PATHFINDER ATOMIC POWER PLANT
THERMAL AND HYDRAULIC DESIGN
OF THE
PATHFINDER BOILING WATER
INTEGRAL NUCLEAR SUPERHEATING REACTOR

MASTER

August 29, 1962

Submitted to
U. S. ATOMIC ENERGY COMMISSION
NORTHERN STATES POWER COMPANY
and
CENTRAL UTILITIES ATOMIC POWER ASSOCIATES

by

ALLIS-CHALMERS MANUFACTURING COMPANY
ATOMIC ENERGY DIVISION
Milwaukee 1, Wisconsin

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ALLIS-CHALMERS MANUFACTURING COMPANY

Under

Agreement dated 2nd Day of May 1957, as Amended
between

Allis-Chalmers Mfg. Co. & Northern States Power Co.
under

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August 29, 1962

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TOPICAL REPORT

THERMAL AND HYDRAULIC DESIGN OF THE PATHFINDER
BOILING WATER INTEGRAL NUCLEAR SUPERHEATING REACTOR

By

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FOREWORD

One of a series of reports on research and development in connection with the design of Pathfinder Atomic Power Plant, this particular report deals with "Thermal and Hydraulic Design of the Pathfinder Boiling Water Integral Nuclear Superheating Reactor."

The Pathfinder plant will be located at a site near Sioux Falls, South Dakota and is scheduled for operation in the Fall of 1962. Owners and operators of the plant will be Northern States Power Company of Minneapolis, Minnesota. Allis-Chalmers is performing the research, development and design as well as being responsible for plant construction.

The U. S. Atomic Energy Commission, through Contract No. AT(11-1)-589 with Northern States Power Company, and Central Utilities Atomic Power Associates (CUAPA) are sponsors of the research and development program.

The plant's reactor will be of the Controlled Recirculation Boiling Reactor type with Nuclear Superheater.

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INTRODUCTION

A comprehensive thermal and hydraulic design investigation of the Pathfinder reactor, including analytical and experimental determinations, as well as a complete set of performance calculations, has been performed. The results of this effort are summarized in this report.

The reactor is a 600 psi, thermal, light water moderated, direct cycle boiling water nuclear superheat unit. The annular boiling section is cooled by forced recirculation, is fueled with slightly enriched UO_2 pellets enclosed in zircaloy cladding tubes, and contains 16 boron stainless steel cruciform control rods. Steam from the boiling section is dried and sent to the integral, water moderated, highly enriched, cermet fueled, superheating section, which contains 4 control rods. A diagram of the reactor is shown in Fig. 1, and a cross section of the core is shown schematically in Fig. 1-A.

A detailed analysis of the reactor energy balance has been conducted. This balance is of interest in an integral superheating system in that the steam conditions in the superheater depend strongly upon the operating parameters of the boiling section. The components of the energy balance are presented in Table I for various steam outlet temperatures. An outlet steam temperature of 725 F is expected near the beginning of superheater life, and 675 F steam conditions are expected at the end of superheater life.

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CONCLUSIONS

The thermal and hydraulic analyses presented in the following report indicate that the Pathfinder Reactor will perform within the limits of the material design criteria and operational requirements.

Fuel element temperatures in both the superheater and boiler sections are not excessive, under the worst combination of circumstances. Thermal stresses in the superheater have been minimized by designing the annular element so that temperatures are kept as uniform as possible at every axial location.

All phases of the reactor operation have been carefully examined with respect to thermal and hydraulic limitations. In all instances, sufficient heat dissipation capacity exists to insure an ample margin of safety.

Fig. 1...Pathfinder Reactor Vessel Design (206530)

Fig. 1-A...Cross Section of Pathfinder Core (43-025-260)

TABLE I

REACTOR

ENERGY BALANCE
(600 psig)INPUT

<u>SUPERHEATER</u>	<u>T=825 F</u>	<u>T=775 F</u>	<u>T=750 F</u>	<u>T=725 F</u>	<u>T=700 F</u>	<u>T=675 F</u>
Superheater Coolant Heat	1362.0	1193.0	1102.6	1017.8	934.4	841.6
Superheater Steam Line Loss	0.8	0.7	0.6	0.6	0.4	0.4
Moderator (p,γ)	50.0	43.8	41.2	38.4	34.2	30.9
Moderator Fuel Region (Rad, Cond)	18.3	15.1	13.6	12.3	11.0	9.81
Moderator Non-fuel Region (Rad, Cond)	10.8	8.6	7.6	6.67	5.8	4.99
<u>Grid Plate and Manifold Losses</u>	<u>0.7</u>	<u>0.6</u>	<u>0.5</u>	<u>0.5</u>	<u>0.4</u>	<u>0.4</u>
TOTAL SUPERHEATER ENERGY (Btu/hr x 10 ⁻⁵)	1442.6	1261.8	1166.1	1076.3	986.2	888.1
" " " " (M.W.)	42.27	36.97	34.17	31.5	28.90	26.0
<u>BOILER</u>						
Boiler Latent Heat	4486.6	4486.6	4486.6	4486.6	4486.6	4486.6
Boiler Sensible Heat	859.0	871.4	876.5	881.6	887.7	893.4
Vessel Heat	2.1	2.1	2.1	2.1	2.1	2.1
Control Rod Drives Heat	1.7	1.7	1.7	1.7	1.7	1.7
<u>Pump Recirculation Line Losses</u>	<u>0.6</u>	<u>0.6</u>	<u>0.6</u>	<u>0.6</u>	<u>0.6</u>	<u>0.6</u>
TOTAL BOILER ENERGY (Btu/hr x 10 ⁻⁵)	5350.0	5362.4	5367.5	5372.6	5378.7	5384.4
" " " " (M.W.)	156.76	157.12	157.27	157.42	157.60	157.76
<u>TOTAL REACTOR</u>						
Superheater	42.27	36.97	34.17	31.5	28.90	26.0
<u>Boiler</u>	<u>156.76</u>	<u>157.12</u>	<u>157.27</u>	<u>157.4</u>	<u>157.60</u>	<u>157.8</u>
TOTAL REACTOR ENERGY (M.W.)	199.03	194.09	191.44	188.9	186.50	183.8
Superheater Power Fraction	.2122	.1905	.1785	.1668	.1550	.1415
<u>PUMP WORK</u>						
Recirculation Pump Power (Btu/hr x 10 ⁻⁵)	24.5	24.5	24.5	24.5	24.5	24.5
<u>INLET STREAMS</u>						
Feedwater Heat	2144.3	2144.3	2144.3	2144.3	2144.3	2144.3
<u>Pump Seals</u>	<u>5.2</u>	<u>5.2</u>	<u>5.2</u>	<u>5.2</u>	<u>5.2</u>	<u>5.2</u>
TOTAL INPUT ENERGY (Btu/hr x 10 ⁻⁵)	8966.6	8798.2	8707.6	8622.9	8538.9	8446.

TABLE I
(Cont'd)

OUTPUT

	Superheater Outlet Temperature					
	T=825 F	T=775 F	T=750 F	T=725 F	T=700 F	T=675 F
Steam Energy	8773.3	8605.0	8514.5	8429.8	8346.0	8253.6
Purification Stream Energy	188.1	188.1	188.1	188.1	188.1	188.1
Steam Line Energy Loss	0.8	0.7	0.6	0.6	0.4	0.4
Vessel Energy Loss	2.1	2.1	2.1	2.1	2.1	2.1
Control Rod Drives Energy Loss	1.7	1.7	1.7	1.7	1.7	1.7
Pump & Recirculation Line Loss	0.6	0.6	0.6	0.6	0.6	0.6
TOTAL OUTPUT ENERGY (Btu/hr $\times 10^{-5}$)	8966.6	8798.2	8707.6	8622.9	8538.9	8446.5

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SECTION I

PATHFINDER BOILER THERMAL AND HYDRAULIC ANALYSIS

1.1 INTRODUCTION

An extensive analytic and experimental program has been completed for the design of the Pathfinder boiling section. This program included a detailed hydraulic and heat transfer analysis of the boiler fuel assemblies at both steady-state and transient conditions. The performance of the boiler control rods has also been evaluated.

The single pass boiler section of the Pathfinder reactor produces 616,125 LB/hr of 600 psig, 489 F steam. It is rated at 157.4 MWT., with the active boiler flow established at 57,400 GPM. An exit void content of 45.5 per cent is expected. The pertinent operating parameters are given in Table 1.1.

Table 1.1

Boiler Core Operating Parameters

Full Power	157.4 Mwt
Full Flow (ACTIVE)	57,400 GPM
Inlet Subcooling	3.8 Btu/LB
Steam Production	616,125 LB/HR
Exit Voids	45.5%
Pressure Drop	13.6 psi
Inlet Cooling Velocity	13.6 Ft/sec
Maximum surface cladding temp.	514 F
Maximum fuel temperature	4215 F
Average power density	1280 KW/FT ³ core
Average specific power	24.0 KW/Kg. of U
Average heat flux	122,000 Btu/hr/ft ²
Maximum heat flux	447,000 Btu/hr/ft ²

The boiler region of the reactor core consists of 96 fuel element assemblies each enclosed in a 5 inch square by 99 inch long zircaloy box.

Each fuel assembly contains a 9 by 9 array of fuel bearing zircaloy tubes, 6 ft. long on a 0.535 inch pitch. The tube size is reduced in the upper half of the boiler to compensate for void reactivity effects. A description of the boiler core layout is shown in Figure 1.1. The details of the step tube boiler element can be seen in Figure 1.2. The pertinent fuel element dimensions of the boiler core and fuel element are summarized in Table 1.2.

Table 1.2

Boiler Core and Fuel Element Details

Core

Mean outside diameter	69.0 in.
Mean inside diameter	31.3 in.
Active fuel length	72 in.
Volume	123 ft ³
Number of fuel elements	96

Fuel Element Assemblies

Bundle size @ room temp.	4.735 sq. in.
Number of fuel rods/bundle	81
Rod sections/rod	4

Lower Section

Upper Section

Pellet diameter @ room temp.	0.348 in.	0.310 in.
Clad thickness	0.028 in.	0.026 in.
Rod diameter @ room temp.	0.408 in.	0.367 in.

Box material	Zircaloy-2
Clad material	Zircaloy-2
Fuel material	UO ₂
Fuel density	10.41 gm/cc

The results of the thermal and hydraulic performance of the Pathfinder boiler, including a detailed pressure drop determination, void fraction evaluation and burnout ratio analysis, are presented in the following sections.

1.2 DISCUSSION

1.2.1 HYDRAULIC ANALYSIS

1.2.1.1 Description of Flow Paths

A schematic diagram of the primary flow paths is shown in Fig. 1. Subcooled water enters the lower inlet plenum from the recirculation piping, expands upward into the upper inlet plenum, enters the 96 boiler fuel elements, and leaves the fuel elements as a two-phase mixture. Steam is separated from the water at the free surface, in the demisters, and in the steam separators, and then enters the superheater. The remaining water is mixed with the feedwater and enters the recirculation piping through the exit nozzles. At this point it is necessary to define "leakage" as the term is used in this analysis. All water which enters the vessel from the recirculation piping but which does not flow through the 96 boiler fuel elements is termed "leakage". The remaining water is called "active" flow. In order to simplify the analysis, the leakage flow is assumed not to be heated as it moves through the core. Therefore the power requirement for providing steam to the superheater is assigned completely to the boiler fuel. Actually, some of this power will come from sources other than the boiler fuel and will be transferred directly to the leakage flow (for example, in the superheater moderator region). Note that this technique of assigning the total power requirement to the boiler fuel does not affect the mixed enthalpy of the boiler effluent, since the unheated leakage is assumed to completely mix with the active flow at the top of the core. Thus, there is a slight conservatism introduced in defining the power produced in the boiler fuel.

A schematic diagram showing both the primary and leakage flow paths is shown in Figure 1.3. There are four principal areas in which leakage can occur:

1. Moderator flow to superheater
2. Coolant flow to boiler control rods
3. From the upper inlet plenum through the clearance between the superheater baffle and the grid plate, up around the boxes to the outlet region
4. From the upper inlet plenum through the cooling holes in the under side of the grid plate out through the cooling holes on the outer edge of the grid plate.

It is recognized that the last type of leakage listed is not strictly compatible with the assumption that all of the unheated leakage is mixed with the active flow at the top of the boiler. However, this leakage will later be shown to be only 0.2 per cent of the total boiler flow, and the discrepancy has been neglected.

Although some sub-cooled nucleate boiling (local boiling) is expected to occur in the first quadrant under normal operating conditions, it has not been accounted for in the pressure drop analysis. This is because this boiling is expected to produce no steam which will significantly affect the coolant flow but only steam bubbles forming and collapsing along the heat transfer surfaces (Ref. 1.1, 1.2).

In order to simplify the analyses, the static pressure used to evaluate the fluid properties was assumed to be constant at the outlet value at all elevations in the fuel elements. The error due to this assumption is negligible compared to the uncertainties in the pressure drop calculations.

Throughout the preceding discussion, it has been implied that the core pressure drop could be computed from the characteristics of one typical fuel element. The particular characteristic of interest is the fuel element power level, since it is known that the pressure drop flow relationship is sensitive to power level. The assumption is that the average core pressure drop is the same as the pressure drop in an average power element. This assumption has been examined analytically for the radial boiler power shapes available and found to result in errors of less than 0.1 per cent.

1.2.1.2 Computation of Pressure Drops - Leakage Flow Paths

The computation of pressure drops for the leakage flow paths was based upon application of classic isothermal single-phase friction and form loss coefficients. The non-isothermal and/or boiling effects either do not exist or are negligible for all leakage path calculations except for the flow through a control rod channel where the rod is inserted. However, the nominal control rod cooling leakage calculations are based upon all rods being withdrawn, since this represents the maximum leakage. The reduction in leakage has been accounted for in establishing the control rod cooling requirements for an inserted rod, but for simplicity will be ignored here. Therefore, non-isothermal and boiling effects on the leakage pressure drops have been neglected in this section.

1.2.1.3 Determination of Leakages

Once the resistance of each of the leakage paths has been identified and the available driving forces in terms of pressure drops associated with the active flow have been established, the computation of the leakage rates is straightforward. An active boiler flow rate of

54,000 GPM was assumed. Assuming an outlet pressure of 615 psia, static pressures were computed for the upper inlet plenum and the outlet region opposite the holes around the edge of the grid plate. Using these pressures and the previously computed resistances of the leakage paths, the leakage flows were computed as follows in terms of per cent of total boiler flow:

1. Moderator flow to superheater - 6% *
2. Coolant flow to boiler control rods - 2.5% *
3. Between the superheater baffle and the grid plate - 2.0%
4. Through the holes around the edge of the grid plate - 0.2%
5. Total leakage - 10.7%

* Design requirements.

Thus in order for the assumed active flow of 54,000 gpm to have been available, the total flow entering the reactor would necessarily have been 60,470 gpm.

It is noted that these leakage calculations are rigorously applicable only at nominal full power conditions with a total flow of 60,470 gpm. However, it will be assumed that the relative leakage figures (per cent of total flow) apply at all flows and all power levels. The errors due to this assumption are negligible; it is true that the relative leakage flows will be slightly reduced as the boiler power is reduced. (Assuming total flow remains essentially constant.)

1.2.1.4 Dependence of Pressure Drops on Core Operating Parameters

Following the determination of the core leakage as a per cent of total flow, standard procedure would call for establishing recirculation circuit pressure drop vs total flow curve. The intersection between this curve and the pump-head curve would establish the core operating point (the actual total flow). This will be done, but not before several complicating

factors have been identified. These factors all stem from the effect of boiling on the core pressure drop. Since the pressure drop is sensitive to the amount (or degree) of boiling, and the core operating point is sensitive to the core pressure drop, any variable which affects the amount of boiling in the core will affect total boiler flow. Such variables are as follows:

1. Core power level
2. Core inlet subcooling
3. Core pressure
4. Axial power distribution.

An additional complication arises because these variables plus total boiler flow are all interrelated due to the design of the power plant and method of control. Nevertheless, in order to establish the effect of these variables on core pressure drop, studies have been carried out in which the parameters were varied separately and independently.

The effect of separately varying flow, power, and inlet sub-cooling on the core pressure drop is shown in Figure 1.4. Reasonable changes in the axial power shape have been shown to change core pressure drop by a total of about 0.5 psi. The effect on pressure drop of varying pressure while holding inlet sub-cooling fixed is considered to be negligible over a range of at least ± 65 psi.

The importance of these effects will be discussed in the next section.

1.2.1.5 Determination of Boiler Flow Rates

The first step is to determine the so-called nominal boiler flow rate. This was based upon the following boiler parameters:

Power	157.4 MW (100%)
Pressure	615 psia
Inlet Sub-cooling	3.8 BTU/lb.

The boiler pressure drop vs flow curve (obtained by adding the vessel and piping losses to the core losses) for these conditions and the latest pump head curve are shown in Figure 1.5. The nominal boiler flow was established at 66,100 gpm.

Next, uncertainties in the hydraulic analyses were considered.

1. Core boiling friction and form losses $\pm 25\%$
2. Other non-boiling losses $\pm 7.5 \%$
3. Other circuit losses $\pm 10\%$

The pressure drop vs flow curves representing these uncertainties in the extreme combinations are also shown in Figure 1.5. It is seen that, considering these uncertainties, the minimum boiler flow is 63,200 gpm and the maximum flow is 69,300 gpm. The active flow rates are 56,400 gpm and 61,800 gpm respectively.

The effect of power level changes was taken from Figure 1.4 and a separate analysis for zero power. The corresponding pressure drop vs flow curves are shown in Figure 1.6 and the resulting boiler flow rates are presented as a function of power in Figure 1.7.

The effect of changes in inlet subcooling was taken from Figure 1.4 and the pressure drop flow curves are presented in Figure 1.8.

Using the small perturbation approach, the following expression can be used to predict approximately the boiler flow as a function of power and inlet subcooling:

$$\frac{\Delta F}{F} = 0.07 \frac{(100 - \% \text{ Power})}{100} - 0.001597 (\Delta H_{\text{sub}} - 3.8)$$

Considering that changes in axial power shape can affect core pressure drop by 0.5 psi, the corresponding effect on boiler flow would be 0.46% ($\pm 0.23\%$).

1.2.1.6 Natural Circulation Flows

Interest in the flow decay following a complete loss-of-flow accident has stimulated a separate study to estimate the natural circulation flow rate as a function of power level at normal conditions of temperature and pressure. This analysis was conducted using the principles previously described and assuming that the pump impellers continued to rotate. The results of this study are shown in Figure 1.9.

1.2.2 THERMAL ANALYSIS

1.2.2.1 Void Distribution

The void distribution in the boiling section was considered on the basis of each of the 96 elements acting as a parallel resistance. Iteration between thermal, hydraulic and nuclear diffusion analyses produced the final expected flow and corresponding void distribution among the 96 elements for normal full power conditions. The ratio of element flow to average flow was found to vary from 0.91 to 1.19; the ratio for all but 20 elements is expected to fall between 0.94 and 1.10. The corresponding exit void fractions were found to vary from 30 to 55 per cent compared with the exit void fraction averaged for the whole core of 47.5 per cent. The void fraction correlation used in this study was the Modified Martineilli Relationship (Ref. 1.3). The void distribution was calculated on the basis of the axial power distribution shown in Fig. 1.10.

1.2.2.2 Fuel and Clad Temperatures

The boiler heat balance for rated reactor power showed that the boiler must supply 157.4 MW. In the fuel temperature calculations, all this heat is assumed to be generated in the fuel itself.

(This is slightly conservative, since some heat is generated in the water.)

Cladding temperatures were calculated from the equation:

$$\Delta T_{\text{clad}} = \frac{\phi_o R_o}{K_{\text{zirc}}} \ln \frac{R_o}{R_i}$$

where ϕ_o is the heat flux at the outer surface, R_o and R_i are the outer and inner radii, and K_{zirc} is the thermal conductivity of the zircaloy cladding. Fuel temperatures were calculated from the equation:

$$\Delta T_f = \frac{\phi_o R_o}{2 K_{\text{fuel}}}$$

where K_{fuel} is the conductivity of the homogenized fuel and gases inside the clad and is conservatively taken to be 0.85 (Ref. 1.4). The film temperature rise for boiling was found from the Jens and Lottes correlation: (Ref. 1.5):

$$\Delta T = 60 \left(\frac{\phi_o}{10^6} \right)^{.25} e^{-P/1000}$$

The values of heat flux (ϕ_o) for the above equations were arrived at by using the boiler total power, the calculated neutron flux distributions and the appropriate hot spot factors. The maximum temperatures were arrived at by using the following peaking factors:

<u>Item</u>	<u>Heat Flux Factor</u>
Radial Flux	2.10
Axial Flux	1.42
Fuel Loading	1.05
Fuel Eccentricity	1.25

The pertinent fuel centerline and clad inner surface temperatures are given in the following table:

	<u>Lower Half</u>	<u>Upper Half</u>
Clad (Ave.)	553 F	539 F
(Max.)	672	662
Fuel (Ave.)	1918	1494
(Max.)	4215	3845

1.2.2.3 Boiler-Fuel-Pin End-Cap Temperatures

An analysis to establish the temperature distribution in the vicinity of fuel-pin end-caps was completed. The temperatures were computed on the IBM-704 computer, using the PDQ neutron-diffusion program. Both ends of a fuel pin, i.e., with a fission-gas space and with an alumina-water insulator, were studied. The analysis was based on the following assumptions:

1. The pellet and gases between the pellet and clad, were treated as a homogeneous heat-generating material having a thermal conductivity of $0.85 \text{ Btu/hr-ft-}^{\circ}\text{F}$ and a heat generation rate of $7.93 \times 10^7 \text{ Btu/hr-ft}^3$.
2. The heat generation rate was taken as the maximum value in the core.
3. The gas in the fission gas space was assumed to be a conducting region only (no convection) with a thermal conductivity of $0.1 \text{ Btu/hr-ft-}^{\circ}\text{F}$ (the spring in the fission gas space was not considered).

The results of the analysis are presented in Figures 1.11 and 1.12. Actual fuel element temperatures may be obtained by adding 489 F to the values in the figures.

Some difficulty was experienced with specifying the appropriate heat transfer boundary conditions in the neutron diffusion code. As a result, the point on the clad surface where the heat transfer mechanism changes from single-phase forced convection to nucleate boiling had to be estimated in order to solve for the temperatures. Inaccuracies in this estimate caused the calculated temperatures near transition to be questionable. While the analysis could be continued by iterating for the proper boundary condition representation, it was considered reasonable to approximate the results in this area by continuous temperature curves. This approximation is indicated

in the curves by the dotted portions.

1.2.2.4 Analysis of Fission Gas Release

To determine the fission gas release within the step tube UO_2 boiler fuel rods, an IBM-704 program - GRER (Gaseous Release Evaluation Routine) was constructed (Ref. 1.6). Stable gaseous fission products are assumed to be released from the solid UO_2 matrix by a temperature dependent diffusion process. Fuel temperatures are determined, assuming only radial heat conduction through the fuel material. A power profile table provides the variation of boiler power with axial length along the boiler fuel rod.

As the hottest boiler fuel pin will dictate the size of the fission gas spaces within the entire boiler core, time integrated hot channel factors obtained by physics burnup calculations were employed to thermally describe the boiler pin. A constant thermal conductivity for the UO_2 pellet of 1.0 Btu/hr-ft- $^{\circ}\text{F}$ was used. The pellet surface temperature was 600 F, constant along the fuel length.

The fuel pin was divided into four axial elements, with the total fractional release in each element calculated for individual fission gas space sizing. Each element was subdivided into 20 axial segments consisting of 10 radial rings in order to facilitate the estimation of the radial and axial temperature profiles. In each element the density of the UO_2 fuel pellet was assumed to be 95% of theoretical.

Results of the fission gas release study are shown in Table 1.3.

Table 1.3

Fission Gas Release Analysis of Pathfinder
Boiler Core

Hot Channel Factor	<u>1.554</u>	<u>1.638</u>	<u>1.712</u>
Fuel Pin Surface Temp - °F	600.	600.	600.
Total Channel Power, Btu/hr x 10 ⁶	8.685	9.155	9.568
Irradiation Time, days	515.	515.	515.
Theoretical Density, %	95.	95.	95.
<u>AXIAL ELEMENT #1</u>			
Average Element Temp - °F	1334.	1374.	1409.
Volumetric Power, Btu/hr-in ³ x 10 ⁻⁴	1.591	1.677	1.752
Fuel Element Length, in.	16.888	16.888	16.888
Fuel Pin Radius, in.	0.175	0.175	0.175
Total Release for Element, %	4.82	6.08	7.36
Maximum Centerline Temp - °F	2878.	3001.	3110.
<u>AXIAL ELEMENT #2</u>			
Average Element Temp - °F	1619.	1675.	1723.
Volumetric Power, Btu/hr-in ³	2.209	2.329	2.434
Fuel Element Length, in.	16.879	16.879	16.879
Fuel Pin Radius, in.	0.175	0.175	0.175
Total Release for Element, %	10.24	13.92	17.35
Maximum Centerline Temp - °F	3064.	3197.	3314.
<u>AXIAL ELEMENT #3</u>			
Average Element Temp - °F	1507.	1557.	1600.
Volumetric Power, Btu/hr-in ³	2.276	2.400	2.508
Fuel Element Length, in.	16.879	16.879	16.879
Fuel Pin Radius, in.	0.175	0.175	0.175
Total Release for Element, %	5.87	8.08	10.35
Maximum Centerline Temp - °F	2765.	2882.	2985.
<u>AXIAL ELEMENT #4</u>			
Average Element Temp - °F	987.	1008.	1026.
Volumetric Power, Btu/hr-in ³	.972	1.025	1.071
Fuel Element Length, in.	16.888	16.888	16.888
Fuel Pin Radius, in.	0.175	0.175	0.175
Total Release for Element, %	2.00	2.02	2.04
Maximum Centerline Temp - °F	2082.	2162.	2232.

The four axial element results starting from the bottom of the core (axial element #1) are shown. Three integrated average hot channel factors of 1.554, 1.638, and 1.712 were investigated. The maximum fission gas release was 17.35%, occurring in axial element #2 with the 1.712 hot channel

factor. In general, the release was below 10 per cent, with the top element of the boiler core (axial element #4) at or below the minimum 2 per cent cutoff release value.

1.2.2.5 Analysis of Burnout

An investigation of analytical methods suitable for calculating the burnout safety margin in the Pathfinder boiler was conducted. The principal complicating factors concerning the burnout analysis are the flow redistribution among fuel elements and within fuel elements. These arise primarily from the effect of boiling on flow resistance. Increased boiling causes increased resistance, causing less flow which results in more boiling. Fortunately, this reaction appears to be well damped in Pathfinder, but the net effects on flow are very important. Burnout data obtained at Greendale and other laboratories indicate that the burnout heat flux* is strongly dependent on local enthalpy or quality. Since the quality is dependent on flow, the redistribution effects must be considered in burnout calculations.

The Pathfinder boiler core experiences two different mechanisms of flow redistribution. One mechanism pertains to differences between the total flows through individual fuel elements having different average power levels. The second mechanism pertains to variations of flow within a given fuel element cross section, due to some fuel pins being hotter than others. The among-element mechanism is termed "bounded", since flow can neither enter or leave the element once it enters from the bottom. The within-element mechanism is termed "unbounded", since flow can either enter or leave the vicinity of a pin after it enters this region from the bottom.

* Heat flux greater than that which gives rise to nucleate boiling.

It is apparent that each type of redistribution must be studied separately. The IBM-704 ART Code (Ref. 1.7) has been used to analyze the among-element case. Important assumptions were as follows:

1. The total pressure drop across each of the 96 fuel elements is the same.
2. The total pressure drop across each of the elements can be represented by the pressure drop across an element of average power receiving average flow.
3. The flow and power distributions at any horizontal cross section within a fuel element are uniform.

Additional consideration was given to the effect of power, flow inlet sub-cooling, and axial power shape on this among-element flow distribution. Small changes in the parameters of power, flow, and inlet sub-cooling were found to have a negligible effect on the flow distribution; major changes in axial power shape were also found to have a negligible effect.

The IBM-704 CAT Code (Ref. 1.8) has been used to analyze the within-element type of flow redistribution. Important assumptions were as follows:

1. The axial pressure profile in a given fuel element can be represented by the axial pressure profile in an element of the same average power and the same total flow.
2. The static pressure distribution at any horizontal cross section within a fuel element is uniform.

An additional complicating factor is that the within-element results have to be superimposed upon the results of the among-element analysis.

Consider, for example, two fuel elements having different total power levels but having the same local power level along a column of fuel pins. The total power level in one element is the same as core average; the total power level

in the other is higher than core average. Since the flow in the element of average power is the core average, only the within-element mechanism of flow redistribution need be considered. However, the total flow in the element of higher than average core power will be less than core average.

In order to establish which combination would lead to the most severe thermal condition, several different cases were analyzed. It was found that for a given local power, the minimum burnout ratio is essentially independent of average fuel element power, i.e. each type of flow redistribution results in about the same thermal conditions at the point of minimum burnout ratio.

Consideration was given to the possibility that this result was fortuitous, and was only valid for the particular axial power shape studied. However, similar studies were conducted for four different axial power shapes and the same result was found. This finding serves to simplify later core analyses, since the limiting condition can be considered as a pin column of maximum power located in a fuel element of average power, thus requiring only one mechanism of flow redistribution to be included.

Following the establishment of methods for including the flow redistribution effects, attention was directed toward transient analyses. The objective of this part of the investigation was to establish whether the flow transients of interest were slow enough to be handled by a series of steady-state analyses (quasi-steady-state method of analysis). Both the ART and CAT codes are capable of solving a form of the transient flow equations; however, this use of the codes requires large amounts of IBM-704 computing time. The ratio of computing time to real time varies from about 300 to 1000 depending upon the detail required, whereas a steady-state analysis requires only a few minutes of computing time.

The complete loss of flow accident was selected as a basis for comparing the quasi-steady state and transient methods of analyses. This accident represents the most rapid flow transient of interest.

Initial values of power, pressure, and inlet sub-cooling were taken at nominal full power conditions. The hot pin column was assumed to be located in a fuel element of average power, while the total fuel element flow was assumed to be about 90 per cent of average (plenum factor).

The core flow and average heat flux during the transient were taken from analog computer results. It was concluded that the quasi steady-state method is acceptable for this accident, and also for any accident with less rapid flow changes.

Experimental Burnout Data

Burnout limit calculations were based on data obtained from one pin burnout tests. The one pin test sections were all six feet long with annular flow passages, and had length to equivalent diameter ratios (L/D_e) which varied from 78 to 151. Mass velocities ranged from 0.5×10^6 to 2.5×10^6 LB/HR-FT². The bulk of the data were taken at 600 psig, but some points were also taken at 200 and 400 psig.

Correlation of the data was accomplished by plotting burnout heat flux versus local steam quality. It was found that all of the data at a given pressure fell within ± 20 per cent of a correlating line and the 600 psi data are shown in Figure 1.13. The equation of this line biased by a factor of 0.75 to cover all the data points conservatively was then used in the burnout limit calculations. The 600 psi design equation is:

$$\frac{\phi_{BO}}{10^6} = .0536 \left(\frac{H}{10^3} \right)^{-4.12}$$

This equation is valid for steam qualities from zero to 25 per cent ($H = 475$ to 657 BTU/LB), since this is the range covered by the experimental data.

No significant effect of variation in L/D_0 or mass velocity was discernable in the burnout data for any of the three pressures tested. It was also noted that the data for 400 psig and 200 psig were lower than the 600 psig data by approximately 5% and 30% respectively.

The burnout data correlation was compared to the burnout test results of Levy (Ref. 1.9) for internally heated annuli at 1000 psi. The G.E. data were higher than the A-C 600 psi data by approximately 20%. Both sets of data exhibited very nearly the same slope when plotted on ϕ_{BO} versus quality coordinates.

Burnout Margin

Average heat fluxes were multiplied by hot channel factors to arrive at the most severe conditions for burnout limit calculations. Neutron flux calculations showed that a radial hot channel factor of 2.10 was necessary. In addition, a local hot spot factor of 1.25 was used to cover engineering uncertainties (tolerances on dimensions, fuel homogeneity, and fuel eccentricity).

The burnout safety margin was investigated by using a parameter called burnout ratio (BOR), which is defined as the ratio of the burnout heat flux at a point in the core to the actual heat flux at the same point. Calculation of this parameter was accomplished by using the CAT IBM-704 program.

At steady-state full power operation, the minimum BOR was found to be 1.9. It occurs just downstream from the core center at a local steam quality of 5.5 per cent. At this point, the local heat flux (including hot channel factors) is $430,000$ BTU/HR-FT².

Burnout Margin at Partial Powers

When the boiler is operated at powers less than full power and recirculation flows less than full flow, the burnout ratio will vary through a wide range of values depending upon the combination of power and flow chosen. To determine safe power levels for various recirculation flow rates, the CAT IBM-704 program was used to determine burnout ratios over a wide range of these parameters. From these parameter studies, the conditions at which the minimum BOR was equal to 1.5 could be determined. This value was taken as a safe limit for reactor operation. A plot of boiler power versus boiler recirculation flow for BOR = 1.5 is shown in Figure 1.14.

Since fuel temperatures are only dependent upon the heat flux out of the pellets, partial power operation will obviously result in lower fuel temperatures than full power operation as long as burnout is not encountered.

1.2.3 Transient Performance

1.2.3.1 Burnout Ratio

The CAT IBM-704 program was used to analyze the various possible reactor transients considering appropriate corrective actions executed by the reactor protection system. The accident giving rise to the smallest value of the burnout ratio was found to be a rod withdrawal accident initiated at full power, for which the burnout ratio drops to 1.5 before the protective action (scram in this case) is effective. For other over-power and/or loss of pressure accidents, the burnout ratio is always greater than 1.5, and for all loss of flow accidents, including loss of all three pumps, the burnout ratio is never less than 1.9.

Potential instrument inaccuracies, transient overshoot, and dead-bands have been considered in establishing set points of the reactor instrumentation systems. The boiler steady-state power levels which would give rise to a BOR of 1.5 are shown in Figure 1.14 as a function of recirculation flow. These relationships have been used to evaluate reactor transients initiated at other than full power - full flow conditions. For example, at 50 per cent recirculation flow, the burnout ratio would not drop to 1.5 until the boiler power reached 91 per cent of full power with no inlet subcooling.

No geometry factor was included to account for uneven spacing of rods. However, the 1.10 plenum factor is somewhat conservative in view of the test data on flow maldistribution.

The CAT code treats each element as an open lattice (i.e. no radial pressure gradients within the element), and computes the flow reduction near the hottest pins. The flow distribution among elements was determined by a conventional parallel channel calculation based upon the average characteristics of each element and using the plenum factor (1.1) for the hottest locations.

1.2.3.2 Flow Oscillations

The possibility of flow oscillations occurring in one or more boiler fuel elements has been considered in the hydraulic analysis and test program. The high recirculation ratio in Pathfinder (42 to 1) and attendant low steam qualities result in small departures from the non-boiling flow-pressure drop relationships. For example, the core pressure drop at full power varies with flow to the 1.65 power whereas the pressure drop at no power (non-boiling) varies with flow to the 1.91 power. These

relationships have been verified by 600 psi boiling and non-boiling experiments. This degree of similarity to a non-boiling system is considered to preclude the occurrence of hydraulic flow oscillations in individual elements.

It is noted that the maximum exit void fraction for any of the boiler fuel elements is 56 per cent, which is less than maximum values existing in other boiling reactors which have demonstrated stability.

1.2.4 Boiler Control Rod Analyses

1.2.4.1 Hydraulic Analysis of Control Rods.

A test was conducted to determine the size of holes in the control rod stub which would deliver the required amount of cooling water to the boiler control rods. A schematic diagram of the test apparatus is shown in Figure 1.15.

The flow path consists of a section representing the pipe in the reactor grid plate, a base plate with four lateral plenums representing the base plate of the fuel element box, a cruciform stub with five holes in each arm (20 total) representing the control rod stub, a simulated control rod channel and control rod (2 instead of 6 ft. long), and an outlet plenum. Three stubs with holes sizes of 0.2, 0.25, and 0.30 in. dia. were tested. The simulated control rod was positioned fully at the bottom, 1/2 in. from the bottom, 1 in. from the bottom, and 1 in. into the control rod channel, and fully withdrawn, to simulate important control rod positions. The more critical position is the fully inserted position, since the control rod then rests on the stub restricting the coolant flow out of the stub.

The results of the experiment are presented in Figure 1.16. Overall loss coefficients are presented for the stub, base plate, and inlet pipe, corresponding to the measured pressure drops between tap 0 and taps 1 and

2 (which were the same). These measured coefficients are compared to the theoretical prediction for the fully inserted and withdrawn positions. Loss coefficients for all intermediate rod positions were essentially the same as the fully withdrawn case. The agreement between the data and the calculations is within the expected errors of the analysis, which are large because of the complex geometry. The poorer agreements for the largest sized holes is due to the fact that the pressure drops for this case more significantly consist of highly uncertain losses, e.g. turning losses, etc. The coefficients as presented in Figure 1.14 were found to be nearly independent of Reynolds number at the higher values of Reynolds numbers achieved during the tests.

Based on the results, a hole size of 0.25 in. dia. was selected for the boiler control rod stub. This hole size will deliver nominally 2.5 per cent of the total 64,300 gpm recirculation flow to the 16 boiler control rods. Considering uncertainties in the stub test results, and uncertainties in the boiler pressure drop, this flow could be as little as 80 gpm for an inserted control rod, including the boiling pressure drop along the rod. In this extreme case, the exit quality from the rod channel would be no more than 1.7 per cent, accounting for uncertainties in the control rod heating rate.

1.2.4.2 Temperature Distribution in a Boiler Control Rod

Temperature distributions were computed using PDQ02 for a boiler control rod at the axial location of maximum heat generation rate. The rod was considered to be off-center in the control rod channel in order to include a maximum condition for bending stress considerations.

The sixteen boiler control rods are cruciform in shape as shown in

Figure 1.17. The boron stainless steel neutron poison section is 72 inches long. The blades are 1/4 inch thick having an overall width of 10-7/16 inches.

The calculation results along with the boundary conditions used are presented in Figure 1.18. The heat generation rate in the rod was taken as 3.03×10^6 Btu/hr-ft². This rate was assumed to consist of approximately one-half fast effects and one-half thermal effects. The fast effects were further assumed to give rise to a uniform power distribution around the rod perimeter. The thermal effects were assumed to give rise to a power distribution identical to the flux distribution around the rod perimeter.

1.3 REFERENCES

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1.4 ILLUSTRATIONS

Pertaining to Section I

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Fig. 1.1...Boiler Core Layout (43-002-107)

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Fig. 1.3...Pathfinder Boiler Flow Paths (43-025-287)

NOMINAL CONDITIONS

ACTIVE FLOW 54000 GPM
 POWER 159.8 MW
 INLET SUBCOOLING(ΔH_{SUB}) 3.8 BTU/LB

Fig. 1.4...Effects of Flow, Power and Inlet Subcooling on Boiler Core Pressure Drop (43-025-288)

DETERMINATION OF BOILER FLOW

Fig. 1.5...Determination of Boiler Flow (43-025-243)

Fig. 1.6...Pressure Drop-Flow Curves for Various
Boiler Powers (43-025-240)

Fig. 1.7...Effect of Power on Boiler Flow (43-025-289)

Fig. 1.8...Determination of Boiler Flow as a Function
of Inlet Subcooling (43-025-290)

Fig. 1.9...Natural Circulation Performance of
Pathfinder Boiler (43-025-291)

Fig. 1.10...Axial Power Distribution (43-025-042)

Fig. 1.11...Fuel Rod End Cap Temperature Distribution - Female (43-024-414)

Fig. 1.12...Fuel Rod End Cap Temperature Distribution -
Male (43-024-415)

Fig. 1.13...Single Rod Burnout Heat Flux Data (43-024-517)

Fig. 1.14...Burnout Limits of Plant Operation (43-025-244)

CONTROL ROD STUB TEST

Fig. 1.15...Control Rod Stub Test (43-024-514)

Fig. 1.16...Control Rod Stub Pressure Drop Coefficients (43-024-515)

NOTE #1:-

- (A) MK*501
1. CLAMP IT*7 (IT*8 IN WELDING FIXTURE, TOOL*) NO WELDING GAP.
2. WELD AS PER INDICATED AT (H-2) ON DIAGRAM "X".
3. GRIND RADIUS ON EDGES OF IT*8 TO BLEND WITH RADIUS ON IT*7.
4. CLAMP IT*5, 7, 8, 16 IN WELDING FIXTURE TOOL*.
5. WELD AS PER INDICATED AT (H-5) ON DIAGRAM "X".

NOTE #2:-

- (A) MK*502
1. CLAMP IT*4 (IT*5 IN WELDING FIXTURE, TOOL*) NO WELDING GAP.
2. WELD AS PER INDICATED AT (H-2) ON DIAGRAM "X".
3. GRIND RADIUS ON EDGES OF IT*5 TO BLEND WITH RADIUS ON IT*4.
4. CLAMP IT*5, 4, 9, 13 IN WELDING FIXTURE TOOL*.
5. WELD AS PER INDICATED AT (H-5) ON DIAGRAM "X".

NOTE #3:-

- (A) MK*503
1. CLAMP (2) OF MK*501 (1) OF MK*502 INTO WELDING FIXTURE, TOOL* 97-501-404-601, MAKING SURE THAT MK*502 IS CENTERED IN THE FIXTURE.

2. WELD (1) OF MK*501 TO MK*502 WITH A SERIES OF $\frac{1}{8}$ (MAX) FILLET WELDS, (SEE SECTION "A-A") FOLLOWING THE SEQUENCE IN DIAGRAM "X" (51-67).
3. ROTATE FIXTURE 180°; WELD (1) OF MK*501 TO MK*502 FOLLOWING THE SEQUENCE IN DIAGRAM "X" (68-76).
4. REMOVE FROM TOOL AFTER COOLING.
5. BLEND ENDS OF EACH FILLET WELD LENGTH WHERE NECESSARY.

NOTE #4:-

- (A) MK*506
1. CLAMP (1) OF IT*12 (2) OF IT*15 INTO WELDING FIXTURE, TOOL* , MAKING SURE IT*12 IS CENTERED IN FIXTURE.
2. WELD (1) OF IT*15 TO IT*12 WITH A SERIES OF $\frac{1}{8}$ (MAX) FILLET WELDS (SEE SECTION "B-B") FOLLOWING THE SEQUENCE IN DIAGRAM "Y" (77-91).
3. ROTATE FIXTURE 180°; WELD (1) OF IT*15 TO IT*12 FOLLOWING THE SEQUENCE IN DIAGRAM "Y" (92-106).
4. REMOVE FROM FIXTURE AFTER COOLING.
5. BLEND ENDS OF FILLET WELD LENGTH WHERE NECESSARY.

NOTE #5:-

- (A) MK*508
1. CLAMP (1) OF MK*503 (1) OF MK*506 IN WELDING FIXTURE, TOOL* 97-501-404-601.

2. WELD AS PER INDICATED IN DETAIL "E" FOLLOWING SEQUENCE (107-112).
3. SLIP IT*17 OVER END OF TAIL; SET IN FIXTURE. ADJUST LENGTH USING . CLAMP IT*17 SECURELY.
4. WELD AS PER INDICATED IN DETAIL "E".
5. INSPECT FINAL DIMENSIONS USING TOOL* 97-502-854-601 TO INSPECT ENVELOPE.
6. ATTACH IT*25 AS PER SECTION "E-E".

NOTE #6:-

25 OF THE PLANES OF IT*17; REMAINDER OF BOILER CONTROL ROD SHALL BE WITHIN .060 T.I.R. OF EACH OTHER.

NO.	REV.	DESCRIPTION	MATERIAL	PART NUMBER	DATE
1	1	BOILER CONTROL ROD		43-500-970	508
2	1	BLADE ASSEMBLY		43-500-970	509
3	1	BLADE SUB-ASSEMBLY		43-500-970	502
4	1	BLADE		43-500-970	507
5	1	TIP		43-500-970	501
6	1	BLADE SUB-ASSEMBLY		43-500-970	501
7	1	BLADE		43-500-970	501
8	1	TIP		43-500-970	501
9	1	TAIL LATCH ADAPTOR ASSEMBLY		43-500-970	501
10	1	TAIL SECTION ASSEMBLY		43-500-970	506
11	1	TAIL SUB-ASSEMBLY		43-500-970	508
12	1	TAIL		43-500-970	507
13	1	CONNECTOR		43-500-970	502
14	1	TAIL SUB-ASSEMBLY		43-500-970	508
15	1	TAIL		43-500-970	507
16	1	CONNECTOR		43-500-970	502
17	1	CONTROL ROD LATCH ADAPTOR		43-500-970	507
18	1	FILLER WIRE - 3/16 DIA (ASTM A198 C1508 1/2)		THIS	018
19	1	ANTI-SPATTER (TACK WELD)		THIS	019
20	1	FLUID PENETRANT INSP. SPEC.		43-500-970	401
21	1	CLEANING PROC. SPEC.		43-500-970	402
22	1	MARKING SPEC.		43-500-970	401
23	1	FABRICATION SPEC.		43-500-970	401
24	1	WELD PROC. SPEC.		43-500-970	401
25	1	1/16 DIA 3/8 LG. WAGON BOX RIVET		43-500-970	501
26	1	WELD PROC. SPEC.		43-500-970	401

DIAGRAM "X"

DIAGRAM "Y"

DETAIL "C"

DETAIL "E"

ALL TWIST & WARP TO BE HELD TO A MINIMUM SO THAT THE BLADES OF THE TAIL SECTION ASSEMBLY FIT WITHIN THE .3125 ENVELOPE FOR THE ENTIRE 8-10 1/2\"/>

SECTION "E-E"

SECTION "B-B"

SECTION "A-A"

NOTE #2:- ALL TWIST & WARP TO BE HELD TO A MINIMUM SO THAT THE BLADES OF THE BLADE ASSEMBLY FIT WITHIN THE .3125 ENVELOPE FOR THE ENTIRE 8-10 1/2\"/>

Fig. 1.17...Boiler Control Rod (43-500-970)

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-55-
TEMPERATURE DIFFERENCE BETWEEN ROD & COOLANT (F)

F. I. 18...Control Rod Temp. Distribution (43-024-416)

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SECTION 2

PATHFINDER SUPERHEATER THERMAL AND HYDRAULIC ANALYSIS

2.1 INTRODUCTION

The design of the nuclear superheater has included an extensive heat transfer and fluid flow analysis supported by an out-of-pile experimental program. All necessary phases of the test program have been completed.

The superheater section of the Pathfinder reactor is rated at a full power value of 31.5 Mwt. It receives the 616,125 lb/hr of saturated steam (489 F produced in the boiler section and superheats it to 725 F.

The superheater core, consisting of 468 individual cells and surrounding water moderator region, is centered within the boiler region. It has a mean diameter of 31.3 inches with an active fuel length of 71.5 inches. The 468 superheater positions are apportioned as follows: 52 for control rods, 1 for the source, 3 for nuclear instrumentation, and 412 active fuel elements. The steam flow required to cool the control rods, source, and nuclear instrumentation is termed "leakage" flow and the remaining steam flow is called "active" flow. The total "active" flow is 608,935 lb/hr (1478 lb/hr/element) with the total "leakage" coolant flow requirement established at 7190 lb/hr.

The Pathfinder superheater fuel element is composed of two concentric fuel tubes centered about a poison rod. This integral assembly is inserted into an insulating container. This insulating container consists of two concentric tubes that are sealed together at the top and are open at the bottom - providing an insulating layer of stagnant steam between these tubes.

When the fuel element is inserted into the insulating tube, three concentric annular steam flow passages are formed. Three straight wires within each channel (coolant and insulating passages) provide the spacing support and maintain the concentric flow passages.

The thermal design objective was to reduce the fuel metal temperature to as low as possible - consistent with the flow and power requirements of the superheater. From a safety standpoint it is desirable to obtain as low a fuel surface temperature as possible. Excessive cermet fuel element temperatures coupled with high burnup greatly restrict the lifetime of the core. Minimum temperature difference across the fuel plates at the hot spot was established to minimize the potential bowing and thermal stress problems associated with large element temperature differences.

Fuel assembly tolerances were used in the hot channel analysis to describe the minimum coolant gaps. Hot channel factors were used including thermal conduction results with respect to the influence on fuel temperature of the wire spacers and lack of bonding of the cladding.

An evaluation of the component steam flow requirements was performed to insure sufficient cooling of all superheater auxiliaries. Leakage steam has been provided to cool the neutron source, ion chambers and control rods so as not to exceed a tolerable surface temperature.

The superheater was analyzed under all modes of reactor operation. Element performance under reactor shutdown conditions with zero steam coolant flow was investigated over a wide range of low superheater powers. The Pathfinder reactor starting procedure has also been examined with respect

to tolerable superheater fuel temperatures.

2.2 RESULTS

2.2.1 Superheater Description

A description of the Pathfinder superheater core showing the core layout is presented in Figure 2.1. A summary of the pertinent design features of the superheater core are shown in Table 2.1.

TABLE 2.1
SUPERHEATER CORE DESCRIPTION

Superheater Mean Diameter	30 in.
Superheater Volume	32.5 ft ³
Number of Fuel Elements	412
Active Fuel Length	71.5 in.
Number of Control Rod Clusters	4
Number of Source and Instrumentation Tubes	4
Total Core Flow Area	0.993 ft ²
Total Core Heat Transfer Area	1817 ft ²
Average Power Density	970 kw/ft ³ core volume
Average Heat Flux	59,100 Btu/hr/ft ²
Maximum Heat Flux	219,000 Btu/hr/ft ²
Maximum Clad Surface Temp.	1270 F
Thermal Power Generated	31.5 Mwt
Thermal Power to Coolant	29.8 Mw
Average Exit Bulk Temperature	725 F
Core Pressure Drop	61 psi

The fuel element dimensions are as noted in Table 2.2 with its design features shown in Figure 2.2.

TABLE 2.2
HIGH ENRICHED FUEL ELEMENT DIMENSIONS

	COLD	OPERATING CONDITION	
		Average	Hot
Outer Dia. Outer Insulating Tube	1.076	1.080 $\pm$.002	1.080
Inner Dia. Outer Insulating Tube	1.024	1.028 $\pm$.002	1.028
Outer Dia. Inner Insulating Tube	.967	.974 $\pm$.005	.976
Inner Dia. Inner Insulating Tube	.937	.944 $\pm$.001	.946
Outer Dia. Outer Fuel Tube	.839	.848 $\pm$.005	.850
Inner Dia. Outer Fuel Tube	.769	.777 $\pm$.002	.779
Outer Dia. Inner Fuel Tube	.630	.636 $\pm$.005	.638
Inner Dia. Inner Fuel Tube	.560	.566 $\pm$.002	.567
Outer Dia. Poison Rod	.467	.470 $\pm$.001	.471
Fuel Clad Thickness (cold)	.0075		
Fuel Meat Thickness (cold)	.020		

The finalized high enriched element dimensions as shown in Table 2.2 represent the element which was found to have a minimum fuel surface temperature variation in the hot channel.

2.2.2 Superheater Operating Performance

In Table 2.3 the operating performance of a nominal superheater fuel element is presented. The 725 F outlet bulk steam temperature case represents the reference design value.

TABLE 2.3

NOMINAL SUPERHEATER HEAT TRANSFER & FLUID FLOW OPERATING PARAMETERS

Outlet bulk average steam temp. - °F	825	775	750	725	700	675
Inlet steam temp. - °F	489	489	489	489	489	489
Average mass velocity - (lb/hr-ft ² × 10 ⁻⁶)	.611	.611	.611	.611	.611	.611
Average inlet velocity - fps	128	128	128	128	128	128
Average outlet velocity - fps	227	217	212	207	200	195
Core pressure drop - psi	65.8	62.6	61.7	61.0	59.1	58.2
Average heat flux - Btu/hr-ft ²	76,600	67,000	61,900	59,100	52,400	48,800

The results of the hot channel analysis are shown in Table 2.4.

TABLE 2.4

HOT CHANNEL SUPERHEATER HEAT TRANSFER & FLUID FLOW OPERATING PARAMETERS

Outlet steam temperature (nom) - °F	825	775	750	725	700	675
Outlet steam temperature (hot)	1325	1200	1135	1089	1015	972
Steam mass velocity (lb/ft ² -hr × 10 ⁻⁶)	.5015	.5092	.5139	.517	.5232	.525
Inlet steam velocity - ft/sec	104.5	106.1	107.1	108	109	110
Outlet steam velocity - ft/sec	266.3	250.4	242.2	234	226.5	214
Hot spot film coefficient-Btu/hr-ft ² -°F	500	495	490	485	490	490
Average heat flux - (Btu/ft ² -hr × 10 ⁻⁶)	.115	.100	.093	.088	.079	.073
Hot spot heat flux - (Btu/ft ² -hr × 10 ⁻⁶)	.159	.139	.128	.123	.108	.101
Effective peak heat flux - Btu/ft ² -hr × 10 ⁻⁶)	.282	.247	.228	.219	.192	.180
Maximum fuel surface temperature (°F)	1598	1423	1343	1270	1192	1133

At the reference design steam outlet temperature of 725 F, the maximum fuel surface temperature is 1270 F. A maximum fuel surface variation of 25 F was obtained at the "hot spot" in the hot channel.

2.2.3 Superheater Shutdown Characteristics

In Table 2.5 the results of the shutdown thermal analysis of the superheater fuel element are presented.

TABLE 2.5

FUEL ELEMENT SHUTDOWN CHARACTERISTICS

System pressure, psi	600
Moderator temperature, °F	489
Maximum fuel surface temperature, °F	1270
Steam flow rate, lb/hr/element	0
Reactor power level, per cent of full power	1.7
Heat dissipation ability, Btu/hr/element	4282

At zero steam flow the superheater will be capable of safely dissipating 1.7% of the design full power heat generation.

2.2.4 Superheater Startup Characteristics

An evaluation of the Pathfinder reactor startup procedures produced the thermal results as shown in Table 2.6.

TABLE 2.6

THERMAL CONDITIONS DURING REACTOR STARTUP	Reactor				
	Nuclear Warmup	Draining	Approach To Power	Pressurization	Rod Conversion
System pressure, psig	550	300	300	600	600
Superheater power fraction	0.084	0.0696	0.0696	0.0696	0.0923
Radial hot channel factor	2.02	2.18	2.18	2.18	2.18
Coolant flow rate	340 gpm	Variable	20%	20%	20%
Maximum fuel surface temp. °F	453	1270	950	920	1050
Outlet temperature, °F	420	~1000	535	563	592

As noted in Table 2.6, at no time during the reactor startup operation will the fuel surface temperature exceed the 1270°F maximum tolerable fuel surface temperature value.

2.3 DISCUSSION

2.3.1 Operating Hydraulic Analysis

2.3.1.1 Method of Analysis

For calculating core pressure drop, each fuel element is considered as one of 412 resistances in a parallel flow circuit; i.e., the pressure drop across each of the superheater tubes is considered to be identical. Correspondingly, each of the three flow passages within each fuel element is taken as one of three resistances in the parallel flow circuit for each element; i.e., the pressure drops across each of the three flow passages are considered to be identical.

For representing average core conditions, each active element is assumed to receive an equal portion of the total active flows. However, the flow distribution between each of the three passages within the element is computed for the actual element geometry, including average heating effects. In all friction (and heat transfer) considerations, the wire spacers are taken into account by deducting the area occupied and acknowledging the total wire surface in the equivalent diameter calculation.

An IBM-704 program (STER) (Ref. (2.1)) has been written to solve the flow split within an individual fuel element for the same total core pressure drop (it also evaluates the surface temperature). The pressure drop is evaluated for two equal axial lengths of fuel element and summed to provide

the total pressure drop across the element. This division into two parts accounts for the relatively small compressibility effects. The first axial section calculation includes the inlet contraction effects into the fuel element plus the acceleration and friction components up to the element mid-plane. Section 2 includes the remainder of the acceleration and friction loss along with the expansion loss at the exit of the element. Isothermal pressure losses above and below the active fueled region are also considered. Friction, expansion and contraction may be considered in these regions. These losses are then added to the fueled region pressure losses providing the total core pressure drop.

2.3.1.2 Friction Factor Evaluation

The friction factors for the annular flow passages were taken from the Moody curve (Ref. 2.2) which can be represented for the turbulent Reynolds number range of interest as follows:

$$\frac{1}{\sqrt{f}} = 4.0 \log (Re \sqrt{f}) - 0.40$$

and

$$\Delta P = 2 \frac{v_m G^2}{gc} f \left(\frac{L}{D_e} \right)$$

where the Reynolds number (Re) is based upon bulk steam temperatures, using Timroth viscosity data (Ref. 2.3). To cover uncertainties, a 1.1 factor has been applied to the above friction factor equation.

LITERATURE SURVEY

One of the few sets of friction data for superheated steam was reported by Keenan (Ref. 2.4). Friction factors for superheated steam flowing in a long (54.4 ft) 1 inch (1.05 in. I.D.) pipe were obtained. Flow velocities

up to the velocity of sound and initial pressures of the steam ranging from 25 psia to 200 psia were investigated. Both adiabatic and non-adiabatic friction factors were determined. Friction factors for incompressible water were also studied in the same test section for comparison with the compressible steam data.

The friction factors obtained with compressible steam were essentially the same function of the Reynolds number as the water friction factors. They were consistently higher, by about 0.001, than the Nikuradse equation (smooth Moody) used in the Pathfinder hydraulic analysis. This was attributed to the greater roughness of the commercial pipe used as the test section. No difference was observed between the adiabatic and non-adiabatic steam friction factor data.

Friction factor data for fully developed turbulent flow with heat addition have been reported by numerous other investigators using gases other than steam. Humble, Lowdermilk and Desmon (Ref. 2.5) studied frictional pressure drop for air flowing through heated round tubes. They found their data correlated best when they used fluid properties evaluated at the film temperature. This investigation was extended to rectangular, triangular and square ducts by Pinkel (Ref. 2.6). It was found that the friction factor for these non-circular ducts could be correlated by the Karman-Nikuradse (smooth Moody) curve if the fluid properties were evaluated at the film temperature and the equivalent diameter concept utilized.

In a more recent study, Davenport and Magee (Ref. 2.7) investigated the friction losses of nitrogen flowing in heated round tubes. They also found that their data agreed with the smooth Moody curve for Reynolds numbers

above 30,000 if they used properties evaluated at the film temperature.

EXPERIMENTAL PROGRAM

Experimental friction factor data has been obtained for thin annuli, similar to the Pathfinder superheater size, in a low pressure air loop and also in a high pressure steam facility. As shown in Figure 2.3, experimental data for unheated air in thin passages of 73 mil, 54 mil, and 25 mil gaps, duplicating the three Pathfinder coolant channels, have agreed with the Moody correlation to within about 4 per cent.

Isothermal and heated friction factors have also been obtained at Pathfinder steam conditions (Ref. 2.8). The test sections (one section heated from both sides and one section unheated) consisted of a 0.60 in. annular flow passage, 6 ft long, with three longitudinal wire spacers. Reynolds numbers from 35,000 to 140,000 and steam temperatures up to 1300 F were investigated under both isothermal and non-isothermal conditions. The friction factor results appear in Figures 2.4, 2.5, 2.6, and 2.7. Figure 2.4 shows the good agreement of the experimental results with the smooth Moody curve for the friction factor obtained in the unheated test section which was located downstream from the heated section.

The heated test section friction factors are shown in Figures 2.5, 2.6, and 2.7 for steam properties evaluated at the bulk, film, and wall temperatures, respectively. Using the surface temperature for property value evaluation provided the closest agreement with the accepted friction curve. Evaluation of the friction factors based on the bulk temperature and film temperature yielded curves about 8 and 5 per cent, respectively, below the

Moody curve.

The experimental friction factor data obtained at Pathfinder conditions are in good agreement with those determined for fully developed flow as reported in the literature. Although application of the bulk temperatures in the property evaluations produced the largest deviation from the smooth Moody (8 per cent), its application is nevertheless considered to be adequate.

The 1.1 uncertainty factor used in the Pathfinder hydraulic evaluation appears sufficient to cover all pressure loss contingencies. For applicability of the equivalent diameter, the concept appears valid. In spite of the complex superheater geometry, with wire spacers separating the flow into three channels, the standard equivalent diameter ($De = 4 Af/Pw$ where Pw includes the total wire perimeter) rationalized the data very well.

2.3.1.3 Loss Coefficients

The pressure losses in the entrance and exit sections of the fuel element are taken into account by conventional methods. The major entrance loss component occurs just at the entrance to the three annular flow passages. A loss coefficient (K) of 0.4 was used, based upon data for air entering a single annular passage, duplicating fuel element geometry as closely as possible, i.e.,

$$\Delta P = \frac{G^2 v_m}{2g_c} (1 - \sigma^2 + K)$$

The exit losses consist of a series of sudden expansions and contractions plus some friction. Two low-pressure air-flow tests were completed to determine

the pressure losses in the superheater inner insulating tube downstream from the fuel. The test was performed using a full-scale model of the inner insulating tube and the lower portion of the high-enrichment element. The loss coefficient, K, as determined from test data is 1.33 where,

$$\Delta P = \frac{KVG^2}{2g_c (144)}$$

and G^2 is based on the area of the lower insulating tube. This value of the loss coefficient is 15 per cent less than that which had been determined in previous analytical studies.

2.3.1.4 Other Considerations

The moisture present in the saturated steam entering the superheater is considered to be essentially zero, based upon the results from the moisture separation test program (Ref. 2.9). Any effect of surface roughness has been neglected, since the fuel element specifications call for a surface finish at least as smooth as 32 μ in. RMS, which would give an L/De of 0.00041 in the smallest flow channel. Based upon the Moody roughness curves, this relative roughness (L/De) would give rise to about a 7 per cent increase in friction factor at $Re = 0.7 \times 10^5$ for flow in a round pipe.

Another consideration with respect to the friction factor is the effect of variable fluid properties during heating. This effect is small (less than 10 per cent), as established from the high pressure superheated steam experiments (see Figures 2.5, 2.6 and 2.7). The variation in exit loss among the different fuel elements associated with variations in the lengths of the lower insulating tubes (due to curvature in the grid plate) has been neglected, and losses for a tube of average length have been used. These

variations would result in pressure drop differences of less than 1 per cent at the average steam flow rate.

2.3.1.5 Superheater Pressure Losses

A detailed breakdown of the total superheater core pressure drop is shown in Table 2.7. The reference element full power steam flow rate of 1478 lb/hr/element and power corresponding to a 725 F bulk steam temperature were utilized. The dimensions of the assembly are those as given in Table 2.2 and shown in Figure 2.2.

TABLE 2.7

PRESSURE LOSSES IN PATHFINDER SUPERHEATER CORE

Pressure Drop Component	Description	Pressure Drop (Psi)
1	Upper grid plate loss	0.70
2	Friction drop inlet section	0.87
3	Contraction loss at fuel element handle	0.13
4	Friction loss along fuel element handle	0.02
5	Contraction loss at poison rod extension	0.49
6	Friction loss along poison rod extension	0.21
7	Contraction loss into fueled region	6.62
8	Acceleration loss across fuel element	2.31
9	Friction loss across fuel element	43.55
10	Expansion loss out of fueled region	-1.75
11	Friction loss in intermediate section	0.24
12	Form loss at bottom fixture	1.50
13	Contraction loss into lower section	2.30
14	Friction loss in lower section	4.48
15	Expansion loss into bottom plenum	-1.24
TOTAL		60.43

As seen in Table 2.7, the greatest pressure loss component is the frictional loss in the fueled section. Of the total superheater core pressure drop (60.43 psi) the friction loss in the fueled section contributes 72 per cent.

2.3.2 Operating Thermal Analysis

2.3.2.1. Method of Analysis

The bulk of the heat transfer and fluid flow calculations have been performed using the Superheater Temperature Evaluating Routine (STER), a program for the IBM-704 computer.

The pressure drop is evaluated for two equal axial lengths of the fuel element and summed to provide the total pressure drop across the element. This division into two parts is used to account for the relatively small compressibility effects. The first axial section calculation includes the contraction, acceleration and frictional effects. Section 2 contains expansion, acceleration and frictional effects. Each plate and each coolant gap, divided into 180 axial sections, are treated as individual entities but they are also coupled through the equation by boundary conditions. The heat transfer analysis considers the effects of heat loss to the moderator, the temperature dependence of fluid properties, and the flux shape across and along the element. The heat split in the fuel plates is expressed in the STER program as a function of the plates surface temperature.

Relatively simple inputs, such as plate thickness, gap dimensions and power distribution, along with operating conditions, (inlet temperature, flow rates, etc), describe the problem. The program then calculates an average bulk and film temperature axial profile using the heat transfer subroutine for the average channel. The pressure drop across each flow channel is next calculated, using the previously calculated bulk temperature distri-

bution. These pressure drops are then compared and, if not equal, the program iterates on the flow until the pressure drops are the same across all channels of the element. The most important program output is the axial temperature profiles of each fuel plate and of the coolant in each channel which produced this flow and pressure drop.

The hot channel calculation is performed by a similar parallel flow channel method, but with the hot spot factors and any dimensional changes included. The pressure drop as calculated for the average element is impressed upon the hot element. The steam flow rates are then adjusted internally by the program to produce this pressure drop across the element in question. The bulk and film temperatures which result at this balance point represent the maximum superheater temperatures.

By necessity, several assumptions and approximations were made in the writing and programming of the STER program. These inherent limitations, although not major, should be recognized when evaluating the results obtained from the program.

The major assumptions and approximations are as follows:

1. No axial or angular heat conduction along the fuel element.
2. Constant fuel and cladding properties.
3. No gap between clad and fuel.
4. Constant flux and heat generation across an individual fuel plate.
5. Heat transfer across the inner and middle flow channel is by convection only, (no radiation).
6. The axial flux profile is the same for each fuel plate in an element.

7. The radial flux profile across the element is constant independent of length in the axial direction.
8. Viscous heating effects are neglected.
9. Arithmetic average specific volumes are used to obtain the friction pressure drop loss.
10. The pressure drop is calculated from a momentum balance which assumes incompressible fluid flow and neglects potential energy changes.
11. No heat generation in clad or steam gap.

2.3.2.2 Heat Transfer Coefficient Evaluation

The forced convection local heat transfer coefficients for the annular flow passages were taken from McAdams et al (Ref. 2.10):

$$(Nu)_f = 0.0214 (Re)_f^{0.8} (Pr)_f^{1/3} \left(1 + \frac{2.3}{L/De} \right)$$

where the subscript, f, refers to properties evaluated at a film temperature equal to the average of the surface and bulk temperature. Pressures from 115 psia to 3500 psia and Reynolds numbers from 6,000 to 40,000 were investigated. A summary description of the test assembly and conditions that were investigated are shown in Table 2.8.

TABLE 2.8

McADAMS HEAT TRANSFER COEFFICIENT EXPERIMENT

Test Assembly

Length of test section	12 inches
Inner diameter of annulus	0.25 inches
Outer diameter of annulus	0.382 inches
Equivalent diameter of annulus	0.131 inches
Maximum L/De obtained	57

TABLE 2.8 Continued

Test Conditions

Steam pressure	115 to 3500 psia
Steam temperature	430 to 1000 F
Temperature difference	100 to 630 F
Wall temperature	607 to 1288 F
Reynolds number	7000 to 40,000

The experimental data, corrected for thermal radiation effects, were correlated using the Timroth steam viscosity data (Ref. 2.3) and the thermal conductivity values obtained by Keyes and Sandell (Ref. 2.11).

Literature Survey

J. B. Heineman (Ref. 2.12), investigated heat convection to steam flowing within a round tube and a rectangular section. The following conditions were investigated:

TABLE 2.9

HEINEMAN HEAT TRANSFER COEFFICIENT EXPERIMENT

Tube Test Section

Steam pressure	300 - 1500 psia
Steam temperature	550 - 900 F
Steam superheat	73 - 142 F
Surface temperature	650 - 1290 F
Heat flux	50,000 - 287,000 B-hr ⁻¹ - ft ⁻²
Steam mass velocity	144,000 - 792,000 Lb-hr ⁻¹ - ft ⁻²
Reynolds number	60,000 - 370,000
Tube Radius	0.333 inches

Rectangular Test Section

Steam pressure	300 - 1000 psia
Steam temperature	470 - 850 F
Steam superheat	5 - 160 F
Surface temperature	520 - 1125 F
Heat flux	23,000 - 96,000 B-hr ⁻¹ - ft ⁻²
Steam mass velocity	197,000 - 553,000 Lb-hr ⁻¹ - ft ⁻²
Reynolds number	25,000 - 100,000
Aspect ratio	26.6: 1

The local heat convection coefficients were obtained from L/De of 6 to 132.5 and correlated into the following equations:

$$(Nu)_f = 0.0157 (Re)_f^{0.84} (Pr)_f^{1/3} (L/De)^{-0.04} \quad 6 < L/De < 60$$

$$(Nu)_f = 0.0133 (Re)_f^{0.84} (Pr)_f^{1/3} \quad L/De > 60$$

There was no observable difference between the rectangular data and the round channel correlation in the L/De range from 60 to 121. The steam viscosity values used were essentially from Timroth (Ref. 2.3) with the steam thermal conductivity values from the smoothed Russian data (Ref. 2.13).

Recently, Collier and Lacy (Ref. 2.14) reported local heat transfer coefficients of steam at pressures up to 1,075 psia. Three series of tests were performed in essentially the same annular test section geometry; two series at 20 psia and one series at pressures ranging from 155 - 1,075 psia. The conditions of the experiments are summarized in Table 2.10 below.

TABLE 2.10

COLLIER AND LACY HEAT TRANSFER COEFFICIENT EXPERIMENT

	A	B	C
<u>Test Assembly</u>			
Equiv. dia. (in)	0.178	0.176	0.241
Inner dia. (in)	0.375	0.375	0.625
Outer dia. (in)	0.553	0.551	0.866
Heated length (in)	29.62	29.	29.
Maximum L/De ratio	151.7	153.4	112.0
<u>Test Conditions</u>			
Pressure (psia)	155 - 1075	20	20
Mass flow rate, $(lb/hr-ft^2) \times 10^{-6}$	0.432-1.02	0.778-0.141	0.0468-0.0741
Reynolds Numbers, $\times 10^{-5}$	1.30 - 3.2	0.34 - 0.64	0.26 - 0.44
Heat flux $(Btu/hr-ft^2) \times 10^{-4}$	5.1 - 12.8	0.62 - 1.0	0.5 - 1.0
Maximum ΔT_f , ($^{\circ}F$)	220	80	100

From the complete results of both the high and low pressure series, two relationships were obtained:

$$(Nu)_f = 0.0058 (Re)_f^{0.945} (Pr)_f^{1/3} (L/De)^{-0.1}$$

$$(Nu)_f = 0.0035 (Re)_f^{0.945} (Pr)_f^{1/3} (1 + 4.56 \frac{De}{L})$$

The transport property data used were the same as Heineman's (Ref. 2.13). When only the high pressure results were processed (the slope in the intermediate L/De range was heavily weighted by low pressure results which were obtained at lower Reynolds numbers and heat fluxes), the following expressions were obtained:

$$(Nu)_f = 0.0357 (Re)_f^{0.8} (Pr)_f^{1/3} (L/De)^{-0.1}$$

$$(Nu)_f = 0.0208 (Re)_f^{0.8} (Pr)_f^{1/3} (1 + 8.97 De/L')$$

where: $L' = L + 12 De$

Thus, it is apparent that the available data suffer from several limitations. The data of McAdams cover only small L/De and low Reynolds numbers; also a sudden turn in the flow stream just upstream from the test section was a possible complicating factor. The data of Heineman cover a somewhat larger range, but are still limited to small L/De and low superheat steam temperatures. The data of Collier are also limited to small (L/De) and consist predominantly of low pressure points. In addition, the effect of test section geometry cannot be isolated.

Experimental Program

An experimental test program was conducted to determine the heat transfer characteristics of steam at the conditions of the Pathfinder superheater fuel element (Ref. 2.8). This investigation, in an annular flow passage,

included Reynolds numbers from 35,000 to 140,000, L/De up to 685 and super-heat up to 800 F. A long, thin annular passage, heated both internally and externally simulating the center flow passage of the Pathfinder fuel element was employed. In Table 2.11 the heat transfer and fluid flow parameters that were investigated are shown.

TABLE 2.11

PATHFINDER HEAT TRANSFER COEFFICIENT EXPERIMENT

Test Assembly

Equiv. dia. (in.)	0.1046
Heated length (in.)	72
Inner dia. (in.)	0.875
Outer dia. (in.)	0.995
Annulus gap (in.)	0.060
Maximum L/De	685

Test Conditions

Steam pressure, psia	520 - 615
Steam temperature, °F	489 - 1220
Temperature difference, °F	175
Surface heat flux, Btu/hr-ft ²	up to 125,000
Reynolds number	20,000 - 140,000
Mass flow rates, lb/hr-ft ²	0.13×10^6 to 0.72×10^6

All property values were evaluated at the local film temperature (including the density and viscosity in Reynolds number). Steam density values were taken from Keenan and Keyes (Ref. 2.15) and the viscosity and Prandtl number values were taken from Fritsch and Grosh (Ref. 2.13). A plot of the Nusselt numbers obtained from the experiment versus Reynolds number is shown in Figure 2.8.

The heat transfer coefficients obtained at Pathfinder conditions agree with the correlation proposed by Heineman (Ref. 2.12). Although, his

correlation was obtained in round and rectangular channels; no measurable difference due to the difference in geometry was observed. In addition, the data indicates that the Nusselt numbers for the outside and inside heated walls are identical in the small gap (0.060 in.) and large diameter (0.878 in. I.D.) Pathfinder fuel element.

Application of the McAdams heat transfer coefficient expression to predict fuel surface temperatures in the Pathfinder superheater fuel element, although not necessarily the most appropriate, is nevertheless conservative. Figure 2.9 compares the heat transfer coefficients as predicted by the McAdams expression with those obtained with the Heineman correlation. The steam property data used by each of the above two observers was employed. As Figure 2.9 shows, the McAdams expression consistently predicts lower heat transfer coefficients by about 80 per cent at the Pathfinder conditions ($Re \approx 80,000$ $T_f \approx 800$ F).

In addition, an overall uncertainty factor of 1.1 has been applied to the calculated film temperature drops.

2.3.2.3 Pathfinder Full Power Heat Loss

The selection of the steam insulation gap thickness which surrounds each of the individual fuel cells requires a knowledge of the heat lost from the element at full power, steady-state reactor operating conditions. From a reactor shutdown (no coolant flow) consideration, the gap should be designed as thin as possible. The steady-state situation, however, limits this minimum gap size to some thickness which will prevent the resulting heat loss from producing excessive voids in the superheater moderator.

Furthermore, the insulation gap thickness is limited to a width of about 20 mils to insure no contact between tubes.

The intent, then, was to determine insulation gap thickness which would, (1) allow sufficient decay heat dissipation from a shutdown consideration, and (2) provide adequate insulation at steady-state full power to minimize the moderator void content, and yet (3) prevent possible physical contact between the two insulating tubes.

The thermal resistance offered by the stagnant steam insulating gap has been determined by a shutdown cooling experiment. The temperature of the inner surface of the outer insulating tube was assumed equal to the moderator temperature (490 F). The temperature on the outer surface of the inner insulating tube was assumed to be equal to the bulk steam temperature at that axial location. The emissivities of both stainless steel walls were taken as 0.80 as evaluated experimentally in the shutdown cooling tests (see Section 2.3.3.2).

Several reactor parameters were considered constant in this analysis. The reactor was assumed to contain 412 fuel cells. The total reactor steam flow was assumed constant, independent of reactor power, at 616,125 lb/hr. Reactor powers necessary to produce the 616,125 lb/hr of steam with bulk temperatures of 825 F, 775 F, and 700 F were studied. The moderator temperature in each instance was assumed constant at 490 F.

Heat Loss in Fueled Section

With the above data the STER code was utilized to generate the axial temperature profiles in the outer channel of the nominal fuel element. The

axial superheater power profile is shown in Figure 2.10. Figure 2.11 shows these temperatures profile for 825 F, 775 F, and 700 F bulk outlet steam temperatures. Although these profiles will change slightly with changing insulation gap, this effect is generally quite small in the gap sizes of interest and has been neglected.

The fuel element was subdivided into nine equal axial lengths and, as Figure 2.11 indicates, the average temperature within each axial segment was determined. The outer surface of the insulation gap was assumed to be equal to the outer flow channel, bulk temperatures. The film drop and metal temperature drops are negligible at full power steady-state conditions and hence were neglected.

The heat loss was then evaluated using the heat loss model assumed earlier. The linear heat loss for each axial element at a particular insulation gap thickness was evaluated at the corresponding average temperature for that axial segment. The heat losses in each incremental axial lengths were summed, yielding the total heat loss for each case. Insulation gaps were obtained by holding the outer insulation tube diameter fixed at 1.024" I.D. and adjusting the insulating tube diameter to provide the desired insulation gap. Values of 20, 30, 40, 57 and 70 mils for the steam insulation gap were investigated.

Figure 2.12 shows the results of the heat loss in the fueled region of the superheater with 412 active fuel elements as a function of bulk steam temperature and insulation gap thickness.

Other Thermal Conduction & Radiation Heat Losses in the Superheater

In addition to the heat loss through the insulation tubes surrounding the fueled region there are other areas in the superheater core which contribute to the overall heat loss from the superheater. These losses occur in the following areas:

- (1) Intermediate region just below active core
- (2) Lower process tube below intermediate region
- (3) Grid plate and manifold.

The heat losses through the intermediate and lower process tube sections also occur through a steam insulation gap which surrounds each tube. In investigating the heat loss from the above two areas the steam gap was assumed constant. The bulk outlet temperature from the active fueled region was varied. The previously described heat transfer model and surface emissivities were again employed. The mixed mean bulk inlet temperatures which were chosen represent the range of outlet steam temperatures from the active fueled region of the superheater core. It was assumed that these bulk temperatures were constant at the inlet values along the length of section. The calculated heat loss values for these sections contain some conservatism, though the temperature drops were not great, usually less than 5 F.

Figures 2.13 and 2.14 are the results of the heat loss versus inlet mixed mean bulk steam temperature for the intermediate and lower process tube sections, respectively.

Heat is also lost from the superheater by thermal conduction through the grid plate and lower steam manifold. This amount was estimated, assuming

no convection resistance, to be 0.7×10^5 Btu/hr at 825 F steam operating condition. For lower powers this heat loss was calculated assuming a linear relationship with power.

Heat Generation in the Moderator

By far the largest contribution to the heat which is received in the superheater moderator is that which is generated directly therein by (n, γ) radiation. This quantity of heat production is virtually unaffected by sizing of the insulation gaps. The amount generated has been estimated to be 50×10^5 Btu/hr at the 825 F steam temperature operation. A linear relationship with superheater power was assumed and the heat generation at the other powers (i.e. outlet steam temperatures) calculated. The heat generation in the outer insulation tube due to neutron and gamma heating in the stainless steel has been included in the above moderator heat generation estimates.

Heat Generation in Inner Insulation Tubes

The heat transfer model which was used to describe the heat loss from the superheater presupposes that there is no heat generation in the inner insulating tubes. The effect of any (n, γ) heating in the inner insulating tube will, of course, reduce the heat loss from the steam within the superheater coolant channels.

The amount of heat generated in the inner insulating tubes surrounding the active fueled region was calculated to be 510 Btu/hr-ft^3 . This calculation was for 825 F steam operation and with a 57 mil gap. This amount of heat represents approximately

20 per cent of the total heat loss due to radiation and conduction at these reactor conditions. The amount of (n, γ) heat generation in the lower process tube is quite a bit less and is only about 2 per cent of the total radiation and conduction loss. At lower powers (i.e. bulk steam outlet temperatures) assuming a linear generation with power, the same relative percentages may be expected.

It can thus be concluded that, although not influencing the total heat loss to the superheater moderator, the (n, γ) generation of heat in the inner insulating tube tends to reduce the fraction of heat loss from the superheated steam. From a superheater efficiency standpoint this conservation of heat allows the possibility of reducing the insulation gap without the direct loss of superheated steam energy.

In sizing the gaps no credit was taken for this (n, γ) heating of the inner insulation tube. It was felt that, since the total heat loss was unaffected, the benefits gleaned from increased fuel efficiency should not be used as a criteria for insulation gap selection.

Effect of Rib Spacers on Heat Loss

The heat loss as shown in Figures 2.12, 2.13 and 2.14 assumes there is no heat loss due to the presence of wire spacers between the two insulating tubes. This additional heat loss in the fueled and non-fueled sections was estimated, assuming that 2 of the 4 wires are in good thermal contact.

The importance of the wire spacers on the total heat loss can be seen in Figure 2.15. The heat loss for 825 F steam condition, as plotted in Figure 2.15, contains a conduction loss through the wire spacers between the insulating tubes in the fueled and lower process tube regions. As

Figure 2.15 shows, consideration of the wire spacers increased the total heat loss from 5.42 per cent to 7.35 per cent for 825 F steam with a 27 mil insulation gap thickness. In each figure the intermediate region had a 129 mil insulation thickness and the lower process region had a 50 mil insulation thickness.

Summary of Superheater Heat Losses

The total superheater heat loss including the following sources:

- (1) Heat loss in active fuel region
- (2) Heat loss in intermediate region
- (3) Heat loss in lower process tube
- (4) Heat generation in the superheater moderator

are shown in Figure 2.16. No account has been taken of the heat losses due to wire spacers.

With the size of the insulation gap thickness established at:

- (1) Fueled region-27 mils
- (2) Intermediate region - 192 mils
- (3) Lower process tube region - 50 mils,

the full-power reactor energy balances were calculated for 825, 775, 750, 725, 700 and 675 F steam outlet conditions. It was assumed that the total steam flow rate was held constant in each instance at 616,125 lb/hr with the boiler pressure at 615 psia. Inlet feedwater conditions were also assumed constant with no changes in recirculation flow rate. The moderator radiation and conduction heat losses were calculated as outlined previously with the effect of wire spacers assumed negligible. The (n, γ) heating of

the moderator, superheater steam line losses and grid plate and manifold losses were assumed linear with power. The total reactor energy balance is presented in Table I (in Introduction Section).

The effect of heat loss on the exit void content in the superheater moderator is shown in Figure 2.17. The exit void limit has been established by physics considerations at 10 per cent. The moderator flow orifices have been sized for 3600 gpm flow. The 27 mil insulation gap in the fueled region will result in about 4 per cent void and hence provide ample assurance that the 10 per cent void criteria will be met. The negligible importance of the wire spacers contribution to the total heat loss to the moderator is also shown.

2.3.2.4 Reference Full Power Performance

Nominal Channel Performance

The nominal channel performance of the element described in Table 2.2 is given in Table 2.3. The STER program was employed, using average superheater conditions of steam flow and power. A heat loss table for the active full section was used and is shown in Figure 2.18. The flow distribution between each of the three passages within the element is computed for the actual element geometry including average heating effects. No hot spot or hot channel factors were employed. However, an axial power shape with a peak/average of 1.35 is explicit in the analysis.

Hot Channel Analysis

The hot channel analysis of the high enriched superheater utilized constant multipliers (hot channel factors) in conjunction with a detailed geometry description, considering tolerances and element eccentricity.

(A) Hot Channel Factors

The Pathfinder superheater analysis employs essentially two sets of "hot spot" factors, (1) a bulk hot spot factor and (2) a film "hot spot" factor, F_F . The bulk hot spot factor is a constant multiplier which acts on the bulk steam temperature rise. The film hot spot factor is required because the film temperature rise depends heavily upon local effects, whereas the bulk temperature rise depends only on axially averaged quantities. This factor, also a constant multiplier, acts on the temperature rise from the steam bulk temperature to the fuel surface. The hot channel factors for the superheater are summarized in Table 2.12.

TABLE 2.12

SUPERHEATER HOT CHANNEL FACTORS
(EXCLUDING DIMENSIONAL TOLERANCE EFFECTS)

Radial Power Distribution	1.43	1.43
Axial Power Distribution	1.00	1.35
Fuel Loading	1.05	1.15
Spacer Wires and Unbonding		1.66
Geometry*	Explicit in Analysis	
Heat Transfer Coefficient		1.10
Combined	1.50	4.06

*The actual axial flux distribution is used in the heat transfer calculations.

The only engineering hot channel factor assigned is associated with local and average variations in fuel loading. A factor accounting for possible variations in boron loading in the poison rod has been estimated at less than one-half of one per cent. Variations in flow passage resistance associated with variations in surface roughness (according to the specifications) have also been considered and were found to be negligible.

Possible flow distribution effects associated with the inlet and outlet plenum regions were considered. It was established that the addition of an inlet and outlet plenum velocity head pressure loss to the hot channel pressure drop would have a negligible effect on the hot channel flow rate.

(B) Geometric Description

In addition, the STER code will allow explicit analysis of detailed element tolerances (i.e. eccentricity) which will provide, when combined with the "hot spot factors" analysis, a complete description of the Pathfinder superheater performance. The inner wall of the flow channel is assumed to be eccentrically located within the outer wall boundary, such that two of the three wire spacers are in contact with the outer wall. The "channel" between the two contacting wires is considered to be the "hot" channel as it offers the maximum resistance to steam flow. Temperature gradient analyses take into account the opposite effect of eccentricity regarding conditions near the location of "maximum" gap.

The effect of fuel element tolerances was included explicitly by accounting for adverse effects in terms of actual "hot" channel dimensions. Eccentric effects were also included by specific analyses described earlier.

In general, tolerances were considered such that the steam flow passages were minimized. Whenever eccentric effects dominated so that the minimum steam gap occurred with a small inner tube and a larger outer tube, the tolerances were taken in this direction. It was found that the particular set of dimensions representing the worse case for each of the three steam passages cannot physically occur in the same fuel element.

However, the heat transfer design calculations are carried out assuming this to be the case; the net effect is slightly higher calculated surface temperatures since the steam in adjacent channels is taken to be hotter than could actually be the case.

Similarly, eccentric effects are assumed to occur such that the minimum gaps all lie at the same angle, although this is physically impossible. Again, the consequences are slightly higher calculated surface temperatures.

In Table 2.13, a comparison between the dimensions of the three nominal flow channels and the possible three hot channels for the annular Pathfinder superheater element is presented. Also shown are the individual coolant passage steam flow rates, bulk steam outlet temperatures, and maximum fuel surface temperatures. The hot channel factors presented in Table 2.12 were employed with a 725 F turbine steam temperature condition assumed.

TABLE 2.13
COMPARISON BETWEEN NOMINAL AND HOT CHANNELS

	Nominal Channels			Hot Channels		
	Outer Passage	Middle Passage	Inner Passage	Outer Passage	Middle Passage	Inner Passage
Coolant gap, in.	0.048	0.0705	0.048	0.0356	0.0635	0.0418
Flow area, in. sq.	0.1297	0.1447	0.0727	0.0965	0.1346	0.0637
Equivalent dia., in.	0.0853	0.1134	0.0784	0.0651	0.1069	0.0703
Steam flow rate, lb/hr	526.4	678.9	272.7	296.7	558.1	205.9
Bulk outlet steam temp, °F	707.6	757.0	756.0	1055.	1076.	1065.
Max. surface temp, °F	770.8	800.8	804.7	1270.	1263.	1252.

Note the 25.6 per cent flow area reduction of the hot outer channel when compared to its nominal value. This is due to the manufacturing tolerances

required to assemble the element.

It is noted that this method of analysis, while slightly conservative, is vastly simpler than a more exact treatment.

Effect of Wire Spacers on Maximum Fuel Temperature

The effect of the wire spacers upon the temperature distribution within a fuel tube was investigated using a two-dimensional heat conduction analysis.

The analogy between heat conduction and neutron diffusion theory was utilized employing the IBM 704 PDQ program (Ref. 2.17). The analysis considered the effect of a wire spacer thermally bonded, over varying areas, to the cermet clad, and the results were reported in terms of a temperature peaking factor, ϕ , i.e.

$$\phi = \frac{T_{\text{max}} (\text{perturbed}) - T_{\text{bulk}}}{T_{\text{max}} (\text{unperturbed}) - T_{\text{bulk}}}$$

The factors are reproduced in Figure 2.19. Due to calculational limitations it was necessary to treat the round wire as a square rod and the cylindrical fuel region as a flat plate. Based upon these results, a temperature peaking factor of 1.25, corresponding to effective insulation over 75 per cent of the wire diameter, was introduced into the design analysis.

Additional analyses were performed which considered various insulating areas of the type expected from wire spacers, but took no advantage of any thermally unbonded regions. The temperature peaking factors resulting are shown in Figure 2.20. The completely insulated point is shown for comparison. The analyses are based upon the latest fuel element dimensions.

A separate effort was devoted to establishing the effect of unbonded regions on the cermet clad interface. The unbond was assumed to act as an adiabatic barrier. Two specific calculations were performed: the first was for an unbond 0.1175 in. wide and "infinitely" long, and the second was for a circular unbond 0.1175 in. in diameter. The temperature peaking factors were found to be 1.48 and 1.29 respectively.

Finally, unbonded regions lying opposite the wire spacers were considered. The first study considered a wire effectively blocking the heat flow over a 0.055 in. width (slightly more than 75 per cent of the diameter of the largest wire spacer, which is 0.0695 in.). An unbond was assumed to exist on the side of the cermet opposite from the wire. Unbonds were considered to be infinitely long and of various widths. The results are shown in Figure 2.21. It is seen that unbonds narrower than 10 to 15 mils do not significantly affect the peak temperature but rather enlarge the cermet area already affected by the wire.

It was felt that the assumption of an infinitely long unbond was unnecessarily conservative since the expected condition is an area no larger than 0.02 in^2 which might be a circular unbond 0.1595 in. in diameter. Since the direct solution to this problem would be exceedingly expensive (several hours of IBM-704 time), the result was synthesized by the following approach:

- 1.) Since results were available for both the wire effect and the infinitely long unbond effect individually, it was possible to compare the product of wire factor and unbond factor with the

results on Figure 2.21. For example, the wire factor for an 0.055 in. insulating width is 1.28; the unbond factor for an infinitely long unbond 0.1175 in. wide is 1.48 and the product is 1.89 compared with 2.08 from Figure 2.21.

- 2.) Based upon this encouraging agreement (about 10 per cent), it was decided to reduce the factors in Figure 2.21 by the ratio of the factor for the circular unbond divided by the factor for the infinitely long unbond. The unbond factors were assumed to be a linear function of the characteristic unbond dimension.

The results of this approach are shown in Figure 2.22. The current unbond specification restricts the size of the unbond to a maximum length of 3/32 in. The most limiting case then, is considered to be this 0.094 in. long unbond, and the corresponding peaking factor now being applied in the design calculations is 1.66 instead of the previous 1.25.

Hot Channel Results

The maximum fuel surface temperatures in the superheater have been established for normal operating conditions as a function of nuclear radial factor and outlet steam temperature, and the results are shown in Figures 2.23 and 2.24. The difference between the maximum surface and maximum fuel temperature is only 10 F.

The maximum fuel surface temperature for the normal operating conditions (725 F outlet steam and design radial factor = 1.43) is seen to be 1270 F. This temperature will drop from 1270 F to 1130 F as the outlet steam temperature changes from 725 F to 675 F.

All the superheater hot channel operating parameters are shown in Table 2.4 as a function of outlet steam temperature.

The temperature gradients in the superheater fuel elements have been examined for possible adverse effects. For an outlet steam temperature of 750 F and a nuclear radial factor of 1.43, the maximum temperature difference from one side of a fuel tube to another, considering eccentricity, was found to be 250 F, and the maximum temperature difference between the surface and maximum temperatures was found to be 15 F. The maximum difference between clad temperatures on one fuel tube was found to be 50 F. These temperature differences are approximately proportional to superheater power level at full steam flow and would then be correspondingly less at lower superheater outlet temperatures.

Combination of the axial superheater power profile (Figure 2.10), the expected steam temperature variation over life (Figure 2.25), and the thermal analysis for the hot channel yields the burnup temperature curves shown in Figure 2.26. A reliability curve in Figure 2.26 is superimposed (Ref. 2.18).

2.3.2.5. Partial Power and Overpower Performance

Low Power Superheater Performance

The performance of the high enriched superheater element at low steady-state superheater powers was studied. The results of core pressure drop and maximum fuel surface temperatures (both average element and hot channel) were evaluated at superheater powers of 1, 5, 10 and 20 per cent of the full reactor power (750 F superheater reactor steam). A flow to power ratio

of one was assumed for all of these cases.

The results are shown graphically in Figure 2.27. As Figure 2.27 indicates, with a constant power-to-flow ratio of one, the maximum fuel surface temperature will never exceed the surface temperature at full power (1271 F).

Overpower Superheater Performances

The operating characteristics of the high-enriched superheater element at superheater conditions greater than full power was evaluated. Again, the standard full power condition was taken as the 750 F outlet steam condition. The steam flow rate was held constant at 1478 lb/hr-element.

In Figure 2.28 the results for over reactor power conditions of 110 per cent and 120 per cent power based upon a superheater outlet steam condition of 750 F steam are shown.

2.3.2.6 Component Cooling Analysis

Superheater Control Rod Cooling Analysis

A thermal and hydraulic analysis of the superheater control rods was completed. The objectives of the analysis were: (1) to determine the maximum temperature of a control rod at shutdown, assuming no coolant flow; (2) to provide design data for steam flow-regulating nozzles; (3) to design a set of three long-radius flow nozzles located in the lower portion of the insulating tube which will regulate the flow of steam without causing unduly high control rod temperatures; and (4) to analyze the thermal and hydraulic performance of the control rod and channel with the nozzles in place.

Fifty-two individual superheater control rods, ganged into four groups and driven by four drive units, provide short term adjustment of steam conditions. Each control rod, as shown in Figure 2.29, consists of a 3/4 inch diameter poison section with a tubular extension of 304L stainless steel. The poison material is 2.0 w/o natural boron in 304L stainless steel.

The problem of limiting the superheater control rod temperature has been investigated. The average volumetric heat generation in a superheater control rod, which consists of thirteen poison fingers, has been calculated to be 1.3×10^6 Btu/hr-ft³. This is equivalent to a heat generation rate of 2.438×10^4 Btu/hr in each poison finger. Assuming that the control rod is concentrically located in the guide tube, a steam flow rate of 125 lb/hr/channel will limit the control rod surface temperature to 925 F. The exit coolant steam temperature would be 790 F.

A concentrically oriented control rod cannot be guaranteed, since the use of spacers would interfere with the rapid operation of the rod, and might cause rod binding. The problem of eccentric rod spacing was analyzed using the IBM-704 computer. The thermal model used assumed that the control rod is in contact with the flow channel at $\phi = 0$. Axial conduction was neglected. The flow region was divided into 15 sub-regions each with its own heat transfer coefficient. The results indicate that the peak temperature is 1160 F.

A pressure drop across the control rod channel for 125 lb/hr steam flow is 5 psi. Since the total pressure drop across the superheater core is approximately 75 psi, it was necessary to design a flow nozzle that

would pass 125 lb/hr of steam with a 70 psi drop. A test was performed to determine the coefficients of discharge for small diameter nozzles that meter coolant for the superheater control rods. Three nozzles of 0.2793 in. diameter and β ratio of 0.19 were tested over a Reynolds number range of 200,000 to 450,000. Results are given in Figure 2.30. The higher value of nozzle 2 is attributed to a faulty pressure tap installation on the upstream side of the nozzle. The loss coefficient that is being used in the control rod coolant flow analysis is 0.95.

Analysis showed that the maximum temperature of the control rod during shutdown with no coolant flow is 1160 F, assuming 3 per cent reactor power (25 per cent control rod power) and no coolant flow.

The nozzles were designed with a throat diameter of $0.1340 \pm .0005$ in., cold, built according to ASME standards for long-radius flow nozzles.

Analysis of the control rod then showed a maximum temperature of 1010 F with the reactor at full power with the control rod fully inserted assuming the rod is eccentric in the channel. The steam flow rate at these conditions is no less than 110 lb/hr assuming a core pressure drop of 52 psi. At partial power with rods fully inserted, the temperature may reach a maximum of 1230 F.

The average flow rate through a single control rod channel with the rod fully withdrawn is 172 lb/hr.

Neutron Source

The temperature and flow distribution calculations for the neutron source were performed based on the initial dimensions and heat generation rates. The results indicated that the steam gaps were more than adequate

for cooling the source during full-power operation, but were too large to permit adequate cooling during shutdown, with zero steam flow.

Additional analyses indicated that a reduction of the steam gap to 0.015 inches would provide satisfactory cooling during operation and under shutdown conditions. These results are summarized in Table 2.14. The input data are summarized in Table 2.15.

TABLE 2.14

NEUTRON SOURCE THERMAL ANALYSIS RESULTS

	<u>FLOW</u>	<u>SUPERHEATER POWER</u>	<u>MAX. TEMP.</u>
	195 lb/hr	100%	645 F
30 mil steam gap	0	100%	1320 F
Hot Channel	0	3%	920 F
	65 lb/hr	100%	720 F
15 mil steam gap	0	100%	1250 F
Hot Channel	0	30%	900 F

TABLE 2.15

NEUTRON SOURCE THERMAL ANALYSIS CONDITION

CONDITIONS

Steam Inlet Temp.	490 F	
Dome Pressure	615 psia	
<u>Heat Generation Rates</u>	$\times 10^{-3}$ Btu/hr	<u>New Values</u>
Antimony	4.70	5.1
Clad, SS	<u>1.22</u>	<u>1.1</u>
Total for Rod	5.92	6.2
Beryllium Tube	4.1	4.75
Outer Clad, SS	2.0	2.5 for both
Inner Clad, SS	<u>1.2</u>	
Total Beryllium Tube	7.3	7.25
TOTAL FOR SOURCE	13.2	13.45

TABLE 2.15 Continued

<u>Dimensions</u>		<u>Properties</u>	
	<u>OLD</u>	<u>NEW</u>	
Be			
I.D.	0.632 in.	0.622 in.	k_{Sb} 9.7 B/hr-ft ² -F
O.D.	0.960 in.	0.970 in.	k_{Be} 65.0 B/hr-ft ² -F
Clad	0.020 in.	0.015 in.	k_{SS} 12.0 B/hr-ft ² -F
Sb	0.492 in.	0.522 in.	<u>Hot Channel Factors</u>
			Axial 1.35
Clad	0.020 in.	0.020 in.	Radial 1.43

The source was redesigned to conform to the recommendation that the steam gap be reduced to 0.015. The new heat generation rates were based on the revised design. The overall increase in heat generation was 2 per cent which would result in a relatively small increase in the temperature of the source and is compensated for, at least partially, by an increase in the heat transfer surface. The dimensions and heat generation rates for the new design are compared with the previous values in Table 2.15. In comparing the ratio of the heat generation rate in the Sb rod to the diameter of the rod for the two designs, it is seen that the heat flux through the gap is unchanged by the design changes. From this, it is evident that the temperature distribution will be essentially unchanged in the revised design from those previously predicted for a 0.015 steam gap.

Ion Chambers

The thermal analysis of the superheater ion chambers included the investigation of the ion chambers performance during all phases of reactor

operation from full power to complete shutdown. At no time will the maximum temperature of the ion chamber exceed 810 F. This temperature was obtained during the reactor shutdown -- zero flow assumed conditions.

During reactor shutdown the mechanism for heat loss from the ion chamber has been assumed to be thermal radiation and conduction across the stagnant steam gaps. Three stagnant steam gaps are formed by the ion chamber and its assembly:

- (1) ion chamber to dummy fuel rod
- (2) dummy fuel rod to inner insulating tube
- (3) inner insulating tube to outer insulating tube.

At the shutdown condition the heat fluxes across the three gaps are 1130 Btu/hr-ft², 1090 Btu/hr-ft² and 1330 Btu/hr-ft² for the three gaps listed consecutively above. Each heat flux includes a 1.35 axial plus a 1.43 radial hot channel factor. The calculated temperature distribution across the ion chamber superheater assembly for the reactor shutdown situation was 810 F, 700 F and 563 F for the ion chamber, dummy fuel rod and inner insulating tube respectively. The outer insulating tube temperature was assumed to be equal to the 489 F moderator water.

At full reactor power 300 lb/hr of steam will cool each of the four ion chambers. The hottest ion chamber temperature is expected to be only 589 F. At 5 per cent of full power and flow, a maximum ion chamber temperature of 614 F can be expected.

2.3.3 Shutdown Thermal Analysis

The superheater element, the dimensions of which were selected from the steady-state heat loss and performance studies, was investigated to

determine its shutdown heat dissipation ability. The assumption of no steam flow for this shutdown situation was applied. Shutdown power levels relative to the 725 F steam outlet temperature were studied.

2.3.3.1 Method of Analysis

A heat transfer model assuming conduction and radiation across the steam gaps was used. As the temperature of the center poison rod was of concern, the physical model consisted of one solid center poison rod surrounded by 2 fuel tubes and an inner insulation tube with four steam gaps providing the resistance to the heat flow. The element dimensions used were shown in Table 2.2.

The following assumptions were used in the shutdown heat dissipation analysis:

- (1) Thermal radiation and conductivity across steam gaps are the only modes of heat transfer.
- (2) Temperature of the inner surface of the outer insulating tube are the same as moderator water temperature (489 F).
- (3) Emissivities of all stainless steel surfaces are equal (0.8).
- (4) Steam is a non-luminous gas.
- (5) There are no axial or angular temperature gradients.

2.3.3.2 Experimental Results

A series of experimental tests were performed to establish and correlate the heat loss relationship of a typical Pathfinder Superheater Fuel element with no steam flow (Ref. 2.16). In all, three prototype Pathfinder annular fuel elements were examined. Each test was performed in a pressure vessel at

reactor conditions of 600 psig. External strip heaters provided the saturated "moderator" water external to the insulated tube assembly as well as saturated steam above and below the test section. The test section was heated by power supplied from a D.C. motor-generator set rated at 12 volts at 1250 amps.

Figure 2.31 is a compilation of the results obtained from the three test sections. Also shown are the results of a preliminary calculation based upon conservative assumptions and approximation. The spread in the heat dissipation versus temperature curves which were obtained for the three sections were felt to be within the experimental accuracies of the test.

The test results of Figure 2.31 were correlated with an analytical heat transfer model. The modes of heat transfer were assumed to be by thermal radiation and conduction exclusively. Steam was considered to be a non-luminous gas.

Figure 2.32 shows a comparison of the results from Test Section 3 with values obtained from the mathematical model using two values of emissivity for both stainless steel surfaces. Test Section 3 was chosen as it represents the most conservative data from a shutdown dissipation standpoint. As Figure 2.32 indicates, the heat transfer model can be forced to fit the experimental data of Test Section 3 when an emissivity of 0.8 is employed for both stainless steel surfaces.

2.3.3.3 Shutdown Heat Dissipation Capacity

Two situations were investigated; a nominal fuel element which represented the average superheater element's dimensions and a hot channel

case where the dimensional tolerances were taken into consideration.

In both of the studies the peaking factors shown in Table 2.16 were applied to the heat generation ratio.

TABLE 2.16

SHUTDOWN COOLING PEAKING FACTORS

Reactor Shutdown Cooling Peaking Factors

Axial = 1.35

Radial = 1.43

Fuel Loading = 1.15

The heat split between the four generating regions' poison tube and two annular plates' fuel and inner insulating tube was based upon the decay gamma heating rate at various percentages of full reactor power (725 F).

Figure 2.33 is a composite of the results of the shutdown analysis.

The shutdown cooling test results, although obtained with larger steam gaps and hence not directly applicable, are also shown for comparison. A summary of results are also presented in Table 2.5 of Section 2.2.3.

During flooding, it is conceivable that during a brief period, a superheater fuel element will be receiving no net steam flow prior to being filled with water. During this period, the only mechanism for heat removal has been assumed to be heat loss to the moderator water. Assuming that the period in question is long enough for equilibrium conditions to be reached (5°F/sec/per cent power, assuming adiabatic conditions), the maximum fuel temperature could reach 1265 F at a decay power level of 1.7 per cent based upon results from the shutdown cooling test (see Figure 2.33.) This

temperature will not be exceeded so long as the superheater is not flooded (i.e., steam flow secured) until 23 min. after shutdown based upon decay heat curves and an infinite history of operation at full power (Ref. 2.19). It is noted that even if the superheater is flooded as soon as 3 minutes after shutdown, the maximum fuel temperature would not exceed 1600 F.

2.3.4 Reactor Startup

The normal reactor startup procedure has been examined with respect to superheater fuel temperatures. It has been established that these temperatures will never exceed 1270 F, the normal full power value for 725 F outlet steam operation. An ample margin of safety was demonstrated to exist between the tolerable fuel temperatures and associated steam conditions and those expected during all phases of the startup operation.

The reactor startup procedure consists essentially of five distinct phases:

- Phase I -- Nuclear Warmup
- Phase II -- Superheater Draining
- Phase III -- Approach to 20 per cent power
- Phase IV -- Reactor Pressurization
- Phase V -- Conversion to Rod Configuration

After completion of the above five steps, the reactor will have progressed from a zero superheater coolant flow, decay, or zero power state to a 20 per cent steam flow (100% = 616,125 lb/hr) -- 20 per cent reactor power (100% = 188.9 MW) condition. It will then be brought up to the full power condition or any other desired condition.

2.3.4.1 PHASE I -- Nuclear Warmup

During the nuclear warmup operational period, water will be circulated through both the boiler and superheater cores with the recirculation valves fully open. All of the superheater control rods will be fully inserted, as will the eight boiler control rods nearest the superheater. The power fraction (as evaluated by physics considerations) in the superheater will be 8.4 per cent of the total reactor power. The radial power factor in the superheater has been calculated as 2.02 with a 10 per cent uncertainty.

The superheater inlet cooling water flow will consist of 300 gpm (420 F) recirculation water along with an additional 40 gpm (100 F) pump seal leakage flow. The system will be held at 550 psia throughout Phase I by the nitrogen tanks.

The nuclear warmup thermal analysis included the evaluation of both laminar flow and turbulent flow conditions. The heat transfer characteristics associated with laminar flow (reduction in film coefficient) were examined. Tolerable fuel surface temperatures were obtained even when two of the three annular coolant channels of the superheater fuel element were assumed to be in laminar flow. The maximum superheater surface temperature was not permitted to exceed 433 F, which is 25 F less than the saturation temperature value.

An adequate margin to prevent bulk or local boiling from occurring in the superheater core at reactor recirculation water temperatures up to 420 F is provided by not allowing the reactor power to exceed 7.25 MW(th). Other coolant flow rates and inlet temperatures were examined with respect

to safe superheater power levels during the nuclear warmup period. As Figure 2.34 indicates, a safe power up to 10 MW(th) is possible during nuclear warmup with slight adjustment in inlet cooling water conditions.

2.3.4.2 PHASE II -- Superheater Draining

Upon reaching the desired 550 psia pressure and 420 F water temperature in the nuclear warmup phase, the reactor will be shut down and the draining operation will begin. The pressure will fall rapidly to 300 psig and continued draining will permit flashing at the top of the reactor vessel with a subsequent lowering of the water level to the operating range level.

The draining operation will not introduce any excessive thermal limitations in either the superheater or the boiler. The ability of the superheater fuel element to safely dissipate the heat generated at reactor powers up to 2.6 MW(th) under conditions of zero steam flow has been demonstrated experimentally (Ref. 2.16). A thermal condition in the superheater worse than this zero flow case will be impossible during Phase II. The reactor will be shut down with only decay heat present. Coolant flow will be available from the flashing process during the draining period. The moderator temperature (sink temperature for radiation and conduction heat dissipation) will at no time exceed the zero flow 489 F (615 psia) condition. Hence, the heat removal ability of the superheater element will be improved or at least no worse during this draining operation than at zero flow after shutdown.

Forced circulation of the water in the boiler will provide the necessary boiler cooling capacity during and after the draining operation.

When the desired water level is attained, the draining will terminate, and the reactor will be brought to criticality using the outer eight boiler rods. Additional reactivity will be inserted until a power level of 10 KW(th) is reached (assuming no decay heat; with decay heat this step is eliminated). It should be noted that the 10 KW(th) power value is considerably below the thermal capabilities of either the superheater or the boiler at zero flow conditions.

2.3.4.3 PHASE III -- Approach to Maneuvering Power

At this point a steam flow equivalent to 1 per cent of full flow will be established and reactor power will be brought to that level where 1 per cent steam is being generated in the boiler (about 1 per cent of full power). The radial factor will increase to 2.18 ± 10 per cent, and the power fraction will now be 6.96 per cent. It is expected that any drop in reactor pressure resulting from the independent control of steam flow and power will be small. All three recirculation pumps will be operating. The feedwater inlet temperature will be maintained at a temperature not lower than 100 F.

After establishing a steady-state condition, the steam flow will be increased to 2.5 per cent of full flow and then the power increased to about 2.5 per cent of full power. Similar increases in flow, and then power, will be made until a steady-state condition of 20 per cent of full steam flow, 20 per cent of full power condition is attained (about 20 per cent reactor power).

Operation of the reactor at the low steam flow rates required for startup introduces two major areas of design uncertainty: (1) non-turbulent flow conditions, and (2) reactor heat losses.

During startup, laminar, transitional or turbulent flow through the superheater fuel elements may occur. As Figure 2.35 shows, the steam flow rates during reactor startup cover the three ranges of possible flow regimes. The discussion presented previously on the physical factors affecting the heat transfer film coefficient (h) and the friction factor (f) take on increased significance in the region of non-turbulent flow. No longer is the velocity profile relatively flat across the coolant annuli as with turbulent flow. A detailed description of this velocity profile with consideration for the effect of wire spacers, uneven heating in the annulus, variable fluid properties, etc., would be required to evaluate the performance of the annular superheater fuel element. The lack of the above type of information, analytic or experimental, precludes an exact heat transfer and hydraulic behavior prediction.

However, an estimate of the largest deviation in the temperature predictions resulting from the use of a turbulent flow model in the laminar and transitional flow region is possible. Because of the parallel channel core pressure drop coupling between the average channel and hot channel, the steam flow rate is virtually unaffected by the selection of friction factor correlation. The bulk temperature rise prediction, using the turbulent model, will be quite accurate, more than when calculated over the laminar or transitional flow range.

Application of a turbulent flow correlation for predicting film temperature in the laminar and transitional range, however, is less accurate. Figure 2.36 shows a plot of the ratio of the turbulent heat transfer

coefficient h local to the laminar heat transfer coefficient h local as a function of Reynolds Number. As expected, the largest film temperature factor ($h_{\text{turbulent}}/h_{\text{laminar}} = 3$) occurs at the highest Reynolds Number (10,000). For lower Reynolds Numbers the agreement between the laminar and turbulent correlations is better. They are equal at a Reynolds Number of 3500.

Hence, the estimation of the expected surface temperature, assuming turbulent flow at all flow rates, is justified. The possibility of true laminar flow existing at a Reynolds Number as large as 10,000 is remote, and it is conceivable that a deviation as large as 3 at a Re number of 10,000 can be obtained by applying the turbulent model. The experimental data of McAdams support this contention (Ref. 2.10).

The evaluation of the reactor startup procedures requires an accurate accounting of the heat losses from the reactor to the surroundings and also between the superheater and the boiler. At the low steam flow rates the sensitivity of the bulk steam temperature to heat loss increases markedly.

Figures 2.37 and 2.38 show the heat losses from the active superheater fueled region and non-fueled region, respectively. Figure 2.39 presents the (n, γ) heat generated in the superheater moderator which in turn leaks into the boiler region. The influences of the steam flow rate (convection film resistance) and system pressure (moderator or sink temperature) on the heat losses are shown.

At no time during Phase III operation will the fuel surface temperature in the superheater exceed the maximum 1270 F temperature expected at full

power. The maximum surface temperature calculated for the superheater fuel during the approach to maneuvering power will be 950 F. This reduction in surface fuel temperature at these low powers, from the full power and flow value, is due primarily to the lower superheater power fraction. The control rod pattern in the reactor shields the superheater, reducing its power fraction from the full power 0.1668 value to 0.0690 during this low period.

The bulk outlet steam temperatures from the superheater were calculated over the flow rate range from 5 per cent to 20 per cent. Line A of Figure 2.40 is this expected temperature trace as the reactor is brought up to maneuvering power. The maximum bulk outlet temperature is 535 F which occurs at the 20 per cent of full flow condition.

The steam temperatures and flow rates, which give rise to the 1270 F fuel surface temperature for Phase III operation, appear as line B of Figure 2.40. As line B indicates, the steam temperature could rise to 583 F at the 20 per cent flow point before the 1270 F fuel surface temperature is attained.

There exists, then, a considerable margin of safety before limiting superheater temperatures are exceeded. An increase in superheater power of 40 per cent without any increase in steam flow would be required to attain the 1270 F limiting surface temperature conditions. Furthermore, any conceivable reduction in the inlet feedwater temperature will not produce intolerable steam conditions.

2.3.4.4 PHASE IV -- Reactor Pressurization

Pressurization of the system from 315 psia to the reference 615 psia

pressure will begin after steady-state reactor operation at 20 per cent of full steam flow is attained. Small stepwise increases in the system pressure will be accomplished by step reductions in steam flow rate. When main steam line isolation valve is closed slightly, the pressure drop across the steam line will increase almost immediately. As the power in the reactor will be held constant at its 20 per cent value, the reactor dome pressure will increase. The bulk steam temperature out of the superheater will also increase.

The reactor dome pressure will continue to increase until its magnitude is sufficient to drive the total amount of steam generated in the boiler through the superheater. The system, then, will again be at steady-state. Approximately the same steam flow rate and bulk temperature will be obtained as existed before closing the isolation valve. The system pressure will now, however, be slightly higher.

Reductions in steam flow will be controlled to step changes less than 10 per cent of the steady-state - 20 per cent flow rate. The control rod configuration and radial power factor will remain at Phase III, 2.18 ± 10 per cent and 6.96 per cent values, respectively. Feedwater temperatures will gradually be increased during this phase until a temperature of 350 F at the 615 psia pressure has been reached.

During pressurization of the system, a maximum steady-state fuel surface temperature of 945 F has been calculated for the superheater at 315 psia and 920 F at the final 615 psia pressure. The steam flow reduction will cause the surface temperature to increase briefly by 90 F. However, this

temperature drops within 50 seconds to some intermediate steady-state temperature (between 920 F and 945 F).

The bulk steam temperature from the superheater will vary from 535 F at 315 psia to 563 F at 615 psia during this period. Again a brief period of slightly higher bulk steam temperature will occur immediately after the movement of the control valve. This rise will be effectively damped reaching only 590 F at its maximum and returning to some intermediate temperature after 50 seconds.

Curve C of Figure 2.40 shows the expected bulk temperature rise as the system pressure is increased to 615 psia. The expected bulk steam temperatures during the pressurization process are seen to be well below the limiting values. A superheater power increase of 44 per cent occurring at 615 psia without any increase in steam flow would be required to attain a maximum fuel surface temperature of 1270 F. The inlet feedwater temperature would have to be reduced from 350 F to about 100 F before the excessive steam temperature conditions are reached.

Line D of the same Figure represents the limiting (1270 F surface temperature) steam conditions for 615 psia system pressure. Bulk steam temperature of 622 F would be required to exceed the tolerable 1271 F surface temperature, at this 20 per cent steam flow.

2.3.4.5 PHASE V -- Conversion to Rod Configuration

With the reactor system pressure established at 615 psia and operating at 20 per cent of full power and flow at steady-state, the adjustment of the control rods will begin. The outer eight control rod banks in the

boiler will be inserted, and the inner eight control rod banks in the boiler will be withdrawn to hold the steam flow and power at about the 20 per cent level.

During this adjustment in boiler control rods, the superheater control rods will remain fully inserted. An increase in superheater fractional power to 0.0923 has been calculated. The radial power factor in the superheater will remain at 2.18 ± 10 per cent.

The higher superheater power fraction will result in an increase in the bulk steam temperature from the superheater. As line E of Fig. 2.40 indicates, the bulk steam temperature will increase from 563 F to 592 F during control rod adjustment. These steam temperatures are still below the limiting 622 F temperature value as shown by line D, Figure 2.40.

During rod manipulation the maximum surface temperature will be 1050 F. To attain the 1271 F surface value the superheater power would have to increase 22 per cent without any increase in steam flow. An inlet feedwater temperature reduction from 350 F to 100 F, a remote possibility, would also be required before the superheater temperature becomes excessive.

Following Phase V, the superheater control rods will be withdrawn to increase the superheater power fraction while providing reactivity compensation with the boiler rods. The superheater power fraction will be increased until the reference 725 F outlet steam temperature condition has been reached.

The reactor protection system is, of course, in operation throughout the course of the startup period. A thorough analysis of possible transients

during this period has shown all such transients to be less severe than similar transients initiated at higher power levels (i.e., more than 20 per cent).

It is noted that two of the steps in the startup procedure require control rod manipulation while holding power approximately constant. These steps are being examined further so that power distributions associated with intermediate rod configurations for short periods can be taken into account.

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2.5 ILLUSTRATIONS

Pertaining to Section 2

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Fig. 2.1...Superheater Core Layout (43-002-109)

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NOTE 1: LEVEL TUBE TOLERANCES SHALL BE $\pm .001$ ABSOLUTE (NOT AVE.) ON BOTH O.D. & I.D. INCLUDING QUALITY. AVERAGE O.D. & I.D. TOLERANCE TO BE $\pm .001$ (SEE NOTE 3). TOLERANCE ON O.D. OF THE CENTER POISON ROD (ITEM 7) SHALL BE $\pm .001$ INCLUDING QUALITY.

NOTE 2: B.P. #1, B.P. #2, AND B.P. #3 APPLIES TO ANY TRANSVERSE PLANE OF ITS PARTICULAR COMPONENT.

NOTE 3: AVERAGE FUEL TUBE DIAMETERS MUST BE DETERMINED BY AIR GAGE SPINDLES OR RINGS WITH AT LEAST THREE (3) JETS.

NOTE 4: SPACER WIRES SHALL EXTEND TO $\frac{3}{8}$ FROM THE BOTTOM OF THE TUBES.

NOTE 5: ABSOLUTE DIAMETERS SHALL BE DETERMINED BY .025 JET AIR GAGES.

NOTE 6: PENETRATION OF THE TIG WELD SHALL EXCEED .030 FROM THE SURFACE OF THE TUBE. THE WELD SHALL BE GROUND FLUSH.

NOTE 7: TOTAL TROUGHOUT OF THE FINISHED FUEL ELEMENT SHALL NOT EXCEED .020 T.I.R. NOR .005/FT. T.I.R. LOCALLY.

NOTE 8: MINIMUM INNER FLOW CHANNEL SHALL EQUAL $H_1 - .001$ AT ANY POINT. MINIMUM MIDDLE FLOW CHANNEL SHALL EQUAL $H_2 - .001$ AT ANY POINT.

DETAIL 'F'
REFER TO ITEM 14 FOR DEFINITION OF CLAD

$$H_1 = \frac{\text{AVERAGE I.D. OF L.F.T.} - \text{AVERAGE O.D. OF P.R.}}{2} = 1.0005$$

$$H_2 = \frac{\text{AVERAGE I.D. OF O.F.T.} - \text{AVERAGE O.D. OF L.F.T.}}{2} = 1.0005$$

$$H_3 = \frac{.925 \text{ AVERAGE O.D. OF O.F.T.}}{2} = 1.0005$$

$$B.P. \#1 = \text{AVERAGE I.D. OF L.F.T. } 1.001$$

$$B.P. \#2 = \text{AVERAGE I.D. OF O.F.T. } 1.001$$

$$B.P. \#3 = .925 \pm .001$$

ITEM	DESCRIPTION	MATERIAL	QTY
1	SUPERHEATER FUEL ELEMENT ASSEMBLY	THIS	001
2	GUIDE / SPACING FIN	THIS	001
3	GUIDE / SPACING FIN	THIS	001
4	GUIDE / SPACING FIN	THIS	001
5	LOWER POISON ROD PLUG	THIS	001
6	UPPER POISON ROD PLUG FITTING	THIS	001
7	CENTER POISON ROD (P.R.)	THIS	001
8	POISON PELLETS .0025 $\pm$.0005 $\frac{3}{4}$ LG.	THIS	001
9	INNER FUEL TUBE (O.F.T.)	THIS	001
10	OUTER FUEL TUBE (O.F.T.)	THIS	001
11	SPACER WIRE	THIS	001
12	SPACER WIRE	THIS	001
13	SPACER WIRE	THIS	001
14	FUEL ELEMENT ASSEMBLY PRODUCT SPEC.	43-500-881	001
15	WELD ROD (ASTM A198-55T CLER SIGEL)	316	001

Fig. 2.2...Superheater Fuel Element Assembly (43-500-881)

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Fig. 2.4...Friction Factors for Superheated Steam in an Isothermal (Unheated)
Annular Test Section at 600 PSI with a 60 mil Gap (43-024-975)

Fig. 2.5 Friction Factor for Superheated Steam in a Heated Annular Test Section at 600 PSI with a 60 mil Gap...Steam Properties Evaluated at Bulk Temperature (43-024-978)

Fig. 2.6...Friction Factors for Superheated Steam in a Heated Annular Test Section at 600 PSI with a 60 mil Gap...Steam Properties Evaluated at Film Temperature (43-024-976)

Fig. 2.7...Friction Factor for Superheated Steam in a Heated Annular Test Section at 600 PSI
with a 60 mil Gap...Steam Properties Evaluated at Heated Surface Temperature (43-024-972)

Fig. 2.8...Heat Transfer Coefficients for Superheated Steam in Annulus with Both Walls Heated (43-024-974)

STER CODE — $h_1 = K/D \cdot 0.0213 (Re)^8 (Pr)_f^{1/3} [1 + 2.3/L/D]$

HEINEMAN — $h_4 = 0.0133 (Re)^{84} (Pr)_f^{1/3} L/D^{.760}$

Fig. 2.9...Heat Transfer Coefficient Comparison (43-025-218)

Fig. 2.10...Pathfinder High Enriched Superheater Power Profile - Axial (43-024-817)

Fig. 2.11... Axial Outer Superheater Fuel Channel Steam Temperature Profile (43-025-219)

Fig. 2.12...Fueled Region Heat Loss vs Insulating Gap Thickness (43-025-220)

Fig. 2.13...Intermediate Region Heat Loss vs Bulk Steam Temperature
(43-025-225)

Fig. 2.14...Lower Process Tube Heat Loss vs Bulk Steam Temperature
(43-025-224)

Fig. 2.15...Total Heat Loss vs Insulating Gap Thickness (43-025-226)

Fig. 2.16...Total Heat Received in Superheater Moderator vs Insulating Gap Thickness (43-025-227)

FUELED REGION INSULATION GAP - MILS

Fig. 2.17...Superheater Moderator Exit Voids vs Insulating Gap Thickness (43-025-228)

Fig. 2.18...Superheater Heat Loss Curve for STER Program (43-025-229)

Fig. 2.19...Temperature Peaking Factor vs Effective Spacer Width
 (43-025-230)

Fig. 2.20...Temperature Peaking Factor vs Fraction of Surface Insulated (43-025-231)

Fig. 2.21...Temperature Peaking Factor vs Length of Unbond (43-025-232)

Fig. 2.22...Temperature Peaking Factor vs Effective Dimension of Spacer or Unbond (43-025-233)

Fig. 2.23...Maximum Superheater Fuel Surface Temperature vs Average Bulk Steam Outlet Temperature (43-024-815)

Fig. 2.24... Maximum Superheater Fuel Surface Temperature vs Radial Factor (43-024-812)

SUPERHEATER
POWER
FRACTION

BULK
STEAM TEMP
@ FULL POWER

Fig. 2.25...Operational Performance of Superheater (43-024-779)

Fig. 2.26...Superheater Fuel Burnup vs Superheater Fuel Surface Temperature (43-024-813)

Fig. 2.27...Superheater Low-Power Performance (43-025-234)

Fig. 2.28...Superheater Over-Power Performance (43-025-235)

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Fig. 2.30...Superheater Control Rod Nozzle Discharge Coefficients (43-024-646)

Fig. 2.31...Emessivity Comparison - Test Section No. 3
vs Two Values from Theoretical Model (43-025-239)

Fig. 2.32...Compilation of Results from Afterglow Test (43-025-237)

PERCENT OF REACTOR POWER (FOR 725 STEAM OUTLET TEMPERATURE CASE)

CURVES:

- A ——— NOMINAL AVERAGE CHANNEL DIMENSIONS
- B - - - - AVERAGE CHANNEL DIMENSIONS WITH TOLERANCES
APPLIED TO MAKE ALL GAPS MAXIMUM WIDTH SIMULTANEOUSLY
- C - - - - CURVE B FOR INNER FUEL TUBE WITH ADDED 1.25 UNCERTAINTY FACTOR

HOT SPOT FACTORS APPLIED TO CURVES A,B,AND C:

AXIAL = 1.35
RADIAL = 1.43
FUEL LOADING = 1.15

Fig. 2.33...Superheater Shutdown Temperatures for High Enrichment Element vs Reactor Power (43-202-548)

REACTOR PRESS. = 550 PSIA

Fig. 2.34...Permissible Reactor Power as a Function of Inlet Cooling Water Rate and Temperature (43-024-942)

Fig. 2.35...Outer Superheater Channel Flow Regimes (43-024-940)

Fig. 2.36...Heat Transfer Coefficient Ratio vs Reynolds Number (43-024-399)

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Fig. 2.37...Superheater Fueled Region Total Core Heat Loss (43-024-979)

Fig. 2.38... Total Heat Losses from Superheated Steam Between Fueled Channel Outlet and Console Meter Thermocouple (43-202-589)

Fig. 2.39...Superheater Moderator Heat Generation as a Function of Mean Bulk Steam Temperature (43-024-943)

- A. Expected Phase III operating line.
- B. Permissible steam line thermocouple temp., all rods in, 315 PSIA.
- C. Expected Phase IV operating line.
- D. Permissible steam line thermocouple temp., all rods in, 615 PSIA.

- E. Expected Phase V operating line.
- F. Expected approach to reference steam temp. operating line.
- G. Permissible steam line thermocouple temp., all rods out, 615 PSIA.

Fig. 2.40...Superheater Bulk Steam Temperature During Reactor Startup (43-202-590)