LOFT TECHNICAL REPORT LTR 111-127

APRIL 19, 1979

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L2-3 PRE-LOCE MANEUVER CORE SAFETY ANALYSIS

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IDAHO OPERATIONS OFFICE UNDER CONTRACT EY-76-C-07-1570

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**LOFT TECHNICAL REPORT
LOFT PROGRAM**

FORM EG&G-229
(Rev. 12-78)

TITLE L2-3 PRE-LOCE MANEUVER CORE SAFETY ANALYSIS		REPORT NO. LTR 111-127
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PERFORMING ORGANIZATION LOFT Reactor Systems Branch/Thermal Analysis Branch		DATE April 19, 1979
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SUMMARY

This report summarizes the core safety analyses and reactivity control analyses that have been done to support safe operation of the LOFT reactor in the LOCE pre-blowdown (or pre-LOCE) operating mode (Operating Mode 8) for the L2-3 nuclear loss of coolant experiment. This report also summarizes safety analyses done to support LOFT operation in other operating modes (Modes 5, 6, and 7) with changes in operating conditions or assumptions due to requirements for safe operation of the L2-3 test (changes from conditions or assumptions for previous safety analyses for those operating modes). The analyses discussed herein do not include analyses for potential loss of coolant accidents, or for accidents during the LOCE blowdown mode (Mode 9), or for potential radiation releases during LOCE operation. These latter analyses are discussed in:

- (1) "L2-3 Blowdown Scoping Experiment Safety Analysis (ESA)", C. H. Cooper, (November 27, 1978).
- (2) "L2-3 Test Experiment Safety Analysis (ESA)", J. L. Plum, (to be published).

This report and the ESA reports above provide the core safety analysis to support LOFT operation for the L2-3 test.

DISPOSITION OF RECOMMENDATIONS

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I. INTRODUCTION

This report summarizes the core safety analyses and reactivity control analyses that have been done to support safe operation of the LOFT reactor in the LOCE pre-blowdown operating mode (operating Mode 8) for the L2-3 nuclear loss-of-coolant experiment (LOCE). LOCE pre-blowdown operation (also referred to as pre-LOCE operation) is primarily that period of operation prior to the initiation of the blowdown portion of the LOCE when the reactor is in steady-state operation at the desired initial test conditions, i.e., operating at the power level and low flow condition necessary to obtain the test conditions for maximum linear heat generation rate (MLHGR) and core enthalpy rise (ΔH). Pre-LOCE or Mode 8 operation also includes the low flow maneuver, when the low flow trippoint has been decreased to the Mode 8 value, and flow is being decreased while temperature is being increased to desired test values. Reactor power is assumed to be near the desired test value prior to initiation of the low flow maneuver.

The analyses discussed herein do not include analyses for potential loss-of-coolant accidents (LOCA's) during L2-3 Mode 8 operation, accidents during the LOCE blowdown (Mode 9), or analyses for potential radiation releases during LOCE operation. These analyses or studies are scoped by References [1], [2], and [3].

This report also summarizes safety analyses done to support LOFT operation in Modes 5, 6, and 7 (zero power physics testing, startup, and power range operation, respectively) with changes in operating conditions or assumptions due to some specific requirements for safe operations of the L2-3 test. (The changes are changes from conditions or assumptions for previous safety analyses for these operating modes.) Examples are analyses and studies done to support operation with LOFT nominal PCS pressure reduced by 93 psi to 2169 psia in order to gain safety margin for L2-3 Mode 8 operation.

The potential accidents and other safety considerations are much the same as for the L2-2 pre-LOCE core safety analysis [9]. However, the L2-3 power level is 50% higher than for L2-2; therefore, the safety margins are significantly less. Because of this, the conservative approach used for L2-2 Mode 8 analyses to determine operating limitations, trippoints, and safety limits could not be used for L2-3. The L2-3 safety requirements thus become more numerous and leave less operating margin.

II. SUMMARY OF ANALYSIS RESULTS AND CONCLUSIONS

1. Mode 8 Analysis - Steady-state and transient safety analyses were performed for potential L2-3 LOCE pre-blowdown (Mode 8) operating conditions to establish the margins of safety and required operational bounds for L2-3 Mode 8 operation. The thermal-hydraulic criteria for judging the acceptability of the steady-state and transient analyses are discussed in Section III.1. The criteria used is the Damage Criteria of the LOFT Technical Specifications.

The steady-state analyses are discussed in Section IV.1. The transient analyses are discussed in Section V. Off-nominal limits for the reactor PCS parameters and other operating bounds were determined from the steady-state and transient analyses either as required bounds in order that the Damage Criteria could be met, or as reasonable conditions necessary for doing a core safety analysis. These resulting analysis bounds, or limiting conditions for operation (LCO's), are listed in Table II-1. These LCO's are not all-inclusive. Not included are those analysis bounds which are unchanged from the analysis bounds (or assumptions) used for power range or L2-2 safety analyses (i.e., valve response times, opening and closing rates, relief valve set pressure, etc.).

High reactor power is limited by the shutdown reactivity analyses performed in Section IV.4.1. A peak to core average (P/A) of 2.3 will satisfy the constraint of 41.7 MW (off-nominal high power) and allow the L2-3 LOCE requirement of 12 kW/ft to be attained.

The low reactor power bound is a bound used to keep the consequences of high reactivity rate control rod withdrawal accidents (CRWA) within the Damage Criteria.

The hot leg temperature bound, high pressure bound, maximum measured P/A, and boron concentration limit are all required for satisfactory mitigation of CRWA's. The control rod position bounds are needed so that P/A will not exceed 2.60, and for the scram reactivity insertion assumptions. The

TABLE II-1

L2-3 MODE 8LIMITING CONDITIONS FOR OPERATIONBASED ON L2-3 MODE 8 ANALYSIS BOUNDS

Reactor Power (high)	Power must be controlled to $\leq$ nominal + 2.5 MW. Nominal power = $90.23/(P/A)$ MW where P/A is the measured peak to core average power ratio (maximum relative power). This condition is satisfied if indicated power is held below the top control band plotted in Figure II-1.
Reactor Power (low)	Power must be controlled to $>$ nominal - 3.5 MW. This condition is satisfied (with an extra margin of 1 MW) if indicated power is held above the bottom control band plotted in Figure II-1.
Loop Flow (low)	Loop flow must be above a flow calculated as $[3.390/(P/A)] - 0.125$ Mlbm/h. This low flow bound is plotted in Figure II-2.
Hot Leg Temperature (high)	$\leq 613^{\circ}\text{F}$ (loop flow ≤ 3.0 Mlbm/hr). $\leq 600^{\circ}\text{F}$ (loop flow > 3.0 Mlbm/hr).
PCS Pressure (high) (Reactor outlet)	Pressure ≤ 2257 psia (2219 psia indicated).
PCS Pressure (low) (Reactor outlet)	Pressure ≥ 2081 psia
Control Rod Position	51 to 57 inches indicated withdrawn
Measured Peak-to-Core average power ratio	$2.3 \leq P/A \leq 2.60$
Boron Concentration in the PCS	Greater than or equal to 500 ppm (measured).
Steam Flow Control Valve stroke time	SFCV stroke time shall be within 17 to 40 s.

Note: All values are true value except CRA position which is indicated.

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FIGURE II-1

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FIGURE II-2
LCO FOR L2-3 MODES
PCS LOOP FLOW

MEASURED PEAK TO CORE
AVERAGE POWER RATIO

boron concentration bound of 500 ppm is limited by the assumptions for the development of the high bounds for the L2-3 moderator feedback coefficient (see Appendix A-5). The limit on high operating pressure was also required for the determination of steady-state limits (safety limits) that satisfy Damage Criteria A.

The bound for low pressure results from the analysis for the loss of steam load accident.

The steady-state analyses established the safety limits for steady-state L2-3 Mode 8 operation that are listed in Table II-2. See Section IV.1 for a discussion of the assumptions and analyses for determining these safety limits.

Steady-state analyses also determined that flow instability was not likely for L2-3 operation at worst-case values of the reactor thermal-hydraulic parameters^[36].

The L2-3 LOCE pre-blowdown (pre-LOCE) accident analyses demonstrate that the consequence limit of Damage Criteria A is satisfied for all accidents but one. Since the accident consequences are only required to meet the limits of Damage Criteria B or C, then the safety margins for these accidents are acceptable. The one accident for which Damage Criteria A is not satisfied is the rod ejection accident. The consequences of a rod ejection do, however, meet the limits of Damage Criteria C with significant margins which is acceptable for this limiting fault. The accident analyses are discussed in Section V. As discussed in the previous section, the accident analyses herein do not include LOCA's or accidents during the LOCE blowdown.

The PPS trippoints required for L2-3 safety are discussed in Section IV.2. Table II-3 lists the final trippoints as established by the L2-3 Mode 8 transient analyses and discussed in Appendix A-3. These trippoints are, in some cases, slightly more conservative than those required by the analysis in Section IV.2. Figure II-1 plots the trippoint requirements for the reactor power trip as a function of P/A. All other trippoints are single valued for L2-3 operation.

TABLE II-2

L2-3 MODE 8SAFETY LIMITS BASED ON L2-3MODE 8 STEADY-STATE ANALYSIS

Reactor Power	Nominal power + 6 MW (see plot in Figure II-3)
PCS Loop Flow	0.80 Mlbm/h
Hot Leg Temperature	639°F
High PCS Pressure	2490 psia
Low PCS Pressure	2000 psia*
Peak to Average Corepower	2.9

* No specific low pressure limit established; 2000 psia is lower bound for the LOFT-3 DNB data.

FIGURE II-3
L2-3 MODE 8
REACTOR POWER SAFETY LIMIT

TABLE II-3
L2-3 PPS TRIP SETPOINTS - MODE 8
FINAL VALUES^(c)

	<u>Nominal Trip Setpoint</u>	<u>LSSS</u>	<u>Worst- case Trippoint</u>	<u>Time Constant (s)</u>
High average power ^(a) (MW)	(Nominal +1.5) (Nominal + 2)(Nominal + 3.5) (These are plotted in Figure II-1)			0.25
Peak Power	NA	NA	NA	NA
High Hot Leg Temperature (°F)	613	615	617	18
PCS Loop Flow (Mlbm/h)	1.25	1.20	1.06	1.0
High Pressure (psia)(b) (psig)	2282 2269	2312 2299	2340 2327	1.0
Low Pressure (psia)(b) (psig)	2058 2046	2028 2016	2000 1988	1.0
Reflood Assist Bypass valve (% open)	5	8	10	0.10

(a) Nominal power = 90.23/(P/A)

(b) For conservatism, the high pressure channel uses 1 atm = 13 psia, and the low pressure channel uses 1 atm = 12 psia.

(c) Development and determination of these final values is discussed in Appendix A-3.

Conclusions from the transient analyses are discussed in Section V.6. The conclusions are much the same as those for the L2-2 core safety analysis. Reactivity or power increase accidents are the most limiting type of accident for L2-3 Mode 8 operation (including the loss of steam load accident). As was shown for the L2-2 core safety analyses, the accident consequences are worst (lowest MDNBR) for analyses with the maximum assumed measured P/A. The slow power rise accidents were a particular problem for the L2-3 safety analyses. Considerable parametric study was required before the analysis and trip-point bounds could be defined which would result in satisfactory mitigation of these transients. The lowest value of MDNBR (other than for the high energy rod ejection accident) was 1.150 for a CRWA or maximum boron dilution with a ramp rate of reactivity insertion of $0.7 \times 10^{-4} \Delta\rho/s$.

Analyses for CRWA's and the loss of steam load accident showed that the use of Damage Criteria A or B for evaluation of the accident consequences was not significantly different. A case was not found for these slow transients for which the consequence limit for Damage Criteria A could be exceeded (MDNBR > 1.14) without also exceeding the consequence limit for Damage Criteria B (peak clad surface temperature, PCST > 1650°F). This may be largely due to the many conservative assumptions used in the FRAPT4 LOFT fuel pin analysis, as well as the practice of forcing the fuel pin surface into DNB any time the probability for DNB exceeds 5%.

Reactivity controls and considerations for L2-3 pre-LOCE operation are discussed in Section IV.4. The potential effects of Xenon on the safety of L2-3 pre-LOCE operation is discussed in Section IV.4.5. The discussions in these sections conclude that certain limitations must be imposed for safe reactor operation. In Mode 7, the maximum power must be limited to less than the 46 MW due to the Xenon inversion effect resulting from a Mode 8 scram. The Mode 8 maneuver is limited in that Hot Leg Temperature must be less than 600°F (off-nominal) for PCS loop flows greater than 3.0×10^6 lbm/hr. With these limitations it is concluded that with the suggested controls on boron concentration and control rod position, LOFT can be operated safely in the L2-3 pre-LOCE evolution.

The conclusion of the steady-state and accident analyses is that the LOFT reactor can be operated safely at L2-3 LOCE pre-blowdown (Mode 8) conditions within the analysis envelope assumed, and with reactor trippoints within the assumed worst-case trippoints, since the analysis results demonstrate that the consequences of L2-3 Mode 8 operation will satisfy the consequence limits of the applicable damage criteria.

2. L2-3 Analyses for Modes 5, 6, and 7 - Safety analyses and studies for L2-3 test pre-LOCE operation for other operating modes (other than Mode 8) were done which show:

- A. Safety margins are not reduced for operation in Modes 5, 6, or 7 with nominal LOFT PCS pressure reduced to 2169 psia, the low pressure worst-case trippoint lowered to 2000 psia, and the high pressure worst-case trippoint reduced to 2340 psia.
- B. High flow operation safety margins are not reduced with the assumption of the less reactive "214 HF" scram curve (discussed in Section IV.4.2) as long as the high off-nominal power is 50 MW or less (or nominal power is 47.5 MW or less).
- C. Mode 7 safety margins (and therefore Modes 5 and 6 also) are not reduced for restart into Mode 6 and 7 after building in peak Xenon following L2-3 Mode 8 operation as long as nominal power is 46 MW or less.

Therefore, from these analyses, it is concluded that the operating pressure bounds and trippoints can be the same in all operating modes for L2-3 Mode 8 operation. Restriction on other operating conditions for those operating modes are not changed from what has previously applied. It is also concluded that nominal power for Mode 7 operation must be restricted to 46 MW or less for the L2-3 test. The analyses and results for the L2-3 Mode 5, 6, and 7 analyses are discussed in Section VI.

III. ANALYSIS ASSUMPTIONS AND MODELS

1. Analysis Criteria - L2-3 pre-LOCE steady-state and accident analyses are required to demonstrate that the consequences of accidents, operational transients, or normal steady-state operation within allowable bounds are predicted to be within the consequence levels of the damage criteria specified in the LOFT Technical Specifications (Specifications 3.13.3 and 4.13.3). Normal pre-LOCE operation (including operational transients) is required to satisfy the consequence levels of Damage Criteria A. Accident analyses demonstrate that the consequence levels of the damage criteria that correspond to the accident probability (or event category) are satisfied. These consequence levels are: Damage Criteria B for Incidents of Moderate Frequency, Damage Criteria C for Infrequent Incidents, and Damage Criteria D for Limiting Faults. Essentially, for the purposes of core thermal-hydraulic analyses, the consequence levels of the damage criteria are as listed in Table III-1.

Steady-state and operational transient analyses discussed in Sections IV and V demonstrate that consequence levels (a) and (b) of Damage Criteria A are satisfied, or the analyses define the limits within which consequence levels (a) and (b) are satisfied. FSAR, zero power, and power range safety analyses^{[3][15][18]} determined that consequence level (a) of Damage Criteria A was satisfied for operation at normal LOFT flow rates (otherwise referred to as "high" flow) if the minimum DNBR (MDNBR) is ≥ 1.13 when using the LOFT-1 DNB (or CHF) correlation^[4]. The LOFT-1 correlation was based, however, on data for channel mass fluxes greater than 0.75 Mlbm/h-ft^2 . The channel mass fluxes for the low flows desired for LOCE pre-blowdown operation are below the data bound for the LOFT-1 correlation. Additional DNB data for a simulated LOFT fuel bundle was collected recently at Columbia University for mass fluxes from 0.33 Mlbm/h-ft^2 to 0.60 Mlbm/h-ft^2 . The data for mass fluxes of 0.38 Mlbm/h-ft^2 and greater was added to the LOFT-1 data, and a new LOFT DNB correlation was developed. The results of the analysis for the new DNB correlation (the LOFT-3 correlation) are reported in Appendix A-1, and discussed in more detail in Reference [22]. The MDNBR for greater than a 95% probability at a 95% confidence level that DNB will not occur on the hot fuel pin using the LOFT-3 correlation is a MDNBR ≥ 1.14 .

TABLE III-1

THERMAL-HYDRAULIC ANALYSES DAMAGE CRITERIADamage
CriteriaConsequence Levels

A	(a) The minimum DNBR shall be greater than that value required for a 95% probability at a 95% confidence level that DNB will not occur on the hot fuel pin. (b) The fuel rod centerline temperature shall not reach the melting temperature at the hot spot. Fuel melting occurs at 5080°F at BOL (5062°F at 2000 EFPH).
B	(a) The maximum temperature at the fuel cladding outer surface shall be less than 1650°F.
C	(a) The maximum fuel cladding temperature shall not exceed 2200°F. (b) Fuel damage shall not result in more than 244 fuel rods exceeding an outside cladding temperature of 1900°F. (c) The total oxidation of the cladding shall nowhere exceed 0.17 times the total cladding thickness before oxidation. As used therein, the total oxidation means the total thickness of cladding metal that would be locally converted to oxide if all the oxygen absorbed by and reacted with the cladding locally were converted to stoichiometric zirconium dioxide. (d) Average fuel pellet enthalpy at the hot spot shall not exceed 280 cal/g in any fuel rod during reactivity-initiated events.
D	(No thermal hydraulic limits are specified. However, usually attempt to satisfy Damage Criteria C.)

In addition to the consequence levels (a) and (b) of Damage Criteria A, the steady-state performance objective reported in Section 4.1.2.3.3 of the FSAR^[6] that "Hydrodynamic instability shall not occur during normal operation and operational transients" needs to be demonstrated. The potential for hydrodynamic instability and the assurance that instability will not occur for L2-3 pre-LOCE operation is discussed in Section IV.1.

The steady-state analyses discussed in Section IV.1 were done to demonstrate that the above criteria are satisfied for normal pre-LOCE operation for L2-3 test conditions with one or more core thermal-hydraulic parameters equal to or exceeding the worst-case trippoint for that parameter while all other sensitive thermal-hydraulic plant parameters are at their off-nominal values. The steady-state analyses also defined at what value of the sensitive thermal-hydraulic plant parameters the limit of Damage Criteria A is reached. These steady-state analyses define the bounds for steady-state L2-3 Mode 8 operation. These bounds or limits meet the intent of 10 CFR 50.36 for safety limits as illustrated in the 1968 Guide to the Content of Technical Specifications^[23].

2. Analysis Assumptions - Desired L2-3 test operating conditions are as specified in the EOS^[24]. The primary requirements are:

Maximum Linear Heat Generation Rate (MLHGR)	12kW/ft (39.4kW/m)
---	--------------------

Core Differential Temperatures	64.5°F (35.8°C)
--------------------------------	-----------------

To obtain the 64.5°F core ΔT , the specified core enthalpy rise (for a 617°F hot leg temperature, 2264.6 psia, and 1.44 Mlbm/h loop flow) is specified as 88.07 BTU/lbm.

The L2 test series EOS^[24] specifies that reactor power and flow will be adjusted to obtain the specified MLHGR and ΔH as a function of the peak-to-core average power ratio (P/A) determined using the TIP system. The process by which the TIP measurements are used to construct the core power distribution and P/A is described in Reference [47]. Power is then adjusted according to the equation^[24]:

$$P_L = 7.519 \frac{\text{MLHGR}}{\text{P/A}} \text{ MW, where MLHGR} = 12 \text{ kW/ft.}$$

Flow will be adjusted to obtain the specified ΔH according to the equation^[24]:

$$\dot{M} = \frac{P_L}{\Delta H} (3.413) \text{ Mlbm/h.}$$

Therefore, reactor power and loop flow will be a function of the core power P/A determined by the TIPS program. For $\Delta H = 88.07 \text{ BTU/lbm}$, $\dot{M} = 3.502/(P/A)$. However, as has been indicated, the final test conditions necessary to achieve acceptable accident analysis consequences are different than specified in the EOS^[24]. For the final allowable test conditions (609°F hot leg temperature, and 2169 psia) the ΔH corresponding to a 64.5°F core ΔT is 86.31 BTU/lbm. For this ΔH , $\dot{M} = 3.568/(P/A)$. The original specified ΔH was used for determining the loop flows assumed for the L2-3 core safety analyses. Therefore assumed flows are conservative for the final allowable test conditions.

The control rod assembly position (CRP) is specified as 54 (± 0.5) in. withdrawn. At 54 inches withdrawn, P/A is predicted to be 2.40 for equilibrium Xenon conditions (see Appendix A-3). However, the CRP bounds desired for operational flexibility are 50 to 58 inches withdrawn. Since P/A increases as control rods are inserted, then P/A could be greater than 2.40. A study for the potential values of P/A for L2-3 operation is in Appendix A-2. From Appendix A-2, the predicted P/A for CRP* = 50 inches is 2.57. As discussed in the Appendix, the agreement between TIP-determined and predicted P/A is usually within 1%, and almost always within 3%. A $P/A \geq 2.65$ would envelope operation for L2-3 with a CRP as low as 50 inches and allow for a 3% difference between measured and predicted P/A . However, since operation significantly below CRP = 56 inches (54 inches withdrawn) is not intended, the safety analyses were based on a maximum measured (TIP-determined) P/A of 2.60. This maximum measured P/A envelopes potential operation at a CRP = 52 inches with a 4% margin, and still envelopes the expected P/A for CRP = 50 inches with a margin $> 1\%$. The maximum measured P/A during the L2-2 maneuver was 2.44 as compared to a predicted P/A of 2.43. Note that these CRP's are actual positions, and a comparison of indicated rod withdrawal to these values will need to consider the accuracy of the indication (± 1 inch indication error plus 2 inch displacement of rod tips above fuel bottom for 0 inches withdrawn).

* Control Rod Position refers to actual position of poison tips above fuel bottom.

The measured P/A may also be less than the predicted 2.40, especially if CRP > 56 inches. The safety analysis for L2-2 demonstrated that analyses for the highest assumed P/A was always limiting since the higher measured P/A results in the lowest test-specified loop flow^[10]. Therefore, only a few analyses were done for a P/A less than 2.40 (at P/A = 2.37) to verify the L2-2 conclusion. As for L2-2, a higher assumed P/A is found to be most limiting for L2-3 safety analyses.

The values for off-nominal (ON) operating conditions (used in the analysis) are established according to the equations:

High Reactor Power:

$P_{ON} = P_{NOM} \pm 2.5 \text{ MW}$ (low reactor power has been set to $P_{NOM} - 3.5$ giving a -2 MW control band)

Allows for a +1 MW control band, a 1 MW calorimetric power measurement accuracy, and a 0.5 MW neutron power channel accuracy.

Low Loop Flow:

$M_{ON} = [3.327/(P/A)] - 0.125 \text{ Mlbm/h}$ (based on $T_{HOT} = 617^\circ\text{F}$ nominal)

Off-nominal flow is established as 0.125 Mlbm/h (channel accuracy including indicator) less than the low-flow alarm point (at 95% nominal flow).

High Hot Leg Temperature:

Nominal $T_{Hot} + 4^\circ\text{F}$

Allows for +2°F control band and a 2°F channel accuracy (including indicator).

Pressure:

Nominal $\pm 88 \text{ psia}$

Allows for a +50 psi control band, and a 38 psi total indication accuracy.

The safety analyses are initiated from off-nominal bounds, unless initiation from within the control bands will result in more severe consequences. The nominal and off-nominal conditions for power and flow as a function of assumed measured P/A's used in L2-3 safety analyses are:

Measured P/A	2.37	2.50	2.55	2.60	2.66
Nominal Reactor Power (MW)	38.1	36.1	35.4	34.7	33.9
High Off-Nominal Power (MW)	40.6	38.6	37.9	37.2	36.4
Nominal Loop Flow (Mlbm/h)	1.48	1.40	1.37	1.35	1.31
Low Off-Nominal Flow (Mlbm/h)	1.28	1.20	1.18	1.15	1.12

Initial steady-state and transient analyses for L2-3 Mode 8 determined that Damage Criteria A (a LOFT-3 MDNBR ≥ 1.14) could not be satisfied at the high pressure limit (at 2440 psia). Also, parametric studies indicated that the largest single gain in safety margin (increase in MDNBR) could be obtained by lowering the pressure. A subsequent study was done of the effect of pressure on MDNBR as calculated by the LOFT-3 correlation, and the pressure effect on the critical heat flux (CHF) as seen in the LOFT DNB test data for low (L2-3 off-nominal) flows. This study, in Appendix A-4, concluded that LOFT nominal pressure (as determined at the reactor outlet) should be lowered from 2262 psia to at least 2169 psia. This decrease in pressure lowered the off-nominal high pressure to 2257 psia, and appeared to be worth approximately a 4% increase in MDNBR. Subsequent safety analyses were performed with these reduced pressures.

The assumed hot leg temperature (T_{hot}) was varied in the safety analyses to determine a value for which the applicable damage criteria could be satisfied. The range of values of T_{hot} used were 606°F (602°F nominal) to 621°F (617°F nominal).

The reactor trippoints for L2-3 reactor power and low flow can be a function of measured P/A. However, for the assumed trips for the safety

analysis, only the reactor power trip was varied. The assumed low flow trip was based on the value that would be required for the worst-case trip for a measured P/A of 2.66. The assumed worst-case trippoints for the safety analyses were:

High Reactor Power	Variable, but not less than nominal + 3.5 MW
Low Loop Flow	1.01 Mlbm/h
High Hot Leg Temperature	Nominal + 8°F
High Pressure	2340 psia
Low Pressure	2000 psia
RABV Opening	10% full open

The assumed time constants for the trips were the same as used for the L2-2 safety analyses with the exception of the time constant for the T_{hot} trip. Initial analyses for the control rod withdrawal accident determined that the damage criteria could not be satisfied with a 40 s time constant for the T_{hot} trip. An investigation into the assumed time constant determined that with a modification to the thermal wells (addition of a thermal conducting compound inside the thermal wells) a 15 time constant should be attainable. This time constant was then used for most final safety analyses. The limiting accident for a hot leg temperature trip, a slow control rod withdrawal accident was analyzed with a hot leg trip constant of 18 s which corresponds to the limit given in the LOFT Technical Specifications. The assumed trip time constants were:

Reactor power	1.0 s [0.25 s required by LOFT Technical Specifications]
Loop flow	1.0 s
Hot Leg Temperature	15 s and 18 s

Pressure	1.0
RABV Opening	0.10
Loss of PSMG Field	0.10 delay time

A 7% uncertainty was assumed for the local power distribution at the hot fuel pin given a TIP-determined in-core power distribution. Bases for the 7% uncertainty are discussed in Appendices A-2 and A-3. This power uncertainty is considered reasonable in that the power distribution measurement uncertainty in typical nuclear power reactors is less than 5%^[25]. The section on analysis models discusses how the power distribution uncertainty is applied.

The maximum and minimum values for moderator and Doppler reactivity feedback coefficients as used for the safety analyses were redefined for L2-3 operating conditions. The spread between the FSAR maximum and minimum values was considered excessive. Measured data for the coefficients from LOFT zero power physics tests and power range physics tests were used to define reasonable, but still bounding, limits. The moderator density and Doppler reactivity feedback coefficients used for L2-3 safety analyses are:

	<u>Maximum</u>	<u>Minimum</u>
Moderator Density Coefficient ($\Delta\rho/\text{lbm}/\text{ft}^3$)	6.5×10^{-3}	2.6×10^{-3}
Doppler Coefficient ($\Delta\rho/^{\circ}\text{F}$)	-1.08×10^{-5}	-0.44×10^{-5}

A discussion of the test-measured coefficients and how they were used to derive a spread in the coefficient values for L2-3 is in Appendix A-5.

Two scram curves were assumed for the L2-3 Mode 8 analyses. Most analyses were done using the "10/26 LF" scram curve (scram reactivity vs time) as used for the L2-2 Mode 8 safety analysis^[11]. A conservative determination of

the scram reactivity for L2-3 operating conditions was made after most L2-3 accident analyses had been done. Only those accidents whose consequences are most susceptible to change due to a change in the rate of negative reactivity insertion following a scram were re-analyzed with the new scram curve. As is discussed in Section V, the consequences of L2-3 accident analyses are not significantly changed by use of the new scram curve. The scram reactivity assumption for L2-3 operation is compared to the 10/26 LF scram curve in Figure IV-1, and is discussed in Section IV.4.2. Also discussed in Section IV.4.2 is the revised scram reactivity assumption for power range operation (Modes 6 and 7).

3. Analysis Models - The accident analyses were in all but one case based on the plant response to those accidents predicted using the LOFT hybrid computer model. The LOFT hybrid computer model is the same as used for the FSAR analyses as described in References [26], [27], and [28]. There has been hybrid model changes made in which some former analog calculations were changed to digital calculations to improve calculational precision, but otherwise the LOFT model is the same as for the FSAR analyses. Assumptions specific to the L2-3 hybrid computer analyses for specific accidents are discussed in Section V.

Analyses for the rod ejection accident were performed using the LOFT rod ejection RELAP4 model^[29] with modifications^[16]. This RELAP 4 kinetics model is the same as used for the FSAR, zero power physics testing, power range testing, and the L2-2 Mode 8 safety analyses, except that initial conditions have been changed to those for L2-3 operation. The "10/26 LF" scram curve was used for the L2-3 rod ejection accident analyses, with a 0.25 S delay. Although the L2-3 scram curve is worth slightly less than the "10/26 LF" scram curve in the first 1.5 seconds after scram (see Figure IV-1) the rod eject accident is terminated by fuel Doppler feedback. Therefore, the consequences of the rod ejection accident are not significantly affected by the scram assumption.

The calculations for steady-state and transient DNBR's used the COBRA IIIC computer code to solve for the thermal hydraulic response in the LOFT reactor fuel bundles. A subroutine in the LOFT versions of COBRA IIIC calculate the DNBR's in the coolant channels using the LOFT-3 DNB correlation (see Appendix A-1). The steady-state analyses and the analyses for the limiting transients

used the LOFT 1/8th core model. The COBRA LOFT 1/8th core model and the model assumptions are basically the same as was used for the steady-state FSAR analyses as described in the FSAR^[5] and in LTR 1111-38^[21]. A cross-section of the 1/8th core model used for LOFT is in Figure III-1. The LOFT small core model, representing 1/8th of the center or "A" fuel assembly and not containing the more exact representation of the cross flow interfaces (thus being more conservative), was used for the non-limiting transients. The LOFT small core COBRA model and model assumptions are basically the same as used for the FSAR transient analyses as described in LTR 1111-38^[21]. Figure III-2 shows a cross-section of the small LOFT core model. Differences in the L2-3 COBRA analyses from the model assumptions used for the FSAR are discussed in the following paragraphs.

The COBRA model assumptions for power distribution and power distribution uncertainties for the L2-2 core safety analyses were discussed in detail in Appendix A-4 of LTR 111-116^[12]. This discussion is attached to this report as Appendix A-6 for ease of reference. For L2-3 as for L2-2 the axial power distributions chosen for safety analyses were chosen for a flatter profile and a peak further up the channel. Appendix A-6 discusses the conservatism inherent in using a flatter power distribution, and how use of profile with a high radial peak to average power ($F_{\Delta H}$) achieves this purpose. (For a given total P/A, a hot pin power distribution with a high $F_{\Delta H}$ will therefore have a low axial peak-to-average power ratio (F_{AX}) or a flatter axial power profile since $P/A = F_{\Delta H} F_{AX}$). A study of power distributions determined that seldom was $F_{\Delta H} > 1.60$. Therefore, $F_{\Delta H} = 1.60$ was assumed for the L2-2 analyses. A study of the L2-2 experience and the relationship of pre-test PDQ predictions to TIP-determined power distributions is in Appendix A-2. In Appendix A-2 it was concluded that $F_{\Delta H} \leq 1.56$ for L2-3 operation. Therefore, the axial profile for PDQ run L1012^[30] with a P/A of 2.59 between 24 and 27 inches (axial channel height) and a radial peaking factor of 1.57 for a control rod position (CRP) of 50.6 inches above fuel bottom was chosen for the safety analyses. The radial peaking was increased to 1.58 to obtain a total P/A = 2.60. The L2-3 predicted power distribution, in Appendix A-3, for a CRP of 54 inches, has a peak only 21 to 24 inches up the channel, and an $F_{\Delta H} = 1.51$. The L2-3 P/A = 2.60 axial power profile used in the safety analyses is plotted in Figure III-3. The predicted radial power distribution in the core was used for the L2-3 analyses. This distribution is input into the COBRA model normalized to the value for the hot pin (pin number 13 in Figure III-1 and III-2).

Figure III-1 COBRA-III Model of 1/8 Segment of LOFT Core for DNB Analysis

Figure III-2 COBRA-IIIC Model of LOFT Core Central Assembly for Transient DNB Analysis

Appendix A-6 discussed how the uncertainties on the local power distributions were input into the COBRA model for the L2-2 analyses. The power distribution uncertainty is applied to all fuel pins surrounding the hot channel, as is the densification factor. The engineering hot spot factor is applied to the hottest pin adjacent to the hot channel. The same procedure was used for the L2-3 analyses. However, the L2-3 model differed in two respects from that used for the L2-2 analyses. These differences were:

A. The engineering hot spot factor was redefined using statistical methods. The redefined engineering hot spot factor was based on a margin of three standard deviations from the mean for the same fuel pellet density and enrichment data that was used to develop the previously-used engineering hot spot factor. The new engineering hot spot factor is a 1.026 multiplier applied to the hottest fuel pin adjacent to the hot channel. A discussion of the derivation of this redefined hot spot factor is in Appendix A-7.

B. The hot fuel pin in the core (pin number 13) is not moved to position 20 adjacent to the hot channel (channel 15). Figures III-1 and III-2 show these locations in the LOFT core. As stated in LTR 1111-38^[21], "Uncertainties in the prediction of the power profile are accounted for in the COBRA-II analysis by assuming that the hottest rod in the core is adjacent to the subchannel with the limiting DNB ratio (subchannel 15)". This was accomplished in the LOFT model by setting the normalized radial power factor for rod (or fuel pin) 20 to 1.0. But for L2-3 operation, operating power is set as a function of the measured P/A whereas for power range operation, power is independent of P/A as long as the limitations on minimum control rod position are satisfied. For L2-3 operation, the local power distribution uncertainty is taken to be 7%, an uncertainty that is greater than would be provided by setting the radial power factor for rod 20 to 1.0 (which increases the rod 20 power by 2.3%). Therefore, the rod 20 radial power factor is left as predicted by PDQ, but then a 7% local uncertainty factor (a 1.07 multiplier) is applied to all three rods surrounding channel 15 (rods 14, 20, and 21). As a result of this approach, rod 14 becomes the hottest rod since it has a larger radial power factor than rod 20.

The resulting three power uncertainties are input in the COBRA models as multipliers to the radial power factors obtained from PDQ (which were

normalized to the radial power ratio for the PDQ hot fuel pin, or pin 13). The three uncertainty multipliers as modeled for the L2-3 COBRA analyses are then:

- A. Engineering hot spot factor (1.026). Applied only to the hottest fuel pin adjacent to channel 15 (pin 14).
- B. Local power uncertainty factor (1.07). Applied to pins 14, 20, and 21.
- C. Densification factor (1.011). Applied to pins 14, 20, and 21.

The radial power distribution for PDQ run L1027^[31] (the power distribution used for FSAR and power range safety analyses for a control rod position of 50 inches above the bottom was used for the L2-3 safety analyses. The normalized radial power factors for this case are listed for the hot pins in Table III-2. Also listed in Table III-2 are the corresponding radial power factors for PDQ case L1012, the low flow case used for the L2-3 axial power profile. As seen in Table III-2, use of the L1027 radial power distribution may be slightly conservative, but not significantly so.

PDQ run L1058^[30] with $P/A = 2.44$, $F_{\Delta H} = 1.534$, and a peak between 24 and 27 inches up the channel were used for the COBRA analyses for measured $P/A = 2.50$ and 2.55 . For these analyses, the radial peaking was increased to 1.57 and 1.60, respectively. Tables of the axial and radial power factors used for the L2-3 COBRA analyses are in Appendix A-8.

A low conductance (for heat conduction across the fuel pellet to cladding gap) of $750 \text{ BTU/h-ft}^2\text{-}^\circ\text{F}$ was assumed for COBRA analyses for all accidents except power increase accidents. For power increase accidents, the gap conductance was assumed to have a conservatively high value of $10,000 \text{ BTU/h-ft}^2\text{-}^\circ\text{F}$. For other than power increase accidents, the small conductance tends to hold up the heat generated in the fuel and release it later in the transient when the thermal-hydraulic conditions are more severe (lower flow, higher coolant temperature). The high conductance offers very little resistance to heat flow out of the fuel, and therefore for a power increase accident, it results in maximum heat flux when the power is highest with a minimum transient hold-up of the heat. For slow power increase

TABLE III-2
L2-3 COBRA MODEL RADIAL POWER FACTORS

<u>COBRA PIN NUMBER</u>	<u>NORMALIZED FACTORS FROM PDQ L1027</u>	<u>NORMALIZED FACTORS FOR PDQ L1012</u>	<u>L2-3 NORMALIZED FACTORS WITH MULTIPLIERS</u>
13	1.0	1.0	1.0
14	0.998	0.997	1.108
20	0.977	0.976	1.057
21	0.973	0.971	1.053

transients, as for steady-state calculations, the value of the gap conductance in the COBRA model makes little difference.

During the development of LOFT models for use with the COBRA IV-I computer code, it was determined that the predictions for channel mass flux were significantly different from the predictions obtained using COBRA IIIC for slow transients. COBRA IIIC predicted significantly more flow diversion out of the hot channel, and thus predicted significantly smaller values for mass flux and DNBR. The COBRA IV results appeared reasonable, since it predicted results close to those obtained using a steady-state assumption. For slow power rise transients, such as boron dilution or slow control rod withdrawal accidents, a pseudo-steady-state solution approach should be reasonable and conservative.

An investigation for possible reasons or causes for this discrepancy determined that the equation for calculating the spacer grid (or spacer blade) loss coefficients in the COBRA LOFT model was in error. The equation, as it existed in the COBRA III model was:

$$CD = CD + K (Re)^r$$

This equation was added to COBRA to make the spacer loss coefficient (CD) a function of Reynolds number (Re). For the LOFT fuel bundle, CD is initially set to zero, and $r = -0.142$. As a result of the above equation, upon every axial flow solution iteration, CD was increased by an increment equal to $K(Re)^r$. Therefore, the spacer loss coefficients were continually increasing through the transient. As a result, the axial flow restriction continually increases. Since the Reynolds number exponent is negative, the increase in the coefficient is greater in the channels with less flow, particularly in the hot channel (channel 15 in the COBRA model). This results in increased flow diversion out of the hot channel as the transient progresses. The correct equation for spacer loss coefficient is:

$$CDS = CDI + K(Re)^r$$

where CDI has an initial constant value set by the user (equal to zero for LOFT). This corrected equation for the spacer grid loss coefficient was used for the final analyses for the limiting L2-3 transients.

Since the error in the loss coefficient equation increases the flow resistance in the hot channel relative to most other channels in the LOFT fuel bundle, then results obtained in analyses with the error should be conservative (a higher flow resistance results in lower mass flux in the channel, higher quality, and lower DNBR). The conservatism of analyses with the error in COBRA was shown by comparative calculation for different type of transients. One of these transients, the loss of both PCS pumps without a flywheel-assisted coast-down, is considered to be the type of accident for which the loss coefficient equation error would have the least effect (or possibly a non-conservative effect) because of the rapid change in the flow forcing function at the inlet to the channels. As seen in the results of the comparative COBRA calculations in Table III-3 for this accident, although the effect of the error on MDNBR is small, it still remains a conservative effect. Appendix A-9 contains more discussion on the correction of the spacer grid loss coefficient equation in COBRA III.

The FRAP-T4^[20] computer code was used to calculate fuel and clad temperatures for those transients for which the probability for DNB on the hot pin exceeded 5% (MDNBR < 1.14). The results of the FRAP calculations were then compared to the consequence limits of Damage Criteria B or C (whichever was applicable). For the final L2-3 Mode 8 transient analyses, a FRAP analysis was done only for the rod ejection accident.

Transient channel flow and enthalpy histories from the COBRA IIIC calculations are used as input for the FRAP analysis. The FRAP model for LOFT is a single (hot) pin in a single channel, whereas a fuel pin in the LOFT core is flanked by four open channels in the COBRA model. The FRAP coolant history input option used for the LOFT safety analyses is to:

- A. Input the inlet and outlet plenum enthalpy history.
- B. Input the core average pressure history.
- C. Input an average channel mass flux history. (For LOFT safety analysis, the mass flux history at the point of MDNBR in the hot channel is used as the channel mass flux input.)

TABLE III-3

COMPARATIVE COBRA IIIC CALCULATIONS FOR
EFFECT OF SPACER GRID LOSS COEFFICIENT EQUATION ERROR

	<u>With Loss Coefficient</u> <u>Equation Error</u>		<u>With Corrected Loss</u> <u>Coefficient Equation</u>	
	<u>Mass Flux @</u> <u>Point of MDNBR</u> <u>Mlbm/h-ft²</u>	<u>MDNBR</u>	<u>Mass Flux @</u> <u>Point of MDNBR</u> <u>Mlbm/h-ft²</u>	<u>MDNBR</u>
0.1 x 10 ⁻⁴ Δρ/s Reactivity Increase	0.512	1.131	0.600	1.174
0.5 x 10 ⁻⁴ Δρ/s Reactivity Increase	0.513	1.109	0.601	1.151
2.0 x 10 ⁻⁴ Δρ/s Reactivity Increase	0.444	1.098	0.591	1.154
Loss of Steam Load	0.481	1.120	0.607	1.156
Two Pump Loss Without Flywheel Coastdown	0.400	1.175	0.412	1.179

The hot pin axial power profile and the average linear heat generation rate history for the hot pin are input to FRAP. These inputs are the same as for the COBRA calculations. A radial power profile in the fuel pin is input as described in Figure 7 of Reference [21].

The LOFT instant densification model is used for FRAP analyses for the hot fuel pin. The instant densification model is described in Reference 32. In this model, the fuel pellet diameter is decreased initially (before the start of the analysis) to a densified diameter. The densified diameter of the fuel pellet was calculated according to the equation recommended by Reference [33]. This assumption of instant densification results in use of an initial pellet diameter of 0.3603 inches (9.15 mm), instead of the nominal pellet diameter of 0.3659 inches (9.29 mm). Therefore, the LOFT FRAP model uses a high assumption for diametrical gap width (0.33 mm instead of the nominal 0.19 mm). Pellet mass and fuel content are considered constant. Therefore, the use of the instant densification assumption results in the calculation of high initial fuel temperatures and high latent energy in the fuel pellet. This initial condition will result in the calculation of high clad temperatures during a transient after the surface heat transfer state goes into film boiling. The effect of the instant densification assumption can be seen in the results of two FRAP calculations for a slow power transient in Table III-4. Use of instant densification dimensions results in 270°C higher fuel centerline temperature and 66°C higher maximum clad temperature than would be calculated using the nominal pellet dimensions. For rapid transients with high peak powers (such as for a rod ejection), the difference may be less.

A hot spot factor of 1.05 is applied to the linear heat generation rate for the LOFT FRAP fuel pin model. The development of this hot spot factor is discussed in Reference [32]. The FRAP hot spot factor includes the effect on local power of:

A. Fuel pellet stack shortening due to instant densification. The amount of stack shortening, and the resulting increase in linear heat rate, is calculated from an equation recommended in Reference [33]. The resulting hot spot factor (multiplier) is 1.024 for a 24% decrease in stack height.

TABLE III-4
COMPARATIVE FRAPT CALCULATIONS FOR EFFECT
OF INSTANT DENSIFICATION ASSUMPTION
FOR A SLOW REACTIVITY INSERTION TRANSIENT

	<u>With Instant Densification</u>	<u>With Nominal Pellet</u>
Fuel Pellet Diameter (mm)	9.15	9.29
Fuel-to-clad diametral gap width (mm)	0.33	0.19
Maximum Fuel Centerline Temperature		
(°C)	2278	2008
(°F)	4133	3646
Maximum Clad Temperature		
(°C)	1083	1017
(°F)	1982	1863
Peak Clad Surface Temperature		
(°C)	1059	994
(°F)	1939	1822

Hot spot was forced into film boiling at 29 s.

Maximum temperatures occurred at approximately 35 s.

Reactor scram was at 34.4 s.

B. Power spiking due to pellet-to-pellet gaps resulting from pellet densification. Although this would be a local (axial point) effect, the 1.026 hot spot factor is applied to the entire fuel pin.

C. Fuel pellet stack thermal expansion in response to densification. This results in a decrease in linear heat rate. The combination of this effect with the stack shortening effect of densification results in a net hot spot factor of 1.011 for densification fuel stack shortening (as used in the COBRA model).

D. Fuel enrichment tolerance (a hot spot factor of 1.0125).

The fuel pellet density tolerance is included in the input fuel density used in the LOFT FRAP model. The above FRAP input assumptions are the same as used for previous FSAR related LOFT FRAP analyses^[7], for the zero power physics testing safety analysis^[17], and L2-2 Mode 8 safety analysis^[13]. FRAP T4, Mod. 004, version 6/23 was used such that surface heat transfer could be forced into film boiling at times and axial locations input to the code.

IV. L2-3 OPERATIONAL AND REACTIVITY LIMITS

1. Steady-State Analyses and Safety Limits - Steady-state calculations were done using COBRA IIIC and the LOFT 1/8th core model to define safety limits for L2-3 Mode 8 operation. The analysis method was to set all the sensitive thermal-hydraulic parameters except one to their off-nominal or a more limiting value (more limiting for determining DNBR), then to vary the remaining parameter until either:

- (a) MDNBR = 1.14,
- (b) a physical limit is reached,
- (c) a limit of analysis validity is reached, or
- (d) as a minimum, the limiting worst-case trip level for that parameter is reached or exceeded.

The sensitive reactor thermal-hydraulic parameters are:

- (a) Peak-to-core average power ratio (P/A). This value is held constant at its maximum measured value.
- (b) Reactor power.
- (c) PCS loop flow (or core flow).
- (d) PCS temperature. This value is defined by specifying the hot leg temperature.
- (e) PCS pressure. This value is defined by specifying the reactor outlet pressure.

The off-nominal values for the sensitive parameters were calculated according to the equations in Section III.2.

Early COBRA calculations for desired L2-3 test conditions had indicated that it may not be possible to meet Damage Criteria A ($MDNBR \geq 1.14$) at limiting steady-state values (worst-case trip values) of the sensitive thermal-hydraulics parameters (see Appendix B-2). These calculations were for $P/A = 2.43$, 40.6 MW, 1.04 Mlbm/h, 625°F T_{Hot} , and 2440 psia. Subsequent calculations for a maximum P/A of 2.66 determined that MDNBR was only 1.17 for all off-nominal conditions (36.4 MW, 1.12 Mlbm/h, 631°F, and 2350 psia) and that $MDNBR > 1.14$ at worst-case power (37.4 MW) or worst-case pressure (2440 psia). Therefore, the conditions for L2-3 Mode 8 operation needed to be modified from those specified as nominal in the L2 EOS in order to meet Damage Criteria A at steady-state operation limits, and to meet Damage Criteria B for incidents of moderate frequency.

The change that accomplished the greatest gain in MDNBR with what appeared to be a minimum impact on test objectives was to decrease the nominal operating pressure of LOFT. A study for a decrease in LOFT operating pressure and its influence on DNBR is in Appendix A-4. The suggested nominal pressure decrease was from 2262 psia to 2169 psia, which would increase the steady-state MDNBR by approximately 4%. This decrease in nominal operating pressure lowered the high off-nominal pressure from 2350 psia and allows for a decrease in the worst-case high pressure trip to less than 2400 psia. Therefore, the steady-state limits are based on a 2169 psia nominal reactor outlet pressure for LOFT. In addition, the maximum measured peak-to-core average power ratio (P/A) was decreased to 2.60 (see Section III.2 for a discussion of P/A assumptions). With these two changes the MDNBR for all off-nominal conditions was increased from 1.17 to 1.24. Transient analyses for control rod withdrawal accidents and the loss of steam load accident determined that an additional modification to L2-3 initial conditions was required. The hot leg temperature was subsequently decreased to 613°F off-nominal. The results of the L2-3 Mode 8 steady-state limit analysis are tabulated in Table IV-1 for 613°F high off-nominal T_{Hot} . The steady-state analysis results are discussed further in Appendix B-1.

The reactor power safety limit was determined for a maximum measured peak-to-core average power ratio (P/A) of 2.60. The steady-state and transient analyses have consistently shown that the lowest values for MDNBR are

TABLE IV-1

L2-3 MODE 8 SAFETY LIMIT ANALYSISRESULTS. T_{HOT} (OFF-NOMINAL) = 613°F

<u>Parameter</u>	<u>Safety Limit</u>	<u>Limit Criteria</u>	<u>Comments</u>
Reactor Power	Nominal power + 6 MW	$MDNBR \geq 1.14$	Calculated limit was 41 MW for P/A = 2.60.
Hot Leg Temperature	639°F	Saturation at the reactor outlet for low off-nominal pressure = 2050 psia	$MDNBR > 1.18$ with pres- sure = 2257 psia
PCS Loop Flow	0.8×10^6 lbm/h	Lower limit of the LOFT-3 correlation (mass flux of 0.4×10^6 lbm/h-ft ²)	$MDNBR > 1.24$
High PCS Pressure (Reactor outlet)	2490 psia	$MDNBR \geq 1.14$	
Peak to Average Core Power	2:9	Analysis limit	P/A modified by uncertainties

Off-nominal conditions assumed were:

P/A (measured)	2.60
Reactor Power	37.2 MW
Hot Leg Temperature	613°F
PCS Loop Flow	1.15×10^6 lbm/h
PCS Pressure	2257 psia

obtained for the highest assumed measured peaking. This result is to be expected since for any measured P/A, power is adjusted to obtain the specified nominal maximum linear heat generation rate (MLHGR) of 12 kW/ft. With a higher measured P/A, power is lowered to obtain the specified MLHGR. Therefore, to obtain the specified core differential temperature, flow must be lowered. Therefore, for a high measured P/A, the core is operated with the specified MLHGR, but with a lower core flow, which results in a more severe thermal-hydraulic condition in the hot channels. Thus, the power safety limit of 41 MW for $P/A = 2.60$ is limiting for all $P/A \leq 2.60$ since the low off-nominal flow for $P/A = 2.60$ is a minimum for all $P/A \leq 2.60$. This safety limit is more than 6 MW above the nominal power for $P/A = 2.60$ (34.7 MW), or 18% above nominal power. Since the nominal power for $P/A = 2.60$ is a minimum for all $P/A \leq 2.60$, then the power safety limit can be conservatively defined as nominal + 6 MW for all $P/A \leq 2.60$.

The steady-state hot leg temperature was limited by reactor outlet saturation conditions. The minimum saturation temperature is 639.3°F for a low off-nominal reactor outlet pressure of 2050 psia. As seen in Table IV-1, MDNBR is still > 1.18 with a 639.3°F T_{Hot} off-nominal power, and a high off-nominal pressure of 2257 psia. For the 2050 psia pressure that is consistent with the 639.4°F saturation temperature, MDNBR > 1.31 . Hot leg temperature is limited by saturation because of the potential increase in the probability for hydraulic instability when the reactor exit temperature reaches saturation.

The analysis for a limit on flow was limited by the lower limit for the DNB data upon which the LOFT-3 DNB correlation is based, that is a local mass flux at the point of MDNBR of 0.40 Mlbm/h-ft² (see Appendix A-1). For $P/A = 2.60$ and L2-3 off-nominal conditions (an off-nominal $T_{\text{Hot}} = 613^\circ\text{F}$), this mass flux limit will occur at a loop flow of 0.69 Mlbm/h. However, below 0.80 Mlbm/h loop flow, the hot channel changed from channel 15 to channel 14 (channel 14 has less flow at these conditions in the region of peak power). Also, at loop flows less than 0.80 Mlbm/h, the inlet temperature has to be significantly less than 500°F in order to maintain a 613°F T_{hot} and the core ΔT for high off-nominal power. Therefore, the safety limit has been defined as 0.80 Mlbm/h for

L2-3 conditions and 613°F high off-nominal T_{hot} . At these conditions, MDNBR > 1.24, and the mass flux at the point of MDNBR is 0.509 Mlbm/h-ft². For the same flow rate, if $P/A < 2.60$, reactor power would be higher, ΔT would also be higher, and inlet temperature would therefore be less. Since MLHGR does not change, then MDNBR for cases where $P/A < 2.60$ and flow not changed should be equal to or greater than that for $P/A = 2.60$. A comparative calculation for $P/A = 2.66$ and $P/A = 2.37$ with $T_{Hot} = 621^\circ\text{F}$ (see Appendix B-1) show that while the MDNBR for 2.37 is less than that for 2.66, the difference is not significant. Therefore, the low flow safety limit is single valued for all $P/A \leq 2.60$.

Initial steady-state calculations for L2-3 conditions determined that the MDNBR limit may be approached at the LOFT high pressure limits. For $P/A = 2.60$ and $T_{Hot} = 621^\circ\text{F}$, MDNBR = 1.14 at 2400 psia, a pressure less than the worst-case trip level used for FSAR analyses (2440 psia). For a 613°F high off-nominal T_{Hot} and $P/A \leq 2.60$, the high pressure safety limit (where MDNBR = 1.14) was determined to be 2490 psia.

No low pressure safety limit was defined for L2-3 operation, since the LOFT-3 correlation predicts an increase in MDNBR at low flows as pressure is decreased. This behavior was examined by reviewing the LOFT-3 DNB data for low flow, and found to be supported by the LOFT-3 data. A discussion of the pressure dependence of the LOFT-3 low flow DNB data is in Appendix A-4. Therefore, the only low pressure safety limit for L2-3 Mode 8 is what might be implied by the low pressure data limit for the LOFT-3 correlation of by the L2-3 Mode 8 low pressure worst-case trip, that is a limit of 2000 psia for both considerations. It should be noted, that the no penalty of a statistical nature is incurred by the use of this limit.

In conclusion, the L2-3 Mode 8 steady-state analysis has established the following steady-state or safety limits:

L2-3 MODE 8 STEADY STATE SAFETY LIMITS

Reactor Power	Nominal +6 MW
PCS Loop Flow	0.80×10^6 lbm/h

Hot Leg Temperature	639°F
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High PCS Pressure	2490 psia
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Analysis for the hot fuel pin peak centerline temperature (PCT) for the FSAR demonstrated that PCT does not reach melting temperature for the fuel pellet unless reactor power exceeds full reactor power, that is at a maximum Linear Heat Generation Rate (MLHGR) greater than 22 kW/ft^[35]. Although the analysis for PCT was for normal or "high" flow, steady-state PCT is relatively insensitive to channel flow. The major portion of the temperature rise from the coolant channel to fuel center is across the clad-to-fuel gap and through the fuel pellet itself. Flow rate only influences the film temperature rise from the bulk coolant to the clad surface, which is not significant unless DNB is exceeded. Therefore, since L2-3 operation is only at a MLHGR of 12 kW/ft, the steady-state PCT will not approach melting temperature.

A calculation for PCT, and the peak cladding surface temperature (PCST), was made using the FRAP-T4 computer code in conjunction with some of the accident analyses for L2-3 pre-LOCE operation. The calculation was for initial off-nominal conditions of:

Power	36.4
Loop Flow	1.12 Mlbm/h
P/A	2.66
MLHGR	12.9 kW/ft (not including hot spot multipliers)

A calculated steady-state PCT was 4496°F, with a value for PCST of 662°F. Therefore a large margin exists from fuel pellet melting, which requires a temperature of 5080°F at BOL, or 5075°F at 200 EFPH^[35].

Note that if flow is increased during Mode 8 operation, core differential temperature will decrease and core inlet temperature will increase, changing the MDNBR. An analysis for L2-2 test conditions^[14] showed that such an increase in flow during Mode 8 operation will result in an increase in safety margin (MDNBR increases). Based on that result, the calculation was not performed for L2-3 Mode 8 analysis.

A study for the potential for hydrodynamic instability during LOFT operation for the L2-3 test has been performed^[36]. This analysis indicates that thermal-hydrodynamic flow instabilities are not expected to occur for nominal test conditions, or for operation at worst-case type conditions for the L2-3 test. The instability study shows that static instability, due to a significant change in two-phase conditions and a large change in flow resistance, will not occur at L2-3 worst-case operating conditions (reactor exit bulk boiling will not occur, and the degree of boiling in the channels will not sufficiently change the core pressure drops to cause instability). The potential for dynamic instability was studied by comparing LOFT operating conditions for the L2-3 test to operating conditions for a reference PWR which had been shown to be dynamically stable. With regard to dynamic instability, LOFT operation for L2-3 compared favorably with the reference PWR. Differences in geometry and operating parameters between LOFT and the PWR generally tend to make LOFT more stable. LOFT's larger pressure drop in the upper vessel structure, and the greater core inlet subcooling in LOFT are exceptions. But, the study shows that the larger pressure drop in the upper structure of LOFT is not sufficient to result in instability. Also, the study shows as long as the core is protected from CHF (DNB), which the above steady-state analysis results demonstrate, then the greater core inlet subcooling in LOFT will not cause instabilities.

2. L2-3 Pre-LOCE Trip Limits - Recommended reactor trippoints for L2-3 LOCE pre-blowdown (Mode 8) operation (Table IV-2) are developed in Appendix A-3 (with exception of Reflood Assist Bypass). These recommended trippoints, are based on the possible operating range of power and flow as based on the core power distribution measured by the TIP system and defined by a $T_{Hot} = 609$ nominal

TABLE IV-2

RECOMMENDED L2-3 PPS TRIP SETPOINTS - MODE 8 (APPENDIX A-3)

	<u>Nominal Trip Setpoint</u>	<u>LSSS</u>	<u>Worst- case Trippoint</u>	<u>Time Constant (s)</u>
High average power (MW) ^(a) 2.3 ≤ P/A ≤ 2.6	Nominal+1.5	Nominal+2	Nominal+3.5	0.25
Peak Power	NA	NA	NA	NA
High Hot Leg Temperature	613	615	617	18
PCS Loop Flow (Mlbm/h) ^(b)	1.25	1.20	1.06	1.0
High Pressure (psia)	2282	2312	2340	1.0
(psig) ^(c)	2269	2299	2327	
Low Pressure (psia)	2058	2028	2000	1.0
(psig) ^(c)	2046	2016	1988	
Reflood Assist Bypass Valve (% open)	5	8	10	0.10

(a) Nominal power = 90.23/(P/A)

(b) The recommended flow trippoints for the L2-3 evolution are based on the following equations (for Mlbm/h) with P/A = 2.60 and core ΔH = 86.31 Btu/lbm:

$$\text{Nominal flow} = 3.568/(P/A)$$

$$\text{Nominal frip} = 3.247/(P/A)$$

$$\text{LSS} = [3.247/(P/A)] - 0.05$$

$$\text{WCT} = [3.211/(P/A)] - 0.175$$

(c) For conservatism, the high pressure channel uses 1 atm = 13 psia, and the low pressure channel uses 1 atm = 12 psia.

resulting from the transient safety analysis. Trippoints were established by considering the required operating bands, PPS trip accuracies, trip setpoint allowances, and drift allowances, and adding or subtracting these as required to obtain the trippoints. The trippoints in Appendix A-3 provide operational margin and still satisfy the trip requirements of the L2-3 transient analyses (for Mode 8).

The steady-state and accident analyses for L2-3 LOCE pre-blowdown (pre-LOCE) operation were done with assumed worst-case trippoints (WCT) equal to or more severe (further away from nominal) than those in Appendix A-3. These analyses, discussed in Sections IV.1 and V of this report, show that LOFT can be operated safely (the applicable damage criteria are satisfied) with the worst-case trippoints and trip instrumentation time constants listed in Table IV-2.

All loss of flow accident analyses used a loop low flow WCT equal to or less than 1.01 Mlbm/h. It has been shown that this low flow trip can be single valued even though the nominal and off-nominal loop flows must vary with measured P/A.

Although the safety analysis showed that the power trip margins from nominal may be increased for lower values of measured P/A, the final recommended power trips in Appendix A-3 is a WCT = nominal + 3.5 MW.

The hot leg temperature trippoints are limited by the 613°F limit established for high off-nominal T_{Hot} for the transient analyses. A 18 s hot leg temperature trip time constant is required for satisfactory mitigation of slow control rod withdrawal accidents (CRWA) from less than nominal power. These transients are tripped by high hot leg temperature.

The high pressure WCT of 2340 psia is required for satisfactory consequences of the CRWA's and the loss of steam load accident (which is tripped on high pressure).

The low pressure trip is only actuated for a steamline break accident, pressure control failure, or a control rod drop accident. As the

steamline break accident is worsened by an earlier reactor trip, and the results of an analysis with an instant trip signal were used for the cooldown analysis, then the analysis does not define a limit on the low pressure trip. The analysis for a failure of the automatic pressure control system, an accident which would have actuated a low pressure trip, is shown to result in satisfactory results down to pressures significantly less than 2000 psia. The consequences of the control rod drop accident (Section V.3.3) were shown to be acceptable with a 2000 psia WCT. The consequences may still be acceptable without a trip (as they were for the L2-2 analysis^[37]). However, a low pressure trip is still desired to limit the period of operation with an adverse perturbation of the power distribution in the core.

The worst-case trippoint assumed for opening of the reflood assist bypass valves (RABVs) was 10% of the full open area, the same as used for power range testing analyses^[18]. The RABV opening accident analysis, discussed in Section V.3.11, determined that a 10% open trip was acceptable (Damage Criteria A was satisfied). Therefore, the RABV open worst-case trippoint and the associated RABV trip setpoints remain the same as for power range testing operation. A 0.10 s time constant, as assumed for power range testing safety analyses^[23], was allowed for by adding a 0.10 s scram delay to the hybrid simulation.

The peak power trip is not applicable to L2-3 pre-LOCE operation since CRA's are constrained to 50 to 58 inches withdrawn, a range in which the peak power trip is not effective because the high power trip will provide earlier protection (will trip the reactor at a lower power).

The minimum required trip allowances for instrument accuracy, drift, control bands, etc., are discussed in Appendix A-3. Using the allowances in Appendix A-3, and applying them to the worst-case trippoints discussed above, the trippoints in Table IV-2 are established for L2-3 Mode 8 operation. The low flow trip setpoints (for a worst-case trippoint of 1.06 Mlbm/h) are those associated with a P/A of 2.60 using the relationships for flow and flow trips as a function of P/A given in Table IV-2. The values are in

units of 10^6 lbm/h (or Mlbm/h) of loop flow. These relationships were based on a nominal T_{hot} of 609°F, 2169 psia pressure, and on L2-3 core ΔH of 86.31 BTU/lbm for a specified test differential temperature of 64.5°F.

3. Reactivity Control Limits - The requirement for PCS boron concentration (minimum concentration limit) for reactivity control for L2-3 LOCE Pre-blowdown (Mode 8) operation is the same as the requirement for operation in Modes 6 and 7, that is a minimum boron concentration limit of 100 ppm (for shutdown margin considerations). This minimum boron concentration limit for Mode 8 is based on an extension of the cooldown reactivity analysis performed for Modes 6 and 7^[34] (see Section IV.4). The analysis^[34] assumed a minimum PCS boron concentration of 100 ppm in order to maximize the positive reactivity insertion due to moderator reactivity feedback during a moderator cooldown. A boron concentration of less than 100 ppm could result in greater positive reactivity insertions than used in the cooldown analysis, and thus, the requirement for PCS boron concentrations of greater than or equal to 100 ppm is established for Mode 8 operation (Section II establishes a higher value).

The required shutdown reactivity to prevent criticality during a steam line rupture accident or single rod ejection accident while in Modes 2 through 4^[34] is not affected by Mode 8 operation and must be adhered to for operations in Modes 2 through 4. In Section IV.1, it is shown that the Mode 4 shutdown reactivity requirement is satisfied for the nonreactivity accident entries from Mode 8 for CRA positions greater than 50 inches withdrawn.

For Mode 8 operation, the control rod assembly bank position is limited to positions between 50 and 58-inches withdrawn. The control rod position limits stem from the following sources:

- A. Prevention of radical rod motions which could result in core power distributions outside the bounds assumed for the Mode 8 safety analyses.
- B. Ensuring adequate scram shutdown reactivity to prevent re-criticality in the event of a steam line rupture accident.

- C. Ensuring that the scram reactivity insertion rate used for the Mode 8 safety analyses is a limiting value.
- D. Ensuring that the maximum positive reactivity insertion rate assumed for the Mode 8 control rod withdrawal accident safety analyses is a bounding value.
- E. Ensuring that the maximum positive reactivity insertion assumed for the Mode 8 single control rod ejection accident is a bounding value.

An additional consideration is limiting the effects that Mode 8 operations can have on the safety analyses previously performed for other reactor operational modes, specifically Modes 6 and 7. This is an especially important consideration because recovery from scrams while in Mode 8 will be via Modes 6 and 7, not Mode 8. Discussions in Section IV.4 will address re-entry into Modes 6 and 7 following Mode 8 operation. That the Mode 8 control rod assembly position limits satisfy each of the five items described above is demonstrated in the discussions that follow.

4. L2-3 Mode 8 Reactivity Considerations

4.1 Shutdown Reactivity Considerations - The cooldown reactivity analyses for Modes 6 and 7^[34] were extended to Mode 8 operations to demonstrate that the lower limit of control rod assembly position (50 inches withdrawn) provides sufficient shutdown reactivity after scram to:

- A. Prevent recriticality during a steam line rupture accident with coincident equipment failure resulting in a single stuck rod, and
- B. Satisfy the Mode 4 shutdown reactivity requirement for planned or inadvertent (nonaccident) entries into Mode 4 via scram from Mode 8.

Two cooldown analyses using data and techniques developed in Reference 34 were performed to specify operating limitations that ensure conformance to the above requirements. Initial conditions and results appear in Table IV-3. The high power used corresponds to a P/A of 2.3 thus imposing a lower limit on allowed range of P/A.

A cooldown reactivity analysis was performed to demonstrate that the Mode 4 shutdown reactivity requirements are satisfied for planned or inadvertent (nonaccident) entries into Mode 4 via reactor scram from Mode 8. However, for the Mode 8 analysis rod scram worth was evaluated at a boron concentration of 100 ppm versus the conservative 1500 ppm level^[34]. This was done to remove the extreme conservatism which results when assuming 100 ppm boron for determining moderator reactivity feedback while assuming 1500 ppm boron for determining rod scram worth. Using a boron concentration of 1500 ppm for calculating both scram worth and moderator reactivity feedback would have resulted in greater shutdown margins than those determined by using the 100 ppm boron concentration for both scram worth and moderator reactivity. Results of the cooldown analysis are given in Table IV-3 for a scram and cooldown to 535°F. As can be seen, the shutdown reactivity requirement of Mode 4^[34] (9% $\Delta\rho$ shutdown reactivity) is satisfied for the nonaccident entries from Mode 8 for initial control rod positions equal to or greater than 50 inches withdrawn.

A cooldown reactivity analysis for a Mode 8 steam line rupture was performed to demonstrate non-recriticality for the limiting accident specified in Reference 34. As can be seen from Table IV-3 the initial conditions allowable in Mode 8 by other safety analyses had to be modified to assure non-an upper bound for off nominal high hot leg temperature (T_H) while PCS flow is above 3.0 Mlbm/hr. A conservative bound for off-nominal high T_H is 600°F for flows greater than 3.0 Mlbm/hr. The use of these limitations assure criticality will not occur for a steamline rupture with a resulting 225°F cooldown for Mode 8 operations with control rod positions equal to or greater than 50 inches withdrawn.

4.2 Scram Reactivity Considerations - Appendix A-10 provides 3-rod bank differential worths used in generating the Mode 8 3-rod scram reactivity

TABLE IV-3

SUMMARY OF MODE 8 COOLDOWN REACTIVITY ANALYSIS

	<u>Demonstration of</u>	
	<u>Mode 4 Shutdown Compliance</u>	<u>Non-recriticality from Cooldown</u>
T_H (°F)	613	613
P (MW)	41.7	41.7
$\dot{M}$ (10^6 lbm/h)	4.0	3.0
p (psia)	2100	2100
<u>Cooldown to 535 °F</u>		<u>ΔT Cooldown = 225°F</u>
$\Delta\rho$ Doppler	.97%	.98%
$\Delta\rho$ Moderator	5.12%	8.67%
$\Delta\rho$ Rod Scram	15.24%	9.69%
$\Delta\rho$ Mode 4	9.00%	--
$\Delta\rho$ Shutdown	0.15%	0.04%
<hr/>		
$\Delta\rho$ Doppler =	Reactivity insertion due to cooling fuel to average initial Moderator temperature	
$\Delta\rho$ Moderator =	Reactivity insertion due to cooling moderator and fuel from average initial temperature to final temperature	
$\Delta\rho$ Rod Scram =	Reactivity insertion due to either 3-rod or 4-rod scram, as appropriate, from 50-inches withdrawn	
$\Delta\rho$ Mode 4 =	Required shutdown reactivity for Mode 4	
$\Delta\rho$ Shutdown =	$\Delta\rho$ Rod Scram - $\Delta\rho$ Doppler - $\Delta\rho$ Moderator - $\Delta\rho$ Mode 4	

insertion rate. The control rod differential worth analysis was performed using the LOFT core physics one-dimensional PDQ 07 model (described in Section 3.4 of Reference 18). As reactivity insertion rate is most important in terminating fast transients, initial conditions were chosen to conservatively minimize the initial rate. The conditions accomplishing this are:

- A. Largest power to flow ratio (neglecting radial redistribution effects of at-power operation)
- B. Xenon free condition
- C. Highest core inlet temperature
- D. Maximum control rod withdrawal
- E. Single stuck rod remaining at initial bank position

Table IV.4 presents the 3-rod differential worths based on the above conditions for both Modes 7 and 8. The axial range of the worth data are active control rod tip positions from 34 inches to 60 inches above active fuel bottom. This range is adequate to demonstrate conservative termination of all fast transients. The remainder of the axial rod motion range is then conservatively calculated using "10/26 HF" data developed for Mode 7 analysis^[18]. The technique for performing this operation is detailed in Reference [9]. It should be noted that a blending of data occurs at the 35 inch position but the resulting integral worth is still very conservative.

Rod drop times computed^[18] for a 58-inch withdrawn initial scram position were used to convert the differential rod worth data of Table IV-4 to the scram reactivity worth insertion data shown in Table IV-5. These data, designated the "214 HF" and "213 LF" scram curves, represent conservative scram reactivity insertions for Mode 7 and Mode 8 operations.

Many Mode 8 safety analyses did not use the "213 LF" scram curve. The curve used was the "10/26 LF" curve developed for the L2-2 safety analysis.

TABLE IV-4

DIFFERENTIAL 3-ROD SCRAM WORTHS

Position* (inches)	$d\rho/dh$ (% $\Delta\rho$ /inch)			
	A	B	C	D
61	.0076	.0068	.0141	.0127
59	.0115	.0104	.0209	.0188
57	.0152	.0137	.0266	.0240
55	.0186	.0168	.0312	.0281
53	.0214	.0193	.0342	.0308
51	.0257	.0231	.0392	.0353
49	.0330	.0297	.0490	.0441
47	.0385	.0346	.0550	.0495
45	.0440	.0396	.0605	.0545
43	.0500	.0450	.0662	.0596
41	.0568	.0511	.0726	.0653
39	.0646	.0581	.0799	.0719
37	.0726	.0653	.0870	.0783
35	.0803	.0723	.0931	.0817
33		.1147		.1257
31		.1468		.1562
29		.1735		.1793
27		.1990		.2010
25		.2262		.2262
23		.2515		.2515
21		.2690		.2690
19		.2861		.2861
17		.3341		.3341
15		.3893		.3893
13		.3907		.3907
11		.3400		.3400
9		.2504		.2504

Case A: 3-rod differential worths from Appendix A-10, Mode 8 conditions.

Case B: Final corrected differential worths used to generate scram curve. Conservatism factor of .9 has been applied.

Case C: 3-rod differential worths from Appendix A-10 Mode 7 conditions.

Case D: Same as B except for Mode 7.

* Axial position of poison tip of control rod bank above fuel bottom.

TABLE IV-5

3-ROD SCRAM REACTIVITY WORTH VS. TIME

<u>Time (s)</u>	<u>Position (inches withdrawn)*</u>	<u>Case I</u>	<u>Case II</u>
		<u>Worth %$\Delta\rho$</u>	
0.000	58.000	0.0000	0.0000
0.075	57.858	-0.0023	-0.0012
0.160	57.364	-0.0107	-0.0059
0.245	56.519	-0.0208	-0.0147
0.331	55.330	-0.0526	-0.0292
0.417	53.804	-0.0906	-0.0512
0.503	51.951	-0.1429	-0.0826
0.589	49.783	-0.2106	-0.1251
0.672	47.313	-0.3026	-0.1858
0.753	44.555	-0.4326	-0.2752
0.830	41.525	-0.5974	-0.3951
0.905	38.239	-0.8037	-0.5542
0.977	34.713	-1.0629	-0.7658
1.050	30.966	-1.4180	-1.0806
1.127	27.012	-2.0295	-1.6564
1.211	22.999	-2.8388	-2.4572
1.265	20.676	-3.3985	-3.0170
1.336	18.004	-4.1112	-3.7297
1.403	15.816	-4.7439	-4.3623
1.514	12.858	-5.7870	-5.4054
1.590	11.195	-6.4474	-6.0659
1.710	9.068	-7.2383	-6.8568

Case I - Mode 8 Scram Curve "214 HF"

Case II - Mode 7 Scram Curve "213 LF"

* These data are reported to more significant figures than are physically meaningful to facilitate checking of computer data. For CRA axial position above fuel bottom add 2 inches to given value.

However, accidents most likely to be effected by the less conservative "10/26 LF" were reanalyzed using the "213 LF" curve. The analysis using the "213 LF" scram showed no significant changes in consequences due to the new scram curve. Figure IV-1 displays the relationship of the reactivity insertion rates obtained with the two curves. Also included for comparison purposes is the high flow "817" curve used in the L2-2 safety analysis and the "214 HF" Mode 7 scram curve. All curves must be used with a scram delay time of .05 seconds or greater.

In conclusion, the control rod position limits of 50 to 58-inches withdrawn for Mode 8 operations provide a sufficient reactivity insertion rate to satisfactorily terminate accident transients.

4.3 CRWA Reactivity Insertion Rate Considerations - The control rod position limits of 50 to 58-inches withdrawn for Mode 8 operation result in conditions that ensure the reactivity insertion rate resulting from a continuous CRA withdrawal accident is bounded by the maximum value assumed for the L2-3 pre-LOCE (Mode 8) safety analyses. The maximum insertion rate used for the Mode 8 CRWA safety analyses was $8 \times 10^{-4} \Delta\rho/\text{sec}$ (see Section V.2.2). For a maximum CRA withdrawal rate of 29 inches/min (assuming accident conditions), the limiting differential rod worth is

$$(8 \times 10^{-4} \Delta\rho/\text{sec}) \left(\frac{1 \text{ min}}{29 \text{ inches}} \right) \left(\frac{60 \text{ sec}}{\text{min}} \right) = 0.166\% \Delta\rho/\text{inch}.$$

Calculations of differential rod worths were performed using the one-dimensional PDQ07 model of the LOFT core (see Section IV.4.2) to demonstrate that acceptable differential rod worths would occur even under extreme operating conditions. The case that was analyzed is described in Table IV-6. Results of the analysis are provided in Table IV-7. As can be seen by examination of these data, none of the calculated differential worths exceed the 0.166% $\Delta\rho/\text{inch}$ limit, and, in fact they are well below the limit (0.126% $\Delta\rho/\text{inch}$ is the maximum calculated differential rod worth for rod tip positions of 50 to 66-inches above fuel bottom). Therefore, the maximum reactivity insertion rate used for the Mode 8 CRWA safety analyses is bounding for Mode 8 operation within the 50 to 58-inches withdrawn control rod position limits.

FIGURE IV-1
SCRAM REACTIVITY INSERTION COMPARISONS

TABLE IV-6

SCENARIO FOR DIFFERENTIAL ROD WORTH CALCULATIONS

<u>Scenario</u>	<u>Justification and Comments</u>
Zero-power 7.5 hours after shutdown from 42.7 MW burn to equilibrium xenon with CRA bank 50 inches above fuel bottom, PCS flow rate of 1.0 Mlbm/hr and inlet temperature of 555°F.	This case represents the extreme situation for entry into Modes 6 and 7 after operations in Mode 8. The calculated differential rod worths are maximized for these situations because analysis has shown that maximum differential rods worths occur in the top portion of the core (1) at the time of peak xenon following reactor scram and (2) for recovery at zero-power (isothermal core conditions). [*] Thus, the transition from Mode 7 to Mode 8 without reactor scram (normal Mode 8 entry) will yield differential rod worths high in the core which are lower than calculated for these scenarios. Low flow operation in Mode 8 will reduce rod worth high in the core as compared to high flow operation because of slightly reduced relative power in top portion of the core.

* A complete discussion of these effects is provided by E. C. Anderson letter to W. A. Spencer, "Transient Xenon Effects in the LOFT Reactor," ECA-8-78, September 19, 1978, in Appendix B-3 of Reference [9].

TABLE IV-7

RESULTS OF DIFFERENTIAL ROD WORTH CALCULATIONS FOR CRWA

CRA Bank Position (Inches Withdrawn)	<u>$d\rho/dh$ ($\%\Delta\rho/\text{inch}$)</u>	
	<u>Case A</u>	<u>Case B</u>
34.5	.1195	.1315
37.0	.1099	.1209
39.0	.1059	.1165
41.0	.1025	.1128
43.0	.1015	.1117
45.0	.1025	.1128
47.0	.1050	.1155
49.0	.1076	.1184
51.0	.0984	.1082
53.7	.1039	.1143
56.0	.1120	.1232
58.0	.1148	.1263
60.0	.1108	.1219
62.0	.0958	.1054
64.5	.0652	.0717

Note: See Table IV-6 for description of case.

Case A: Data from Appendix A-10

Case B: Case A increased by 10% conservatism.

The scenario analyzed considered recovery in Modes 6 and 7 from a scram while in Mode 8. The maximum reactivity insertion rate considered from a CRWA in Modes 6 and 7 was $0.207\% \Delta\rho/\text{inch}$ ^[18]. Because the calculated differential rod worths shown in Table IV-7 are below this limiting value, there is no limitation due to reactivity insertion rate considerations on re-entry to Modes 6 or 7 following a scram from Mode 8.

4.4 Rod Ejection Accident Reactivity Considerations - The control rod position limits of 50 to 58-inches withdrawn for Mode 8 operation result in conditions that ensure the reactivity insertion resulting from a single control rod ejection accident is bounded by the maximum value assumed for the L2-3 pre-LOCE (Mode 8) safety analyses. The maximum reactivity insertion used for the Mode 8 rod ejection safety analyses was $0.363\% \Delta\rho$ (see Section V.4.1).

For Modes 6 and 7 operations, the maximum reactivity insertion for a single rod ejection accident from an initial rod position of greater than or equal to 50-inches withdrawn was determined to be $0.351\% \Delta\rho^*$. Based on the results of the differential rod worth calculations presented in Section IV.4.3, the maximum reactivity insertion for a single rod ejection accident in Mode 8 would be equal or less than the value calculated for Modes 6 and 7. This is because the differential rod worths high in the core for Mode 8 would be equal to or less than the value calculated for Modes 6 and 7. Therefore, the reactivity insertion for a single rod ejection accident in Mode 8 is bounded by $0.363\% \Delta\rho$ reactivity.

Appendix A-10 presents the calculations of maximum single rod eject worth. The value calculated for the Mode 8 conditions following the Xenon scenario of Table IV-6 is $.225\% \Delta\rho$. This value is bounded by the value used in the analysis. Therefore, the reactivity insertions assumed for the single rod ejection accident analyses for Modes 6 and 7 are still limiting values and there is no limitation due to reactivity insertion considerations on re-entry to Modes 6 and 7 following a scram from Mode 8.

*R. A. Dimenna ltr to E. C. Anderson, "38 MW Operation of the LOFT Reactor", RAD-8-78, (September 25, 1978)

4.5 Xenon Effects - Under certain circumstances, xenon can significantly affect core power distribution which in turn can affect core thermal margin and differential control rod worth. In order to quantify the effects of xenon on core thermal margin (an evaluation of the effects of xenon on differential control rod worths was given in Section IV.4.3) for Mode 8 operation and for Modes 6 and 7 operations following operation in Mode 8, analyses have been performed for equilibrium and transient xenon conditions. The set of calculations performed were the result of a judicious selection of operational scenarios based upon the experience derived from performing xenon studies for the power range safety analyses.

The dominant influence in the approach to the limiting core thermal margin is a peak xenon distribution low in the core forcing a high peak power condition in the middle to upper core regions. The initial condition is best produced by operation at highest allowable power levels at lowest allowable rod position. The final condition is best satisfied by a restart after reactor scram, in which the xenon distribution is allowed to peak, using in general the highest allowable rod position with an appropriate power level (with zero-power producing the highest relative power peak in both magnitude and axial position).

For current considerations, there are two operational regimes; High Flow (Modes 6 and 7), and Low Flow (Mode 8). Using these two regimes, there are three allowable operational changes:

- A. Low Flow operation, scram, restart with High Flow operation.
- B. High Flow operation, maneuver to Low Flow operation.
- C. Low Flow operation, maneuver to High Flow operation.

Of the above operational changes, the scram case where peak xenon is allowed to build in is more limiting, and the maneuvering cases (B and C above) may be excluded from consideration.

The initial conditions and event sequence that produces the most limiting power distribution is identical to those that produced the most conservative control rod differential worth for CRWA analysis. See Table IV-6 for the conditions and sequence of events.

The axial power distributions produced by each of the neutron flux calculations involved in generating the differential rod worths, were examined to choose the most limiting distributions. The calculation with an axial rod height of 61 inches above fuel bottom was found to be most limiting. Figure IV-2 presents the axial distribution of relative power density* just prior to reactor scram and at time of zero power start up.

Using the results of the core power distribution analysis discussed above, an analysis of core thermal margins was performed using COBRA IIIC with the 1/8-th core model to determine the effects of the xenon perturbed axial power distribution on core thermal performance. The results of the core thermal analysis showed for steady-state operation at off-nominal Mode 7 conditions but with power restricted to 46 MW or less the MDNBR is greater than obtained with the beginning of life and xenon perturbed axial profile used for the Mode 6 and 7 safety analyses. The calculation assumed a radial peaking of 1.6. Results of this analysis showed that an operational restriction on power (46 MW nominal) due to xenon perturbation is required for re-entry into Modes 6 and 7, following reactor operations in Mode 8. See Section VI.3 for discussion of this analysis.

*A one-dimensional calculation produces an average pin profile. Radial peaking factors are assumed unchanged in the thermal calculations.

V. L2-2 MODE 8 ACCIDENT STUDIES

1. Introduction - The accidents studied for L2-3 Mode 8 (LOCE pre-blowdown) operation and discussed below include those accidents discussed in Section 15 of the LOFT FSAR^[3] which affect the fuel thermal performance, with the exception of LOCA's and accidents during the LOCE blowdown (which is Mode 9). These exceptions are covered by the scoping analyses discussed in Reference 1 or by the analysis in the FSAR^[3]. The potential for non-LOCA accidents is no different during steady-state LOCE pre-blowdown (pre-LOCE) operation than for the normal power and flow operation studied and reported upon in the FSAR^[3] and LTR 111-104^[18]. A unique operation, however, is the potential for accidents during the low flow maneuver, in which loop flow is lowered to pre-LOCE conditions from normal high flow values. During the low flow maneuver, the low flow trippoint will have been lowered to the pre-LOCE (Mode 8) trip level, and flow will be lowered (probably in two steps) while the core primary coolant temperature is adjusted upward (in three steps). Other than the low flow maneuver, the primary difference and consideration for accident analyses between studies for the FSAR and those for pre-LOCE operation is the influence on the consequences of the accidents of operation at low flow (loop flow about one-third of normal full flow).

With the low flow trip reduced to a Mode 8 trip level prior to the maneuver, it is possible to experience a loss of flow accident with the low flow trip level reduced significantly farther from the initial flow than was analyzed for the FSAR. Loss of flow accidents from normal full flow with a Mode 8 low flow trip were analyzed for the L2-2 Mode 8 safety analysis and the results compared to those for the same accidents initiated from Mode 8 low flow conditions. The comparison showed that the MDNBR was significantly less for the low flow accidents initiated from Mode 8 low flow conditions^[37]. Therefore, these accidents were not analyzed again for L2-3 operation.

Experience with the L2-2 low flow and pre-LOCE maneuvers showed that the following conditions occur during these maneuvers:

- A. As flow is reduced from normal full flow values, the reactor power decreases to less than the nominal test power.

- B. The maximum measured peaking (P/A) occurs shortly after flow has been reduced, while power is below nominal.
- C. Shortly after flow has been reduced, when peaking is a maximum and power is below nominal, the hot leg temperature is raised to its final test conditions.
- D. Shortly before the blowdown is initiated, power is adjusted up to the test nominal based on a final measured P/A (flow was not adjusted for L2-2, but it may be for L2-3).
- E. When the blowdown loop isolation valves are opened just prior to blowdown, pressure decreases due to leakage into the blowdown loop. Therefore, pressure is first raised above nominal.

The L2-2 maneuver experience shows that the initial conditions for an accident during Mode 8 operation are likely to be with:

- A. Reactor power below nominal, therefore a larger than nominal margin to trip exists.
- B. Full test-specified hot leg temperature.
- C. Low Mode 8 loop flow.
- D. Peaking slightly above nominal (for L2-2 maximum peaking was only 0.4% above nominal).
- E. Prior to blowdown initiation, the pressure may range from high-off nominal to low off-nominal.

The above likely initial conditions are also the worst initial conditions for initiation of limiting Mode 8 transients (power increase accidents). Therefore, the L2-3 transient analyses were analyzed for initial conditions within the

range indicated in Table V-1, with the actual initial conditions for specific transients chosen such that the time to reactor scram is a maximum.

The worst-case trippoints assumed for the transient (or accident) analyses are discussed in Section III.2. The power trippoints are discussed further for the control rod withdrawal accidents. Assumed conditions or trips specific to certain accidents are discussed in the sections for those accidents. See the preceding Section III. for more discussion of analysis assumptions and models. The results of the L2-3 Mode 8 transient analyses are discussed in the following sections and in Appendix B-4.

2. Boron Dilution Accident and Operational Transients - The reactivity insertion rate due to a boron dilution accident is considered to be $0.67 \times 10^{-4} \Delta\rho/s$ ^[3]. This insertion rate is bounded by the reactivity assumption for the CRWA analysis in Section V.3.2.

The operational transients considered for the L2-3 Mode 8 conditions are boron dilution (6 gpm) and normal rod motion (2 inches per minute). The normal 6 gpm dilution rate (FSAR dilution accident used 27.5 gpm) for boron dilution gives a maximum insertion rate less than $0.1 \times 10^{-4} \Delta\rho/s$. A 2-inch per minute control rod withdrawal (assuming maximum worth of 0.126% $\Delta\rho/in.$) gives a maximum reactivity insertion rate of $0.4 \times 10^{-4} \Delta\rho/s$.

The reactivity insertion rates associated with the above operational transients are bounded by the analysis performed for the CRWA analysis and the resulting MDNBR's satisfy Damage Criteria A as required for operational transients. Therefore, no constraints on normal operation are imposed for Mode 8.

3. Incidents of Moderate Frequency

3.1 Startup Accident - A startup accident, that is a continuous withdrawal of the CRA's from initial criticality, was not analysed for L2-3 pre-LOCE operation. Initial criticality and startup with low loop flow is not a part of pre-LOCE operation. That is, startup would only occur under Mode 6.

TABLE V-1

INITIAL CONDITIONS FOR L2-3 TRANSIENT ANALYSISInitial Conditions

	<u>Minimum</u>	<u>Maximum</u>
Measured Peaking (P/A)	2.30	2.60
Reactor Power (MW)	30	37.2
PCS Loop Flow (Mlbm/h)	1.15	
Hot Leg Temperature (°F)		613 ^(a)
PCS Pressure (psia)	2081	2257

(a) Transient analyses were done for high off-nominal hot leg temperatures from 606°F to 621°F. The final limiting analyses were done for 613°F.

operating conditions, not Mode 8. Also, it appeared that the damage criteria could not be met for a power transient with CRA's low in the core as they could be during a startup.

With the restriction that criticality and startup must occur in Mode 6 (with full flow), then the startup accident is enveloped by the analysis in the FSAR^[3].

3.2 Control Rod Withdrawal Accident (CRWA) - The CRWA analysis simulates a continuous withdrawal of all CRA's from a control rod position (CRP) of 50 inches or above. The hybrid computer calculations for this accident assume a constant ramp rate of reactivity addition, independent of CRP, to scope the possible real behavior of a CRWA.

FSAR analyses for the CRWA^[3] were done for high reactivity ramp rates ($10 \times 10^{-4} \Delta\rho/s$) with low moderator and Doppler feedback coefficients ($2 \times 10^{-3} \Delta\rho/lbm/ft^3$ and $-0.3 \times 10^{-5} \Delta\rho/^\circ F$), and low reactivity ramp rates ($1 \times 10^{-4} \Delta\rho/s$) with high moderator and Doppler feedback coefficients ($9.1 \times 10^{-3} \Delta\rho/lbm/ft^3$ and $-1.5 \times 10^{-5} \Delta\rho/^\circ F$). Analyses were done for CRWA's from high off-nominal power, from intermediate power (15 MW), and from a low power (4.8 MW). A high ramp rate with low feedback coefficients results in a maximum peak power, as the reactivity addition is high, and the feedback effects which tend to lower the transient power are minimized. However, a low ramp rate with high feedback coefficients results in a slow power rise which adds a maximum amount of energy into the PCS before a high power or high hot leg temperature trip finally terminates the power rise. A CRWA from low power can be worse due to the large separation between initial conditions and the trippoints, which allows for more reactivity and energy addition prior to trip. The FSAR analysis showed, for a 53 MW off-nominal power, a 55 MW worst-case high power trip, and a 609°F high hot leg temperature trip (with a 12 s time constant), that the worst CRWA (lowest MDNBR) was for a $1 \times 10^{-4} \Delta\rho/s$ ramp rate, high moderator and Doppler coefficients, and initiated from low power^[21].

The CRWA analyses for the L2-2 test^[37] determined that Damage Criteria A was satisfied (the L2-2 CRWA MDNBR is ≥ 1.20) for the analysis assumptions and conditions tabulated in Table V-2. The L2-2 analysis determined:

TABLE V-2
LIMITING CONDITIONS FOR L2-2 CRWA ANALYSES^[37]

	Off-nominal <u>Minimum</u>	Off-nominal <u>Maximum</u>	Worst-case <u>Trip</u>
Measured P/A		2.78	
Reactor Power (MW)		24.1	30
For 602°F T _{hot}	15		
For 568°F T _{hot}	10		
For 554°F T _{hot}	4.8		
PCS Loop Flow (Mlbm/h)	1.1		
Hot leg Temperature (°F)		602	609
Hot Leg Trip Time Constant (s)		40	
PCS Pressure (psia)		2350	
Moderator Coefficient (Δρ/lbm/ft ³)	2 x 10 ⁻³	9.1 x 10 ⁻³	
Doppler Coefficient (Δρ/°F)	-0.3 x 10 ⁻⁵	-1.5 x 10 ⁻⁵	

- A. MDNBR was always less for maximum P/A.
- B. Limiting MDNBR's were obtained for CRWA's from low initial power, for a low reactivity ramp rate ($1.0 \times 10^{-4} \Delta\rho/s$), with minimum moderator and Doppler feedback.
- C. For CRWA's with maximum moderator and Doppler feedback, the lowest MDNBR is obtained for a $4.0 \times 10^{-4} \Delta\rho/s$ reactivity ramp rate.

These L2-2 analysis results indicated that L2-3 CRWA's with 50% higher fuel powers could probably not be shown to satisfy the Damage Criteria unless changes were made in the assumed conditions which would tend to increase the safety margin. Therefore, a parametric study was done to determine what enveloping conditions are required in order for the consequences of a CRWA to satisfy Damage Criteria B (a peak cladding surface temperature, or PCST, of 1650 °F). The parametric study (which included steady-state analyses, see Section IV.1) determined that the following limits on operational conditions were required:

- A. Maximum measured peaking limited to ≤ 2.60 (see Section III.2).
- B. Minimum initial power ≥ 30 MW for $P/A = 2.60$, or 4.5 MW less than nominal power.
- C. High worst-case pressure trippoint ≤ 2340 psia.
- D. Hot leg temperature (high off-nominal) $\leq 613^\circ\text{F}$.
- E. Hot leg temperature worst-case trippoint $\leq 617^\circ\text{F}$ (8°F above nominal).
- F. Hot leg temperature trip time constant ≤ 18 s.
- G. Moderator and Doppler reactivity feedback coefficients within:

	Moderator ($\Delta\rho$ /lbm/ft ³)	Doppler ($\Delta\rho$ /°F)
Maximum	6.5×10^{-3}	-1.08×10^{-5}
Minimum	2.6×10^{-3}	-0.44×10^{-5}

- H. Worst-case power trip 3.8 MW above nominal for P/A = 2.60 (38.5 MW trip power).

All of the above restrictions on operating conditions or analysis assumptions were required for the consequences of the CRWA's to satisfy the Damage Criteria. Table V-3 tabulates the results of the final CRWA analyses within the above bounds.

The limiting CRWA analyses were done assuming that the initial power and initial pressure were such that the power and pressure were at their respective worst-case trip levels when the reactor is tripped by the slower hot leg temperature trip. These initial powers and pressures are listed in Table V-3. As seen in the Table, the initial power is then generally below nominal (except for the slow 0.1×10^{-4} $\Delta\rho$ /s ramp rate), and initial pressures are between nominal and high off-nominal values.

Results of the CRWA parametric study are tabulated in Tables V-4-A, B, and C.* The CRWA parametric study resulted in the following conclusions:

- A. The worst CRWA (with lowest MDNBR) was for a 0.7×10^{-4} $\Delta\rho$ /s reactivity ramp rate with low moderator and Doppler feedback coefficients. However, the difference in MDNBR was small for ramp rates from 0.4×10^{-4} $\Delta\rho$ /s to 1.0×10^{-4} $\Delta\rho$ /s. (See Table V-4-A.)

* It is important to note the COBRA spacer grid error is present in these calculations. Therefore while the MDNBR's appear in most cases not to meet Damage Criteria A, they in fact do. The data is valid for drawing conclusions about general trends.

TABLE J-3

FINAL LIMITING CRWA ANALYSIS RESULTS FOR L2-3 P/A = 2.60

<u>Case I.D.</u>	<u>Ramp Rate $10^{-4} \Delta\rho/s$</u>	<u>Initial Power MW</u>	<u>Initial Pressure psia</u>	<u>MDNBR</u>	<u>Time s</u>	<u>T_{Hot} °F</u>
CRW 91 D	0.1	36.5	2231	1.162	82	618.2
CRW 621E	0.5	33.7	2220	1.153	34.4	620.7
CRW 85 B	0.7	32.5	2209	1.150	30.2	621.8
CRW 90 A*	2.0	31.7	2209	1.154	26.2	622.3
CRW 106**	0.7	32.5	2194	1.150	31.2	622.2

* This case was the limiting case with high moderator and Doppler coefficients.
All of the other final cases were with low moderator and Doppler coefficients.

** This case differed from the rest in that a time constant of 18 s was assumed for the hot leg temperature trip. All other final analysis assumed a 15 s T_{Hot} trip time constant.

TABLE V-4-A

L2-3 CRWA PARAMETRIC STUDY RESULTS^(a) EFFECT OF RAMP RATE (P/A = 2.60)

Case I.D. CRW	Ramp Rate $10^{-4} \Delta \rho / s^{(d)}$	Initial Power MW	Initial T_{Hot} °F	MDNBR	Trip ^(b) Power MW	Pressure @ MDNBR psia	$F_E^{(c)}$
91C	0.1	36.2	613	1.131	38.2	2340	1.026
621B	0.5	33.4	613	1.125	38.2	2340	1.026
85A	0.7	32.2	613	1.120	38.2	2340	1.026
62B	0.3	35.2	613	1.087	38.7	2373	1.032
62C	0.4	34.5	613	1.080	38.7	2375	1.032
621	0.5	33.9	613	1.080	38.7	2379	1.032
85	0.7	32.5	613	1.086	38.5	2393	1.026
87	1.0	30.9	613	1.092	38.5	2403	1.026
72A	1.5	33.3	613	1.086	38.7	2384	1.032
"	"	"	"	1.098 ^(e)	38.5	"	1.026
90	2.0	31.7	613	1.098	38.5	2396	1.026
55	4.0	32.0	617	1.136	39.0	2333	1.032
56	6.0	32.0	621	1.158	39.0	2290	1.032
57	8.0	32.0	621	1.237	39.0	2261	1.032

TABLE V-4-B

L2-3 CRWA PARAMETRIC STUDY RESULTS^(a)
EFFECT OF MODERATOR AND DOPPLER COEFFICIENTS (P/A = 2.60)

Case I.D. CRW	Ramp Rate $10^{-4} \Delta p/s$ ^(d)	Mod and DOP	Initial Power MW	Initial T_{Hot} °F	MDNBR	Trip ^(b) Power MW	Pressure @ MDNBR psia	F_E ^(c)
621A	0.5	Low	33.4	609	1.109 1.107 ^(e)	38.2 38.5	2379 2372	1.032 1.026
84	0.5	High	35.6	609	1.119	38.5	2372	1.026
85	0.7	Low	32.5	613	1.086	38.5	2393	1.026
86	0.7	High	35.2	613	1.109	38.5	2369	1.026
87	1.0	Low	30.9	613	1.092	38.5	2403	1.026
71A	1.0	High	34.5	613	1.084	38.7	2375	1.032
"	"	"	"	"	1.096 ^(e)	38.5	"	1.026
88	1.5	Low	30.0 ^(f)	613	1.138	38.5	2386	1.026
72A	1.5	High	33.3	613	1.086	38.7	2384	1.032
"	"	"	"	"	1.098 ^(e)	38.5	"	1.026
89	2.0	Low	30.0 ^(f)	613	1.184	38.5	2355	1.026
90	2.0	High	31.7	613	1.098	38.5	2396	1.026

TABLE V-4-C

L2-3 CRWA PARAMETRIC STUDY RESULTS^(a)
EFFECT OF P/A AND POWER TRIP

Case I.D. CRW	P/A	Ramp Rate $10^{-4} \Delta p/s$ ^(d)	Initial Power MW	Initial T_{Hot} °F	MDNBR	Trip ^(b) Power MW	Pressure @ MDNBR psia	F_E ^(c)
95	2.50	0.1	38.4	609	1.196	40.3 ⁽ⁱ⁾	2369	1.026
91B	2.60	0.1	36.2	609	1.140	38.2 ^(g)	2370	1.026
104	2.55	0.1	37.3	613	1.143	39.2 ^(h)	2340	1.026
91C	2.60	0.1	36.2	613	1.131	38.2 ^(g)	2340	1.026
67A	2.50	0.5	35.6	613	1.110	40.3 ⁽ⁱ⁾	2379	1.032
621	2.60	0.5	33.9	613	1.080	38.7 ⁽ⁱ⁾	2379	1.032
103	2.55	0.5	34.2	613	1.120	39.2 ^(h)	2340	1.026
103A	2.55	0.5	33.9	613	1.133	38.9 ^(g)	2340	1.026
621B	2.60	0.5	33.4	613	1.125	38.2 ^(g)	2340	1.026

TABLE V-4 Notes

- (a) These calculations were done with COBRA IIIC using the one-eighth LOFT core model, with the conservative error in the spacer grid loss coefficient equation (see discussion in Section III.3).
- (b) The CRWA's with $< 4 \times 10^{-4} \Delta\rho/s$ ramp rate tripped on hot leg temperature with power just at the worst-case trip level.
- (c) Engineering hot spot factor in COBRA.
- (d) Low moderator and Doppler coefficients assumed for ramp rates $\leq 1.0 \times 10^{-4} \Delta\rho/s$.
High moderator and Doppler coefficients assumed for ramp rates $\geq 1.5 \times 10^{-4} \Delta\rho/s$.
- (e) An estimate.
- (f) Minimum starting power assumed was 30 MW. This case trips on high power.
- (g) A worst-case trip power 3.5 MW above nominal.
- (h) A worst-case trip power 3.8 MW above nominal.
- (i) A worst-case trip power 4.2 MW above nominal.

- B. The MDNBR is less for lower assumed values for the moderator and Doppler feedback coefficients for reactivity ramp rates $\leq 1.0 \times 10^{-4} \Delta\rho/s$. The MDNBR is less for high assumed values for the moderator and Doppler feedback coefficients for reactivity ramp rates $\geq 1.5 \times 10^{-4} \Delta\rho/s$. (See Table V-4-B).
- C. MDNBR is less for the maximum peaking cases (with $P/A = 2.60$), even when the margin to the power trip is greater for the lower peaking cases. Therefore, safety analyses can be based on the $P/A = 2.60$ cases. (See Table V-4-C).

As a result of the CRWA analyses for $P/A = 2.60$, as tabulated in Table V-3, and the comparative calculation results tabulated in Table V-4-C for different assumed P/A , it is concluded that the L2-3 Mode 8 CRWA's can satisfy Damage Criteria A ($MDNBR \geq 1.14$) for all measured peaking ≤ 2.60 with the worst-case high power trip 3.8 MW above nominal power for $P/A > 2.50$, and 4.2 MW above nominal power for $P/A \leq 2.50$. This conclusion is, of course, based on the previously stated list of operational conditions and analysis assumptions.

Most of the final CRWA analyses assumed a time constant of 15 s for the hot leg temperature trip. Measurements for this time constant, with the recommended improvements in the thermal wells, indicated that the time constant may be between 16 and 17 s. Therefore the limiting CRWA (a $0.7 \times 10^{-4} \Delta\rho/s$ ramp rate) was reanalyzed with an 18 s T_{Hot} trip time constant. This analysis (case CRWA 106 in Table V-3) showed that the resulting MDNBR was not significantly different ($MDNBR = 1.15$). This is to be expected since the only significant difference in this transient and the case with a 15 s time constant (CRW 85B) is a $0.4^\circ F$ increase in the hot leg temperature at the time of MDNBR. Therefore, the consequences of a CRWA are satisfactory with an 18 s T_{Hot} trip time constant.

An analysis was done to see if, for the final analysis conditions, the operating pressure band could be increased to include a high pressure worst-case trip level of 2400 psia. The calculation for a $0.7 \times 10^{-4} \Delta\rho/s$ ramp rate (the worst ramp rate), $P/A = 2.60$, a power trip of 38.5 MW (3.8 MW above nominal power), and 2400 psia peak pressure resulted in a MDNBR of 1.120. Therefore, a 2340 psia high pressure worst-case trippoint is required.

The maximum ramp rate for a CRWA for L2-2 operation was calculated as $0.141\% \Delta\rho/\text{inch}$ ^[8]. For L2-3 Mode 8 operation, the maximum differential CRA worth (4 CRA bank withdrawal) is $0.126\% \Delta\rho/\text{inch}$ (see Section IV.4.3). For a maximum withdrawal speed of 29 inches/minute and a $0.141\% \Delta\rho/\text{inch}$ differential worth, the maximum reactivity ramp rate for a CRWA would be:

$$1.41 \times 10^{-3} \Delta\rho/\text{inch} \frac{1}{60} \frac{\text{min}}{\text{s}} 29 \frac{\text{in.}}{\text{min}} = 6.8 \times 10^{-4} \Delta\rho/\text{s}$$

Therefore, the $8 \times 10^{-4} \Delta\rho/\text{s}$ CRWA analyzed for L2-3 operation envelopes the maximum CRWA ramp rate. Conversely, the $8 \times 10^{-4} \Delta\rho/\text{s}$ CRWA envelopes 4-CRA bank withdrawal differential worths up to $0.166\% \Delta\rho/\text{inch}$.

Since these CRWA's (for a ramp rate $\geq 0.1 \times 10^{-4} \Delta\rho/\text{s}$) are Incidents of Moderate Frequency, then for Mode 8 operation, it is only required to meet Damage Criteria B (PCST $\leq 1650^\circ\text{F}$) rather than Damage Criteria A (MDNBR ≥ 1.14). This means it would be acceptable to predict a possibility of exceeding CHF in the core as long as the peak clad temperature limit is not exceeded. However, in a analysis for a slow CRWA with a MDNBR of 1.137, PCST was predicted to be 1747°F . This high temperature results since the hot spot remains in film boiling for more than 2 seconds after being forced into film boiling when MDNBR = 1.14, even though the reactor was scrammed 0.3 s after MDNBR = 1.14. The high initial fuel temperature due to the conservative FRAP model assumptions (see Section III.3) results in a rapid increase in clad temperature when the surface is forced into film boiling. A PCST of 1650°F is reached in about 1 second. Therefore, there is no significant difference between the consequence limits of Damage Criteria A and Damage Criteria B for slow power rise accidents. This conclusion was also shown for a FRAP T4 calculation for a loss of steam load accident (which is also a slow power increase transient). The loss of steam load case for which the FRAP calculation was done had the following characteristics and results:

Time when MDNBR < 1.14	13.51 s
MDNBR	1.131
Time of MDNBR	13.8 s
PCST	1804°F
Time of PCST	15.3 s

See Section III.3 for a discussion of the LOFT FRAPT fuel pin model.

3.3 Control Rod Drop Accident - A low-worth control rod assembly (CRA), when dropped, can reduce power to a low value. The drop in power cooled with a continued high steam flow can result in a cooldown and a subsequent power recovery. The analysis for this accident seeks to determine the extent and the consequences of the power recovery.

The FSAR analysis for this accident (Section 15.2.3^[3]) assumed a worth of $-2.3\% \Delta\rho$ for the dropped CRA. For the L2-2 pre-LOCE analysis^[37], two worths were used for the hybrid calculation: (1) $-2.2\% \Delta\rho$, and (2) $-3.17\% \Delta\rho$. The L2-2 analysis demonstrated that the drop of the low worth CRA results in a greater power recovery and less pressure decrease.

The hybrid computer analysis for a dropped CRA for L2-3 Mode 8 operation assumed a worth of $-2.1\% \Delta\rho$. High moderator and Doppler feedback was assumed to maximize the power recovery. The hybrid computer results for this accident are:

Initial Power	37.3 MW
Minimum Power	10.1 MW
Time of Minimum	2.05 s
Max Power Recovery	18.3 MW (49% of initial)
Time of Max Recovery	25.6 s
Initial Loop Flow	1.15 Mlbm/h
Loop Flow After Recovery	1.24 Mlbm/h (108% of initial)
Initial T_{hot}	617°F
T_{hot} After Recovery	571°F (46°F less than initial)
Initial Inlet Temperature	539°F
Inlet Temperature After Recovery	532°F (7°F less than initial)
Initial T_{ave}	578°F
T_{ave} After Recovery	551.5°F (26.5°F less than initial)

The transient was terminated by a 2000 psia worst-case low pressure trip at 25.5 seconds.

The dropped control rod causes a radial flux tilt in the core which results in an increased core peak-to-average power ratio and increased integrated rod power for the hot rod. Based on the results of the power distribution calculations^[38], the core power peaking and integrated rod power has been estimated to increase by approximately 10%. Physics calculations^[38] indicate that the axial power shape is essentially unchanged by the dropped rod.

The effect of the increased radial power factor is to reduce the core MDNBR after the dropped rod. However, as shown above, the core power level and other thermal-hydraulic conditions after power recovery are less severe than before initiation of the accident. The improved power level and other thermal-hydraulic conditions tend to increase the MDNBR. After the rod drop, power decreases to 10 MW and an increase in the MDNBR will occur. As the transient progresses, the MDNBR will decrease to a value corresponding to the reactor conditions at maximum recovery before the reactor trip. This value of the MDNBR after recovery can be estimated based only on the change in power level and the change in radial power distribution by:

$$\Delta \text{MDNBR} = [(1-0.10) \frac{\text{Initial Pwr}}{\text{Max Recovery Pwr}} - 1]$$

This estimate assumes that the change in MDNBR is linear with power. Analysis experience with COBRA has shown that the MDNBR will change by more than the power change since a change in power also influences the channel flow and quality. Use of this equation will neglect the beneficial effects of lower core coolant temperature and increased loop flow. Using the above equation for the L2-3 control rod drop, MDNBR will increase by more than:

$$\Delta \text{MDNBR} = \frac{0.90}{0.49} - 1 = 84\%$$

In conclusion, the MDNBR for the control rod drop accident will be well above the Damage Criteria A consequence limit of $\text{MDNBR} \geq 1.14$. Reactor scram will be provided via a low-pressure trip for a worst-case trippoint of 2000 psia.

3.4 Control Rod Assembly Misalignment - L2-3 LOCE pre-blowdown
(Mode 8) operation will be controlled within a maximum band for control rod position of 50 to 58 inches withdrawn. All steady-state and transient thermal analyses have been done to include operation down to a control rod position of 50 inches above fuel bottom, at which the highest peaking occurs for operation within this CRP band. Thus, operation with control rod misalignment within the operation band is enveloped.

An undetected control rod misalignment with the misaligned control rod assembly (CRA) below 48 inches indicated withdrawn (CRP = 50 inches) could result in peak powers significantly greater than analyzed for L2-3 Mode 8 operation. Therefore, control rod position and CRA movement must be controlled to limit a potential misalignment below 48 inches withdrawn to a very low probability event. The requirements for control rod position surveillance are detailed in the LOFT Technical Specifications with observance of these controls, the conclusions for misalignment remain unchanged from those in the FSAR^[3].

3.5 Primary Coolant Addition and Control System Malfunction - A malfunction or operator error effecting a potential addition of unborated (or low boron) water to the PCS can result in a slow reactivity addition to the core. The maximum reactivity addition rate for this accident, as is described in Section 15.2.5 of the FSAR^[3], is enveloped by the analyses for CRWA's, discussed in the preceding Section V.3.2 of this report.

3.6 Primary Coolant Flow Loss with Flywheel Assisted Coastdown - A loss of both PCS pumps, with coastdown, was analyzed for L2-3 pre-LOCE initial (off-nominal) operating conditions. Analysis for this type of accident in Section 15.2.6 of the FSAR^[3] showed that the loss of both pumps envelopes the consequences for loss of only one pump (with coastdown). Therefore, only the loss of both pumps was analyzed for L2-3 Mode 8 operation.

Analysis for loss of power to both PCS pumps due to a loss of commercial (off-site) power in Section 15.2.9 of the FSAR^[3], showed that the loss of both pumps without a loss of commercial power was the more limiting case. This is because a loss of commercial power also drops the CRA's, thus

providing an instantaneous scram. Therefore, loss of commercial power was not specifically analyzed for L2-3 pre-LOCE operation.

A loss of both PCS pumps with coastdown, was analyzed for $P/A = 2.60$, a high off-nominal hot leg temperature of 617°F , and a low flow worst-case trippoint of 1.01 Mlbm/h . The MDNBR was 1.209 , well above the Damage Criteria A consequence limit of 1.14 .

The L2-2 analyses for loss of flow accidents showed that the low flow trippoint was not required to be a function of measured peaking, even though nominal and off-nominal flow vary with measured peaking^[37]. A comparative steady-state analysis (Appendix B-1) was done for L2-3 for loop flow at the worst-case low flow trip level of 1.01 Mlbm/h , with two different assumptions for peaking. The results were (for $T_{\text{hot}} = 621^{\circ}\text{F}$ and 2350 psia pressure):

<u>P/A</u>	<u>Off-Nominal Power (MW)</u>	<u>MDNBR</u>
2.66	36.4	1.161
2.37	40.6	1.157

These results verify the L2-2 analysis conclusion for L2-3 conditions: The MDNBR for Mode 8 conditions is relatively insensitive to small changes in initial loop flow, and therefore the low flow trippoint can be single-valued for all values of allowable measured peaking.

3.7 Loss of Steam Load - A loss of steam load in the LOFT reactor will result in an increase in temperature and pressure as heat transfer to the secondary system is lost. This accident was analyzed in Section 15.2.7 of the FSAR^[3] as a closure of the Steam Flow Control Valve (SFCV) at a rate of 5% (of full open) per second (a 20 s stroke time). Analyses for power range testing safety analysis^[18] for SFCV stroke times between 1 and 40 s determined that the safety margins are not significantly decreased from the FSAR case for SFCV closure for stroke times between (and including) 10 and 40 s^[19]. The analyses also showed that the minimum DNBR is obtained for SFCV closure at the fastest rate (shortest stroke time).

A study of potential stroke time for the SFCV determined that the stroke time was not likely to be faster than 18 s (measured stroke time has been between 19 and 20 s). A 17 s stroke time was assumed for the final L2-3 Mode 8 loss of steam load accident analysis.

The L2-3 analysis adjusted the initial power and pressure such that when the reactor was tripped by high pressure, the power was at the high worst-case trip value. The transient was initiated from a low off-nominal initial pressure of 2081 psia, in order to maximize the time to scram.

The FSAR analysis^[3] (Section 15.2.7) determined that maximum PCS pressure was calculated assuming that the pressurizer spray and power-operated relief valves did not operate. But, the minimum DNBR was calculated assuming operation of these pressure limiting plant features. The FSAR analysis envelops L2-3 pre-LOCE operation for maximum PCS pressure, since the high pressure is primarily a function of the imbalance between the reactor power and the heat rejection to the secondary system. Therefore, for the L2-3 pre-LOCE analysis, those conditions which tend to reduce DNBR during the transient were assumed, that is pressurizer spray and power-operated relief valve operation, off-nominal initial conditions, and a low (absolute) value for the moderator feedback coefficient. Measured P/A was assumed to be 2.60, the off-nominal hot leg temperature was 613°F, and the worst-case power trip level was 38.2 MW. Since this is a limiting transient, the worst-case L2-3 scram curve was used. Most of the analysis assumptions are listed in Appendix B-4 for this and other L2-3 transients.

For the L2-3 Mode 8 loss of steam load accident with 2081 psia initial pressure, PCS pressure rises to the 2340 psia high pressure trippoint (initiating reactor trip) in 12.75 s, and the control rods drop at 13.6 s, pressure continues to rise slowly, and the inlet temperature is just starting to rapidly increase when scram occurs. The MDNBR was 1.156 at 14.15 s. Therefore, the consequences of the loss of steam load accident are seen to be comparable to those for the limiting CRWA's, for which MDNBR = 1.15 for a 38.5 MW high power trip level. The consequences of this accident are seen to meet the consequence limit for Damage Criteria A (MDNBR $\geq$ 1.14).

Since this transient involves a simultaneous slow power rise and pressure increase, as did the CRWA's, then little gain in operating bounds or trippoints is expected by taking the consequences to the Damage Criteria B limit since there is no significant difference between the Damage Criteria A and B limits using present conservative assumptions for the FRAPT analyses.

3.8 Steam Generator Feedwater Accidents - These accidents include loss of feedwater, and excessive heat removal due to excessive feedwater. These accidents were shown in the FSAR analyses^[3] in Sections 15.2.8 and 15.2.10, to result in very little change in the safety margins, and are thus enveloped by other steam generator accidents (loss of steam load, steam load increase). Therefore, feedwater accidents were not specifically analyzed for L2-3 pre-LOCE operation.

3.9 Steam Load Increase - A steam load increase, due to an inadvertent opening of the SFCV, results in reactor PCS cooldown and a subsequent power rise due to the negative feedback coefficients. FSAR analysis for this accident, in Section 15.2.11 of the FSAR^[3], was done for both a high and a low initial power. The high initial power case was shown to be limiting, and therefore, the analysis of this accident for L2-3 pre-LOCE operation was done for high off-nominal power. Analyses for LOFT Power Range Testing for different SFCV stroke times showed that safety margin was not significantly decreased from the FSAR analysis for stroke times between 1 and 40 s, and that the longer stroke time (40 s) was most limiting^[19]. This conclusion was confirmed by the L2-2 Mode 8 safety analyses for the steam load increase accident^[37].

The steam load increase accident was analyzed for L2-3 Mode 8 operation for a SFCV stroke time of 40 s (an opening rate of 2.5% per second). Analysis was performed for P/A = 2.60 off-nominal conditions for a high initial pressure of 2257 psia since pressure decreases during this accident. The maximum value was assumed for the moderator feedback coefficient so as to increase the positive reactivity feedback due to the cooldown. A minimum value was assumed for the Doppler feedback, as in FSAR analyses. The assumed off-nominal hot leg temperature was 617°F, and the power worst-case trippoint was 38.7 MW. The MDNBR for this accident is 1.24, well above the consequence limit for Damage Criteria A. The accident was tripped by high power.

3.10 Failure of Automatic Pressure Control System - Analysis for the FSAR for this accident (Section 15.2.12 of the LOFT FSAR^[3]) determined that safety margins (MDNBR) increased throughout the transient. Still, this accident was analyzed for L2-2 pre-LOCE operations^[37], since it is the limiting anticipated low pressure transient.

The accident simulated, using the Hybrid computer, inadvertent opening of the power-operated relief valves, which results in the most severe low pressure transients for this accident type. During the transient, reactor power, PCS pressure, and PCS temperature all slowly but steadily decrease. Reactor flow also decreases slightly. The L2-2 analysis assumed no trip occurred. Even without the trip, MDNBR increased throughout the transient, just as it did for the FSAR analysis. The L2-2 analysis was terminated at 115 s when pressure equals 1674 psia. No specific analysis was done for L2-3 operation for this accident since both the FSAR and L2-2 analyses showed no decrease in MDNBR prior to scram of the reactor by the low pressure trip.

3.11 Inadvertent Opening of Reflood Assist Bypass Valves - Inadvertent opening of both Reflood Assist Bypass Valves (RABV's) was reanalyzed for LOFT Power Range Testing in LTR 111-104^[39]. The analyses for power range operation determined that the Damage Criteria A consequence level ($\text{MDNBR} \geq 1.13$ using the LOFT-1 correlation) could be satisfied with a worst-case trippoint of 15% of the full valve opening area, and a time constant of 0.10 s. This analysis was for an opening rate of 10% per second. An analysis for a 10% opening area trip and a 5%/s opening rate predicted a $\text{MDNBR} = 1.25$.

The opening of both RABV's was analyzed for L2-3 Mode 8 operation for both a 5%/s and a 10%/s opening rate with a 10% open area worst-case trippoint. A 0.10 second scram delay was assumed after reaching the trippoint. The accident was analyzed for high measured peaking conditions ($P/A = 2.60$) with a 617°F off-nominal T_{Hot} .

The results of the analysis for inadvertent opening of the RABV's are:

	<u>5%/s opening</u>	<u>10%/s opening</u>
Reactor trip time (s)	2.15	1.15
MDNBR	1.205	1.195
MDNBR time (s)	1.3	1.2
Fraction of initial core flow @ MDNBR	0.906	0.834
Fraction of initial power @ MDNBR	0.951	0.904
Power to Flow Ratio @ MDNBR	1.050	1.084
Fraction of initial core flow @ trip	0.851	
Fraction of initial power @ trip	0.873	
Power to Flow Ratio @ trip	1.026	

As seen above, for the slower opening time the MDNBR is a minimum prior to reactor trip since the moderator feedback brings the power down. With the faster 10%/s opening time, feedback is not as influential, and MDNBR occurs shortly after the initiation of the reactor trip. For either case, however, MDNBR is above the Damage Criteria A consequence limit. Low feedback coefficients were assumed to minimize the effect of feedback on power.

4 Infrequent Faults

4.1 Rapid Primary Coolant Flow Coastdown - This accident analysis simulates a loss of power to one of the PCS pumps without a flywheel assisted coastdown. Although a loss of field trip could occur and provide a quick reactor trip, the accident was analyzed with a low flow trip with a worst-case trippoint of 0.98 Mlbm/hr. This accident was analyzed for P/A = 2.60 off-nominal conditions with an off-nominal T_{hot} of 617°F.

The reactor is tripped at 2.05 s by the low flow trip, but MDNBR occurs at 1.4 s, prior to the trip. This is due to the effect moderator feedback has on lowering reactor power in response to a flow decrease. A minimum

value for the moderator feedback coefficient was assumed. This accident was analyzed for two different scram reactivity assumptions. One analysis was done using the "10/26 LF" scram curve as used for the L2-2 Mode 8 safety analyses, and as used for most L2-3 Mode 8 transients as well (except for the final steam load increase accident). Another analysis was done using the more conservative L2-3 worst-case scram curve discussed in Section IV.4.2. The comparison is made to determine if the difference in the resulting MDNBR due to the scram assumption is significant. As discussed for the power range testing safety analysis^[40], the rapid flow coastdown accidents are considered to be the most sensitive to the scram reactivity assumption (demonstrated by the power range testing safety analyses for the rapid flow coastdown accidents).

The results of the analysis for the rapid coastdown of one PCS pump are:

	<u>"10/26 LF" scram</u>	<u>L2-3 worst-case scram</u>
MDNBR	1.188	1.183

The two-pump rapid coastdown, discussed below, was also analyzed with both scram reactivity assumptions with even less effect on MDNBR due to the scram assumption. Thus, none of the L2-3 transient analysis results will be changed significantly (no change in MDNBR in the second decimal place) by the use of either the "10/26 LF" or the L2-3 worst-case scram curves.

As seen above, the consequences of the one-pump rapid coastdown for L2-3 Mode 8 operation satisfies the consequence limit for Damage Criteria A.

5. Limiting Faults

5.1 Rod Ejection Accident - An ejection of a CRA out of the LOFT core, although an extremely low probability event, can result in a large and rapid reactivity addition to the core. As such, the rod ejection accident is the design basis reactivity excursion for LOFT.

The results of the L2-3 Mode 8 analysis for a maximum control rod ejection accident are discussed in Appendix B-4. The maximum rod ejection accident analysis was done using the LOFT RELAP4 rod ejection kinetics model to

calculate power and PCS response, using COBRA IIIC to calculate hot channel conditions and the LOFT-3 DNBR's, and using FRAPT4 to calculate maximum cladding temperature. Assumptions used for the L2-3 maximum rod ejection accident are:

Ejected rod worth	0.363% $\Delta\rho$
Measured P/A	2.66
Worst-case power trippoint	46.5 MW
Off-nominal T_{hot}	621 °F

The ejected rod worth was input as a step insertion of reactivity. The ejected worth used is significantly greater than the maximum calculated worth of 0.225% $\Delta\rho$ for an ejected rod for L2-3 operation (see Section IV.4.4). The high power trippoint is 9 MW above the expected worst-case trip level for high measured P/A, and is high enough to envelope the trip power for any measured P/A down to P/A = 2.13. The off-nominal T_{Hot} is significantly greater than the value of 613°F set by the limiting slow power rise transients. A discussion in Appendix B-4 further describes the conservatisms in the L2-3 rod ejection analysis.

The results of the maximum L2-3 rod ejection accident analysis are:

Peak power	70 MW (192% initial)
MDNBR	1.03
Time of MDNBR	0.9 s
Maximum average hot spot fuel enthalpy	153 cal/gm
Peak centerline fuel temperature	4670°F
Maximum cladding temperature	1943°F
PCST	1910°F
Time of peak temperatures	2.3 s
Vessel pressure increase	15 psi

The maximum enthalpy is significantly less than the Damage Criteria C limit of 280 cal/gm, and is approximately equal to the maximum enthalpy for a rod ejection from 50 inches withdrawn during full power operation (154 cal/gm). The maximum clad temperature is below the Damage Criteria C limit. The peak centerline fuel

temperature remains below the melting temperature for the fuel. Therefore, the consequences of the rod ejection accident not only meet Damage Criteria D (as required by the Technical Specifications), but also satisfy Damage Criteria C.

A rod ejection analysis was also done for a measured $P/A = 2.37$, for comparison to the high peaking case. Off-nominal power = 40.6 MW and off-nominal loop flow = 1.28 Mlbm/h for $P/A = 2.37$. The peak power was relatively the same as for the $P/A = 2.66$ case (192% of initial), but the peak centerline temperature and clad temperatures were less. The maximum overage fuel enthalpy at the hot spot was greater (158 cal/gm), but not significantly. Therefore, the worst rod ejection accident is for the maximum assumed P/A .

The rod ejection analysis used the "10/26 LF" scram reactivity curve. However, as discussed in Appendix B-4, the combination of reactivity assumptions for this rod eject analysis (0.363% $\Delta\rho$ step insertion and a "10/26 LF" scram curve) is conservative compared to the final L2-3 reactivity assumptions (0.225% $\Delta\rho$ ramp insertion and a L2-3 worst-case scram curve).

5.2 Steam Line or Feedwater Line Rupture

5.2.1 Steam Line Rupture at Full Pre-LOCE Power - Analysis and discussion in the FSAR for steam line and feedwater line ruptures were based on steam line breaks, which are more severe than feedwater line breaks, and showed that a maximum power rise is calculated for a 100% steam line break from full power while a maximum cooldown (with a potential for re-criticality) is calculated for a 50% steam line break at low power^[3].

A 100% steam line break at off-nominal power (the maximum power rise case) was analyzed for L2-2 pre-LOCE operation^[37]. The accident was analyzed using the Hybrid LOFT steam line break model^[28] assuming a worst-combination of L2-2 pre-LOCE initial off-nominal conditions. Although the power increased quickly to trip level, the concurrent rapid decrease in reactor inlet temperature and a slower decrease in pressure resulted in minimum DNBR at the start of the transient, with MDNBR increasing thereafter. Part of the influence of the transient PCS conditions on MDNBR is due to a characteristic of the LOFT

DNB correlations (both LOFT-1 and LOFT-3) in which a larger DNBR is calculated for a lower pressure, within the normal pressure operational range (2000 to 2400 psia). But the major influences increasing MDNBR during the transient are the rapid decrease in coolant temperature, and the short duration of the power burst (approximately 4 s). Therefore, the consequence of a large steam line break near full power for pre-LOCE operation is the same as for the FSAR analysis (Section 15.4.5 in the LOFT FSAR^[3]), that is no decrease in MDNBR during the transient.

This transient was not analyzed specifically for L2-3 Mode 8 operation since the consequences would be no different than those discussed above.

5.2.2 Maximum Cooldown Accident - A maximum cooldown is calculated for a 50% steam line break at low operating power as shown for the FSAR steam line break analyses^{[28][3]}. This accident was analyzed for L2-2 Mode 8 conditions^[37]. The analysis^[37] considered the case in which the reactor was tripped concurrently with initiation of the steam line break, and was therefore conservative, since an early reactor trip increases the cooldown. The L2-2 analysis actually applies to any low flow operation with off-nominal loop flow ≥ 1.045 Mlbm/h, since the analysis is for low power operation (2.5 MW), and is an analysis for PCS cooldown only, without any dependence on power distribution. The L2-2 analysis and its conclusions therefore also apply to L2-3 Mode 8 operation.

The cooldown calculated for pre-LOCE (low flow) operation is larger than was calculated for normal (high flow) operation. The cooldown calculated for the FSAR was from an average temperature of 544°F to 335°F for a 209°F cooldown^[3], approximately the same as for the above. However, even without the feedwater trip the cooldown is still less than the 225°F maximum cooldown used as a basis for LOFT shutdown reactivity control limits. Therefore, with LOFT adherence to the shutdown reactivity limits based on a 225°F cooldown, the cooldown due to the 50% steam line break for L2-2 or L2-3 pre-LOCE operation will not result in re-criticality.

5.3 Rapid Loss-of-Flow from Both Primary Coolant Pumps - This accident, a loss of both PCS pumps simultaneously, without a flywheel assisted coast-down, is the design basis loss of flow accident for the LOFT reactor. This two-pump rapid coastdown was analyzed for $P/A = 2.60$ off-nominal conditions, with an off-nominal T_{hot} of 617°F . It was analyzed for both a reactor trip due to the low flow trip, and due to the faster loss of field current trip.

The result with the loss of field current trip was a MDNBR of 1.20 at 0.8 s. With a low flow trip at 0.98 Mlbm/n loop flow, the MDNBR was 1.175 at 1.4 s. The MDNBR meets the consequence limit for Damage Criteria A ($\text{MDNBR} \geq 1.14$). Therefore, Damage Criteria B and C are also satisfied, which satisfies the consequence limits required for this accident. Assumptions and results for this accident are listed in Appendix B-4.

6. Accident Analysis Conclusions - The following Table V-5 summarizes the results and consequences of the accident analyses for L2-3 LOCE pre-blowdown (Mode 8) operation. The consequences for all of the accidents analyzed for L2-3 LOCE pre-blowdown operation meet the consequence limits for Damage Criteria A ($\text{MDNBR} \geq 1.14$), with the exception of the rod ejection accident. The consequences of the rod ejection accident meets the consequence limits for Damage Criteria C (maximum clad temperature $< 2200^{\circ}\text{F}$, maximum fuel enthalpy $< 280 \text{ cal/g}$, $< 17\%$ cladding oxidation). Since the rod ejection accident is a limiting fault, then satisfaction of the consequence limits for Damage Criteria C is acceptable (see Section III.1). The limiting transients were the control rod withdrawal accident ($0.7 \times 10^{-4} \Delta\rho/\text{s}$ reactivity ramp rate was the worst), the loss of steam load accident, and the control rod ejection accident.

Reactivity or power increase transients are the most limiting type of accidents for pre-LOCE conditions because it is these accidents which increase the power to near full power levels while the flow is below that which would be obtained for a loss of one PCS pump from normal full flow (high flow) conditions. Therefore, for normal full power, full flow operation, the loss of flow accidents are limiting, but for pre-LOCE, low flow operation, reactivity transients (power excursions) are limiting. Analysis was not done for a start-up accident for pre-LOCE conditions because no start up in Mode 8 is allowed due to the low power restriction vs. core P/A .

TABLE V-5

L2-3 MODE 8 ACCIDENT ANALYSIS SUMMARY

<u>Event Category</u>	<u>Accident</u>	<u>T_{hot}(°F)^(a)</u>	<u>MDNBR^(b)</u>	<u>Notes</u>
Operational Transient	Boron Dilution and Rod Motion $R \leq 0.4 \times 10^{-4} \Delta\rho/s$	613	1.16	
Incident of Moderate Frequency	CRWA (or Boron Dilution) $R \leq 8 \times 10^{-4} \Delta\rho/s$	613	1.15	
	Control rod drop	613	>1.25	(c)
	Control rod misalignment			(d)
	Loss of pumps with flywheel coastdown	617	1.21	(g)
	Loss of steam load 17 s stroke time on SFCV	613	1.16	
	Steam generator feedwater accidents			(e)
	Steam load increase 40 s stroke time on SFCV	617	1.24	
	Pressure control failure		>1.25	(c)
	RABV opening 10%/s opening rate	617	1.20	
Infrequent Fault	Rapid one pump loss of flow	617	1.18	(g)
Limiting Fault	Control rod ejection (0.363% $\Delta\rho$ step)	621	1.03 max clad temp = 1943 Hot spot fuel enthalpy = 153 cal/g	
	Steamline break			(f)
	Two pump loss rapid coastdown low flow trip	617	1.175	(g)
	Same as above with field trip	-----Bounded by above-----		

TABLE V-5 Notes:

- (a) High off-nominal hot leg temperature. This temperature is 4°F above nominal.
- (b) Calculated using the LOFT-3 correlation.
- (c) MDNBR increases throughout this transient. See discussion for control rod drop accidents in Appendix B-4.
- (d) Consequences and protective controls for this accident are no different than for L2-2 Mode 8 operation.
- (e) The consequences of loss of feedwater or increased feedwater accidents are enveloped by the steam load accidents.
- (f) The analysis and consequences for the steamline break maximum cooldown is no different from that analyzed for L2-2 Mode 8 operation, as discussed in Reference [37]. As discussed in Reference [37], the MDNBR increases throughout a steam line break transient, even if initiated from high off-nominal power.
- (g) A loss of flow accident during the low flow maneuver is not as severe as during low flow operation. This was shown in the L2-2 Mode 8 safety analysis^[37].

As was shown for the L2-2 core safety analyses, the consequences of the accidents are always the worst for the highest assumed measured peak-to-core average power ratio (peaking or P/A). The maximum assumed P/A for the L2-3 analyses was $P/A = 2.60$. Also, as for the L2-2 analyses, it was shown for L2-3 that the low flow trip can be single valued, that is it is not a function of measured P/A. The worst case low flow trip for L2-3 analyses is 1.01 Mlbm/h loop flow. The high power trip, however, could not be single valued. The required high power WCT is:

$P/A > 2.55$	WCT = nominal + 3.5 MW
$2.55 > P/A \leq 2.50$	WCT = nominal + 3.8 MW
$P/A \leq 2.50$	WCT = nominal + 4.2 MW

Nominal power = $90.23/(P/A)$ MW, as specified in the L2 EOS.

Satisfactory mitigation of the slow power rise transients (CRWA's and loss of steam load) required that the potential initial operating conditions be restricted. The required operating condition bounds included a need to lower the nominal operating pressure in order to gain DNBR margin. The case for lowering the L2-3 operating pressure is discussed in Section III.2 and Appendix A-4. The required operating bounds are:

Maximum measured $P/A \leq 2.60$
 Minimum Initial power $\geq$ nominal - 4.5 MW
 High off-nominal hot leg temperature $\leq 613^\circ\text{F}$
 (thus nominal hot leg temperature $\leq 609^\circ\text{F}$)
 High off-nominal pressure ≤ 2257 psia
 (nominal pressure = 2169 psia)

Other bounds may or may not have been required, but were a part of the assumed analysis conditions. These were:

High off-nominal power $\leq$ nominal + 2.5 MW
 Low off-nominal loop flow $\geq [3.327/(P/A)] - 0.125$ Mlbm/h
 Low initial pressure ≥ 2081 psia
 Differential control rod worth $\leq 0.165\% \Delta\rho/\text{inch}$
 Steam flow control valve stroke time ≥ 17 s, ≤ 40 s
 RABV opening rate $\leq 10\%$ open/s

The limiting values of worst-case trippoints assumed for the L2-3 Mode 8 accident analyses are:

<u>Trip</u>	<u>Worst-case Trippoints</u>	<u>Time Constant(s)</u>
High reactor power	(See the above discussion)	0.25
Low loop flow	1.01 Mlbm/h	1.0
High Hot Leg Temperature	617°F	18
High PCS pressure	2340 psia	1.0
Low PCS pressure	2000 psia	1.0
RABV opening	10% open	0.10

The hot leg temperature trip time constant needed to be significantly decreased from the 40 s assumed for the L2-2 Mode 8 safety analysis for satisfactory mitigation of the CRWA's. The high pressure WCT had to be lowered to 2340 psia (2440 psia was assumed for previous safety analyses) for satisfactory mitigation of the L2-3 CRWA's and the loss of steam load accident. Other trippoint assumptions, shown above, may not be bounding, but are as assumed for the L2-3 safety analyses.

Most of the L2-3 accidents were analyzed using the "10/26 LF" scram reactivity insertion curve used for the L2-2 Mode 8 analyses. A more conservative scram curve, the L2-3 worst-case scram curve (213 LF), was developed after most analyses were complete. However, comparative calculations for the most scram-sensitive transients (rapid loss of flow accidents) showed that use of the L2-3 worst case scram curve did not significantly change the resulting MDNBR. This is discussed further in Section V.4.1.

Analyses for CRWA's and loss of steam load accidents for L2-3 conditions showed that the use of Damage Criteria A or B made no significant

difference for these accidents since a peak cladding surface temperature is reached very shortly following forcing the FRAP hot fuel pin surface into film boiling when $MDNBR < 1.14$ (when the probability for DNB first exceeds 5%). This is largely due to many conservatisms in the LOFT fuel pin model assumptions used with the FRAPT4 code.

In conclusion, the accident analyses show that the LOFT reactor can be operated safely at L2-3 Mode 8 conditions within the final analysis envelope assumed, and with trippoints within the final assumed worst-case trippoints, since the analysis results demonstrate that the accident consequences satisfy the consequence limits of the applicable damage criteria (see Section III.1).

VI. MODES 5, 6, AND 7 ANALYSES FOR L2-3 OPERATION

1. Operation With Reduced Nominal Pressure - As discussed in Sections 3.2 and 4.1, satisfactory mitigation of limiting transients and establishment of satisfactory Mode 8 safety limits required that the initial safety margins be improved by lowering the LOFT nominal pressure. As shown in Appendix A-4, a pressure decrease will result in an increase in the DNB heat flux, an effect that is seen in the use of the LOFT-3 correlation for DNB heat flux. For Mode 8, the nominal pressure was reduced from 2262 psia to 2169 psia, high off-nominal reduced from 2350 to 2257 psia, the high pressure worst-case trippoint (WCT) reduced from 2490 to 2340 psia, and the low pressure WCT was reduced from 2076 to 2000 psia. The safety analyses for L2-3 Mode 8 operation demonstrates the operational acceptability of operation with these pressure bounds and trips.

Additional analyses and reviews of the LOFT FSAR and subsequent safety analysis summaries were done to demonstrate that LOFT operation with the same reduced nominal pressure and reduced WCT's was also acceptable for operation in Modes 5, 6, and 7. The only transients terminated by a low pressure trip for the Mode 7 (at power, full flow) safety analyses are:

- (a) Control rod drop.
- (b) Failure of the automatic pressure control system (a 2076 psia WCT was assumed).
- (c) Steam line break at low power (maximum cooldown accident).
- (d) LOCA.

Hybrid computer simulation was done for a control rod drop accident during full power (Mode 7) operation with a 2000 psia low-pressure WCT. The original analysis for this case in the FSAR^[3] assumed an initial pressure near nominal (2259 psia) and a dropped CRA worth $2.39\% \Delta p$. These conditions were also assumed for this analysis. Two analyses were done. One with the FSAR

maximum values for moderator and Doppler feedback coefficients ($9.1 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$ and $-1.5 \times 10^{-5} \Delta\rho/^\circ\text{F}$, respectively), and another with values increased 7% to 10% above those in LTR 111-52^[43] for a hot reactor. The results for the control rod drop from full power (53 MW off-nominal power) are:

- (a) The reactor is tripped by a 2000 psia low pressure WCT.
- (b) At the time of maximum power recovery:

	<u>FSAR Max. Feedback</u>	<u>LTR 111-52 Max Feedback</u>
Power Recovery % of Initial (see Figures A.3 and A.4)	74	50
Core Average Temp Decrease ($^\circ\text{F}$)	29	29
Core Flow Increase (%)	5.5	6
Increase in MDNBR with 10% Flux Tilt (%)	22	78

Since the assumption from near nominal pressure (2259 psia) is above the high off-nominal pressure for the assumed decrease in nominal pressure for L2-3 operation (2257 psia high off-nominal), then the above results apply to operation with reduced nominal pressure as well. Therefore, the Mode 7 control rod drop accidents analyzed demonstrate that a 2000 psia low-pressure WCT is suitable for Mode 7 operation with the nominal pressure reduced to 2169 psia. The control rod drop accident analyses for both Modes 7 and 8 are discussed in Appendix B-4.

A failure of the automatic pressure control system results in a slow and continuous decrease in PCS pressure. The decreasing pressure causes a

reduction in power due to the pressure coefficient of reactivity (a moderator density decrease occurs). Analysis for the FSAR determined that MDNBR increases throughout the transient (FSAR^[3], Section 15.2.12). Therefore, the level of the low pressure WCT is not important as long as it is sufficient to trip the reactor within a reasonable time. A 2000 psia WCT will therefore terminate operation during this transient just as satisfactorily as the 2076 psia WCT assumed for the FSAR analysis. For operation with 2169 psia nominal pressure, a 2000 psia WCT is 17 psi closer to nominal than a 2076 psia WCT during operation with a 2262 psia nominal pressure.

A steam line break accident from low power is a cooldown transient in which the safety concern is not power level or DNBR, but the potential for recriticality after scram. The pressure decrease and scram for this transient are significantly faster than the cooldown. Therefore, a small difference in the low pressure trippoint is insignificant. A 2031 psia trip was assumed for the FSAR analysis^[28]. Protection for a cooldown transient is provided by assuring sufficient shutdown reactivity in the core. The LOFT requirements for shutdown reactivity for cooldown accident protection are discussed in LTR 111-97^[34] for Modes 2-7 and LTR 111-116^[41] for Mode 8. The requirements are based on a maximum cooldown transient due to a 50% steam line break at zero power, hot reactor conditions, with a high steam generator level, and a reactor average power trippoint of 1 MW. Operation with lower nominal pressure, to 2169 psia, or with a 2000 psia low pressure trip will therefore not change the consequences of this maximum cooldown transient.

The ECCS trippoint for a LOCA is 1850 psia. As long as the low pressure WCT is above this, then the level of the low pressure WCT is not a consideration during a LOCA unless the time to reactor trip is important. However, upon the initiation of limiting LOCA's, pressure very rapidly drops to saturation (~ 1700 psia). Therefore, a slight difference in the low pressure reactor trip or the initial operating pressure will not significantly effect the consequences of a LOCA.

The above discussions lead to the conclusion for LOFT operation in any mode that the consequences of transients which are terminated by a low

pressure trip are not significantly changed by operation with a 2000 psia low pressure WCT or with nominal pressure reduced to 2169 psia.

Another consideration is the pressure assumptions used for safety analyses for transients other than those few terminated by a low pressure trip. The COBRA analyses for the FSAR assumed an initial pressure of 2205 to near nominal pressure (2262 psia). The analyses for the low flow safety limit used a high pressure 2240 psia, because the LOFT-1 correlation was slightly more conservative at high pressure for calculations with below nominal flow^[45]. However, a study for the effect of pressure on DNB heat flux at LOFT full-flow levels concluded^[44]: "At loop flows below 4×10^6 lbm/hr, the actual DNB data do not show any trend with pressure. This pressure effect study concluded that below loop flows of 4×10^6 lbm/hr, pressure in the range of 1820 to 2240 psia has no significant effect on the calculated DNB heat flux (the predicted differences are well within the scatter of the data). Therefore, the pressure assumed for these analyses is not a significant consideration." The reduction in DNBR calculated using the LOFT-1 correlation for a change in pressure from nominal (2262 psia) to 1820 psia at a loop flow of 3.22×10^6 lbm/hr was only 1.3%. Therefore, the Mode 7 safety margins are not significantly affected by reducing nominal pressure to 2169 psia. Even for PCS flow greater than 4×10^6 lbm/hr, the estimated decrease in DNBR for the 442 psia decrease in pressure to 1820 psia is not more than 7%.

Operation for zero power physics testing (Mode 5) was analyzed for PCS pressures as low as 1820 psia^[15]. As long as the position of the control rod bank is 35 inches or more withdrawn when at 1820 psia then no reduction in safety margin is predicted from that existing for operation at 2262 psia and a minimum critical control rod position (24 inches withdrawn). However, this analysis was based on a possible 7% reduction in DNBR for a reduction in pressure from 2262 to 1820 psia, and an initial DNBR > 40. Therefore, even if control rod position was not changed, the change in safety margin would not be significant since the difference in the probability for DNB between a DNBR of 40 and 37 (7% less) is not significant. Also the possible 7% decrease in DNBR is based on high flow data (loop flow > 4×10^6 lbm/h).

Transient analyses for zero power operation are referred back to the FSAR analyses noting that the consequences are much less severe for zero power operation^[15]. Therefore, since operation with lowered nominal pressure and/or a 2000 psia low pressure WCT is satisfactory for Mode 7, it is also satisfactory for Mode 5. The conclusions for Mode 6 (start up) are no different than for Mode 5 and Mode 7.

The conclusions regarding a possible low pressure safety limit for Modes 5, 6 and 7 are not changed by operation at 2169 psia nominal pressure with a 2000 psia low pressure WCT. The conclusion is still as stated in LTR 111-104^[44], and LTR 111-116, Section 1^[9]. That is, "Core safety margins are not significantly changed by lowering pressure to as low as 1820 psia. Although no steady state power range safety limit has been determined for low pressure (where MDNBR would = 1.13 [LOFT-1]), it is definitely below 1820 psia." "The only implied limits on the pressure in these steady-state analyses are the limits of the data for the LOFT-3 DNB correlation (2000 to 2400 psia)."

In conclusion, operation of the LOFT reactor with nominal pressure reduced to 2169 psia and/or with a 2000 psia worst-case trippoint does not significantly change the safety margins for operation in any operating mode. Loss of steam load accident, as discussed in Section 15.2.7 of the FSAR^[3], is mitigated by a high pressure trip with a worst-case trippoint of 2440 psia. The accident was analyzed from an initial pressure of 2247 psia^[21], or 15 psi less than the nominal 2262 psia. An increase in the spread between nominal and trip could result in a lower MDNBR for the transient. Therefore, for a 93 psi decrease in nominal pressure (from 2262 to 2169 psia), the worst-case trippoint will need to also be decreased by at least 93 psia, unless additional analysis is done to show that no significant change results for the consequences of the loss of steam load accident. In the absence of a re-analysis for this accident, a reduction in the high pressure worst-case trippoint to 2340 psia (a 100 psi decrease) as for L2-3 Mode 8 operation is needed for Modes 5, 6, and 7. (Mode 7 or FSAR conditions are limiting for Modes 5 and 6 also for a loss of steam load accident.)

2. L2-3 Mode 7 Operation With Revised Scram Curve - The L2-3 Mode 7 scram curve (the "214 HF" curve) described in Section 4.4.2 is more conservative than the "817" scram curve used for previous Mode 7 core safety analyses. Therefore, the effect of this more-conservative scram curve on Mode 7 transients needed to be considered. As discussed in LTR 111-104^[40], the Mode 7 transients most sensitive to a change in the scram assumption are the one-pump loss and two-pump loss rapid coastdown accidents. For L2-3 power levels (nominally 75% of full rated power), the consequences of a rapid coastdown transient from an initial low flow level will envelop the consequences for the same accident from initial high flow levels. This was shown by comparative analyses for the rapid flow coastdown for the L2-2 test safety analysis^[37]. This is especially true since use of the L2-3 low flow scram curve (the "213 LF" curve) results in the insertion of less scram reactivity with time than the "214 HF" curve (see Figure IV-1).

The consequences of the rapid loss of flow accidents for L2-3 power and initial low flow are (from Section 5.):

	<u>One-pump loss</u> <u>rapid loss-of-flow</u>	<u>Two-pump loss</u> <u>rapid loss-of-flow</u>
MDNBR	1.183	1.174

Therefore, Damage Criteria A is also satisfied for Mode 7 operation (full flow) with L2-3 power levels and the L2-3 scram curve (the "214 HF" curve).

Analyses were also done for the rapid loss-of-flow (or rapid coastdown) accidents with the "214 HF" scram curve and full Mode 7 power (53 MW off-nominal power). The analysis assumptions were the same as for the power range safety analyses in Reference [40], except for the scram curve. The results of these analyses are discussed with comparisons to the power range testing analysis results in Appendix B-5.

The 53 MW analysis for the one-pump loss rapid coastdown calculated a peak cladding surface temperature (PCST) of 1669°F, whereas the PCST

with the "817" scram (Case FLOW .4D) was 1638°F ^[40]. Thus, the consequence limit of 1650°F for Damage Criteria B for this accident would be exceeded for 53 MW operation. The MDNBR with the "214 HF" scram is 1.092, whereas MDNBR = 1.11 for FLOW .4D. It appears that the Damage Criteria would be satisfied at a maximum off-nominal power of 50 MW (6% less power, whereas the difference in MDNBR's and PCST's is only 2%).

The consequences and results of the 53 MW analysis for the two-pump loss rapid coastdown with the "214 HF" scram were less severe in every case than for the calculation using the "817" scram (problem designation FLOW .6K). The MDNBR's were approximately the same, but the "214 HF" scram case (problem designation FLOW .6Q) went into DNB later in time (when MDNBR < 1.14), had a higher mass flow rate at the hot spot, and had less of the hot fuel pin in the film boiling regime. Therefore, no FRAPT4 calculation was run for FLOW .6Q, since it is apparent that the calculated maximum clad temperatures will be less than for FLOW .6K.

In conclusion, the Damage Criteria can be satisfied for Mode 7 operation with the more conservative L2-3 high flow scram assumption (the "214 HF") scram curve, as long as Mode 7 power is restricted to 50 MW or less (high off-nominal). This off-nominal power limit corresponds to a 47.5 MW nominal power.

3. L2-3 Xenon Effects on Mode 7 Operation - The worst Xenon perturbed axial power distribution for LOFT L2-3 operation is discussed in Section 4.4.5. The worst distribution (a worst axial distribution is one with a maximum peak high up in the core) is obtained just at start up (zero power) with the CRA's 61 inches above fuel bottom after building in Xenon during Mode 8 operation with the CRA's 50 inches above fuel bottom. This worst power distribution profile is plotted in Figure IV-2. As seen in Figure IV-2, the profile is nearly axially inverted from an equilibrium Xenon profile.

Steady state COBRA IIIC calculations were done for LOFT operation at power assuming the axial power distribution to be as plotted in Figure IV-2, Case B, and assuming a radial peaking in the hot pin of 1.60. Calculations were

done for Mode 7 conditions, and for two power levels: (1) 50 MW, as for previous Mode 7 Xenon effects analyses, and (2) 39 MW, and L2-3 power level for total peaking = 2.3. The results of the calculations are:

	<u>39 MW</u>	<u>50 MW</u>
MDNBR	1.84	1.38

Previous Mode 7 Xenon effects analyses showed that with steady-state and transient analyses, the Xenon perturbed distribution resulted in significantly higher MDNBR's than the BOL axial power distribution with 50 inches CRA position that was used as a basis for FSAR and other power range safety analyses^[46]. The MDNBR for the base case distribution is 1.40 (case X50-1 in Reference [46]). The MDNBR for the previous-worst Xenon perturbed Mode 7 distribution is 1.55 (Case X50-2 in Reference [46]). These previous Xenon effects analyses assumed a radial hot pin peaking of 1.586.

The COBRA IIIC calculations for the L2-3 worst Xenon perturbed axial power distribution are discussed in Appendix B-3. The analysis showed that a nearly linear relationship exists between power and MDNBR for these steady-state calculations with everything else held constant. Therefore, it is shown that at a L2-3 worst-case trip power level of 43 MW, the MDNBR would be 1.69, and similarly, that a MDNBR of 1.55 would be obtained for a power of 46 MW with the L2-3 Xenon perturbed power distribution. Therefore, for a power of 46 MW, the safety margins would be approximately the same as for the previous Mode 7 Xenon perturbed case analyzed in Reference [46]. However, the MDNBR's were still significantly greater than for the FSAR and power range safety analyses (9 to 10% greater^[46]). Therefore, operation with the L2-3 Xenon perturbed power distribution at a power 9% greater than 46 MW, or 50 MW, should result in safety margins no less than those for the FSAR and other power range core safety analyses, especially since the peaking for the Xenon restart will decrease as power is increased above zero power. However, without additional analysis, the L2-3 Mode 7 nominal recovery power following Mode 8 operations should not exceed 46 MW; i.e., that power at which the safety margin is approximately the same as for the previous Mode 7 Xenon effects studies done for a 50 MW nominal power^[46].

4. Miscellaneous L2-3 Mode 7 Analyses - COBRA IIIC steady-state calculations were done for the LOFT core and full power (Mode 7) conditions to determine the effect of fuel burnup or lifetime on the MDNBR. An earlier study for the FSAR (using FRAPS) had shown that the fuel centerline temperatures at beginning of life (BOL) are limiting over the core lifetime^[35]. The MDNBR vs fuel lifetime study is discussed in Appendix B-5. Power distributions calculated by PDQ 07 for two different control rod positions (CRP at 36 and 54 inches) as a function of core lifetime (expressed as effective full power hours or EFPH) were used for the analysis. The calculations for 36 inches CRP were done for a power of 33 MW, whereas the 54 inches CRP calculations assumed full off-nominal power, or 53 MW. All conditions, other than the power distribution, were held constant with burnup. The result of the analysis showed that as for the fuel centerline temperature, MDNBR is most limiting at BOL and increases as fuel burnup or lifetime increases.

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APPENDIX A
ANALYSIS ASSUMPTIONS AND MODELS

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APPENDICES

APPENDIX A. ANALYSIS ASSUMPTIONS AND MODELS

- A-1. The LOFT-3 DNB Correlation
- A-2. Pin Power Distribution Spread and Uncertainty for 12 kW/ft Test
- A-3. L2-3 Mode 8 Trippoints, Operating Conditions, and Predicted L2-3 Power Distribution
- A-4. Nominal Pressure Decrease for L2-3 Mode 8
- A-5. L2-3 Moderator and Doppler Reactivity Feedback Coefficient Spread
- A-6. Power Distribution Assumptions for L2-2 Thermal Safety Analysis
- A-7. Statistical Evaluation of the LOFT Heat Flux Engineering Hot Spot Factor
- A-8. L2-3 COBRA Models Input
- A-9. COBRA IIIC - COBRA IV-I Comparison and Spacer Grid Loss Calculation Error
- A-10. Results of Requested Rod Worth Calculations for LOFT (L2-3)

APPENDIX B. ANALYSIS RESULTS

- B-1. L2-3 Mode 8 Steady-State Analysis Results
- B-2. MDNBR vs MLHGR Plot for L2-3 Conditions
- B-3. COBRA Analysis for L2-3 Mode 7 Operation with Maximum Xenon Buildin
- B-4. L2-3 Mode 8 Transient Analysis Results

APPENDIX A-1
THE LOFT-3 DNB CORRELATION

INTEROFFICE CORRESPONDENCE

date August 30, 1978
to T. M. Howe
from S. A. Eide *SAE*
subject LOFT-3 CHF CORRELATION - AN EXTENSION OF THE LOFT-1
CORRELATION TO LOW MASS FLUXES - SAE-4-78

The new steady-state LOFT CHF correlation, LOFT-3, developed to include the recent low flow (0.4×10^6 , 0.5×10^6 lbm/hr.ft²) data obtained at Columbia University, is presented below:

$$q_{CHF} = 1.31569 \times 10^6 - 3.79605 \times 10^2 P + 8.32015 \times 10^{-2} G \\ + 1.08312 \times 10^2 PX - 1.01982 GX$$

where

q_{CHF} = predicted CHF (Btu/hr.ft²)

P = system pressure (psia)

G = local mass flux (lbm/hr.ft²)

X = local equilibrium quality.

The applicable range of LOFT-3 is:

Pressure 2000 to 2400 psia

Mass flux 0.4×10^6 to 2.5×10^6 lbm/hr.ft²

Equilibrium quality - 0.3 to 0.2.

In SI units, the LOFT-3 correlation is:

$$q_{CHF} = 4.14837 \times 10^6 - 1.73589 \times 10^2 P + 1.93605 \times 10^2 G \\ + 4.95298 \times 10^1 PX - 2.37306 \times 10^3 GX$$

where

q_{CHF} - W/m²

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P - kPa

G - $\text{kg/s}\cdot\text{m}^2$.

The applicable range is:

Pressure 1.38×10^4 to 1.65×10^4 kPa

Mass flux 540 to $3400 \text{ kg/s}\cdot\text{m}^2$

Equilibrium quality - 0.3 to 0.2

The LOFT-3 correlation has the following statistics:

$\bar{x} = 1.000$

$s = 0.0686$

$n = 526$ (455 previous + 71 new low flow data).

A MDNBR of 1.14 with the LOFT-3 correlation provides a 95% probability at a 95% confidence level that DNB will not occur on a rod at that DNBR.

A separate CHF correlation for the data with mass fluxes between 0.40×10^6 and $1.30 \times 10^6 \text{ lbm/hr}\cdot\text{ft}^2$ was developed, but the transition between this correlation and LOFT-1 at $1.30 \times 10^6 \text{ lbm/hr}\cdot\text{ft}^2$ was not smooth. The two correlation predictions differed by as much as 15%, depending on the quality and pressure. Therefore, a single CHF correlation, LOFT-3, was developed to cover the complete range of mass fluxes.

ul

cc: E. C. Anderson
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INTEROFFICE CORRESPONDENCE

date September 18, 1978

to T. M. Howe

from S. A. Eide *La C*subject COMPARISON OF LOFT-1 CHF CORRELATION WITH THE NEW LOFT-3
CORRELATION - SAE-6-78

- Ref: (1) S. A. Eide and R. C. Gottula, Evaluation and Results of LOFT Steady State Departure from Nucleate Boiling Tests, TREE-NUREG-1043 (April 1977).
- (2) Letter from S. A. Eide to T. M. Howe, August 30, 1978, SAE-4-78.

The LOFT-1 CHF correlation (Ref. 1) has been compared with the new low flow, 540 and 680 kg/s·m² (0.4 and 0.5 x 10⁶ lbm/hr·ft²), CHF data obtained at Columbia University. The LOFT-1 is conservative (i.e., predicts lower CHF's than the experimental CHF's) for the new data.

The LOFT-1 has also been compared with the new LOFT-3 (Ref. 2) which was developed from the LOFT-1 data base and the new low flow data base. The results are presented in the attached five tables. For each ratio, the numerator is the maximum heat flux allowable (for a given pressure, mass flux and equilibrium quality) using LOFT-3 without experiencing CHF on a rod at 95% probability and confidence. The denominator is the same quantity except that LOFT-1 is used. Wherever the ratio is less than one, the use of the LOFT-3 correlation would be more limiting on core power than the use of the LOFT-1, and for ratios greater than one the LOFT-3 would be less limiting.

Either CHF correlation can be used in the extended mass flux range of 540 to 1020 kg/s·m² (0.40 to 0.75 x 10⁶ lbm/hr·ft²). However, the new LOFT-3 was developed from a data base including the new low mass flux data, and its use is recommended. Except for equilibrium qualities less than - 0.20, the LOFT-3 is less limiting than the LOFT-1 in this low mass flux range.

u1

Attachments:
As stated

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COMPARISON OF LOFT-3 / LOFT-1 CORRELATIONS --- (95/95 LOWER LIMIT) *

P = 2000. psia

13.8 MPa

13.8 MPa		QUALITY			QUALITY			QUALITY			QUALITY		
		-0.30	-0.25	-0.20	-0.15	-0.10	-0.05	0.00	0.05	0.10	0.15	0.20	
340	0.25	0.972	0.988	1.005	1.022	1.040	1.059	1.078	1.098	1.120	1.142	1.165	
680	0.50	0.974	0.986	0.998	1.012	1.026	1.042	1.059	1.078	1.098	1.120	1.144	
1020	0.75	0.975	0.983	0.993	1.003	1.015	1.028	1.042	1.058	1.077	1.097	1.122	
1360	1.00	0.975	0.982	0.989	0.996	1.005	1.015	1.027	1.040	1.056	1.074	1.097	
MASS 1700	1.25	0.976	0.980	0.985	0.990	0.997	1.004	1.013	1.023	1.035	1.051	1.071	
FLUX 2040	1.50	0.976	0.979	0.982	0.985	0.989	0.994	1.000	1.007	1.015	1.027	1.042	
2380	1.75	0.977	0.978	0.979	0.981	0.983	0.985	0.988	0.991	0.996	1.002	1.011	
2720	2.00	0.977	0.977	0.977	0.977	0.977	0.977	0.977	0.977	0.977	0.977	0.977	
3060	2.25	0.978	0.977	0.975	0.974	0.972	0.970	0.967	0.963	0.958	0.951	0.939	
3400	2.50	0.978	0.976	0.974	0.971	0.967	0.963	0.958	0.950	0.940	0.924	0.897	

kg / s . m²

lbm / hr . ft²
x 10⁻⁶

$$* \frac{(LOFT-3) / 1.14}{(LOFT-1) / 1.13}$$

COMPARISON OF LOFT-3 / LOFT-1 CORRELATIONS -- (95/95 LOWER LIMIT) *

P = 2100. *psia*

<i>14.5 MPa</i>	-0.30	QUALITY -0.25	-0.20	QUALITY -0.15	-0.10	QUALITY -0.05	0.00	QUALITY 0.05	0.10	QUALITY 0.15	0.20
340 0.25	0.959	0.976	0.994	1.012	1.032	1.052	1.073	1.095	1.118	1.142	1.167
680 0.50	0.965	0.977	0.990	1.004	1.019	1.035	1.053	1.072	1.093	1.116	1.141
1020 0.75	0.970	0.978	0.987	0.998	1.009	1.021	1.035	1.051	1.069	1.090	1.114
1360 1.00	0.973	0.979	0.985	0.992	1.000	1.009	1.019	1.031	1.046	1.063	1.084
MASS 1700 1.25	0.976	0.980	0.983	0.987	0.992	0.998	1.005	1.013	1.023	1.035	1.051
FLUX 2040 1.50	0.979	0.980	0.982	0.984	0.986	0.988	0.992	0.996	1.001	1.007	1.016
2380 1.75	0.980	0.980	0.980	0.980	0.980	0.980	0.980	0.979	0.979	0.979	0.978
2720 2.00	0.982	0.981	0.979	0.977	0.975	0.972	0.969	0.964	0.958	0.949	0.936
3060 2.25	0.983	0.981	0.978	0.975	0.970	0.965	0.958	0.950	0.937	0.919	0.890
3400 2.50	0.985	0.981	0.977	0.972	0.966	0.959	0.949	0.936	0.917	0.889	0.840

kg/s.m² *lbm/hr.ft²*
 $\times 10^{-6}$

$$* \frac{(LOFT-3)/1.14}{(LOFT-1)/1.13}$$

COMPARISON OF LOFT-3 / LOFT-1 CORRELATIONS --- (95/95 LOWER LIMIT) *

P = 2200. psia

15.2 MPa		QUALITY			QUALITY			QUALITY			QUALITY		
		-0.30	-0.25	-0.20	-0.15	-0.10	-0.05	0.00	0.05	0.10	0.15	0.20	
340	0.25	0.955	0.973	0.993	1.013	1.034	1.056	1.079	1.104	1.129	1.156	1.185	
680	0.50	0.965	0.977	0.991	1.006	1.022	1.039	1.058	1.078	1.101	1.126	1.154	
1020	0.75	0.972	0.981	0.990	1.000	1.011	1.024	1.039	1.055	1.073	1.095	1.120	
1360	1.00	0.978	0.983	0.989	0.995	1.003	1.011	1.021	1.033	1.047	1.063	1.084	
MASS 1700	1.25	0.982	0.985	0.988	0.992	0.996	1.000	1.006	1.012	1.021	1.031	1.045	
FLUX 2040	1.50	0.986	0.987	0.987	0.988	0.989	0.990	0.992	0.993	0.996	0.999	1.003	
2380	1.75	0.989	0.988	0.987	0.985	0.984	0.982	0.979	0.976	0.971	0.965	0.956	
2720	2.00	0.992	0.989	0.986	0.983	0.979	0.974	0.967	0.959	0.948	0.931	0.906	
3060	2.25	0.994	0.990	0.986	0.981	0.975	0.967	0.957	0.943	0.925	0.897	0.850	
3400	2.50	0.996	0.991	0.986	0.979	0.971	0.960	0.947	0.929	0.902	0.861	0.789	

kg/s.m² lbm/hr.ft²
 x 10⁻⁶

$$\frac{(LOFT-3)/1.14}{(LOFT-1)/1.13}$$

COMPARISON OF LOFT-3 / LOFT-I CORRELATIONS --- (95/95 LOWER LIMIT) *

P = 2300. psia

15.9 MPa	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY
	-0.30	-0.25	-0.20	-0.15	-0.10	-0.05	0.00	0.05	0.10	0.15	0.20
340 0.25	0.960	0.981	1.003	1.026	1.050	1.075	1.102	1.130	1.159	1.190	1.223
680 0.50	0.973	0.987	1.002	1.018	1.036	1.055	1.076	1.100	1.125	1.154	1.186
1020 0.75	0.983	0.992	1.002	1.013	1.025	1.039	1.054	1.072	1.093	1.117	1.146
1360 1.00	0.990	0.995	1.001	1.008	1.015	1.024	1.035	1.047	1.062	1.080	1.102
MASS 1700 1.25	0.996	0.998	1.001	1.004	1.008	1.012	1.017	1.023	1.031	1.042	1.055
FLUX 2040 1.50	1.000	1.000	1.001	1.001	1.001	1.001	1.001	1.002	1.002	1.003	1.004
2380 1.75	1.004	1.002	1.000	0.998	0.995	0.992	0.987	0.982	0.974	0.964	0.948
2720 2.00	1.007	1.004	1.000	0.996	0.990	0.983	0.974	0.963	0.947	0.924	0.887
3060 2.25	1.010	1.005	1.000	0.994	0.986	0.976	0.963	0.945	0.921	0.883	0.819
3400 2.50	1.012	1.007	1.000	0.992	0.982	0.969	0.952	0.929	0.896	0.842	0.744

kg/s.m² lbm/hr.ft²
 $\times 10^{-6}$

$$* \frac{(LOFT-3)/1.14}{(LOFT-1)/1.13}$$

COMPARISON OF LOFT-3 / LCFT-1 CORRELATIONS --- (95/95 LOWER LIMIT) *

P = 2400. psia

16.5 MPa	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY	QUALITY
	-0.30	-0.25	-0.20	-0.15	-0.10	-0.05	0.00	0.05	0.10	0.15	0.20
340 0.25	0.978	1.003	1.028	1.056	1.084	1.114	1.146	1.180	1.215	1.253	1.293
680 0.50	0.993	1.009	1.027	1.046	1.066	1.089	1.114	1.142	1.173	1.208	1.248
1020 0.75	1.004	1.014	1.025	1.038	1.052	1.069	1.087	1.109	1.134	1.163	1.199
1360 1.00	1.012	1.018	1.024	1.032	1.041	1.051	1.063	1.078	1.096	1.118	1.146
MASS 1700 1.25	1.018	1.020	1.024	1.027	1.031	1.036	1.042	1.050	1.059	1.072	1.089
FLUX 2040 1.50	1.023	1.023	1.023	1.023	1.023	1.023	1.024	1.024	1.024	1.025	1.026
2380 1.75	1.027	1.025	1.022	1.020	1.016	1.012	1.007	1.000	0.991	0.978	0.957
2720 2.00	1.030	1.026	1.022	1.017	1.010	1.002	0.992	0.978	0.959	0.930	0.882
3060 2.25	1.033	1.027	1.021	1.014	1.005	0.994	0.979	0.958	0.929	0.882	0.798
3400 2.50	1.035	1.029	1.021	1.012	1.000	0.986	0.966	0.939	0.899	0.833	0.705

kg/s.m² lbm/hr.ft²
x10⁻⁶

* $\frac{(LOFT-3)/1.14}{(LOFT-1)/1.13}$

INTEROFFICE CORRESPONDENCE

date January 15, 1979
to D. G. Satterwhite
from S. A. Atkinson *S. A. Atkinson*
subject PIN POWER DISTRIBUTION SPREAD AND UNCERTAINTY FOR 12 KW/FT TEST
Atki-1-79

Attached are two studies: (1) To determine the potential spread in TIP measured hot pin power for the next LOCE and, (2) To obtain an estimate for the 2 standard deviation (2σ) uncertainty on hot pin or hot channel power as determined by TIP measurements.

The first study was based on comparisons of pre-test 3-D PDQ7 predictions to the power distributions inferred by TIP measurements for power range testing and the L2-2 test. The study looked at the total peaking, i.e., the ratio of peak power to core average power (P/A), and the radial peaking, the ratio of average hot pin power to core average power. The conclusions and results of the study are:

1. For good correspondence between the conditions for the PDQ7 prediction and actual test conditions during the TIP measurements, close agreement is obtained: Within 0.1% for total P/A, within 1% for radial P/A. Therefore, TIP-inferred hot pin power should be within 1% of a good pre-test PDQ7 prediction.
2. Axial power distribution shapes show close agreement, particularly in the high power (mid-core) regions.
3. Discrepancies between pre-test predictions and TIP-measured distribution seems to be largely due to differences in core conditions between the PDQ7 reference case and actual test conditions. However, even for wide differences in reactor power, flow, or depletion, the difference between prediction and TIP measurement is about 3%, and nearly always within 4%. The radial P/A will agree within 2%.
4. Based on the above, the expected total peaking for the next test (a 12 Kw/ft maximum linear heat generation rate LOCE) is $P/A = 2.43$. This is for a control rod position (CRP) of 54 inches withdrawn, and a core depletion of 200 to 300 EFPH. The power distribution should closely correspond to PDQ7 case L1056 in LTR 111-86.

5. The potential spread in total P/A for a limit on CRP of 50 to 58 inches withdrawn is:

CRP = 50 inches
P/A < 2.66

CRP = 58 inches
P/A > 2.23

These values include an uncertainty of 3 to 4% for the TIP-measured P/A without closely corresponding PDQ7 predictions. The measured hot pin radial P/A should be ≤ 1.56 .

6. If the minimum CRP was 52 inches withdrawn, then maximum total P/A < 2.60.

The nominal, off-nominal, and limiting worst case values for reactor power and loop flow corresponding to a spread in total peaking of 2.30 to 2.66 are in the attached Table 1.

The study for hot pin power 2σ uncertainty determined that the 7% uncertainty used for the L2-2 core safety analyses is a good estimate for the 2σ uncertainty. The standard deviation from this study for the uncertainty of total P/A as inferred for TIP measurements is 3.4 to 3.8%.

The hot pin is shifted in the LOFT COBRA models to a location that is adjacent to the hot channel. This hot pin location shift in the COBRA model represents an increase in that pin's peak power of 2.3%. Therefore, the additional uncertainty allowance required in COBRA for a 2σ hot pin power uncertainty is 4.7%, or a hot spot factor of 1.047. The use for the L2-2 safety analyses of a 1.07 hot channel factor for power distribution uncertainty plus the hot pin location shift was equivalent to allowing for a 9.3% total uncertainty.

A low flow, 50 inch CRP power distribution with a maximum radial P/A is proposed for COBRA analyses for the 12 Kw/ft test. The radial P/A then is increased to 1.61 to obtain a total peaking of 2.66 in the hot pin. For the proposed profile (case 1012 in LTR 111-86), the hot pin location shift plus the additional increase in radial P/A represents an additional hot channel power increase of 3.1%. Therefore, the hot channel factor for a total hot channel power distribution uncertainty of 7% (2σ) needs to be 1.039.

If the hot pin is not shifted in the COBRA model, then the increase in hot pin radial P/A represents an increase of 2.6% in all channel powers in the model since the radial distribution is normalized to 1.0 at the hot pin. This 2.6% will satisfactorily allow for the potential difference between measured and predicted radial power distribution. Then, the full 7% power distribution uncertainty needs to be applied to the pins

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D. G. Satterwhite
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APPENDIX A-2

adjacent to the hot channel. This approach would be applicable for any power distribution and does not apply extra conservatism and therefore is considered preferable.

In summary, these studies conclude for the COBRA analyses for the next LOCE that:

1. Maximum peaking is 2.66 for a CRP of 50 inches withdrawn. The corresponding off-nominal and limiting worst-case values for reactor power and loop flow are as listed in the attached Table 1.
2. Radial P/A is not likely to exceed 1.56. However, the use of a power distribution with a maximum radial P/A as used for the L2-2 core safety analysis, should be retained for the COBRA analysis. Use of a maximum radial P/A allows use of the COBRA analysis as a scoping analysis for future 12 Kw/ft tests with more depletion and flatter axial profiles.
3. The 2σ local power distribution uncertainty is 7%, to be applied as a hot spot/hot channel factor to the pins adjacent to the hot channel.

ul

Attachments:
As stated

cc: w/attachments
B. S. Anderson
S. A. Atkinson (2)
D. F. Elger
N. F. Fausz
J. E. Fisher
TAB File 3.7

w/o attachments
E. C. Anderson
J. L. Liebenthal
R. P. Wadkins *[Signature]*
Central File

TABLE I
REACTOR POWERS AND LOOP FLOWS FOR 12 KW/FT LOCE

Control rod position (inches)	50	52	54	58
Case description	max peak	max peak	nominal	min expected
Total peaking	2.66	2.60	2.43	2.30
Nominal power (MW)	33.9	34.7	37.2	39.2
Nominal loop flow (Mlbm/h)	1.314	1.345	1.44	1.52
Off-nominal power (MW)	36.4	37.2	39.7	41.7
Off-nominal loop flow (Mlbm/h)	1.12	1.15	1.245	1.32
Minimum worst-case power (MW)	37.4	38.2	40.7	42.7
Maximum worst-case flow (Mlbm/h)	1.01	1.04	1.12	1.19

STUDY FOR MEASURED HOT PIN PEAKING
SPREAD FOR NEXT LOCE

S. A. Atkinson

A study of TIP-measured LOFT core power distributions for power range testing, and how the measured distributions compared to PDQ pre-test predictions was reported by Reference (a). Reference (a) reported that the difference in the measured peak to core average power ratio (or peaking, P/A) from that predicted by available PDQ cases was + 0.6% to - 5.1%. The radial power distribution (average pin power to core average ratio) was within $\pm 2\%$ for hot pins, and within 4.4% for outer core positions. The maximum differences in power were at the core extremes, top or bottom and edges. The hot pin axial profiles were in good agreement in the high power (central) region. The TIP axial distributions were generally less than the PDQ prediction upstream of the hot spot, and greater than PDQ prediction downstream of the hot spot. Therefore, the actual enthalpy increase to the point of minimum DNBR should be less than calculated based on the PDQ predicted distribution. The calculations for MDNBR (Minimum Departure from Nucleate Boiling Ratio) are therefore conservative.

Additional study has been done both for the power range TIP measurements and for TIP-measured hot pin power distribution for the L2-2 test. This additional study only looked at the hot pin (PDQ pin 10) powers. The results of the additional study, based in part on Reference (a), are discussed as follows:

Table 1 shows a comparison of PDQ7 predictions for full power, full flow BOL (Beginning of Life) operation to power range TIP measurements at 11.5 MW. Although the 11.5 MW data is not BOL, as no specific BOL data was taken, it is close to BOL for the power range tests. Higher power data was not taken until after considerable core depletion (approaching 200 EFPH). These relative powers are for a control rod position (CRP) of 54 inches withdrawn.

Table 2 shows a comparison of PDQ7 predictions for full power full flow operation for BOL and 200 EFPH to the TIP measurements at 48.7 MW. The estimated depletion at 48.7 MW during power range testing was near 200 EFPH.

Table 3 shows a comparison of PDQ7 predictions for full flow operation with a CRP of 36 inches withdrawn. The PDQ predictions are for BOL, whereas the TIP measurement was taken with partial depletion (less than 200 EFPH). The TIP measured relative powers are compared to two PDQ predictions; (1) for low inlet enthalpy, and (2) for high inlet enthalpy. The test inlet enthalpy was somewhere in between (about 529 Btu/lbm for 535°F inlet temperature).

Table 4 shows a comparison of PDQ7 predictions for low flow operation, with 200 EFPH and other depletions, to L2-2 TIP measured hot pin relative powers. The comparison is for a CRP of 54 inches withdrawn. The approximate depletion for the L2-2 TIP measurement was also 200 EFPH.

The comparisons between PDQ pre-test predictions and TIP-measured power distributions shown in Tables 1 through 4 show the following results:

1. For close correspondence of reactor conditions, the total P/A is predicted well within 1%, as close as 0.1%. This is especially evident when comparing the TIP data for 48.7 MW and the LTR 111-60B PDQ prediction for 55 MW and 200 EFPH in Table 2, and when comparing the L2-2 TIP data and the L1057 PDQ prediction in Table 4. The radial P/A is predicted within 1%.
2. Even when there is a wide difference in reactor conditions, (such as the amount of depletion, the operating power, inlet enthalpy, flow rate, but not including control rod position) the difference between TIP values and PDQ prediction is about 3% or less for total P/A and about 2% or less for radial P/A. The largest difference was 4.9% when comparing TIP measurements for 200 EFPH and high inlet enthalpy to PDQ run L1037 for BOL and low inlet enthalpy.
3. The comparisons show that if a closely corresponding PDQ7 prediction is available, the potential difference between pre-test predictions and TIP-determined relative powers in the hot pin will be within 1%.
4. The comparisons show that as long as a PDQ7 prediction is available for the desired control rod position, the measured hot pin total P/A can be expected to be within 3% of the prediction, and the hot pin radial P/A can be expected to be within 2% of the prediction. However, the

comparisons show that inlet enthalpy (which strongly influences the boron concentration requirement) is a sensitive parameter. Therefore, care should be taken to try to use a PDQ prediction for a comparable inlet enthalpy.

Figure 1 shows the TIP-determined hot pin axial relative power profile for the L2-2 tests as compared to the PDQ prediction from run L1057 in LTR 111-86^[d]. As seen in Figure 1, the agreement is very good.

Since it has now been determined that the PDQ7 predictions are reliable predictions of the core power distribution as determined by the use of the TIP system even if the reactor conditions do not closely correspond, then PDQ predictions can be used to determine what the measured peaking may be for future test conditions. A PDQ7 predicted hot pin total peaking is plotted as a function of core depletion in Figure 2. Two lines are plotted in Figure 2, one for CRP = 54 inches, and one for CRP = 50.6 inches. As seen in Figure 2, the effect of depletion on total P/A appears to be linear after 200 EFPH. The lack of sufficient predictions for BOL at the same reactor conditions as for the depleted predictions prevented a resolution of how total P/A varies up to 200 EFPH depletion. However, the BOL points (for 54 inch CRP) are still within 3% of each other.

Figure 3 shows PDQ predictions for hot pin total P/A as a function of CRP for low flow operation. Based on the points in Figure 3, the potential measured P/A for the next LOCE test can be predicted. The predicted relative powers (or peaking) are:

<u>CRP (inches)</u>	<u>Total P/A</u>	<u>Total P/A ± 3%</u>
50	2.57	2.65
52	2.50	2.58
54	2.43	
58	2.3	2.23

Study for Measured Hot Pin Peaking Spread for Next LOCE
S. A. Atkinson
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Therefore, total P/A can be considered to be less than 2.66, for 50 inch CRP, or 2.60 (allowing for 4% uncertainty) for 52 inch CRP. The hot pin radial P/A is greatest for the higher CRP, equal to 1.501 to 1.534 at 54 inch CRP. Radial P/A is less at the lower CRP's, about 1.48 for run L1038 in LTR 111-86^[d] (for 50 inch CRP and 12 Kw/ft, low flow conditions). Therefore, radial P/A is expected to be less than 1.56.

For the predicted range in total P/A, the corresponding nominal reactor power and flow for a 12 Kw/ft test are^[e]:

$$P_N = 7.519 \frac{12}{P/A} \text{ MW}$$

$$\dot{m}_N = \frac{P_N}{88.07} 3.413 \times 10^6 = (3.875 \times 10^4) P_N \text{ lbm/h}$$

The off-nominal values are:

$$P_{ON} = P_N + 2.5 \text{ MW}$$

$$\dot{m}_{ON} = 0.95 \dot{m}_N - 0.125 \times 10^6 \text{ lbm/h}$$

The limiting worst-case trip (WCT) values, that is those values as close as practical to desired nominal operating points, are:

$$P_{WCT} = P_N + 3.5 \text{ MW}$$

$$\dot{m}_{WCT} = 0.91 \dot{m}_N - 0.01 \dot{m}_N - 0.175 \times 10^6 \text{ lbm/h}$$

These equations for off-nominal and limiting WCT values are based on the PPS trippoint and accuracy information discussed in Appendices A-1 and A-2 of Reference [f].

The following Table 5 shows the values of reactor power and loop flow that correspond to the predicted range in measured peaking (for CRP between 50 and 58

inches withdrawn). The 58 inch CRP values are based on the predicted P/A of 2.30 instead of a P/A 3% less. This is done since safety analysis for higher P/A was shown to be most limiting for the L2-2 core safety analyses^[g] and in steady-state parametric studies for a 12 Kw/ft test, and since no PDQ power distributions are available for P/A less than 2.30.

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TABLE 1

HOT PIN RELATIVE POWERS FOR FULL FLOW
CRP = 54 INCHES WITHDRAWN AND BOL

<u>Source</u>	<u>TIP Power Range Testing</u>	<u>PDQ LTR 111-81^[b] Case L1037</u>	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 11160B^[c]</u>	<u>% TIP Different From PDQ</u>
Depletion	Partial	BOL		BOL	
Power (MW)	11.5	60.3		55	
Loop Flow (Mlbm/h)	3.78	3.25		3.42	
Inlet Enthalpy (Btu/lbm)	~529	500.1		557.2	
Radial P/A	1.573	1.572	+ .06	1.548	+ 1.6
Axial P/A	1.575	1.625	- 3.1	1.612	- 2.3
Total P/A	2.477	2.555	- 3.1	2.495	-0.7

TABLE 2

HOT PIN RELATIVE POWERS FOR FULL FLOW
CRP = 54 INCHES WITHDRAWN 200 EFPH

<u>Source</u>	<u>TIP Power Range Testing</u>	<u>PDQ LTR 111-60B</u> ^[c]	<u>% TIP Different From PDQ</u>
Depletion	~200 EFPH	200 EFPH	
Power (MW)	48.7	55	
Loop Flow (Mlbm/h)	3.78	3.42	
Inlet Enthalpy (Btu/lbm)	~529	539.8	
Radial P/A	1.548	1.532	+ 1.0
Axial P/A	1.570	1.588	- 1.1
Total P/A	2.430	2.433	- 0.1

<u>Source</u>	<u>PDQ LTR 111-81</u> ^[b] Case L1037	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 111-60B</u> ^[c]	<u>% TIP Different From PDQ</u>
Depletion	BOL		BOL	
Power (MW)	60.3		55	
Loop Flow (MLBM/h)	3.25		3.42	
Inlet Enthalpy (Btu/lbm)	500.1		557.2	
Radial P/A	1.572	- 1.5	1.548	0
Axial P/A	1.625	- 3.4	1.612	- 2.6
Total P/A	2.555	- 4.9	2.495	- 2.6

TABLE 3

HOT PIN RELATIVE POWERS FOR FULL FLOW

CRP = 36 INCHES WITHDRAWN

<u>Source</u>	<u>TIP* Power Range Testing</u>	<u>PDQ LTR 111-81^[b] Case L1030</u>	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 111-60B^[c]</u>	<u>% TIP Different From PDQ</u>
Depletion	Partial	BOL		BOL	
Power (MW)	15	31		49.5	
Loop Flow (Mlbm/h)	3.78	3.25		3.42	
Inlet Enthalpy (Btu/lbm)	~529	485.7		557.2	
Radial P/A	1.611	1.629	- 1.1	1.581	+ 1.9
Axial P/A	2.009	2.055	- 2.2	2.037	- 1.4
Axial Peak Location (inches)	22.5	22.5		17.4	
Total P/A.	3.236	3.347	- 3.3	3.221	+ 0.5

*Actual CRP was 37 inches withdrawn.

TABLE 4

HOT PIN RELATIVE POWERS FOR LOW FLOW

CRP = 54 INCHES WITHDRAWN 200 EFPH

<u>Source</u>	<u>TIP L2-2</u>	<u>PDQ LTR 111-86[d] Case L1057</u>	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 111-86[d] Case L1058</u>	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 111-86[d] Case L1056</u>	<u>% TIP Different From PDQ</u>
Power (MW)	24.9	23.1		13.8		35.0	
Loop Flow (Mlbm/h)	1.48	1.36		1.38		1.33	
Inlet Enthalpy (Btu/lbm)	553	552.2		553.7		550.3	
Radial P/A	1.542	1.533	+ 0.6	1.534	+ 0.5	1.501	+ 2.7
Axial P/A	1.576	1.584	- 0.5	1.591	- 0.9	1.616	- 2.5
Total P/A	2.430	2.429	+ 0.04	2.441	- 0.5	2.426	+ 0.2

<u>Source</u>	<u>PDQ LTR 111-60B[c]</u>	<u>% TIP Different From PDQ</u>	<u>PDQ LTR 111-60B[c]</u>	<u>% TIP Different From PDQ</u>	<u>PDQ Case L1075</u>	<u>% TIP Different From PDQ</u>
Depletion	200 EFPH		BOL		650 EFPH	
Power (MW)	55		55		24.6	
Loop Flow (Mlbm/h)	2.04		3.42		1.44	
Inlet Enthalpy (Btu/lbm)	539.8		557.2		553	
Radial P/A	1.509	+ 2.2	1.548	- 0.4	1.523	+ 1.2
Axial P/A	1.624	- 3.0	1.612	- 2.2	1.561	+ 1.0
Total P/A	2.450	- 0.8	2.495	- 2.6	2.378	+ 2.2

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TABLE 5

NOMINAL, OFF-NOMINAL, AND LIMITING WCT VALUES
FOR POWER AND FLOW FOR 12 KW/FT TEST

CRP (inches)	50	52	54	58
Measured Total P/A	2.66	2.60	2.43	2.30
Nominal Power (MW)	33.9	34.7	37.2	39.2
Nominal Flow (Mlbm/h)	1.314	1.345	1.44	1.520
Off-Nominal Power (MW)	36.4	37.2	39.7	41.7
Off Nominal Flow (Mlbm/h)	1.12	1.15	1.245	1.32
Min Power WCT (MW)	37.4	38.2	40.7	42.7
Max Flow WCT (Mlbm/h)	1.01	1.04	1.12	1.19

FIGURE 1
HOT PIN AXIAL POWER PROFILE COMPARISON

DEPLETION EFFECT ON LOW FLOW PEAKING FROM PDQ7 CALCULATIONS

LEGEND

- L2-3 CONDITIONS
- L2-2 CONDITIONS
- ▲ FULL POWER, HIGH TIN, 1.7 Mlbm/h
- △ FULL POWER, 54" CRP, HIGH TIN
- ▽ FULL POWER, 54" CRP, LOW TIN
- ESTIMATE FOR L2-3 CONDITIONS BASED ON 50" CRP PREDICTIONS
- BOL, 39 MW, 1.04 Mlbm/h, 50" CRP
- * FULL POWER, 5440 FTIN, 2 Mlbm/h

FIGURE 2

PDQ PREDICTED HOT PIN PEAKING
WITH AND WITHOUT DEPLETION
FOR LOFT L-2 SERIES OPERATION
(L2-2 AND L2-3 WITH LOOP FLOW OF
1.4 mlbm/h to 1.7 mlbm/h)

FIGURE 3

STUDY FOR LOFT HOT PIN POWER UNCERTAINTY
GIVEN TIP POWER DISTRIBUTION MEASUREMENTS

S. A. Atkinson

The method for inferring a LOFT core power distribution from TIP axial scans in 3 core locations is described in Reference [a]. The method is based on matching the axial fission power measurements in the TIP scan tubes to x-y power distributions predicted by 3-D PDQ7 calculations. Therefore, the uncertainty in the power for any pin is a combination of the uncertainty in the PDQ7 calculated x-y distribution and the TIP system uncertainties. The PDQ7 distribution uncertainties are discussed in References [a] and [b]. The TIP system uncertainties (detector and associated electronics and support) are discussed in Reference [c]. A review of these three references determined the following estimates for uncertainties which relate to the determination of hot pin power.

PDQ7 X-Y DISTRIBUTION UNCERTAINTIES

1. The uncertainty due to non-uniform moderator effects in the hot pin next to a water-filled guide tube is 1 standard deviation (1σ) = $\pm 2\%$ ^[b].
2. The uncertainty due to the inability to accurately predict the effect of the steel reflector is less than 1.5% at the hot spot^[b]. Therefore, if this estimate for maximum uncertainty is taken as a 2σ bound, then the 1σ uncertainty is 0.75%.
3. Remaining uncertainty in the ability to predict or match actual power distribution with the PDQ7 prediction is taken to be $< 2\%$ ^[b]. However, these remaining uncertainties are of the type that can be detected by the TIP traces since they are a general distribution uncertainty and not a local effect. This uncertainty is accounted for by allowing for a maximum spread in measured peaking in the safety analysis. This spread is based on a 3 to 4% uncertainty derived by comparing PDQ7 predictions to TIP measured distributions.

TIP FACTOR UNCERTAINTY

The fission densities measured in a TIP tube are translated to power densities in an adjacent pin location, but in the 3/4 of the LOFT core

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without the air-filled TIP tubes by use of TIP factor multipliers. These TIP factors are based on PDQ7 calculations. An uncertainty in the TIP factors represents a possible bias in the TIP-determined core power distribution. This uncertainty is estimated to be within $\pm 2\%$ for the hot pin peak power^[a]. Since this is an estimate for maximum uncertainty, it is taken to be a 2σ bound. Then, the 1σ uncertainty for hot pin peaking is $\pm 1\%$.

TIP SYSTEM ACCURACY

1. The major source of possible inaccuracy in the TIP measurements is the uncertainty of the sensitivity of the detector to gamma radiation (γ -sensitivity)^[c]. This γ -sensitivity uncertainty is within 2.9% of the reading plus 1.3% of range^[c]. The relationship of range to normal peak plane readings is not described. If 1.3% of range is taken to be approximately the same as 1.9% of reading in the peak power plane, then the effect of the γ -sensitivity uncertainty is 4.8% of reading. Taking this maximum estimate to be a 2σ bound, then the 1σ uncertainty for TIP detector γ -sensitivity is $\pm 2.4\%$.
2. The remaining accuracy of the detector and supporting electronics is within $\pm 1.3\%$ of reading^[c]. Taking this maximum estimate to be a 2σ bound, then the 1σ uncertainty is 0.65%.
3. Reference [c] also discusses a positional accuracy. However, the practice of axially translating all TIP traces to the same grid spacer locations tends to minimize the effect of positional accuracy^[a]. Therefore, the uncertainty due to positional accuracy is neglected.

In summary, the LOFT hot pin power uncertainties for a TIP-measured power distribution are:

CALCULATIONAL ACCURACY	<u>1σ Uncertainty</u>
Local Non-Uniform Moderator Effect	2%
Reflector Effect on X-Y Distribution	0.75%
TIP Factor Uncertainty	1%

Study for LOFT Hot Pin Power Uncertainty Given Tip
Power Distribution Measurements
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TIP SYSTEM ACCURACY	1σ <u>Uncertainty</u>
γ -Sensitivity	2.4%
Detector and Electronics	0.65%

The overall 1σ uncertainty is then:

$$1\sigma = [(.02)^2 + (.0075)^2 + (.01)^2 + (.024)^2 + (.0065)^2]^{1/2} * 100$$

$$1\sigma = 3.4\%$$

The uncertainties due to the effect of non-uniform moderation near the hot pin and the steel reflector effects at the hot pin could be biased in the same direction. If so, then the overall 1σ uncertainty would be:

X-Y distribution uncertainty at hot pin, $1\sigma = 2.75\%$

$$1\sigma = [(.0275)^2 + (.01)^2 + (.024)^2 + (.0065)^2]^{1/2} * 100$$

$$1\sigma = 3.8\%$$

Therefore, the 2σ uncertainty for hot pin power is approximately 7%.

Some of the above uncertainties would be less if averaged over the hot pin instead of being determined for the peak power point. However, the available references do not supply any data for an average hot pin or hot channel power uncertainty. Therefore, for the COBRA safety analyses, the above uncertainty should also be applied as a hot channel factor.

The LOFT COBRA models used for the core safety analyses shift the location of the hot pin such that it is adjacent to the hot channel. This location shift has the effect of a hot spot or hot channel factor for power distribution uncertainty. For the power range testing power distributions, for CRP = 50 inches, this hot pin locations shift represented a 2.3% increase in the peak power for pin 20 in the COBRA models. (The hot pin is pin 13 in the COBRA models or pin 10

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in the PDQ7 model). The case L1012 PDQ7 distribution from LTR 111-86^[d] is being proposed for use as the base power distribution for a total $P/A = 2.66$. The predicted radial $P/A = 1.570$ in case L1012 for the hot pin is adjusted to a radial $P/A = 1.61$ to obtain total $P/A = 2.66$ (the case L1012 predicted total $P/A = 2.585$). Shift of the hot pin location from pin 13 to pin 20 in the L1012 distribution represents an increase in pin 20 peak power of 2.3%. Therefore, for a total 2σ power distribution uncertainty of 7%, the additional potential increase in pin power is 4.7%. Thus, the power distribution uncertainty hot spot factor should be 1.047 for a 2σ hot pin power distribution uncertainty of 7%.

An alternate way to handle a 7% power distribution uncertainty in the COBRA analyses would be by eliminating the hot pin shift, and apply the full 7% uncertainty to the pins adjacent to the hot channel (COBRA channel 15). This approach would be applicable for any power distribution, whereas with the hot pin shift, the additional adjustment could vary with power distribution. Therefore, elimination of the hot pin shift in the COBRA model and use of a 7% power distribution uncertainty factor for the pins surrounding the hot channel is the more straightforward and preferable approach.

REFERENCES

- [a] G. E. Putnam and B. L. Rushton, "Three-Dimensional Pin Powers from LOFT Core-I TIP Data", LTR 111-87 (RE-Z-77-001) September 1977.
- [b] Harvey W. Graves, Jr., "A Review of Uncertainties Associated with the Evaluation of Peak Linear Power Density and DNB Flux During LOFT Steady State Operation," A report for the Nuclear Regulatory Commission, December 30, 1977.
- [c] P. A. Quinn, "LOFT Traversing IN-Core Probe (TIP) Uncertainty Analysis," LTR 141-39, Supplement 7, November 1975.
- [d] E. F. Aber, "LOFT Core-I: Low Flow Power Distributions as a Function of Control Rod Position, Core Power Level, and Inlet Enthalpy", LTR 111-86 (RE-P-77-076) August 29, 1977.

APPENDIX A-3

L2-3 MODE 8 TRIPPOINTS, OPERATING CONDITIONS,
AND PREDICTED L2-3 POWER DISTRIBUTION

INTEROFFICE CORRESPONDENCE

date March 26, 1979

to E. C. Anderson *ec*

from D. G. Satterwhite *DGS*

subject LOCE L2-3 MODE 8 PPS TRIP SETPOINTS AND OFF-NOMINAL OPERATING CONDITIONS - DGS-1-79

- Ref:
- (a) R. A. Dimenna/G. L. Hunt ltr to S. A. Atkinson, RAD-2-78, GLH-4-78, "PPS Trip Setpoints and Time Constants for L2-2 Safety Analysis," May 16, 1978
 - (b) E. C. Anderson ltr to J. R. White, ECA-14-77, "Off-Nominal Plant Conditions for L2-3 Pre-LOCE Operation," December 14, 1977
 - (c) E. F. Aber, "LOFT Core-1: Low Flow Power Distributions as a Function of Control Rod Position, Core Power Level, and Inlet Enthalpy," LTR 111-86, August 29, 1977
 - (d) B. L. Rushton, "LOFT Core-1: Physics Methods Verification," LTR 111-19, March 15, 1971
 - (e) Harvey W. Graves, Jr., "A Review of Uncertainties Associated with the Evaluation of Peak Linear Power Density and DNB Flux During LOFT Steady State Operation," A report for the Nuclear Regulatory Commission, December 30, 1977
 - (f) P. A. Quinn, "LOFT Traversing In-Core Probe (TIP) Uncertainty Analysis," LTR 141-39, Supplement 7, November 1975
 - (g) N. F. Fausz ltr to E. C. Anderson, NFF-1-79, "Statistical Evaluation of the LOFT Heat Flux Engineering Hot Spot Factor," February 27, 1979

The purpose of this letter is to detail the LOCE L2-3 Mode 8 PPS trip set-points and off-nominal operating conditions. Included in the discussion are deviations from the EOS, Volume 2, Revision 2, test conditions required by Mode 8 safety analysis.

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Plant and LOCE EOS Modification

The worst case postulated transient in the anticipated category is a slow reactivity insertion in the range of 0.7×10^{-4} β_p /sec. that would result in the worst case power trip and worst case hot leg temperature trip to be initiated coincidentally. This reactivity insertion rate could result from a slow control rod withdrawal accident or a boron dilution accident. In order to minimize the consequences of these transients and to assure adequate thermal margin to DNB, the following requirements have been incorporated in the Mode 8 safety analysis.

- (1) Reduction of nominal primary system pressure from 2250 psia to 2169 psia
- (2) Reduction of nominal PCS hot leg temperature from 617°F to 609°F
- (3) Require PCS hot leg RTD response time constant <15 sec.

Nominal Power and Flow

The nominal power and flow for given LOCE condition are determined as functions of the core peak to average power ratio, F_q . The current LOCE L2-3 conditions affecting this determination are:

Vessel coolant temperature differential (ΔT)	= 64.5°F
Peak linear heat rate (PLHR)	= 12 kW/ft
Vessel outlet coolant temperature (T_H)	= 609°F
Primary system pressure (P)	= 2169 psia

Using the above condition, the required core enthalpy increase, ΔH , is 86.31 BTU/lbm. Then using the expression developed in Reference (a), nominal power becomes:

$$P(\text{MW}) = 7.519 \frac{(12 \text{ kW/ft})}{F_q} = \frac{90.23}{F_q}$$

and nominal loop flow in

$$\begin{aligned} \dot{m}(\text{lbm/hr}) &= \frac{P(\text{MW})}{\Delta H(\text{BTU/lbm})} \times 3.413 \times 10^6 \frac{\text{BTU/hr}}{\text{MW}} \\ &= \frac{3.568}{F_q} \times 10^6 \text{ lbm/hr} \end{aligned}$$

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The values obtained from these expressions are graphically presented in Figure 1 (attached) for the range of F_q used in the safety analysis.

PPS Trip Setpoints

The PPS trip setpoints and instrument error stackup for worst case trip values developed for the LOCE L2-3 are derived in the same manner with the same assumption for instrument and drift accuracies as detailed in Reference (a). Deviations in values and implementation are as follows: (See Figure 2-7, attached)

Temperature Channel

No changes from Reference (a). Nominal T_H is set at 609°F by safety analysis considerations.

Pressure Channel

The reduction in pressure results from safety analysis requirements. The LOFT-3 DNB correlation indicates that at low mass fluxes, CHF increases significantly with decreasing pressure (other accepted correlations corroborate this result). The worst case low pressure trip was maintained at 2000 psia because that coincides with the low pressure limit of the data used in developing the LOFT-3 DNB correlation. Nominal pressure is set at 2169 psia (93 psid from LOCE L2-2).

Flow Channel

The low flow trip setpoint is determined using the worst case $F_q = 2.6$. Variation of the trip with variation of F_q is not required. The high and low flow alarms remain set to $\pm 5\%$ of nominal flow and thus are functions of F_q .

Peak Power Channel - N/A

Average Power Channel

Stringent controls are required for Pre-LOCE power levels. The nominal trip setpoint and control band (+1, -2 MW) must vary as functions of F_q . The lower control band was necessitated by the need to minimize the power differential between an initial power and resulting trip power during a reactivity insertion transient. Operationally, the lower control band will be administratively controlled while the nominal trip setpoint and high power alarm will be functionally controlled. The implementation of the functional control is accomplished by setting the instrumentation

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such that 75% indicated power corresponds to the required actual power based on F_q . The upper control band alarm is then set at 77% with the nominal trip at 78%. This scheme assures the required constant power differential between nominal power and nominal trip power and can be easily adjusted with variation in F_q .

Off-Nominal Operating Conditions

Power Distribution and F_q

Reference (b) transmitted a recommended axial/radial power distribution for use in ESA enveloping analyses. The recommended distribution is PDQ07 case L1012 from Reference (c) with an associated F_q of 2.585. For comparison purposes, the current best estimate distribution ($F_q = 2.447$), calculated using the LOFT core follow physics model, for LOCE L2-3 is presented in Tables 1 and 2 (attached). A comparison of the hot pin power profiles of the two distributions is presented in Figure 8 (attached).

The best estimate distribution was calculated using the same maneuvering history as L2-2 with the active rod material positioned at 54 in. above fuel bottom. In order to present an estimate of actual expected F_q for the LOCE, the $F_q(\text{BE})$ must be reduced by 2%. This is the effect of active rod material being positioned at 56 in. (indicated 54 in. withdrawn). Therefore, the expected F_q for the LOCE (assuming same maneuvering history as LOCE L2-2) is $F_q = 2.40$.

In general, the higher in the core the axial peak occurs, the more conservative the calculation when concerned with margin to DNB. However, LOCE calculations are predominately sensitive to PLHR and not initial local channel properties.* Therefore, the original power distribution is still applicable for all types of safety analysis.

The range of F_q used in the Mode 8 safety analysis is $2.3 \leq F_q \leq 2.6$. While as stated the recommend distribution is applicable, it is modified to conform to the enveloping worst case situation; i.e., scaling the peak to the $F_q = 2.6$ and flattening the peak as required by analysis techniques. The nominal power and flow would then follow based on F_q .

Uncertainty in PLHR

Given a desired PLHR, various uncertainty factors must be applied to determine the actual value attainable. There are uncertainties in measurement of F_q , uncertainties in indicated vs. actual core average power, as well as

* As long as DNB is not present.

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control band considerations, and uncertainties in local power due to effect of fuel rod fabrication tolerances and fuel densification effects. All uncertainties are expressed as two standard deviation (2σ) values.

The uncertainties in Fq measurement stem from two basic sources. The first source is the actual XY physics calculations that supply the basis vectors used in the TIP program. In general, for an XY calculation, the uncertainty of the local pin power is 3% to 4% of value for unrodded environments. Reference (d) states a value of 3% for LOFT physics methods. Reference (e) expresses concern over the effect of the radial steel reflectors on overall power, thus affecting the relative pin power. While this latter effect has not been demonstrated by higher order transport physics calculation, an additional 1% uncertainty is included. Therefore a total physics uncertainty of 4% of value is assumed.

The second source of uncertainty in Fq results from TIP system properties. The TIP system uncertainties are developed in Reference (f). The results are:

Gamma Sensitivity	2.9% reading plus 1.3% range
Detector/Electronics	1.3% reading

The 1.3% range uncertainty stems from propagated uncertainty in an offset related to the gamma sensitivity. The author of Reference 6 chose to sum rather than statistically combine these errors. This is overly conservative. The detector/electronics uncertainty will be statistically combined with the gamma sensitivity terms. For power range applications, the peak reading is assumed to be one-half the range. Therefore, 1.3% of range is equivalent to 2.6% of reading thus yielding a total of 5.5% of reading uncertainty for gamma sensitivity. Note that if the reading is very low, the error would become quite large. Upon statistical combination the total TIP system uncertainty is estimated to be 5.7%.

Statistical combination of physics and TIP system uncertainties then yield an Fq measurement uncertainty of 7%.

The FSAR adequately addresses the densification factor (1.011), therefore its discussion will not be presented here. The engineering hot channel factor of 1.032 (FSAR value) has been replaced by a statistical value of 1.026. Reference (g) presents the derivation of the statistical hot channel factor.

For completeness, in addition to the channel properties affecting PLHR, all of the channel parameters used to obtain off-nominal condition for Mode 8 safety analysis is presented below:

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<u>Channel</u>	<u>Control Band</u>	<u>Accuracy</u>
Power	+1, -2 MW	1.5 MW
Flow	+5% Nom.	0.125 Mlbm/hr
Temperature	$\pm 2^{\circ}\text{F}$	2°F
Pressure	± 50 psid	38 psid

The off-nominal PLHR should then be determined using the following factors:

$$\begin{aligned}\text{Fq Measurement Uncertainty} &= 1.07 \\ \text{Fq Engineering Hot Channel Factor} &= 1.026 \times 1.011 = 1.037 \\ \text{Operational CV and Inst. Accuracy} &= \frac{40.1 \text{ MW}}{37.6 \text{ MW}} = 1.066 \\ &(\text{Assumes Fq} = 2.4)\end{aligned}$$

Then the PLHR would become:

$$\text{PLHR} = 12 \text{ kW/ft} \times 1.07 \times 1.037 \times 1.066 = 14.2 \text{ kW/ft}$$

If any questions should arise with regards to the contents of this letter please contact me.

im

Attachments:

1-8 Figures 1 through 8
9-10 Tables 1 and 2

cc: S. A. Atkinson	J. L. Plum
M. A. Bray	T. F. Pointer
W. R. Carpenter	B. L. Rushton
C. H. Cooper	T. K. Samuels
G. A. Dinneen	W. A. Spencer
H. Henze	J. C. Stachew
G. L. Hunt	K. C. Sumpter
N. C. Kaufman	J. R. White
L. P. Leach	Central File
O. R. Meyer	D. G. Satterwhite File
S. A. Naff	
M. D. Peters

FIGURE 2

FIGURE 3

FIGURE 4

AVERAGE POWER CHANNEL

Off-Nominal High Average Power - $90.23/P/A + 2.5 \text{ MW}$

Off-Nominal Low Average Power - $90.23/P/A - 3.5 \text{ MW}$

FIGURE 5

FLOW CHANNEL

Off-Nominal Low Flow - 3.390/P/A - 0.125 Mlbm/hr

Nominal Trip Setpoint, LSSS and WCT Based on a P/A of 2.60

- * Must be greater than or equal to 50,000 lbm/hr + 1% Nom. Flow
- ** Must be greater than or equal to 50,000 lbm/hr.

FIGURE 6

T_H CHANNEL

Off-Nominal High T_H = 613°F

FIGURE 7

PRESSURE CHANNEL

Off-Nominal High Pressure - 2257 psia

Off-Nominal Low Pressure - 2081 psia

For Conservatism: High Pressure Channel Use $P_{atm} = 13$ psia.Low Pressure Channel Use $P_{atm} = 12$ psia

FIGURE 8
RELATIONSHIP OF RELATIVE POWER
VS. AXIAL POSITION FOR HOT PIN

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Attachment 9
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TABLE I
11191, L2-3 PRE-LOOSE CONDITIONS, 19 HRS DEPLETION AT LOW FLOW,
55.5°F INLET TEMP., 1.4×10^6 lb/hr FLOW, 75% POWER

DEPLETION PLANES BOUNDING THE AXIAL DEPLETION BLOCKS AND THEIR DISTANCE FROM THE BOTTOM OF THE CORE		AXIAL LENGTH OF EACH DEPLETION BLOCK		RELATIVE POWER DENSITY (RPD) RPD = $\frac{\text{AVER. POW. IN FUEL PIN BLOCK}}{\text{AVERAGE CORE POWER}}$		
NO.	DISTANCE (in)	BLOCK NO.	AXIAL LENGTH (in)	HOT PIN	AVERAGE PIN IN CENTER ASSEMBLY	AVERAGE PIN IN CORE
4	0.0	1	20.6	0.884	0.847	0.584
5	20.6	2	44.4	0.899	0.833	0.582
6	65.0	3	87.4	1.310	1.200	0.843
7	152.4	4	152.4	1.906	1.749	1.233
8	304.8	5	101.6	2.278	2.093	1.484
9	406.4	6	35.8	2.317	2.130	1.517
10	442.2	7	44.4	2.254	2.072	1.480
11	486.6	8	46.7	2.388	2.199	1.574
12	533.3	9	76.2	2.447	2.257	1.620
13	609.5	10	76.2	2.419	2.235	1.610
14	685.7	11	76.2	2.318	2.145	1.553
15	761.9	12	101.9	2.118	1.963	1.430
16	863.8	13	44.4	1.846	1.711	1.255
17	908.2	14	158.5	1.720	1.600	1.178
18	1066.7	15	152.4	1.330	1.240	0.917
19	1219.1	16	66.3	0.987	0.920	0.679
20	1285.4	17	44.4	0.774	0.719	0.525
21	1329.8	18	41.7	0.675	0.624	0.429
22	1371.5	19	152.4	0.455	0.413	0.250
23	1523.9	20	152.4	0.198	0.179	0.104
24	1676.3					

5

3) H identifies Hot Pin

[illegible]

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APPENDIX A-4
NOMINAL PRESSURE DECREASE
FOR L2-3 MODE 8

APPENDIX A-2

PIN POWER DISTRIBUTION SPREAD AND
UNCERTAINTY FOR 12 KW/FT TEST

INTEROFFICE CORRESPONDENCE

date February 2, 1979
to E. C. Anderson
from S. A. Atkinson *J. A. Atkinson*
subject NOMINAL PRESSURE DECREASE FOR L2-3 MODE 8 - Atki-3-79

- Refs:
- (a) Richard C. Gottula and Steven A. Eide, "Evaluation and Results of LOFT Steady State Departure from Nucleate Boiling Tests", TREE-NUREG-1043, April 1977
 - (b) Richard C. Gottula, "LOFT Steady State Critical Heat Flux Tests (6.9 to 13.8 MPa)", TREE-NUREG-1166 (NUREG/CR-0237), June 1978
 - (c) Steven A. Eide and Richard C. Gottula, "LOFT Steady State Critical Heat Flux Tests (470 to 680 Kg/s·m²)," TBP.

COBRA analyses for steady-state limits and control rod withdrawal accidents (CRWA) for operation of the LOFT reactor in Mode 8 prior to the L2-3 LOCE have indicated the necessity of changing some of the reactor operating conditions for L2-3 in order to show compliance with the Damage Criteria. The change that appears to gain the most in safety margin with a minimal effect on previous analyses or the results of a LOCE is a reduction in operating pressure of the primary coolant system (PCS). A reduction in the nominal operating pressure (in the pressurizer) from 2262 psia to 2169 psia (about a 100 psi decrease) increases the MDNBR for the CRWA's by about 4%, when using the LOFT-3 CHF correlation. This increase in safety margin due to a reduction in pressure is required for the L2-3 Mode 8 steady-state analysis to show compliance with Damage Criteria A ($MDNBR \geq 1.14$ using LOFT-3) for operation at the worst-case power trip level. As you know, we are finding it very difficult to show compliance with Damage Criteria B for the slow CRWA's. The gain in safety margin from a decrease in operating pressure (and maybe more) is required for the CRWA analyses. Therefore, I strongly recommend that nominal operating pressure in the LOFT PCS be reduced 93 to 100 psi for L2-3 operation.

The attached plots show the influence of pressure on the critical heat flux (CHF), or DNB, for data taken from the simulated LOFT fuel bundles. This data is reported in References a, b, and c. The high flow data from Reference a (loop flows from about 1.7 Mlbm/h to 4 Mlbm/h) and the low flow data from Reference c (loop flows from about 0.8 Mlbm/h to 1.3 Mlbm/h) were combined to derive the LOFT-3 correlation. Figures 1 through 3 show the low flow data (ANC-6 bundle data). The CHF data is plotted as a function of the enthalpy at the point of DNB. These plots definitely show an increase in CHF as pressure is decreased. Figures 4 and 5 show CHF data for the same flow ranges (flow being expressed as mass velocity in the hot channel) including low

February 2, 1979
E. C. Anderson
Atki-3-79
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pressure CHF data from Reference b. The data indicates that the effect of reduced pressure on increased CHF probably extends down to 1000 psia. Figure 6 shows CHF data from Reference a for the low end of the high flow data, and low pressure data for the same flows from Reference b. For this data, any pressure effect between 2000 and 2300 psia is lost in the data scatter. However, there is a visible increase in CHF as pressure is decreased from the 2000 to 2300 psia group to 1500 or 1000 psia. Both the LOFT-1 correlation (based only on high flow data) and the LOFT-3 correlation indicate an increase in CHF as pressure is decreased at these flows (0.69 to 0.84 Mlbm/h-ft²).

Figures 7 and 8 show how the low flow data compares to the LOFT-3 prediction. As seen in Figure 7, at the ANC-6 flows, LOFT-3 underpredicts most of the data. For the higher flow rates for the ANC-2 data (0.69 to 0.84 Mlbm/h-ft²), LOFT-3 overpredicts most of the data. However, the mass velocity range of concern for the L2-3 safety analyses is near to 0.5 Mlbm/h-ft² at an enthalpy near 680 Btu/lbm. For these conditions, LOFT-3 is expected to underpredict the data. The LOFT-3 prediction is compared to the CHF data in Figure 1 for 0.5 Mlbm/h-ft². It appears that LOFT-3 overpredicts the effect of pressure change on CHF. However, for a change in high off-nominal pressure from 2350 psia to 2250 psia, due to lowering nominal PCS pressure by 100 psia, LOFT-3 will still be underpredicting the data.

In conclusion, a 93 to 100 psia reduction in nominal operating pressure is recommended for LOFT operation for L2-3, and for operation for other LOCE's with a nominal MLHGR of 12 Kw/ft or greater. This reduction in pressure will increase the safety margin for operation in Mode 8 prior to the blowdown as calculated using the LOFT-3 correlation for CHF. The LOFT bundle data for CHF at low flows (ANC-6 data) supports the calculated increase in safety margin (increase in DNBR) for a decrease in pressure.

The corresponding trippoints and justification for those trippoints for operation with reduced nominal pressure will be presented in following correspondence after a review of accidents for L2-3 Mode 8 and other operational modes. At this time, it appears that the high pressure worst-case trip (WCT) should be no greater than 2400 psia. The low pressure WCT should be 2024 psia (for a 100 psia nominal pressure decrease) to 2031 psia (for a 93 psia nominal pressure decrease).

ul

Attachments:
As stated

cc: B. S. Anderson
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TAB File 3.13

FIGURE 1

ANC-6 DATA

- - 2000 PSia (13.8 MPa)
- * - 2200 PSia (15.2 MPa)
- - 2400 PSia (16.5 MPa)

$G = 0.4 \text{ Mlbm/hr-ft}^2$
(540 kg/s.m²)

CHF
MBTU/hr-ft²

1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3

580 600 620 640 660 680 700 720 740 760 780 800

LOCAL ENTHALPY BTU/lbm

FIGURE 2

FIGURE 4

FIGURE 6

ANL-6 DATA

- - 2000 PSIA
- * - 2200 PSIA
- - 2400 PSIA

$G = 0.35, 0.4, \& 0.5 \text{ mlbm/h-ft}^2$

USING LOFT-3
 $q_{CHF}/MEAS / q_{CHF}/PRED$

LOCAL ENTHALPY BTU/lbm

FIGURE 7

FIGURE 8

L2-3 MODERATOR AND DOPPLER REACTIVITY FEEDBACK COEFFICIENT SPREAD

The safety analyses for the LOFT FSAR, zero power physics, and power range testing assumed the following values for the moderator and Doppler reactivity feedback coefficients^[a]:

	<u>High Value</u>	<u>Low Value</u>
Moderator Coefficient	$9.1 \times 10^{-3} \Delta \rho / \text{lbm}/\text{ft}^3$	$2.0 \times 10^{-3} \Delta \rho / \text{lbm}/\text{ft}^3$
Doppler Coefficient	$-1.52 \times 10^{-5} \Delta \rho / ^\circ\text{F}$	$-0.30 \times 10^{-5} \Delta \rho / ^\circ\text{F}$

Either the high or low value of coefficient, whichever was most limiting for the accident being analyzed, was assumed. However, based on moderator temperature and power coefficient measurements taken during zero power and power range physics testing, it appears that it is possible to reduce the conservative spread in these coefficients used in the safety analyses.

From power range physics testing, the following coefficients were measured:

% Full power	30	50	75
Average core temperature (°F)	533	537	541
PCS boron concentration	1096	970	861
Moderator temperature coefficient (¢/°F)	2.56	3.01	2.59
Power coefficient (¢/% power)	2.29	2.28	1.99

For 2250 psia pressure and for the range of average temperatures for the measurements, the change in moderator density per change in moderator temperature is (for water) $0.063 \text{ lbm}/\text{ft}^3/^\circ\text{F}$. The measured moderator temperature coefficients can be converted to the moderator density coefficient used for LOFT safety analysis using the above conversion factor. For LOFT, $\beta = 0.00725$. Therefore, the power range measured moderator density coefficients are:

% Full power	30	50	75
Average core temperature ($^{\circ}\text{F}$)	533	537	541
PCS boron concentration	1096	970	861
Moderator density coefficient ($\Delta\rho/\text{lbm}/\text{ft}^3$)	2.94×10^{-3}	3.46×10^{-3}	2.98×10^{-3}

The moderator coefficients measured during the zero power physics tests are discussed in Reference (b). The measured range of moderator density coefficients for power range testing was 2.9×10^{-3} to $3.5 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$. For the zero power physics tests, for the same range of moderator temperature (530 to 540°F) the moderator density coefficient varies from 0.37 to $0.42 \Delta\rho/\text{lbm}/\text{ft}^3$ or 2.7×10^{-3} to $3.1 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$ for a boron concentration of approximately 1500 ppm.

Predicted reactivity coefficients were calculated and reported in LTR 111-52(c). For a moderator temperature of 540°F and a boron concentration of 1100 ppm (above that existing during power range testing), the predicted moderator coefficient is $3.8 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$ (from Figure A-5-1). The coefficient becomes larger for lower boron concentration. Therefore, it is seen that the increased coefficients are 9 to 27% smaller than the predicted coefficients in LTR 111-52. Therefore, the use of the values from LTR 111-52 should be sufficient as a high value for the moderator coefficient. Especially since the LTR 111-52 values are for the rods all the way out (withdrawn), and thus should be low values.

The range of boron concentration in the coolant or moderator for L2-3 operation is predicted to be 600 to 1000 ppm. The moderator temperature will be high for L2-3 Mode 8 operation. Figure 8 of LTR 111-52 shows that both these conditions (low boron concentration, high temperature) result in higher values for the moderator coefficient. For 600 ppm boron concentration and 584°F moderator temperature (average reactor coolant temperature or T_{ave}), Figure 8 of LTR 111-52 predicts a moderator temperature coefficient of $-41.5 \times 10^{-5} \Delta\rho/^{\circ}\text{F}$, or a density coefficient of $6.5 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$. This value can be taken as the maximum value for moderator coefficient for L2-3 Mode 8 operation, even if boron concentration should be as low as 500 ppm. For 500 ppm, the density coefficient would be $6.76 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$, only 4% greater, and power range measurements showed that the actual coefficients are 9 to 27% less than calculated.

For low moderator temperature for power operation, 500°F, a moderator density coefficient from the zero power physics tests was $0.277 \Delta\rho/\text{lbm}/\text{ft}^3$ or $2.0 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$, equal to the low FSAR value. But, this value is lower than reasonable for L2-3 operation. The minimum moderator coefficient measured during power range testing was $2.94 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$. Since this value was for a boron concentration higher than likely for L2-3 operation, and for a temperature lower than likely for L2-3 operation (533°F T_{ave}), then it should be reasonable to use this value as a minimum for L2-3 operation. If the minimum power range value is reduced by an extra 10%, it becomes $2.6 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$. This was the value chosen as a minimum for the L2-3 safety analyses.

A Doppler coefficient can be derived from the measured power coefficients. The full power MLHGR is approximately 19 Kw/ft. A 1% power change therefore represents an increase in MLHGR of approximately 0.2 Kw/ft. From previous calculations for LOFT fuel pin temperatures using FRAPS^[d], for a change in MLHGR from 16 Kw/ft to 16.2 Kw/ft (a 0.2 Kw/ft change), the change in fuel temperature is approximately 30°F. Using an equivalence of 30°F = 1% power change, then the power range measured Doppler coefficients are (for $\beta = 0.00725$):

% Full power	30	50	75
Doppler coefficient ($\Delta\rho/^\circ\text{F}$)	-0.55×10^{-5}	-0.55×10^{-5}	-0.63×10^{-5}

From LTR 111-52 for hot reactor conditions and 1313 ppm boron, the predicted Doppler coefficient is $-0.98 \times 10^{-5} \Delta\rho/^\circ\text{F}$. The measured coefficients are seen to be significantly smaller than those from LTR 111-52. Therefore, the LTR 111-52 values should be sufficient as a maximum value for Doppler coefficient. Adding an extra 10% to the LTR 111-52 (hot reactor) value gives the maximum value used for L2-3 safety analyses, or $-1.08 \times 10^{-5} \Delta\rho/^\circ\text{F}$. A minimum value for the Doppler coefficient was derived by reducing the minimum power range testing value by 20% to obtain $0.44 \times 10^{-5} \Delta\rho/^\circ\text{F}$.

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APPENDIX A.5

In conclusion, for the L2-3 safety analyses, the following spread (maximum and minimum) values were used for moderator density and Doppler reactivity feedback coefficients:

	<u>Maximum</u>	<u>Minimum</u>
Moderator density coefficient ($\Delta\rho/\text{lbm/ft}^3$)	6.5×10^{-3}	2.6×10^{-3}
Doppler coefficient ($\Delta\rho/^\circ\text{F}$)	-1.08×10^{-5}	-0.44×10^{-5}

REFERENCES

- [a] LOFT Integral Test System Final Safety Analysis Report, Revision 1, (August 1977), Section 15.2.2.3.
- [b] B. L. Rushton and T. M. Howe, Comparison of LOFT Zero Power Physics Testing Measurement Results with Predicted Values, LTR 111-107 (June 26, 1978).
- [c] A. J. Scott, Temperature Coefficients and Boron Worth for LOFT I at Beginning of Life, LTR 111-52, (August 31, 1973)
- [d] S. A. Eide, Fuel Melting Limitations on Power for Various Control Rod Withdrawals - 0 to 2000 EFPH, LTR 1111-21, Rev. 1 (June 1977).

FIGURE A-5-1. MODERATOR COEFFICIENTS OF REACTIVITY FROM LTR 111-52 VS. BORON CONCENTRATION.

APPENDIX A-5

L2-3 MODERATOR AND DOPPLER REACTIVITY
FEEDBACK COEFFICIENT SPREAD

APPENDIX A-6

POWER DISTRIBUTION ASSUMPTIONS FOR L2-2 THERMAL SAFETY ANALYSIS

EG&G Idaho

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Central Files

**LTR 111-127
APPENDIX A-6**

INTEROFFICE CORRESPONDENCE

date May 8, 1978

to T. M. Howe and R. A. Dimenna

from D. F. Elger *S. A. Atkinson for D.F.E.*

subject POWER DISTRIBUTIONS FOR L2-1 and L2-2 THERMAL SAFETY ANALYSIS - DFE-1-78

references a) S. A. Eide and R. C. Gottula, Evaluation and Results of LOFT Steady State Departure from Nucleate Boiling Tests, TREE-NUREG-1043, (April 1977).

b) L. S. Tong, Boiling Crisis and Critical Heat Flux, Oak Ridge, Tennessee: USAEC Technical Information Center, 1972.

The intention of this letter is to discuss the core thermal power distributions used for DNB analysis. The letter is separated into three parts: (a) Power profile influence on calculated DNBR, (b) Incorporating power profiles into COBRA and (c) Uncertainties applied to the power profile. These three parts are included as an attachment.

For ESA work, DNBR is defined at an axial location of a fuel rod as:

$$DNBR = \frac{q''_{DNB}}{q''_{LOC} \cdot F_f}$$

q''_{DNB} , the DNB heat flux, is defined as the heat flux which will produce DNB at that location. It is a function of local fluid conditions surrounding the fuel rod and is calculated using the LOFT-1 correlation (Ref. a). Specifically, DNB heat flux is a function of local mass flux, local equilibrium quality and system pressure.

q''_{LOC} is the local heat flux at the axial location of the fuel rod.

F_f is the Tong nonuniform flux factor (Ref. b). The LOFT DNB correlation is based on data obtained from uniformly heated fuel rod models. The nonuniform flux factor is to account for the nonuniform power profiles encountered under reactor conditions. Typical values range from 1.01 to 1.06.

jh

Attachment: As stated

LTR III-127
APPENDIX A 6

Part I - Power Profile Influence on Calculated DNBR

In the DNBR calculation, the power profile will influence local mass flux, the Tong nonuniform flux factor, equilibrium quality and local heat flux. Power profile influence on local mass flux and the Tong nonuniform flux factor will not be considered in this letter.

In order to provide flexibility to meet desired L2-1 and L2-2 test conditions, thermal analysis is done as a function of the power profile peak-to-average ratio, F_Q .

For nominal L2-2 conditions the predicted value of peak-to-average is 2.43(1). The intention of this letter is to establish criteria for characteristics of a conservative power profile as a function of F_Q . Clearly the most limiting power profile at a given F_Q is the profile which produces the highest value of equilibrium quality and local heat flux at the point of MDNBR. Equilibrium quality increases as more heat is deposited in the coolant up to the point of MDNBR.

There are two criteria which influence the value of equilibrium quality at a given F_Q at the point of MDNBR. The first criteria is the location of the power peak. DNBR typically occurs a short distance following the power peak. As the location of the power peak moves down the core (towards the core exit) the value of equilibrium quality at the point of MDNBR increases. Thus, MDNBR decreases and so the power profile which peaks farthest down stream in the core is the most conservative. An analysis of predicted power profiles which could occur in L2-1, L2-2 or under high flow conditions shows that the farthest down stream in the core a power profile peaks is 25.5 inches (7.5 inches below core midplane). Note: These profiles are found in References 1, 2 and 3. So, all power profiles used for ESA had the peak power located at 7.5 inches below core midplane. Additionally, as an added conservatism, the peak heat flux is assumed to be flat between 25.5 and 27 inches.

The other criteria which influences equilibrium quality also influences the local heat flux at the point of MDNBR. This criteria is the shape of the axial power profile. As a power profile becomes flatter, at a given F_Q , the values of equilibrium quality and local heat flux increase. Thus, at a given F_Q , flatness is the criteria for a conservative power profile.

Choice of a flat axial power profile, for a given value of F_Q , is based on using a high value of the hot channel enthalpy rise factor, $F_{\Delta H}$. $F_{\Delta H}$ is defined as the integral of the hot fuel rod power divided by the integral of the average fuel rod power. Assuming separability of radial and axial power shapes, $F_{\Delta H}$ has the same value as the power peaking in the radial direction. Raising the value of $F_{\Delta H}$ is done by simply scaling a predicted power profile.

A high value of $F_{\Delta H}$ insures a flat axial power profile because of the following: The value of $F_{\Delta H}$ is equivalent to the value of radial peaking. Thus, the total peaking, F_Q is equal to the product of $F_{\Delta H}$ and the axial peaking, $F_Q = F_{\Delta H} \cdot F_Q^{Ax}$. Therefore, at a fixed value of F_Q a high value of $F_{\Delta H}$ means a low value of F_Q^{Ax} . And as the axial peaking, F_Q^{Ax} , decreases the axial power profile will flatten out.

Another viewpoint is that $F_{\Delta H}$, as previously defined, is approximately equal to enthalpy rise in the hot subchannel divided by core average enthalpy rise. Thus, as $F_{\Delta H}$ increases, more heat is dumped into the hot subchannel. At a fixed F_Q , this additional heat could only be explained by higher relative power densities along the axial power profile. This is equivalent to flattening out the axial power profile. The nominal power profile has a value of $F_{\Delta H} = 1.53$. An analysis of many predicted power profiles showed that $F_{\Delta H}$ never exceeded a value of 1.6⁽⁴⁾. Thus all power profiles used in analysis had value of $F_{\Delta H} \geq 1.6$.

For ESA the range of peaking analyzed is $2.3 \leq F_Q \leq 2.78$. Four profiles were used to cover this range. The profiles were chosen on the basis of location of the power peak (7.5" below core midplane) and the value of F_Q for that profile (not on the basis of the conditions for which they were generated). All profiles were then scaled such that $F_{\Delta H} = 1.6$ with the exception of the profile for $F_Q = 2.78$. This profile has a $F_{\Delta H} = 1.64$. The actual power profiles which were used are:

Case	F_Q	F_Q^{AX}	$F_{\Delta H}$
2000 EFP High Flow ⁽³⁾	2.3	1.44	1.6
1000 EFP High Flow ⁽³⁾	2.43	1.52	1.6
L1012 ⁽¹⁾	2.63	1.64	1.6
L1020 ⁽²⁾	2.78	1.70	1.64

In summary, the choice of a conservative power profile at a given F_Q is met by using two criteria. The criteria are: 1) the location of the power peak is 7.5 inches below core midplane and 2) a value of $F_{\Delta H} \geq 1.6$.

Part II - Incorporating Power Profiles Into COBRA

DNB analysis is done using the computer code COBRA to predict reactor coolant conditions. Then the computer code "DNBR" is used to calculate MDNBR. (This is now called as a subroutine to COBRA). The description of the model used in COBRA is discussed in Reference 5.

In the COBRA model, subchannel number 15 is the subchannel with the most limiting fluid conditions⁽⁶⁾. This means that the DNB heat flux, q''_{DNB} , is lower in subchannel 15 than in any other subchannel at typical MDNBR locations. A sketch of this subchannel and a few surrounding subchannels is shown in Figure 1. Subchannel 15 is composed of three fuel rods and one guide tube. Based on physics predictions fuel rod number 13 is the hottest fuel rod. Because this fuel rod does not occur in channel number 15, fuel rod number 20 is artificially raised in power to become the hottest fuel rod. Thus, the most limiting subchannel in terms of fluid conditions is combined with the hottest fuel rod.

Power profile input into COBRA is done in an axial relative power vs relative location table, and as a radial relative rod power factor for each fuel rod.

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May 8, 1978
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For ESA all radial factors are 1.0 (flat radial assumption) and the radial and axial contributions to the power shape are combined in the axial heat flux table. An exception is the fuel rods forming subchannel number 15. The radial factors for these subchannels are greater than 1.0 to allow for uncertainties, as described in Part III of this letter.

All fuel rods in the model have the same axial power shape. The power shape used for a given F_Q is the predicted hot pin power profile scaled such that $F_{\Delta H} \geq 1.6$.

The values of relative power density which go into COBRA are found from physics data in the following manner. The predicted hot pin power profile is plotted as a function of distance. Data points are taken from the center of each calculation block. Then a smooth curve is drawn through these points and from this curve the axial heat flux table for COBRA input is found. The location of the peak is stretched over a distance of at least 1.5 inches.

In the COBRA model, the core thermal power is distributed as follows: 95.09% of the core thermal power is deposited in the fuel rods, 2.29% is deposited in the coolant directly and 2.62% is deposited in the core support structures, reflectors and other core materials⁽⁷⁾.

Part III - Uncertainties Applied to the Power Profile

The uncertainties and conservatisms applied to the power profile are divided into two groups: (1) Factors influencing the local heat flux at the point of MDNBR and (2) factors influencing equilibrium quality at the point of MDNBR.

Factors which influence the local heat flux are local factors. That is, they are only applied to the local heat flux at the point of MDNBR. So these factors are used only to calculate the DNBR and they have no effect on the DNB heat flux value. Thus, they are used only to calculate MDNBR.

Since equilibrium quality is a function of enthalpy rise in the hot subchannel, factors effecting equilibrium quality are applied over the full length of the hot subchannel. These factors are only used in COBRA for calculation of the DNB heat flux. Typically, these factors are applied by increasing the heat flux along the full length of the three rods forming the hot subchannel. Sometimes the factors are applied only to the hottest fuel rod in the hot subchannel.

The conservatisms applied to the local heat flux are:

1. F_U : Uncertainty on the value of the peak to average ratio. The LOFT reactor safety limits for L2 pre-LOCE operation are to be set on the basis of a measured P/A ratio. This uncertainty makes an allowance for measurement and calculational errors in the determination of the P/A ratio. It has a value of 7%⁽⁸⁾.
2. F_Q^E : Engineering hot spot factor. This factor has a value of 3.2% and is to account for possible heat flux increases due to manufacturing variations. It is determined statistically from data on fuel pellet manufacturing tolerances for fuel enrichment, fuel density and fuel pellet diameter. The value of 3.2% represents a probability limit of three standard deviations.⁽⁹⁾

3. F_{QH}^D : Densification hot spot factor. This factor takes into account increase in local heat flux due to densification induced fuel stack shortening. Credit is then taken for thermal expansion⁽⁵⁾. The value of this factor is 1.1%.

The conservatisms which are applied to the hot subchannel which effect the value of equilibrium quality are:

1. $F_{\Delta H}^U$: Hot channel enthalpy rise uncertainty factor. The factor is to account for measurement and calculational errors in the determination of the hot channel enthalpy rise. It has a value of 7% and is applied to the three fuel rods which form the hot subchannel⁽⁸⁾.
2. $F_{\Delta H}^E$: Engineering hot channel factor. This factor takes into account manufacturing variations in average pellet density, enrichment and diameter between fuel rods. However, the required data base is not available for the LOFT fuel assemblies. So as recommended in Reference 10, the engineering hot spot factor, a local uncertainty of 3.2%, is applied over the fuel length of the hot fuel rod. The application of a local engineering factor as an integral uncertainty to three fuel rods would be overly conservative.

Westinghouse reports values of the engineering hot spot factor and hot channel factor of 3% and 1.0%, respectively.⁽⁸⁾ By comparison, it would be expected that the engineering hot channel factor for the LOFT reactor would be approximately 1%. The approach used for ESA is roughly equivalent to applying a 1% factor to all three fuel rods forming the hot subchannel. Thus, the approach used is reasonable.

3. $F_{\Delta H}^D$: Densification enthalpy rise factor. A factor of 1.1% is applied to the full length of the three fuel rods forming the hot subchannel. This factor is to account for increased energy deposition in the hot subchannel due to increased linear power caused by densification induced fuel stack shortening.⁽⁵⁾ Credit is taken for thermal expansion.

Two additional conservatisms used in DNB analysis as discussed earlier are the use of a flat radial power profile and the use of $F_{\Delta H} \geq 1.6$.

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2. E. F. Aber, LOFT Core I: High Flow Power Distributions as a Function of Control Rod Position and Core Power Level, LTR 111-81 (July 1971).
3. B. L. Rushton, LOFT-I: Results from Three Dimensional PDQ-7 Calculations with Thermal Feedback and Depletion to 2000 EFPH, LTR 111-60 (November 1976).
4. T. M. Howe, Personal Communication, October, 1977.
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6. R. C. Gottula, Personal Communication, October 1977.

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7. T. E. Young, Energy Deposition in the LOFT Reactor at 54.71 MW, LTR 111-58 (August 1974).
8. H. W. Graves, Jr., A Review of Uncertainties Associated with the Evaluation of Peak Linear Power Density and DNB Flux During LOFT Steady State Operation, (December 30, 1977).
9. R. C. Gottula, LOFT Core Hot-Spot and Hot Channel Factors Due to Fuel Pellet Density, Enrichment, and Diameter Fabrication Tolerances, LTR 111-19 (May 1975).

FIGURE 1 SUBCHANNEL 15*

* No thermocouples shown

APPENDIX A-7
STATISTICAL EVALUATION OF THE LOFT
HEAT FLUX ENGINEERING HOT SPOT FACTOR

cc: B. S. Anderson
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INTEROFFICE CORRESPONDENCE

LTR 111-127
APPENDIX A-7

date February 27, 1979

to E. C. Anderson

from N. F. Fausz *NFF*

subject STATISTICAL EVALUATION OF THE LOFT HEAT FLUX ENGINEERING HOT SPOT FACTOR - NFF-1-79

- Ref: (a) R. C. Gottula, "LOFT Core Hot Spot and Hot Channel Factors Due to Fuel Pellet Density, Enrichment, and Diameter Fabrication Tolerances", LOFT Technical Report LTR 1111-19, TR-635 (May 21, 1975).
- (b) L. S. Tong and S. J. Weisman, Thermal Analysis of Pressurized Water Reactors, Published by American Nuclear Society (1970) pp 239.
- (c) S. D. Matthews and J. P. Poloski, "MOCARS: A Monte Carlo Code For Determining Distribution and Simulation Limits and Ranking System Components by Importance", TREE-1138 (Revision 1), August 1978.

The LOFT heat flux engineering hot spot factor (F_q^E) has been reevaluated using statistical techniques. The statistical treatment resulted in a value of $F_q^E = 1.026$ as opposed to the old value of $F_q^E = 1.032$, Ref. (a).

The old value of $F_q^E = 1.032$ was obtained using the definition of F_q^E from Ref. (b):

$$F_q^E = (W_{\text{Max Local}}/W_{\text{Nom}})(e_{\text{Max Local}}/e_{\text{Nom}})$$

where

$W \equiv$ Fuel pellet weight per unit length

$e \equiv$ Fuel pellet enrichment

Subscripts: Max Local = Maximum local value of subscripted quantity

Nom = Nominal value of subscripted quantity

The fuel pellet weight per unit length for LOFT was determined from 160 data points, Ref. (b), taken to be normally distributed with a nominal (mean) value of 17.1627 gm/in. and a standard deviation of 0.10742 gm/in. The value for $W_{\text{Max Local}}$ was then taken at three standard deviations above nominal.

For the fuel pellet enrichment, only 23 data points were available, and the data does not follow a normal distribution (Figure 1 shows a histogram of the enrichment data and a normal distribution with the same mean = 3.9784 and standard deviation = 0.017840). For that reason, the value of $e_{\text{Max Local}}$ was taken at the upper bound of the manufacturing tolerance limit. The manufacturing tolerance limits for fuel pellet enrichment are 4.0 ± 0.05 wt%. Thus, $e_{\text{Max Local}} = 4.05$. The LOFT heat flux engineering hot spot factor was then obtained as follows:

$$F_q^E = \left(\frac{17.1627 + 3(0.10742)}{17.1627} \right) \left(\frac{4.05}{4.0} \right) = 1.032$$

In the statistical evaluation of F_q^E , the mean and standard deviation of fuel pellet weight per unit length were determined at the 99% confidence level (see Attachment 1). The resulting values are

$$\mu_w \leq 17.1827 \text{ gm/in.}$$

$$\sigma_w \leq 0.12341 \text{ gm/in.}$$

at the 99% confidence level.

The MOCARS, Ref. (c), computer code was then used to determine the mean and standard deviation of F_q^E by use of a Monte Carlo technique. As input to MOCARS, the fuel pellet weight per unit length was input as a normal distribution with $\mu_w = 17.1827$ and $\sigma_w = 0.12341$. Due to the fact that the 23 fuel pellet enrichment data points were non-normally distributed, the data points were input into MOCARS as an empirical distribution. The MOCARS computer code was then used to perform a Monte Carlo sampling of the function

$$F_q^E = (w_{\text{Max Local}}/w_{\text{Nom}})(e_{\text{Max Local}}/e_{\text{Nom}}), \text{ where}$$

the values of $w_{\text{Max Local}}$ and $e_{\text{Max Local}}$ were picked from their respective cumulative distribution functions by MOCARS for each sample of F_q^E . A total of 3,000 samples of F_q^E were used in MOCARS to determine the sample mean ($\bar{X}_{F_q^E}$) and sample standard deviation ($S_{F_q^E}$) of the function. The results of MOCARS for 3,000 samples gave;

$$\bar{X}_{F_q^E} = 0.9995$$

$$S_{F_q^E} = 0.00838.$$

Figure 1
ENRICHMENT DATA

MOCARS also determined that F_q^E follows a normal distribution at the 99% confidence level using the Kolmogorov-Smirnov goodness-of-fit test. A histogram of the MOCARS results for 3,000 samples of F_q^E compared with a normal distribution having mean = 0.9995 and standard deviation = 0.00838 is shown in Figure 2. Because the MOCARS code output for F_q^E has a normal distribution, the Student's t Distribution and Chi-Square Distribution, as shown in Attachment 1, can be used to obtain:

$$\mu_{F_q^E} \leq 0.9999 \approx 1.000$$

$$\sigma_{F_q^E} \leq 0.00864$$

at the 99% confidence level.

The value of F_q^E is then taken to be:

$$F_q^E = 1.000 + 3\sigma_{F_q^E} = 1.026.$$

Taking the value of F_q^E at three standard deviations above its mean is equivalent to saying that there is a 99.9% probability that the actual value of F_q^E is less than or equal to 1.026 when F_q^E follows a normal distribution with the mean and standard deviation having the values shown above.

If the engineering hot spot factor were to be taken at two standard deviations above its mean, then

$$F_q^E = 1.000 + 2\sigma_{F_q^E} = 1.017.$$

For a two standard deviation level, there is still a 97.7% probability that the actual value of F_q^E is less than or equal to 1.017.

jb

Attachment:
As stated

Figure 2

MOCARS RESULTS FOR ENGINEERING HOT CHANNEL FACTOR

- A. Use of the Student's t Distribution to determine the mean of a normal distribution at a specified confidence level.

$$P \left\{ -t_{\alpha, v} \leq \frac{(\bar{X} - \mu) \sqrt{n}}{s} \right\} = 1 - \alpha$$

this can be rewritten as

$$P \left\{ \mu \leq \bar{X} + \frac{t_{\alpha, v} s}{\sqrt{n}} \right\} = 1 - \alpha$$

where $\bar{X}$ = sample mean

s = sample standard deviation

n = number of samples taken

$v = n-1$ = degrees of freedom

$1-\alpha$ = fractional confidence level

α = tail probability of the t distribution

$t_{\alpha, v}$ = value of t for tail probability α and v degrees of freedom

e.g. $\bar{X} = 17.1627$

$s = 0.10742$

$n = 160$

$v = 159$

$1-\alpha = 0.99 = 99\%$

then, $t_{0.01, 159} = 2.350$

thus, $\mu \leq 17.1627 + \frac{(2.350)(0.10742)}{\sqrt{160}} = 17.1827$

at the 99% confidence level.

- B. Use of the Chi-Square Distribution to determine the standard deviation of a normal distribution at a specified confidence level.

$$P \left\{ \chi^2_{1-\alpha, v} \leq \frac{vs^2}{\sigma^2} \right\} = 1 - \alpha$$

this can be rewritten as

$$P \left\{ \sigma \leq \left[\frac{vs^2}{\chi^2_{1-\alpha, v}} \right]^{\frac{1}{2}} \right\} = 1 - \alpha$$

where $\chi^2_{1-\alpha, v}$ = value of Chi-Square for probability $1-\alpha$ and v degrees of freedom

$v = n-1$ = degrees of freedom

s = sample standard deviation

σ = population standard deviation

Example: $s = 0.10742$

$n = 160$

$v = 159$

$1-\alpha = .99 = 99\%$

then $\chi^2_{.99, 159} = 120.47$

$$\sigma \leq \left(\frac{(159)(.10742)^2}{120.47} \right)^{\frac{1}{2}}$$

$\sigma \leq 0.12341$ at the 99% confidence level.

APPENDIX A-8

L2-3 COBRA MODELS INPUT

TABLE A-8-1

L2-3 COBRA MODELS
RADIAL PIN POWER
FACTORS

ROD NO.	INPUT TYPE NO.	DATA DIA (IN)	RADIAL POWER FACTOR
1	1	.5460	0.0000
2	1	.4220	.9390
3	1	.4220	.9390
4	1	.4220	.9660
5	1	.4220	.9480
6	1	.4220	.9460
7	1	.5460	0.0000
8	1	.4220	.9900
9	1	.4220	.9740
10	1	.4220	.9850
11	1	.4220	.9650
12	1	.4220	.9920
13	1	.4220	1.0000
14	1	.4240	1.1080
15	1	.5460	0.0000
16	1	.5460	0.0000
17	1	.4220	.9360
18	1	.4220	.9550
19	1	.5460	0.0000
20	1	.4240	1.0570
21	1	.4240	1.0530
22	1	.4220	.8390
23	1	.4220	.8760
24	1	.4220	.8870
25	1	.4220	.9020
26	1	.4220	.9210
27	1	.4220	.9160
28	1	.4220	.9070
29	1	.4220	.7790
30	1	.4220	.8020
31	1	.4220	.8250
32	1	.4220	.8400
33	1	.4220	.8540
34	1	.4220	.8640
35	1	.4220	.8670
36	1	.4220	.8660
37	1	.4220	.7310
38	1	.4220	.7550
39	1	.4220	.7760
40	1	.4220	.7970
41	1	.4220	.8120
42	1	.4220	.8250
43	1	.4220	.8350
44	1	.4220	.8380
45	1	.4220	.8370
46	1	.7913	.6980
47	1	.8440	.7200
48	1	.8440	.7460
49	1	.7385	.7740
50	1	.7385	.7980
51	1	.8440	.8090
52	1	.7385	.8230
53	1	.7385	.8310
54	1	.8440	.8290
55	1	3.8520	.6230
56	1	1.2660	.6530
57	1	1.2660	.6800
58	1	1.0550	.7120
59	1	1.0550	.7490
60	1	1.0550	.7660
61	1	.9495	.7690
62	1	1.1610	.7660
63	1	1.0550	.7650
64	1	6.4880	.5010
65	1	1.6880	.5360
66	1	1.6680	.5600
67	1	1.4770	.5910
68	1	1.2660	.6220
69	1	1.4770	.6270
70	1	1.6880	.6200
71	1	1.6880	.6190
72	1	1.4770	.6260
73	1	5.1700	.3680
74	1	4.0080	.3920
75	1	4.0080	.4140
76	1	4.2210	.4270

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TABLE A-8-2
L2-3 COBRA MODELS
AXIAL PIN POWER
RELATIVE POWER
DISTRIBUTIONS

HEAT FLUX X/L	DISTRIBUTION RELATIVE FLUX
0.000	.797
.006	.800
.026	.811
.065	1.190
.136	1.772
.212	2.175
.253	2.250
.277	2.214
.304	2.375
.341	2.476
.386	2.500
.409	2.500
.432	2.451
.460	2.296
.485	2.045
.529	1.959
.589	1.558
.682	1.165
.747	1.090
.780	.799
.806	.540
.864	.233
.954	.076
1.000	

P/A =
2.50

HEAT FLUX X/L	DISTRIBUTION RELATIVE FLUX
0.000	.813
.006	.816
.026	.827
.065	1.214
.136	1.807
.212	2.219
.253	2.295
.277	2.258
.304	2.423
.341	2.526
.386	2.550
.409	2.550
.432	2.500
.460	2.342
.485	2.086
.529	1.998
.589	1.589
.682	1.188
.747	.925
.780	.815
.806	.551
.864	.238
.954	.078
1.000	

P/A =
2.55

HEAT FLUX X/L	DISTRIBUTION RELATIVE FLUX
0.000	.770
.026	.767
.065	1.168
.136	1.761
.175	2.012
.212	2.190
.240	2.274
.253	2.283
.277	2.255
.304	2.437
.341	2.559
.386	2.600
.409	2.600
.432	2.561
.460	2.496
.485	2.400
.529	2.129
.589	2.015
.682	1.525
.780	.709
.864	.439
.955	.163
1.000	.151

P/A =
2.60

INTEROFFICE CORRESPONDENCE

date March 14, 1979
to E. C. Anderson
from S. A. Atkinson *S. A. Atkinson*
subject COBRA IIIC - COBRA IV-I COMPARISON AND SPACER GRID LOSS
CALCULATION ERROR - Atki-8-79

In the development of LOFT models for use with the COBRA IV-I computer code, it was determined that the predictions for channel mass flux were significantly different from the predictions obtained using COBRA IIIC for slow transients. COBRA IIIC predicted significantly more flow diversion out of the hot channel, and thus predicted significantly smaller values for mass flux and DNBR. The COBRA IV results appeared reasonable, since it predicted results close to those obtained using a steady-state assumption. For slow power rise transients, such as boron dilution or slow control rod withdrawal accidents, a pseudo-steady-state solution approach should be reasonable and conservative.

Jim Fisher, of Thermal Analysis, investigated possible reasons or causes for this discrepancy, and determined that the equation for calculating the spacer grid (or spacer blade) loss coefficients in the COBRA LOFT models was in error. The equation, as it existed in both the COBRA III and COBRA IV versions, was:

$$CD = CD + K (Re)^r$$

This equation was added to COBRA to make the spacer loss coefficient (CD) a function of Reynolds number (Re). For the LOFT fuel bundle, CD is initially set to zero, and $r = -0.142$. As a result of the above equation, upon every axial flow solution iteration, CD was increased by an increment equal to $K(Re)^r$. Therefore, the spacer loss coefficients were continually increasing through the transient. As a result, the axial flow restriction continually increases. Since the Reynolds number exponent is negative, the increase in the coefficient is greater in the channels with less flow, particularly in the hot channel (channel 15 in the COBRA model). This results in increased flow diversion out of the hot channel as the transient progresses. The correct equation for spacer loss coefficient is:

$$CDS = CDI + K(Re)^r$$

where CDI has an initial constant value set by the user (equal to zero for LOFT).

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COBRA IV has an improved and significantly more efficient solution algorithm for the flow solution in the implicit solution scheme. As a result, COBRA IIIC requires more iterations for a flow solution than does COBRA IV. As a result of this difference between the codes, the spacer loss coefficient built up much more in the COBRA III solution, resulting in significantly less hot channel mass flux for a slow transient. Since the spacer loss coefficient is initially a small value, fast transients, with few total flow solution iterations, would be little effected by the loss coefficient equation error.

The attached Figure 1 compares the COBRA IIIC and COBRA IV results for the LOFT 3 MDNBR vs. time for a $0.5 \times 10^{-4} \Delta\rho/s$ slow reactivity increase accidents with the spacer loss coefficient equation error. As seen in Figure 1, the solutions diverge significantly. Figure 2 plots the hot channel mass fluxes at the axial location at which MDNBR occurred (33 inches up the channel) for the COBRA III and COBRA IV solutions. COBRA IIIC continuously decreases the hot channel mass flux in response to the increasing spacer grid loss coefficients, while the COBRA IV mass flux remains nearly constant at 0.59 Mlbm/h-ft². Figure 3 shows the calculated hot channel mass fluxes at the axial location at which MDNBR occurred after the spacer grid loss coefficient equation was corrected. The COBRA IIIC and COBRA IV mass fluxes are now in close agreement with a nearly constant mass flux of 0.60 Mlbm/h-ft². This behavior is as expected for a slow power increase transient before significant voiding occurs in the channels. Figure 4 is a plot of the MDNBR for the COBRA III and COBRA IV solutions after the spacer grid loss coefficient equation was corrected. The small differences observed in the solutions are primarily due to small differences in the location of MDNBR, and therefore small differences in the heat flux.

Therefore, it is seen that the difference in the COBRA IIIC and COBRA IV solutions was due to the spacer grid loss coefficient equation error. With the loss coefficient equation corrected, both versions of COBRA give similar results. These results are what would be expected for a slow transient, that is little change in flow behavior, and a MDNBR close to but less severe than for a steady-state solution of equal conditions. As seen in Figure 4, MDNBR = 1.151 at 34.6 s using COBRA IIIC. A pseudo-steady-state solution for the MDNBR for the transient conditions at 34.6 s resulted in MDNBR = 1.141. It is not certain that the COBRA IV solution was fully converged, although it seems certain that it was close. Therefore, no judgement can be made at this time as to which solution is best. We will complete an investigation into COBRA IV convergence and make a recommendation for use of COBRA IV at a later time, especially since COBRA IV is a faster and cheaper version to run.

The spacer grid loss coefficient equation error existed in the COBRA IIIC model used for LOFT FSAR and subsequent analyses. However, the error acts to increase the flow resistance in the hot channel more than in other channels with greater initial flow. Therefore, the existance of the error should result in conservative results for all the LOFT core safety analysis, since it should result in lower mass fluxes, higher qualities, and therefore lower MDNBR's for all transients. This conservatism was evident for the slow reactivity

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increase accident discussed above. Additional comparative calculations were done for a very slow reactivity addition accident, a fast reactivity increase accident, a loss of steam load accident, and a rapid loss of flow accident (simultaneous loss of pump power without flywheel assisted coast-down). The calculations were done for the severe L2-3 mode 8 off-nominal operating conditions for a measured $P/A = 2.60$. The results of the comparative calculations using COBRA IIIC are listed in the attached Table 1.

The rapid loss of flow accident is considered to be the type of accident for which the loss coefficient equation error would have the least effect, or might possibly be non-conservative. However, as seen in the comparative calculation results in Table 1, although the error had little effect for the rapid loss of flow, it still remains a conservative effect. Therefore, we conclude that the analyses performed with the spacer grid loss coefficient equation error in COBRA IIIC were conservative for all types of limiting transients. Therefore, use of COBRA versions for future LOFT safety analyses with a corrected spacer grid loss coefficient equation will not result in any reduction in safety margins.

The results in Table 1 point out another effect of the loss coefficient equation error not discussed before. As the loss coefficients for the spacer grids are compounded during transient solution iterations, the axial flow calculated in the vicinity of the spacer grids is severely depressed in relation to the flow in the rest of the channel. This flow depression (due to cross-flow divergence near the grids) results in the point of MDNBR being near the grid at 35 inches up the channel, instead of being closer to the peak power location.

u1

Attachments:
As stated

cc: B. S. Anderson
S. A. Atkinson
G. A. Dinneen
D. F. Elger
N. F. Fausz
J. E. Fisher
J. L. Liebenthal
R. P. Wadkins *row*
TAB File 3.4
Central Files

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TABLE 1

RESULTS OF COMPARATIVE COBRA IIIC CALCULATIONS FOR
EFFECT OF SPACER GRID LOSS COEFFICIENT EQUATION ERROR

	<u>With Loss Coefficient Equation Error</u>	<u>With Corrected Loss Coefficient Equation</u>
Very Slow Reactivity Increase $0.1 \times 10^{-4} \Delta\rho/s$		
Case No.	CRW 91C	CRW 91D
MDNBR	1.131	1.174
Time of MDNBR (s)	82.0	82.0
MDNBR Axial Location (in.)	33	31.5
Mass Flux (Mlbm/hr-ft ²)	0.512	0.600
Quality	-0.077	-0.111
Rapid Reactivity Increase $2.0 \times 10^{-4} \Delta\rho/s$		
Case No.	CRW 90	CRW 90A
MDNBR	1.098	1.154
Time of MDNBR (s)	26.2	26.2
MDNBR Axial Location (in.)	34.5	31.5
Mass Flux (Mlbm/h-ft ²)	0.444	0.591
Quality	-0.054	-0.082
Loss of Steam Load		
MDNBR	1.120	1.156
Time of MDNBR (s)	14.0	14.0
MDNBR Axial Location (in.)	34.5	31.5
Mass Flux (Mlbm/h-ft ²)	0.481	0.607
Quality	-0.104	-0.144
Two Pump Loss Without Flywheel Assisted Coastdown		
MDNBR	1.175	1.179
Time of MDNBR (s)	14.0	14.0
MDNBR Axial Location (in.)	31.5	31.5
Mass Flux (Mlbm/h-ft ²)	0.400	0.412
Quality	0.002	-0.008

FIGURE 2

LOFT MODEL MASS FLUX AT
33 INCH ELEVATION IN CHANNEL 15
FOR 0.5×10^{-4} $\Delta \rho/\rho$ REACTIVITY INCREASE
ACCIDENT CASE CRW=621

COBRA II C ●
COBRA II-I ○
WITH SPACER GRID
LOSS COEFFICIENT
EQUATION ERROR

FIGURE 3

FIGURE 4

MDNBR (LOFT-3)

LOFT MODEL MDNBR
FOR 0.5×10^{-4} $\Delta P/S$ REACTIVITY
INCREASE ACCIDENT
CASE CRW-621

COBRA III C •
COBRA IV-I ○
WITH CORRECT SPACER
GRID LOSS COEFFICIENT
EQUATION

APPENDIX A-9

COBRA IIIC-COBRA IV-T COMPARISON
AND SPACER GRID LOSS CALCULATION ERROR

APPENDIX A-10

RESULTS OF REQUESTED ROD WORTH

CALCULATIONS FOR LOFT L2-3

INTEROFFICE CORRESPONDENCE

date March 21, 1979
to W. R. Carpenter
from B. L. Rushton *BR*
subject RESULTS OF REQUESTED ROD WORTH CALCULATIONS FOR LOFT
(L2-3) - Rush-5-79

Calculations to provide data for input to L2-3 safety analyses have been completed. These calculations provide differential rod worth data for the following control rod moves:

- 1) Single rod-cluster ejection from the 50 in. bank (three rod clusters remain banked at 50 in. withdrawn),
- 2) Four rod-cluster withdrawal from the 50 in. bank,
- 3) Three rod-cluster scrams from 50 and 60 in.

For items 1 and 2, core conditions were specified to maximize the reactivity insertion effected by control rod withdrawal. For item 3, core conditions were specified to minimize the worth effected by scram. This was accomplished by performing limited scoping calculations and reliance on past experience.

The one-dimensional PDQ-7 model with Version II thermal feedback was used for this study.

Items 1 and 2

For these analyses, calculations were performed to simulate reactor operation at the 75% power level for 100 hours with the control rods banked at 50 inches. Input data for two flow conditions are given in Table I.

W. R. Carpenter
March 21, 1979
Rush-5-79
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TABLE I
SPECIFIED OPERATING PARAMETERS

	<u>Low Flow</u>	<u>High Flow</u>
Core Power (%) ^[a]	75	75
Core Flow (lb/hr)	1.41 + 6	3.42 + 6
Inlet Temperature (°F)	555	535
Control Rod Position (in.) ^[b]	50.0	50.0

[a] 100% Core Power = 48.5 MW
[b] in. withdrawn

At the 100-hour time point, zero-power was assumed and the shutdown transient was computed to the time of maximum ¹³⁵Xe concentration (approximately 7.5 hours). The buildup of ¹³⁵Xe is distributed as the power distribution prior to assuming zero power (skewed down the core) and this will maximize the insertion caused by subsequent rod withdrawal. The rod worth analyses were, therefore, performed with the ¹³⁵Xe concentration and distribution computed after the 7.5 hour shutdown interval. Tables A-I through A-IV give the computed results for single-rod and four-rod withdrawal. Low-flow operation followed by the shutdown transient effect the maximum reactivity insertions on subsequent rod withdrawal from 50 inches. The data in Tables A-I through A-IV were computed for zero power conditions; i.e., uniform 555°F or 535°F. For additional information, results are given in Table A-V that show the effect of going to power at the 7.5 hour time point. That is, after the 7.5 hour shutdown interval a feedback calculation was performed under the low flow conditions of Table I and single-rod withdrawal calculations were then performed. In this case, the power shifts down the core away from the rod tips effecting a reduction in the computed reactivity insertions.

Item 3

As opposed to rod withdrawal, these analyses provide rod worth data for terminating a positive reactivity transient with 3-rod scram and should, therefore, be minimized for conservatism. The calculations were performed for both power range and pre-loce conditions with the reactor in the ¹³⁵Xe-free state. Input data for the two conditions are specified in Table II.

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TABLE II
SPECIFIED POWER RANGE AND PRE-LOCE CONDITIONS

	<u>Power Range</u>	<u>Pre-Loce</u>
Core Power (MW)	53.4	41.4
Core Flow (lb/hr)	3.06 + 6	0.99 + 6
Inlet Temperature (°F)	560.	550.
Control Rod Positions (in.)	50 and 60	50 and 60

Results are given in Tables A-VI through A-IX. These data span a range of conditions and the most conservative case can be chosen for input to subsequent safety analyses.

tgs

Attachments:
Tables A-I-IX

cc: E. C. Anderson
E. F. Aber
D. R. Cooper
G. S. Gill
~~D. G. Satterwhite~~
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G. K. Wachs *ekw*
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TABLE A-1
SINGLE-ROD WITHDRAWAL FROM 50 INCHES^[a]
LOW FLOW CASE (1.41 + 6 lb/hr)^[b]
(Three Clusters Remain Banked At 50 Inches)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	0.963807		
50.61	0.964032	0.0533	50.305
50.96	0.964142	0.0452	50.785
51.66	0.964333	0.0392	51.310
52.36	0.964500	0.0321	52.010
55.00	0.965024	0.0286	53.680
57.00	0.965303	0.0201	56.000
59.00	0.965501	0.0142	58.000
61.00	0.965634	0.00960	60.000
63.00	0.965717	0.00596	62.000
66.00	0.965777	0.00288	64.500

[a] Step insertion = + 0.284\$.

[b] Followed by 7.5 hour shutdown and subsequent calculations at uniform core temperature of 555°F.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-II
SINGLE ROD WITHDRAWAL FROM 50 INCHES^[a]
HIGH FLOW CASE-2 (3.42 + 6 lb/hr)^[b]
(Three Clusters Remain Banked At 50 Inches)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	0.969277		
50.61	0.969477	0.0470	50.305
50.96	0.969575	0.0399	50.785
51.66	0.969743	0.0345	51.310
52.36	0.969891	0.0283	52.010
55.00	0.970356	0.0252	53.680
57.00	0.970604	0.0178	56.000
59.00	0.970780	0.0126	58.000
61.00	0.970898	0.00848	60.000
63.00	0.970972	0.00526	62.000
66.00	0.971025	0.00251	64.500

[a] Step insertion = + 0.18\$.

[b] Followed by 7.5 hour shutdown and subsequent calculations at uniform core temperature of 535°F.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.)

TABLE A-III

4-ROD WITHDRAWAL FROM 50 INCHES
LOW FLOW CASE (1.41 + 6 lb/hr)^[a]

Rod Position ^[b] (in.)	Eigenvalue (λ)	Incremental Worth ^[c] (\$/in.)	Interval Midpoint (in.)
50.00	0.963807	0.14630	50.305
50.61	0.964426	0.13950	50.785
50.96	0.964765	0.13250	51.310
51.66	0.965410	0.12220	52.010
52.36	0.966047	0.14430	53.680
55.00	0.968699	0.15560	56.000
57.00	0.970873	0.15940	58.000
59.00	0.973104	0.15390	60.000
61.00	0.975263	0.13310	62.000
63.00	0.977134	0.09062	64.500
66.00	0.979049		

[a] Followed by 7.5 hour shutdown and subsequent calculations at uniform core temperature of 555°F.

[b] Inches withdrawn.

[c] \$/in. = $\ln (\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-IV
4-ROD WITHDRAWAL FROM 50 INCHES
HIGH FLOW CASE (3.42 + 6 lb/hr)^[a]

Rod Position ^[b] (in.)	Eigenvalue (λ)	Incremental Worth ^[c] (\$/in.)	Interval Midpoint (in.)
50.00	0.963923		
50.61	0.964476	0.1304	50.305
50.96	0.964777	0.1242	50.785
51.66	0.965353	0.1184	51.310
52.36	0.965923	0.1092	52.010
55.00	0.968306	0.1296	53.680
57.00	0.970284	0.1417	56.000
59.00	0.972343	0.1472	58.000
61.00	0.974367	0.1444	60.000
63.00	0.976146	0.1267	62.000
66.00	0.977988	0.0873	64.500

[a] Followed by 7.5 hour shutdown and subsequent calculations at uniform core temperature of 535°F.

[b] Inches withdrawn.

[c] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.)

TABLE A-V

SINGLE ROD WITHDRAWAL FROM 50 INCHES^[a]

LOW FLOW CASE (1.41 + 6 lb/hr)^[b]

75% POWER BEFORE WITHDRAWAL

(Three Clusters Remain Banked At 50 Inches)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	0.932625		
		0.031440	50.305
50.61	0.932754		
		0.026890	50.785
50.96	0.932817		
		0.023310	51.310
51.66	0.932926		
		0.019040	52.010
52.36	0.933022		
		0.016840	53.680
55.00	0.933321		
		0.011880	56.000
57.00	0.933481		
		0.008406	58.000
59.00	0.933594		
		0.005713	60.000
61.00	0.933671		
		0.003555	62.000
63.00	0.933718		
		0.001835	64.500
66.00	0.933755		

[a] Step insertion = + 0.1685\$.

[b] Followed by 7.5 hour shutdown and subsequent worth calculations after converging feedback at 75% power.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-VI
3-ROD SCRAM FROM 60 INCHES^[a]
POWER RANGE^[b]
(¹³⁵Xe-free)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
60.00	1.0016110		
		0.02906	59.00
58.00	1.0011920		
		0.03698	57.00
56.00	1.0006590		
		0.04339	55.00
54.00	1.0000340		
		0.04747	53.00
52.01	0.9993540		
		0.05446	51.01
50.00	0.9985667		
		0.06803	49.00
48.00	0.9975890		
		0.07635	47.00
46.00	0.9964928		
		0.08403	45.00
44.00	0.9952877		
		0.09193	43.00
42.00	0.9939710		
		0.10080	41.00
40.00	0.9925292		
		0.11095	39.00
38.00	0.9909447		
		0.12080	37.00
36.00	0.9892223		
		0.12400	35.53
35.06	0.9883925		
		0.13580	34.54
34.01	0.9873783		
0.00	0.9067342		

[a] 1-rod cluster remains at 60 in.

[b] Power = 53.4 MW, core flow = 3.03 + 6 lb/hr, Inlet = 560°F, Converge feedback at 60 in. rod bank position and perform scram calculations.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.)

TABLE A-VII
3-ROD SCRAM FROM 60 INCHES^[a]
PRE-LOCE (L2-3)^[b]
(¹³⁵Xe-free)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
60.00	0.9941434	0.01604	59.00
58.00	0.9939138	0.02117	57.00
56.00	0.9936108	0.02590	55.00
54.00	0.9932403	0.02977	53.00
52.01	0.9928168	0.03562	51.01
50.00	0.9923052	0.04583	49.00
48.00	0.9916506	0.05346	47.00
46.00	0.9908875	0.06114	45.00
44.00	0.9900155	0.06945	43.00
42.00	0.9890259	0.07890	41.00
40.00	0.9879029	0.08973	39.00
38.00	0.9866273	0.10080	37.00
36.00	0.9851968	0.10610	35.53
35.06	0.9844896	0.11780	34.54
34.01	0.98361350		
0.00	0.8976985		

[a] 1-rod cluster remains at 60 in.

[b] Power = 41.4 MW, Core flow = 0.99 + 6 lb/hr, Inlet = 550°F, Converge feedback at 60 in. rod bank position and perform scram calculations.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-VIII
3-ROD SCRAM FROM 50 INCHES^[a]
POWER RANGE^[b]
(¹³⁵Xe-free)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	0.995935		
48.00	0.995090	0.05895	49.00
46.00	0.994057	0.07212	47.00
44.00	0.992877	0.08246	45.00
42.00	0.991562	0.09205	43.00
40.00	0.990110	0.10177	41.00
38.00	0.988509	0.11235	39.00
36.00	0.986769	0.12237	37.00
35.00	0.985931	0.12558	35.53
34.01	0.984908	0.13732	34.54
0.00	0.902974		

[a] 1-rod cluster remains at 50 in.

[b] Power = 53.4 MW, Core flow = 3.03 + 6 lb/hr, Inlet = 560°F,
Converge feedback at 50 in. rod bank position and perform scram
calculations.

[c] Inches withdrawn.

[d] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.)

TABLE A-IX
3-ROD SCRAM FROM 50 INCHES^[a]
PRE-LOCE (L2-3)^[b]
(¹³⁵Xe-free)

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	0.988672		
48.00	0.988129	0.03815	49.00
46.00	0.987442	0.04833	47.00
44.00	0.986628	0.05724	45.00
42.00	0.985685	0.06646	43.00
40.00	0.984603	0.07624	41.00
38.00	0.983367	0.08722	39.00
36.00	0.981977	0.09828	37.00
35.76	0.981803	0.10210	35.88
35.06	0.981288	0.10420	35.41
34.01	0.980434	0.11520	34.54
0.00	0.893490		

[a] 1-Rod cluster remains at 50 in.

[b] Power = 41.4 MW Core flow = 0.99 + 6 lb/hr, Inlet = 550°F,
Converge feedback at 50 in. rod bank position and perform
scram calculations.

[c] Inches withdrawn.

[d] \$/in. = $\ln (\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

INTEROFFICE CORRESPONDENCE

date April 11, 1979
to W. R. Carpenter
from B. L. Rushton *BR*
subject RESULTS OF REQUESTED CALCULATIONS TO PROVIDE SUPPORT FOR
LOCE L2-3 - Rush-7-79

Additional data for LOCE L2-3 safety analyses have been calculated using the one-dimensional PDQ-7 model with Version II thermal feedback. For your convenient reference, attach this information to Rush-5-79. The calculated results are for the following:

- 1) Four rod-cluster withdrawal from the 34 in. banked position,
- 2) The axial power distribution with the four rod-clusters banked at 61 in. withdrawn,
- 3) Shutdown margins effected by four rod-cluster and three rod-cluster scrams.

Stated rod-bank positions are given relative to the bottom of the active fuel.

Items 1 and 2

For these analyses, calculations were performed to simulate LOFT operation at the 75% power level for 100 hours with the control rods banked at 50 inches. The operating parameters are given in Table I.

TABLE I
SPECIFIED OPERATING PARAMETERS

Core Power (%) ^[a]	75
Core Flow (lb/hr)	1.41 + 6
Inlet Temperature (°F)	555
Control Rod Position (in.) ^[b]	50.0

^[a] 100% Core Power = 48.5 MW

^[b] in. withdrawn relative to the bottom of the active fuel.

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April 11, 1979
Rush-7-79
Page 2

At the 100-hour time point, the shutdown transient described in Rush-5-79 was computed and with nuclide concentrations corresponding to the 7.5 hour shutdown interval four-rod withdrawal calculations were performed. Table A-I gives the resulting incremental worths for withdrawal from 34.01 in. (uniform temperature of 555°F). This is an extension of the Table A-III data given in Rush-5-79 which were for withdrawal from 50 in. In the calculational steps that provide the incremental worths, the axial point-to-average power distribution computed for each rod bank position is an output option. As requested, Table A-II gives the computed power distribution for the 61 in. rod bank position (uniform 555°F) and Figure 1 is a plot of the data.

Item 3

Control rods must compensate for the prompt reactivity increase that would occur if the core were to undergo rapid cooling. Three-rod and four-rod shutdown margins were computed in this study for cooldowns from an at-power case. The thermal-feedback calculations (at-power cases) were made using the specified parameters given in Table II.

TABLE II
OPERATING PARAMETERS BEFORE COOLDOWN

Core Power (MW)	41.4
Core Flow (lb/hr)	4.0 + 6
Inlet Temperature (°F)	590
Control Rod Position (in.)	50.0

The eigenvalue computed for the at-power case was assumed the just critical value and was used as the base case eigenvalue for computing the reactivity transient on cooldown. The sensitivity of the reactivity transient to ^{135}Xe and boron concentrations was also determined. As requested, the cooldown temperatures used were 535°F and 375°F for the four-rod and three-rod scrams respectively. The results are given in Tables A-III, IV, V, and VI.

Shutdown criterion were met for each case analyzed in this study. Namely, the four-rod scram provided a shutdown margin greater than 9% and subcriticality was achieved with the three-rod scrams.

W. R. Carpenter
April 11, 1979
Rush-7-79
Page 3

tgs

Attachments:
As stated

cc: E. F. Aber
E. C. Anderson
D. R. Cooper
G. S. Gill
D. G. Satterwhite
A. J. Scott *ajx*
G. K. Wachs *KW*
B. L. Rushton File
Central Files

TABLE A-I
4-ROD WITHDRAWAL FROM 34 INCHES
LOW FLOW CASE (1.41 + 6 lb/hr)^[a]

Rod Position ^[b] (in.)	Eigenvalue (λ)	Incremental Worth ^[c] (\$/in.)	Interval Midpoint (in.)
34.01	0.947576	0.16600	34.535
35.06	0.948766	0.06282	35.410
35.76	0.949496	0.15140	35.880
36.00	0.949745	0.15260	37.000
38.00	0.951834	0.14700	39.000
40.00	0.953851	0.14240	41.000
42.00	0.955809	0.14100	43.000
44.00	0.957752	0.14240	45.000
46.00	0.959717	0.14590	47.000
48.00	0.961735	0.14940	49.000
50.00	0.963807	0.14630	50.305
50.61	0.964426	0.13950	50.785
50.96	0.964765	0.13250	51.310
51.66	0.965410	0.12220	52.010
52.36	0.966047	0.14430	53.680
55.00	0.968699	0.15560	56.000
57.00	0.970873	0.15940	58.000
59.00	0.973104	0.15390	60.000
61.00	0.975263	0.13310	62.000
63.00	0.977134	0.09062	64.500
66.00	0.979049		

[a] Followed by 7.5 hour shutdown and subsequent calculations at uniform core temperature of 555°F.

[b] Inches withdrawn (relative to the bottom of the active fuel).

[c] \$/in. = $\ln(\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-II
AXIAL POWER DISTRIBUTION^[a]
(Rod Position = 61 in.; T=555°F uniform)

Column No.	Axial ^[b] Distance (in.)	Relative ^[c] Power Density	Column No.	Axial Distance (in.)	Relative Power Density
6	0.0	0.247	49	33.60	1.280
7	0.81	0.217	50	34.01	1.262
8	1.16	0.216	51	34.36	1.248
9	1.51	0.222	52	34.71	1.248
10	1.86	0.233	53	35.06	1.260
11	2.21	0.248	54	35.41	1.283
12	2.56	0.266	55	35.76	1.322
13	3.00	0.291	56	36.00	1.351
14	4.00	0.338	57	37.00	1.421
15	5.00	0.382	58	38.00	1.466
16	6.00	0.423	59	39.00	1.501
17	7.00	0.462	60	40.00	1.531
18	8.00	0.499	61	41.00	1.557
19	9.00	0.535	62	42.00	1.578
20	10.00	0.570	63	43.00	1.594
21	11.00	0.603	64	44.00	1.604
22	12.00	0.634	65	45.00	1.608
23	13.00	0.665	66	46.00	1.606
24	14.00	0.694	67	47.00	1.596
25	15.00	0.721	68	48.00	1.577
26	16.00	0.745	69	49.00	1.547
27	17.00	0.758	70	50.00	1.494
28	17.41	0.752	71	50.61	1.430
29	17.76	0.748	72	50.96	1.389
30	18.11	0.752	73	51.31	1.364
31	18.46	0.763	74	51.66	1.350
32	18.81	0.782	75	52.01	1.349
33	19.16	0.809	76	52.36	1.362
34	19.58	0.842	77	53.00	1.377
35	20.00	0.868	78	54.00	1.350
36	21.00	0.913	79	55.00	1.298
37	22.00	0.951	80	56.00	1.232
38	23.00	0.988	81	57.00	1.154
39	24.00	1.023	82	58.00	1.065
40	25.00	1.057	83	59.00	0.964
41	26.00	1.091	84	60.00	0.848
42	27.00	1.124	85	61.00	0.699
43	28.00	1.156	86	62.00	0.564
44	29.00	1.187	87	63.00	0.464
45	30.00	1.216	88	64.00	0.383
46	31.00	1.244	89	65.00	0.335
47	32.00	1.269	90	66.00	0.392
48	33.00	1.285			

[a] ¹³⁵Xe concentration after 7.5 hour shutdown.

[b] Axial distance from the bottom of the active fuel.

[c] Axial point-to-average power.

TABLE A-III

CALCULATED REACTIVITY WORTH EFFECTED BY COOLDOWN FROM 41.4 MW TO 535°F
AND SHUTDOWN MARGIN FOR 4-ROD SCRAM
(¹³⁵Xe-free; 1232 ppm)

At-Power Eigenvalue: $\lambda_0 = 0.992079^{[a]}$; Uniform 535°F Eigenvalue $\lambda_1 = 1.028257$

Overall Temperature Worth: $\ln \frac{\lambda_1}{\lambda_0} = 0.03582$

535°F Scram Worth: $\ln \frac{\lambda(50.00 \text{ in.})}{\lambda(0.00 \text{ in.})} = 0.151369$

Shutdown Margin: $\ln \frac{\lambda_0}{\lambda(0.0 \text{ in.})} = 0.11555$

Calculated 4-Rod Scram Data Uniform 535°F

Rod Position ^[b] (in.)	Eigenvalue (λ)	Incremental Worth ^[c] (\$/in.)	Interval Midpoint (in.)
50.00	1.028257		
48.00	1.026906	0.09130	49.00
46.00	1.025412	0.10111	47.00
44.00	1.023786	0.11021	45.00
42.00	1.022021	0.11983	43.00
40.00	1.020097	0.13086	41.00
38.00	1.017984	0.14399	39.00
36.00	1.015684	0.15708	37.00
35.76	1.015404	0.15956	35.88
35.06	1.014576	0.16186	35.41
34.01	1.013199	0.17965	34.535
00.00	0.8838185		

[a] Power = 41.4 MW, Core flow = 4.0 + 6 lb/hr, Inlet = 590°F,
Converge feedback at 50 in. rod-bank position.

[b] Inches withdrawn (relative to the bottom of the active fuel).

[c] \$/in. = $\ln (\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-IV
CALCULATED REACTIVITY WORTH EFFECTED BY COOLDOWN FROM 41.4 MW TO 375°F
AND SHUTDOWN MARGIN FOR 3-ROD SCRAM^[a]
(¹³⁵Xe-free; 1232 ppm)

At-Power Eigenvalue: $\lambda_0 = 0.992079$ ^[b]; Uniform 375°F Eigenvalue: $\lambda_1 = 1.052609$

Overall Temperature Worth: $\ln \frac{\lambda_1}{\lambda_0} = 0.05922$

375°F Scram Worth: $\ln \frac{\lambda(50.0 \text{ in.})}{\lambda(0.0 \text{ in.})} = 0.086652$

Shutdown Margin: $\ln \frac{\lambda_0}{\lambda(0.0 \text{ in.})} = 0.0274$

Calculated 3-Rod Scram Data Uniform 375°F

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	1.052609		
48.00	1.051808	0.05286	49.00
46.00	1.050798	0.06672	47.00
44.00	1.049620	0.07789	45.00
42.00	1.048291	0.08798	43.00
40.00	1.046815	0.09785	41.00
38.00	1.045184	0.10828	39.00
36.00	1.043411	0.11790	37.00
35.76	1.043198	0.11815	35.88
35.06	1.042576	0.11834	35.41
34.01	1.041554	0.12973	34.535
00.00	0.9652381		

[a] 1-rod cluster remains at 50 in. (relative to the bottom of the active fuel).

[b] Power = 41.4 MW, Core flow = 4.0 + 6 lb/hr, Inlet = 590°F,
Converge feedback at 50 in. rod-bank position.

[c] Inches withdrawn (relative to the bottom of the active fuel).

[d] \$/in. = $\ln (\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

TABLE A-V
CALCULATED REACTIVITY WORTH EFFECTED BY COOLDOWN FROM 41.4 MW TO 375°F
AND SHUTDOWN MARGIN FOR 3-ROD SCRAM^[a]
(Equilibrium ¹³⁵Xe; 1232 ppm)

At-Power Eigenvalue: $\lambda_0 = 0.9633739$ ^[b]; Uniform 375°F Eigenvalue $\lambda_1 = 1.020917$

Overall Temperature Worth: $\ln \frac{\lambda_1}{\lambda_0} = 0.058015$

Scram Worth: $\ln \frac{\lambda(50.0 \text{ in.})}{\lambda(0.0 \text{ in.})} = 0.087069$

Shutdown Margin: $\ln \frac{\lambda_0}{\lambda(0.0 \text{ in.})} = 0.02905$

Calculated 3-Rod Scram Data Uniform 375°F

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	1.020917		
48.00	1.020044	0.05941	49.00
46.00	1.018984	0.07220	47.00
44.00	1.017784	0.08182	45.00
42.00	1.016461	0.09033	43.00
40.00	1.015018	0.09866	41.00
38.00	1.013443	0.10784	39.00
36.00	1.011751	0.11604	37.00
35.76	1.011547	0.11670	35.88
35.06	1.010957	0.11576	35.41
34.01	1.009989	0.12671	34.535
00.00	0.935787		

[a] 1-rod cluster remains at 50 in. (relative to the bottom of the active fuel).

[b] Power = 41.4 MW, Core flow = 4.0 + 6 lb/hr, Inlet = 590°F,
Converge feedback at 50 in. rod-bank position and perform ¹³⁵Xe search.

[c] Inches withdrawn (relative to the bottom of the active fuel).

[d] \$/in. = $\ln (\lambda_1/\lambda_1)/(0.0072)(\Delta h)$

where Δh = interval width (in.).

TABLE A-VI
CALCULATED REACTIVITY WORTH EFFECTED BY COOLDOWN FROM 41.4 MW TO 375°F
AND SHUTDOWN MARGIN FOR 3-ROD SCRAM^[a]
(¹³⁵Xe free; 100 ppm)

At-Power Eigenvalue: $\lambda_0 = 1.083001$ ^[b]; Uniform 375°F Eigenvalue $\lambda_1 = 1.18038$

Overall Temperature Worth: $\ln \frac{\lambda_1}{\lambda_0} = 0.086101$

Scram Worth: $\ln \frac{\lambda(50.0 \text{ in.})}{\lambda(0.0 \text{ in.})} = 0.099349$

Shutdown Margin: $\ln \frac{\lambda_0}{\lambda(0.0 \text{ in.})} = 0.01325$

Calculated 3-Rod Scram Data Uniform 375°F

Rod Position ^[c] (in.)	Eigenvalue (λ)	Incremental Worth ^[d] (\$/in.)	Interval Midpoint (in.)
50.00	1.18038		
48.00	1.17944	0.05556	49.00
46.00	1.17824	0.07022	47.00
44.00	1.17686	0.08144	45.00
42.00	1.17532	0.09140	43.00
40.00	1.17361	0.10093	41.00
38.00	1.17173	0.11133	39.00
36.00	1.16970	0.12053	37.00
35.76	1.16945	0.12024	35.88
35.06	1.16875	0.12016	35.41
34.01	1.16757	0.13316	34.535
00.00	1.06875		

[a] 1-rod cluster remains at 50 in. (relative to the bottom of the active fuel).

[b] Power = 41.4 MW, Core flow = 4.0 + 6 lb/hr, Inlet = 590°F,
Converge feedback at 50 in. rod-bank position.

[c] Inches withdrawn (relative to the bottom of the active fuel).

[d] \$/in. = $\ln (\lambda_1/\lambda_2)/(0.0072)(\Delta h)$

where: Δh = interval width (in.).

Figure 1. FROM 7.5 HR DOWN RODS AT 61 (IN) T-555 UNIFORM

APPENDIX B
ANALYSIS RESULTS

APPENDIX B-1
L2-3 MODE 8 STEADY-STATE
ANALYSIS RESULTS

INTEROFFICE CORRESPONDENCE

date March 23, 1979
to S. A. Atkinson
from B. S. Anderson *BSA*
subject L2-3 MODE 8 STEADY-STATE LIMITS - BSA-4-79

Steady-state safety limits have been established for core power, hot leg temperature, high core pressure, and low loop flow during L2-3 Mode 8 operation. To establish the limits a series of COBRA runs which bound the limit were made for each parameter. One series for a peak-to-average power ratio of 2.55 and one for a peak-to-average of 2.60. Off nominal values were used for all parameters except the parameter being varied. The off-nominal values used are listed in Table I. The three to five cases run in each series were then plotted and the safety limit was then interpolated from the plots. The large core COBRA model used for this analysis is shown in Figure 1.

For core power the limiting factor is the MDNBR which must be ≥ 1.14 to insure a 95% chance that the core will not be in DNB. Figure 2 shows a plot of the core power versus MDNBR. From this plot the high core power safety limit is 42 MW for the series with a peak-to-average of 2.55 and 41 MW for the series with a peak-to-average of 2.60.

The hot leg temperature is limited by hitting saturation conditions in the hot leg before the MDNBR drops to 1.14. The cases made for this report used a low off-nominal pressure of 2088. The low-off-nominal pressure has since been lowered to 2050 psia which implies a saturation temperature of 639.3°F and therefore, is the limiting hot leg temperature. The range of temperatures run using 2088 psia covers this temperature, and from a MDNBR standpoint the higher pressure will be more conservative, so the cases were not rerun using the lower pressure. Figure 3 shows the hot leg temperature plotted against the MDNBR. Two extra cases however, were run to determine the MDNBR assuming a hot leg temperature of 639.3°F with a core pressure of 2257 psia which very conservatively bounds the MDNBR for the limiting hot leg temperature. Results for the case with a peak-to-average of 2.55 indicated that MDNBR = 1.189 and for the case with a peak-to-average of 2.60 the MDNBR = 1.187.

Like the core power the limiting factor for the high pressure safety limit is a MDNBR of ≥ 1.14 . Figure 4 shows a plot of the core pressure versus MDNBR. From this plot the high core pressure safety limit is 2500 psia for the series with a peak-to-average of 2.55 and 2490 psia for the series with a peak-to-average of 2.60. Since the design pressure of the reactor vessel is 2500 psia the core should not go into DNB up to the limits of the reactor vessel.

March 23, 1979
S. A. Atkinson
BSA-4-79
Page 2

LTR 111-127
APPENDIX B.1

The LOFT-3 DNB correlation has a lower limit on mass flux of 0.4 Mlbm/hr-ft^2 . For the low loop flow rate safety limit the lower limit on the LOFT-3 DNB correlation was assumed to be the lower limit on mass flux at the point of MDNBR. This limit was reached before a MDNBR of 1.14. Figure 5 shows a plot of loop mass flow rate versus the mass flux at the point of MDNBR. From this plot a low loop flow rate safety limit of 0.69 Mlbm/hr was found for both the 2.55 and 2.60 peak-to-average cases. At this point the MDNBR is 1.23. This analysis however, assumes that the steam generator will cool the loop flow to the low core inlet temperatures necessary to maintain steady-state loop flows this low.

Table II summarizes the steady-state safety limits found in this study.

ul

Attachments:
As stated

cc: B. S. Anderson
D. F. Elger
J. E. Fisher
D. G. Satterwhite
R. P. Wadkins
Central File
TAB File 3.5

TABLE I
OFF-NOMINAL PARAMETER VALUES

Parameter	P/A = 2.60	P/A = 2.55
High Off-Nominal Power (MW)	37.2	37.9
High Off-Nominal Hot Leg Temperature (°F)	613	613
High Off-Nominal Pressure (psia)	2257*	2257*
Low Off-Nominal Loop Mass Flow (Mlbm/hr)	1.15	1.15

*An off-nominal low pressure was used for determining the safety limit for the hot leg temperature.

TABLE II

L2-3 MODE 8 STEADY-STATE SAFETY LIMITS

	P/A = 2.60 W = 1.15×10^6 lb/hr	P/A = 2.55 W = 1.18×10^6 lb/hr
Power at which MDNBR = 1.14	41 MW	42 MW
T _{hot} at which saturation is reached in the hot leg	639°F (MDNBR = 1.31)	639°F (MDNBR = 1.31)
Core pressure at which MDNBR = 1.14	2490 psia*	2500 psia*
Loop flux @ which channel 15 flux = $.4 \times 10^6$ lb/hr	.69 MLM/hr (MDNBR = 1.24)	.69 MLM/hr (MDNBR = 1.23)

*Design pressure is 2500 psia.

ANC-B-5333

FIG. 1 COBRA II MODEL OF 1/8 SEGMENT OF LOFT CORE FOR STEADY STATE DNB ANALYSIS

Figure 2- Core Power versus MDNBR

Figure 3 - Hot Leg Temperature versus MDNBR

Figure 4 - Core Pressure versus MDNBR

Figure 5- Loop Mass Flow versus Mass Flux at Point of MDNBR

INTEROFFICE CORRESPONDENCE

date February 22, 1979
to E. C. Anderson
from S. A. Atkinson *J. A. Atkinson*
subject STEADY-STATE (SAFETY) LIMITS FOR L2-3 MODE 8 - Atki-4-79

The limits for which Damage Criteria A is satisfied for steady-state operation in operating Mode 8 for the L2-3 test have been determined. The Damage Criteria A consequence limit of interest is that MDNBR is greater than or equal to 1.14 using the LOFT-3 DNB correlation. Calculations were done using the LOFT large core model (1/8th of the LOFT core) with the COBRA IIIC computer code for thermal-hydraulic analysis in a nuclear fuel rod bundle. The limit analysis method was to set all the sensitive thermal-hydraulic parameters except one to their off-nominal or more severe values (more severe for determining DNB), then to either:

- (a) vary the remaining parameter until MDNBR = 1.14 or,
- (b) determine if MDNBR $\geq$ 1.14 at the worst-case trip value of the remaining parameter.

The parameter limits defined in this manner satisfy the intent of 10 CFR 50.36 for safety limits as illustrated in the 1968 Guide to the Content of Technical Specifications.

The resulting steady-state (safety) limits determined according to the above analysis method are:

<u>Parameter</u>	<u>Safety Limit</u>	<u>Comments</u>
Reactor Power	116% of nominal	Nominal power is a function of measured peak to average power ratio (P/A) as defined in the L2 EOS. The maximum value of P/A was 2.60 for this analysis. The limit corresponds to MDNBR = 1.14.
PCS Loop Flow	1.01×10^6 lbm/hr	The limit corresponds to the maximum flow worst-case trip (WCT) for P/A = 2.66. However, MDNBR is >1.14 for lower P/A's as well. Minimum mass velocity is greater than the lower limit for the LOFT-3 correlation

February 22, 1979
E. C. Anderson
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Page 2

<u>Parameter</u>	<u>Safety Limit</u>	<u>Comments</u>
PCS Temperature (controlled by hot leg coolant temperature)	626°F	MDNBR > 1.14 for operation with a P/A up to 2.60 at this hot leg temperature and at a power above the minimum WCT power.
PCS Pressure (Reactor hot leg)	2400 psia	MDNBR = 1.444 for P/A = 2.66.

The values of all the sensitive parameters for each of the limiting steady-state cases are in Table I attached. The most limiting off-nominal parameter values used for these analyses were:

<u>Parameter</u>	<u>Off-Nominal Value</u>
Reactor Power	Nominal + 2.5 MW
PCS Loop Flow	3.327/(P/A) - (0.125) Mlbm/h
Hot Leg Temperature	621°F
Pressure	2257 psia (off-nominal high)

ul

Attachment:
As stated

cc: S. A. Atkinson
D. F. Elger
J. E. Fisher
J. L. Liebenthal *pl*
D. G. Satterwhite
R. P. Wadkins *power*
TAB File 3.6
Central File

TABLE I

LIMITING STEADY-STATE CASES FOR L2-3

Case No.	P/A	Power** MW	Loop Flow Mlbm/h	Pressure psia	T _{hot} °F	MDNBR
L2-3.13	2.66	36.4	1.01*	2350	621	1.161
L2-3.20	2.66	36.4	1.12	2400*	621	1.144
L2-3.46	2.60	40.2*	1.15	2257	621	1.142
L2-3.48	2.60	38.7	1.15	2257	626*	1.172
L2-3.50	2.37	40.6	1.01*	2350	621	1.157

*Parameter for which the limit is being determined or tested.

**For P/A = 2.66, Off-nominal high power = 36.4 MW.

For P/A = 2.60, Off-nominal high power = 37.2 MW.

Nominal power = 34.7 MW.

Minimum WCT power = 38.2 MW.

For P/A = 2.37, Off-nominal high power = 40.6 MW.

INTEROFFICE CORRESPONDENCE

date April 18, 1978
to T. M. Howe
from S. A. Atkinson
subject MDNBR VS MLHGR PLOT FOR L2-3 CONDITIONS Atki-12-78

Attached is the results of the COBRA analysis done to generate a plot of MDNBR vs MLHGR for L2-3 nominal and worst-case pre-LOCE operating conditions, as was requested of you by Chuck Solbrig. The worse-case conditions assumed for the analysis may be extra conservative but not by a significant amount. Also, the effect of slightly changing some of the worse-case assumptions is not significant. For instance, if worse-case power is assumed to be only 2.5 MW greater than nominal, instead of 3.5 MW greater, the resulting worse-case MDNBR's would only increase by 2 to 3% (a MDNBR of 0.96 instead of 0.94 for MLHGR = 12 kW/ft).

This study indicates that transient analyses for L2-3 pre-LOCE conditions may not be expected to have consequences that fall under Damage Criteria A in the LOFT Technical Specifications. Whether or not Damage Criteria B can be complied with (as is required by the LOFT Technical Specifications) is not discernable from the results of this study.

prk

cc: W/attachment
E. C. Anderson
C. H. Cooper
R. A. Dimenna
G. A. Dinneen
D. F. Elger
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LTR 111-127
APPENDIX B-2

SUMMARY: It was requested that work be done to generate a plot of MDNBR as a function of PLHGR for two sets of conditions. One set of conditions used worst case values of flow, T_{HOT} , power and power profile uncertainties. The input conditions are summarized below. (SCM)

WORST CASE CONDITIONS

$$\underline{FLOW}_L = \frac{(37.13 \text{ MW}) \frac{3.413 \times 10^6 \text{ BTU}}{\text{m-MW}}}{88.08 \frac{\text{BTU}}{\text{lbm}}} - [.395 \times 10^6]$$

$$= 1.04 \times 10^6 \text{ lbm/hr}$$

$$\underline{POWER} = \frac{\text{PLHGR}}{.3232} + 3.5$$

40.6 mw for L2-3

$$\underline{T_{HOT}} = 625^\circ\text{F}$$

PROFILE: L1057 $F_Q = 2.43$ FLAT RADIAL ASSUMPTION

$$F_Q^U = 7\% \quad F_{\Delta H}^U = 7\% \quad F_{\Delta H}^E = 1.032\%$$

$$F_Q^E = 3.2\% \quad F_Q^D = 1.1\% \quad F_{\Delta H}^{\text{Dens.}} = 1.1\%$$

$$\underline{PRESSURE} = 2440 \text{ psia}$$

NOMINAL CONDITIONS

$$\underline{FLOW}_L = 1.44 \times 10^6 \text{ lbm/hr (No 5% penalty to hot assembly)}$$

$$\underline{POWER} = \frac{\text{PLHGR}}{.3232}$$

37.1 mw for L2-3

47.5 mw for 16 lbm/hr

$$\underline{T_{HOT}} = 617^\circ\text{F}$$

PROFILE = L1057 $F_Q = 2.43$ NORMAL RADIAL DISTRIBUTION

No uncertainties, "HOT" pin is not placed in subchannel number 15.

$$\underline{PRESSURE} = 2262 \text{ psia}$$

RESULTS

PLHGR (kW/ft)	MDNBR (worst case)	MDNBR (nominal)
8	1.40	2.295
12	.9375	1.549
16	.6431	1.134

LTR 111-127
APPENDIX B-2

PLHGR (kW/ft)

46 0780

K&E 10 X 10 TO THE INCH • 7 X 10 INCHES
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APPENDIX B-3
COBRA ANALYSIS FOR L2-3 MODE 7
OPERATION WITH MAXIMUM
XENON BUILDIN

INTEROFFICE CORRESPONDENCE

date April 3, 1979

to S. A. Atkinson

from B. S. Anderson *Bruce S. Anderson*

subject MDNBR's FOR LOFT L2-3 OPERATION WITH A XENON PERTURBATED
AXIAL POWER PROFILE - BSA-5-79

- Refs: (a) Letter B. S. Anderson to S. A. Atkinson, "L2-3 Mode 8
Steady-State Limits -BSA-4-79" (March 23, 1979).
- (b) Letter S. A. Atkinson to T. M. Howe, "LOFT Full Power
Safety Margins with Xenon Peaking Perturbation - Atki-32-78"
(September 21, 1978).

Two COBRA IIIC cases have been run to determine steady-state MDNBR while operating at 39 MW and 50 MW with a xenon perturbed axial profile with a radial peaking factor of 1.60. Initial conditions for both cases are listed in Table I. Xenon buildup was generated by assuming that the reactor operated at 75 percent of full power for 100 hours to buildin equilibrium xenon, and then was shut down for 7.5 hours to allow the xenon concentration to reach a peak. The profile was then calculated using peak xenon at zero power, a coolant temperature of 555°F, and the CRA's withdrawn 59 inches. Using these assumptions the peak P/A is moved further downstream in the core (further from the bottom of the core). Figure 1 shows the axial profile. The reactor was then assumed to be brought up to power while this profile existed.

The MDNBR is 1.841 for the 39 MW case and 1.381 for the 50 MW case. The highest power which could occur in L2-3 is the worst case trip power of 42.7 MW. By linear interpolation between the 39 MW case and the 50 MW case the MDNBR will be 1.686 at a power of 42.7 MW. Linear interpolation was used based on Figure 2 which is a plot of power versus MDNBR from Reference (a). As can be seen the core power versus MDNBR is almost linear. A MDNBR of 1.686 compares favorably with case X50-2^(b) which is a 50 MW case with the CRA's 56 inches withdrawn, and a xenon perturbed axial profile. Steady-state MDNBR for case X50-2 is 1.553, and was shown to have a safety margin over case X50-1^(b). Case X50-1 is a 50 MW case with CRA's withdrawn 48 inches, a BOL axial power profile, and is the limiting case for steady-state MDNBR (1.402) reported in the FSAR. Since the highest power expected in LOFT during the L2-3 experiment will be less than 42.7 MW (MDNBR = 1.686) the safety margin with the xenon perturbed axial power profile shown in Figure 1 should be even greater than the margin for case X50-2. The xenon profile of Figure 1 should, therefore, be acceptable for any power level expected during normal L2-3 operation at high loop flow rate.

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APPENDIX B.3

S. A. Atkinson
April 3, 1979
BSA-5-79
Page 2

NOTE: To obtain position of CRA above fuel bottom, add two inches to withdrawn value.

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Attachments
As stated

cc: D. F. Elger
J. E. Fisher
B. L. Rushton
D. G. Satterwhite
R. P. Wadkins
B. S. Anderson File
Central File
TAB File 3.5

TABLE I

THERMAL-HYDRAULIC CONDITIONS FOR XENON PERTURBED
AXIAL PROFILE MDNBR STUDY CASES

<u>Parameters</u>	<u>Case 1</u>	<u>Case 2</u>
Power (MW)	39	50
Inlet Temperature (°F)	567.8	559.3
Pressure (psia)	2262	2262
Loop Flow (lb/hr)	3.42×10^6	3.42×10^6

Figure 1 - Xenon Perturbated Axial Profile

Figure 2 - Core Power versus MDNBR

APPENDIX B-4
MODE 8 TRANSIENT
ANALYSIS RESULTS

INTEROFFICE CORRESPONDENCE

date March 15, 1979

to E. C. Anderson

from S. A. Atkinson *S.A. Atkinson*

subject LOFT L2-3 MODE 8 SAFETY ANALYSIS RESULTS WITH COBRA IIIC
CORRECTION - Atki-9-79

- Refs:
- (a) S. A. Atkinson ltr to E. C. Anderson, Atki-7-79, "L2-3 Mode 8 Transient Analysis Results", March 13, 1979
 - (b) S. A. Atkinson ltr to E. C. Anderson, Atki-8-79, "COBRA IIIC - COBRA IV-I Comparison and Spacer Grid Loss Calculation Error", March 14, 1979
 - (c) S. A. Atkinson ltr to E. C. Anderson, Atki-4-79, "Steady-State (Safety) Limits for L2-3 Mode 8", February 22, 1979.

The analysis results (consequences) for the limiting transients are changed by the correction of the error in the computer coding for the LOFT spacer grids loss coefficient equation as discussed in Reference (b). Therefore, the limiting transients (those for which LOFT-3 MDNBR < 1.14) have been re-analyzed with the COBRA IIIC LOFT large core model and the spacer grid loss coefficient equation corrected as indicated in Reference (b). As indicated in Reference (b), the loss coefficient equation error results in a conservative calculation for MDNBR. Therefore, all other (non-limiting) transients, including the rod ejection accident, were not re-analyzed. The results of analysis for these other transients remains as transmitted by Reference (a), recognizing that the results are somewhat conservative.

The results of the analyses for the limiting transients with the corrected loss coefficient equation are:

<u>Accident</u>	<u>MDNBR</u>
Boron Dilution $R \leq 0.1 \times 10^{-4} \Delta p/s$	1.162
Boron Dilution or Slow CRWA $R \leq 0.7 \times 10^{-4} \Delta p/s$	1.150
CRWA $R > 0.7 \times 10^{-4} \Delta p/s$ ($R = 2.0 \times 10^{-4} \Delta p/s$)	1.154
Loss of Steam Load 17 s stroke time on SFCV	1.156

March 15, 1979
E. C. Anderson
Atki-9-79
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The limits on operating conditions and worst-case trips remain the same as transmitted by Reference (a).

In conclusion, the transient analyses demonstrate that the LOFT Damage Criteria for mode 8 operation can be met for operation within the bounds described in Reference (a) using the LOFT-3 correlation. Reference (a) indicated that additional safety margins may exist if the LOFT low-flow (ANC-6) data is used to define the 95-95 bound for DNB. But, COBRA calculations with the corrected spacer grid loss coefficient equation indicate that the mass fluxes at the point of MDNBR are at the upper bound of the ANC-6 data. Therefore, it would not be appropriate to use the ANC-6 95-95 bound without including LOFT DNB data for higher mass fluxes ($G > 0.6 \text{ Mlbm/h-ft}^2$).

Steady-state COBRA IIIC calculations have also been done, with the corrected spacer grid loss coefficient equation, for a high off-nominal hot leg temperature of 613°F . The results reported in Reference (c) were for a 621°F high off-nominal hot leg temperature. Also, these steady-state calculations defined the safety limit for each parameter in terms of the limit of Damage Criteria A ($\text{MDNBR} > 1.14$), or physical or data limits. The resulting safety limits for a 613°F high off-nominal hot leg temperature are:

<u>Parameter</u>	<u>Safety Limit</u>	<u>Limiting Condition</u>	<u>Comments</u>
Reactor Power	Nominal power + 6 MW	$\text{MDNBR} > 1.14$	Limit was 41 MW for P/A = 2.60
Hot Leg Temperature	639°F	Reactor outlet saturation for low off-nominal pressure = 2050 psia	$\text{MDNBR} > 1.14$
PCS Loop Flow	$< 0.8 \times 10^6 \text{ lbm/h}$	Lower limit of LOFT-3 correlation (mass flux of $0.4 \times 10^6 \text{ lbm/h-ft}^2$)	$\text{MDNBR} > 1.14$
PCS pressure (high) (Reactor Outlet)	2490 psia	$\text{MDNBR} \geq 1.14$	

ul

cc: B. S. Anderson
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TAB File 3.5

TR 111-127
APPENDIX B-4

cc. D. J. Anderson
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INTEROFFICE CORRESPONDENCE

Central File
TAB File 3.5

date March 13, 1979
to E. C. Anderson
from S. A. Atkinson *S. A. Atkinson*
subject L2-3 MODE 8 TRANSIENT ANALYSIS RESULTS - Atki-7-79

Transient analyses for accidents and operational transients have been completed for mode 8 (LOCE pre-blowdown) operation for the L2-3 test. The applicable damage criteria can be met for all transients for the following limitations on operating conditions:

Measured peak-to-core average Power ratio (P/A)	≤ 2.60
Reactor power (MW)	$P_{ON} - 4.5$ to $P_{ON} + 2.5$ $P_{ON} \equiv \text{nominal power} = 90.23/(P/A)$
PCS loop flow (lbm/h)	$\geq [3.327/(P/A)] - 0.125$
Pressure (reactor outlet) (psia)	2050 to 2257
Hot leg temperature	$\leq 613^{\circ}\text{F}$

For the transient analyses, the following worst-case trippoints and trip time constants were assumed:

<u>Trip Parameter</u>	<u>Worst-Case Trippoint</u>	<u>Trip Time Constant (s)</u>
Reactor power (MW)		
$P/A > 2.55$	$P_{ON} + 3.5$	1.0
$2.50 \leq P/A \leq 2.55$	$P_{ON} + 3.8$	
$P/A \leq 2.50$	$P_{ON} + 4.2$	
PCS loop flow (lbm/h)	1.01×10^6	1.0
Pressure (psia)		
low	2000	1.0
high	2340	1.0
Hot leg temperature ($^{\circ}\text{F}$)	$(T_{\text{hot}})_{\text{nom}} + 8$	15
RABV opening	10% full open	0.10
Loss of PSMG field		0.1 s delay

The steam generator main feedwater was assumed to be tripped 1.25 s after scram is initiated.

The equations for nominal power and initial (off-nominal low) flow were supplied by T. M. Howe on January 4, 1979, and are consistent with the specified conditions in the L2 EOS (a). Because the pressure and temperature conditions found to be acceptable for the L2-3 transients are slightly different from those specified in the L2 EOS, then ΔH in the flow equation will change. The flow equation is (a):

$$\begin{aligned} \text{Loop flow } \dot{m} &= \frac{P_L}{\Delta H} \quad 3.413 \text{ Mlbm/h} \\ P_L &= 7.519 \frac{\text{MLHGR}}{(P/A)} \quad \text{MW} = 90.23/(P/A) \\ \dot{m} &= 308 / [(P/A)(\Delta H)] \end{aligned}$$

For 2200 psia and 609°F T_{hot} (nominal), $\Delta H = 86.78 \text{ Btu/lbm}$ (for 64.5°F ΔT across the core). For this condition,

$$\dot{m} = 3.55/(P/A)$$

For the mode 8 safety analysis, the assumed off-nominal flows and low flow trips were based on:

$$\dot{m} = 3.502/(P/A)$$

Therefore, the assumed flows for the safety analysis are conservative for operation with T_{hot} (nominal) $\leq 609^\circ\text{F}$.

The analyzed transients are listed in the attached Table I, along with the results of the COBRA and/or FRAP analysis for the transients.

All the L2-3 transients were analyzed assuming a lowered nominal pressure (lowered from the 2262 psia assumed for previous operation to 2169 psia). This lowering of the nominal pressure was considered necessary to gain safety margin when transients result in increased pressure to near the high pressure trippoint. Reference (e) discusses the improvement in safety margin obtained by lowering the nominal pressure for L2-3 mode 8 low operating flow.

The transients were analyzed with initial power and pressure adjusted within their control bands such that power and pressure are at their worst-case trip levels simultaneously. The boron dilution and control rod withdrawal accidents were tripped by hot leg temperature with power and pressure at or above their worst-case trip levels.

The MDNBR (calculated using the LOFT-3 DNB correlation (f)), was compared to the 95-95 DNB bound for the LOFT low flow DNB data (ANC-6 fuel bundle data). The 95-95 bound is the bound for which 95% of the DNB population for the fuel pin being analyzed is enveloped by the bound (a 95% probability of not having DNB for that fuel pin) with a 95% confidence level. The determination of the

95-95 bound for the LOFT low flow DNB data, and the corresponding LOFT-3 MDNBR for the transient hot channel mass velocity (near 0.5 Mlbm/h-ft²), is discussed in Reference (f). This bound is used to test against the intent of Damage Criteria A, and for forcing DNB in FRAP calculations for Damage Criteria B.

The analyses for the boron dilution accidents, CRWA's, and the loss of steam load accident used a revised value for the LOFT fuel engineering hot spot factor of 1.026. This revised factor is based on the 3-standard deviation bound for combined fuel pellet enrichment and fuel pellet density (a 99.9% probability that the actual value is less than or equal to 1.026). Development of the revised engineering hot spot factor is discussed in Reference (g).

Analysis for a measured peak-to-core-average power ratio (P/A) of 2.60 are limiting, even when analyses for lesser values of P/A was done with a larger margin to the worst-case power trip. This result is evident in the comparisons for MDNBR tabulated in Table 2. Table 2 also shows that the LOFT-3 MDNBR is above the ANC-6 95-95 bound (MDNBR > 1.106 for these transients with margins between nominal and WCT power levels of:

<u>P/A</u>	<u>WCT margin above nominal (MW)</u>
2.50	4.2
2.55	3.8
2.60	3.5

The maximum reactivity addition ramp rate analyzed for was $8 \times 10^{-4} \Delta\rho/s$. For a maximum differential worth for a 4-CRA withdrawal of 0.141% $\Delta\rho/\text{inch}$ (the maximum worth for L2-2 operation) the reactivity addition rate for a 29 inches/min. maximum withdrawal speed is $6.82 \times 10^{-4} \Delta\rho/s$. The L2-3 maximum differential worth has been calculated to be only 0.127% $\Delta\rho/\text{inch}$. Therefore, the $8 \times 10^{-4} \Delta\rho/s$ ramp rate analyzed for envelopes the maximum CRWA for L2-3 mode 8 operation.

In conclusion, the transient analyses demonstrate that the LOFT Damage Criteria for mode 8 operation can be met for operation within the bounds described at the beginning of this correspondence. When MDNBR is compared to the 95-95 bound for the low flow (ANC-6) DNB data, it is seen that there is some extra margin available. Therefore, it may have been possible to meet the Damage Criteria with slightly more severe conditions corresponding more closely to the L2 EOS specifications. An analysis was done for a $0.5 \times 10^{-4} \Delta\rho/s$ ramp rate with an initial off-nominal value for T_{hot} of 617°F. MDNBR = 1.109, but the ANC-6 95-95 bound for this transient was 1.116. Therefore, the available margins are small. Additionally, since future tests (such as L2-5 with a changed center fuel bundle and L2-4 at higher power density) will be more severe, it is advisable to take steps now to increase the available safety margin.

ul

Attachments:
As stated

REFERENCES

- (a) P. A. Harris, et al, "LOFT Experiment Operating Specification, Volume 2, Power Ascension Test Series L2", July 1978.
- (b) S. A. Atkinson ltr to E. C. Anderson, Atki-5-79, "Pressure Trips for L2-3 Mode 8 with 2169 psia Nominal Pressure," March 2, 1979.
- (c) S. A. Atkinson, "L2-2 Pre-LOCE Maneuver Core Safety Analysis," LTR 111-116, November 17, 1978, Section V.
- (d) B. S. Anderson ltr to E. C. Anderson, BSA-1-79, "Results of L2-3 Rod Ejection Cases," January 19, 1979.
- (e) S. A. Atkinson ltr to E. C. Anderson, Atki-3-79, "Nominal Pressure Decrease for L2-3 Mode 8," February 2, 1979.
- (f) S. A. Atkinson ltr to E. C. Anderson, Atki-6-79, "95% Probability (at 95% Confidence) Bound for LOFT Bundle DNB Data at Low Flows," March 7, 1979.
- (g) N. F. Fausz ltr to E. C. Anderson, NFF-1-79, "Statistical Evaluation of the LOFT Heat Flux Engineering Hot Spot Factor," February 27, 1979.

TABLE 1

L2-3 MODE 8 TRANSIENT ANALYSIS RESULTS FOR $P/A \leq 2.60$

Event Category	Accident	[1]		[2]		[3]		Notes
		(T) hot i		MDNBR		ANC-6 95-95 Bound		
Operational Transient	Boron dilution							
	$R \leq 0.1 \times 10^{-4} \Delta\rho/s$	609		1.14 ^[5]		[4]		
		613		1.131		1.106		
	$R < 0.1 \times 10^{-4} \Delta\rho/s$	615		1.15		[4]		
Incident of Moderate Frequency	Boron dilution or CRWA							
	$R \leq 0.5 \times 10^{-4} \Delta\rho/s$ ^[6]	609		1.146		[4]		
		613		1.125		1.106		
	$R = 1.5 \times 10^{-4} \Delta\rho/s$ ^[7]	609		1.125 ^[5]				
		613		1.120		1.106		
	Control rod drop	613		>1.25		[4]		[8]
	Control rod misalignment							[9]
	Loss of pumps with flywheel coastdown	617		1.209		[4]		[10]
	Loss of steam load ^[11]							
	10 s stroke time on SFCV	609		1.131				[5]
	17 s stroke time on SFCV	613		1.120		1.080		
	Steam generator feedwater accidents							[12]
	Steam load increase	617		1.237		[4]		
	40 s stroke time on SFCV							
	Pressure control failure			>1.25				[8]
	RABV opening	617		1.195		[4]		
	10%/s opening rate							
Infrequent Fault	Rapid one pump loss	617		1.183		[4]		[10]
	CRWA							
	$R = 1.5 \times 10^{-4} \Delta\rho/s$ ^[7]	617		1.065		1.117		PCST=1968°F ^[16]
Limiting Fault	Rod ejection (0.363% $\Delta\rho$ step)	621		1.03				PCST=1910°F
								Hot spot fuel enthalpy = 153 cal/g ^[13]
	Steamline break							[14]
	Rapid two pump loss. Low flow trip.	617		1.175		[4]		[10]
	Rapid two pump loss Loss of field trip	617		1.202		[4]		[10]
	Locked pump rotor (one pump)	617		1.213		[4]		[15]

TABLE 1 NOTES

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1. High off-nominal hot leg temperature. This temperature is 4°F above nominal.
2. Calculated using the LOFT-3 correlation.
3. For a 95% probability of no DNB with a 95% confidence for the low flow (ANC-6 test bundle) LOFT DNB data. This number is the LOFT-3 MDNBR which corresponds to the ANC-6 95-95 bound. See Reference (b) for more detail.
4. For the L2-3 mode 8 transients, in which $X < 0$ at the point of MDNBR, the LOFT-3 MDNBR corresponding to the ANC-6 95-95 bound is less than 1.14, the 95-95 bound for the LOFT-3 correlation.
5. For a worst-case power trippoint 3.8 MW above nominal.
6. This is the limiting reactivity ramp rate if control rod withdrawal is inhibited.
7. This is the limiting reactivity ramp rate for all rates up to and including $8 \times 10^{-4} \Delta\rho/s$. However, there is little difference in MDNBR for $0.7 \times 10^{-4} \leq R \leq 2 \times 10^{-4} \Delta\rho/s$.
8. MDNBR increases throughout this transient. See Reference (b) for a discussion of the results of control rod drop accident analyses.
9. Consequences and protective controls for this accident are no different than for L2-2 mode 8 operation. The analysis and discussion in Reference (c) applies to L2-3 mode 8 operation.
10. A loss of flow accident during the low flow maneuver is not as severe as during low flow operation. This was shown in the L2-2 mode 8 safety analysis (c).
11. This accident is the limiting transient for Incidents of Moderate Frequency.
12. The consequences of loss of feedwater or increased feedwater transients are enveloped by the steam load accidents.
13. The results of the L2-3 mode 8 analysis for a maximum control rod ejection accident are discussed in Reference (d). The rod ejection accident analysis assumed a measured P/A = 2.66, and a power worst-case trippoint of 46.5 MW (a trippoint sufficient to cover any measured P/A down to 2.13). The ejected CRA worth ($0.363\% \Delta\rho$) is significantly greater than the maximum calculated worth for L2-3 operation ($0.2\% \Delta\rho$). The hot spot fuel enthalpy is significantly less than the Damage Criteria C limit of 280 cal/g, and the maximum cladding temperature is less than 2200°F. Therefore, the consequences of the rod ejection accident not only meet Damage Criteria D (as required by the Technical Specifications), but also Damage Criteria C.

TABLE 1 NOTES

14. The analysis and consequences for the steamline break maximum cooldown transient is no different from that analyzed for L2-2 mode 8 operation, as discussed in Reference (c). As discussed in Reference (c), the MDNBR increases throughout this transient, even if initiated from high off-nominal power.
15. The event category for a locked pump rotor has not been established, other than it is at least an infrequent fault. This transient is conservatively simulated as a step loss of flow to 50% of the original flow. In actuality, flow will decrease as a rapid ramp to a flow rate greater than 50%. One pump alone, with a high flow resistance (due to the locked rotor) in the other pump, will pump more than 50% of the two-pump flow rate. This transient was analyzed using COBRA IV-I with its improved solution routine, since COBRA IIIC was not able to solve for the effects of such a rapid flow decrease.
16. The consequences for this transient for lower values of off-nominal hot leg temperature are listed under the Incidents of Moderate Frequency. The indicated peak clad surface temperature was calculated by forcing regions of the hot fuel pin into film boiling when the LOFT-3 MDNBR ≤ 1.12 , above the ANC-6 95-95 bound value of the LOFT-3 MDNBR. When the transient was analyzed forcing film boiling when MDNBR ≤ 1.14 , the peak clad surface temperature was 2052°F. Additional conservatisms for this case were a loop flow corresponding to a P/A = 2.66, and use of the conservative FSAR engineering hot spot factor (1.032). The consequences of this transient case meet the limits of Damage Criteria C.

TABLE 2

TRANSIENT ANALYSIS COMPARISONS FOR BORON DILUTION
OR CRWA'S WITH DIFFERENT MEASURED P/A

	$0.1 \times 10^{-4} \Delta p/s$				$0.5 \times 10^{-4} \Delta p/s$				
Measured P/A	2.50	2.60	2.55	2.60	2.50	2.60	2.55	2.55	2.60
Initial T_{hot} (°F)	609	609	613	613	613	613	613	613	613
Power WCT Margin above Nominal (MW)	4.2	3.5	3.5	3.5	4.2	4.0	3.8	3.5	3.5
Pressure (psia) when MDNBR	2369	2370	2340	2340	2379	2379	2340	2340	2340
Engineering Hot Spot Factor	1.026	1.026	1.026	1.026	1.032	1.032	1.026	1.026	1.026
MDNBR	1.196	1.14	1.143	1.131	1.110	1.080	1.130 ²	1.133	1.125

INTEROFFICE CORRESPONDENCE

date March 13, 1979
to S. A. Atkinson
from D. F. Elger *DF Elger*
subject THERMAL SAFETY ANALYSIS OF LOFT REACTOR PLANT ACCIDENT CONDITIONS
OCCURRING DURING THE L2-3 TEST MODE 8 OPERATION - DFE-4-79

- Refs: (a) S. A. Atkinson ltr to F. K. Hyer, "Initial Hybrid Analysis for L2-3 Safety Analysis Support" Atki-38-78 (November 20, 1978)
(b) S. A. Atkinson ltr to E. C. Anderson, "Pressure Trips for L2-3 Mode 8 with 2169 psia Nominal Pressure," Atki-5-79 (March 2, 1979)
(c) R. A. Dimenna/G. L. Hunt ltr to S. A. Atkinson, "PPS Trip Setpoints and Time Constants for L2-2 Safety Analyses" RAD-2-78 (May 16, 1978)
(d) S. A. Atkinson ltr to E. C. Anderson, "Nominal Pressure Decrease for L2-3 Mode 8," Atki-3-79 (February 2, 1979)

This letter presents analysis results for the LOFT plant transients described in reference (a), with the exception of the control rod withdrawal accident. The LOFT reactor hybrid computer model combined with a COBRA IIIC/IV-I LOFT core model was used to show that the minimum predicted DNBR (LOFT-3 DNB correlation) was above 1.14 (this represents a 95% probability at a 95% confidence level of not reaching DNB) for all cases. Therefore, all plant transients satisfied the LOFT FSAR Section 15 allowable consequence levels and the applicable damage criteria in the LOFT Technical Specifications (since Damage Criteria A was met for all these transients).

The results are:

<u>Run ID</u>	<u>Description</u>	<u>MDNBR</u>
ESAL231C	2 pump failure with flywheel coastdown low flow trip	1.209
ESAL232C7	Loss of steam load-high pressure trip	1.156
ESAL233C	Steam load increase - high power trip	1.237
ESAL234C1	5%/sec inadvertent RABV opening-trip at 10% RABV opening	1.205
ESAL234C2	Same as above except opening rate = 10%/sec	1.195

March 13, 1979
S. A. Atkinson
DFE-4-79
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<u>Run ID</u>	<u>Description</u>	<u>MDNBR</u>
ESAL235C1	one pump failure without flywheel coastdown low flow trip	1.183
ESAL236C1	two pump failure without flywheel coastdown field current loss trip	1.202
ESAL236C2	two pump failure without flywheel coastdown low flow trip	1.175
ESAL237C	one control rod drop. Worth=\$2.90 low pressure trip	No DNBR calculated see reference (b)
ESAL238C	locked pump rotor - 50% step flow reduction low flow trip	1.213

In general each plant transient was assumed to start from off-nominal reactor conditions and to trip at worst case values. The initial conditions were defined by the L2-3 desired test conditions corresponding to a $P/A = 2.60$. For a given transient, these conditions produce a slightly lower MDNBR than conditions corresponding to a lower P/A ratio.

Two important changes were made during the course of the analysis: (1) It was discovered that the scram curve being used, the "10/26 LF scram curve", was incorrect and a new scram curve, the "2/13 L2-3 worst case scram curve", was developed, (2) an error was found and corrected in COBRA IIIC and COBRA IV-I.

The new scram curve, the "2/13 L2-3 worst case scram curve," was developed after most of the analysis was completed and rather than redo all the computer runs, only the plant transients most sensitive to the scram assumption were rerun. DNBR's were not significantly effected ($< 0.5\%$). Figure 1 is a plot of the two scram curves.

The COBRA error was a coding error in a modification of the fuel bundle grid spacer loss coefficient calculation for the LOFT versions. The error tends to decrease the local coolant mass flux at the point of MDNBR and consequently added extra conservatism to the COBRA model. Only the steam load loss, the limiting accident studied, was run using the corrected version of COBRA. This increased the predicted MDNBR by 3.1%. The error effects and correction will be documented in a forthcoming letter by J. E. Fisher.

Table 1 is a listing of reactivity coefficients and scram setpoints used in the analysis. Table 2 is a detailed listing of the plant transient initial conditions and results.

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The ultimate result of these calculations is to define L2-3 mode 8 operation as follows

$$F_Q \text{ (measured)} \leq 2.60$$

$$\text{Nominal power (MW)} \leq 90.228/F_Q \text{ (measured)}$$

$$\text{Nominal loop flow (Mlbm/hr)} \geq \frac{\text{Nominal reactor power (MW)}}{.0387}$$

$$\text{Nominal hot leg temperature (°F)} \leq 609^\circ\text{F}$$

$$\text{Nominal pressure (psia)} \leq 2169 \text{ psia}$$

Additionally reactor scram setpoints must be defined by the measured F_Q using the scenario in reference (c) for hot leg temperature, power and loop flow and reference (d) for pressure. Note: other analyses not considered in this letter (reactivity addition transients) could impose more limiting conditions for reactor operation than the above limits.

ul

Attachments:
As stated

cc: B. S. Anderson
D. F. Elger
N. F. Fausz
J. E. Fisher
F. K. Hyer
D. G. Satterwhite
R. P. Wadkins
Central File
TAB File 3.5

Figure 1: SCRAM CURVES USED IN THE ANALYSES

TABLE 1

REACTIVITY COEFFICIENTS AND SCRAM SETPOINTS

Reactivity Coefficients^[1]

Doppler

High (HD) - $1.08 \times 10^{-5} \Delta\rho/^\circ\text{F}$

Low (LD) - $0.44 \times 10^{-5} \Delta\rho/^\circ\text{F}$

Moderator

High (HM) $6.5 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$

Low (LM) $2.6 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$

Scram Setpoints

Power

38.2 MW (3.5 MW above nominal)

Loop Flow^[2] 1.01 Mlbm/hr (see Figure 1, Ref. (c))

Hot leg temperature 617°F (4° above a nominal 613°F)
621°F (4° above a nominal 617°F)

High pressure 2340 psia (171 psia above nominal)

Low pressure 2000 psia (169 psia below nominal)

[1] For a given transient, reactivity coefficients are chosen to maximize the severity.

[2] The actual low flow_l trip point was ~ .98 Mlbm/hr. The correct value of low flow_l trip setpoint for $F_Q = 2.6$ is 1.035 Mlbm/hr.

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TABLE 2

SUMMARY OF L2-3 ACCIDENT ANALYSIS

<u>Run ID</u>	<u>Transient</u>	<u>Initial Conditions</u>	<u>MDNBR</u>
ESAL231C	2 pump failure with flywheel coastdown Low flow trip HD/LM 10/26 LF scram curve Small core model - COBRA IIIC	37.2 MW 1.15 Mlbm/hr 2257 psia 617°F $F_0 = 2.60$ $h_g = 750 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$	1.209 @ 3 sec
ESAL232C7	Loss of steam load-17 sec stroke time High pressure trip ⁽¹⁾ HD/LM 2/13 L2-3 worst case scram curve Large core model - COBRA IIIC Corrected version of COBRA	36.8 MW 1.15 Mlbm/hr 2088 psia 613°F $F_0 = 2.6$ $h_g = 750 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$	1.156 @ 14 sec
ESAL232C7A	Same as above except used version of COBRA with error	Same as above	1.120 @14 sec
ESAL233C	Steamload increase - 40 sec stroke time Power trip LD/HM 10/26 LF scram curve Small core model - COBRA IIIC	37.2 MW 1.15 Mlbm/hr 2257 psia 617°F $F_0 = 2.60$ $h_g = 10000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$	1.237 @14.2 sec
ESAL234C1	Inadvertent RABV opening accident Opening rate = 5%/sec Trip @ 10% RABV opening LD/LM 10/26 LF scram curve Small core model - COBRA IIIC	37.2 MW 1.15 Mlbm/hr 2257 psia $F_0 = 2.60$ $h_g = 750 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ 617°F	1.205 @1.3 sec
ESAL234C2	Same as above except opening rate = 10% sec	Same as above	1.195 @1.2 sec
ESAL235C	1 pump failure without flywheel coastdown Low flow trip HD/LM 10/26 LF Scram curve Small core model - COBRA IIIC	Same as above	1.188 @1.4 sec
ESAL235C1	Same as above except used 2/13 L2-3 worst case scram curve	Same as above	1.183 @1.4 sec
ESAL236C1	2 pump failure without flywheel coastdown. Loss of field current trip HD/LM 10/26 LF scram curve Small core model - COBRA IIIC	Same as above	1.202 @.8 sec
ESAL236C2	Same as above except low flow trip.	Same as above	1.175 @1.4 sec

1: The initial reactor power was scaled such that the reactor power setpoint was reached at the same time as the reactor high pressure scram occurred.

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TABLE 2 (continued)

<u>Run ID</u>	<u>Transient</u>	<u>Initial Conditions</u>	<u>MDNBR</u>
ESAL236C3	Same as above except used 2/13 L2-3 worst case scram curve	Same as above	1.178 @1.4 sec
ESAL237C	Control rod drop - one rod worth \$2.90 Low pressure scram HD/HM 10/26 LF scram curve No COBRA run	Same as above	No DNBR calculation See Ref. (b)
ESAL238C	Locked pump rotor - 50% step flow reduction Low flow trip LD/LM 10/26 LF scram curve COBRA IV-I ⁽²⁾ - small core	Same as above	1.253 @.7 sec

- 2: COBRA IV-I with its improved solution routine was able to perform the transient analysis for this rapid flow loss accident, whereas COBRA IIIC could not.

cc: B. S. Anderson
D. F. Elger
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D. G. Satterwhite (2)
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Central File
TAB File 3.5

LTR 111-127

INTEROFFICE CORRESPONDENCE

APPENDIX B.4

date March 2, 1979
to E. C. Anderson
from S. A. Atkinson *S.A. Atkinson*
subject PRESSURE TRIPS FOR L2-³ MODE 8 WITH 2169 PSIA NOMINAL
PRESSURE - Atki-5-79

- Ref:
- (a) S. A. Atkinson ltr to E. C. Anderson, Atki-3-79, "Nominal Pressure Decrease for L2-3 Mode 8," February 2, 1979
 - (b) S. A. Atkinson, "L2-2 Pre-LOCE Maneuver Core Safety Analysis," LTR 111-116 (RE-A-78-257) November 17, 1978
 - (c) "LOFT Integral Test System Final Safety Analysis Report," Revision 1, August 1977

Reference (a) suggested that LOFT nominal operating pressure be reduced by 93 to 100 psi for L2-3 Mode 9 operation to gain safety margin for potential transients during Mode 8 operation. Operation with such a reduced nominal pressure would result in a reduction in the low-pressure worst-case trippoint (WCT) below the 2076 psia WCT for the power range and L2-2 Mode 8 safety analyses. The L2-2 Mode 8 safety analyses concluded (Ref. (b), Section II), "The L2-2 pre-LOCE analyses did not establish any limitation on operation at a low pressure, other than what might be implied by the 2000 psia lower limit for the LOFT-3 DNB data." Therefore, a 2000 psia WCT was assumed for the L2-3 Mode 8 safety analyses. The L2-3 Mode 8 safety analyses assumed a 2169 psia nominal pressure, at the reactor outlet.

The L2-3 Mode 8 transients were evaluated for a 2000 psia WCT, and the consequences were found to be satisfactory with this WCT. In addition, the control rod drop accident, which is the controlling transient for the low-pressure WCT, was evaluated for both Mode 8 low flow and Mode 7 full flow operation. The 2000 psia WCT was found to be satisfactory for either flow rate. The evaluation of the 2000 psia WCT is discussed in more detail in Attachment 2.

A decrease in nominal pressure can also allow a decrease in the high-pressure WCT. This decrease (a decrease of 100 psi to a 2340 psia WCT) has been found to be necessary for mitigation of the loss of steam load transient and the boron-dilution or slow control rod withdrawal accidents. These accidents will be discussed in more detail in the L2-3 Mode 8 core safety analysis report (LTR 111-127).

The pressure assumptions used and found acceptable for the L2-3 Mode 8 core safety analyses are as indicated in the attached Table I.

jj

Attachments

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TABLE I
L2-3 MODE 8 CORE SAFETY ANALYSIS
PRESSURE ASSUMPTIONS

<u>Pressure Point</u>	<u>Pressure Difference (psi)</u>	<u>psia</u>
High-Pressure WCT		2340
High Off-Nominal		2257
Channel Accuracy	+ 28	
Indicator Accuracy	+ 10	
Top of Control Band		2219
Top Control Band	+ 50	
Nominal Pressure		2169
Bottom Control Band	- 50	
Bottom of Control Band		2119
For High-Pressure Transients		
Channel Accuracy	- 28	
Indicator Accuracy	- 10	
Low Off-Nominal*		2081
Low-Pressure WCT		2000

* Low off-nominal pressure is only considered for those high-pressure transients which are worsened by initiation from a lower pressure for which scram is then delayed. Therefore, the channel accuracy can be taken in a direction consistent with the definition of the high-pressure WCT in which case, actual low off-nominal pressure would be 2137 psia when indicated as 2119 psia. Limiting safety analyses (loss of steam load accident) used 2068 psia as the low off-nominal pressure (or 2070 psia indicated for a +28 psi channel accuracy), an extra low pressure margin of 49 psia.

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EVALUATION FOR A 2000 PSIA WCT

I. EVALUATION OF TRANSIENTS FOR A LOW-PRESSURE TRIP

The LOFT transients which are tripped by low pressure are:

- 1) The steamline break accident at low power.
- 2) An automatic pressure control system failure.
- 3) The control rod drop accident.
- 4) LOCA (trips on low pressurizer pressure coincident with low pressurizer level).

The steam line break accident at low power is a cooldown transient which is worsened by a higher low-pressure trip (a quicker trip will result in slightly greater cooldown). However, the analysis for the steamline break was done assuming an instant trip^(a). Therefore, the low-pressure WCT is not a consideration for this transient.

The MDNBR increases throughout the transient for an automatic pressure control system failure (FSAR^(e), Section 15.2.12, and LTR 111-116^(b)) as the power and temperature decrease in addition to the pressure decrease. Pressure will continue to decrease and the reactor will be scrammed by the low-pressure WCT. This transient, therefore, does not define any limit or lower bound for the low-pressure WCT.

The ECCS trippoint for a LOCA is 1850 psia. As long as the low-pressure WCT is above this, then the low-pressure WCT is not a consideration for a LOCA unless the time to reactor trip is important. However, upon the initiation of the LOCA, pressure very rapidly drops to saturation (~ 1700 psia). Therefore, a slight difference in the low pressure trip or the initial operating pressure will not significantly effect the LOCA transient.

The MDNBR also increases throughout the transient for a control rod drop accident. However, pressure does not continually decrease for this transient. If the reactor is to be automatically shutdown in response to a dropped control rod assembly (CRA), then some limiting (low bound) value for the low-pressure WCT needs to be defined which will result in a reactor trip for this transient. For the Mode 8 core safety analysis for the L2-2 test, it was concluded^(c), "The L2-2 pre-LOCE analyses did not establish any limitation on

operation at a low pressure, other than what might be implied by the 2000 psia lower limit for the LOFT-3 DNB data". Therefore, a 2000 psia low-pressure WCT was evaluated for a control rod drop accident during L2-3 Mode 8 operation.

II. L2-3 CONTROL ROD DROP ACCIDENT

The control rod drop accident was simulated on the Hybrid computer for a low worth CRA (2.1% $\Delta\rho$). The drop of a low worth CRA results in a greater power recovery and less pressure decrease, as was shown for the L2-2 core safety analysis^(d). The Hybrid computer results show:

- 1) The reactor is tripped by a 2000 psia low-pressure WCT.
- 2) At the time of maximum power recovery:
 - power is only recovered to 49% of initial power.
 - core average temperature has decreased 26°F.
 - core flow has increased by 8%.

A dropped CRA causes a radial flux tilt in the core which results in an increased core peak-to-average power ratio and increased integrated rod power for the hot rod. This effect was discussed in LTR 111-116 for the control rod drop accident^(d). As indicated in LTR 111-116, the hot rod power is expected to increase by 10% due to the radial flux tilt. This result is based on power distribution calculations given in Reference (g). The results in Reference (g) also indicates that the axial power shape is essentially unchanged by the dropped rod. The change in MDNBR can be estimated based only on the change in power level, at maximum power recovery, and the radial power distribution increase by^(d):

$$\Delta\text{MDNBR} = [(1-.10) \frac{\text{Initial Pwr}}{\text{Max Recovery Pwr}} - 1] \quad (\text{A-1})$$

This estimate assumes that the change in MDNBR is linear with power. Analysis experience with COBRA has shown that the MDNBR will change by more than the power change, that is:

$$\Delta\text{MDNBR} = C \frac{\text{Initial Pwr}}{\text{Final Pwr}}$$

where $C > 1$.

The above estimate (Equation A-1) also neglects the effects of lower temperature and increased flow which will improve (increase) the MDNBR. Using Equation (A-1), for the L2-3 Mode 8 control rod drop, MDNBR will increase by:

$$\Delta \text{MDNBR} = \frac{(0.90)}{(0.49)} - 1 = 84\%$$

The following Figure A.1 shows the transient power change for the control rod drop during L2-3 Mode 8 operation.

A control rod drop was also simulated for L2-3 power level, but with full (Mode 7) reactor flow (3.42×10^6 lbm/h). The Hybrid computer results show:

- 1) The reactor is tripped by a 2000 psia low-pressure WCT.
- 2) At the time of maximum power recovery: (see the following Figure A.2 for the transient power)
 - power only recovers to 34% of initial power.
 - core average temperature has decreased 28°F.
 - core flow has increased by 6%.
 - the estimated increase in MDNBR is:

$$\Delta \text{MDNBR} = \frac{(0.90)}{(0.34)} - 1 = 165\%$$

For this simulation at full flow, the moderator and Doppler feedback coefficients were also increased above the high values being assumed for the L2-3 core safety analysis. The coefficients used were increased 7 to 10% above those in LTR 111-52^(h) for a hot reactor ($T_{\text{ave}} = 584^\circ\text{F}$) and only 100 ppm boron concentration in the primary coolant system. The corresponding coefficients used are:

	L2-3 Mode 8 Low Flow	L2-3 Power Full Flow
Moderator Coefficient	$6.5 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$	$8 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$
Doppler Coefficient	$1.08 \times 10^{-5} \Delta\rho/^\circ\text{F}$	$1.1 \times 10^{-5} \Delta\rho/^\circ\text{F}$

As seen by the simulation results, the safety margin is greater for a dropped CRA for full flow operation. Therefore, for L2-3 operation, a 2000 psia low-pressure WCT is suitable for any flow rate within the operating bounds, Mode 8 low off-nominal flow to 4×10^6 lbm/h.

III. FULL POWER MODE 7 CONTROL ROD DROP ACCIDENT

Hybrid computer simulation was also done for a control rod drop accident during full power (Mode 7) operation with a 2000 psia low-pressure WCT. The original analysis for this case in the FSAR^(f) assumed an initial pressure near nominal (2259 psia) and a dropped CRA worth 2.39% $\Delta\rho$. Two reanalyses were done. One with the FSAR maximum values for moderator and Doppler feedback coefficients ($9.1 \times 10^{-3} \Delta\rho/\text{lbm}/\text{ft}^3$ and $-1.5 \times 10^{-5} \Delta\rho/^\circ\text{F}$, respectfully), and another with the high values used for the full flow, L2-3 power case discussed above. The results for the control rod drop from full power (53 MW off-nominal power) are:

- 1) The reactor is tripped by a 2000 psia low pressure WCT.
- 2) At the time of maximum power recovery:

	<u>FSAR Max. Feedback</u>	<u>LTR 111-52 Max Feedback</u>
Power Recovery % of Initial (see Figures A.3 and A.4)	74	50
Core Average Temp Decrease ($^\circ\text{F}$)	29	29
Core Flow Increase (%)	5.5	6
Increase in MDNBR with 10% Flux Tilt (%)	22	78

Since the FSAR assumption of initiation from near nominal pressure (2259 psia) is above the high off-nominal pressure for an assumed decrease in nominal pressure for L2-3 operation (2257 psia high off-nominal), then the above results apply to operation with reduced nominal pressure as well. Therefore, the control rod drop accidents analyzed above demonstrate that a 2000 psia low-pressure WCT is suitable for Mode 7 or Mode 8 operation with the nominal pressure reduced to 2169 psia. The following Table A.I tabulates the results and assumptions for all of the control rod drop accidents analyzed. These results do not, however, imply that it is acceptable to operate at full power in Mode 7 with reduced nominal pressure, as other transients or safety analysis assumptions may have to be more fully considered.

IV. REFERENCES

- (a) S. A. Atkinson, "L2-2 Pre-LOCE Maneuver Core Safety Analysis," LTR 111-116 (RE-A-78-257), November 17, 1978, Section V.4.2.2.
- (b) Ibid, Section V.2.10.
- (c) Ibid, Section II.
- (d) Ibid, Section V.2.3.
- (e) "LOFT Integral Test System Final Safety Analysis Report," Revision 1, August 1977, Section 15.2.12.
- (f) Ibid, Section 15.2.3.
- (g) E. F. Aber, "Power Distributions from XYZ Synthesis Calculations to Support LOFT Low-Power Reactor Physics Measurements," LTR 111-76, April 9, 1976.
- (h) A. J. Scott, "Temperature Coefficients and Boron Worth for LOFT I at Beginning of Life," LTR 111-52, August 31, 1973.

FIGURE A.1

L237C.DAT

POWER

L2-3 CONTROL ROD DROP

06-FEB-79 14:58:57

MW

LTR 111 127
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FIGURE A.2
L2-3 POWER, FULL MODE 7 FLOW
CONTROL ROD DROP

L23SA2.DAT

POWER

L23/SAR ROD DROP HD/HM
MW

22-FEB-79 11:27:46

FIGURE A.3
FULL POWER

L23SAR.DAT

L2-3/SAR CONTROL ROD DROP

08-FEB-79 09:00:35

POWER

MW

LTR 111.127
APPENDIX B 4

FIGURE A.4
FULL POWER-FULL FLOW
LTR 111-S2 MAX FEEDBACK COEFFICIENTS

L23SA1

L23/SAR RCD DROP HD/HM

08-FEB-79 09:19:40

POWER

MW

LTR 111.127
APPENDIX B.4

TABLE A.I CONTROL ROD DROP ACCIDENT ANALYSIS RESULTS

FORM INEL-656
(Rev. 2-76)

IDAHO NATIONAL ENGINEERING LABORATORY

ITR 111.127

APPENDIX B.4

MODE 8 | L2-3 | FULL POWER | FULL POWER
LOW FLOW | FULL FLOW | FULL FLOW | FULL FLOW

	CASE I.D.		L237C	L235A2	L235A1	L235AR			
	DROPPED ROD WORTH	% $\Delta \rho$	2.1	2.1	2.3	2.3			
	INITIAL PRESSURE	PSIA	2257	2259	2259	2259			
(P _{initial})	INITIAL POWER	MW	37.4	37.5	53	53.1			
	INITIAL LOOP FLOW	Mlbm/h	1.15	3.42	3.42	3.42			
	INITIAL CORE FLOW	Mlbm/h	1.09	3.25	3.25	3.25			
	INITIAL INLET TEMP	OF	539.1	570.5	559.2	559.2			
	INITIAL THOT	OF	617	598	598	598			
	INITIAL CORE TAVE	OF	578	584.2	578.6	578.6			
	MOD	$\Delta \rho / \text{lbm/h}^3$	6.5×10^{-3}	8×10^{-3}	8×10^{-3}	9.1×10^{-3}			
	DOP	$\Delta \rho / \text{OF}$	-1.088×10^{-5}	-1.1×10^{-5}	-1.1×10^{-5}	-1.5×10^{-5}			
	PRESSURE WCT	PSIA	2000	2000	2000	2000			
	MINIMUM POWER	MW	10.1	7.9	13.3	14.2			
	TIME OF MIN.	S	2.05	9	6	2.05			
(P _{recovery})	MAX POWER RECOVERY	MW	18.3	12.6	26.7	39.2			
	TIME OF MAX RECOVERY	S	25.6	24.6	18.2	21.8			
	CORE FLOW @ MAX	Mlbm/h	1.18	3.44	3.45	3.43			
	INLET TEMP @ MAX	OF	532.0	550.4	540.1	536.8			
	THOT @ MAX	OF	571.5	560.9	558.8	562.5			
	CORE TAVE @ MAX	OF	551.8	555.7	549.5	549.7			
	PRESSURE @ MAX	PSIA	1992	1989	1985	1995			
	— @ MAX RECOVERY —								
	% OF INITIAL POWER		49	33.6	50	74			
	DECREASE IN INLET TEMP OF		7.1	20.1	19.1	22.4			
	DECREASE IN CORE TAVE OF		26.2	28.5	29.1	28.9			
	% INCREASE IN FLOW		8.3	5.8	6.1	5.5			
	ESTIMATED % INCREASE		83.4	167	78	22			
	IN MDNBR WITH 10% RADIAL FLUX TILT								
	$= (1 - 10) \frac{P_{initial}}{P_{recovery}} - 1$								
	This neglects the improvement due to increased flow and decreased temperature.

EG&G

Idaho

**LTR 111-127
APPENDIX B-4**

cc: B. S. Anderson
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INTEROFFICE CORRESPONDENCE

date January 19, 1979

to E. C. Anderson

from B. S. Anderson *B. S. Anderson*

subject RESULTS OF L2-3 ROD EJECTION CASES - BSA-1-79

- Refs: (a) B. S. Anderson and R. A. Dimenna, LOFT Core I-Rod Ejection Accident Analysis, LTR 111-65, Rev. 1, July 6, 1977.
 (b) R. A. Dimenna, Zero Power Physics Testing Safety Analysis, LTR 111-88, Rev. D, June 16, 1978
 (c) Letter, B. S. Anderson to S. A. Atkinson "Comparison of TIP Power Measurements to PDQ Predictions - BSA-4-78", (November 30, 1978)

RELAP4, COBRA-IIIC, and FRAP calculations have been completed for the rod ejection accident study in support of the LOFT L2-3 test. The analytical technique is the same as described in References (a) and (b). Minimum Doppler reactivity feedback (Table II, Reference b), hot moderator reactivity feedback (Table III, Reference b), and the 10/26 LF scram curve were used for these analyses. The axial power distribution used in RELAP4 was based on the TIP measurements taken with the control rods withdrawn 54 inches and the reactor operating at a power of 38.2 MW (Reference c). Table I lists the initial conditions used in the two cases examined.

The calculations show no problems in meeting Damage Criteria C. COBRA IIIC results for both cases indicate that DNB will occur, but the resulting heat-up is well within acceptable limits. Since DNB occurs the average fuel enthalpy at the hot spot was calculated assuming adiabatic heating for five seconds, and FRAP was used to calculate the maximum fuel centerline temperature and maximum cladding surface temperature. A peak power of 78 MW was calculated for case A and 70 MW was calculated for case B. In both cases the transient is turned around by negative Doppler and moderator feedback. The scram, which was delayed 0.25 seconds after the high power setpoint was reached, brought the power to ~7 MW two seconds into the transient. Figures 1 and 2 show the power versus time plots for case A and B. The resulting pressure transients are shown in Figures 3 and 4. Results for these two cases are summarized in Table II.

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Attachments:
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LTR 111127
APPENDIX B.4

TABLE I

INITIAL CONDITIONS FOR THE L2-3 ROD EJECTION CASES

	<u>Case A</u>	<u>Case B</u>
Power (MW)	40.6	36.4
Loop Flow (lb/hr)	1.28×10^6	1.12×10^6
Hot Leg Temperature (°F)	609	609
Pressure (psia)	2257	2257
High Power Scram (MW)	46.5	46.5

TABLE II

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RESULTS OF THE L2-3 ROD EJECTION CASES

	<u>Case A</u>	<u>Case B</u>
Peak Power (MW)	78	70
Minimum DNBR @ Time(sec)	1.00 @ 0.8	1.06 @ 0.9 (1.03 @ 0.9)*
Average Fuel Enthalpy at the Hot Spot (cal/cm)	158	153
Peak $\bar{C}$ Fuel Temperature (°F) @ Time (sec)	4320 @ 1.0	4670 @ 1.2 (4670 @ 1.2)*
Peak Cladding Surface Temperature (°F) @ Time (sec)	1800 @ 2.1	1860 @ 2.4 (1910 @ 2.3)*
Time When Scram Setpoint was Reached (sec)	0.004	0.005
Change in Pressure Due to the Power Transient (psia)	13	15

* COBRA-IIIC and FRAP were rerun assuming a hot leg temperature of 621°F.

Core Power (MW)

Figure 1 - Core Power for L2-3, 40.6 MW Case

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Figure 2 - Core Power for L2-3, 36.4 MW Case

Core Pressure (psia)

Figure 3 - Core Pressure for L2-3, 40.6 MW Case

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APPENDIX B.4

Core Pressure (psia)

Figure 4 - Core Pressure for L2-3, 36.4 MW Case

INTEROFFICE CORRESPONDENCE

date March 22, 1979

to S. A. Atkinson

from B. S. Anderson *Bruce S. Anderson*subject COMPARISON OF PARAMETERS USED IN THE L2-3 ROD EJECTION CASES
TO PARAMETERS USED FOR OTHER L2-3 ANALYSES - BSA-3-79Ref: (a) Letter B.S. Anderson to E. C. Anderson, "Results of L2-3
Rod Ejection Cases - BSA-1-79" (January 19, 1979)

Concern has been expressed that the parameters used in the L2-3 rod ejection accident cases (reference a) may not be conservative when compared to parameters used for present L2-3 analyses. This letter presents a comparison of parameters used in the L2-3 rod ejection cases against those presently being used. Table I lists the comparison of parameters. As can be seen, the hot leg temperature is conservative, while pressure and scram delay time are the same in all analyses. Although the P/A power ratio and loop flow rate for the 40.6 MW case may appear to be nonconservative the values are comparable since they were calculated in a consistent manner with present L2-3 analysis (based on power profile and allowable Δh through the core). A conservative constant value was used in the L2-3 rod ejection cases for the high power trip.

Moderator and Doppler feedbacks are input as tables in the L2-3 rod ejection cases while many of the present L2-3 analyses use a constant value within the range specified in Table I. Over the range of fuel temperatures experienced during the L2-3 rod eject transients the average reactivity change per degree Fahrenheit was calculated and this is the number which appears in Table I and, as can be seen, lies within the range used in present L2-3 studies. The moderator feedback was found to be conservative. When the density begins to drop below steady-state density, negative reactivity is inserted at the rate of $-.334 \text{ } \$/\text{lbm}/\text{ft}^3$ and when the density goes above steady-state density positive reactivity is inserted at a rate of $1.135 \text{ } \$/\text{lbm}/\text{ft}^3$.

Calculations for L2-3 rod ejection worth determined a ramped reactivity insertion for rod ejection which adds .274 \$ in .054 seconds as compared to a .5 \$ step insertion used in the L2-3 rod ejection cases reported in reference (a). The reference (a) rod ejects have a conservative reactivity insertion, but this may be offset by non-conservatism in the 10/26 LF scram curve used for the L2-3 rod ejects. A typical total reactivity curve for the 40.6 MW rod eject case is shown in Figure 1. Superimposed on this curve is a

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S. A. Atkinson
BSA-3-79
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curve generated by adding the difference in reactivity between the L2-3 rod ejection and analyses using the calculated L2-3 rod eject worth and the L2-3 worst case scram as calculated by Equation 1.

$$\Delta\$ = \{ (.5\$) - (\text{Reactivity from the 10/26 LF scram curve}) \} \\ - \{ (\text{Reactivity from rod ejection versus time ramp}) \\ - (\text{Reactivity from the new worst case scram curve}) \} \quad \text{Eq. 1}$$

This new curve is an attempt to show the difference which might be expected using present rod eject worth and scram data. The new curve assumes that the scram occurs at the same time, and that the moderator and Doppler reactivity feedbacks remain essentially the same. From these curves it can be seen that the original analysis was conservative except from .82 seconds to 1.22 seconds. DNB occurred prior to this time, and the fuel rod is in film boiling well before 0.8 s. As seen in Figure 1, the L2-3 rod eject analysis predicts more reactivity insertion and therefore higher power during this critical time period. The peak clad temperature is reached shortly after 2.0 seconds in the L2-3 rod eject cases^(a). Again, over the whole 2 second period, the L2-3 reactivity assumptions are conservative. Therefore, it can be concluded that the L2-3 rod eject analyses using the 0.5 \$ step insertion assumption and 10/26 LF scram are conservative compared to reactivity worth for the L2-3 0.274 \$ rod eject ramp with the L2-3 worst case scram.

The above comparisons and discussion show that the L2-3 rod eject analyses as reported in reference (a) are conservative, and do not need to be rerun for final L2-3 analysis conditions.

ul

Attachments:
As stated

cc: B. S. Anderson
E. C. Anderson
S. A. Atkinson
J. E. Fisher
D. G. Satterwhite
R. P. Wadkins *R. P. Wadkins*
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TAB File 3.5

TABLE I

Parameter	L2-3 Rod Ejection Cases		Present L2-3 Analyses
	40.6 MW Case	36.4 MW Case	
Hot leg temperature (°F)	621	621	613
Power trip (MW)	46.5	46.5	38.5
P/A	2.37	2.66	2.60
Loop flow rate (Mlbm/hr)	1.28	1.12	1.15
Pressure (psia)	2257	2257	2257
Rod eject reactivity (\$)	.5\$ step	.5\$ step	.054 sec Ramp to .274\$
Scram curve	10/26 LF	10/26 LF	L2-3 worst case
Scram delay time (sec)	.25	.25	.25
Doppler feedback reactivity $\left(\frac{\$}{\text{°F}}\right)$	~ -.00100	~ -.00106	~ -.00061 to -.0015
Moderator feedback when density is less than at steady-state $\left(\frac{\$}{\text{lbm/ft}^3}\right)$	-.334	-.334	-.36 to -.90
Moderator feedback when density is greater than at steady-state $\left(\frac{\$}{\text{lbm/ft}^3}\right)$	1.135	1.135	.36 to .90

Figure 1 Comparison of Reactivities

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APPENDIX B-2
MDNBR VS MLHGR PLOT
FOR L2-3 CONDITIONS

APPENDIX B-5

L2-3 MODE 7 TRANSIENT AND MISCELLANEOUS ANALYSIS RESULTS

APPENDIX B-5: ANALYSIS FOR REVISED MODE 7 SCRAM CURVE EFFECTS

This appendix discusses the effect of using the "214 HF" instead of "817" scram curve for analysis of these two LOFT plant accidents: (1) A one pump failure without flywheel assisted coastdown; trip on low flow. (2) A two pump failure without flywheel assisted coastdown; trip on loss of field current. LTR 111-104^[a] identifies these accidents as the two being the closest to their allowable damage limits (Damage Criteria B and C, respectively, as defined in Section 15 of the LOFT FSAR) for those accidents which are sensitive to changes in the scram curve. The results of this analysis and the Reference^[a] analysis are compared in Table B-5-1.

The accidents are assumed to occur during LOFT Mode 7 operation and are initiated from off nominal high power, high average coolant temperature, low loop flow, low control rod position and nominal pressure. The initial conditions are:

Reactor Power = 53.2 MW

Reactor core inlet temperature = 544°F

Reactor loop flow = 3.42 Mlbm/hr

Reactor core exit pressure = 2342 psia

Control rod position = 50" inches, $F_Q = 2.694$ (case L1027 LTR-111-81)

$h_g = 750 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$

The analysis consisted of three steps: (1) model the accident using a hybrid computer simulation of the LOFT reactor. The scram curve model is used in this step. (2) use the hybrid computer results as input to the reactor core subchannel analysis code COBRA IIIC (INFL Code Configuration Control Numbers C0031005 and C0031006; for the small and large core models respectively) to predict local fluid conditions which are used with the LOFT-1 DNB correlation

to calculate a DNBR (3) use the COBRA IIIC results as input to FRAP-T4, (INEL Code Configuration Control Number H0034IB) which uses a detailed single fuel pin model to calculate peak fuel rod thermal behavior.

The methodology and assumptions used are identical to those in Reference [a] except: (1) This analysis applied the full engineering hot spot factor, $F_Q^E = 1.026$, (instead of only the off-nominal enrichment contribution to F_Q^E ; 1.0125) to the FRAP-T4 power history table and (2) In the time between the two analyses, an error was identified and corrected in COBRA IIIC. As discussed in Reference [b] the existence of this error tended to decrease the predicted MDNBR.

As shown in Table B-5-1, only the one pump loss with rapid coastdown was significantly effected by the different scram curve. The most important change is the 39°F increase in predicted peak clad temperature to a temperature of 1669°F. Consequently, the FSAR Section 15 allowable damage criteria for this accident (infrequent incident, damage Criteria B) was not met because the peak clad temperature exceeded the 1650°F limit. A second attempt was made to show that the damage criteria could be met by using the COBRA IIIC large core model and decreasing the engineering factor, F_Q^E , from 1.032 to 1.026 [c]. However, as seen in Table B-5-1 the time to DNB only increased by .1 second and so it was not expected that FRAP-T4 results would change more than a few degrees and no FRAP-T4 calculation was performed.

Table B-5-1 shows that the consequences of the two pump loss with rapid coastdown are less severe than the Reference [a] consequences. All the COBRA III variables which are used as input to FRAP-T4 were in a direction which would decrease the peak temperature prediction. These variables are local mass flux time when DNBR goes below 1.13, and the axial spread of regions where DNBR < 1.13 on the hot fuel pin.

Transient	Results of This Analysis		Results of Reference a Analysis	
	<u>Run ID</u>	<u>Results</u>	<u>Run ID</u>	<u>Results</u>
One pump failure without flywheel coastdown - low flow trip Small core model	Flow .4F	MDNBR = 1.082 t_{DNB} = 1.13 sec $t_{\text{cl}} _{\text{max}}$ = 5036°F $t_{\text{c}} _{\text{max}}$ = 1669°F	Flow .4D	MDNBR = 1.115 t_{DNB} = 1.25 sec $t_{\text{cl}} _{\text{max}}$ = 5002°F $t_{\text{c}} _{\text{max}}$ = 1638°F
Same as above except (1) large core COBRA IIIC model (2) F_Q^E changed to 1.026 from 1.032 in COBRA IIIC. (3) 2.5% decrease in the 5% inlet flow conservatism is not used	Same	MDNBR = 1.092 t_{DNB} = 1.14 sec $t_{\text{cl}} _{\text{max}}$ = (2) $t_{\text{c}} _{\text{max}}$ = (2)	Same as above	Same as above
Two pump failure without flywheel coastdown - loss of field current trip Small core COBRA IIIC model	Flow .6Q	MDNBR = 1.008 t_{DNB} = 1.10 $t_{\text{cl}} _{\text{max}}$ = (3) $t_{\text{c}} _{\text{max}}$ = (3)	Flow .6K	MDNBR = 1.009 t_{DNB} = 1.00 $t_{\text{cl}} _{\text{max}}$ = 5002°F $t_{\text{c}} _{\text{max}}$ = 2010°F

- (1) t_{DNB} = time when DMBR (LOFT-1) = 1.13
 $t_{\text{cl}}|_{\text{max}}$ = maximum fuel pellet centerline temperature
 $t_{\text{c}}|_{\text{max}}$ = maximum fuel pin clad temperature
- (2) No FRAP-T4 run made because it was not expected that the FRAP-T4 results would change by more than 5°F.
- (3) No FRAP T4 run made because the COBRA IIIC results (as compared to flow .6K) were all in a direction which would decrease the FRAP T4 temperature predictions.

REFERENCES

- (a) S. A. Atkinson, R. A. Dimenna, T. M. Howe, Power Range Testing Core Safety Analysis, LTR 111-104, p. 29-37. (December 4, 1978).
- (b)* S. A. Atkinson letter to E. C. Anderson, COBRA IIIC- COBRA IV-I Comparison and Spacer Grid Loss Calculation Error," Atki-8-79 (March 14, 1979)
- (c)* N. F. Fausz letter to E. C. Anderson, "Statistical Evaluation of the LOFT Heat Flux Engineering Hot Spot Factor" NFF-1-79 (February 27, 1979).

*Reference (b) is in Appendix A-9.
Reference (c) is in Appendix A-7.

INTEROFFICE CORRESPONDENCE

date March 13, 1979
to S. A. Atkinson
from B. S. Anderson *BSA*
subject RESULTS OF BOL-EOL MDNBR COMPARISON STUDY - BSA-2-79

- Refs: (a) B. L. Rushton, LOFT-I Results from Three-Dimensional PDO Calculation with Thermal Feedback and Depletion to 2000, EFPH, LTR-111-60, Rev. B (November 12, 1976)
(b) E. F. Aber, LOFT-I High-Flow Power Distributions as a Function of Control Rod Position, Core Power Level, and Full Power Operating History, LTR-111-83, Rev. A (November 1976)

Calculations have been completed to determine the effect of fuel burnup on the minimum DNB ratio for high flow steady-state conditions at power levels of 33 and 53 megawatts. COBRA IIIC was used to calculate the MDNBR assuming power profiles from BOL to 2000 EFPH. Figure 1 shows the small core model used in COBRA IIIC for the calculations, and Table I describes the seven cases and the initial conditions for each. Axial and radial power profiles were taken from references (a) and (b). The axial power profiles are plotted in Figures 2 through 5. The power profiles available for CRA's withdrawn 36 inches were calculated using a core power of 49.4 MW instead of 33 MW. The higher power will affect the profiles slightly, but all three of the 33 MW cases have the same error, and for comparative purposes should not present a problem.

The results of the study are tabulated in Table II and a plot of MDNBR versus EFPH is presented in Figure 6. As can be seen, increasing EFPH results in higher MDNBR's, therefore from a MDNBR standpoint, BOL is the limiting case for steady-state operation. From Figure 6 it can also be seen that increasing EFPH has more effect on the MDNBR for the 53 MW cases than for the 33 MW cases. For the 53 MW cases the MDNBR always occurred 33 inches from the core inlet in channel 15. For the 33 MW cases however, increasing EFPH lowers the point at which the MDNBR occurs and shifts it to channel 21. This can be attributed to the fact that channel 21 has a lower inlet mass flux than channel 15, and fluid is redistributed from channel to channel as flow proceeds up the core. In the 53 MW cases, for example, the axial power profile peaks 25.5 inches from the bottom of the core and flow redistribution near the spacer grid at 35 inches up the channel reduces the flow in channel 15 such that the MDNBR occurs at 33 inches. For the 33 MW cases the axial power peak is at 17.4 inches from the bottom, significantly closer to the core inlet. Although mass flux increases in channel 21 as a result of flow redistribution, the

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S. A. Atkinson
BSA-2-79
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highly peaked power profile results in the occurrence of MDNBR before flow redistribution significantly raises the flow in channel 21 or lowers the flow in channel 15. With significant burnups MDNBR does not occur in channel 15 even though the mass flux is decreasing, because the axial power profile drops rapidly at levels above 25 inches from the bottom.

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Attachments:
As stated

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TAB File 3.4

TABLE I

LTR 111-127
APPENDIX B.5

INITIAL CONDITIONS FOR BOL-EOL MDNBR COMPARISON CASES

<u>Case Number</u>	<u>Core Power (MW)</u>	<u>Loop Flow (Mlb/hr)</u>	<u>Pressure (psia)</u>	<u>Average Core Temperature (°F)</u>	<u>Core Burnup (EFPD)</u>	<u>CRA Position (in)</u>
1	53	3.42	2262	580	0	54
2	53	3.42	2262	580	200	54
3	53	3.42	2262	580	1000	54
4	53	3.42	2262	580	2000	54
5	33	3.42	2262	566	0	36
6	33	3.42	2262	566	1000	36
7	33	3.42	2262	566	2000	36

TABLE II

RESULTS OF BOL-EOL MDNBR COMPARISON STUDY

<u>Case Number</u>	<u>MDNBR</u>	<u>Axial Location of MDNBR (in)</u>	<u>Channel where DNB Occurs</u>
1	1.43	33	15
2	1.53	33	15
3	1.59	33	15
4	1.72	33	15
5	2.06	27	15
6	2.07	18	21
7	2.09	16.5	21

Figure 1 - Diagram of COBRA IIIC Model

Figure 2 - BOL Axial Profiles

Figure 3 - 200 EFPH Axial Profile

Figure 4 - 1000 EFPH Axial Profiles

Figure 5 - 2000 EFP Axial Profiles

Figure 6 - Results of BOL-EOL MDNBR Comparison Study