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AEC Research and
Development Report
UC-81, Reactors-Power
(Special Distribution)

**STEADY STATE AND TRANSIENT
THERMAL AND HYDRAULIC
ANALYSIS OF SM-2
TERMINATION REPORT**

ALCO PRODUCTS, INC.

NUCLEAR POWER ENGINEERING DEPARTMENT
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STEADY STATE AND TRANSIENT THERMAL
AND HYDRAULIC ANALYSIS OF SM-2
TERMINATION REPORT

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ABSTRACT

Thermal characteristics of the SM-2 core were analyzed at steady state and loss of flow conditions. For steady state operation, the steady state code STDY-3 was used. For transients during a loss of flow accident, ART-02, a one-dimensional code, was used.

This analysis indicates the SM-2 core is safe from burnout under steady state operation at design power level (28 tMW) because (1) no nucleate boiling exists, and (2) the minimum burnout ratio is above 2.0. The core is safe from burnout under loss of flow transient because the minimum burnout ratio in the hottest element channel of 1.82 is above the minimum design criteria of 1.5.

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SUMMARY

Minimum steady-state burnout ratios for the SM-2 core at design flow and power (28 MW) range from 2.3 to 3.0 in the second pass and 3.4 to 5.4 in the first pass. The hottest core channel is the combination of second pass element 44 with a burnout ratio of 2.36 and first pass element position 74 with a burnout ratio of 3.47. Design flow rates are: 331 gpm for control rods, 316 gpm for stationary 2nd pass elements and 288 gpm for stationary 1st pass elements. Minimum required flow is established at 86 percent of design to induce nucleate boiling in the hot channel of the hottest element.

The minimum burnout ratio for a loss of flow transient is 1.82 and occurs in the series combination of first pass element 74 and second pass control rod 44 (Rod "C"). Since the hottest element burnout ratio is greater than 1.5 during a loss of flow transient and there is no nucleate boiling at steady state operation, the core is considered safe from burnout.

It is recommended that the following work be performed to complete the thermal and hydraulic analysis of the SM-2 core: (1) Use of a slip flow model, ART-04, and density coefficient to account for the reactivity effects due to boiling. (2) Account for the shift in axial flux distribution in the analysis of the second pass stationary elements. (3) Analyze the core at the scram power level of 35 MW. (4) Perform a rod and load perturbation analysis and the decay heat removal phase of a loss of flow accident.

1.0 STEADY STATE THERMAL ANALYSIS

1.1 INTRODUCTION

As part of the SM-2 core and vessel development contract AT(30-3)-326, a steady-state thermal analysis was conducted and results presented in APAE No. 69.(5) With the advent of newer analytical codes and more refined power distributions, it became necessary to re-analyze the SM-2 core. This termination report covers the steady-state and transient loss of flow accident analysis conducted under SM-2 Task 2 since the work reported in APAE No. 69.

The thermal design criteria utilized as a basis for analysis is that no steady-state boiling will be permitted. However, nucleate boiling is permissible during transients, provided no flow instability occurs. The minimum burnout ratio (departure from nucleate boiling ratio) will be greater than 1.5 during a transient.

Steady-state operation was analyzed using the one-dimensional steady state code STDY-3, written by WAPD for pressurized water reactors. Transients during loss of flow accident employed the one-dimensional code, ART-02, to predict the behavior of a water-cooled and moderated reactor during slow transients. Due to curtailment of SM-2 contract, transients imposed by load or rod perturbations were not investigated.

1.2 METHOD OF ANALYSIS

The steady state thermal analysis of the SM-2 core was computed using the IBM-704 code STDY-3⁽¹⁾. This code, a one-dimensional steady state code for pressurized water reactors, calculates bulk enthalpy, bulk temperature, and steam quality rises for normal channels from input parameters. Through calculation and comparison of hot and normal channel pressure drops, the code determines a hot channel flow rate. Using this hot channel flow rate, values of bulk enthalpy, bulk temperature, steam quality, plate surface temperature, meat centerline temperatures and DNB (departure from nucleate boiling) ratios are calculated for each axial position from input properties and design equations. Where local or bulk boiling exists in either channel, the code designates the axial position at which the phenomena starts.

The burnout correlations used in the code are controlled by the local enthalpy, the channel mass flow rate and system pressure. The general form of the equation selected for system pressures of 2000 psia is given as

$$\frac{\phi_{\text{DNB}}}{10^6} = 0.240 \left(\frac{H_j}{1000} \right)^{-2.5} e^{-0.0012 L/S} \left(1 + \frac{G_{\text{Loc}}}{10^7} \right)^2 \quad (1.1a)$$

where $G_{Loc} > 1.6 \times 10^6 \text{ lb/hr} - \text{ft}^2$, and

$$\frac{\phi_{DNB}}{10^6} = 0.325 F_p \left[\frac{H_j}{1000} \right]^{-2.5} e^{-0.0012 L/S} \quad (1.1b)$$

where $G_{Loc} \leq 1.6 \times 10^6 \text{ lb/hr} - \text{ft}^2$

ϕ_{DNB} = Departure from nucleate boiling heat flux

H_j = Local enthalpy at a specific axial increment

L = Channel length

S = Channel spacing

G_{Loc} = Local mass velocity in the channel

F_p = Correction factor for pressure, at 2000 psia $F_p = 1.0$

The burnout ratio or DNBR is found by dividing the heat flux found in equation (1.1) by

$$DNBR = \frac{\phi_{DNB}}{q_o^* F_{\Delta\theta} f'(z)} \quad (1.2)$$

where q_o^* = Average core heat flux

$F_{\Delta\theta}$ = Local hot channel factor

$f'(z)$ = Axial power at j th elevation

The criteria of local boiling in a channel is based upon a comparison between the average film drop (θ_f) and the film drop as calculated from the Jens-Lottes correlation ($\theta_{J \& L}$) with bulk parameters. If the average film drop exceeds or equals the film drop as calculated from the Jens-Lottes correlation, the channel is considered to be in local boiling and the proper pressure drop correlations for local boiling are utilized. This deviates from the previously

used AP Note 243 (2) definition of nucleate boiling as the condition which occurs when the local film drop equals or exceeds the local Jens-Lottes film drop.

Local boiling exists if $\theta_f \geq \theta_{J \& L}$

$$\theta_f = \frac{q_o^* F_{\Delta T} f'(Z)}{h} \quad (1.3)$$

$$\text{and } \theta_{J \& L} = T_{\text{sat}} + \frac{60 \left(\frac{q_o^* F_{\Delta T} f'(Z)}{10^6} \right)^{1/4}}{e^{P/900}} - T_j \quad (1.4)$$

$$\text{and } h = .023 \frac{K}{(De)} (N_{RE})^{.8} (N_{PR})^{.4} \quad (1.5)$$

where θ_f = Avg. film temperature drop, $^{\circ}\text{F}$

$\theta_{J \& L}$ = Avg. film temperature drop as calculated by the Jen-Lottes correlation, $^{\circ}\text{F}$

h = Avg. film coefficient, $\text{Btu/hr-ft}^2 \text{ } ^{\circ}\text{F}$

T_{sat} = Saturation temperature of the fluid, $^{\circ}\text{F}$

P = System pressure, psia

T_j = Bulk temperature of fluid at specific axial position, $^{\circ}\text{F}$

K = Fluid thermal conductivity, $\text{Btu/hr-ft } ^{\circ}\text{F}$

De = Average equivalent diameter of channel, ft

N_{RE} = Reynolds Number

N_{PR} = Prandtl Number

Since the purpose of this criteria is not to establish the plate surface temperature but to determine whether the increased pressure drop and decreased flow associated with nucleate boiling occurs, the analysis is based upon bulk or average parameters upon which pressure drop is dependent. However, when calculating plate surface temperature, the film drops are compared as in the previous situation, but local parameters are inserted in equations 1.6 to 1.8 below.

$$T_S = T_j + \theta^L$$

where $\theta^L = \theta_f^L \cdot 1_f \cdot \theta_f^L \leq \theta_{J \& L}^L$

or $\theta^L = \theta_{J \& L}^L \cdot 1_f \cdot \theta_f^L > \theta_{J \& L}^L$

$$\theta_f^L = \frac{q_o^* F_{\Delta\theta} f'(Z)}{h^*} \quad (1.6)$$

$$\theta_{J \& L}^L = T_{sat} + \frac{60 \left(\frac{q_o^* F_{\Delta\theta} f'(Z)}{10^6} \right)^{1/4}}{e^{P/900}} - T_j \quad (1.7)$$

and $h^L = .023 \frac{K}{(De^*)} (N_{RE}^L)^{.8} (N_{PR})^{.4} \quad (1.8)$

where the nomenclature is the same as previously used but the superscript^L refers to local conditions.

With these correlations as utilized by the STDY-3 code for defining when nucleate boiling occurs, it is conceivable to have a condition in a channel where some portion of the channel is essentially at a uniform surface temperature with a reasonably high superheat and yet not be in nucleate boiling. This has been observed in some of the elements investigated which are reported in section 1.10. It indicates that these elements are marginally away from the inception of nucleate boiling, although previous analysis⁽²⁾ would have considered this nucleate boiling.

FIGURE 1.1
SM-2 CORE CONFIGURATION

Figure 1.1 – SM-2 Core Configuration

1.3 POWER DISTRIBUTION

To insure the conservatism of the steady state analysis of SM-2, the core has been evaluated at the nominal power level of 28 MW and at the maximum power level of 35 MW. The latter power level is the scram power setting.

These power levels and core heat transfer areas were used to calculate the average core heat flux (q_o^*). The values used in this analysis are tabulated below:

TABLE 1.1
ANALYTICAL VALUES

Core heat transfer area (@ 9% burnup)	602.4	ft ²
q_o^* @ 28 MW	1.6499×10^5	Btu/hr-ft ²
q_o^* @ 35 MW	2.0624×10^5	Btu/hr-ft ²

1.4 RADIAL DISTRIBUTION

Radial power was calculated by the use of IBM codes and experimental correction factors (3). The $Q(\Delta T)$, $Q(\Delta T)$ and $Q(\Delta \theta)$ values used in the thermal analysis are listed in Table 1.2. These average and local peaking factors were used to determine the most adverse element positions. Because the SM-2 is designed to have uniform flow and the nuclear power factors are considered to be symmetrical about the center element, only one quadrant need be analyzed.

These peaking factors are also used to calculate channel factors for the nominal and hot channels. These factors are multiplied by the average and local channel factors to obtain average and local hot channel factors for insertion in the STDY-3 code.

1.5 AXIAL POWER DISTRIBUTION

The axial power distribution was calculated by the IBM-650 Code Valprod⁽⁴⁾, using flux weighted nuclear constants as input. The flux suppressors at the bottom of the core were included in making the calculation. A plot of relative power vs. distance from the bottom of the core is shown in Fig. 1.2 for the stationary elements and Fig. 1.3 for control rods.

Figure 1.2 - SM-2 Axial Power Distribution for Stationary Elements

Figure 1.3 - SM-2 Axial Power Distribution for Control Rods

TABLE 1.2

SM-2 RADIAL POWER PEAKING FACTORS

(T = 510⁰F, no xenon)

<u>Element Positions</u>	<u>$\overline{Q(\Delta T)}$</u>	<u>$Q(\Delta T)$</u>	<u>$Q(\Delta \theta)$</u>
44	1.37	2.02	2.22
45, 43	1.38	1.64	2.11
46, 42	1.27	1.59	1.92
47, 41	0.86	1.23	1.69
54, 34	1.47	1.89	2.08
55, 53, 35, 33	1.29	1.57	2.01
56, 52, 36, 32	1.09	1.47	1.74
57, 51, 37, 31	0.76	1.20	1.64
64, 24	1.16	1.65	1.82
65, 63, 25, 23	1.15	1.45	1.88
66, 62, 26, 22	0.90	1.38	1.70
67, 61, 27, 21	0.55	0.77	1.14
74, 14	0.83	1.42	1.70
75, 73, 15, 13	0.74	1.25	1.64
76, 72, 16, 12	0.56	0.87	1.19

For the nominal channel:

$$F_{\Delta T_N} = F_N F_{\psi} \overline{Q(\Delta T)}$$

$$F_{\Delta \theta_N} = F_N^L F_{\psi}^L Q(\Delta \theta)$$

For the hot channel:

$$F_{\Delta T_H} = F_N F_{\psi} Q(\Delta T) F_{M_{\Delta T}}$$

$$F_{\Delta \theta_H} = F_N^L F_{\psi}^L Q(\Delta \theta) F_{M_{\Delta \theta}}$$

where

$F_{\Delta T_N}, F_{\Delta T_H}$ = the average hot channel factors for the nominal and hot channels

$F_{\Delta \theta_N}, F_{\Delta \theta_H}$ = the local hot channel for the normal and hot channel

F_N	= nuclear uncertainty factor for average or local condition
F_ψ	= power generation factor for average or local condition
$\overline{Q}(\Delta T)$	= average radial peaking factor in the average channel
$Q(\Delta T)$	= average radial peaking factor in the hot channel
$Q(\Delta \theta)$	= local radial peaking factor
$F_{M_{\Delta T}}, F_{M_{\Delta \theta}}$	= mechanical hot channel factor excluding plate spacing, rippling and flow maldistribution factors. Average and local factors respectively

1.6 HOT CHANNEL FACTORS

The mechanical hot channel factors employed in this analysis are the average and local deviations of meat length, uranium content and clad thickness and are discussed below. These factors were calculated using the latest dimensions and tolerances and the equations found in APAE No. 69⁽⁵⁾ and have been included in Table 1.3.

Certain factors which are normally considered as hot channel factors, namely plate spacing, rippling and flow maldistribution, do not appear in the STDY-3 code as a lumped hot channel factor. Instead, they are applied to the individual dimensions with which they are related. Plate spacing is applied to the average dimension of the hot channel while the rippling factor is applied to the local dimension of the hot channel. Maldistribution of channel-to-channel flow is included in the code as a plenum factor.

TABLE 1.3
COMPOSITE MECHANICAL HOT CHANNEL FACTORS

	<u>Stationary Element</u>		<u>Control Rods</u>	
	$F_{M_{\Delta T}}$	$F_{M_{\Delta \theta}}$	$F_{M_{\Delta T}}$	$F_{M_{\Delta \theta}}$
Meat length	1.0233	1.0233	1.0233	1.0233
Uranium content	1.005	1.025	1.005	1.025
Clad thickness	1.007	1.012	1.007	1.012
Composite mech. factor (As applied in STDY-3 Analysis)	1.0356	1.0614	1.0356	1.0614
Nuclear uncertainty	1.05	1.10	1.05	1.10
Power generation factor	1.00	0.95	1.00	0.95

1.6.1 Uranium Content Deviation

a. Average Hot Channel Factor

Both fuel elements forming the hot channel are assumed to contain the maximum allowable uranium density per plate. If, on the average, the uranium content is higher by a certain percentage than the nominal, this will increase the water temperature, since more heat will be sent into the coolant moving between the two plates.

$$\therefore F_{\Delta T} = \frac{W_{ha}}{W_n} = \frac{W_n (1 + \delta)}{W_n} = 1 + \delta$$

where δ is the fractional deviation in the positive direction on uranium density.

For SM-2 $\delta = .005$

b. Local Hot Channel Factor

In addition to the fraction of uranium content deviation per plate, which affects the total sensible heat in the channel, there exists some possibility of having a non-homogeneous mixture which might have deviations also. Designating the deviation in the positive direction as being $+\gamma$, (the fractional deviation), and assuming that a maximum positive homogeneity can occur simultaneously with δ , the γ will then essentially designate the criteria for affecting the local heat flux at the hot spot.

Therefore, the effect of $+\gamma$ is on the film temperature rise, which is

$$F_{\Delta \theta} = 1 + \gamma$$

For SM-2, γ equals .025.

1.6.2 Active Core Length Deviation

For a given volume of meat per plate, a negative deviation in active length increases the amount of meat per unit length and therefore per unit heat transfer area. This affects both the bulk coolant temperature rise and the film gradient, based on the conservative approach that the decreased length increases only meat thickness and not active width.

$$F_{\Delta T} = \frac{L_n}{L_{min}} = F_{\Delta \theta}$$

For SM-2, L_n is 22.5 in., and L_{min} is 22.0 in.

1.6.3 Clad Thickness Deviation Factors

If the two clad thicknesses of a fuel plate are unequal, a greater portion of the total heat generated in the meat will pass out through the thinner clad because of lower thermal resistance. A hot channel, therefore, is defined as being composed of two fuel plates whose inner and outer clad thicknesses are at the minimum and maximum observed values, respectively. Both the average and local hot channel factors are then equal to the proportional increase in the heat transmitted through the inner sides. The water temperature and film coefficients are assumed equal on both sides of the fuel plate shown below.

The differential equations describing the mode of conduction heat transfer for the plate are:

$$\frac{d^2 T_1}{dx^2} = 0 \quad 0 \leq X \leq t_{ch} \quad (1.9)$$

$$\frac{d^2 T_m}{dx^2} = \frac{-Q}{k} \quad t_{ch} \leq X \leq t_{ch} + t_m \quad (1.10)$$

$$\frac{d^2 T_2}{dx^2} = 0 \quad t_{ch} + t_m \leq X \leq t_{ch} + t_m + t_{co} \quad (1.11)$$

In the meat region (i. e. , $t_{ch} \leq X \leq t_m + t_{ch}$)

at $X = t_{ch}$, $T_m = T_{c_1}$; at $X = t_{ch} + t_m$, $T_m = T_{c_2}$

Solving (1.10) and the appropriate boundary conditions, the heat flux into the hot channel is

$$q_h = \frac{\frac{Qt_m}{2} + \alpha Q_{km}}{1 + \frac{k_m}{t_m} \beta} \quad \text{where } \alpha = \frac{1}{h} + \frac{t_{co}}{k_c}$$

$$\beta = \frac{2}{h} + \frac{t_{ch} + t_{co}}{k_c}$$

But nominally the heat flow to either surface would just be $\frac{Q_{tm}}{2}$ if the clad thickness were the same

$$\therefore F = \frac{q_h}{q_n} = \frac{t_m + 2\alpha k_m}{t_m + k_m \beta}$$

if $\beta = 2$ (where $t_{co} = t_{ch}$) then $F = 1$ as expected.

The hot channel factor for this eccentricity of heat flow can therefore be defined as

$$F_{\Delta T} = \frac{q_h}{q_n} = \frac{t_m + 2\alpha k_m}{t_m + k_m \beta}$$

The local factor, $F_{\Delta\theta}$, is computed similarly except that different values of α and β corresponding to the local point conditions are used.

1.7 ADDITIONAL FACTORS INCLUDED IN THERMAL ANALYSIS

1.7.1 Nuclear Uncertainty Factor

To allow for uncertainties in the calculated power distribution, a nuclear uncertainty factor (F_N) of 1.05 was used for the nominal channel and a F_N^L of 1.10 for the hot channel.

1.7.2 Core Power Generation Factor

In order to account for the fraction of fission heat liberated in the fuel plates directly, a power generation factor (F_{ψ}) of 1.00 was used for average conditions and a power generation factor (F_{ψ}^L) of 0.95 for local conditions.

1.7.3 Instrumentation Tolerances

The worst reactor conditions were considered in the analysis and tolerances were applied to the reactor operating parameters. The following instrumentation tolerances were used.

Core power	=	$\pm 4\%$ at operating (28 MW) and scram (35 MW) power
Inlet temperature		$\pm 6^{\circ}\text{F}$ at core inlet temp of 500°F
System pressure		± 40 psia at 2000 psia

1.7.4 Fuel Plate Rippling

To account for the growth of initial ripples in the fuel plates due to compressive stress resulting from the temperature differential between the meat and the side plates, values of rippling were taken from the rippling analysis ⁽⁵⁾ of SM-2 elements and applied to the hot channel description. The values used were:

<u>Element</u>	<u>Ripple Ratio</u>	<u>Hot channel spacing</u>
Stationary	1.55	0.136
Control Rod	1.20	0.133

1.7.5 Channel-to-Channel Flow Maldistribution Factors

To account for the maldistribution in channel-to-channel caused by lower plenum effects, the following maldistribution factors were applied to the plenum factors. These values were obtained from latest tests on SM-2 full scale flow rig ⁽⁶⁾.

Stationary element (both passes)	$\pm 10.3\%$ mass flow
Control rod	$\pm 11.2\%$ mass flow

1.8 CORE FLOW

1.8.1 Design Flow

The SM-2 design flow is 7800 gpm. The core is designed to have uniform flow in each element. The nominal flow is 288 gpm in the first pass stationary elements, 316 gpm in the second pass stationary elements, and 331 gpm in the second pass control rods.

1.8.2 Element-to-element Flow Maldistribution

Variations in elemental flow were measured in the SM-2 full scale rig tests⁽⁶⁾ and found to be:

2nd pass control rods	$\pm 7.0\%$
2nd pass stationary elements	$\pm 8.5\%$
1st pass stationary elements	$\pm 15.0\%$

These values are not indicative of the final design performance as is explained in APAE No. 89⁽⁶⁾.

1.9 OPERATING PARAMETERS USED IN ANALYSIS

To insure thermal safety, the design parameters which produce the most adverse conditions were chosen. These parameters were arrived at by taking plant conditions and applying instrumentation tolerances. The worst conditions are attained at high inlet temperature, high power and low system pressure. The values used as input design parameters for the STDY-3 analysis are:

	<u>28 MW</u>		<u>35 MW</u>	
	<u>Input</u>	<u>Design</u>	<u>Input</u>	<u>Design</u>
Core power - MW	29.2	28.0	36.4	35
1st pass inlet temp - °F	506.0 ⁰	500.0 ⁰	503.8	497.8
2nd pass inlet temp - °F	517.1	511.1 ⁰	517.6	511.6
System pressure - psia	1960	2000	1960	2000

1.10 SELECTION OF ELEMENTS

The index used to determine the relative thermal efficiency of the elements was derived in APAE No. 85⁽⁷⁾. The final equation of this index is given as

$$I_{\text{DNBR}} = Q(\Delta\theta) \left[1 + \frac{CQ(\Delta T)}{G} \right]$$

For SM-2, C is 13.28.

This index is a measure of high peaking flux factors combined with low flow rates to obtain relative magnitude of hotness of the elements. Indices for the SM-2 elements have been calculated and are shown in Table 1.4.

The three elements with the three highest indices were chosen for evaluation on the basis that if the hottest element in the core is safe from burnout then all elements will be safe. The positions considered the hottest on the basis of DNBR index are position 44 (control rod), positions 43 and 45 (stationary), and positions 34 and 54 (stationary).

Since positions 43 and 34 are considered to have the same flow rate and power peaking factor as positions 45 and 54 respectively, only core positions 44, 45, and 54 were analyzed in detail.

TABLE 1.4
DNBR INDEX FOR SM-2

<u>Positions</u>	<u>Type of Element</u>	<u>Pass</u>	<u>I_{DNBR}</u>
44	Control rod	2	2.40
45, 43	Stationary	2	2.26
46, 42	"	2	2.05
47, 41	"	1	1.79
54, 34	"	2	2.26
55, 53, 35, 33	"	2	2.14
56, 52, 36, 32	Control rod	2	1.84
57, 51, 37, 31	Stationary	1	1.73
64, 24	Control rod	2	1.94
65, 63, 25, 23	Stationary	2	1.99
66, 62, 26, 22	"	1	1.81
67, 61, 27, 21	"	1	1.18
74, 14	"	1	1.74
75, 73, 15, 13	"	1	1.26
76, 72, 16, 12	"	1	1.77

1.11 RESULTS OF STEADY STATE THERMAL ANALYSIS

The elements of the fourth quadrant were analyzed at the normal power level of 28 MW with a core heat flux of 165,000 Btu/hr-ft² and an inlet temperature of 500°F. This analysis was repeated for these fifteen elements at the reactor scram power level of 35 MW. Core inlet temperature at this power level was reduced to 497.8 and the average heat flux was increased to 206,000 Btu/hr-ft².

Results of the steady state analysis at both power levels are presented in Tables 1.5 and 1.6. Plate surface temperature of the three most critical elements in the core (Positions 44, 45 and 54) is plotted in Fig. 1.4 for 28 MW and Fig. 1.5 for 35 MW.

At 28 MW, the burnout ratio ranges from 2.36 to 3.05 in the second pass and from 3.47 to 5.44 in the first pass. The maximum surface temperature in the first pass ranges from 578 to 617°F and in the second pass from 627 to 639°F. This means that 6° of superheat exists in the hot channels of the second pass only. The code indicates that local boiling does not exist in element positions 44, 45, or 54. Bulk boiling is not present in the core at 28 MW.

At 35 MW, burnout ratios in the theoretical hot channel range from 1.83 to 2.38 in the second pass and 2.74 to 4.35 in the first pass. The maximum surface temperature ranges from 594 to 639 in the first pass and 638 to 639 in the second pass. The surface temperature thus has a maximum of 6° superheat in both passes. Local boiling and bulk boiling does not occur in the core at 35 MW.

From the plot of the hot channel surface temperature vs. the length of the hot elements at 28 MW, it is seen that 2 to 3 in. of the elements are at 639°F, or 6°F of superheat. At the scram power level (35 MW), elements 45 and 54 have 8 to 9 in. at 639°F and element 44 has 5 in. This gives the appearance of nucleate boiling, but the STDY-3 code does not indicate it. A comparison on a bulk basis to the Jens and Lottes criteria as discussed in Section 1.1.1, equations (1.3) to (1.8), indicate that no nucleate boiling exists. Therefore, there is no increased pressure drop and no flow readjustment associated with nucleate boiling in these elements. However, the local film drop exceeds the criteria established in equation (1.6), and the film drop is established by equation (1.7), the Jens and Lottes local film drop. At 35 MW, this condition of several inches of plate surface with approximately 6° of superheat indicates that the critical elements in the core, though not exhibiting nucleate boiling, are only marginally away from the inception of it.

1.12 MINIMUM FLOW REQUIRED TO PREVENT NUCLEATE BOILING

An analysis was made on the effect of reducing the SM-2 flow rate in the hottest core positions to determine the minimum flow required to prevent nucleate boiling. Stationary element flow rates were studied as a means to determine the

TABLE 1.5
SUMMARY OF THE RESULTS OF THE STEADY STATE THERMAL
ANALYSIS OF SM-2

Core Power 28MW

	Core Position	Pass	G Nominal Channel Flow 10 ⁶ lb/hr-ft ²	G Hot Channel Flow 10 ⁶ lb/hr-ft ²	Hot Channel Bulk Outlet Temp. °F	Hot Channel Maximum Plate Surface Temp. °F	DNBR	Qty.	Local Boiling
22	44	2nd	3.495	3.014	538.3	638.9	2.36	0	0
	45	2nd	3.038	2.658	550.6	638.6	2.69	0	0
	46	2nd	3.038	2.658	549.7	632.9	2.96	0	0
	47	1st	2.802	2.452	533.7	613.9	3.54	0	0
	54	2nd	3.038	2.658	555.4	638.8	2.68	0	0
	55	2nd	3.038	2.658	549.1	637.3	2.83	0	0
	56	2nd	3.495	3.014	532.7	613.4	3.09	0	0
	57	1st	2.802	2.452	533.1	610.6	3.66	0	0
	64	2nd	3.495	3.014	534.6	618.6	2.93	0	0
	65	2nd	3.038	2.658	546.8	629.6	3.05	0	0
	66	1st	2.802	2.452	537.1	616.2	3.48	0	0
	67	1st	2.802	2.452	523.7	578.3	5.44	0	0
	74	1st	2.802	2.452	537.9	616.6	3.47	0	0
	75	1st	2.802	2.452	534.3	611.2	3.65	0	0
	76	1st	2.802	2.452	525.8	582.3	5.16	0	0

TABLE 1.6
SUMMARY OF THE RESULTS OF THE STEADY STATE THERMAL
ANALYSIS OF SM-2

Core Power - 35MW

Core Position	Pass	G	G	Hot Channel Bulk Outlet Temp. °F	Hot Channel	DNBR	Qty.	Local Boiling	
		Nominal Channel Flow 10 ⁵ lb/hr-ft ²	Hot Channel Flow 10 ⁵ lb/hr-ft ²		Maximum Plate Surface Temp. °F				
23	44	2nd	3.495	3.014	543.9	639.2	1.83	0	0
	45	2nd	3.038	2.658	559.2	638.9	2.09	0	0
	46	2nd	3.038	2.658	557.9	638.9	2.30	0	0
	47	1st	2.802	2.452	538.2	638.5	2.81	0	0
	54	2nd	3.038	2.658	565.0	639.1	2.07	0	0
	55	2nd	3.038	2.658	557.2	638.8	2.20	0	0
	56	2nd	3.495	3.014	536.9	637.6	2.41	0	0
	57	1st	2.802	2.452	537.5	634.4	2.91	0	0
	64	2nd	3.495	3.014	539.2	638.9	2.28	0	0
	65	2nd	3.038	2.658	554.3	638.7	2.38	0	0
	66	1st	2.802	2.452	542.4	638.7	2.75	0	0
	67	1st	2.802	2.452	525.9	594.0	4.35	0	0
	74	1st	2.802	2.452	543.4	638.7	2.74	0	0
	75	1st	2.802	2.452	539.0	635.2	2.89	0	0
	76	1st	2.802	2.452	528.4	599.1	4.11	0	0

TABLE 1.7
SUMMARY OF THE RESULTS OF THE STEADY STATE THERMAL
ANALYSIS OF SM-2

Introduction of Nucleate Boiling

Core Posi- tion	Pass	G Nominal Channel Flow 10^6 lb/hr-ft ²	Nominal Channel % of Design Flow	G Hot Channel Flow 10^6 lb/hr-ft ²	Hot Channel % of Design Flow	Hot Channel Bulk Outlet Temp. °F	Hot Channel Maximum Plate Surface Temp. °F	DNBR	Qty.	Local* Boil- ing
CORE POWER 28MW										
44	2	3.006	86.	2.593	74.	541.6	638.9	2.18	0	22
45	2	1.581	52.	1.364	45.	580.2	638.6	2.16	0	11
54	2	1.883	62.	1.601	53.	578.2	638.8	2.21	0	11
Lat- tice	1	1.160	100.	0.798	69.	602.6	638.4	2.51	0	7
Lat- tice	2	2.098	100.	1.442	69.	588.8	638.8	2.11	0	8
CORE POWER 35MW										
44	2	2.796	80.	2.377	68.	554.2	639.2	1.60	0	22
45	2	2.095	69.	1.781	59.	578.1	638.9	1.74	0	11
54	2	2.460	81.	2.091	69.	577.1	639.1	1.88	0	11

* Axial increment at which nucleate boiling begins - measured in inches from bottom of the element.

Figure 1.4 - Plate Surface Temperature of SM-2 Element Positions
44, 45 and 54 (STDY-3 Results) Core Power 28MW

Figure 1.5 – Plate Surface Temperature of SM-2 Element Positions
44, 45 and 54 (STDY-3 Results) Core Power 35 MW

flow rate at which local boiling begins in the range of 100 to 20 percent of design flow and control rod flow rate in the range of 114 to 86 percent. Figure 1.6 is a plot of these results showing element positions 44, 45, 54 (second pass), and 74 (first pass). Table 1.7 is a summary of the analysis of the hottest elements at conditions producing local boiling in the hot channel.

At 28 MW, the minimum flow attainable without the inception of local boiling in core position 44 is 3.006×10^6 lb/hr-ft² (86 percent of design) in the nominal channel and 2.593×10^6 lb/hr-ft² (74 percent of design) in the hot channel. At these conditions, the burnout ratio is 2.18. Local boiling occurs in core positions 45 and 54 at a nominal flow of 1.581×10^6 lb/hr-ft² (52.0 percent of design) and 1.883 lb/hr-ft² (62 percent of design) with burnout ratios of 2.16 and 2.21 respectively. Since the criteria in APAE 69⁽⁵⁾ prohibits steady state boiling, then these flows are limiting.

To maintain a burnout ratio of 1.5 during a transient, a steady state value of 2.0 has been arbitrarily chosen. The minimum flow rate to maintain a DNBR of 2.0 in element 44 is taken from figure 1.6 as 2.08 lb/hr-ft² or 59 percent of design flow. This in terms of nominal channel flow is 2.45 lb/hr-ft² (70 percent of design). The minimum required flow rates to maintain a DNBR of 2.0 (as taken from Fig. 1.6) for second pass element positions 54 and 45 and first pass position 74 are shown in Table 1.8.

TABLE 1.8
MINIMUM REQUIRED FLOW IN HOT CORE
POSITIONS IN SM-2 TO MAINTAIN DNBR = 2.0

<u>AT 28 MW</u>					
<u>Core Position</u>	<u>Design Flow</u>	<u>Nominal Channel Flow</u>	<u>Percent of Design</u>	<u>Hot Channel Flow</u>	<u>% of Design</u>
	lb/hr-ft ²	lb/hr-ft ²			
44	3.495×10^6	2.45×10^6	70.1	2.08×10^6	59.5
45	3.038×10^6	1.23×10^6	40.5	9.95×10^5	32.8
54	3.038×10^6	1.34×10^6	44.1	1.07×10^6	35.2
74	2.802×10^6	6.58×10^5	23.5	4.45×10^5	15.9

The lattice passages were investigated at design flow for both the first and second pass. First pass channels were assumed to have a nominal spacing of 0.132 inches. For the analysis of a hot passage, local dimensions (0.113 min - 0.164 max) are substituted for nominal spacing and a flow maldistribution of 15

percent included. Second pass nominal passage is considered to be 0.146, with min of 0.113, max of 0.192 in., and 15 percent flow maldistribution.

The STDY-3 code analysis indicates that local boiling occurs in both first and second pass hot passage for about four inches starting at a point 7 in. from the bottom of the core. The minimum burnout ratios are 2.51 for the first pass and 2.11 for the second pass. However, this analysis of the lattice is considered to be extremely conservative.

1.13 CONCLUSIONS OF STEADY STATE THERMAL ANALYSIS

Minimum burnout ratios for the SM-2 core at design flow rates and power (28 MW), range from 2.3 to 3.0 in the second pass and from 3.4 to 5.4 in the first pass. The maximum surface temperature is 617° in the first pass and 639°F in the second pass. This means that 6° of superheat exists in the hot channels of the second pass. This compares with values of 2.69 for minimum DNBR and 636°F for maximum surface temperature as were reported in APAE No. 69⁽⁵⁾ for steady state conditions at 28 MW. These values found in APAE No. 69 are compatible with this analysis and variations can be explained by the use of newer and more accurate steady state codes and a more realistic approach to axial power distribution of the control rods.

The hottest elements in the second pass are in positions 44, 45 and 43, and 54 and 34; in the first pass, they are in positions 74 and 14, 6, 62, 26 and 22, and 47 and 41. Element position 44 is the hottest second pass element with a burnout ratio of 2.36, while position 74 is the hottest first pass element with a burnout ratio of 3.47. Since the minimum burnout ratios of the combination of the two hottest core positions is greater than 2.0 at steady state, the core is considered safe from burnout at steady state design conditions.

The minimum flow rate is established by the occurrence of local boiling in the hot channel of hottest element in the core. The flow associated with local boiling in the hot channel of element 44 is a nominal flow equal to 86 percent of design. The lowest flow to prevent local boiling in the hottest channel of element 54 is a nominal flow equal to 62 percent of design.

Figure 1.6 – Hot Channel Flow vs Burnout Ratio for SM-2
Hottest Elements at 28MW

2.0 TRANSIENT ANALYSIS

The thermal analysis of the SM-2 during the critical period immediately following a loss of flow accident has been performed by means of the ART-02(8) IBM-704 Code to determine if this accident will induce a hazard to safe operation. Verification of the safety of the core has been achieved by analyzing the elements with the highest ratio of power generation to available flow (I_{DNBR} as discussed in Section 1.9). If these elements prove to be safe during a loss of flow transient, as measured by the burnout ratio criteria (DNBR), then the entire SM-2 core will be thermally safe.

2.1 METHOD OF ANALYSIS

The detailed study of the core behavior during the loss of flow transient has been performed by means of the ART-02 code. This code utilizes a one-dimensional model to predict the behavior of a water-cooled and moderated reactor with plate type elements during transients which are slower than a prompt excursion. The reactor model considered in the ART Code is either a one or two pass core operating initially in a steady-state condition. The reactor is then subjected to a variation in flow rate with or without a variation in reactivity as induced by control rod motion to simulate a loss of flow with or without scram. This information is utilized to perform reactor kinetics calculations to determine the core power as a function of time.

A single coolant channel through the core is used to represent the nominal behavior of each pass. Thermal calculations are performed for one or more additional channels in each pass. The additional channels are used as hot channels to represent possible extremes in dimensions, pressure drop and heat input. The hot channel is then used to determine reactor safety during the loss of flow transient under consideration.

In performing the hydrodynamic calculation, the version of the ART Code used in this analysis was based upon fog or homogeneous flow. Though this analysis is somewhat less rigorous than the slip flow model as presented in ART-04(9), it has been utilized in the absence of good void fraction data.

For analytical purposes, the problem of the loss of primary coolant flow is segregated into the following two phases. The first phase begins at the instant of pump failure and extends for about 5 sec, or until natural circulation becomes the predominant driving force for the coolant flow. The latter phase, or decay heat removal phase, is reported in APAE No. 69 (5), and it is not repeated at this time. Furthermore, the analysis of the loss of flow by the ART Code is limited to the first seconds because:

1. The ART code only analyzes the reactor core and takes no account of the secondary system effects which makes the representation valid up to the time it takes the fluid to complete one cycle of the primary loop.
2. The ART code as used has no provision for the reactivity changes due to steam formation and as the water quality increases the model becomes much too conservative to formulate a reasonable representation of the actual conditions in the reactor. For this reason, the saturation region pressure data has only been programmed up to 40 percent quality and all problems were discontinued when the exit quality exceeds this criteria. This condition was observed to happen from 3.5 to 7 sec after the initiation of the loss of flow in the cases analyzed in this study.
3. The most important limitation is that the burnout heat flux ϕ_{DNB} is limited to a range of enthalpies (H) ≤ 1000 Btu/lb and mass velocity (G) $\geq 0.2 \times 10^6$ lb/hr - ft². The latter flow restriction on the burnout heat flux occurs approximately 2 sec after the initiation of the loss of flow transient but the analysis has been extended to 5 sec, where the mass velocity is approximately $.1 \times 10^6$ lb/hr-ft² *.

2.2 DESCRIPTION OF THE EQUATIONS

The ART code, a program for handling accidents and other reactor transients, is in many ways very similar to its steady state counterpart STDY-3 as discussed in Section 1.1.1 of this report. The important correlations and equations used in the ART Code are presented in the following sections.

2.2.1 Energy Balance

The sketch below shows a typical axial increment (ΔZ) of a reactor channel.

An energy balance can be written as heat stored equals heat generated minus heat out, where the heat generated in the plate and water in the j^{th} axial section and at time i is $q_{ji} \Delta Z$, but a fraction (r) of this total is generated directly in the water and the heat generated in the plate is $(1-r) q_{ji} \Delta Z$.

The total heat flow out of the plate to the water is $\phi_{ji} \Delta Z$.

* This should produce only very small inaccuracies, since Westinghouse, in WAPD-188(10), shows data which indicates that the ϕ_{DNB} correlations are valid up to $.1 \times 10^6$ lb/hr-ft²

The heat capacity of the plate is $\Delta Z l_5 (\rho C)_c l_2 + (\rho C)_m l_3$,

where $(\rho C)_m$ = heat capacity of the meat per unit volume

$(\rho C)_c$ = heat capacity of the clad per unit volume

Therefore, the energy balance for the plate result in the following differential equation for the mean plate temperature (T_{mj})

$$\Delta Z l_5 \left[(\rho C)_c l_2 + (\rho C)_m l_3 \right] \frac{dT_{mj}}{dt} = (1-r) q_j \Delta Z l_5 - \phi_j \Delta Z l_5$$

or in finite difference form,

$$(T_m)_{j, i+1} = (T_m)_{ji} - \frac{\Delta t_i}{[(\rho C)_c l_2 + (\rho C)_m l_3]} \left\{ \phi_{ji} - q_{ji} (1-r) \right\} \dots (2.1)$$

where

q_{ji} = Total heat generation rate per unit area, BTU/Hr ft²

r = fraction of heat generated in water

$$\phi_{ji} = \text{heat flux, Btu/Hr-ft}^2$$

$$\text{and } q_{ji} = q_o^* f^1(Z)_j F_{M\Delta T} (P/P_o)_i \quad (2.2)$$

where q_o^* = Steady state reference heat generation per unit area =
total core power/core heat transfer area, Btu/Hr-ft²

$f^1(Z)_j$ = local to average axial power ratio at the j^{th} position

$F_{M\Delta T}$ = Average power peaking factor inclusive of average engineering
Hot channel factors nuclear uncertainty factors, and average
hot channel radial power peaking.

$(P/P_o)_i$ = Power coastdown function at the i^{th} time increment.

The correlation for the heat flux from the plate to the water at steady state is equal to the heat generated in the plate:

$$\phi_{jo} = q_{jo} (1-r)$$

During the transient, this equality is not valid, since the heat flux is a function of the average meat temperature bulk fluid temperature, etc., and it is evaluated in the ART code as the larger of the following expressions.

$$\phi_{ji} = U_i \left[(T_m)_{ji} - (T_w)_{ji} \right] \quad (2.3)$$

$$\phi_{ji} = \frac{K_c}{14} \left[(T_m)_{ji} - (T_c)_{ji} \right] \quad (2.4)$$

where U_i the over-all heat transfer coefficient

$$U = \frac{1}{\left[\frac{14}{K_c} + \frac{1}{h_i} \right]} \text{ Btu/Hr-ft}^2 \text{ } ^\circ\text{F} \quad (2.5)$$

K_c = Thermal conductivity of the clad Btu/Hr - ft⁰F

14 = Equivalent plate conduction length where the mean plate temperature exists, F

h_i = the average water film coefficient evaluate at the i^{th} time increment when the average channel mass velocity is G_i ,
Btu/Hr-ft² °F

$(T_w)_{ji}$ = bulk fluid temperature corresponding to the enthalpy H_{ji} , °F

$(T_c)_{ji}$ = local boiling surface temperature predicted from the Jens - Lottes correlation as is used in STDY-3 code.

Rewriting (1.5)

$$(T_c)_{ji} = T_{sat} + \frac{60}{e} P/600 \left[\frac{\phi_{j, i-1}}{10^6} \right] \quad (2.6)$$

where T_{sat} = fluid saturation temperature, $^{\circ}F$

P = System pressure, psia

and as long as nucleate boiling does not occur the actual surface temperature $(T_s)_{ji}$ is evaluated as

$$(T_s)_{ji} = (T_m)_{ji} - \frac{1.4 \phi_{ji}}{K_c} \quad (2.7)$$

In a similar manner, an energy balance is performed for the water contained in the j^{th} axial section to obtain the enthalpy (and temperature) of the fluid.

Heat generated directly in the water = $rq_{ji} \Delta Z$

Heat flux added to the water = $\phi_{ji} \Delta Z$

Heat convected away by the water = $G_{ji} (H_{ji} - H_{j-1, i})$

and in a constant pressure process the stored energy = $\rho_j \Delta Z \ell \frac{dH_j}{d_j}$

Writing the energy balance in finite difference terms,

$$H_{j, i+1} = H_{ji} + \frac{v_{ji} \Delta t_i}{\ell} \left[(\phi_{ji} + rq_{ji}) - \frac{G_{ji}}{\Delta Z} (H_{ji} - H_{j-1, i}) \right] \quad (2.8)$$

where v_{ji} = the specific volume, $\frac{ft^3}{lb}$

2.2.2 Pressure Drop

The pressure drop for any given channel is broken into the following terms:

1. Losses resulting from entrance and exit terms and frictional pressure drop $(\Delta P_f)_i$
2. Losses resulting from change in elevation $(\Delta P_{el})_i$

3. Pressure drop from spatial acceleration $(\Delta P_{a2})_i$

4. Transient acceleration drop resulting from mass velocity changes with time $(\Delta P_{a1})_i$

$$\text{or } \Delta P_i = (\Delta P_f)_i + (\Delta P_{e1})_i + (\Delta P_{a1})_i + (\Delta P_{a2})_i \quad (2.9)$$

$$\text{where } (\Delta P_f)_i = K_e \left(\frac{v_{oi} G_i^2}{2} \right) + K_e \left(\frac{v_{ni} G_i^2}{2} \right) + \left[\frac{(f_{oi} v_{oi})}{2} + \sum_{j=1}^{N-1} f_{ji} v_{ji} + \frac{(f_{ni} v_{ni})}{2} \right] \left[\frac{G_i^2 \Delta Z}{2D_h} \right] \quad (2.10)$$

where K_c and K_e = Unrecoverable loss coefficient for the channel entrance and exit.

v_{oi}, v_{ni} = Specific volume of the fluid at the i^{th} time increment just inside the channel inlet and exit, respectively, ft^3/lb

D_h = Channel hydraulic diameter, ft.

f_{oi}, f_{ni} = friction factor used to predict frictional resistance to fluid flow at the i^{th} time increment just inside the channel inlet and exit respectively.

As in the STDY-3 code the friction factor f_{ji} is given by

$$f_{ji} = (f_{iso})_i \left(\frac{f}{f_{iso}} \right)_{ji} \text{ for a subcooled liquid, } H_{ji} < H_f$$

$$\text{and } f_{ji} = (f_{eso})_i \left(\frac{v_f}{v_{ji}} \right) (\Phi_{Lo}^2)_{ji} \text{ for a steam watermixture, } H_{ji} \geq H_f$$

where v_f = specific volume of a saturated liquid, ft^3/lb

H_f = enthalpy of a saturated liquid, Btu/lb

$(f_{iso})_i$ = Isothermal friction factor at the i^{th} time increment

$(\Phi_{Lo}^2)_{ji}$ = Function to fit saturation region pressure drop

$(f/f_{iso})_{ji}$ = Function to fit subcooled nonisothermal, the pressure drop which is evaluated at the system pressure of 2000 as

$$f/f_{iso} = F_H \left[1 + .93 \left(\frac{1 - \left[\frac{\theta_{ji}^*}{\theta_{ji}} \right]}{(G_i/10^6)^{2/3}} \right) \right] \quad (2.11)$$

where F_H = Heat contribution to subcooled pressure drop which equals the smaller of the two

$$F_H = \begin{cases} 1.0 \text{ or} \\ 1.05 (1 - .0025 \theta_{ji}^*) \end{cases}$$

and θ_{ji}^* = Actual film drop as used in pressure drop calculations which is defined as the smaller,

$$\theta_{ji}^* = \begin{cases} \theta_{ji} = \phi_{ji}/(h_f)_i & \text{or} \\ (T_c)_{ji} - (T_w)_{ji} \end{cases}$$

where θ_{ji} = Film drop used in pressure drop calculations based on the film coefficient $(h_f)_i$, Btu/Hr-ft²

$$(h_f)_i = 1.58 h_i$$

The development of the 1.58 coefficient is given in WAPD-TH-300⁽¹¹⁾

The pressure drop resulting from change in elevation is

$$(P_{e1})_i = g \Delta Z \left[(1/2 (1/v_{oi}) + \sum_{j=1}^{n-1} 1/v_{ji} + 1/2 (1/v_{ni})) \right] \dots \quad (2.12)$$

where g is the component of the acceleration of gravity acting in the axial direction, = 32.174 ft/sec²

Pressure drop resulting from mass velocity changes with time

$$(\Delta P_{a1})_i = h \Delta Z (G_{i+1} - G_i) / \Delta t_i \quad (2.13)$$

and the final pressure drop term for spatial acceleration

$$(\Delta P_{a2})_i = - (1 + \sigma_o^2) \left(\frac{v_{oi} G_i^2}{2} \right) + (1 + \sigma_n^2) \left(\frac{v_{ri} G_i^2}{2} \right) - - - \quad (2.14)$$

where σ_o and σ_n = area ratio at entrance and exit respectively

$$\sigma^2 = \left(\frac{\text{area inside channel}}{\text{area in plenum region}} \right)^2$$

2.2.3 Hot Channel Flow

For each hot channel in parallel with the nominal channel, it is assumed in ART that the total hot-channel pressure drop $(\Delta P)_i^{HC}$ is related to the nominal channel pressure drop $(\Delta P)_i^{NC}$ by

$$(\Delta P)_i^{HC} = K_{pf}^{HC} (\Delta P_f)_i^{NC} + K_{pa}^{HC} (\Delta P_{a1})_i^{NC} + (\Delta P_{a2})_i^{NC} + (\Delta P_{e1})_i^{NC} \quad (2.15)$$

where K_{pf}^{HC} and K_{pa}^{HC} are the plenum maldistribution factors for pressure loss and acceleration terms respectively. They are evaluated:

$$K_{pf}^{HC} = (1 - \delta)^{1.8}$$

$$K_{pa}^{HC} = (1 - \delta)^2$$

where δ is the percent hot channel flow maldistribution.

The mass velocity in the hot channel is established by:

$$G_{i+1}^{HC} = G_i^{HC} + \frac{\Delta t_i}{n \Delta Z} (\Delta P_{a1})_i^{HC} \quad (2.16)$$

2.2.4 Departure from Nucleate Boiling Correlations

The departure from nucleate boiling correlations used in the ART Code are identical to those used in STDY-3 i.e. Eq. (1.1). However, during the loss of flow transients, there is considerable variation in mass velocity (G_i^L). Due

to the limitations on the range of the mass velocities, the following correlations with their indicated ranges are used.

$$\frac{G_i^L \leq 1.6 \times 10^6 \text{ lb/hr Ft}^2}{10^6} \left(\frac{\phi_{\text{DNB}}}{10^6} \right)_{ji} = .325 \left[H_{ji}/10^3 \right]^{2.5} (F_c)_2 \quad (2.17)$$

$$\frac{G_i^L > 1.6 \times 10^6 \text{ lb/Hr Ft}^2}{10^6} \left(\frac{\phi_{\text{DNB}}}{10^6} \right)_{ji} = .240 \left[1 + \frac{G_i^L}{10^7} \left[\frac{H_{ji}}{10^3} \right] \right]^{2.5} (F_c)_2 \quad (2.18)$$

where ϕ_{DNB} = Burnout heat flux, Btu/Hr, Ft²

G_i^L = Local mass velocity, lb/hr Ft²

$$G_i^L = \left(\frac{G}{G} \right) G_i$$

where $\frac{G^L}{G}$ = local mass velocity correction factor

$$\text{evaluated by} \left[\frac{(D_h \text{ avg. min})}{D_h \text{ local max}} \right]^2$$

The DNB ratio is calculated by

$$\text{DNBR} = \frac{\phi_{\text{DNB}}}{\phi_{J_i}^L} \quad (2.19)$$

where $\phi_{J_i}^L$ is the local heat flux evaluated as

$$\phi_{J_i}^L = \left(\frac{\phi^L}{\phi} \right) \phi_{ji}, \quad \text{Btu/Hr Ft}^2$$

where ϕ^L/ϕ = local heat flux correction factor evaluated as

$$\phi^L/\phi = \frac{F_{M\Delta\theta}}{F_{M\Delta T}} \quad (2.20)$$

$F_{M\Delta\theta}$ = Local peak power peaking factor inclusive of engineering hot channel factors, nuclear uncertainty factor and local hot plate radial power peaking factor.

2.2.5 Reactor Kinetics

The ART code assumes the heat generation at any point within the core is proportional to a single power coastdown function P/P_0 . That is, the heat generation rate (q_{ji}) is given by:

$$q_{ji} = f^1(Z)_j F_{\Delta T} q_o^* \left(\frac{P}{P_0}\right)_i \quad (2.21)$$

$$\text{where } \left(\frac{P}{P_0}\right)_i = \alpha_o \left[\frac{\alpha}{\alpha_o} \right] + (1 - \alpha_o) N_i \quad (2.22)$$

α_o = Steady state fraction of power produced by decay heating.

α/α_o = Decay power coastdown function

N_i = Neutron power coastdown function

Therefore, it is seen that the power coastdown function is divided into a decay heat contribution and a neutron power contribution. The neutron power coastdown, N_i , is itself divided into two parts, the prompt neutron contribution and the delayed neutron contribution. The standard reactor kinetics equation for neutron level as a function of time is:

$$\frac{dN}{dt} = \left[\frac{\delta K - \bar{\beta}}{\ell^*} \right] N + \sum_{d=1}^{d=L} \frac{\bar{\beta}_d \chi_d}{\lambda^*} \quad (2.23)$$

where ℓ^* = prompt neutron lifetime

$\bar{\beta}$ = effective delay neutron fraction which is the sum of the fractions of each delayed neutron group

χ_d = Normalized concentration of precursors

$$\text{and } \frac{d\chi_d}{dt} = \lambda_d (N - \chi_d) \quad (2.24)$$

$$\bar{\beta} = \sum_{d=1}^{de} \bar{\beta}_d$$

where λ_d = decay constant

The solution to these equations is:

$$N_i^{K+1} = N_i^K \frac{\left[1 + \frac{(\Delta t_r)_i}{2\ell^*} \left[\delta K_i^K - \bar{\beta} \right] + \frac{(\Delta t_r)_i}{2\ell^*} \sum_{d=1}^{de} \bar{\beta}_d \left[(\chi_d)_i^{K+1} + (\chi_d)_i^K \right] \right]}{\left[1 - \frac{(\Delta t_r)_i}{2\ell^*} \left(\delta K_i^K - \bar{\beta} \right) \right]} \quad (2.25)$$

$$\delta K_i^K = (\delta K_r)_i^K + (\delta K_t)_i^K$$

where (Δt_r) = Time increment

$(\delta K_r)_i$ = Excess reactivity resulting from rod motion

$(\delta K_t)_i$ = Excess reactivity resulting from temperature change

The neutron power coastdown as given in (2.25) results from two negative reactivity effects. The immediate effect is the negative temperature coefficient of reactivity δK_t when the nominal channel temperature rises. The second effect, δK_r , which occurs at a preset (delay) time, t_3 , is caused by a scram and its associated control rod insertion.

The temperature dependent portion of the reactivity is given by

$$(\delta K_t)_i = \frac{\partial (\delta K)}{\partial T_w} \left\{ \sum_{j=1}^n \alpha_j \left[(T_w)_{ji} - (T_w)_{jo} \right] \right\} \quad \begin{matrix} \text{NC} \\ \text{Pass 1} \end{matrix}$$

$$+ \sum_{j=1}^n \alpha_j \left[(T_w)_{ji} - (T_w)_{jo} \right]_{\text{pass 2}}^{\text{NC}} \quad (2.26)$$

where $\frac{\partial (\delta K)}{\partial T_w}$ is the temperature coefficient of reactivity, and the

a_j 's are the temperature weighting factors, chosen so that

$$\sum_{j=1}^n (a_j)_{\text{pass 1}} + \sum_{j=1}^n (a_j)_{\text{pass 2}} = 1$$

2.2.6 Second Pass Inlet Conditions

The first pass exit enthalpy which is applied as inlet conditions to the second pass hot and nominal channels after some delay time is given by:

$$(H_{\text{ex}})_i = (H_{\text{oi}})^{\text{NC}} + F_a (1 + B_m) \left[(H_{\text{ni}})^{\text{NC}} - (H_{\text{oi}})^{\text{NC}} \right] \quad (2.27)$$

where

H_{ex} = first pass exit enthalpy, Btu/lb

H_{oi} = first pass enthalpy just inside channel inlet, Btu/lb

H_{ni} = first pass enthalpy just inside channel exit, Btu/lb

B_m = factor to account for imperfect mixing in the exit water as is applied to the second pass hot and nominal channel individually

F_a = factor to account for 1st pass leakage flow and is evaluated as
 $(1 - \% \text{ leakage first Pass}) =$

$F_a = \frac{\text{Flow heated in first Pass}}{\text{Total flow-lattice flow}} \quad (2.28)$

2.3 POWER DISTRIBUTION

As in the steady state analysis of the SM-2 (Section No. 1.2), the loss of flow analysis has been performed at the design power level of 28 MW. However, to insure the conservatism of the analysis, a 4 percent instrumentation tolerance, (as in section No. 1.6.3) has been applied so that all cases have been evaluated at 29.12 MW. Therefore, the reference steady state heat generation per unit area, q_o (Eq. 2.2), is 1.6499×10^5 Btu/hr-ft².

The axial power distribution used in the transient analysis is given in Fig. No. 1.2 for a stationary element and fig. No. 1.3 for a control rod. Axially, the value of the maximum to average power is 1.592, approximately 8 in. from the bottom of the core. Average radial power peaking factors for the nominal channel ($Q_{\Delta T}$) and hot channel ($Q_{\Delta T}$) along with local radial power peaking factors in the hot channel ($Q_{\Delta\theta}$) are tabulated in Table No. 1.2. The highest local radial power peaking factor ($Q_{\Delta\theta}$) in the core is 2.22 in the center control rod, in core position 44. This produces an over-all core maximum power peak of $2.22 \times 1.592 = 3.53$ at core midlife, which is 21 percent higher than the peak of 2.91 as is reported in APAE No. 69.⁽¹⁵⁾ The latest results are believed the most accurate. However, the figure of 3.53 for the center control rod is conservative, since there has been no attempt to account for the internal flux suppressors at the top of the control rod which would decrease the axial profile at the most critical position in relation to the core's minimum DNBR. Furthermore, the axial peaks as shown in Fig. 1.2 and No. 1.3 have been displaced toward the top of the core somewhat more than they would be in reality by the computational techniques used to evaluate them. The effect of this condition is the decrease in DNBR by the channel length factor (F_C) as is seen in (2.17) and (2.18). Therefore, these two nuclear effects tend to make the DNBR's evaluated in the ensuing sections of this report conservative.

2.4 FLOW DISTRIBUTION

As in the steady state analysis, the flow distribution (Sections 1.65 and 1.7) utilized in evaluating the loss of flow accident is the very latest information, as obtained from the full scale flow rig (6). Therefore, the stationary elements with a 10.3 percent (by mass) channel to channel maldistribution and the control rods with 11.2 percent result in the following maldistribution factors (2.15) for friction (K_{pf}) and acceleration (K_{pa}) losses in the hot channels.

	$K_{pf} = \frac{H_c}{(1.8)^{1.8}}$	$K_{pa} = \frac{H_c}{(1.8)^2}$
Stationary elements (both passes)	.822	.804
Control rods	.807	.789

The nominal (element) flow rates used in the analysis are the design flow rates which have been established as satisfactory by the present steady state analysis (Section 1.11). However, to insure the conservatism of the analysis, the effects of the element to element flow maldistribution as obtained from the initial full scale rig tests have also been evaluated. This is performed even though these element to element maldistributions are believed to be quite conservative, and a consequence of the curtailment of task 6. Therefore, the element flow rates used in the ART analysis performed are:

	<u>Nominal Cases</u>	<u>Full Scale Test Maldistribution</u>	<u>Conservative Cases</u>
1st pass stationary elements	288 gpm	-15.0%	244.8 gpm
2nd pass stationary elements	316 gpm	- 8.5%	289.1 gpm
Control rods	331 gpm	- 7.0%	307.8 gpm

Second pass inlet conditions as described by (2.27) are dependent upon the first pass leakage and the degree of mixing of the fluid prior to entrance to the second pass. Since there is considerable time delay between the two passes (.85 sec) and the flow pattern is well diffused, the SM-2 is considered completely mixed. However, to account for the possibility of a small amount of incomplete mixing, the mixing factor (Bm) has been taken as .05 in the second pass hot channel and zero in the second pass nominal channel.

The factor for the fraction of second pass inlet flow that has been heated in the first pass (F_a) is dependent on the flow conditions in the first pass. For the nominal and conservative cases considered it is:

$$F_a = \frac{\text{Flow heated in first pass}}{\text{Total reactor flow - lattice flow first pass}} = (1 - \text{percent leakage 1st pass}) \quad (2.28)$$

where the lattice flow is taken as 526 gpm and the total flow is 7800 gpm

a. Nominal Cases

24 first pass stationary elements at 288 gpm = 6912 gpm

$$F_{a \text{ Nom}} = \frac{6912}{7800 - 526} = \frac{6912}{7274} = .95$$

b. Conservative Cases

24 first pass stationary elements at 244.8 gpm = 5875 gpm

$$F_a = \frac{5875}{7274} = .81$$

2.5 HOT CHANNEL FACTORS

The hot channel factors used in this analysis were formulated for average conditions which affect heat generation rates and for local conditions which affect local heat fluxes in the DNB correlations. Numerically, the value of the individual factors used in the transient analysis are equal to those used in the steady state analysis (Section 1.5). However, the input format for the ART code is somewhat different than STDY-3 and the manner for formulating these factors is presented in this section.

The heat generation rate at any particular axial section of the core is expressed in (2.2) as:

$$q_{ji} = q_o^* f'(Z) F_{M\Delta T} (P/P_o) i$$

where $F_{M\Delta T}$, the average power peaking factor, is composed of average engineering hot channel factors ($F_{\Delta T}$), and a radial power peak either in the hot channel $Q_{\Delta T}$ or in the nominal channel $Q_{\bar{\Delta T}}$. The average engineering hot channel factor in turn is composed of average factor for core length deviations ($F_{\text{core length}}$), for clad deviation (F_{clad}), and for average deviations in uranium content and homogeneity (F_{Hom}). Therefore, the average power peaking factor $F_{M\Delta T}$ is

$$F_{M\Delta T} = Q_{\Delta T} F_N F_{\text{core length}} F_{\text{clad}} F_{\text{Hom}} \text{ for the hot channel} \quad (2.29)$$

$$F_{M\bar{\Delta T}} = Q_{\bar{\Delta T}} F_N F_{\text{core length}} F_{\text{clad}} F_{\text{Hom}} \text{ for the nominal channel.}$$

However, the engineering factors in the nominal channel are considered unity and

$$F_{M\bar{\Delta T}} = Q_{\bar{\Delta T}} F_N \quad (2.30)$$

The factor which affects the local heat flux ($\frac{\phi^L}{\phi}$) in the DNBR correlations (2.19) is a multiple of the average radial power peaking factor steady state:

$$\phi_{jo} = q_{jo} (1-r) \text{ and } \phi_{jo}^L = \left(\frac{\phi^L}{\phi} \right) \phi_{jo}$$

$$\text{or } \phi_{jo} = \left(\frac{\phi^L}{\phi} \right) q_{jo} (1-r) \quad (2.31)$$

By means of the previously determined nomenclature (Section 1.5):

$$\frac{\phi^L}{\phi} = \frac{F_{M\Delta\theta}}{F_{M\Delta T}}$$

where $F_{M\Delta\theta}$, the local power peaking factor, is composed of local engineering factor, local nuclear uncertainty factor and the (local) hot plate radial power peaking factor.

$$F_{M\Delta\theta} = Q_{\Delta\theta} F_N^L F_{\text{core length}}^L F_{\text{clad}}^L F_{\text{Hom}}^L \quad (2.32)$$

and the local heat flux correction factor ($\frac{\phi^L}{\phi}$) is

$$\frac{\phi^L}{\phi} = \frac{Q_{\Delta\theta} F_N^L F_{\text{core length}}^L F_{\text{clad}}^L F_{\text{Hom}}^L}{Q_{\Delta T} F_N F_{\text{core length}} F_{\text{clad}} F_{\text{Hom}}} \quad (2.32)$$

Using the numerical values of hot channel factors given in Table No. 1.3 and (2.29), (2.30) and (2.32), the value of power peaking factors for SM-2 control rods and stationary elements are:

Stationary Elements

Nominal channel	$F_{M\Delta T} = 1.05 Q_{\Delta T}, \quad \phi^L/\phi = 1.112$	$\frac{Q_{\Delta\theta}}{Q_{\Delta T}}$
Hot channel	$F_{M\Delta T} = 1.087 Q_{\Delta T}, \quad \phi^L/\phi = 1.074$	$\frac{Q_{\Delta\theta}}{Q_{\Delta T}}$

Control Rods

$$\text{Nominal channel} \quad F_M \frac{\overline{\Delta T}}{\Delta T} = 1.05 \frac{Q}{\Delta T}, \quad \phi^L/\phi = 1.112 \frac{Q \Delta \theta}{Q \Delta T}$$

$$\text{Hot channel} \quad F_M \Delta T = 1.087 \frac{Q}{\Delta T}, \quad \phi^L/\phi = 1.074 \frac{Q \Delta \theta}{Q \Delta T}$$

Unlike previous thermal analyses (5), there is no hot channel factor formulated for plate spacing deviations as induced by tolerances or rippling. This factor is omitted because the ART Code will accept a local mass velocity correction factor (G^L/G) as direct input to evaluate variations in the local mass velocity (G_i^L). Therefore, the variations in plate spacing which causes variations in local mass velocities are fully covered by the local correction factor (G^L/G) or

$$G_i^L = \left(\frac{G^L}{G}\right) G_i \quad (2.33)$$

The local mass velocity correction factor is related to the plate spacing (inclusive of rippling) in the following manner:

$$G = \frac{W}{A} \quad W = GA$$

where W = flow rate lb/hr, which is constant for a channel

$$A = \text{Area, ft}^2$$

$$\text{But } A \propto D_h^2$$

$$D_h = \text{hydraulic diameter}$$

and in thin channels the hydraulic diameter can be very well approximated by S , the channel spacing:

$$G_1 S_1^2 = G_2 S_2^2$$

$$\text{or } \frac{G_2}{G_1} = \frac{S_1^2}{S_2^2}$$

The SM-2 with channel spacings, including rippling as shown below and repeated from Section 1.6.4, has the following local mass velocity correction factor (G^L/G):

<u>Stationary Elements</u>	<u>Nominal Min</u>	<u>Local Max</u>	<u>(G^L/G)</u>
Nominal Channel	.123	.127	.938
Hot Channel	.119	.136	.766
<u>Control Rods</u>			
Nominal Channel	.123	.127	.938
Hot Channel	.119	.133	.801

2.6 OPERATING PARAMETERS USED IN THE LOSS OF FLOW ANALYSIS

As in the steady state investigation (Section 1.8), the conservatism of the analysis has been insured by the use of the most adverse operating conditions. The values of operating parameters for all the cases studied by means of the ART Code were:

Core power	-	29.12 MW
First pass inlet temperature	-	506.0°F
System pressure	-	1960 psia

2.7 FLOW COASTDOWN

The flow coastdown of the reactor, which is the driving force for the transient portion of the code, has been represented by decrease in flow in the nominal channel of:

$$\frac{G}{G_0} = \left(\frac{1}{1 + bt} \right)^{1.25} \quad (2.34)$$

where t = time, sec

For conservatism, the analysis is based on the most adverse pump coastdown, that of a stuck or frozen impeller. The value of b for this case has been developed in APAE No. (12) 74 as 5.95. However, some later work in reference to a report by W. J. Love⁽¹³⁾ indicates that the value of b should be approximately 4.00. In light of this and the fact that the pressure drop in the SM-2 pump

with a frozen impeller is not accurately known, cases with both coastdowns have been performed. It would appear that the choice of the coefficient b would be very important, but the results of the runs with the two different coastdown rate indicate only very minor variations in DNBR. A plot of flow as a function of time for the two pump coastdowns considered is given in Figure 2.1.

2.8 ALLOWABLE DEPARTURE FROM NUCLEATE BOILING RATIO (DNBR) IN A LOSS OF FLOW TRANSIENT.

A previous SM-2 thermal analysis⁽⁵⁾ has reported an allowable minimum DNBR of 2.0 in a transient. This large safety factor was predicated on the lack of internal flux suppressors on the stationary elements and the resultant high degree of flux peaking at the bottom of the core. However, the present SM-2 design does have flux suppressors at both the stationary element inlets and control rod element outlets and all thermal analysis is based upon the best evaluation of the core flux distribution. Furthermore, WAPD⁽¹⁰⁾ reports that the uncertainty associated with the latest burnout correlations (as used in the STDY-3 and ART-02 Codes) is only 1.2. This is considerably less than the minimum allowable DNBR of 1.5 due to the uncertainty associated with older burnout correlations.

In light of these facts, the minimum allowable DNBR in a reactor transient will be conservatively set at 1.5. To achieve this condition, the steady state minimum allowable DNBR, which is higher than the transient, will arbitrarily be set at a design value of 2.0. Therefore, if the most critical elements (highest I_{DNBR} index) in the SM-2 do not have a minimum DNBR of less than 1.5 in a loss of flow transient, the entire reactor will be deemed safe for the duration of the thermal transient.

2.9 SELECTION OF ELEMENTS

Verification of the safety of the SM-2 has been achieved by analyzing the elements with the highest ratio of power generation to available flow (I_{DNBR}). If these elements prove to be safe during the loss of flow transient, then the entire SM-2 will be safe thermally.

By means of the I_{DNBR} index (Section 1.9), the three most vulnerable elements in the core are:

<u>Core Position</u>	<u>Type of Element</u>	<u>I_{DNBR}</u>
44	Control rod	2.40
54, 34	Stationary	2.26
45, 43	Stationary	2.26

Thermal evaluation of all three elements has been performed, but for the sake of brevity, the cases run on element 45 (which has a slightly higher minimum DNBR than 54) have been omitted. Therefore, a complete thermal history of the hottest control rod and stationary element, during the critical period immediately following a loss of flow accident, is given in the following sections of this report.

2.10 VARIOUS CASES ANALYZED

Both the control rod in core position 44 and the stationary element in core position 54 have been analyzed under the flow coastdowns of b equaling 5.95 and b equaling 4.00. This has been performed to indicate the extremes of expected DNBR's and the effect of variation in flow coastdown on thermal conditions.

To evaluate the effectiveness of the reactor scram mechanism, the hottest element in the core, the control rod in core position 44, has been scrammed when at the conservative flow coastdown of b equal to 5.95. The time delay of .055 sec has been used to represent the time delay prior to activating the 90 percent flow scram setting and the delay in the release mechanism itself. All other cases investigated assume no scram.

The effect of the element to element maldistribution as obtained from the full scale rig test has also been incorporated to determine the significance of this flow distribution. Thus, cases have been analyzed with nominal flow rates and full scale rig test flow distributions.

For the sake of conservatism, the hot second pass element is always considered in series with the hottest (as determined in the steady state analysis) first pass stationary element, the element in core position 74. This is slightly unrealistic, but it does indicate the most conservative results and an investigation of the hot second pass elements in series with a hypothetical (nominal) first pass element results in very small increases in minimum DNBR. For brevity, these results are omitted from this report.

The table below is a summary of the cases run that are presented in this report.

Case No.	Core Positions	Flow Coastdown $G/G_o = (1/(1+bt))^{1.25}$ $b =$	Scram Time, Sec.	First Pass Nominal Flow, GPM	Second Pass Nominal Flow, GPM
I	74-44	5.95	α	244.8	307.8
II	74-44	5.95	α	288.0	331.0
III	74-44	5.95	.055	244.8	307.8
IV	74-44	4.00	α	244.8	307.8
V	74-54	5.95	α	244.8	289.1
VI	74-54	4.00	α	244.8	289.1

Figure 2.1 - Flow Coastdown Investigated in Loss of Flow Analysis

The results of these cases is presented graphically in Section 2.11 and summarized in Table 2.1.

2.11 RESULTS OF LOSS OF FLOW ANALYSIS

2.11.1 Case I

This case is the most conservative or critical case analyzed. It has been formulated to establish the thermal safety (minimum DNBR) of the core in the critical early periods immediately following a loss of flow accident. The elements analyzed were a series combination of the hottest element in the first pass which is the element in core position 74 and the central control rod (Control Rod "C") in core position 44.

This case has been performed with the most severe flow coastdown ($b = 5.95$) resulting from a locked rotor. For conservatism, the element to element maldistributions as obtained from the initial full scale rig tests has also been incorporated into this analysis. Therefore, the conditions under which this analysis was performed are:

Element in core position 74 flow rate = 244.8 gpm

Control rod "C" flow rate = 307.8 gpm

The results of this analysis are presented graphically in Figs. 2.2, 2.3, 2.4, and 2.5.

2.11.2 Case II

This case is identical to Case I, except that the flow rates in control rod "C" and the stationary element in core position 74 are the expected flow rates of:

Element in core position 74 flow rate = 288.0 gpm

Control rod "C" flow rate = 331.0 gpm

The purpose of this case is to more realistically establish the core thermal behavior under expected nominal flow rates that would exist if Task 6 had not been curtailed and to evaluate the effect of the element to element maldistribution.

Since the power coastdown function will be identical to that in Case I (Fig. 2.4), it has not been repeated in this section.

The results of this case are presented as a plot of minimum DNBR ratio of the hot channel of Control Rod "C" (Fig. 2.6).

TABLE 2.1. SUMMARY OF RESULTS OF LOSS OF FLOW ANALYSIS

<u>Case No.</u>	<u>Core Loca- tions</u>	<u>First Pass Nominal Flow, gpm</u>	<u>Second Pass Nominal Flow, gpm</u>	<u>Coast- down Function b =</u>	<u>Scram, Sec.</u>	<u>Minimum DNBR in 2nd Pass Hot Channel During Transient</u>	<u>Hot Spot Quality, %</u>	<u>Time, Sec.</u>	<u>Initia- tion of Bulk Boiling Sec.</u>
I	74-44	244.8	307.8	5.95	α	1.82	18.8	5.0	.99
II	74-44	288.0	331.0	5.95	α	1.83	16.6	5.0	1.00
III	74-44	244.8	307.8	5.95	.055	1.86	None	5.0	—
IV	74-44	244.8	307.8	4.00	α	1.84	8.2	5.0	1.50
V	54-44	244.8	289.1	5.95	α	2.19	40.0	3.4	.73
VI	54-44	244.8	289.1	4.00	α	2.19	38.9	5.0	.99

Figure 2.2 – Minimum Burnout Ratio (DNBR) of the Hot Channel of Control Rod "C" when in series with the Stationary Element in Core Position 74 Under the Condition of Case I

Figure 2.3 – Power Coastdown and Power to Flow Ratio when the Flow Coastdown is $G/G_0 = (1/1+bt)^{1.25}$

Figure 2.4 – Hot Spot Bulk Water Temperature and Quality of Control Rod "C" when in Series with Stationary Element in Core Position 74 under Conditions of Case I

Figure 2.5 - Minimum Burnout Ratios (DNBR) of the Various Channels When Control Rod "C" is in Series with the Stationary Element in Core Position 74 under Conditions of Case I

Figure 2.6 - Minimum Burnout Ratio (DNBR) of the Hot Channel of Control Rod "C" when in Series with the Stationary Element in Core Position 74 Under the Conditions of Case II

2.11.3 Case III

The flow conditions in Case III are identical to those of Case I in that the nominal flow through the first pass stationary element is 244.8 gpm and the nominal flow in the control rod is 307.8 gpm. However, to evaluate the effectiveness of the reactor scram mechanism, the rods have been inserted after a .055 sec delay in the mechanism. The results are presented graphically in Figs. 2.7, 2.8 and 2.9.

2.11.4 Case IV

This case has been evaluated at the conservative flow rates due to element to element maldistribution of:

Element in core position 74 flow rate = 244.8 gpm

Control rod "C" flow rate 307.8 gpm

However, the flow coastdown has been modified in that the coastdown function, b , is 4.0, a considerably slower flow coastdown.

The purpose for investigating this case is to determine how great is the effect of flow coastdown and to establish the upper limit on expected core minimum DNBR.

The results of this case are presented graphically in Figs. 2.10, 2.11, 2.12, 2.13.

2.11.5 Case V.

This case has been performed to obtain the thermal history of the hottest second pass stationary element (element in core position 54) when in series with the hottest first pass stationary element (element in core position 74). Flow conditions used in this analysis are the conservative conditions resulting from the full scale rig test:

Element in core position 74 flow rate = 244.8 gpm

Control rod "C" flow rate = 289.1 gpm

In order to establish the lower limit of expected DNBR, the case has been analyzed with a flow coastdown coefficient of b equal to 5.95.

The power function coastdown and the power to flow ratio have been repeated in this section because the stationary second pass element will have a slightly different power coastdown than a control rod.

Figure 2.7 - Minimum Burnout Ratio (DNBR) of the Hot Channel of Control Rod "C" when in Series with the Stationary Element in Core Position 74 Under the Conditions of Case III

Figure 2.8 – Power Coastdown and Power to Flow Ratio when the Flow Coastdown is $G/G_0 = (1/1+bt)^{1.25}$ and the Reactor is Scrammed

Figure 2.9 – Hot Spot Bulk Water Temperature of Control Rod "C" when in Series with the Stationary Element in Core Position 74 Under Conditions of Case III

Figure 2.10 – Minimum Burnout Ratio (DNBR) of the Hot Channel of Control Rod "C" when in Series with the Stationary Elements in Core Position 74 Under the Conditions of Case IV

Figure 2.11 – Power Coastdown and Power to Flow Ratio when the Flow Coastdown is $G/G_0 = (1/1+bt)^{1.25}$

Figure 2.12 - Hot Spot Bulk Water Temperature and Quality of Control Rod "C" when in Series with the Stationary Element in Core Position 74 Under Conditions of Case IV

Figure 2.13 - Minimum Burnout Ratios (DNBR) of the Various Channels when Control Rod "C" is in Series with the Stationary Element in Core Position 74 Under Conditions of Case IV

Unlike previous cases, results are only presented for the first .8 seconds. This condition has been caused by a computer malfunction (print-out tape). This is not too detrimental, since the hot channel minimum DNBR is reached well before .8 seconds. Furthermore, it was determined from the computer on-line printer that this case would have normally been stopped at 3.4 sec due to a hot channel water quality in excess of 40 percent.

Results of the analysis are presented graphically in Figs. 2.14, 2.15, 2.16, 2.17.

2.11.6 Case VI

Case VI has been performed at the same (conservative) flow conditions as Case V but with a slower flow coastdown of b equal to 4.0 to establish the effect of the coastdown on thermal conditions of the hot second pass stationary element.

First pass stationary nominal flow = 244.8 gpm

Second pass stationary nominal flow = 289.1 gpm

Results of the analysis of this case are presented graphically in Figs. 2.18, 2.19, 2.20 and 2.21.

2.12 CONCLUSIONS OF THE TRANSIENT ANALYSIS

From the graphical presentation of the results and the summary as given in Table 2.1 it is seen that even under the most adverse conditions (Case I) the minimum DNBR during the transient is 1.82. This represents a decrease in the minimum DNBR of 2.32 as reported in APAE No. 69⁽⁵⁾. This condition is attributed to compilation of the following effects.

1. A 21 percent increase in core peak to average power.
2. An increase in the severity of the flow coastdown.
3. An element to element maldistribution of 15 percent in the first pass element and 7.0 percent in the second pass control rods.
4. A more realistic axial power distribution of the control rod.

However, even under these conditions, the burnout ratio remains well above the value of 1.5 which is postulated for core thermal safety in a transient. Hence it is concluded that the SM-2 is capable of suffering a pump failure without adverse effects.

Figure 2.14 – Minimum Burnout Ratio (DNBR) of the Hot Channel of the Stationary Element in Core Position 54 when in Series with the Stationary Element in Core Position 74 Under Conditions of Case V

Figure 2.15 – Power Coastdown and Power to Flow Ratio when the Flow Coastdown is $G/G_0 = (1/1+5.95t)1.25$

Figure 2.16 – Hot Spot Bulk Water Temperature and Quality of the Stationary Element in Core Position 54 when in Series with the Stationary Element in Core Position 74 Under Conditions of Case V

Figure 2.17 – Minimum Burnout Ratios (DNBR) of the Various Channels when the Stationary Elements in Core Positions 54 and 74 are in Series Under Conditions of Case V

Figure 2.18 – Minimum Burnout Ratio (DNBR) of the Hot Channel of the Stationary Element in Core Position 54 when in Series with the Stationary Element in Core Position 74 Under the Conditions of Case VI

Figure 2.19 - Power Coastdown and Power to Flow Ratio when the Flow Coastdown is $G/G_0 = (1/1+4.0t)^{1.25}$

Figure 2.20 – Hot Spot Bulk Water Temperature and Quality of the Stationary Element in Core Position 54 when in Series with the Stationary Element in Core Position 74 Under Conditions of Case VI

Figure 2.21 – Minimum Burnout Ratios (DNBR) of the Various Channels when the Stationary Elements in Core Positions 54 and 74 are in Series Under Conditions of Case VI

Furthermore, by comparing the various cases, it is concluded that the steady state minimum DNBR of the second pass hot element as calculated by the ART code is slightly less than, but reasonably compatible to, the value obtained by the STDY code. This condition is created by the difference in input format in the two codes and the increased conservatism in the ART code.

	<u>MIN. DNBR By Means of ART-02</u>	<u>MIN. DNBR By Means of STDY-3</u>
Control rod C	2.08	2.36
Stationary element in core position 54	2.30	2.68

The effect of the element to element maldistribution as obtained in the full scale rig test is negligible in relation to the minimum DNBR.

Case I Minimum DNBR = 1.82

Case II Minimum DNBR = 1.83

but as expected, the increased flow will impede boiling.

In the range of pump coastdowns applicable to the SM-2, the minimum DNBR is not a strong function of the coastdown rate.

Case I Minimum DNBR = 1.82

Case IV Minimum DNBR = 1.84

Due to the delay of .055 sec in the scram mechanism, the minimum transient DNBR is effectively reached and the effect is a minor increase in the DNBR from 1.82 to 1.86. However, the scram mechanism is effective in preventing bulk boiling during the first five seconds.

2.13 ANALYSIS OF LOCAL DENTS IN SM-2 FUEL PLATES

The problem of having a random local dent on the outer fuel plate which forms a half lattice channel is unique. This local dent deflected inward from the outer plate causes a possible local widening of the lattice passage. Effect of this condition upon core thermal safety (minimum DNBR) is estimated to be the same as a ripple of the same depth. This problem has been analyzed on a one-dimensional basis in reference to local rippling in the latest steady state and transient analyses and has been found safe with depressions up to 8 mils.

There are various two-dimensional steady state and transient codes to realistically evaluate thermal conditions of this nature, i.e. EXITE, MITE and TITE by Bettis Atomic Power Laboratory. However, these digital methods of analysis are not presently available. In view of this fact, the verification of the effect of local dents has been deferred until these codes are operational at ALCO.

3.0 RECOMMENDATIONS

With the prevalent steady state and transient analysis, the thermal safety of the SM-2 core has been established. However, because of curtailment of the task, certain aspects of the analysis were omitted. Therefore, it is recommended that the following investigations be performed to complete and finalize the thermal analysis.

1. To perform the hydrodynamic analysis more realistically, the fog flow model as used in ART-02 should be replaced by the slip flow model used in ART-04 and a density coefficient should be incorporated into future studies to account for the reactivity effects due to boiling.
2. The second pass stationary elements should be analyzed using an axial flux distribution accounting for the shift in distribution corresponding to a maximum xenon override (peak occurs near the exit of the channel rather than near the center).
3. To account for the worst feasible condition, the core should be analyzed at the scram power level of 35 MW.
4. Rod and load perturbations should be performed to complete the thermal analysis.
5. Decay heat removal or natural circulation phase should be repeated using the latest available codes and models.

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