HIT-3459-11

MASTER

ANALYTICAL INVESTIGATION OF
NUCLEAR AND THERMAL-HYDRAULIC
DESIGN CHARACTERISTICS OF
SM-1A, CORE 3

Volume II

HIT-161

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INTRODUCTION

A preliminary nuclear and thermal-hydraulic analysis of the SM-1A Core 3 was performed and reported in HIT-141 (Reference 1). The results of that analysis indicated potential problems could exist during operation of this core. Therefore, a detailed nuclear and thermal-hydraulic analysis was undertaken and is reported here.

This report is divided into two volumes. Volume I contains a discussion of the problems considered, a brief description of the analytical methods used, the results obtained for each of the cases considered, and an evaluation of the resultant core operating limitations. Volume II is a Technical Appendix which contains:

Appendix I - A detailed description of the SM-1A Core 3.

Appendix II - A description of the analytical techniques and data used in the nuclear analysis.

Appendix III - A description of the analytical techniques and data used in the thermal-hydraulic analysis.

APPENDIX ICONSTANTS

The following is a compilation of the constants used in this analysis. The constants listed in this section are general for the system and the core. Detailed constants pertinent to the nuclear or thermal-hydraulic analysis are presented in Sections II and III, respectively.

A. Plant Parameters

Thermal Power (nominal)	20.2 Mw
Thermal Power (scram)	24.2 Mw
Operating Pressure (nominal)	1200 psia
Operating Pressure (scram)	1105 psia
Coolant Flow (nominal)	7400 gpm
Coolant Flow (scram)	6650 gpm
Coolant Temperature (average)	435°F
Coolant Temperature Rise (average)	23°F
Coolant Inlet Temperature (average) at 20.2 Mw	423°F
Coolant Outlet Temperature (average) at 20.2 Mw	446°F
Coolant Outlet Temperature (scram)	470°F

B. Core and Thermal Shield Parameters

Core Configuration	7 x 7 array of elements (corners missing)
Equivalent Diameter of Core	22.24 inches
Active Core Height (nominal)	22 inches
Radial Reflector	Water
Inside Diameter of First Thermal Shield	35.5 inches

Outside Diameter of First Thermal Shield	39.5 inches
Inside Diameter of Second Thermal Shield	40.5 inches
Outside Diameter of Second Thermal Shield	44.5 inches
Inside Diameter of Pressure Vessel	45.5 inches
Outside Diameter of Pressure Vessel	51.5 inches
Number of Stationary Elements	38
Number of Movable Elements	7
Material Composition of Core	
U-235 Content	36.3 Kg
B-10 Content	68.1 g
Europium Content (suppressor)	796 g
Europium Content (absorbers)	4.23 Kg
Core Arrangement	Figure I-1
Stationary Element	Figure I-2
Long Fuel Plate	Figure I-3
Short Fuel Plate	Figure I-4
Movable Element	Figure I-5
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C. Orifice Sizes

The orifice size for each stationary fuel element location is given below. The orifice is a round hole in a thin steel plate. The diameter is maintained within ± 0.005 inch.

<u>Location</u>	<u>Diameter (inches)</u>
12	1.758
13	1.882
14	1.870
15	1.855
16	1.745
21	1.705
22	1.680
23	1.764
25	1.808
26	1.734
27	1.673
31	1.767
32	1.720
34	1.815
35	1.823
36	1.730
37	1.815
41	1.773
43	1.823
45	1.864
47	1.833
51	1.817
52	1.707
53	1.840
54	1.790
56	1.820
57	1.802
61	1.740
62	1.735
63	1.733
65	1.790
66	1.736
67	1.750

<u>Location</u>	<u>Diameter (inches)</u>
72	1.734
73	1.800
74	1.810
75	1.925
76	1.820

D. Lattice Orifices

Data for the lattice orifices were developed from Alco Products, Inc. drawing numbers B 9-15-1015 and B 9-15-1016.

xx - Element Position Identification

Rod y - Control Rod Identification

Fuel elements are lined up in the direction shown.

Figure I-1. SM-1A Core 3 Configuration

FIGURE I-2. SM-1A CORE 3 STATIONARY ELEMENT

FIGURE I-3. SM-1A CORE 3 LONG FUEL PLATE

7. BREAK ALL SHARP EDGES AND REMOVE ALL BURS. DEBURRING MUST NOT BREAK EDGES MORE THAN .002 R.

DATE	REV	REVISION RECORD	AUTH	DR
3/10/63	A	ADDED WORD MIN TO F/D	M.R.G.	CRP/ML
4/10/63	B	ADDED NOTE #13		CRP/ML
11/19/63	C	DELETED 1 OF 5 FILES - ADDED AN LENGTH C - CHECKED LENGTH OF DEAD EDGE.	M.Y.G.	CRP/ML

6. THESE DIMENSIONS TO BE EQUAL WITHIN .002.
5. THE SUPPRESSOR CORE (d) SHALL HAVE A MIN. PROJECTED AREA IN THE SIDE VIEW OF 1.16 IN^2 AND A MIN. THICKNESS OF 0.028 OVER THIS AREA.
4. SUPPRESSOR CORE LIMIT IS A SURFACE THAT SEPARATES A REGION CONTAINING EUROPIUM FROM ONE THAT DOES NOT.
3. THE FUEL CORE (d) SHALL HAVE A MIN. PROJECTED AREA IN THE SIDE VIEW OF 56.81 IN^2 AND A MIN. THICKNESS OF 0.028 OVER THIS AREA.
2. FUEL CORE LIMIT IS A SURFACE THAT SEPARATES A REGION CONTAINING URANIUM AND/OR BORON FROM ONE THAT DOES NOT.
1. THIS DWG IS PART OF SPEC FMA-SM-CT-3-1, FUEL PLATE SHALL BE MFG'D IN ACCORDANCE WITH SECTION 3.0

NOTES:

12. THE CORE WIDTH AT THE FUEL CORE (d) SUPPRESSOR CORE (d) INTERFACE (AS SHOWN IN CDS-10) SHALL BE 2.600 MIN. AT THIS POINT ONLY.

SECTION A-A
SCALE: 8:1

11. THESE DIMENSIONS TO BE EQUAL WITHIN $\frac{1}{16}$.
10. THE NOMINAL SUPPRESSOR LOADING IN EACH -100 FUEL PLATE SHALL BE 100 GRAMS OF EUROPIUM WITH A MAX. OF 105 GRAMS AND A MIN. OF 95 GRAMS.
9. THE NOMINAL POISON LOADING IN EACH -100 FUEL PLATE SHALL BE 86.90 MILLI GRAMS OF BORON-10 WITH A MAX. OF 91.25 MGMS. AND A MIN. OF 82.55 MGMS.
8. THE NOMINAL FUEL LOADING IN EACH -100 FUEL PLATE SHALL BE 46.30 GRAMS OF URANIUM-235 WITH A MAX. OF 46.65 GRAMS AND A MIN. OF 45.84 GRAMS.

NOTES: (CONT)

-100		FUEL PLATE ASS'Y		MATERIAL	
PART NO.		DESCRIPTION		MATERIAL	
STATIONARY FUEL PLATE (LONG)				HITTMAN ASSOCIATES, INC.	
SCALE: FULL				RDS 125	
UNLESS OTHERWISE SPECIFIED DIMENSIONS ARE IN INCHES AND ARE AFTER PLATING.					
FRACTIONS		TOLERANCES ON DECIMALS		ANGLES	
1/16		.01		1/2	
DRAWN BY: C.R. Hocking				CHECKED: [Signature]	
APPROVED: [Signature]				C 302-6100	

FIGURE I-4. SM-1A CORE 3 SHORT FUEL PLATE

-100 ASSEMBLY

12. THE CORE WIDTH AT THE FUEL CORE (d) -
 (B) SUPPRESSOR CORE (d) INTERFACE (AS SHOWN IN FIG. 10) SHALL BE 2.600 MIN. AT THIS POINT ONLY.

DATE	REV	REVISION RECORD	AUTH.	CHK.	ONE
9/1/63	A	DELETED MISS-PLACED			
		NARROW HEADS	W.D.	CHP	
10/1/63	B	APPLIED NOTE # 12			

11. THESE DIMENSIONS TO BE EQUAL WITHIN $\pm .1$.
10. THE NOMINAL SUPPRESSOR LOADING IN EACH -100 FUEL PLATE SHALL BE 1.00 GRAMS OF EUROPIUM WITH A MAX. OF 1.05 GRAMS AND A MIN. OF 0.95 GRAMS.
9. THE NOMINAL POISON LOADING IN EACH -100 FUEL PLATE SHALL BE 86.90 MILLI GRAMS OF BORON-10 WITH A MAX. OF 91.25 MGMS AND A MIN. OF 82.55 MGMS.
8. THE NOMINAL FUEL LOADING IN EACH -100 FUEL PLATE SHALL BE 46.30 GRAMS OF URANIUM-235 WITH A MAX. OF 46.65 GRAMS AND A MIN. OF 45.84 GRAMS.
7. BREAK ALL SHARP EDGES AND REMOVE ALL BURRS. DEBURRING MUST NOT BREAK EDGES MORE THAN .002 R.

	B	5" DIM. $\pm .1$ WAS			
		23 1/2			
10/1/63	C	DELETED 2 DSB PLATE			
		APPLY ON LENGTH	N.Y.O.	CHP	

6. THESE DIMENSIONS TO BE EQUAL WITHIN .002.
5. THE SUPPRESSOR CORE (d) SHALL HAVE A MIN. PROJECTED AREA IN THE SIDE VIEW OF 1.16 IN² AND A MIN. THICKNESS OF 0.028 OVER THIS AREA.
4. SUPPRESSOR CORE LIMIT IS A SURFACE THAT SEPARATES A REGION CONTAINING EUROPIUM FROM ONE THAT DOES NOT.
3. THE FUEL CORE SHALL HAVE A MIN. PROJECTED AREA IN THE SIDE VIEW OF 56.81 IN² AND A MIN. THICKNESS OF 0.028 OVER THIS AREA.
2. FUEL CORE LIMIT IS A SURFACE THAT SEPARATES A REGION CONTAINING URANIUM AND/OR BORON FROM ONE THAT DOES NOT.

NOTES: 1. THIS DWG IS PART OF SPEC FHA-SM-CT-3-1. FUEL PLATE SHALL BE MANUFACTURED IN ACCORDANCE WITH SECTION 3.0.

-100 FUEL PLATE ASSY			
PART NO.	DESCRIPTION	MATERIAL	
STATIONARY FUEL PLATE (SHORT)		HITTMAN ASSOCIATES, INC.	
SCALE FULL		1/2" = 1"	
UNLESS OTHERWISE SPECIFIED DIMENSIONS ARE IN INCHES AND ARE AFTER PLATING.			
FRACTIONS	DECIMALS	ANGLES	
$\pm \frac{1}{16}$	.X XX .XX	.1 .01 .001	
TOLERANCES ON DECIMALS		ANGLES	
.1 .01 .001		.1/2°	
DRAWN BY C. R. Hopkins		CHECKED	
APPROVED		C 302-6200

- [illegible]

Technical drawing of a reactor assembly cross-section, labeled **-100 ASSEMBLY**. The drawing shows a central fuel core limit, a flux suppressor, and a dead edge. Dimensions include 2.400 MIN. for the fuel core limit, 2.300 MIN. for the flux suppressor, and 2.300 MIN. for the dead edge. A section line A-A is indicated. The drawing is labeled **SECTION A-A** and **SCALE: 0:1**.

- NOTES:

[illegible]

- 12. THE CORE WIDTH AT THE FUEL CORE (4)-SUPPRESSOR
 (A) CORE (4) INTERFACE (AS SHOWN IN CCS-10) SHALL BE
 2.350 INCH. AT THE POINT ONLY.
- 11. THE 250 DIMENSIONS TO BE EQUAL WITHIN
 $\pm .0$.
- 10. THE NOMINAL SUPPRESSOR LOADING IN EACH
 -100 FUEL PLATE SHALL BE 0.91 GRAMS OF
 EUROPIUM WITH A MAX. OF 0.95 GRAMS
 AND A MIN. OF 0.87 GRAMS.
- 9. THE NOMINAL POISON LOADING IN EACH -100
 FUEL PLATE SHALL BE 76.90 MILLI GRAMS
 OF BORON-10 WITH A MAX OF 80.74 MGMS.
 AND A MIN. OF 73.06 MGMS.
- 8. THE NOMINAL FUEL LOADING FOR EACH -100
 FUEL PLATE SHALL BE 40.90 GRAMS OF URANIUM
 -235 WITH A MAX OF 44.29 GRAMS AND A MIN. OF
 40.57 GRAMS.

-100	FUEL PLATE ASS'Y	
PART NO.	DESCRIPTION	QUANTITY

MOVEABLE FUEL PLATE

**HITTMAN
Associates, Inc.**

700 WEST 89TH
STREET - CHICAGO, ILL. 60619

SCALE FULL	RWS	☒	
-------------------	-----	-------------------------------------	--

UNLESS OTHERWISE SPECIFIED
DIMENSIONS ARE IN INCHES AND ARE AFTER
PLATING.

	TOLERANCES ON DECIMALS			ANGLES
FRACTIONS	X	.XX	.XXX	
C Yrds				
inches	1	.01	.001	1/2

GRAND BY *G.R. Hittman*

CHECKED *[Signature]*

APPROVED *[Signature]*

C302-7106

FIGURE I-7. SM-1A CORE 3 ABSORBER ELEMENT

DETAIL "A"
ENLARGED VIEW
OF WELD

DETAIL -101
BREAK ALL SHARP EDGES

DETAIL -100

2 WELD MUST FALL WITHIN BOUNDARIES ON DETAIL 'A' AND MUST BE ESSENTIALLY FLUSH AND FREE OF DEFECTS AS PER DYE PENETRANT EXAMINATION.

1 THIS DWG IS A PART OF SPEC FHA-SM-CT-3-1. ABSORBER SECTION SHALL BE MFG'D IN ACCORDANCE WITH SECTION

NOTES:

DATE	LET	REVISION RECORD	AUTH	BY	CHK
4/24/64	A	DELETED DIM + (6m)			
		FROM DETAIL -100			
	A	DELETED SOLID LINE			
		FROM DETAIL -100			
	A	DELETED EXTRA CALL			
		OUTS FROM SIDE VIEW			
		OF DETAIL -100			

-101		PIN	
-100		ABSORBER SECTION ASBY	
PART NO	DESCRIPTION	MATERIAL	
ABSORBER SECTION ASSEMBLY		HITTMAN ASSOCIATES, INC.	
SCALE FULL		DATE 7/5	
UNLESS OTHERWISE SPECIFIED DIMENSIONS ARE IN INCHES AND ARE AFTER PLATING.		DRAWN BY C. R. Hocking	
TOLERANCES ON DECIMALS		CHECKED	
FRACTIONS		APPROVED	
X .XX .XXX		C302-800A	
± 1 ± .01 ± .00 ± .005			

FIGURE I-8. SM-1A CORE 3 ABSORBER PLATE

DATE	LET	REVISION RECORD	AUTH	DE. CAL.
4/25/66	A	DELETED PARAGRAPH 8 -		
		ICULARITY REQUIREMENT		
	A ₁	CHANGED PLATE LENGTH		
		FROM 14.000 TO 26.125		
	A ₂	DELETE NOTE # 2		
	A ₃	DELETE CHAMFER ON		
		-101	HYD. DR.

DETAIL NO	HOLE
302-B10-101	"V" (.377) DRILL
302-B10-102	OMIT

1 THIS DWG IS A PART OF SPEC. FHA-SM-CT-3-1.
ABSORBER PLATE SHALL BE MFG'D IN ACCORDANCE
WITH SECTION .

NOTES:

302-B10-102	PLATE (ABSORBER)	
302-B10-101	PLATE (ABSORBER)	
PART NO	DESCRIPTION	MATERIAL
ABSORBER PLATE		
SCALE FULL		RHS 1/25
UNLESS OTHERWISE SPECIFIED DIMENSIONS ARE IN INCHES AND ARE AFTER PLATING.		
TOLERANCES ON DECIMALS		
FRACTIONS	7	XX
1/16	1	± .03
1/32	1	± .01
1/64	1	± .005
1/128	1	± .002
DRAWN BY C.E. Hooten		CHECKED
APPROVED		
C302-810 A		

REFERENCES

1. "Preliminary Nuclear and Thermal-Hydraulic Analyses of the As-Built SM-1A Core III," HIT-141, July 9, 1964.

APPENDIX II

NUCLEAR PHYSICS ANALYSIS

A. Physics Method for Analyzing SM Reactor Cores

The physics analytical methods for SM-1A Core 3 are based on the solution of three-energy-group neutron diffusion equations. The evaluation of the parameters to be inserted into these equations has been developed (Reference 1).

The procedure treats the radial and axial calculations separately and combines the results to synthesize the actual three-dimensional reactor conditions. In this procedure, the active core is separated into two axial fuel regions, one containing stationary and moving fuel, the other containing stationary fuel and control element poison boxes. Appropriate three-group parameters are calculated which treat the stationary fuel, moving fuel, and control element poison boxes as separate radial regions. Two-dimensional radial calculations are carried out which directly determine the gross radial power distributions. These results are used to evaluate the flux weighted diffusion theory parameters and the equivalent absorption cross section for the poison rod sections to be used in one-dimensional axial calculations. The axial calculations determine the critical bank height and the axial power distributions.

Two computer programs have been used to carry out the calculations. The PDQ Code (Reference 2) is applied for the two-dimensional radial calculations and the WANDA Code (Reference 3) is applied for the one-dimensional axial calculation.

1. Description of Method

a. Three-Group Equations. The three-group method that is used to calculate the reactivity and power distribution considers neutron fission and absorption to occur in the epithermal and thermal groups. The three-group equations applicable for all diffusion regions in the reactor are:

$$-D_1 \nabla^2 \phi_1 + \Sigma_{sL1} \phi_1 = \nu (\Sigma_{f2} \phi_2 + \Sigma_{f3} \phi_3)$$

$$-D_2 \nabla^2 \phi_2 + (\Sigma_{a2} + \Sigma_{sL2}) \phi_2 = \Sigma_{sL1} \phi_1$$

$$-D_3 \nabla^2 \phi_3 + \Sigma_{a3} \phi_3 = \Sigma_{sL2} \phi_2$$

(1)

where:	D	=	diffusion coefficient
	Σ_{sl}	=	slowing down or removal cross section
	Σ_a	=	absorption cross section
	Σ_f	=	fission cross section
	ν	=	number of neutrons released per fission
	1, 2, 3	=	subscripts are designations of the neutron energy groups

The three neutron energy groups can be defined as the fast, 10 Mev to 180 Kev, epithermal, 180 Kev to thermal cutoff energy; and thermal, from the thermal cutoff energy to zero energy.

If the neutron absorption cross section is larger than the scattering cross section for one or more neutron groups within a region, the neutron flux for those groups is not defined interior to such a region, but a transport condition is applied at the boundary of the region

b. Thermal Cross Sections

(1) Neutron Temperature. In order to determine the thermal cross sections, the microscopic cross sections are averaged over the proper thermal spectrum. The procedure that has been adopted is to use a "hardened" or shifted Maxwellian spectrum whose most probable energy T_n is determined by the following expression:

$$T_n = T_0 \left(1 + 75 \frac{\Sigma_a}{\xi \Sigma_s} \right)^2 \quad (2)$$

where:	T_0	=	the physical temperature in electron volts
	Σ_a	=	the absorption cross section at temperature T_0
	$\xi \Sigma_s$	=	the slowing down cross section just above thermal energy

The thermal cutoff energy for the Maxwellian spectrum is defined as $4 T_n$.

A neutron temperature is computed for each defined diffusion region within the reactor.

(2) Microscopic Absorption Cross Sections. The microscopic cross sections for the $1/v$ absorbers at a neutron velocity of 2200 m/sec that are contained within an SM fuel element are as follows:

	<u>σ_{2200} (barns)</u>
H	0.332
Cr	3.10
Fe	2.53
Ni	4.8
Mn	13.2
U-238	2.71
B-10	3813
C	0.0034

The average thermal cross sections are found by averaging over a Maxwellian spectrum determined by Equation (2) in the interval from 0 to $4 T_n$. For the $1/v$ absorbers, numerical factors can be simply calculated to convert from the value at 2200 m/sec:

$$\bar{\sigma}_{1/v} = 0.93 \left[\frac{0.0253}{T_n} \right]^{1/2} \sigma_{2200} \quad (3)$$

For the non- $1/v$ absorbers, U-235 and Xe-135, a numerical integration of the cross sections is performed. For U-235 and Xe-135, the following average absorption microscopic cross sections have been computed as a function of neutron temperature:

<u>T (ev)</u>	<u>U-235 (barns)</u>	<u>Xe (megabarns)</u>
0.025	633	2.73
0.040	484	2.36
0.060	387	1.88
0.080	338	1.48
0.100	303	1.18

The value of ν , neutrons released per thermal fission, for U-235 is 2.46. The value of α , ratio of non-fission to fission capture for thermal neutrons is chosen to be 0.184 for U-235 independent of neutron energy.

(3) Microscopic Thermal Transport Cross Sections. The transport cross section, σ_{tr} , of an atom is defined in the usual form for isotropic scattering.

$$\sigma_{tr} = \sigma_s \left(1 - \frac{2}{3A} \right) \quad (4)$$

where σ_s is the scattering cross section and A is the atomic mass. The hydrogen transport cross section is assumed to have a $1/v$ dependence and is averaged in a similar manner to the $1/v$ absorber. For all other elements, the transport cross section is assumed to be independent of temperature and spectrum.

	σ_{tr} (barns)
H	36.8 (1/v)
Cr	3.0
Fe	11.0
Ni	17.5
Mn	2.3
O	4.0
U	8.3
B	3.8
C	4.5

(4) Macroscopic Cross Sections. The macroscopic thermal cross section for a fuel region within the core consisting of fissile material and burnable poison in a stainless steel matrix, clad with stainless steel, and joined by stainless steel structure is the following:

$$-\Sigma_{a3} = \frac{V_F \Sigma_F + V_{BP} \Sigma_{BP} + V_M \Sigma_{SS} + \frac{\phi_{CL}}{\phi_F} V_{CL} \Sigma_{SS} + \frac{\phi_{H_2O}}{\phi_F} V_{H_2O} \Sigma_{H_2O} + \frac{\phi_{ST}}{\phi_F} V_{ST} \Sigma_{SS}}{(1 - V_{CL} - V_{H_2O} - V_{ST}) + \frac{\phi_{CL}}{\phi_F} V_{CL} + \frac{\phi_{H_2O}}{\phi_F} V_{H_2O} + \frac{\phi_{ST}}{\phi_F} V_{ST}} \quad (5)$$

where F, BP, M, Cl, H₂O and ST refer to fuel, burnable poison, matrix, clad, moderator, and structure respectively; v is the volume fraction; $\frac{\phi_{CL}}{\phi_F}$, $\frac{\phi_{H_2O}}{\phi_F}$, and $\frac{\phi_{ST}}{\phi_F}$ refer to the disadvantage factors for the respective regions.

Σ_{tr3} is similarly defined. The total transport cross section, $\Sigma_{tr_{tot}}$, is the sum of Σ_{tr3} and Σ_{a3} . The thermal diffusion coefficient D_3 for the region is:

$$D_3 = \frac{1}{3 \Sigma_{tr_{tot}}} = \frac{1}{3 (\Sigma_{tr3} + \Sigma_{a3})} \quad (6)$$

(5) High Absorption Regions. For high absorption regions, the quantity d , the reciprocal of the logarithmic derivative of the flux at the surface of the absorber, can be related to the transmission probability of a neutron penetrating a slab region:

$$d = \frac{2}{3} \lambda_{tr} \left(\frac{1+T}{1-T} \right) \quad (7)$$

where λ_{tr} is the transport mean free path and T is the transmission probability. For the thermal neutron energy group:

$$T = 2 E_3 (\bar{\Sigma} t) \quad (8)$$

where $E_3(x) = \int_1^\infty \frac{e^{-xy}}{y^3} dy$ and $\bar{\Sigma} t$ is the

average of the macroscopic absorption cross section of the slab multiplied by the slab thickness.

The quantity d can be applied directly to the PDQ computer program. Artificial cross sections can be derived which duplicate this condition for computer programs such as WANDA that only consider diffusion regions.

c. Disadvantage Factors. The disadvantage factors are calculated from an analytic expression using diffusion theory in the moderator and a transport condition at the surface of the fuel region. The clad is treated as a void region to calculate $\frac{\phi_{H_2O}}{\phi_F}$, the relative flux in the water to that in the fuel. The disadvantage factor of the clad, $\frac{\phi_{CL}}{\phi_F}$, is taken as $(1 + \frac{\phi_{H_2O}}{\phi_F})/2$, and the disadvantage factor of the structure, $\frac{\phi_{ST}}{\phi_F}$, is assumed to be identical to $\frac{\phi_{H_2O}}{\phi_F}$.

$$\frac{\phi_{H_2O}}{\phi_F} = \frac{3 \Sigma_f \lambda_0}{k \lambda} \left[k s - \frac{1}{k t} + \coth k t \right] \quad (9)$$

where:

- λ = neutron transport mean free path
- k = inverse neutron thermal diffusion length
- λ_0 = half thickness of plate
- t = half thickness of water channel
- Σ_f = macroscopic absorption cross section of plate
- s =
$$\frac{2 \lambda}{3} \frac{\left[\frac{1}{2} + E_3(2 \Sigma_f \lambda_0) \right]}{\left[\frac{1}{2} - E_3(2 \Sigma_f \lambda_0) \right]}$$

d. Epithermal Absorption and Fission Cross Sections. The epithermal absorption and fission cross sections for this reactor type have been found empirically based on the thermal group values:

$$\begin{aligned} \Sigma_{a2} &= \frac{\Sigma_{a3}}{0.93 (\Delta U)} \\ \Sigma_{f2} &= \frac{\Sigma_{a3}^{U-235}}{0.93 (\Delta U) (1.35)} \end{aligned} \quad (10)$$

where ΔU is the lethargy of the epithermal group (the value of α , ratio of non-fission to fission capture for epithermal neutrons, is 0.35).

For control rod regions, a calculated transmission probability is used for the epithermal group.

e. Diffusion Coefficients. The diffusion coefficients for the first and second groups are defined in a manner similar to that used for the thermal diffusion coefficient - with the exception that neutron absorption is neglected. The microscopic transport cross sections are found by averaging the measured values over appropriate neutron fluxes. The first-group flux is assumed to be the flux determined by the solution of the infinite medium Boltzmann equation for a fission source in pure water. The second group is assumed to have a $1/E$ energy dependence. The resulting microscopic transport cross sections are the following for each group:

	σ_{tr_1} (barns)	σ_{tr_2} (barns)
H ₂ O	6.03	16.44
Stainless Steel [18% Cr, 8% Ni]	3.09	9.02
UO ₂	14.18	21.28

The total macroscopic transport cross section, Σ_{tr} , is the sum of the volume-weighted individual macroscopic cross sections with the diffusion coefficient being:

$$D = \frac{1}{3\Sigma_{tr}} \quad (11)$$

f. Slowing Down or Removal Cross Sections. The slowing down or removal cross section, Σ_{sl} , for each group is defined as the ratio of the diffusion coefficient to the neutron age for a non-absorbing medium.

The neutron age is determined for the fast and epithermal groups by calculating the equivalent transport and slowing down factors of the materials relative to those of pure water, neglecting absorption occurring within the group. The age in pure water to Indium resonance that is used as the basis of these calculations is 30.5 cm^2 . This value is best able to predict both the results of migration area measurements and the reactivity of small cores in which neutron leakage is quite high. The age is divided such that 25 cm^2 of the total is associated with the first group and the remainder with the second group. In addition, the age in the second group must be extended to the thermal cutoff. For the first group:

$$\gamma_1 = \frac{25(1 + V_{ss} + V_{UO_2})^2}{(\rho_{H_2O} + V_{ss} X_1^{ss} + V_{UO_2} X_1^{UO_2})(\rho_{H_2O} + V_{ss} Y_1^{ss} + V_{UO_2} Y_1^{UO_2})} \quad (12)$$

where V_{ss} and V_{UO_2} are the volume ratio of stainless steel and UO_2 to water respectively, X_1 and Y_1 are the transport and slowing down equivalence of these materials to water; and ρ_{H_2O} is the density of water.

For the epithermal group:

$$\gamma_2 = \frac{5.5\left(\frac{\Delta U}{11.75}\right)(1 + V_{ss} + V_{UO_2})^2}{(\rho_{H_2O} + V_{ss} X_2^{ss} + V_{UO_2} X_2^{UO_2})(\rho_{H_2O} + V_{ss} Y_2^{ss} + V_{UO_2} Y_2^{UO_2})} \quad (13)$$

where V , X and Y are defined the same as for the first group, and ΔU is the lethargy interval of the epithermal group. The equivalence factors are the following for each group:

	X_1	X_2	Y_1	Y_2
Stainless steel [7.85 g/cm ³ 18% Cr; 8% Ni]	1.30	1.39	0.189	0.021
UO_2 (10.25 g/cm ³)	1.61	0.885	0.445	0.019

The migration area, M^2 , is the sum of the total age and the square of the thermal diffusion length.

g. Reflector Saving. The reflector saving of a water, or water-stainless steel region external to the core is a function of the leakage of neutrons from the core. An expression for the reflector saving, δ , which appears to be consistent with both machine calculations and experimental measurements is:

$$\delta(\text{cm}) = 7.2 + 0.10(M^2 - 40.0) \quad (14)$$

where M^2 is the migration area. This saving is used in two-dimensional calculations to determine axial buckling and in one-dimensional calculations to determine radial buckling.

h. Poison Rod Region and Flux Suppressors. Since the control rods are operated as a bank throughout lifetime, an equivalent absorption cross section is calculated for the core region containing the poison rod section rods and the window-shade technique is used to determine critical bank height and axial power distributions.

The procedure followed is to perform two-dimensional calculations in which the geometrical configuration of the poisoned region is described exactly. The parameters for the control rod region itself are calculated by the method described previously for high absorption regions. The resulting reactivity is inserted into a three-group reactivity solution of Equation (1), in which bucklings based on reflector saving are used, and the equivalent thermal and epithermal absorption cross sections for the control regions are determined.

The reactivity worth of individual control assemblies depends on the fuel and poison content of the stationary and moving fuel sections and the radial and axial locations of the other control assemblies. Detailed analysis has been carried out for the initial fuel and poison concentrations for specific rod configurations at 68°F and 435°F. The total reactivity change associated with individual rod movement from complete insertion to complete withdrawal is calculated directly. Based on these calculations, equivalent absorption cross sections are determined for rod banks containing 7 rods, 6 rods, (rod C at 22 inches), 5 rods (rods A and B at 22 inches) and 4 rods (rods A, B and C at 22 inches). These equivalent absorptions are used in the one-dimensional axial analysis to determine reactivity change per inch for the 7-rod and 5-rod banks. For the individual rod movement of rods A, B, and C, the same theoretical model is used to determine reactivity change per inch but it is not expected to be as good an approximation as the bank calculations, since individual rods are not as accurately described by a smeared out equivalence absorption.

In order to describe the flux suppressor region of the fuel elements, various models were investigated to ascertain the most accurate procedure that could best be incorporated into the entire analytical package. Based on the comparison of analysis with SM-1 Core III experiments, discussed in Part B, it has been found that the suppressor region of the stationary fuel elements can be treated as an axial region that is identical to the fuel region but with a zero probability for fission.

i. Power Distributions. Gross radial and axial power distributions are determined directly from the PDQ and WANDA calculations. Each of these calculations is normalized to unity and the combination of these results yields the three-dimensional gross power generation per fuel element.

The detailed radial power generation along a particular traverse to determine plate power distributions within a fuel element is found by superimposing local peaking factors upon gross radial power distribution. The local peaking factors are produced by larger water gaps between fuel elements than exist between fuel plates. The difference of the spacing between end plates of adjacent fuel elements and the spacing between plates within an element is defined as equivalent channel thickness.

The local peaking factor, $\frac{\phi_p}{\phi_o}$, defined as the ratio of peak flux produced by the channel to the asymptotic flux, is

$$\frac{\phi_p}{\phi_o} = 1 + \frac{l}{2L} e^{-x/L} \quad (15)$$

where l is the equivalent channel thickness, L is the thermal diffusion length in the fuel element and x is the distance measured from the edge of the fuel element.

j. Temperature Defect and Temperature Coefficient. The temperature defect, which is defined as the reactivity change produced by a change in temperature, is calculated by determining the critical bank positions at different temperatures and relating the change in bank position to reactivity.

The temperature coefficient is calculated by assuming the reactivity change to be proportional to the density change of water, i. e.,

$\frac{dk}{dT} = F \frac{d\rho}{dT}$ where F is a constant determined by the temperature defect calculation and $\frac{d\rho}{dT}$ is the density change of water with temperature.

For these calculations the critical bank position is determined for three temperature conditions: 68°F, 300°F, and 435°F. The value of the constant, F , is then determined from the differential reactivity bank calculations.

k. Flux Distributions External to the Core. The three-group diffusion theory used for calculating static core properties has been extended to include calculation of neutron fluxes in the iron-water regions external to the core (Reference 4). The neutron spectrum external to the core is determined by neutrons that penetrate the water regions before suffering their first hydrogen collision. These unscattered neutrons have high energy, since the energy dependence of the hydrogen cross section does not permit low energy neutrons to penetrate without collision.

The inelastic scattering cross section of iron at these high energies for the type of spectra that exist is essentially independent of energy. The effective hydrogen cross section as a function of neutron penetration through water has been determined by analytically fitting the experimentally measured thermal flux distributions. Similarly, the appropriate iron cross sections have been derived by empirically fitting experimental results. These iron and water cross sections can be employed in primary shield design to generate thermal, epithermal, and fast neutron flux distribution. The distribution of gamma ray sources resulting from neutron capture, as well as the magnitude of the integrated neutron flux affecting ductility of the pressure vessel, can be determined from these neutron flux distributions. The comparison of the result with SM-1 Core I critical measurement is shown in Part B.

The calculations of the fast flux specify the flux in the energy range > 180 Kev. The number of neutrons with energy in excess of 1 Mev is found by integrating the fission neutron spectrum to determine the ratio of the number of neutrons with energy greater than 1 Mev to that greater than 180 Kev. This factor is 0.675.

1. Lifetime Calculations. The objective of the lifetime calculations is to determine the operating lifetime of the core and to ascertain at what point in life, if any, operating characteristics are more adverse than at startup. To perform a lifetime calculation to as high a degree of precision as has been utilized for the startup condition would require a detailed description of the non-uniform burnup of fuel and poison and the non-uniform production of fission products. Such analysis would be relatively complicated and very costly. Therefore, an attempt is made to reduce the cost of analysis while introducing relatively small uncertainties in the predicted lifetime characteristics.

Previous lifetime analysis of the SM-1A Core 3 (Reference 5) has demonstrated the effect of possible error in assuming uniform radial power distribution during lifetime. These calculations show the radial power distribution near midlife, or maximum reactivity, is very nearly the same as at initial startup and also indicate the decrease in lifetime to be expected for non-uniform radial burnup. Consequently, the uniform radial burnup assumption simplifies lifetime analysis while minimally decreasing calculational accuracy.

Assuming a uniform radial burnup, a method has been developed which utilizes a one-dimensional axial burnup in which the spatial dependence of fuel and poison burnup is described in reasonable detail and which takes into account the relative axial position of the moving fuel relative to the stationary fuel. In this procedure, the stationary and moving fuel regions are each divided into ten axial regions. At each time interval, the 7-rod critical bank height is located and the power distributions are used to determine fuel and poison burnout and fission product formation for the next time interval.

In order to further simplify these one-dimensional calculations, the equivalent epithermal and thermal absorptions applicable to the axial region containing the poison section of the control elements are determined at startup and maintained constant throughout the lifetime. The justification for this assumption is that two-dimensional radial calculations are required to determine the equivalent absorption cross sections at each time step which, if performed, would add significantly to the cost of performing calculations. Using this constant equivalence for the poison section of the control element has little effect on the lifetime analysis. However, it does affect calculations such as shutdown margin when the poison section of the moving fuel is moved into the burned out portion of the core. In this case, the shutdown margin would be over-calculated and a correction has to be introduced that reduces slightly the control rod effectiveness.

The effect of fission product absorption is determined in terms of equivalent B-10 formation (Reference 6).

2. Three-Group Diffusion Parameters

The nuclear characteristics of startup have been calculated for three different core conditions: 68°F, no xenon; 435°F, no xenon; and 435°F, equilibrium xenon. The three-group diffusion parameters used in the analysis are given in Table II-1.

TABLE II-1

Three-Group Parameters for Stationary and Moving
Fuel Elements

	<u>Stationary Fuel Element</u>		
	<u>68°F, No Xenon</u>	<u>435°F, No Xenon</u>	<u>435°F, Eq. Xe.</u>
D_1	1.52900	1.71633	1.71633
D_2	0.56234	0.63145	0.63145
D_3	0.18521	0.24882	0.24978
Σ_{sL1}	0.05240	0.04461	0.04461
Σ_{sL2}	0.07066	0.06146	0.06156
Σ_{a2}	0.03115	0.02605	0.02589
Σ_{a3}	0.35268	0.29028	0.29341
$\nu\Sigma_{f2}$	0.03974	0.03360	0.03342
$\nu\Sigma_{f3}$	0.50864	0.42416	0.42090

TABLE II-1 (Cont.)

	<u>Moving Fuel Element</u>		
	<u>68°F, No Xenon</u>	<u>435°F, No Xenon</u>	<u>435°F, Eq. Xe.</u>
D_1	1.52275	1.70337	1.70337
D_2	0.55625	0.62192	0.62192
D_3	0.18496	0.25006	0.25081
Σ_{SL1}	0.05135	0.04375	0.04375
Σ_{SL2}	0.06843	0.05956	0.05964
Σa_2	0.02691	0.02210	0.02196
Σa_3	0.30618	0.24786	0.25110
$\nu \Sigma_{f2}$	0.03292	0.02722	0.02706
$\nu \Sigma_{f3}$	0.42097	0.34417	0.34133

B. Comparison of Physics Analysis with SM Core Measurements

A comparison of the calculated core nuclear characteristics has been made with the measured values at the start of life condition for SM-1 Core I and with the zero power critical experiment for SM-1 Core III.

1. SM-1 Core I at 0 Mw-Yr

a. Reactivity. The critical 5-rod bank position has been determined at 68°F (no xenon), 440°F (no xenon), and 440°F (equilibrium xenon). The results of these calculations were compared with the actual results reported in Reference 6. The material and dimensional constants for SM-1 Core I were obtained from design reports, fabrication drawings, and personal communications. The comparison of these results is listed in Table II-2.

TABLE II-2
Comparison of 5-Rod Bank Criticality

<u>Operating Condition</u>	<u>5-Rod Bank Position (in)</u>	<u>Measured K_{eff}</u>	<u>Calculated K_{eff}</u>
68°F (no xenon)	3.7	1.000	0.987
440°F (no xenon)	6.7	1.000	0.998
440°F (equilibrium xenon)	8.3	1.000	1.001

The cold condition deviates 1.3% Δk from criticality which corresponds to an error of 0.7 inch in bank height. The hot conditions are duplicated almost exactly. These errors are due to inaccuracies in the method, inaccuracies in determining physical and geometric constants, and inaccuracies in the experimentally reported values.

b. Flux Distributions. The SM-1 Core I zero power experiments investigated the detailed shield design. Thermal neutron fluxes were measured in one quadrant of the core. Figure II-1 shows the experimental arrangement used in the calculation.

The calculated three-group fluxes for the SM-1 shielding experimental arrangement and the comparison with the measured thermal flux are shown in Figure II-2. The agreement in the material regions is excellent. In the void region, the measured thermal fluxes are higher by an order of magnitude. This discrepancy is caused by neutrons streaming between neighboring slabs and cannot be accounted for by diffusion theory.

2. SM-1 Core III Critical Experiments

a. Zero Power Critical Experiments. Zero power critical experiments have been performed on SM-1 Core III and reported in Reference 7. This core contains flux suppressors and europium rods. In fact, with the exception of the boron-10 burnable poison and the use of 37 fuel elements rather than 45, the core is almost identical to SM-1A Core 3. Therefore, a comparison between theory with SM-1 Core III experiment permits evaluation of the methods to be used in SM-1A Core 3.

There are two types of measurements that can be compared against analysis; criticality and reactivity. Criticality involves a critical configuration so that direct comparison between theory and experiment is possible. Reactivity measurement involves calibration of control rods expressed in units of dollars. For large reactivity changes, the measured values consist of integrated differential values. Also, reactivity calculations are performed in units of Δk . For SM-1 Core III, 0.0078 Δk is equal to one dollar of reactivity. This conversion factor is taken from safeguards report ACNP-64516 and agrees within 2 percent with the value calculated by the analytical techniques used in this report.

FIGURE II-1. SM-1 CORE I SHIELDING ARRANGEMENT (ALL DISTANCES IN INCHES)

FIGURE II-2. NEUTRON FLUX DISTRIBUTION FOR SM-1
CORE I ZERO POWER SHIELDING EXPERIMENT

In addition to the direct comparison between theory and experiment to determine accuracy of the analytical methods, the analysis of the SM-1 Core III permits improvement and simplification of analytical models to reduce the time to prepare calculations and reduce the cost of digital computer machine time used to carry out the calculations for all SM cores.

b. SM-1 Core III Five-Rod Bank Analyses

(1) Four Region Core. The full SM-1 core contains 37 fuel elements, 8 dummy elements and a 0.050 inch stainless steel skirt. The five-rod critical bank position is 7.155 inches.

Calculations were first carried out using the most accurate geometrical description of the core. As a function of axial height, four different cross sectional regions exist as shown in Figure II-3. The limits of these regions starting from the bottom of the active core are: the distance between the bottom of the stationary fuel to the bottom of the flux suppressors of the moving fuel, the height of the flux suppressors of the moving fuel which is 0.5 inch, the distance between the top of the flux suppressors of the moving fuel to the bottom of the poison of the moving fuel which is 1.0 inch, and the distance between the bottom of the poison of the moving fuel to the top of the stationary fuel. For each of these core regions, two-dimensional X-Y calculations were performed and the average three-group constants for each core region were calculated.

For the suppressor region, the 0.5 inch region below the stationary fuel, the effective three-group diffusion constants are calculated using diffusion theory in the fast group and transport theory in the thermal and epithermal groups. A uniformly distributed poison is assumed for the thermal neutron energy group and the transmission probability for thermal neutrons to penetrate the suppressor region is calculated; the results of the calculation yield a transmission probability that is essentially zero. For the epithermal neutron energy group, an equivalence factor relative to boron-10 is calculated to determine the epithermal transmission probability; this value is 0.84. These transmission probabilities, which correspond to transport conditions, can be inserted into the WANDA multigroup diffusion code in terms of effective diffusion cross sections. For a region of thickness, t , with three mesh points describing the region, the effective absorption cross section and diffusion coefficient as a function of the transmission probability, T , are:

$$\Sigma_{a\text{ eff}} = \frac{2}{t} \left[\frac{1 - \sqrt{T}}{1 + \sqrt{T}} \right]$$

$$D_{\text{eff}} = \frac{t}{2} \left[\frac{\sqrt{T}}{1 - T} \right]$$

FIGURE II-3. SCHEMATIC REPRESENTATION OF CORE USING FOUR REGIONS

The core parameters for each of the core regions together with the suppressor and water reflector regional parameters are put into the one dimensional WANDA code and the reactivity, k_{eff} , is calculated. At the measured critical bank position, $k_{\text{eff}} = 1.000$, the calculated reactivity is $k_{\text{eff}} = 0.996$.

Varying the epithermal transmission probability of the suppressor region by 10 percent changes the reactivity by $0.002 \Delta k$. This shows the calculation of k_{eff} is relatively insensitive to this parameter.

(2) Two Region Core. In order to simplify the calculation without loss of accuracy, a two region core model, shown in Figure II-4, was investigated. The lower region contains moving and stationary fuel. The upper region contains stationary fuel and absorber plates. The lower region extends from the bottom of the fuel in the stationary elements to the top of the flux suppressor in the movable fuel assembly. The upper region extends from the top of the suppressor in the movable fuel assembly to the top of the fuel in the stationary elements. The boundary between the two regions defines the control rod height. This definition corresponds to the experimental definition of control rod height.

For the suppressor region, fuel region parameters are used with the exception that the fission cross section is proportional to the number of moving fuel elements in the suppressor region. The justification for this is that for the fast neutrons the composition of the core and suppressor is almost identical. In the thermal energy group it is only necessary for the region to be black to thermal neutrons, thus the elimination of the fission cross sections for that region yields a highly absorbing region. Also, since up to seven moving fuel rods can be in the suppressor region this model would permit taking into account the fission produced in these rods and their subsequent effect on reactivity during the core lifetime.

Using this model at the critical bank position $k_{\text{eff}} = 1.000$, the calculated reactivity is $k_{\text{eff}} = 1.002$.

Thus, the two region model is as accurate as the four region model and is the prescribed model for all remaining calculations.

c. SM-1 and PM-2A Critical Rod Configurations. Two symmetrical control rod arrangements were analyzed for the SM-1 and PM-2A cores. These cores have the same configuration, except for the number of control rods; the PM-2A has five rods while the SM-1 has seven.

For the SM-1 loading, using rods 1, 2, 3, and 4 as a bank with the remaining rods withdrawn from the core, the critical bank position is 2.831 inches.

FIGURE II-4. SCHEMATIC REPRESENTATION OF THE CORE USING TWO REGIONS

For the PM-2A loading with rods 1, 2, 3 and 4 fully inserted, the reactor is critical with the central rod at 20.134 inches.

For these two cases, the reactivities are $k_{\text{eff}} = 1.000$ at the critical position. The calculated reactivities are:

$$\text{SM-1 } k_{\text{eff}} = 0.994$$

$$\text{PM-2A } k_{\text{eff}} = 0.993$$

d. SM-1 and PM-2A Shutdown Margins. For the same four rod arrangement of the SM-1 and PM-2A described above, the shutdown margin was calculated for the case of four rods completely inserted in the SM-1 core and for the case of five rods completely inserted in the PM-2A.

For SM-1, a one-dimensional calculation was performed. The calculated shutdown margin is \$1.31, while the measured shutdown margin is \$1.20.

For PM-2A, the four and five rod configurations were calculated to evaluate the central control rod. Two dimensional calculations were performed with the four control rods completely inserted and with five control rods completely inserted. The calculated reactivity worth of the central rod, which is the shutdown margin of the core for this configuration, is \$9.46. The experimental value is \$9.10.

e. Total Reactivity of Five- and Seven-Rod Banks. The total reactivity change produced by insertion of five- and seven-rod banks into the core is calculated by determining the reactivity difference between a core consisting of thirty-seven fuel elements (thirty fixed fuel and seven moving fuel) and a core consisting of thirty stationary elements and seven poison rods. The comparison is:

	<u>Calculated</u>	<u>Measured</u>
seven-rod total reactivity	\$ 42.17	\$ 38.20
five-rod total reactivity	\$ 31.90	\$ 29.40

f. Temperature Coefficient of SM-1. To evaluate temperature coefficients, calculations were performed at 70°F and 140°F. The calculated reactivity change, which is the temperature defect is \$0.69. The temperature coefficient is calculated by relating the reactivity change to the density change of water with temperature, i. e., $\frac{dk}{dT} = F \frac{d\rho}{dT}$, where F is a constant determined by the temperature defect calculation and $\frac{d\rho}{dT}$ is the density change of water with temperature. A comparison of the calculated values with the measured values is shown in Figure II-5. The measured temperature defect between 70°F and 140°F is \$0.45.

FIGURE II-5. TEMPERATURE COEFFICIENT OF SM-1 CORE IN AC
A FUNCTION OF TEMPERATURE

The measured values are significantly lower than the calculated values. However, they are also much lower than the measured values on SM-1 Core I, which was a larger core. Also, from the scatter of experimental points, it would appear that these measurements have a large experimental error. Thus, it is believed that the calculations yield a better description of the temperature coefficient than do the experiments.

g. Relative Reactivity Worth of Stationary and Moving Fuel Elements.
The calculated increase in reactivity produced by replacing the moving fuel rods A and B with stationary fuel is \$0.66. The measured reactivity change is \$0.68.

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APPENDIX III

THERMAL-HYDRAULIC ANALYSES

This is the appendix to the thermal-hydraulic analyses discussed in Volume I of this report. Section A describes the methods of analysis and Section B presents the detailed results.

A. Method of Analysis

The analytical methods used in the thermal and hydraulic analyses are discussed below. Experimental results, where available from previous programs, are also presented and compared to the analytical methods. Individual topics discussed below are:

- (1) Stationary element pressure drop.
- (2) Movable assembly pressure drop.
- (3) Pressure drop in the lattice region.
- (4) Burnout correlations.
- (5) STDY-3 computer program.
- (6) Hot channel factors.
- (7) Hydraulic stability and outlet enthalpy determination.

1. Stationary Element Pressure Drop

The variation of static pressure drop across each stationary element was determined as a function of flow rate. This information was needed to establish the amount of the total core flow that each element received.

The pressure drop in the stationary fuel element is the sum of the individual pressure drops which occur along the fuel plates, in the inlet and outlet end boxes, and across the orifice. These three components of the total element pressure drop were treated individually and summed to determine the overall stationary element pressure drop.

a. Pressure Drop Along the Fuel Plates. The pressure drop which occurs along the fuel plates, as a result of the abrupt contraction as the flow enters the fuel plate region and the abrupt enlargement as the flow leaves the fuel plate region, was determined utilizing the STDY-3 code modified for use on the 7090.

A brief description of the modified code is presented in section (5), below. This code considers the pressure drop associated with the entrance and exit area changes, the frictional pressure drop for non-boiling and local boiling conditions, acceleration pressure drop, and the elevation pressure drop.

b. End Box Pressure Drop. The pressure drop which occurs in the inlet and outlet end boxes of the SM-1A type 3 stationary fuel assemblies was estimated based on calculations which agree closely with isothermal experimental data results reported in References 1 and 2. The basic analytical technique utilized estimated the pressure drop associated with each area change or frictional loss within the fuel assembly. The results of this analysis is shown in Table 1.

The data presented in Table 1 was based on fuel assembly entrance and exit pressure losses corresponding to those which would be encountered when the fuel assembly is inserted into the core, i. e., the actual plenum velocities were considered. The resultant total calculated pressure drop of 2.70 feet of working fluid is considerably less than the 3.91 feet reported in Reference 1. However, it is not clear from that report exactly how the plenums were mocked up or what pressure differential was measured. From the photographs and drawings in the report it appears that the element received flow from and discharged into a zero velocity plenum. The pressure drops entering and leaving the fuel element were recalculated assuming a zero velocity plenum and a total pressure drop of 3.89 feet resulted. This is in excellent agreement with the reported measured value leading to the conclusion that the analytical technique is accurate and that the tests reported in Reference 1 did in fact mock up the zero velocity plenum.

Data presented in Reference 2 was also utilized to verify the accuracy of isothermal calculational techniques. Although this report actually deals primarily with sizing of the orifices for the SM-1 core, data is presented concerning the pressure drop across a single unorificed fuel element. The single element calibration test in the report shows reasonably well that the SM-1 inlet plenum was mocked up but that the plenum discharged right into the room which acted as an infinite plenum. The analytical technique utilized to predict the pressure drop of the SM-1A type 3 fuel element was used to calculate the pressure drop across the elements from the experimental conditions reported in Reference 2. The calculated pressure drop was 4.03 inches of water compared to the reported measured value of 3.74 inches of water. Therefore, the analytical value was within 8 percent of the experimental value.

Since the total analytically calculated pressure drop agreed well with the available experimental data, and since there is relatively little question of the ability to accurately predict the frictional pressure drop along the fuel plates, it was concluded that the pressure losses associated with the area changes in the inlet and exit end boxes were correctly analyzed.

The analytical predictions of pressure losses in the end boxes were utilized in the flow distribution calculation as follows:

- (1) All the predicted pressure losses upstream of the fuel plates were summed to yield a single head loss at a flow rate of 100 gpm. This head loss was converted to pressure drop by multiplying by the inlet density; the pressure drop was then varied as the square of the flow rate.

Table 1Stationary Element End Box Pressure Drop

Isothermal Conditions
Flow Rate = 100 gpm

<u>Type of Loss</u>	<u>Enlargement or Contraction Coefficient</u>	<u>Minimum Area (in²)</u>	<u>Velocity (ft/sec)</u>	<u>Head Loss (ft)</u>
Entrance Contraction	0.23	3.98	8.03	0.230
Round to Square Transition	0.05	3.98	8.03	0.050
Exit Enlargement	0.025	6.25	5.12	0.010
Contraction Entering Fuel Plates	0.07	5.83	5.49	0.032
Enlargement Leaving Fuel Plates	0.07	5.83	5.49	0.032
Friction Drop Along Fuel Plates	2.75	5.83	5.49	1.286
Outlet Box Contraction and Transition Loss	0.26	3.14	10.18	0.418
Exit from Element	0.394	3.14	10.18	0.639
			Total	2.697

- (2) All the head losses downstream of the fuel plates were summed to yield a single head loss. However, the head loss associated with the abrupt enlargement at the exit of the element is not included in this sum since its loss is accounted for in the orifice calculation. The exit end box head loss was converted to pressure drop by multiplying by the average outlet density of each fuel assembly and the resultant pressure drop was again assumed to vary as the square of the flow rate.

c. Orifice Pressure Drop. The major contributor to the total pressure drop in a stationary fuel element is the orifice. The orifices, more than any other component of the core, determine the overall flow distribution. These, in the SM-1A type 3 core, are located at the outlet of the fuel assembly. Unfortunately, no experimental information for the pressure drop across the orifices in the SM-1A type 3 fuel elements was found. Reference 1 presents some information regarding the distribution of flow between the channels of a single fuel element with an orifice at the element outlet, but does not present any information regarding the pressure drop - flow relationship across the orifice.

Since no experimental data is available concerning outlet orifice pressure drop in the SM-1A type 3 core, data for the inlet orifices of the SM-1 core were utilized to determine the orifice pressure drop versus flow characteristics. The SM-1 orifice pressure drop data consist of data for a single orifice diameter (Reference 1), and considerable data based on air flow (Reference 2). The data in Reference 2 actually covered three distinct sets of orifices for the 38 stationary fuel elements. These data were utilized to define the orifice pressure drop versus flow relationship. The resultant correlation was then compared with the data for the single orifice diameter presented in Reference 1.

The data in Reference 2 presents the flow rate through each stationary assembly along with the total core pressure drop for each of the three sets of orifices tested. To accurately determine the pressure drop across the orifice, it was first necessary to accurately determine the pressure drop across the remainder of the stationary fuel element and subtract this from the total static pressure drop. An analysis similar to that discussed in section 1. b. was performed for the SM-1 type stationary fuel element at isothermal conditions. All the pressure losses prior to the contraction entering the fuel plates were lumped together with the orifice pressure drop. This was done since the pressure drop in the round to square transition end box would be quite dependent on the flow pattern leaving the orifice and, therefore, on the orifice diameter. The pressure drop, at isothermal conditions, beyond this inlet region was estimated to be 2.09 feet of head at a flow rate corresponding to 100 gpm. This corresponded to 60.4 percent of the pressure drop measured without an orifice in the calibration tests in which the outlet plenum was not accurately represented. Using this 60.4 percent value, the pressure drop across the orifice and in the inlet end box was estimated for each stationary fuel element for each of the three test runs performed.

Several techniques were utilized in attempting to correlate the orifice data. The method which yielded the best comparison with the experimental data used a standard orifice equation with a constant orifice coefficient, but with a term to account for the velocity of approach. This equation, taken from Reference 3, is:

$$W = C S_2 \sqrt{\frac{2 g \rho^2 \Delta H}{1 - \beta^4}}$$

where:

- ΔH = the orifice differential, feet of fluid
- W = weight of discharge, pounds per second
- C = coefficient of discharge, dimensionless
- g = local acceleration due to gravity, ft/sec²
- ρ = density, pounds per cubic foot
- S_2 = cross sectional area of the discharge opening square feet
- β^2 = ratio of cross section of constriction to that of upstream channel, dimensionless

The orifice pressure drop data was best correlated using constant value of $C = 0.76$ for all orifice diameters. The value of $1 - \beta^4$, however, was dependent on the orifice diameter as indicated by its definition.

A comparison of the predicted and experimental orifice pressure drops, using this technique, is shown in Figure III-1. As this figure shows, 67 percent of the experimental data was within + 10 percent of the predictions, 82 percent within + 15 percent, and 88 percent of the data was within + 20 percent of the predictions. Most of the data points which deviated markedly from the predicted values occurred with the smaller orifice diameters. If one considers only orifice diameters 1.5 inches or greater, then 83 percent of the data fell within + 10 percent of the predicted value, 91 percent of the data points fell within + 15 percent of the prediction, and 96 percent of the data points fell within + 20 percent of the prediction. This is an important point since the smallest orifice diameter utilized in the SM-1A Core 3 is 1.673 inches.

FIGURE III-1. COMPARISON OF PREDICTED AND EXPERIMENTAL HEAD LOSS

The applicability of this correlation technique to the SM-1A reactor core in which the orifices are at the outlet rather than the inlet of the fuel element was then considered. Since all the stationary fuel elements are orificed and are hydraulically in parallel, a slight systematic error in the orifice correlation would have virtually no effect on the prediction of the division of flow among the stationary elements. However, any inaccuracy in the orifice correlation does affect the prediction of flow rate in the lattice spacing and in the unorificed movable fuel elements.

It is anticipated that the utilization of the orifice correlation technique developed from the SM-1 core for the SM-1A core would yield acceptable results because the magnitude of static pressure recovery downstream of the orifice would be similar in both configurations. Uncertainty in the element flow rates were accounted for by the use of flow hot channel factors described in Section 6.

2. Movable Fuel - Control Assembly Pressure Drop

The geometry of the inlet and outlet flow paths of the SM-1A control element varies considerably from that of the SM-1 control elements. For example, there is a dual flow path in the entrance section of the SM-1A control elements, while the SM-1 control elements have a single flow path. Also, the holes in the outlet section of the SM-1A control element are of different diameter than those of the SM-1. Therefore, it was decided that a valid comparison of calculational technique could not be made with the experimental pressure drop data for SM-1 control elements reported in Reference 2.

The total pressure drop across the control elements was determined in a manner similar to the stationary element pressure drop, i. e., the total pressure drop across the element was found by summing the component pressure drops across the element for various flow rates.

Contributions to the control element pressure drop are made by the inlet, the fuel plates, the joint between the fuel and absorber sections, the absorber section and the outlet. These pressure drops, except for friction in the absorber section and fuel plates, are due to enlargement or contraction losses. However, the inlet and outlet pressure drops of the control elements are slightly more complex than in the case of the stationary elements. In the control elements, the fluid enters and leaves the element through dual flow paths. Further, the direction of flow entering and leaving the element is perpendicular to the flow path through the element. It was assumed that in making a right angled bend that a total velocity head loss occurred.

Pressure drop across the fuel plates in the control elements were determined from STDY-3.

Pressure drop in the absorber section of the control element is determined by the equation:

$$\Delta P = \rho f \frac{L v^2}{D (2g)}$$

where: ΔP = pressure drop, lb/ft²
D = equivalent diameter, feet
g = acceleration due to gravity, ft/sec²
V = fluid velocity, ft/sec
f = friction factor

and f is determined by the equation $f = \frac{0.184}{Re^{0.2}}$

in which Re is the Reynolds number (dimensionless).

The coefficients, areas, velocities and head losses associated with the components of pressure drop excluding plate ΔP are shown in Table 2 for a flow rate of 100 gpm.

Table 2Control Element Pressure Drop

Flow = 100 gpm

<u>Type of Loss</u>	<u>Enlargement or Contraction Loss</u>	<u>Minimum Area</u> in ²	<u>V</u> ft/sec	<u>Head Loss</u> ft.
Dual Inlet				
a)	1.5	1.78	5.82	
b)	0.5	6.28	5.10	
c)	$\Delta h = \frac{1.5 Va^2 + 0.5 Vb^2}{2g}$			0.991
Contraction Entering Fuel Plates	0.07	5.37	5.96	0.039
Enlargement Leaving Fuel Plates	0.07	5.37	5.96	0.039
Contraction and Enlargement at Fuel Assembly Handle				
1) Contraction to handle	0.04	4.88	6.55	0.027
2) Contraction to pin	0.025	4.27	7.49	0.022
3) Enlargement to absorber section	0.030	4.27	7.49	0.026
Friction Drop Along Absorber	0.203	5.27	6.09	0.117
Dual Exit Loss				
a)	1.5	3.19		
b)	0.5	5.29		
c)	$\Delta h = \frac{1.5 Va^2 + 0.5 Vb^2}{2g}$			0.487
Total				1.748

3. Pressure Drop in the Lattice Region

The determination of the fraction of the total coolant flow that is exterior to the fuel elements was carried out by the same techniques used for the movable and stationary elements, i. e., calculating the total pressure drop through the core as a function of flow rate. The resulting data were plotted. This plot, in combination with similar plots for each stationary and movable fuel element, was then employed to determine the pressure drop through the core for the specified coolant flow.

The lattice flow, i. e., coolant flowing exterior to the fuel elements, pressure drop included four components: friction, orifice, acceleration, and elevation. The procedures employed in the determination of each are described below.

a. Friction Pressure Drop. The pressure drop due to friction at the fuel element outer surfaces was computed by a standard technique. The flow area is 40 in² for all the spaces within the coolant flow area which are not occupied by fuel elements. The perimeter of the lattice flow area is 591 inches -- 83 inches at the core periphery, 430 inches around the stationary fuel elements, and 78 inches around the control rod baskets. The hydraulic radius for lattice flow is 0.0676 inch or 0.00563 foot.

For each flow rate, a Reynolds number was calculated and a friction factor derived from the relationship:

$$f = \frac{0.184}{Re^{0.2}}$$

where: f = friction factor
 Re = Reynolds number

The pressure drop was determined from the relationship:

$$\Delta h = f \frac{L}{De} \frac{V^2}{2g}$$

where: Δh = pressure drop, feet
 L = flow path length, feet = 2.5 feet
 V = velocity, ft/sec
 De = equivalent diameter, feet
 g = Acceleration of gravity, ft/sec²

The calculated values are presented in Table 3, along with the other components of lattice flow pressure drop.

Table 3

Lattice Flow Head Loss and Pressure Drop

<u>Flow Rate</u> <u>(gpm)</u>	<u>Friction</u> <u>(ft)</u>	<u>Orifice</u> <u>(ft)</u>	<u>Acceleration</u> <u>(ft)</u>	<u>Elevation</u> <u>(ft)</u>	<u>Total</u> <u>h (ft)</u>	<u>P (psi)</u>
600	0.801	0.752	0.120	2.75	4.42	1.59
700	1.070	1.050	0.143	2.75	5.02	1.80
800	1.356	1.380	0.165	2.75	5.65	2.03
900	1.677	1.733	0.172	2.75	6.33	2.27
1000	2.00	2.140	0.181	2.75	7.07	2.54
1100	2.37	2.590	0.194	2.75	7.90	2.84
1200	2.78	3.07	0.204	2.75	8.80	3.17
1300	3.20	3.60	0.233	2.75	9.78	3.52
1400	3.66	4.20	0.257	2.75	10.87	3.91
1500	4.15	4.80	0.270	2.75	11.97	4.31

b. Orifice Pressure Drop. The core plates at the top of the core contain 60 orifices for the lattice flow. These orifices, located directly above the corners of the fuel elements range in diameter from 0.375 inch to 1 inch, depending on the volume of flow desired. The smallest are located at the periphery of the core and the large orifices are located nearer the center.

The calculations of the pressure drop through these orifices was not straightforward as in the previous case. A maximum and minimum pressure drop were calculated and a conservatively weighted average taken to arrive at a best estimate. The minimum pressure drop was calculated by assuming that all the lattice flow area and all the orifice flow area were combined into a single channel. The total orifice area is 28.7 in². The orifice coefficient was the same as that employed in the fuel element calculations (0.76). However, it is obvious that the actual geometry will result in a greater pressure drop because of the large number of individual contractions and the cruciform, rather than circular, flow path between the fuel elements. The maximum pressure drop was calculated by assuming that the contraction through the orifice was from the width of the cruciform flow path to the diameter of an average size orifice (0.875 inch). This assumption is illustrated below, with the assumed flow path diameters inscribed with dotted lines.

In order to minimize the fuel element flow rate, the average ΔP was weighted toward the minimum value. One-fourth of the difference between minimum and maximum values was added to the minimum in order to obtain what is considered the most probable value. This most probable value results in a flow rate through the fuel elements which is 0.635 percent above the minimum possible and 1.69 percent below the maximum possible. While the variation in orifice pressure drop between maximum and minimum is rather large, the contribution of the other head losses to the total tends to damp out this difference. The head loss across the orifice was calculated as follows:

$$\Delta h = \frac{Q^2 (1 - \beta^4)}{K^2 A^2 2g}$$

where: Δh = head loss, feet of fluid
 Q = volumetric flow rate, ft³/sec
 β = ratio of smaller flow diameter to larger
 K = orifice coefficient
 A = orifice area, ft²
 g = gravitational acceleration, ft/sec²

c. Acceleration Pressure Drop. Acceleration losses generally result from two sources: the increase in velocity resulting from the decrease in fluid density as it is heated and the variation in cross section of the flow path. In this analysis, contraction at the entrance to the fuel element was assumed to be from the fuel element cross section to the inlet cross section. The cross section of the lattice flow remains essentially constant until the orifice at the outlet. These assumptions are not exactly true, but since the calculations are simpler and whatever error is introduced is in the direction of reducing coolant flow through the fuel elements, this procedure is permissible.

The only acceleration head loss considered was due to coolant heating in the lattices. An average heat flux of one-half that for the fuel elements was assumed since only half of the stationary fuel element outer surface is heat generating. The density changes were small, ranging from about 0.9 lb/ft³ at 600 gpm to about 0.3 lb/ft³ at 1500 gpm. As is seen in Table 3, the acceleration pressure drop contributes little to the total. The head loss due to acceleration was computed by:

$$\Delta h = \frac{2 G^2}{(\rho_1 + \rho_2)g} \left(\frac{1}{\rho_2} - \frac{1}{\rho_1} \right)$$

where: Δh = pressure drop, feet of fluid
 G = mass flow rate, lb/hr - ft²
 ρ_1 = inlet density, lb/ft³
 ρ_2 = outlet density, lb/ft³
 g = gravitational constant, ft/hr²

d. Elevation Pressure Drop. The elevation loss is simply the difference due to the change in elevation of the upflowing water in the core. The path length for elevation has been taken as 33 inches throughout the core analysis. The head loss due to elevation is simply 33 inches or 2.75 feet.

This is greater than the 2.5 feet employed for friction pressure drop since the dimensional analysis employed for the friction loss applies only to 30 inches of the 33 inch total height.

4. Burnout Correlations

Various subcooled burnout correlations were evaluated for use in the SM-1A Core 3 design analysis during the preliminary phase of this program. The results of this review were presented in Reference 4 and are summarized here.

Correlations were evaluated based on their:

- (1) Accuracy.
- (2) Applicability to the SM-1A Core 3 geometry and conditions.
- (3) Ease of use in design type calculations.

There is apparently little current effort being devoted to the generation and correlation of subcooled boiling burnout data. Efforts now appear to be devoted to burnout in the bulk boiling regime and/or the derivation of a general correlation applicable to many fluids and a wide range of pressures. Therefore, only one recent correlation was reviewed.

In the course of this investigation, three burnout correlations pertaining to subcooled water were evaluated:

- (1) The 1958 Bettis Correlation (Reference 5).
- (2) Tong's (1963 Westinghouse) Correlation (Reference 6).
- (3) The Menegus' Correlation (1959) (Reference 7).

Also a report (Reference 8) dealing with the prediction of burnout heat flux in rectangular channels having a non-uniform heat generation rate in the transverse direction was reviewed.

a. Bettis Correlation. A large portion of the subcooled burnout data currently available was generated at the Bettis Laboratory and published and correlated in 1958. This data is generally applicable for the SM-1 Core III design since it was obtained using rectangular test sections. However, virtually all subcooled data was obtained at a pressure of 2,000 psia. In addition to the subcooled data, much data in the quality range was also generated. Some of this data in the quality regime was at pressures below 2,000 psia.

This rectangular test section data and other data available at that time was correlated by a single simple expression applicable to both the subcooled and boiling regimes. The burnout heat flux predicted by this correlation for the SM Core III rectangular fuel plate geometry is shown in Figure III-2 as a function of subcooling.

The authors of Reference 5 also suggest an effect of pressure on burnout heat flux. They indicate that, at constant enthalpy, the burnout heat flux increases as the pressure is reduced to 1200 psia and then decreases as pressure is further reduced. However, they note that the data to substantiate such a trend is quite limited.

b. Tong's Correlation. Recent empirical burnout correlations, both for the bulk boiling and subcooled region, are presented in Reference 6. The authors claimed to have utilized all published burnout data, which meets certain heat balance accuracy requirements, in deriving the new correlations. A large portion of the subcooled burnout data was the rectangular channel data reported in Reference 5.

The results of the predictions of the subcooled correlation for the SM-1 geometry is also presented in Figure III-2 for a pressure of 2000 psia. Figure III-3 shows the burnout heat flux predicted for a pressure of 1100 psia.

The use of Tong's Correlation is rather inconvenient, even for a uniformly heated channel. The subcooled boiling correlation is a function of local subcooling, inlet enthalpy and channel length. An iterative procedure is required to use the correlation. This procedure involves matching the inlet enthalpy, the channel length, and the average heat flux in the channel with the local conditions at the point of interest by using a heat balance. For the case of non-uniform axial heating, such as the situation in a reactor, a numerical integration of the heat flux is required.

The relationship between inlet enthalpy and channel length was investigated for a pressure of 2000 psia and a local subcooling of 100°F. It was found that the predicted burnout heat flux for a long test section with low inlet enthalpy and a short test section with high inlet enthalpy differed by only 6 percent.

c. Menegus Correlation. Reference 7 presents a basically empirical correlation of burnout heat flux in the subcooled regime. The correlation intentionally utilized data over a wide range of pressures to determine the effect of pressure on burnout heat flux.

The predictions of this correlation for the SM-1 geometry as a function of subcooling is shown in Figure III-2, for a pressure of 2000 psia. Agreement with the other correlations is quite good despite the fact that the Menegus correlation used none of the data utilized in deriving the other correlations and was obtained primarily with circular rather than with rectangular test sections. Although the Menegus correlation predicts a somewhat higher heat flux than the Tong correlation, the difference between the predicted burnout heat flux for the two correlations are well within the claimed accuracy of either correlation. A comparison of Tong's and Menegus' Correlations at 1100 psia is shown in Figure III-3.

FIGURE III-2. BURNOUT HEAT FLUX

FIGURE III-3. BURNOUT HEAT FLUX

d. Non-Uniform Heat Flux in the Transverse Direction. The power distribution data on the SM-1 Core III indicates that there is an appreciable variation of heat flux in the transverse direction of a single fuel plate in some fuel elements. Therefore, the question of predicting burnout heat flux for this situation arose. Reference 8 is an experimental investigation of just such a case. A rectangular, electrically heated test section was designed to yield a peak heat flux in the corners of the channel. The data in Reference 8 shows that the burnout heat flux can be predicted quite accurately utilizing the correlation presented in Reference 5, if average local enthalpy in the channel is utilized and compared to the actual heat flux at the burnout point. That is, it appears that for turbulent flow conditions, at velocities very similar to those of SM-1 Core III, that adequate mixing occurs to warrant the use of bulk properties.

5. Modified STDY-3 Computer Program

The original STDY-3 code is described in Reference 9. Included in that document is a list of equations, a description of the iteration performed by the code, and sample problem input and output. None of these characteristics (except the DNB equations) were changed.

The original STDY-3 code was written for the IBM 704 digital computer. Because this machine was limited as to speed and storage capability, the program was rewritten for the more advanced 7090 computer. With the increased speed of operation and higher cost per unit of operating time characteristic of the recently developed digital computers, the use of on-line printing, program stops, reading input cards directly into the computer, and other time consuming operations have been virtually eliminated. The use of sense switches is less widespread. Further, increased internal storage capacity was reduced requirements for storage on drums and tape.

In order to adapt STDY-3 for the 7090 computer, several general modifications were made. All READ Input statements were replaced by READ Input Tape 5. The number of output tapes were reduced from three to one. All on-line printing and STOP statements were eliminated as was drum data storage. Sense switch and sense light options were also eliminated. Another modification was necessitated by the inability of the 7090 to correctly maintain the several indices involved in the major DO loop of the main program. Changing the DO instructions to a more basic operation in which the index was treated as a separate storage location eliminated this problem. In order to prevent an unnecessary stop by the computer, an EXIT subroutine was included to start the next program immediately after completion of the last problem.

In addition to the modifications required by the change in computer, two other groups of changes were made: Changes made to improve the accuracy of the code and changes made for the convenience of the operators. The major change in the first group was employment of two recently developed burnout correlations, by Tong (Reference 6) and Menegus (Reference 7). Tong's correlation is utilized in the bulk boiling region and Menegus' correlation in the subcooled region.

The second group of changes generally concerned the elimination of various options which were expected to be of limited usefulness. These included options for the plotting of results directly on the printout and the printout of the hot channel search problem results. Added to the program was a procedure for automatic normalization of the axial flux weighting factors.

A listing of STDY-3 as modified for this study is presented in Table 4. The original symbols presented in Reference 9 are retained in the modified code.

TABLE 4

STOY STOY3 SOURCE PROGRAM MAIN

```

    DIMENSION G1(5),G2(5),PO(5),TIN(10),FFAVE(6),FAVE(6,5),FPZ(30,6),
    1ST(6),HAVE(30),X(30),TAVE(30),A(6),B(6),FFLOC(6),STL(6),TS(30),
    2TM(30),BOR(30),SIGO(6),SIGI(6),EKC(6),EKE(6),CK1(4),CK2(4),CKP1(4)
    3,CKP2(4),GX(21),PD(21),U2(2),XC(6),XM(6),HIN(31),DPT(6),ELEV(6),
    4FRIT(6),ACC(6),GY(10),PDY(10),HDT(6),HOS(6,10),AA(30,6),BB(30,6),
    5FAC(30,6),GXX(21),PGMM(4),STOREA(30),STOREB(30),STOREC(30),STORED(
    630),STOREE(30)
320 READ INPUT TAPE 5,323,NPROB
323 FORMAT(I2)
    IF (NPROB)321,321,322
321 CALL EXIT
322 READ INPUT TAPE 5,108
108 FORMAT(72H
1
    WRITE OUTPUT TAPE 6,108
    LPG=1
    WRITE OUTPUT TAPE 6,1,LPG
    COMMON RGA0L,RGA1L,RGA2L,RGA3L,RGA0H,RGA1H,RGA2H,RGA3H,TFPH1,
    1TFPH2,TFPH3,TFPH4,HFPT1,HFPT2,HFPT3,HFPT4,TEM4,TEM5,TEM7,TEM8,
    2PMN42,CTJL,HSAT,TSAT,ROSAT,VF,HFG,VFG,VG,ROG,P,FPZ,FFLOC,FFAVE,
    3FAVE,EKE,EKC,CKP2,CKP1,CK2,CK1,B,A,PO,SIGI,SIGO,STL,ST,TIN,U2,XC,
    4XM,FDT,FA,CKM,CKC,BOCN3,BOCN2,BOCN1,BCD,ABC,PD1,PD2,SHC,SL,G2,G1,
    5AA,8B,FAC,CONV,FBO,RO,TAVG,Q,DE,HINLET,EL,DH,HOUT,FLOC,DZ
1 FORMAT(115H+
1
    WRITE OUTPUT TAPE 6,109
109 FORMAT(54H0 -
    WRITE OUTPUT TAPE 6,283
    CALL INPUT (ITV,IPV,IFV,IIV,INL,INK,JIG,JOINT,K,KEN1,KEN2,
    1KICK,MEQ,MIY,MIZ,NC)
    GO TO (320,912),INK
912 WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,244
244 FORMAT(45H0                      AVERAGE CHANNEL VALUES (FIRST PASS))
    D=K
    DZ=SL/D
    IG=1
    I=1
    K1=1
270 I1=1
271 IT=1
272 L=1
232 NS=1
    N=1
    ICT=0
    G=G1(I1)
    LLL=1
    ASSIGN 180 TO KKK
    MC=1
    NG1=1
    NE=1

```

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**** STORED IN MEMORY ****)

TABLE 4 (Cont.)

STDY	STDY3 SOURCE PROGRAM * MAIN
	XIN=0.
	RGIN=0.
	P=PO(I)
702	CONTINUE
264	IF(P-1850.)8,9,9
8	M2=2
	MPRES=2
	HCO=.023
728	IF(MEQ-2)3,107,117
117	IF(MEQ-4)120,3,107
120	M2=3
	GO TO 3
9	IF(MEQ-2)235,235,236
235	M2=1
	GO TO 237
236	M2=4
237	MPRES=1
107	HCO=.03
3	CALL CURVE
	TINL=TIN(IT)
289	IF(TINL-1.)291,292,292
291	XIN=TINL
	HIN(1)=HSAT+XIN*HFG
	MRET=4
	GO TO 819
292	HIN(1)=HFPT1+TINL*(HFPT2+TINL*(HFPT3+TINL*HFPT4))
	ROEIN=63.8+TINL*(-.01781+TINL*(1.132E-5+TINL*(-6.786E-8)))
	SPT2=1./ROEIN
166	IF(N-1)167,167,168
167	HINLET=HIN(1)
168	Q=G/1.E6
	ASSIGN 69 TO M7
7	SUM1=0.
	SUM2=0.
	SUM3=0.
	SUM4=0.
	THSTL=0.
	F2LO=0.
	VB=0.
	RG=0.
	XOUT=0.
	RGOUT=0.
	FIOST=0.
	ASSIGN 22 TO M3
	ASSIGN 59 TO M5
	IBBCNT=0
	LBCNT=0
	PHI=0.
	DO 999 J=1,K
999	PHI=FPZ(J,N)+PHI
	FACTOR=D/PHI
	DO 998 J=1,K
998	FPZ(J,N)=FPZ(J,N)*FACTOR
	DO 230 J=1,K
230	X(J)=0.
290	J = 1

STDY

STDY3

SOURCE PROGRAM MAIN

```

401 THETA=FFAVE(N)*FAVE(N,L)*FPZ(J,N)
    DH=(THETA*2.*DZ)/(G*ST(N))
    HOUT=HIN(J)+DH
    HAVE(J)=(HIN(J)+HOUT)/2.
    HIN(J+1)=HOUT
734 IF(HAVE(J)-HSAT)20,21,21
    21 GO TO M3,(22,23)
    22 ASSIGN 23 TO M3
    ASSIGN 84 TO M1
    USAT=1./(8.506E-3*TSAT-.3643)
    FIOST=A(N)/(((G*ST(N))/(6.*USAT))*B(N))
    IBBCNT=J
    23 X(J)=(HAVE(J)-HSAT)/HFG
    GO TO (265,266),MPRES
265 FAM=1.+(7.*X(J)**.75)/(Q**(.1+X(J)))
266 TAVE(J)=TSAT
199 GO TO (735,200),KEN1
200 FIOST=AA(J,N)/(((G*ST(N))/(6.*USAT))*BB(J,N))
735 IF(X(J)-.2)24,25,25
    24 IF(X(J)-.02)26,27,27
    26 GO TO (37,28),MPRES
    28 F2LO=1.
736 IF(X(J)-.01)30,31,31
    30 VB=VF+X(J)*VFG
    GO TO 738
    27 GO TO (33,32),MPRES
    32 F2LO=(X(J)**TEM5)/TEM4
737 IF(X(J)-.1)31,35,35
    31 RG=RG AOL+X(J)*(RGAIL+X(J)*(RGA2L+X(J)*RGA3L))
    GO TO 47
    37 F2LO=FAM*ABC
    GO TO 41
    33 FMN=(X(J)**TEM5)/TEM4
    38 FMN2T=(X(J)**.48)/.14252
    40 F2LO=(FMN/FMN2T)*FAM*ABC
    41 VB=VF+X(J)*VFG
    IF(X(J)-.4)350,350,351
350 IF(G-7.0E5)242,250,250
351 IF(G-7.0E5)352,241,241
352 F2LO=(F2LO*6.8007/FAM)*(FMN/4.75)*(1.+.93*(.7-Q))
    GO TO 250
241 F2LO=F2LO*((1.+(3.521)/(Q**(.1+X(J))))/FAM)*(FMN/4.75)
    GO TO 250
242 F2LO=F2LO*((1.+(7.*X(J)**.75)/.607)/FAM)*(1.+0.93*(.7-Q))
    GO TO 250
    25 GO TO (43,42),MPRES
    42 IF(1.-X(J))45,44,44
    44 F2LO=(X(J)**TEM8)/TEM7
706 IF(.9-X(J))30,35,35
    43 IF(X(J)-1.0)48,48,45
    48 FMN=(X(J)**TEM8)/TEM7
    FMN2T=(X(J)**.61)/.11526
    GO TO 40
    35 RG=RG AOL+X(J)*(RGAIL+X(J)*(RGA2H+X(J)*RGA3H))
    47 VB=VF*VG/(VF*RG+VG*(1.-RG))
738 IF(G-7.E5)248,248,249

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STDY STDY3 SOURCE PROGRAM * MAIN

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248 F2LO=F2LO*(1.36+0.0005*P+.1*(G/1.E6)-.000714*P*(G/1.E6))*ABC
    GO TO 250
249 F2LO=F2LO*(1.26-.0004*P+.119*(1.E6/G)+.00028*P*(1.E6/G))*ABC
250 IF(F2LO-ABC)251,49,49
251 F2LO=ABC
    49 SUM4=SUM4+(F2LO*DZ*FI0ST)
        SUM2=SUM2+(DZ/VB)
        THJL=TSAT+(CTJL*(THETA/1.E+6)**.25)-TAVE(J)
        ASSIGN 84 TO M1
        GO TO 130
20  TAVE(J)=TFPH1+HAVE(J)*(TFPH2+HAVE(J)*(TFPH3+HAVE(J)*TFPH4))
    IF(TSAT-TAVE(J))76,75,75
76  TAVE(J)=TSAT
75  U=1./(8.506E-3*TAVE(J)-.3643)
    RO=63.8+TAVE(J)*(-.01781+TAVE(J)*(1.132E-5+TAVE(J)*(-6.786E-8)))
    THJL=TSAT+(CTJL*(THETA/1.E+6)**.25)-TAVE(J)
    GO TO (203,201),KEN1
201 FISO=AA(J,N)/(((G*ST(N))/(6.*U))*BR(J,N))
    GO TO 739
203 FISO=A(N)/(((G*ST(N))/(6.*U))*B(N))
739 IF(HAVE(J)-600.)50,51,51
    50 FC=20281.7+HAVE(J)*(135.5+HAVE(J)*(-.10361))
    GO TO 55
    51 FC=25391.+64.78*HAVE(J)
    55 GO TO (204,205),KEN2
204 THF=THETA/((HCO*FC*(G/1.E+6)**.8)/(ST(N)/6.)**.2)
    GO TO 740
205 THF=THETA/(((HCO*FC*(G/1.E+6)**.8)/(ST(N)/6.)**.2)*FAC(J,N))
740 IF(THF-THJL)56,57,57
    57 ASSIGN 84 TO M1
        THST=THJL
750 GO TO M5,(58,59)
    59 LBCNT=J
        ASSIGN 58 TO M5
    58 GO TO (60,77),MPRES
    60 THSTS=THJL
    62 IF(THSTS-60.)63,64,64
    64 THSTS=60.
    63 PSY=1.-(THSTS/THF)
        SC1=PD1*(1.-.0025*THSTS)
741 IF(SC1-1.)66,66,65
    65 SC1=1.
    66 IF(PSY)1014,67,68
    68 GO TO M7,(69,70)
    69 FIPC=PD2*((3500.-P)/1500.)*(1.E+6/G)**.6667
        ASSIGN 70 TO M7
    70 FFISO=SC1*(1.+(FIPC*PSY))
        GO TO 71
    67 FFISO=SC1
        GO TO 71
    56 ASSIGN 82 TO M1
        ASSIGN 59 TO M5
        THST=THF
751 GO TO (73,72),MPRES
    72 IF(MEQ-2)115,116,115
    115 FFISO=1.

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TABLE 4 (Cont.)

STDY	STDY3	SOURCE PROGRAM * MAIN
		GO TO 71
116		FFISO=1.-.001304*THF
		GO TO 71
73		THSTS=THF
		GO TO 62
77		TLBST=THJL+TAVE(J)-THF
78		IF(TAVE(J)-TLBST)79,79,80
79		FFISO=1.
		GO TO 71
80		FFISO=1.+((TAVE(J)-TLBST)/(TSAT-TLBST))*(PMN42-1.)
71		SUM1=SUM1+(RO*DZ)
		SUM3=SUM3+(FFISO/RO)*FISO*DZ
130		IF(SENSE SWITCH 3)234,133
133		GO TO (399,234),NE
234		FLOC=FFLOC(N)*FAVE(N,L)*FPZ(J,N)*FFAVE(N)
		GO TO M1,(82,84)
82		GO TO (206,207),KEN2
206		THSTL=FLOC/((BCD*FC*(G/1.E+6)**.8)/(STL(N)/6.)**.2)
		GO TO 742
207		THSTL=FLOC/(((BCD*FC*(G/1.E+6)**.8)/(STL(N)/6.)**.2)*FAC(J,N))
742		IF(THSTL-THJL)96,96,84
84		THSTL=THJL
96		TS(J)=TAVE(J)+THSTL
		TM(J)=TS(J)+(FLOC/12.)*(XC(N)/CKC+XM(N)/(4.*CKM))
		EL=J
		DE = 2.*ST(N)
		TAVG = TAVE(J)
		IF(TSAT-TAVG)4000,4000,3000
3000		SUBCO=TSAT-TAVG
		IF(P-POLD)1500,2222,1500
1500		ALPHA=((3200.-P)/3200.)*(.2036*P/(P+19.63))-.04126+ (.08341-((P-165
		10.)/5814.))**.2.))**.5
		BETA0=((P+811.)/(P+38.4))*4.48/(3200.-P)+1.814E-6*(P/100.)*(3200.-
		1P)/3200.
		BETA1=(1.234E-4+1.098E-5*P/100.)*(3200.-P)/3200.
		GAM0=(0.01004*P/(P+118.54)+1.08E-5)*(3200.-P)/3200.+1.814E-8*P
		GAM1=1.176E-6*P/100.+2.016E-4+2.576E-4*100./P
		DELTA=3.888E-7*P/100.+1.037E-6+1.8816E-3*100./P
2222		VEE=Q*1.E6/(3.6E3*RO)
		FBO=3.413E6*ALPHA*(1.+.555*BETA0*SUBCO+.555*BETA1*SUBCO*12./DE)*(1
		1.+GAM0*VEE+GAM1*VEE*12./DE)/(1.+12.*DELTA/DE)
		POLD=P
		GO TO 90
4000		DHDNB = 0.529*(HSAT-HINLET) + HFG*(EXPF(-1.5*Q)*(0.825 + 2.36*EXP
		1F(-17.*DE)) -0.41*EXPF(-0.0048*(EL-.5)*DZ/DE)-1.12*VF/VG+.548)
		FBO=DHDNB/(HOUT-HINLET)*FLOC
90		BOR(J)=FBO/FLOC
		IF(KICK)1031,399,920
920		STOREA(J)=FFISO
		STOREB(J)=F2LO
		STOREC(J)=RO
		STORED(J)=V8
		STOREE(J)=FISO
399		IF(J-K)400,132,132
400		J = J+1
		GO TO 401

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STDY          STDY3  SOURCE PROGRAM • MAIN

132 CONTINUE
   HOS(N,LLL)=HOUT
745 IF(HOUT-HSAT)141,160,160
141 TOUT=TFPH1+HOUT*(TFPH2+HOUT*(TFPH3+HOUT*TFPH4))
   ROUT=63.8+TOUT*(-.01781+TOUT*(1.132E-5+TOUT*(-6.786E-8)))
   SPT1=1./ROUT
   GO TO 144
160 XOUT=(HOUT-HSAT)/HFG
   SPT1=VF+XOUT*VFG
   ROUT=1./SPT1
   GO TO (144,746),MPRES
746 IF(XOUT-.9)135,135,144
135 IF(XOUT-.1)137,136,136
137 IF(XOUT-.01)144,138,138
136 RGOUT=RGAOH+XOUT*(RGA1H+XOUT*(RGA2H+XOUT*RGA3H))
   GO TO 143
138 RGOUT=RG AOL+XOUT*(RGA1L+XOUT*(RGA2L+XOUT*RGA3L))
143 SPT1=(VF*(1.-XOUT)**2)/(1.-RGOUT)+(VG*XOUT**2)/RGOUT
144 CON=G**2/6.0092928E10
   ACC1=(SPT1-SPT2)*CON
   ACC2=((SIGO(N)**2-1.)/(2.*ROUT)-(SIGI(N)**2-1.)/(2.*ROEIN))*CON
   ACC(N)=ACC1+ACC2
   ELEV(N)=(SUM1+SUM2)/1728.
   FRIT(N)=CON/(4.*ST(N))*(SUM3+(SUM4*VF))
   ENXT=CON/2.*(EKC(N)/ROEIN+EKE(N)/ROUT)
   DPT(N)=ACC(N)+ELEV(N)+FRIT(N)+ENXT
   HDT(N)=HOS(N,LLL)-HIN(1)
719 CONTINUE
684 GO TO (257,650,651),NJP
650 GO TO (651,652,653,654,655,656,657,658,659,660,661,662),JOINT
651 NUM=2
   GO TO 688
653 NB=4
   GO TO 652
654 NJP=3
   NUM=3
   GO TO 688
655 NB=1
   GO TO 652
656 NB=2
   GO TO 652
658 NB=7
657 NJP=1
   NUM=4
   GO TO 688
659 NB=8
   GO TO 652
660 NB=3
   GO TO 652
661 NB=5
   GO TO 652
688 DPHC=0.
   GO TO 685
662 NB=6
652 NJP=1
257 GO TO (145,312,184,186),NG1

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STDY

STDY3 SOURCE PROGRAM * MAIN

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145 DPHC=(CK1(NS)*CK2(NS))*DPT(N)+(1.-(CK1(NS)*CK2(NS)))*ELEV(N)+(CK1(
    1NS)*CKP1(NS)*CKP2(NS)+CK2(NS)*CKP1(NS)-(CK1(NS)*CK2(NS))*(1.+CKP1(
    2NS)))*ACC(N)
    DPHCU=DPHC*(1.+CONV)
    DPHCL=DPHC*(1.-CONV)
    NUM=1
685 WRITE OUTPUT TAPE 6,252,P,FAVE(N,L),ST(N),SL,TINL,HIN(1)
252 FORMAT(10HOPRESSURE=F5.0,31H      AVERAGE CHANNEL- HEAT FLUX=1PE10.4
    1,12H, THICKNESS=OPF5.3,9H, LENGTH=F5.2,14H, INLET TEMP.=F5.1,4H, H
    2=F5.1)
    WRITE OUTPUT TAPE 6,6
    6 FORMAT(1H0,36H      J      T AVE      H AVE      X)
    WRITE OUTPUT TAPE 6,114,(J      ,TAVE(J),HAVE(J),X(J),J=1,K)
114 FORMAT(1H ,I6,F11.2,F12.2,F11.3)
    WRITE OUTPUT TAPE 6,125,N,G,HDT(N),ACC1,ACC2,ELEV(N),FRIT(N),ENXT,
    1DPT(N),DPHC,LBCNT,IBBCNT,TSAT,HSAT
125 FORMAT(16HCHANNEL NUMBER=I2,32H      CHANNEL MASS VELOCITY(G)=1P
    1E9.3,29H      ENTHALPY RISE(BTU/LB)=OPF6.2/20H      ACCELERATION DP
    2=F6.3,4H(1),F6.3,19H(2)      ELEVATION DP=F6.3,15H      FRICTION DP=F6.3
    3,22H      ENTRANCE + EXIT DP=F6.3/29H CHANNEL TOTAL PRESSURE DROP=F7.
    43,51H      AVERAGE CHANNEL PRESSURE DRCP X PLENUM FACTOR=F7.3/24H L
    50CAL BOILING STARTS J=I2,27H      BULK BOILING STARTS J=I2/7H T SAT
    6=F7.2,11H      H SAT=F7.2)
753 GO TO (686,193,122,121),NUM
686 ASSIGN 860 TO MQUAL
687 GO TO (851,850),M
850 GO TO (852,853),INL
852 HIN(1)=U2(1)*(HINLET+FA*HDT(1))+(1.-U2(1))*(HINLET+FDOT*HDT(1))
    GO TO 854
853 HIN(1)=U2(1)*(HINLET+FA*HDT(1))+(1.-U2(1))*HQS(2,1)
854 IF(HIN(1)-HSAT)857,858,858
858 MRET=1
    GO TO 819
857 TINL=TFPH1+HIN(1)*(TFPH2+HIN(1)*(TFPH3+HIN(1)*TFPH4))
    ROEIN=63.8+TINL*(-.01781+TINL*(1.132E-5+TINL*(-6.786E-8)))
    SPT2=1./ROEIN
851 NN=1
    N=N+1
    G=G*SHC
    GFR=G/20.
    WRITE OUTPUT TAPE 6,281 ,
281 FORMAT(51H0      MASS VELOCITY VERSUS PRESSURE DROP TABLE)
    WRITE OUTPUT TAPE 6,282,N
282 FORMAT(48H      1ST HOT CHANNEL      CHANNEL NUMBER=I2)
    WRITE OUTPUT TAPE 6,283
283 FORMAT(1H )
    NG1=2
    IF(N-2)168,286,168
286 IF(TIN(6))168,168,287
287 TINL=TIN(IT+5)
    GO TO 289
312 WRITE OUTPUT TAPE 6,246,G,DPT(N)
246 FORMAT(22H      G=1PE9.3,7H      DP=OPF7.3)
    GX(NN)=G
    PD(NN)=DPT(N)
    NN=NN+1

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STDY

STDY3 SOURCE PROGRAM * MAIN

G=G-GFR
747 IF(G)860,860,168
45 GO TO MQUAL,(860,53)
53 WRITE OUTPUT TAPE 6,285
285 FORMAT(54H0**NORMAL CHANNEL QUALITY EXCEEDS RANGE OF EQUATIONS**)
GO TO 278
860 ASSIGN 178 TO LN
ASSIGN 182 TO LM
ASSIGN 866 TO IX
NG1=3
754 GO TO (317,316),MIY
316 NX=NN
NN=1
GO TO 173
317 NN=NN-1
173 IF(DPHCL-PD(NN))195,868,176
176 GO TO LN,(179,178,865)
178 ASSIGN 865 TO LM
ASSIGN 179 TO LN
179 NN=NN-JIG
755 GO TO (183,181),MIY
181 IF(NN-NX)173,855,855
183 IF(NN)855,855,173
855 GO TO KKK,(180,856)
856 ASSIGN 180 TO KKK
LLL=1
GO TO 197
195 IF(DPHCU-PD(NN))174,868,868
174 GO TO LM,(179,182,865)
182 ASSIGN 865 TO LN
ASSIGN 179 TO LM
GO TO 179
865 GO TO (862,861),MIY
861 NN=NN-1
862 II=1
GY(1)=GX(NN)
PDY(1)=PD(NN)
GFS=GFR/5.
G=GX(NN)-GFS
866 IF(G-GX(NN+1))870,870,168
868 G=GX(NN)
NE=2
NG1=4
GO TO (799,168),MIY
799 NN=NN-1
GO TO 168
184 IF(DPHCU-DPT(N))869,340,871
871 IF(DPHCL-DPT(N))340,340,869
340 NE=2
NG1=4
GO TO 168
869 II=II+1
GY(II)=G
PDY(II)=DPT(N)
G=G-GFS
756 GO TO IX,(866,334)

STDY

STDY3

SOURCE PROGRAM * MAIN

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870 GY(6)=GX(NN+1)
    PDY(6)=PD(NN+1)
    ICT=1
    IRK=1
325 II=6
    ASSIGN 331 TO LN1
    ASSIGN 330 TO LM1
    ASSIGN 334 TO IX
326 IF(DPHCL-PDY(II))327,1016,329
327 IF(DPHCU-PDY(II))328,1017,1017
328 GO TO LM1,(330,332,333)
329 GO TO LN1,(331,332,333)
330 ASSIGN 333 TO LN1
    ASSIGN 332 TO LM1
    GO TO 332
331 ASSIGN 333 TO LM1
    ASSIGN 332 TO LN1
332 II=II-1
748 IF(II)1020,1020,326
333 GO TO (798,791),IRK
798 GY(1)=GY(II)
    PDY(1)=PDY(II)
    GY(6)=GY(II+1)
    PDY(6)=PDY(II+1)
    II=1
    GFS=GFS/5.
    G=GY(1)-GFS
334 IF(II-5)168,336,336
336 ICT=ICT+1
749 IF(ICT-5)325,790,790
790 IRK=2
    GO TO 325
791 G=GY(II+1)
    WRITE OUTPUT TAPE 6,792,LLL
792 FORMAT(102H      THE G VS DP CURVE CONTAINS A STEP IN PRESSURE DRO
1P AT THE TRANSITION FROM LOCAL TO BULK BOILING./99H      HOT CHANNE
2L PRESSURE DROP OCCURS WITHIN THIS STEP. THE NORMAL CONVERGENCE CR
3ITERION IS WAIVED./77H      A SOLUTION FOR THE NEAREST FLOW IS PRIN
4TED ON THE NEXT PAGE AS SOLUTION-12)
    GO TO 340
186 WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,279,LLL,ICT
279 FORMAT(47H0 TAPE 6  HOT CHANNEL VALUES - SOLUTION NUMBER 12,19H
1      11)
    WRITE OUTPUT TAPE 6,794,P,FAVE(N,L),ST(N),SL,TINL,HIN(1)
794 FORMAT(10HOPRESSURE=F5.0,27H      HOT CHANNEL- HEAT FLUX=1PE10.4,12H
1, THICKNESS=OPF5.3,9H, LENGTH=F5.2,14H, INLET TEMP.=F5.1,4H, H=F5.
21)
    WRITE OUTPUT TAPE 6,162
162 FORMAT(1H0,72H      J      T AVE      H AVE      X      TS
1      TM      DNBR)
    WRITE OUTPUT TAPE 6,163,{J,TAVE(J),HAVE(J),X(J),TS(J),TM(J),BOR(J)
1,J=1,K)
163 FORMAT(1H ,I6,F11.2,F12.2,F10.3,F11.2,F12.2,F11.3)

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```
WRITE OUTPUT TAPE 6,125,N,G,HDT(N),ACC1,ACC2,ELEV(N),FRIT(N),ENXT,
1DPT(N),DPHC,LBCNT,IBBCNT,TSAT,HSAT
ICT=0
IF(KICK)1031,757,921
921 WRITE OUTPUT TAPE 6,108
LPG=LPG+1
WRITE OUTPUT TAPE 6,1,LPG
WRITE OUTPUT TAPE 6,922
922 FORMAT(29HO SPECIAL HOT CHANNEL EDIT *)
WRITE OUTPUT TAPE 6,923
923 FORMAT(34HO J FFISO F2LO RO VB FISO)
DO 924 J=1,K
924 WRITE OUTPUT TAPE 6,925,J,STOREA(J),STOREB(J),STOREC(J),STORED(J),
1STOREE(J)
925 FORMAT(1H ,I3,3F6.2,F6.3,F7.4)
WRITE OUTPUT TAPE 6,261
261 FORMAT(109HO TOUT XOUT HOUT ROUT RGOUT SPT1 SPT2 HFG V
1FG ROSAT VF VG ROG XIN ROEIN RGIN FIOST)
WRITE OUTPUT TAPE 6,262,TOUT,XOUT,HOUT,ROUT,RGOUT,SPT1,SPT2,HFG,
1VFG,ROSAT,VF,VG,ROG,XIN,RCEIN,RGIN,FIOST
262 FORMAT(1H ,F6.2,F6.3,F7.2,F6.2,3F6.3,F7.2,F7.4,F6.2,2F7.4,2F6.3,F6
1.2,F6.3,F7.4)
757 GO TO (197,185),MIZ
185 ASSIGN 856 TO KKK
ASSIGN 182 TO LM
ASSIGN 178 TO LN
ASSIGN 866 TO IX
NG1=3
LLL=LLL+1
NE=1
IF(LLL-10)758,197,197
758 GO TO (173,179),MIY
197 IF(N-NC)192,193,1021
192 N=N+1
149 GO TO (127,128,129,134,139,147,151,152),NB
127 NJP=3
DPHC=0.
GO TO 126
128 NB=1
GO TO 134
139 NB=8
GO TO 134
147 NB=3
134 G=G1(I1)
NS=2
239 DPHC=(CK1(NS)*CK2(NS))*DPT(N-2)+(1.-(CK1(NS)*CK2(NS)))*ELEV(N-2)+
1(CK1(NS)*CKP1(NS)*CKP2(NS)+CK2(NS)*CKP1(NS)-(CK1(NS)*CK2(NS))*(1.+
2CKP1(NS)))*ACC(N-2)
DPHCU=DPHC*(1.+CONV)
DPHCL=DPHC*(1.-CONV)
194 NG1=2
NN=1
G=G*SHC
GFR=G/20.
WRITE OUTPUT TAPE 6,108
LPG=LPG+1
```

TABLE 1 (Cont.)

STDY	STDY3	SOURCE PROGRAM	MAIN
		WRITE OUTPUT TAPE 6,1,LPG	
		WRITE OUTPUT TAPE 6,281	
		WRITE OUTPUT TAPE 6,284,N	
284		FORMAT(48H	2ND HOT CHANNEL CHANNEL NUMBER=12)
		WRITE OUTPUT TAPE 6,283	
		NE=1	
		GO TO 168	
151		G=G2(I1)	
		NS=4	
		GO TO (153,154),INL	
153		HIN(1)=U2(2)*(HINLET+FA*HDT(1))+(1.-U2(2))*(HINLET+FDT*HDT(1))	
		GO TO 155	
154		HIN(1)=U2(2)*(HINLET+FA*HDT(1))+(1.-U2(2))*HDS(2,1)	
155		IF(HIN(1)-HSAT)156,157,157	
157		MRET=2	
		GO TO 819	
156		TINL=TFPH1+HIN(1)*(TFPH2+HIN(1)*(TFPH3+HIN(1)*TFPH4))	
		ROEIN=63.8+TINL*(-.01781+TINL*(1.132E-5+TINL*(-6.786E-8)))	
		SPT2=1./ROEIN	
		XIN=0.	
		RGIN=0.	
		GO TO 239	
129		N8=7	
		GO TO 152	
121		N=N+1	
152		MC=2	
		NS=3	
		NG1=1	
126		G=G2(I1)	
		NE=1	
		ASSIGN 53 TO MQUAL	
		HIN(1)=HINLET+FA*HDT(1)	
		IF(HIN(1)-HSAT)158,159,159	
159		MRET=3	
		GO TO 819	
158		TINL=TFPH1+HIN(1)*(TFPH2+HIN(1)*(TFPH3+HIN(1)*TFPH4))	
		ROEIN=63.8+TINL*(-.01781+TINL*(1.132E-5+TINL*(-6.786E-8)))	
		SPT2=1./ROEIN	
165		WRITE OUTPUT TAPE 6,108	
		LPG=LPG+1	
		WRITE OUTPUT TAPE 6,1,LPG	
		WRITE OUTPUT TAPE 6,280	
280		FORMAT(46H0	AVERAGE CHANNEL VALUES (SECOND PASS))
		GO TO 168	
122		N=N+1	
		GO TO 126	
180		WRITE OUTPUT TAPE 6,198	
198		FORMAT(1H0,13H NO SOLUTION.)	
315		CONTINUE	
278		GO TO (220,224,227,300),IG	
193		GO TO (216,217,218,219),K1	
216		IF(IPV-I)1022,221,222	
220		IF(I-1)1032,300,221	
221		K1=2	
		I=1	
217		IF(I1V-I1)1023,224,225	

STDY

STDY3 SOURCE PROGRAM * MAIN

```
222 I=I+1
    WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,244
    GO TO 270
225 I1=I1+1
    WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,244
    IG=2
    GO TO 271
224 K1=3
    I1=1
218 IF(ITV-IT)1024,227,228
228 IT=IT+1
    WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,244
    IG=3
    GO TO 272
227 K1=4
    IT=1
219 IF(IFV-L)1025,300,231
231 L=L+1
    WRITE OUTPUT TAPE 6,108
    LPG=LPG+1
    WRITE OUTPUT TAPE 6,1,LPG
    WRITE OUTPUT TAPE 6,244
    IG=4
    GO TO 232
819 TINL=TSAT
    XIN=(HIN(1)-HSAT)/HFG
    SPT2=VF+XIN*VFG
    ROEIN=1./SPT2
    GO TO (826,820),MPRES
820 IF(XIN-.9)821,821,826
821 IF(XIN-.1)822,823,823
822 IF(XIN-.01)826,824,824
823 RGIN=RGAOH+XIN*(RGA1H+XIN*(RGA2H+XIN*RGA3H))
    GO TO 825
824 RGIN=RG AOL+XIN*(RGA1L+XIN*(RGA2L+XIN*RGA3L))
825 SPT2=(VF*(1.-XIN)**2)/(1.-RGIN)+(VG*XIN**2)/RGIN
826 GO TO (851,239,165,166),MRET
300 WRITE OUTPUT TAPE 6,283
    WRITE OUTPUT TAPE 6,318
318 FORMAT(25H0      END OF THIS PROBLEM)
    GO TO 320
1014 MMM=1014
    GO TO 1033
1016 MMM=1016
    GO TO 1033
1017 MMM=1017
    GO TO 1033
```


1020 MMM=1020
GO TO 1033
1021 MMM=1021
GO TO 1033
1022 MMM=1022
GO TO 1033
1023 MMM=1023
GO TO 1033
1024 MMM=1024
GO TO 1033
1025 MMM=1025
GO TO 1033
1031 MMM=1031
GO TO 1033
1032 MMM=1032
1033 WRITE OUTPUT TAPE 6,1034,MMM
1034 FORMAT(22H0 MACHINE ERROR- TYPE I4)
GO TO 320
END(1,1,0,0,1,1,1,1,0,0,0,0,0,0)

6. Hot Channel Factors

The power hot channel factors utilized in the core thermal analyses are presented in Table 5. Those power hot channel factors which result from scram set points and possible instrumentation uncertainty are referenced in Table 5 as to the source of this information. The uncertainty in power distribution predictions was estimated based on a comparison of the analytically predicted power distribution with the experimental measurements made on the SM-1A type 3 core. The power hot channel factors resulting from possible variations in fuel loading and local inhomogeneity were taken as directly proportional to the uncertainty in uranium content. The power hot channel factors associated with uncertainties in burnable poison loading are based on the results of physics calculations.

The derivation and values of the flow hot channel factors used in the analysis are discussed below in two parts:

- (1) The uncertainty in total element flow.
- (2) The non-uniform distribution of flow within a fuel element.

a. Flow Uncertainty in the Fuel Elements. The total pressure drop across a fuel element was determined by summing the pressure drops through the element. In the stationary elements the total pressure drop is made up of the pressure drops across the inlet and outlet boxes, the fuel plates and the orifice. Uncertainty in the calculation of the total pressure drop is due to the uncertainties in the calculations of the component pressure drops through the element.

For the determination of flow uncertainty in an element it was assumed that there is no uncertainty in the calculation of the elevation pressure drop. The pressure drop across the inlet and outlet boxes is due to the enlargement and contraction losses which were determined by the equation

$$\Delta P = \rho K \frac{V^2}{2g}$$

Uncertainty in this pressure drop calculation is caused by uncertainty in the dimensional tolerances of these components. Uncertainty, therefore, in the pressure drop due to an enlargement or contraction loss is given by the equation

$$\begin{aligned} \Delta (\Delta P) &= \frac{d \Delta P}{d A} \times \Delta A \\ &= \frac{\pi \rho K Q^2 D \Delta D}{2g A^3} \end{aligned}$$

Table 5
Power Hot Channel Factors

<u>Factors Affecting Power Produced in Entire Element</u>		<u>Reference</u>
Ratio of Scram Power to Nominal Full Power	= 1.2	(10)
Possible Power Instrumentation Error	= 1.03	(10)
Uncertainty in Poison Loading	= 1.02	
Uncertainty in Prediction of Power Distribution	= 1.05	
Product	= 1.324	
 <u>Additional Factors Affecting Power Produced in a Fuel Plate</u>		
Maximum Plate Fuel Loading	= 1.0075	(11)
Uncertainty in Power Distribution	= 1.05	
Product	= 1.058	
 <u>Additional Factor Affecting Localized Power Production</u>		
Maximum Local Fuel Loading	= 1.2	(11)

where: A = flow area
 D = diameter
 g = gravitational constant
 K = contraction or enlargement coefficient
 ΔP = pressure drop
 Q = volumetric flow rate
 ρ = density

Taking 125 gpm as a typical stationary element flow rate, this yields an error in pressure drop of 2.76 percent across the inlet box which may be considered as typical of the enlargement and contraction loss uncertainties. To be conservative, it was assumed that the uncertainty in this pressure drop is 5 percent.

The same technique was applied to the equation for orifice pressure drop to determine the effect of dimensional variation on the orifice pressure drop.

The orifice pressure drop is given by:

$$\Delta P = \frac{(1 - \beta^4)(W/A)^2}{C^2 g \rho}$$

where: A = area, square feet
 $\beta = \frac{D_2}{D_1}$ (dimensionless)
 D_2 = the orifice diameter, feet
 D_1 = the diameter of the channel upstream of the orifice, feet
 C = the orifice coefficient, dimensionless
 W = the mass flow rate, lb/sec
 ρ = density, lb/ft³
 g = gravitational constant, ft/sec²

$$\Delta(\Delta P) = \frac{d(\Delta P)}{d\beta} \Delta\beta + \frac{d(\Delta P)}{dA} \Delta A$$

and

$$\frac{\partial (\Delta P)}{\partial \beta} = \frac{4 \beta^3 (W/A)^2}{C^2 2g}$$

$$\frac{\partial (\Delta P)}{\partial A} = \frac{(1 - \beta^4) W^2}{A^3 g \rho}$$

Element number 27 has the smallest orifice. The calculation of pressure drop for this element, therefore, has the greatest effect on the uncertainty in flow for this element. Applying the specified dimensional tolerances for the orifice and the channel upstream of the orifice to the equation for orifice pressure drop uncertainty, yields an uncertainty of 7.35 percent in the orifice pressure drop. However, as discussed above, it was found that the methods used to calculate the fuel element pressure drop predicted results which agree within 20 percent accuracy to experimental data on the SM-1 core. Therefore, it was assumed that there is a 20 percent uncertainty in the SM-1A orifice pressure drop calculation.

The pressure drop across the fuel plates was determined by STDY-3 and, in the absence of better information, it was assumed that the uncertainty in this pressure drop is 10 percent.

The flow uncertainty in a fuel element was determined by the following equation:

$$\left(\frac{W}{W_0} \right) = \left[\frac{1}{1 + U_{in} \Delta P_{in} + U_{plate} \Delta P_{plate} + U_{out} \Delta P_{out} + U_{orif} \Delta P_{orif}} \right]^{1/2}$$

where U is the uncertainty for each component ΔP . This equation yields a flow uncertainty of 6.98 percent for element number 27 at 98.5 gpm which is close to the nominal conditions. Therefore, a value of 7 percent was taken as the flow uncertainty for the stationary element.

The same procedure was used to determine the flow uncertainty in the control elements. There are, however, no orifices in the control elements. The total pressure drop across the control elements is equal to the sum of the pressure drop across the inlet, the fuel plates, the outlet and other enlargement and contraction losses within the control element. In the absence of experimental information it was assumed that the prediction of pressure drop across the inlet and outlet of the control elements could be

in error by as much as 25 percent. It is felt that this is a conservative estimate. As in the stationary elements, it was assumed that the pressure drop errors due to the enlargement and contraction amounts to 5 percent. Also, as previously assumed for the stationary element, an error of 10 percent is assigned to the fuel plate pressure drop in the control elements. Applying the same technique as was used for the stationary elements, a flow rate error of 7.7 percent is obtained for the control element. A value of 7.7 percent in flow uncertainty was, therefore, used for the control elements.

b. Non-Uniform Distribution Within the Fuel Element. The variation of velocity between the parallel channels of an SM-1A stationary fuel element was measured experimentally and reported in Reference 1. Figure III-4, which is reproduced from Reference 1, shows the channel to channel variation in velocity for various total element flow rates. This figure shows that the outermost channels, A and Q, have the minimum velocity. As a portion of the experimental program reported in Reference 1, the channel spacing for each channel was measured at one inch increments. This data, also reproduced from Reference 1, is presented as Table 6. This information was utilized to determine the flow channel factors due to plenum effects which were then utilized in the subsequent calculations.

Figure III-4 shows that the central channels (I, J and K) receive somewhat less flow than the immediately adjacent channels on either side. This occurs because of the spacer pin located at the inlet of the fuel plate. Since channels I, J and K are located at the center of the fuel element, one would anticipate that they would have essentially identical velocities. However, as shown in Figure III-4 this is not the case. Table 6 however, shows that the dimensions of these three channels are not identical. The average width of channels I, J and K were found to be 0.1215, 0.1152, and 0.1298 inch, respectively.

The relationship between the channel velocity and the channel dimension was then investigated. Since the tests were conducted in a horizontal plane under isothermal conditions, there are no acceleration or elevation components to the measured pressure drop. Therefore, the frictional pressure drop in each of the parallel channels is identical. Therefore,

$$\Delta P = \frac{\rho f v^2 L}{2g D}$$

$$f = 0.184 / \left(\frac{\rho v}{\mu} \right)^{0.2}$$

Figure III-4- SM-1A Stationary Element Flow Distribution - 1.68" Orifice Located at Exit

Table 6

**CHANNEL MEASUREMENTS FOR
STATIONARY FUEL ELEMENT**

Channel Letter

	A	B	C	D	E	F	G	H	I	J	K	L	M	N	O	P	Q
1)	121	119	114	125	118	124	129	124	121	116	129	123	114	120	121	119	130
2)	122	116	116	125	119	124	130	123	121	116	128	123	115	120	120	119	130
3)*	122	122	119	124	122	125	124	124	122	123	124	123	124	122	122	121	122
	123	118	114	125	118	124	129	123	120	116	129	123	114	121	118	120	129
	122	121	123	122	124	124	123	125	124	125	122	125	121	122	119	121	124
4)	123	116	115	125	118	123	129	122	121	115	129	123	115	121	119	120	129
5)	123	117	116	124	119	124	129	122	122	115	129	123	115	121	119	120	129
6)*	123	121	120	124	123	125	124	124	122	123	124	123	124	122	122	120	122
	122	117	116	124	119	125	130	121	122	116	129	123	115	121	119	121	128
	122	121	124	122	123	125	123	124	124	125	122	125	121	122	119	121	124
7)	121	117	115	125	118	125	129	123	122	115	129	123	116	120	120	119	130
8)	122	118	115	125	118	124	130	123	121	116	129	123	115	121	119	120	129
9)	122	118	115	125	119	124	130	123	121	116	129	123	115	121	119	119	129
10)*	123	122	122	123	124	125	124	124	123	123	124	124	123	123	123	122	125
	122	118	115	125	118	123	131	122	121	116	130	122	115	121	120	118	130
	124	122	123	122	123	124	124	125	123	123	124	123	123	122	122	119	125
11)	122	117	115	125	118	124	131	122	122	115	131	122	115	121	120	119	130
12)	122	118	115	124	118	124	131	123	122	115	131	122	114	121	119	120	128
13)	122	118	115	124	118	124	131	123	122	114	131	122	115	120	119	121	128
14)*	124	123	122	123	123	125	123	125	123	123	123	124	123	123	123	122	125
	123	118	115	124	118	124	131	123	122	114	131	122	115	121	119	120	129
	126	124	124	122	122	124	123	125	124	123	124	123	123	122	122	118	125
15)	123	118	114	124	119	123	130	123	122	115	130	122	115	121	119	120	128
16)	122	119	114	125	118	123	130	123	122	115	130	122	115	120	120	118	129
17)	122	118	115	125	118	123	130	124	122	115	130	121	115	121	119	118	129
18)*	122	122	119	124	122	125	124	124	123	122	124	122	124	122	122	120	122
	122	118	114	125	117	123	129	124	121	115	130	122	115	121	119	118	129
	123	121	122	121	123	124	123	124	124	123	123	124	121	122	120	120	124
19)	121	117	114	124	118	123	129	124	122	114	130	122	115	120	119	117	131
20)	122	118	113	125	117	124	129	124	122	114	130	122	115	120	119	117	131
21)	121	119	113	125	117	123	130	125	121	115	131	122	114	120	120	117	130
22)*	124	121	122	123	124	125	124	125	123	124	125	124	123	123	121	124	123
	120	118	115	125	117	124	128	125	122	116	131	122	115	122	120	120	129
	124	121	124	122	122	125	124	126	124	123	124	124	123	121	122	121	126

NOTE: 1st measurement 1-1/4" from inlet end of fuel plate. All others at 1" increments; measurement given in mils.

Therefore, for constant properties

$$\Delta P \sim \frac{V^{1.8}}{S^{1.2}}$$

where: D = equivalent diameters
 f = friction factor
 g = gravitational constant
 L = length
 S = channel spacing (D/2)
 V = velocity
 ρ = density
 μ = viscosity
 ΔP = pressure drop

As the above equations show, the velocity in any channel would be expected to vary as the channel spacing to the 2/3 power. That is, it would be anticipated that channel I would have 3.4 percent more flow than channel J and that channel K would have 7.9 percent more flow than channel J. When the measured channel velocities were compared to these predictions, excellent agreement was obtained as is shown in Table 7.

Review of the channel dimensions presented in Table 6 shows that channel A, even though it has the lowest velocity of any channel in the fuel element, had a larger than average channel width. Therefore, it was concluded that if channel A had had the nominal channel width of 0.123 inch it would have received 3.4 percent less flow. It was this further reduced flow in channel A that was utilized to define the plenum hot channel factor. The value of this factor is presented in Figure III-5, as a function of flow rate.

Table 7
Velocity Ratios

Flow Rate (gpm)	V_I/V_J (Measured)	V_I/V_J (Predicted)	V_K/V_J (Measured)	V_K/V_J (Predicted)
50.5	1.033	1.034	1.059	1.079
75.8	1.011	1.034	1.08	1.079
100.0	1.028	1.034	1.082	1.079
125.0	1.030	1.034	1.082	1.079

Flow Hot Channel Factor Due to Plenum Effects

1.32
1.30
1.28
1.26
1.24
1.22

Stationary Element

50 60 70 80 90 100 110 120 130 140

Flow Rate, GPM

FIGURE III-5. STATIONARY ELEMENT FLOW HOT CHANNEL FACTOR

The flow hot channel factor due to plenum effects was applied only to the frictional, entrance and exit pressure drops. This is done since isothermal experimental data with a horizontal test section was used to obtain the plenum factor. Therefore, there was no acceleration or elevation pressure drop components of the total pressure drop.

The flow hot channel factor due to plenum effects in the movable elements was determined in a manner identical to that used in the stationary elements. The variation of coolant velocity and detailed measurements of channel spacing within a movable fuel assembly was determined and reported in Reference 1. These are presented here as Figure III-6 and Table 8, respectively. Figure III-6 shows that, similar to the stationary elements, the channels at the periphery of the movable assembly receive less than an average amount of flow. Since the lattice flow channels (channels A and Q) receive heat from only one, rather than two, fuel plates, the flow rate in these channels is not crucial. Channels B and P, however, do receive heat from two fuel plates and the flow rate in these channels is crucial. For those cases in which the total assembly flow was 76 gpm or greater, channel B always had the minimum flow. The average width of channel B was found to be 0.1213 inch from the data measurements presented in Table 8. This is slightly less than the nominal channel spacing of 0.123 inch and, therefore, the flow rate in channel B would be 1.4 percent higher if this channel had a nominal spacing. This correction was made and the resulting plenum hot channel effect was determined in a manner identical to that utilized in the stationary elements. The resulting flow hot channel factor due to plenum effects is presented in Figure III-7 as a function of total element flow rate.

The appropriate equations used to determine pressure drop in the hot and average channels with heat addition and with the fuel elements in a vertical orientation are:

$$\Delta P_{H.C.} = K_P (\Delta P_{friction} + \Delta P_{entrance} + \Delta P_{exit}) \\ + \Delta P_{elev.} + \Delta P_{accel.}$$

$$\Delta P_{A.C.} = \Delta P_{friction} + \Delta P_{entrance} + \Delta P_{exit} \\ + \Delta P_{elev.} + \Delta P_{accel.}$$

where: H.C. = hot channel
A.C. = average channel
 K_P = plenum factor

FIGURE III-6.

SM-1A and PM-2A Control Rod Assembly Flow Distribution

Table 8
CHANNEL MEASUREMENTS FOR
CONTROL ROD FUEL ELEMENT

		Channel Letter														81
		B	C	D	E	F	G	H	I	J	K	L	M	N	O	P
1)*		123	122	122	122	123	122	123	122	125	123	122	121	123	124	122
		121	122	125	118	126	119	124	125	120	125	123	124	122	124	127
		123	124	121	122	122	122	123	123	123	123	123	125	125	133	124
2)		118	122	124	117	126	118	123	124	120	125	122	121	117	122	124
3)		121	119	126	117	125	119	123	124	120	125	122	122	116	122	124
4)		121	118	126	117	125	119	123	122	120	126	121	121	116	121	124
5)*		122	121	122	120	122	121	121	120	122	122	120	121	120	121	120
		121	118	126	116	125	119	123	122	120	126	121	121	117	122	124
		120	120	120	120	122	119	122	122	120	123	120	121	122	120	121
6)		120	119	125	118	124	119	124	122	120	125	121	122	117	121	124
7)		121	118	126	118	124	120	123	122	121	125	121	121	117	123	124
8)		121	119	126	118	125	119	123	123	120	125	121	121	117	123	124
9)*		122	119	122	119	120	121	122	120	122	121	121	119	119	120	117
		121	119	127	117	125	119	123	123	121	125	121	121	117	123	124
		119	119	120	119	123	120	124	121	122	123	119	120	123	119	121
10)		122	118	127	118	125	119	123	124	119	125	121	121	117	123	124
11)		122	119	125	118	125	119	123	125	120	125	121	121	117	123	124
12)		121	119	125	119	125	119	124	124	120	125	122	120	118	122	125
13)*		120	120	121	121	121	121	121	121	121	122	120	120	119	122	119
		122	119	125	119	124	120	124	124	120	125	121	120	118	123	124
		121	121	120	120	122	120	123	122	121	122	121	121	121	121	121
14)		122	119	125	118	124	121	123	124	120	125	121	121	118	123	124
15)		122	119	125	119	124	120	124	124	121	124	121	120	119	121	125
16)		122	119	125	118	125	121	123	123	121	125	121	120	119	121	125
17)*		122	121	120	121	121	121	121	120	122	122	120	121	120	122	120
		122	119	125	119	124	120	124	123	121	125	121	120	119	122	124
		121	121	120	120	122	120	123	122	122	122	121	121	121	121	121
18)		122	120	125	119	124	120	125	124	121	125	121	121	119	121	125
19)		122	120	125	118	124	119	124	123	121	125	122	120	119	121	124
20)*		122	121	121	121	121	121	121	121	122	123	120	120	120	122	120
		122	120	126	119	124	119	125	123	121	125	121	121	117	122	125
		121	120	120	120	122	120	123	122	122	122	122	121	121	121	121
21)		122	119	126	118	124	119	125	123	121	124	122	119	120	122	125
22)		121	120	125	118	125	119	124	124	121	126	122	119	119	121	127
23)		120	123	125	121	125	121	124	124	121	126	123	121	120	122	125

NOTE: 1st measurement 1/2" from inlet end of fuel plate, remainder at 1" increments from 1st measurement, all measurements in mils.

* Three positions: top, center and bottom, all other measurements made at center of

FIGURE III-7. CONTROL ELEMENT FLOW HOT CHANNEL FACTOR

7. Hydraulic Stability and Outlet Enthalpy Determination

An investigation was made of the existence of flow instability in the fuel elements which could occur if the slope of the pressure drop versus flow mass velocity curve becomes negative in the vicinity of the operating point for any channel. Fuel element number 44, at the conditions of the beginning of life, hot, equilibrium xenon was selected for the instability analysis since its hot channel outlet enthalpy is the maximum in the core.

Calculations were performed over a range of power levels and hot channel flow rates which insured that bulk boiling in the hot channel is included in the analysis.

Static pressure drop versus mass velocity was calculated for subcooled flow using both the STDY-3 computer program and the IBM code having more recent local boiling friction factor and void correlations. Appropriate hot channel factors due to plenum effects were utilized in the analysis.

In the quality region, the static pressure drop was determined by the STDY-3 code. It was felt, however, that the methods employed in STDY-3 do not properly treat the pressure differential across the enlargement at the outlet from the plates in the two phase flow regime. The STDY-3 calculations include the recovery in static pressure at the plate outlet for the total flow. This calculation was modified for two phase flow using the assumption that the vapor phase condenses at the outlet, and that no static pressure recovery can be attributed to the vapor fraction of the total flow. However, static recovery of the liquid phase was included.

This approach was utilized since the hot channel outlet mixes with the subcooled liquid leaving the other channels.

The sketch below represents the enlargement at the fuel plate outlet.

The static pressure change from 1 to 2 is given by:

$$P_{\text{stat } 1} - P_{\text{stat } 2} = \frac{K_e G_1^2}{2g\rho} + \frac{G_2^2}{2g\rho} - \frac{G_1^2}{2g\rho} \quad (1)$$

where: $P_{\text{stat } 1}$ = static pressure at 1
 $P_{\text{stat } 2}$ = static pressure at 2
 K_e = enlargement coefficient
 G = mass velocity
 g = acceleration due to velocity
 ρ = fluid density at the plate outlet

also: $G_2 = \sigma G_1$

and: $G_2^2 = \sigma^2 G_1^2$

where: σ = exit area ratio

thus $P_{\text{stat } 1} - P_{\text{stat } 2} = \frac{G_1^2}{2g\rho} (K_e + \sigma^2 - 1)$

ρ_{out} was determined from the STDY-3 calculation of ΔP_e and e , where ΔP_e and e is the pressure drops across the inlet and outlets of the plates. ΔP_e and e is given by:

$$\Delta P_e \text{ and } e = \frac{K_e G_1^2}{2g} \left(\frac{1}{\rho_{\text{in}}} + \frac{1}{\rho_{\text{out}}} \right)$$

where ρ_{in} and ρ_{out} are the fluid density at the inlet and outlet.

$\frac{1}{\rho_{\text{out}}}$ is then given by:

$$\frac{1}{\rho_{\text{out}}} = \frac{2g \Delta P_e \text{ and } e}{K G^2} - \frac{1}{\rho_{\text{in}}}$$

Thus, to obtain the actual static pressure recovery of the liquid phase, the static pressure loss is calculated from equation 1 using $\rho = \rho_{\text{sat}}$.

To determine the static pressure rise calculated by STDY-3, equation 1 was used with $\rho = \rho_{\text{out}}$.

The difference between these two results was then added to the uncorrected STDY-3 results to determine the total static pressure drop across the fuel plates.

This analysis was performed on element number 44 at beginning of life, equilibrium xenon conditions at power levels of 1.0, 1.2, 1.4, 1.7, and 2.0 times the scram power. The results of this analysis are presented in Figure III-8. An interpretation of these results is presented in Volume I of this report.

The static pressure drop in the hot average channel was set equal to that in the average channel to determine the hot channel flow rate. This flow rate was then utilized to determine the enthalpy at the exit of the hot channel.

B. Detailed Results

The detailed results of the thermal and hydraulic analyses are presented in this section of the appendix.

1. Flow Distribution

The total core pressure drop is presented in Figure III-9 for the nominal conditions of:

Power = 20.2 Mw(t)

Inlet Temperature = 423°F

Pressure = 1200 psia

The pressure drop across the individual assemblies for the same conditions of power, inlet temperature and pressure are presented in Figures III-10 - III-14.

The corresponding data for "worse case" conditions of:

Power = 24.2 Mw(t)

Inlet Temperature = 445°F

Pressure = 1105 psia

are presented in Figures III-15 - III-20.

The lattice flow pressure drop is presented in Figure III-21.

FIGURE II-8. HOT CHANNEL STATIC PRESSURE DROP
(ELEMENT 44, HOT, EQ. XENON VERSUS MASS VELOCITY)

FIGURE III-9
CORE PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Nominal" Case

FIGURE III-10
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A CORE 3 "NOMINAL" CASE

FIGURE III-11
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Nominal" Case

FIGURE III-12
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Nominal" Case

FIGURE III-13
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Nominal" Case

FIGURE III-14

FUEL ELEMENT PRESSURE DROP VERSUS FLOW

SM-1A Core 3 "Nominal" Case

FIGURE III-15
CORE PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Worse" Case

FIGURE III-16
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Worse" Case

FIGURE III-17

FUEL ELEMENT PRESSURE DROP VERSUS FLOW

SM-1A Core 3 "Worse" Case

FIGURE III-18
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Worse" Case

FIGURE III-19
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Worse" Case

FIGURE III-20
FUEL ELEMENT PRESSURE DROP VERSUS FLOW
SM-1A Core 3 "Worse" Case

FIGURE III-21
PRESSURE DROP VERSUS FLOW
SM-1A Core 3 Lattice Flow
"Nominal" and "Worse" Cases

2. Enthalpy Rise, Mass Velocity Ratios, DNB Ratio and Fuel Element Temperature

The stationary and movable fuel elements were studied at various times during life. The resulting values of the four thermal-hydraulic parameters which were tabulated in Volume I of the report are repeated here as Table 9. The maximum fuel element surface and matrix temperatures are also included in Table 9. Table 9 also has a column indicating those cases where the hot channel enthalpy rise was based on the average heat flux of the two plates cooled by the coolant. The hot channel enthalpy rise was based on the hot plate heat flux, yielding conservative values, where no X appears in that column.

3. Hydraulic Stability

The variation of the static pressure drop along the fuel plates of the hot channel of element 44, at the beginning of life and hot equilibrium xenon is presented. Various power levels and flow rates were considered. The inlet system pressure was 1105 psia and the inlet temperature was 445°F.

Figure III-8 presents the results calculated using the STDY-3 code but with the two phase static recovery corrections discussed in Section A. 7. of this appendix. Figure III-22 presents the results obtained using more recent local boiling and friction factor correlations.

FIGURE III-22. HOT CHANNEL STATIC PRESSURE DROP (ELEMENT 44,
HOT, EQ. XENON)

Table 9

Fuel Element Parameters

Element Number	Conditions	Number of Axial Increments	Enthalpy Rise Average Channel	Hot Channel	Hot Channel Enth. Rise Based on Avg. of Two Plates	Hot to Average Channel Mass Velocity Ratio	DNBR min.	Max. Surface Temp. °F	Max. Matrix Temp. °F
12	Beginning of life, clean	10	37.14	68.62	X	0.880	5.199	570.41	620.04
13	"	10	33.72	51.88		0.825	7.411	569.85	605.26
14	"	10	36.28	53.14		0.869	7.579	569.78	604.36
15	"	10	36.84	55.97		0.880	7.027	570.06	607.92
16	"	10	42.06	82.88	X	0.825	4.840	570.50	621.52
21	"	10	43.60	85.92	X	0.825	4.917	570.41	620.04
22	"	10	45.74	68.19		0.825	7.425	569.69	603.28
23	"	10	44.90	71.02		0.825	5.739	570.50	613.95
23	End of life	10	48.15	91.14		0.768	5.395	570.53	614.30
24	Peak reactivity	10	38.97	83.89		0.660	3.499	571.89	635.58
25	Beginning of life, clean	10	46.29	74.42		0.825	5.168	570.84	618.93
26	"	10	44.74	62.54		0.880	7.399	569.80	604.64
27	"	10	47.04	99.32	X	0.770	4.607	570.50	621.52
27	Peak reactivity	10	46.57	89.81	X	0.880	4.113	571.15	634.11
27	Beginning of life, clean	25	47.04	88.88	X	0.880	4.631	570.53	622.69
31	"	10	40.84	62.05		0.880	7.21	569.93	606.26
32	"	10	48.20	81.23		0.825	5.154	570.74	617.49
32	End of life	10	51.68	112.84		0.716	4.754	570.75	617.63
32	"	25	51.68*	91.59*		0.660*	5.907*	570.14*	608.92*
33	Peak reactivity	10	44.60	109.64		0.605	2.677	572.49	649.27

Table 9 (Cont.)

Element Number	Conditions	Number of Axial Increments	Enthalpy Rise Average Channel	Hot Channel	Hot Channel Enth. Rise Based on Avg. of Two Plates	Hot to Average Channel Mass Velocity Ratio	DNBR min.	Max. Surface Temp. °F	Max. Matrix Temp. °F
33	Beginning of life, eq. xenon	23	46.6	93.27	X	0.660	3.976	571.34	628.45
33	"	23	46.6*	84.01*	X	0.74*	4.085*	571.39*	626.94
34	End of life	10	57.55	95.04		0.825	4.487	571.16	624.04
34	Beginning of life, clean	10	52.10	75.99		0.825	4.922	571.04	622.13
35	"	10	53.56	70.30		0.880	5.585	570.69	616.63
36	"	10	52.37	79.44		0.825	6.018	570.32	611.37
37	"	10	39.02	59.28		0.880	6.939	570.06	607.92
41	"	10	42.42	61.36		0.880	7.352	569.86	605.33
42	Peak reactivity	10	38.33	94.06		0.650	3.002	572.27	644.68
43	End of life	10	56.9	107.7		0.771	4.061	571.39	627.96
43	Beginning of life, clean	10	51.52	76.39		0.825	4.662	571.20	624.80
44	Peak reactivity	10	50.54	101.19		0.660	2.831	572.40	647.20
44	Beginning of life, eq. xenon	23	52.3	97.16	X	0.660	3.741	571.45	631.44
44	"	23	52.3*	88.65*	X	0.74*	3.808*	571.51*	632.45
45	Beginning of life, clean	10	48.13	71.37		0.825	4.750	571.20	624.80
45	End of life	10	53.16	93.91		0.826	4.238	571.39	627.96
46	Peak reactivity	10	38.33	94.06		0.650	3.002	572.27	644.68
47	Beginning of life, clean	10	38.66	55.92		0.880	7.499	569.86	605.33
51	"	10	38.27	58.15		0.880	6.969	570.06	607.92

Table 9 (Cont.)

Element Number	Conditions	Number of Axial Increments	Enthalpy Rise Average Channel	Hot Channel	Hot Channel Enth. Rise Based on Avg. of Two Plates	Hot to Average Channel Mass Velocity Ratio	DNBR min.	Max. Surface Temp. °F	Max. Matrix Temp. °F
52	Beginning of life, clean	10	54.14	82.12		0.825	5.968	570.32	611.37
53	"	10	51.93	68.17		0.880	5.628	570.69	616.63
54	"	10	54.03	84.05		0.825	4.779	571.04	622.13
54	End of life, clean	10	59.68	105.59		0.769	4.346	571.16	624.04
55	Peak reactivity	10	44.6	109.64		0.605	2.677	572.49	649.27
55	Hot eq. xenon	23	46.6	93.27	X	0.660	3.976	571.34	628.45
55	"	23	46.6*	84.01*	X	0.74*	4.085*	571.39*	626.94*
56	End of life	10	44.02	83.31		0.826	5.19	570.75	617.63
56	Beginning of life, clean	10	41.06	69.20		0.825	5.381	570.74	617.49
57	"	10	38.05	57.81		0.880	7.242	569.93	606.26
61	"	10	42.25	83.25	X	0.825	4.834	570.50	621.52
62	"	10	44.91	62.77		0.880	7.393	569.80	604.64
63	"	10	51.89	83.42		0.825	5.009	570.84	618.93
65	"	10	43.24	68.39		0.825	5.794	570.50	613.95
65	End of life	10	46.36	81.91		0.825	5.551	570.53	614.30
66	Beginning of life, clean	10	41.86	58.51		0.880	7.675	569.69	603.28
67	"	10	40.36	79.54	X	0.825	5.015	570.41	620.04
72	"	10	42.64	84.02	X	0.825	4.822	570.50	621.52
73	"	10	39.95	60.70		0.880	6.903	570.06	607.92
74	"	10	39.73	57.57		0.880	7.455	569.86	605.33
75	"	10	31.37	48.26		0.869	7.526	563.86	599.27
76	"	10	36.13	67.59	X	0.869	5.218	570.41	620.04

* Results based on recent local boiling correlations; all other cases obtained from STDY-3.

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