for Reactor Blowdown Analysis, IN-1321 (June 1970).
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- B-3. C. E. Slater and R. D. Hentzen, "Nodal Sensitivity in Modeling a Large Pressurized Water Reactor System for a Loss-of-Coolant Accident", Proceedings of Conference on New Developments in Reactor Mathematics and Applications, CONF-710302 (March 1971).

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APPENDIX C
THETA1-B MODELS

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APPENDIX C

THETA1-B MODELS

The THETA1-B computer code^[C-1], THETA1-B/MOD 005, was used to determine the thermal response of the LOFT and large PWR cores during decompression resulting from a double-ended break of the inlet piping. This appendix describes the analytical model, thermal properties, heat transfer correlations, and other data used to determine the hot-spot cladding surface temperatures of LOFT and a large PWR.

1. DISCUSSION

The THETA1-B^[C-1] heat transfer code (THETA1-B/MOD 005) was used to determine the hot-spot cladding temperature response of the LOFT and large PWR cores. Since this code considers only the fuel rod and an associated fluid channel, the boundary conditions at the channel entrance were obtained from RELAP3. RELAP3 solves the conservation of mass, momentum, and energy equations for the complete reactor primary system (Appendix B). The required THETA1-B input data from RELAP3 are the power generation rate in the fuel rod, core pressure, and upper and lower plenum-core junction mass flow rates and fluid enthalpies.

Since THETA1-B solves only the fluid energy equation and not the coupled set of conservation equations for the fluid, several simplifying assumptions were made. For example, the pressure and mass flux were assumed to be uniform throughout the channel at any instant. Also, since RELAP3 solves the coupled set of conservation equations, fluid expansion effects in the core were taken into account. As a result, the fluid may be flowing into or out of the core at both ends simultaneously during portions of the transient. Such simultaneous flow implies that a stagnation point exists at some location in the core, and the flow rate in the core was assumed to be zero during these periods.

At times when the flow was predominantly in one direction, the mass flux and enthalpy were determined in the following manner. Upward flow through the core was considered positive and downward flow negative. The mass flux and enthalpy of the lower plenum-core junction were input when the flow was positive, and the mass flux and enthalpy at the upper plenum-core junction were input when the flow was negative. The mass flux was reduced by 10% to account for possible reduced flow in the hot channel due to fluid expansion, core manufacturing tolerances, and core flow redistribution.

The power generation rate in the fuel rod was obtained from the reactor kinetics model in RELAP3. The power generation rate dropped very rapidly to a level about 7% of normal operating power and then decreased slightly during the remainder of the transient.

A nearly symmetric axial power distribution with an axial peak-to-average ratio of 1.66 was used for the LOFT 5.5-foot core; whereas, a bottom skewed distribution with an axial peak-to-average ratio of 1.72 was used for the large PWR core. The axial power profile of the LOFT core is shown in

Figure C-1 and is the same as used in the CHF analysis of Appendix A. The peak-to-average ratios, however, vary slightly from those assumed in other appendices, primarily because of the difference in the number of nodes used for describing the axial power profile in the core.

Fig. C-1 LOFT axial power profile used by THETA1-B.

The model of the fuel rod for the THETA-1B calculations is shown in Figure C-2. The rod is divided into 11 equal lengths with a node located at the center of each length. Six radial nodes in the fuel, one node at the fuel surface, two nodes in the cladding, and one node at the cladding inner surface are used at each axial node point or axial level. For the fuel, the six nodes are located at the middle of equal thickness annuli. For the cladding (assuming a cladding thickness of δ), the first node is in the middle of an annular ring which is $3/4\delta$ thick. The other node is considered to be at the outside surface and located in the middle of the outside annular ring of $1/4\delta$. The nodes at the inside surface of the cladding and the surface of the fuel define the fuel pellet-cladding gas gap, which was assumed to be occupied by helium. Radially, all nodes are connected by conductances based upon the properties of the material as a function of temperature. Axially, only the cladding has conduction between nodes.

The heat transfer model used in this study allowed subcooled and superheated forced convection, subcooled and saturated nucleate boiling, transition boiling, and film boiling heat transfer regimes. The heat transfer correlations that were used are as follows:

Fig. C-2 THETA1-B fuel rod model.

Forced Convection	Dittus-Boelter ^[C-2]
Nucleate Boiling	Thom ^[C-3]
Forced Convection Vaporization	Schrock and Grossman ^[C-4]
Transition Boiling	McDonough, Milich, King ^[C-5]
Stable Film Boiling	Dougall, Rohsenow ^[C-6]

A combination of Babcock & Wilcox Company (B&W-2)^[C-7], Barnett^[C-8], and Modified Barnett^[C-9] correlations was used as the critical heat flux correlation as follows:

$p \geq 1500$ psia	B&W-2
$1500 > p > 1300$	Interpolation between B&W-2 and Barnett
$1300 \geq p \geq 1000$	Barnett
$1000 > p > 725$	Interpolation between Barnett and Modified Barnett
$725 \geq p$	Modified Barnett

In this combination, the CHF value is based directly on the correlations when the mass flux is above 200,000 lb_m/ft²-hr. The 200,000 lb_m/ft²-hr value is chosen as a lower limit to the validity of the correlations because these correlations predict the available experimental data to within $\pm 15\%$ only down to this value of mass flux. The capability of the correlations to predict the data below a mass flux of 200,000 lb_m/ft²-hr is in doubt. A CHF value for lower mass fluxes, therefore, is obtained as follows. The correlation for the particular pressure range is evaluated at the current pressure and at a mass flux of 200,000 lb_m/ft²-hr. Then an interpolation is performed between this value and the minimum allowable CHF value of 90,000 Btu/ft²-hr.

Any metal-water reaction that might occur was calculated with the Baker-Just^[C-10] parabolic rate law.

2. PARAMETERS

The design parameters that were held constant during this portion of the design study were listed in the body of this report. Thermophysical properties used in THETA1-B were as follows:

Uranium dioxide thermal conductivity in $\frac{\text{Btu}}{\text{hr-ft-}^\circ\text{F}}$

$$k = \frac{3978.1}{T + 692.61} + (6.0698 \times 10^{-12})(T + 460)^3$$

for $77^\circ\text{F} < T < 5072^\circ\text{F}$

Uranium dioxide specific heat in $\frac{\text{Btu}}{\text{lb}_m\text{-}^\circ\text{F}}$

$$C_p = 7.26 \times 10^{-2} + (3.33 \times 10^{-6}) T - \frac{4.74 \times 10^3}{(T + 460)^2}$$

$$\text{for } 77^\circ\text{F} < T < 2240^\circ\text{F}$$

$$C_p = -(3.6289 \times 10^{-15}) T^4 + (4.6457 \times 10^{-11}) T^3 \\ -(2.0447 \times 10^{-7}) T^2 + (3.8303 \times 10^{-4}) T - 0.18426$$

$$\text{for } 2240^\circ\text{F} < T < 5072^\circ\text{F}$$

$$\text{Zircaloy-4 thermal conductivity in } \frac{\text{Btu}}{\text{hr-ft-}^\circ\text{F}}$$

$$k = 4.14 + (1.044 \times 10^{-2}) T - (5.276 \times 10^{-6}) T^2 \\ + (1.536 \times 10^{-9}) T^3$$

$$\text{for } 572^\circ\text{F} < T < 2732^\circ\text{F}$$

$$\text{Zircaloy-4 specific heat in } \frac{\text{Btu}}{\text{lb}_m\text{-}^\circ\text{F}}$$

$$C_p = (6.80 \times 10^{-2}) + (1.33 \times 10^{-5}) T$$

$$\text{for } 32^\circ\text{F} < T < 1652^\circ\text{F}$$

$$C_p = 8.6 \times 10^{-2}$$

$$\text{for } T > 1652^\circ\text{F}$$

$$\text{Uranium dioxide density, } \rho = \frac{649.7 \text{ lb}_m}{\text{ft}^3}$$

$$\text{Zircaloy-4 density, } \rho = \frac{404.9 \text{ lb}_m}{\text{ft}^3}$$

3. REFERENCES

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APPENDIX D FLOODING MODELS

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FLOODING MODELS

This appendix describes the methods used to obtain the rod surface heat transfer coefficients during reflooding. Similarly to the use of the RELAP-THETA combination during blowdown, a combination of two basic calculations was used during reflooding. A finite difference solution of the continuity equation is used to calculate the core flooding rates. These rates are input to a correlation to obtain heat transfer coefficients that are used in a hybrid analog-digital nuclear rod model to compute the thermal response of the cladding.

1. CORE FLOODING RATES

Flooding rates for LOFT and a large pressurized water reactor were calculated using a flooding solution which considers the ECC injection rates, area and heights to be filled, steam generation in the core, and resistance to steam flow from the core to the break. Although the resulting flooding rates are valuable for purposes of comparing LOFT with a large PWR, their absolute values are optimistic because the calculations neglect the potential for ECC water plugging of the loop piping.

Resistance and ECC injection data were taken from RELAP3 output^[a]. The cold vessel ECC injection rates are shown in Figure D-1. These curves are the result of considering all forms of injection except that provided by the charging pump and accumulator on the broken loop. Core reflooding was assumed to start when the RELAP3-predicted volume of water in the lower plenum and annulus was equal to the volume in the lower plenum. Flooding times were 32.5 and 35 seconds after the pipe rupture for LOFT and the large PWR, respectively.

To convert the RELAP3-predicted mass injection rates, $\dot{m}$, in lb/sec to injection rates for the vessel, V_{ECC} , in in./sec, the following equation was used:

$$V_{ECC} = (\dot{m}_{total}) (12) / (\rho A_{total}) \quad (1)$$

A density, ρ , of 61 lb_m/ft³ was used. The total area, A_{total} , and the areas of the core, bypass, and downcomer components are given in Table D-I.

Downcomer heights of 16.23 and 13.71 feet were used for the large PWR and LOFT, respectively. Core flow resistance and effective loop flow resistances were calculated taking into account quality (χ), superheating in the steam generators, Lockhart-Martinelli two-phase flow friction multipliers (ϕ^2), and the flow split (SP) between the broken and unbroken loops. The core and loop pressure drops, ΔP , were calculated from the equation:

$$\Delta P = \frac{R'}{\rho_{steam}} \left[(\chi) (SP) (EN) (\dot{m}_{core}) \right]^2 \quad (2)$$

[a] RELAP3 Computer Runs PWR1-1, 2, and 4A; LOFT-3, 1, 5; and LOFT-3, 1, 6.

Fig. D-1 ECC injection rates into cold vessel.

TABLE D-I

VESSEL CROSS-SECTIONAL AREAS (ft²)

	<u>Core</u>	<u>Bypass</u>	<u>Downcomer</u>	<u>Total</u>
PWR	52.8	24.33	26.72	103.85
LOFT	1.77	0.7818	1.140	3.692

where

$$R' = \Delta P \rho / \dot{m}^2 \text{ for normal prebreak operation}$$

EN = the percent of core fluid which is entrained (80% was used in the calculations).

The core fluid quality was assumed to be 90% for the large PWR core and 80% for the LOFT core. The upper-plenum flow split was calculated from a parallel resistance network. Both LOFT and the large PWR had approximately 34% of the upper plenum flow exit through the broken loop. The core SP in Equation (2) was 1.0.

The density upstream of active steam generators was evaluated at 65 psia and $T = T_{\text{sat}}$. Flow was assumed to be superheated to 460°F by the time it got to Junction 5 (Figures B-1 and -2 of Appendix B) in the steam generators for both the large PWR and LOFT. From Junction 5 to the break, ϕ^2 and χ in Equation (2) were both equal to one.

The resistance network for the RELAP3 model of the large PWR is shown in Figure D-2. The pump resistance value is midway between the free-wheeling and the forward-flow locked rotor values. Similar LOFT resistance networks were obtained by multiplying the large PWR values in Figure D-2 by the ratio of the core flow areas squared. Thus, the R' values for the LOFT core are 889.8 times the large PWR values. This scaling method is attractive during flooding because, as Equation (2) shows, the core and loop pressure drops are direct functions of the mass flow into the core. Thus, to have the pressure drop for the large PWR equal to that for LOFT at the same core reflooding rates:

$$R'_{\text{LOFT}} = R'_{\text{PWR}} \left(\frac{A_{\text{PWR}}}{A_{\text{LOFT}}} \right)_{\text{core}}^2 \quad (3)$$

Actually, for closer simulation of the large PWR some of the other variables could be taken into account. For instance, the pressure drop can only be as large as the available water head in the downcomer. Results in Figure D-3 show the time at which this maximum head is reached and Figure D-4 shows the rate at which this head drives ECC through the core. By reducing LOFT resistances by the ratio of the downcomer heights (13.71/16.23), LOFT flooding rates would be even closer to large PWR rates. Differences evident in Figures D-3 and -4 are caused by differences in downcomer elevations and ECC injection rates and slight differences in effective resistances.

Fig. D-2 Large PWR resistances used in RELAP3 model.

Fig. D-3 Water height in the vessel as a function of time.

Fig. D-4 Core flooding rates.

2. CORE THERMAL RESPONSE DURING REFLOODING

Flooding rates were input to rod heat transfer coefficient correlations derived by Aerojet Nuclear Company from reflooding data obtained from the PWR-FLECHT Project[D-1]. The correlation yields heat transfer coefficients as a function of rod power, axial position, flooding rate, and time. Since the correlation is based on constant flooding rate data from a 12-foot-long core with a symmetrical power profile, methods were derived for using the correlation with a shorter core under conditions of variable flooding rates and skewed power profiles.

The comparative flooding rates predicted for a large PWR and for LOFT with a 5.5-foot core were very nearly 2 in./sec. Figure D-5 compares results from the correlation with PWR-FLECHT 2 in./sec flooding rate data and demonstrates that the correlation is generally accurate and slightly conservative.

A single-node hybrid analog-digital nuclear rod model was used to calculate the hot-spot cladding temperature with respect to time after flooding initiation. The input to the model was heat transfer coefficients versus time, cladding temperature at the time of flooding initiation, power generation versus time, and the heat transfer coefficient reference temperature. A single axial level model was used because: (a) the resistance to axially conducted energy flow is quite large compared to the resistance to radially conducted energy flow and so axial energy flow is neglected; and (b) the heat transfer coefficient reference temperature is the saturation temperature of the upper plenum and is not affected by energy transferred to the coolant at lower levels.

Fig. D-5 Comparison of results of heat transfer coefficient correlation with PWR-FLECHT heat transfer data for a flooding rate of 2 in./sec.

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