

A THERMAL-HYDRAULIC ANALYSIS OF A POOL-TYPE
REACTOR IN NATURAL CIRCULATION

A Thesis

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ABSTRACT

A proposal to increase the power of the NRAD reactor at Argonne National Laboratory-West is evaluated to determine whether the thermal-hydraulic safety limits would be exceeded. To do this, a digital computer program for single-phase and two-phase natural circulation flow is developed. The results from this program indicate that the NRAD proposal is unacceptable, because void-induced power oscillations are predicted to be present at the proposed power.

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Chapter 1

INTRODUCTION

This thesis is an analysis of the thermal-hydraulics of a low-power research reactor, the Neutron Radiography (NRAD) reactor, at Argonne National Laboratory-West, in Idaho. The reactor is a TRIGA-FLIP reactor designed by General Atomic Company for research and irradiation at power levels up to 2.0 megawatts (MW). The need for this analysis arose in the course of work conducted by Wade Richards of Argonne National Laboratory on a proposal to increase the NRAD power from the present 0.3 MW power to a power of 1.0 MW.

The most important thermal-hydraulic characteristic of the NRAD reactor is that it operates with natural circulation cooling in the core--in other words, the coolant flow in the core is driven solely by the bouancy of the heated water in the coolant channels between the fuel elements. The significance of this fact is that the coolant flow rate through the core depends on the thermal power of the reactor and is influenced by such factors as wall friction and steam produced by nucleate boiling on the fuel element surface. The situation is further complicated by a non-uniform axial heat flux profile. This analysis attempts to

model these phenomena, with the goal of predicting the thermal-hydraulic behavior of the NRAD core at present and proposed power levels. Because of the complexity of the problem, a digital computer program was developed to accomplish this goal.

The present NRAD core is composed of fuel elements from the Puerto Rico Nuclear Center (PRNC) reactor which operated in the 1960's. The PRNC reactor was designed to operate at a power of 2.0 MW, but in operation experienced power oscillations at about 1.2 MW.⁽¹⁾ A likely cause of these power oscillations was the presence of significant steam volume fractions (void fractions) in the high power regions of the core. This problem is discussed in the NRAD safety analysis report, where it was calculated that, for the present 0.3 MW core, an average 0.6% void fraction in the upper half of the nine hottest channels could result in power oscillations of 1%.⁽²⁾ Since the NRAD reactor is used for neutron radiography, significant power oscillations are undesirable. The problem of void-induced power oscillations will be considered in this analysis.

The proposed 1.0 MW NRAD power level can be considered acceptable, from a thermal-hydraulic viewpoint, if the following conditions are met:

1) The maximum fuel element cladding temperature is below the technical specification limit of 1652°F. (3)

2) The heat flux at all locations is less than the critical heat flux by a certain margin. The minimum allowable margin can only be determined by statistical methods, and is beyond the scope of this analysis.

3) Power oscillations are not present. Void related instabilities are difficult to predict analytically; thus, this analysis will compare the thermal-hydraulic conditions in the 1.0 MW NRAD core to the conditions in the PRNC core at 1.2 MW. It is assumed that if the NRAD conditions are less severe than those of the PRNC core at the onset of oscillations, then oscillations will not be present in the NRAD reactor.

The following analysis attempts to determine whether the 1.0 MW NRAD core will be able to operate within these limits.

Chapter 2

REACTOR DESCRIPTION

The NRAD reactor is described in this chapter. Section 1 gives a physical description of the reactor; the information presented comes from Reference 2. Section 2 describes the idealized model of a single core flow channel which is used in Chapter 3 to develop the thermal-hydraulic equations used in this analysis.

2.1. General Description

The NRAD reactor has a core composed of TRIGA-FLIP fuel elements of 70% enrichment arranged in a square lattice. There are a total of 61 fuel elements in the present core; the proposed core will have 80 fuel elements. The fuel elements are held in clusters of four by upper and lower end fittings. These end fittings have flow holes to allow the cooling water easy access to the fuel element flow channels. A typical cluster is shown in Figure (2-1). The flow channel formed by the fuel elements is shown in Figure (2-2). The lower end fitting of each cluster is fitted into a hole in the grid plate, which supports the fuel clusters. The core, grid plate, and core support structure are shown in Figure (2-3).

The NRAD core is contained in an open tank 6.5 feet in diameter, 11.5 feet high, and containing about

ALL DIMENSIONS IN INCHES

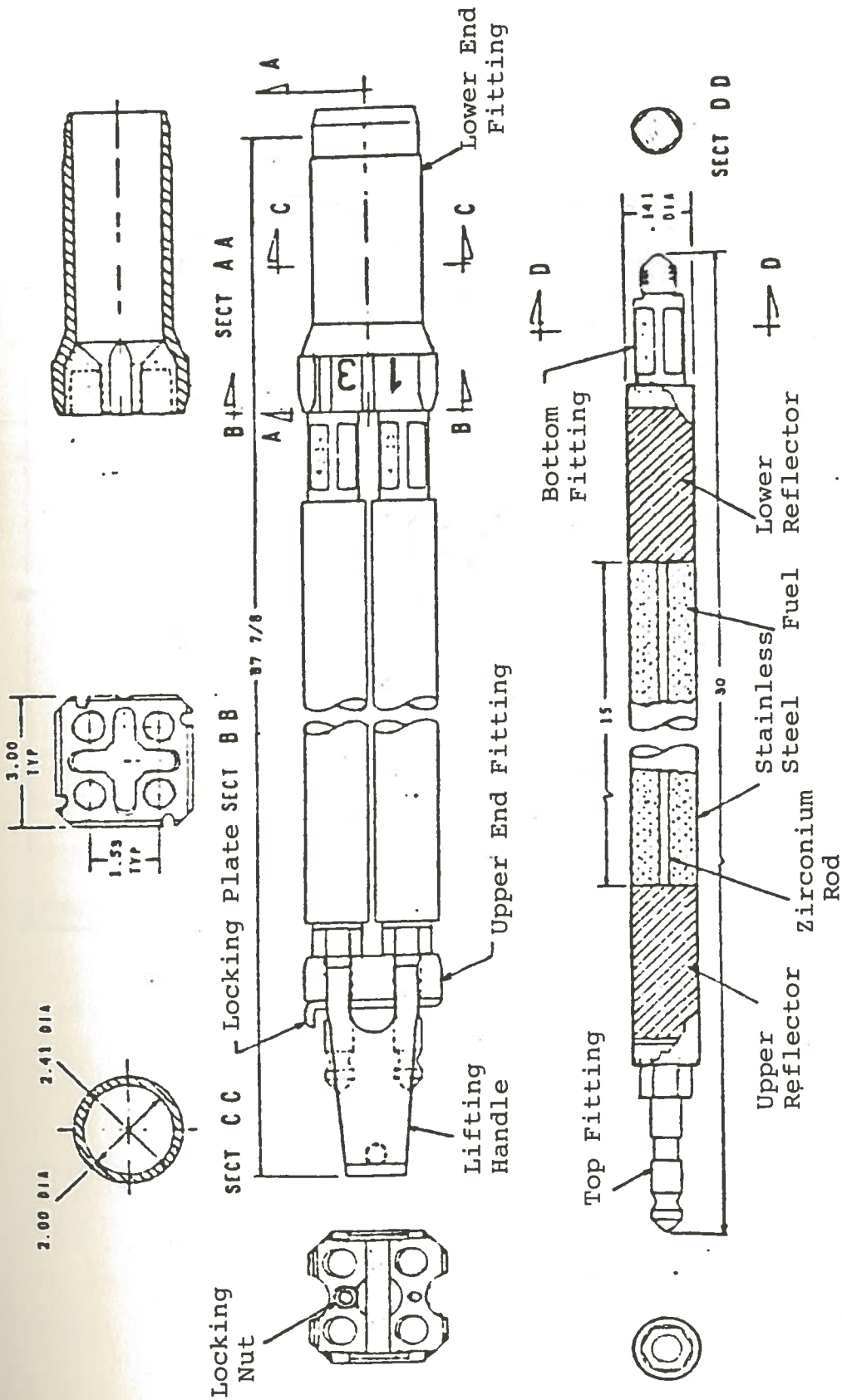


Figure 2-1
Fuel Element Cluster

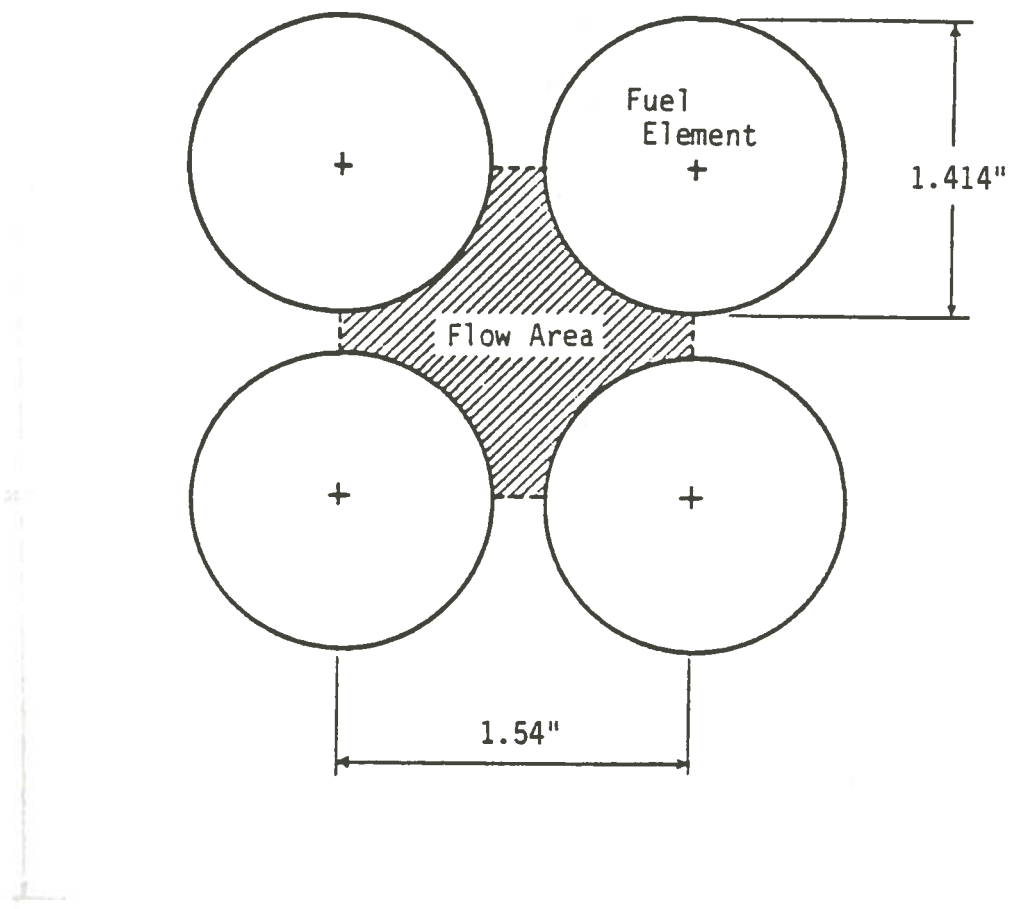


Figure 2-2
Fuel Cluster Flow Channel

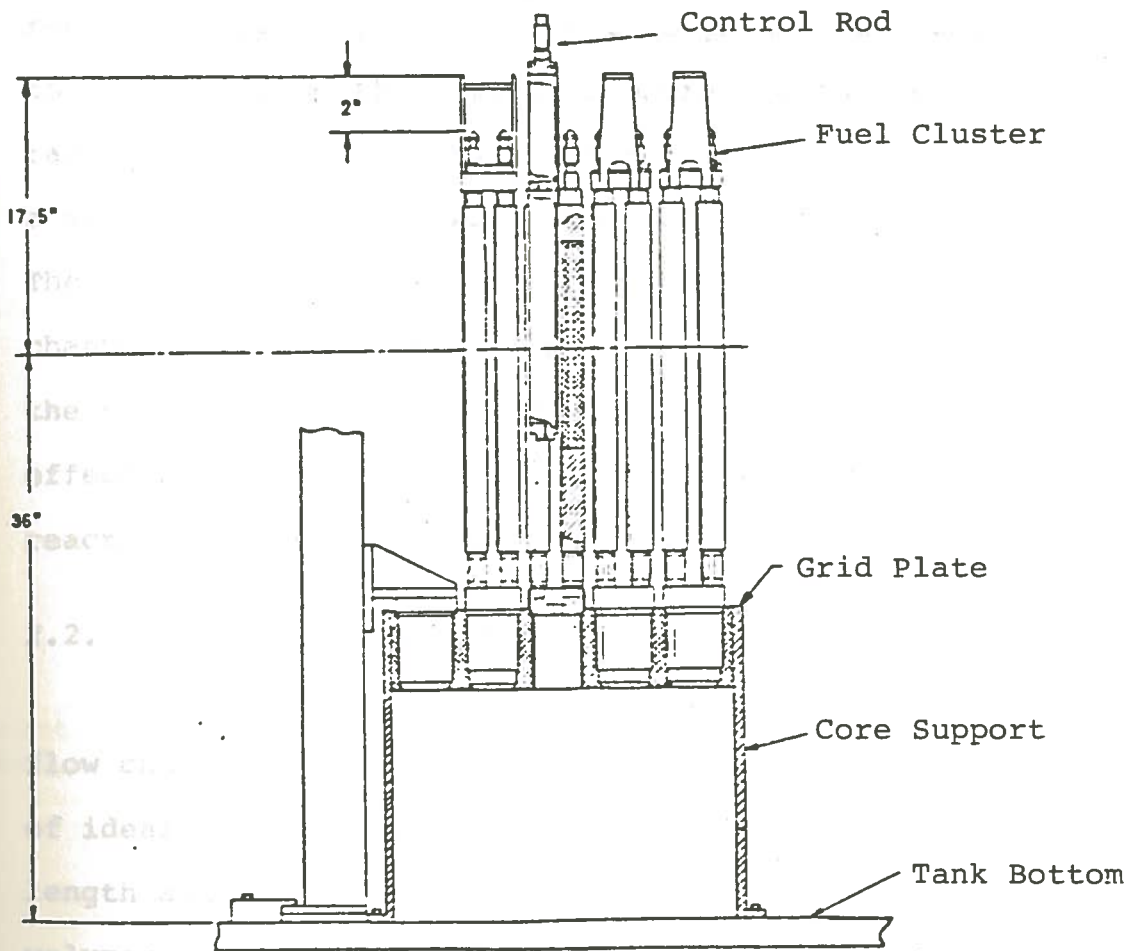


Figure 2-3

NRAD Core and Core Support Structure

area), and

2700 gallons of water. The water acts as both a coolant and a moderator for the reactor core. During reactor operation, the thermal power produced in the core is removed by pumping tank water through a heat exchanger to maintain a constant water temperature in the reactor tank. The cooled water is returned to the tank in such a way as to induce a swirl flow pattern in the tank water. The influence of this swirl flow on the flow in the hot channel is unknown, but probably is not important since the hottest channels are in the center of the core. Any effect of swirl flow is neglected in this analysis. The reactor tank is shown in Figure (2-4).

2.2. Flow Channel Geometry

For the purpose of this analysis, the coolant flow channel through a fuel cluster is treated as a series of idealized fluid volumes, each described by a vertical length and a flow area. In addition, the fuel rod volumes have a hydraulic diameter and heated perimeter specified; these dimensions are used in friction and heat transfer calculations.

Dimensions for the fuel element volumes are given in Table (2-1). All other dimensions were scaled from the drawing in Figure (2-1). The following volumes were used: two volumes in the lower end fitting (one for each flow area), and one volume each for the bottom fitting of the

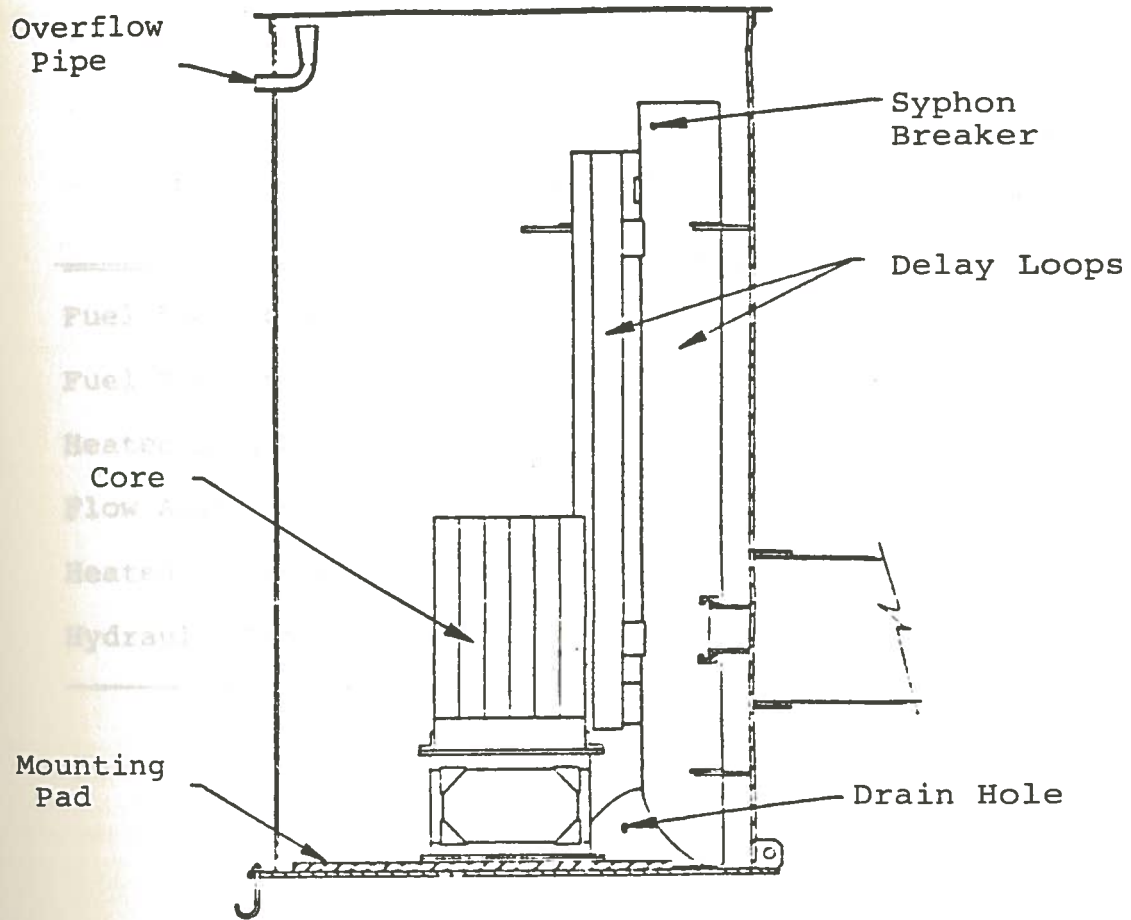


Figure 2-4

NRAD Reactor Tank

Table 2-1

NRAD Fuel Cluster Flow Channel Dimensions

Fuel Element Diameter (in)	1.414
Fuel Element Pitch (in)	1.53
Heated Length (in)	15
Flow Area (in ²)	0.769
Heated Perimeter (in)	4.44
Hydraulic Diameter (in)	0.694

fuel element, the lower reflector, the heated length, the upper reflector, the top fitting of the fuel element, and the upper end fitting. The calculation of local pressure drops at the inlet and outlet to the flow channel also requires the flow area of the reactor tank. The dimensions of these flow volumes are given in Table (2-2). Between each pair of volumes of different flow area is a sudden area change.

Table 2-2

Table 2-2
Flow Volume Dimensions

Volume	Flow Area (ft ²)	Flow Length (ft)	Hydraulic Diameter (ft)	Heated Perimeter (ft)
Reactor Tank	32	--	--	--
Lower End Fitting #1	0.0218	0.438	--	--
Lower End Fitting #2	0.0141	0.118	--	--
Bottom Fitting	0.0108	0.1732	--	--
Lower Reflector	0.00534	0.3293	0.0578	--
Heated Length	0.00534	1.25	0.0578	0.37
Upper Reflector	0.00534	0.3475	0.0578	--
Top Fitting	0.0101	0.0625	--	--
Upper End Fitting	0.00545	0.1042	--	--
Reactor Tank	32	--	--	--

Chapter 3

THERMAL-HYDRAULIC EQUATIONS

This chapter describes the equations used to calculate the flow channel pressure drop, the wall temperature and critical heat flux, the liquid subcooling, and, for two-phase flow, the void fraction. The fluid flow equations presented here are one-dimensional, steady-state equations, based on average fluid properties and velocities over the channel cross-section. Crossflow between channels is assumed to be negligible and is not considered; this is a conservative assumption.

3.1. General Equations

The coolant flow rate, W , in any flow channel in the core is calculated by finding the flow rate which gives a total pressure drop through the channel equal to the elevation, or static, pressure drop in the tank water over the height of the core. This static pressure drop is the boundary condition for natural circulation. Since for natural circulation the dominant pressure drop through the flow channel is the elevation pressure drop, and the elevation pressure drop is influenced by the amount of heating of the coolant in the channel, the flow rate is different in each flow channel, due to the difference in

power in each channel. The hottest, or limiting, channel is investigated in this analysis, since this is the channel where the most severe thermal-hydraulic conditions will occur.

The natural circulation condition described above can be stated as follows:

$$\Delta p_{\text{tot}} = \rho_{\text{avg}} \frac{g}{g_c} H_{\text{core}} \quad (3-1)$$

Here Δp_{tot} is the calculated total pressure drop through the flow channel, H_{core} is the total height of the core, ρ_{avg} is the average tank water density in the height H_{core} , g is the acceleration due to gravity, and g_c is the gravitational conversion factor. The total pressure drop can be broken into three parts:

$$\Delta p_{\text{tot}} = \Delta p_{\text{in}} + \Delta p_{\text{LH}} + \Delta p_{\text{out}}, \quad (3-2)$$

in which Δp_{in} is the pressure drop in the inlet region, including the lower reflector, Δp_{LH} is the pressure drop in the heated length, and Δp_{out} is the pressure drop in the outlet region, including the upper reflector. Because of the wide variation of liquid temperature, steam void fraction, and heat flux over the heated length, the heated length is broken into increments of equal length

ΔZ and a pressure drop, Δp_i , is calculated for each increment. The three pressure drop terms in Equation (3-2) are the sums of several pressure drop components, as shown in the following equations:

$$\Delta p_{in} = \Delta p_{e,in} + \Delta p_{f,LR} + \sum_{j=1}^4 \Delta p_{\ell,j} \quad \text{and} \quad (3-3a)$$

$$\Delta p_{out} = \Delta p_{e,out} + \Delta p_{f,UR} + \sum_{j=5}^7 \Delta p_{\ell,j}, \quad (3-3b)$$

where

$\Delta p_{e,in}$ = elevation pressure drop in inlet

$\Delta p_{f,LR}$ = friction pressure drop in lower reflector

$\Delta p_{\ell,j}$ = local pressure drop at area change j

$\Delta p_{e,out}$ = elevation pressure drop in outlet

$\Delta p_{f,UR}$ = friction pressure drop in upper reflector,

and

$$\Delta p_{LH} = \sum_{i=1}^N \Delta p_i = \sum_{i=1}^N [\Delta p_{e,i} + \Delta p_{f,i} + \Delta p_{a,i}], \quad (3-4)$$

where

$\Delta p_{e,i}$ = elevation pressure drop in increment i

$\Delta p_{f,i}$ = friction pressure drop in increment i

$\Delta p_{a,i}$ = acceleration pressure drop in increment i

N = number of increments in heated length.

These pressure drop components are covered in more detail in later sections.

The rise in the liquid temperature in each increment in single-phase flow is calculated from

$$T_{k+1} = T_k + \frac{q_i'' P_H \Delta Z}{W C_{p,i}}, \quad (3-5)$$

in which T is the bulk liquid temperature, q'' is the local channel heat flux, P_H is the heated perimeter, and C_p is the liquid specific heat. In Equation (3-5), the subscript k and $k+1$ refer to the lower and upper ends, respectively, of increment i and the subscript i refers to the average of the values at points k and $k+1$. This notation is retained throughout this chapter. The calculation of the local heat flux, q'' , is covered in a later section.

3.2. Single-Phase Pressure Drop

The elevation pressure drop in single-phase flow is given by the general equation

$$\Delta p_e = \rho \frac{g}{g_c} L, \quad (3-6)$$

which is the same as Equation (3-1). Here L is the axial height considered and ρ is the average density over the height L . In the inlet region L is the total length

of the first four volumes (the height from the channel inlet to the beginning of the heated length) and ρ is the density of the inlet water. In the outlet region L is the total length of the last three volumes (the height from the end of the heated length to the channel outlet) and ρ is the density of the coolant leaving the heated length. In the heated length, Equation (3-6) becomes

$$\Delta p_{e,i} = \left[\frac{\rho_k + \rho_{k+1}}{2} \right] \frac{g}{g_c} \Delta Z. \quad (3-7)$$

The local pressure drop, $\Delta p_{\ell,j}$, is calculated from (4)

$$\Delta p_{\ell,j} = \left[\frac{A_j^2}{A_2^2} - \frac{A_j^2}{A_1^2} + e_v \right] \frac{W^2}{2 \rho_j g_c A_j^2}, \quad (3-8)$$

where

A_1 = flow area upstream of area change j

A_2 = flow area downstream of area change j

A_j = the smaller of A_1 and A_2

e_v = irreversible pressure loss coefficient at area change j

ρ_j = liquid density at area change j .

The term

$$\frac{A_j^2}{A_2^2} - \frac{A_j^2}{A_1^2}$$

in Equation (3-8) is the reversible pressure loss coefficient. It should be noted that e_v is the loss coefficient corresponding to the velocity in the smaller flow area. Since the flow in the lower end fitting supplies four channels, the flow rate used in Equation (3-8) for the first two area changes is four times W . The loss coefficients used in this analysis are given in Table (3-1).

The friction pressure drop in the heated length, $\Delta p_{f,i}$, is given by

$$\Delta p_{f,i} = f_i \frac{\Delta Z}{D_H} \frac{W^2}{2 \rho_i g_c A_f^2}, \quad (3-9)$$

where

f_i = average friction factor in increment i

D_H = hydraulic diameter

ρ_i = average density in increment i ,

A_f = flow area in fuel rod channel.

This equation and the friction factor equations which follow are from Reference 5. The friction factor, f_i , includes the effect of wall heating, and is calculated from the isothermal friction factor, f_{iso} , by the equation

$$f_i = f_{iso} \left[\frac{\mu_{w,i}}{\mu_i} \right]^{0.6}, \quad (3-10)$$

Table 3-1
Pressure Loss Coefficients
(Reference 5)

Area Change #	Upstream Flow Area (ft ²)	Downstream Flow Area (ft ²)	Loss Coefficient
1	32	0.0218	0.53
2	0.0218	0.0141	0.27
3	0.0141	0.0108	0.23
4	0.0108	0.00534	0.35
5	0.00534	0.0101	0.18
6	0.0101	0.00545	0.31
7	0.00545	32	1.0

in which $\mu_{w,i}$ is the liquid viscosity evaluated at the average wall temperature, $T_{w,i}$, and μ_i is the liquid viscosity evaluated at the average bulk temperature, T_i . Three flow regimes are considered in calculating the isothermal friction factor: laminar flow, transition flow, and turbulent flow. The laminar flow equation is

$$f_{iso} = \frac{64}{Re_i}, \quad (3-11)$$

where

$$Re_i = \frac{W D_H}{A_f \mu_i}. \quad (3-12)$$

The end of the laminar region and the beginning of the transition region is assumed to occur at $Re = 2100$. Turbulent flow is assumed to begin at $Re = 4000$. The equation for turbulent flow is

$$f_{iso} = 0.184 Re_i^{-0.2}. \quad (3-13)$$

In the transition region, $2100 < Re < 4000$, the isothermal friction factor is assumed to vary linearly from the laminar friction factor at $Re = 2100$ to the turbulent friction factor at $Re = 4000$. Equation (3-9) is also used to calculate the friction pressure drop in the lower and upper reflector volumes. In this case, the increment

i becomes the particular volume considered and ΔZ becomes the volume length.

The acceleration pressure drop is given by⁽⁶⁾

$$\Delta p_{a,i} = \frac{W^2}{g_c \rho_i A_f^2} \left[\frac{1}{\rho_{k+1}} - \frac{1}{\rho_k} \right]. \quad (3-14)$$

This pressure drop is caused by fluid acceleration due to the decrease in density brought about by heating.

3.3. Steam Quality and Void Fraction

In order to calculate the pressure drop in the two-phase region, the steam quality and void fraction must be calculated. The method used is a modification of the subcooled void fraction model of Roubani.⁽⁷⁾ This model takes a mechanistic, or phenomenological approach to the subcooled boiling process. A mechanistic model was chosen because of the non-uniform axial heat flux profile and the shortage of useful experimental data at the low pressures and flow rates characteristic of the NRAD core. The general procedure used is to first calculate the steam quality, considering steam generation at the heated surface and condensation of the steam in the bulk stream, and then to calculate the void fraction from the quality. Since the amount of condensation depends on the amount of steam present, the equations are iterated on quality and void fraction until a solution is reached. The

following analysis assumes that the vapor phase is at saturation.

The steam quality, X , is defined as

$$X = \frac{W_g}{W}, \quad (3-15)$$

in which W_g is the mass flow rate of saturated steam and W is the total mass flow rate. The void fraction, α , is defined as

$$\alpha = \frac{V_g}{V}, \quad (3-16a)$$

where V_g is the steam volume and V is the total mixture volume. At any point in the channel, the void fraction averaged over the channel flow area is

$$\alpha = \frac{A_g}{A}, \quad (3-16b)$$

in which A_g is the flow area occupied by vapor and A is the total channel flow area, A_f .

The change in quality in increment i is given by

$$X_{k+1} = X_k + \frac{\Delta Q_{b,i} - \Delta Q_{c,i}}{W h_{fg,k}} \quad (3-17)$$

Here $\Delta Q_{b,i}$ is the amount of heat per unit time in increment i that goes into steam production, $\Delta Q_{c,i}$ is the amount of heat per unit time in increment i that

goes into the liquid through condensation of steam, and $h_{fg,k}$ is the latent heat at point k. The steam production term, $\Delta Q_{b,i}$, is calculated by the equation

$$\Delta Q_{b,i} = \frac{q_i'' \rho_{g,k} h_{fg,k} P_H \Delta Z}{\rho_{g,k} h_{fg,k} + C_{p,i} \rho_{l,i} \theta_i}, \quad (3-18)$$

where

$\rho_{g,k}$ = vapor density at point k

$C_{p,i}$ = average liquid specific heat in increment i

$\rho_{l,i}$ = average liquid density in increment i

θ_i = average liquid subcooling in increment i.

The liquid subcooling is given by

$$\theta_i = T_{s,k} - \frac{T_k + T_{k+1}}{2}, \quad (3-19)$$

in which $T_{s,k}$ is the saturation temperature at point k.

In Equation (3-18), two modes of heat transfer at the heated surface are considered: nucleate boiling and heating of the subcooled liquid which replaces the detached steam bubbles. Fully developed nucleate boiling is assumed to be present in the two-phase region; thus, heat transfer by single phase convection is not considered. The condensation term, $\Delta Q_{c,i}$, in Equation (3-17) was modified by using an empirical correlation given by Levinpthal⁽⁸⁾ of the form

$$q''_{\text{Cond}} P_H = H_o \frac{h_{fg}}{V_{fg}} A_f \alpha \theta, \quad (3-20)$$

where q''_{Cond} is the steam condensation heat flux, H_o is an empirical constant with units of $1/F^\circ$, and V_{fg} is the difference between the liquid and vapor specific volumes. By making use of the following definition:

$$\Delta Q_c = q''_{\text{Cond}} P_H \Delta Z. \quad (3-21)$$

Equation (3-20) is transformed into

$$\Delta Q_{c,i} = H_o \frac{h_{fg,k}}{V_{fg,k}} A_f \alpha_i \theta_i \Delta Z, \quad (3-22)$$

where α_i is the average void fraction in increment i . This is the expression used in Equation (3-17) for $\Delta Q_{c,i}$. It should be noted that if the average liquid temperature reaches the saturation temperature, $\Delta Q_{c,i}$ is equal to zero.

The change in the bulk liquid temperature in increment i due to both heating of the subcooled liquid at the heated surface and condensation of steam is

$$T_{k+1} = T_k + \frac{q''_i P_H \Delta Z + \Delta Q_{c,i} - \Delta Q_{b,i}}{W C_{p,i} (1-X_i)}, \quad (3-23)$$

in which X_i is the average quality in increment i .

The void fraction at point $k+1$ is calculated from the quality at $k+1$ by using the Zuber-Findlay drift-flux model. The following equations are from Lahey.⁽⁸⁾ The void fraction is given by

$$\alpha_{k+1} = \frac{X_{k+1}}{C_{O,k} \left[X_{k+1} + \frac{\rho_{g,k+1}}{\rho_{l,k+1}} (1-X_{k+1}) \right] + \frac{\rho_{g,k+1} A_f V_{gj,k+1}}{W}}, \quad (3-24)$$

where $C_{O,k+1}$ is the void concentration coefficient and $V_{gj,k+1}$ is the drift velocity. The concentration coefficient is calculated using the correlation of Dix, as follows:

$$C_O = \beta \left[1 + \left(\frac{1}{\beta} - 1 \right)^b \right], \quad (3-25)$$

where β is the steam volumetric flow fraction given by

$$\beta = \frac{X}{X + \frac{\rho_g}{\rho_l} (1-X)} \quad (3-26)$$

and b is given by

$$b = \left(\frac{\rho_g}{\rho_l} \right)^{0.1}. \quad (3-27)$$

The drift velocity is calculated from

$$V_{gj} = 2.9 \left[\frac{(\rho_l - \rho_g) \sigma g g_c}{\rho_l^2} \right]^{0.25}, \quad (3-28)$$

in which σ is the surface tension. In Equations (3-25) through (3-28) the steam quality and fluid properties are evaluated at point $k+1$, to give $C_{o,k+1}$ and $V_{gj,k+1}$.

The two-phase flow region is assumed to begin at the axial point where steam bubbles produced on the heated surface by nucleate boiling begin to detach and join the bulk liquid flow. This is the point where the steam bubbles begin to add to the buoyancy of the coolant. The departure point was determined experimentally by Saba and Zuber to be the point where the subcooling equalled θ_d , where θ_d is given by

$$\theta_d = \frac{q'' D_H}{455 k_l}, \quad (3-29)$$

in which k_l is the liquid thermal conductivity.⁽⁹⁾ This correlation applies to heat transfer controlled bubble departure, which is characteristic of the low flow rate in the NRAD core. The two-phase flow equations are used in any increment i if nucleate boiling is present and the subcooling at point $k+1$ is less than θ_d evaluated at $k+1$. The test for nucleate boiling is described in a later section.

In any increment where the subcooling is greater than θ_d and two-phase flow was present in the previous increment, the steam is assumed to condense completely within the increment, with the change in fluid density and wall friction averaged over the length of the increment. Additionally, if the liquid phase is subcooled at the end of the heated length, the steam is assumed to condense completely after the calculations in the heated length are completed. Conversely, if the liquid is saturated at the end of the heated length, the quality and void fraction at that point are assumed to remain constant through the rest of the channel length. The increase in liquid temperature due to complete condensation of steam of quality X at point $k+1$ is calculated by using Equations (3-17) and (3-23), and assuming that q_i'' , $\Delta Q_{C,i}$, and X_{k+1} are equal to zero and that X_k equals X . These assumptions result in the following equation:

$$T = T_{k+1} + \frac{h_{fg,k+1}}{C_{p,k+1}} X. \quad (3-30)$$

In Equation (3-30), T is the liquid temperature after the steam condenses and X is the quality of the end of the previous increment. If this condensation occurs in an increment in the heated length, T becomes T_k , and the

temperature rise due to heating is added to this temperature rise.

3.4. Two-Phase Pressure Drop

In two-phase flow, the elevation pressure drop is calculated by using a two-phase density in place of the density in Equations (3-6) and (3-7). The two-phase density, $\bar{\rho}$, is defined as⁽⁸⁾

$$\bar{\rho} = (1-\alpha)\rho_l + \alpha\rho_g. \quad (3-31)$$

The friction pressure drop in two-phase flow is calculated by using a two-phase friction factor, f_{TP} , in place of the friction factor in the single-phase friction pressure drop equation, Equation (3-9). In this case, Equation (3-9) is used with the assumption that the total flow is single-phase; in other words, the only change to Equation (3-9) for two-phase flow is the friction factor. The two-phase friction factor is the maximum of two different friction factors. The first is the subcooled boiling friction factor, f_{SCB} , given by⁽¹⁰⁾

$$f_{SCB} = \frac{8 q'' A_f}{W C_p (T_w - T)}. \quad (3-32)$$

As in the case of single-phase flow, this friction factor is averaged over the length of each increment. Since

Equation (3-32) is for subcooled boiling, it is only used when boiling is present. The second friction factor is a more conventional two-phase friction factor based on a two-phase friction multiplier, ϕ_{fO}^2 . With this second method, the two-phase friction factor is

$$f_{TP} = f \phi_{fO}^2, \quad (3-33)$$

where f is the single-phase friction factor from Equation (3-10). The two-phase multiplier used is the Baroczy multiplier, which includes the effects of steam quality, mass flux, and pressure.⁽¹¹⁾ This multiplier is composed of two parts: the friction multiplier for a mass flux, G , of $10^6 \text{ lb}_m/\text{ft}^2\text{-hr}$ and a correction factor, Ω , for other mass fluxes, where mass flux is defined as

$$G = W/A_f. \quad (3-34)$$

Thus the two-phase multiplier is given by

$$\phi_{fO}^2 = \phi_{fO}^{\prime 2} \Omega, \quad (3-35)$$

where $\phi_{fO}^{\prime 2}$ is the multiplier at a mass flux of $10^6 \text{ lb}_m/\text{ft}^2\text{-hr}$. Tables (3-2) and (3-3) show the $\phi_{fO}^{\prime 2}$ and Ω values used in this analysis; these values are for a pressure of 14.7 psia.

Table 3-2

Two-Phase Friction Multiplier for $G = 10^6 \text{ lb}_m/\text{hr-ft}^2$

Quality (%)	ϕ_{fo}^2
0	1.0
0.1	2.1
1	8.5
2	14
5	32.5
10	63.5
15	100
20	140
30	220
40	300
60	455
80	650
100	890

Note: Linear interpolation is used between values in table.

Table 3-3

Two-Phase Friction Multiplier Correction Factor

Quality (%)	Ω	
	$G = 2.5 \times 10^5 \text{ lb}_m/\text{hr-ft}^2$	$G = 5 \times 10^5 \text{ lb}_m/\text{hr-ft}^2$
0	1.0	1.0
0.1	1.38	1.2
1	1.38	1.285
5	1.27	1.2
10	1.48	1.285
20	1.48	1.27
40	1.345	1.2
60	1.233	1.15
80	1.125	1.085
100	1.0	1.0

Note: Linear interpolation is used between values in table.

The acceleration pressure drop in two-phase flow is⁽¹²⁾

$$\Delta p_{a,i} = \frac{W^2}{g_c A_f^2} [V'_{k+1} - V'_k]. \quad (3-36)$$

Here V' is given by

$$V' = \left[\frac{(1-X)^2}{1-\alpha} + \frac{\rho_l}{\rho_g} \frac{X^2}{\alpha} \right] \frac{1}{\rho_l}. \quad (3-37)$$

For the case where X_{k+1} is zero, V'_{k+1} becomes $1/\rho_{l,k+1}$.

If two-phase flow is present in the outlet volumes, the local pressure drop for single-phase flow, Equation (3-8), is multiplied by another two-phase multiplier, ϕ , to account for two-phase friction effects in the area change. The multiplier is given by⁽¹³⁾

$$\phi = \left[\frac{(1-X)^2}{1-\alpha} + \frac{\rho_l}{\rho_g} \frac{X^2}{\alpha} \right]. \quad (3-38)$$

In this expression, the quality and void fraction are the values at the area change. As with the two-phase friction pressure drop, Equation (3-8) is used with the assumption that the total flow is single-phase.

3.5. Power Distribution and Heat Transfer

In this analysis, the heat flux at the surface of a fuel element is assumed to be a function of axial

position only. For simplicity, axial conduction is not considered. The local heat flux, q'' , at any axial location, Z , is calculated by multiplying the average surface heat flux of the element, q''_{avg} , by the relative heat flux, RHF, defined as

$$\text{RHF} = q''/q''_{avg}. \quad (3-39)$$

Since the heated perimeter of a single fuel element is equal to the heated perimeter of the channel, the average heat flux is

$$q''_{avg} = \frac{Q_{\text{element}}}{\pi D L_H} = \frac{Q_{\text{element}}}{P_H L_H}, \quad (3-40)$$

where Q_{element} is the total thermal power in the element and D is the element diameter. The relative heat flux is calculated from the axial peaking factor, F_Z^Q , which is defined as

$$F_Z^Q = \frac{q''_{\text{max}}}{q''_{\text{avg}}}, \quad (3-41)$$

where q''_{max} is the maximum heat flux on the fuel element surface. Since axial conduction is not modeled, the local surface heat flux is assumed to be proportional to the local power density averaged over the element cross-section; thus, axial peaking factors defined in

terms of surface heat flux and power are equivalent. The heat flux at location Z , where Z is the axial distance from the beginning of the heated length, is given by

$$q'' = F_Z^Q q''_{\text{avg}} \cos [\pi(1+A)(Z-L_H/2)/L_H]. \quad (3-42)$$

This equation is adapted from an equation by Tong and Weisman⁽⁵⁾ for a chopped-cosine axial heat flux profile. The chopped-cosine profile is an approximation to the actual NRAD axial profile, which is shown in Figure (3-1). In Equation (3-42), A is a factor calculated from the peaking factor by solving the following equation:

$$F_Z^Q = \frac{\pi(1+A)}{2 \sin[\pi/2(1+A)]} \quad (3-43)$$

for A ; this equation is limited to the range $1 < F_Z^Q \leq 1.57$. Equation (3-42) is derived in Appendix B.

The preceding heat flux analysis was developed for a single fuel element. Since the heated perimeter of the flow channel is the same as the heated perimeter of a fuel element, the channel receives approximately one fourth of the power of each of the surrounding four elements; therefore, the channel heat flux is actually the average of the heat fluxes of each fuel element. The local channel heat flux is calculated by using the

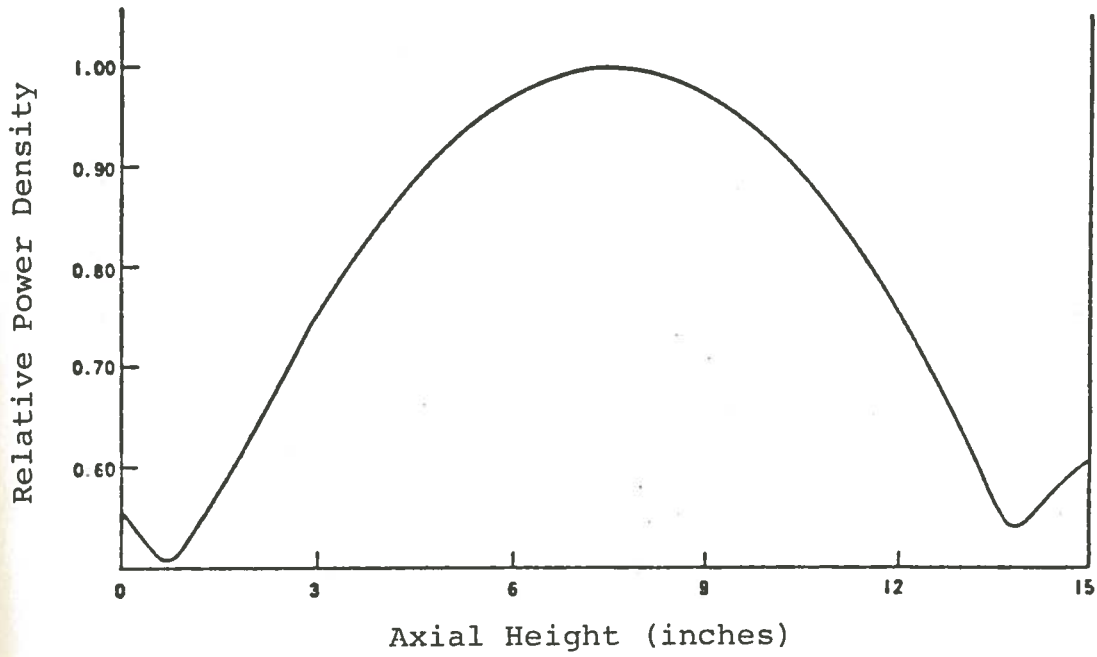


Figure 3-1
NRAD Axial Power Profile

average of the powers of the surrounding fuel elements in place of the single element power, Q_{element} , in Equation (3-40). This average element power is also the channel power, Q_{channel} . Thus, the channel heat flux can be calculated either from an element power, Q_{element} , or from the channel power.

Three heat transfer modes at the heated surface are considered: combined forced laminar convection and natural convection, forced turbulent convection, and nucleate boiling. Nucleate boiling is assumed to be the heat transfer mechanism if the fuel element surface temperature, T_w , calculated from the nucleate boiling equation is less than the surface temperature calculated from the appropriate convection equation. This is an approximation to the onset of nucleate boiling region of the boiling curve.

The two convection equations are as follows: for low flow rates (Re less than 2500) the combined convection equation, (4)

$$\frac{H_{SP} D_H}{k_{l,f}} = 0.17 Re_b^{0.33} Pr_b^{0.43} \left(\frac{Pr_b}{Pr_w}\right)^{0.25} Gr_f^{0.1} \quad (3-44)$$

is used; for high flow rates (Re greater than 2500) the turbulent forced convection equation, (5)

$$\frac{H_{SP} D_H}{k_{\ell,b}} = 0.023 Re_b^{0.8} Pr_b^{0.4} \left(\frac{\mu_b}{\mu_w}\right)^{0.14} \quad (3-45)$$

is used. In these equations the variables are

H_{SP} = heat transfer coefficient

Re = Reynold's number

Pr = Prandtl number

Gr = Grashof number,

where

$$Re = \frac{W D_H}{A_f \mu} \quad (3-46)$$

$$Pr = \frac{\mu C_P}{k_{\ell}} \quad (3-47)$$

and
$$Gr = g\beta\rho^2 (T_w - T) D_H^3 / \mu^2, \quad (3-48)$$

with β = coefficient of thermal expansion.

In Equations (3-44) and (3-45), the subscripts b, f, and w refer to properties evaluated at bulk, film, and wall temperatures, where the film temperature is defined as

$$T_f = \frac{T_w + T}{2} . \quad (3-49)$$

for

The wall temperature in convection is calculated from the heat transfer coefficient and the heat flux by

The

$$T_w = T + \frac{q''}{H_{SP}} . \quad (3-50)$$

These convection equations are iterated on the wall temperature until a solution is reached.

In the case of nucleate boiling, the wall temperature is given by⁽¹⁴⁾

$$T_w = T_s + \left(\frac{q''}{0.074}\right)^{0.259}, \quad (3-51)$$

where T_s is the saturation temperature.

The critical heat flux, q''_{crit} , is the heat flux which causes an undesirable increase in wall temperature due to a change in the boiling regime. This temperature excursion can sometimes be great enough to cause failure of the fuel element cladding; hence, critical heat flux is to be avoided in normal operation. The critical heat flux is calculated from the following equations:⁽¹⁵⁾

$$q''_{\text{crit}} = 270 D_H^{-0.2} \left(\frac{L_H}{D_H}\right)^{-0.85} G^{0.85} \quad (3-52)$$

for G less than $70,155 \text{ lb}_m/\text{hr-ft}^2$ and

$$q''_{\text{crit}} = 1400 D_H^{-0.2} \left(\frac{L_H}{D_H}\right)^{-0.15} G^{0.5} \quad (3-53)$$

for G greater than $70,155 \text{ lb}_m/\text{hr-ft}^2$. These equations give the minimum critical heat flux in the channel, based on a uniform heat flux throughout the channel. The margin with respect to the critical heat flux is

given by the departure from nucleate boiling ratio, DNBR. This is defined as the ratio of the critical heat flux to the actual heat flux. Since Equations (3-52) and (3-53) are for uniformly heated channels and the NRAD channel has a non-uniform axial heat flux profile, the following approximation for the DNBR in non-uniformly heated channels is used⁽¹³⁾

$$\text{DNBR} = 0.85 q''_{\text{crit}}/q''_{\text{avg}} \quad (3-54)$$

Chapter 4

PROGRAM DESCRIPTION

The equations in Chapter 3 were coded into a digital computer program called NAFQ. This program calculates the channel flow rate for any channel, given the channel dimensions, the power to be removed by the channel, the inlet temperature, and the static pressure drop in the tank. To do this, the program iterates on the channel mass flow rate until Equation (3-1) is satisfied, within an allowable error. As given earlier,

$$\Delta P_{\text{tot}} = \rho_{\text{avg}} \frac{g}{g_c} H_{\text{core}} \quad (3-1)$$

The static pressure drop given by the right hand side of Equation (3-1) is input in the form of inlet and outlet pressures, which form the pressure boundary condition for the flow rate solution.

To begin the flow rate calculation, an estimate of the correct flow rate is input; the program then determines whether to step up or down in flow rate, and then begins the flow rate iteration, calculating a total channel pressure drop for each iteration. When the pressure boundary condition is satisfied, the results are printed and the program stops. For the channel

considered, the results printed are the flow rate, the departure from nucleate boiling ratio, the minimum subcooling, and for each axial increment, the bulk liquid, saturation, and wall temperatures, the heat flux, the steam quality, and the void fraction at the increment boundaries. The overall logic of the program is shown by the flow chart in Figure (4-1), where Δp_0 is the static pressure drop in the tank.

The major input to the program is shown in Table (4-1). The following points about several of these input values must be made:

1) The fluid properties are input in tables of the property as a function of temperature, and linear interpolation is used between table values.

2) If the radial peaking factor, F_{XY}^Q , is input, the element power (Q_{element}) for the hottest channel is calculated from

$$Q_{\text{element}} = \frac{F_{XY}^Q \times \text{Reactor Power}}{\text{Number of Fuel Elements in Core}} \quad (4-1)$$

Alternatively, the channel power (Q_{channel}) can be input directly. The constant used to convert power in megawatts (MW) to power in BTU/hr is 3.4138×10^6 BTU/hr-MW. (16)

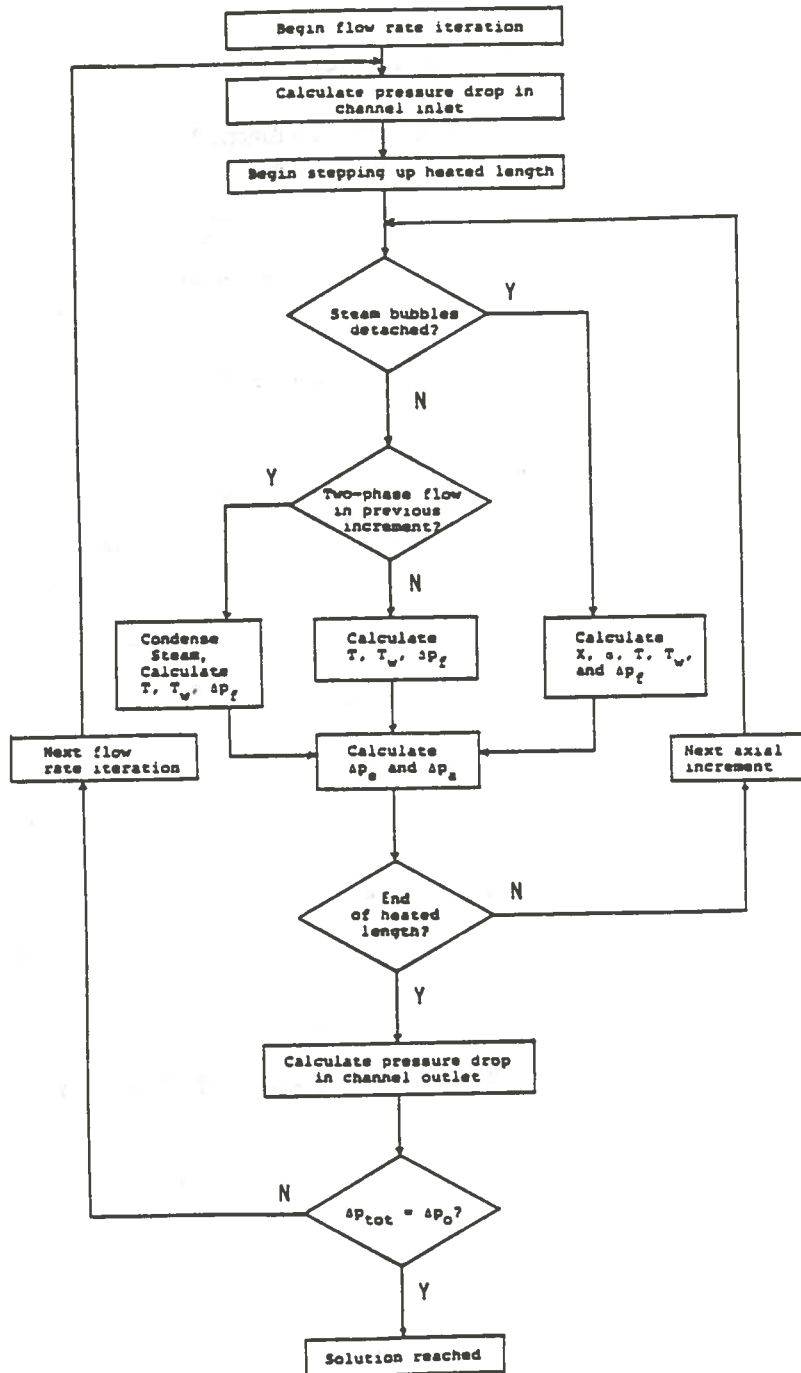


Figure 4-1

Overall Program Logic

Table 4-1
Summary of NAFQ Input

Flow Volume Geometry:

Flow Area
Flow Lengths
Hydraulic Diameter
Heated Perimeter

Fluid Properties:

Liquid Density
Vapor Density
Liquid Specific Heat
Liquid Viscosity
Liquid Thermal Conductivity
Liquid Thermal Expansion Coefficient
Surface Tension
Latent Heat
Saturation Pressure

Reactor Power

Number of Fuel Elements in Core

Power Factors:

Axial Peaking Factor
Channel Power or Radial Peaking Factor

Inlet and Outlet Pressures

Inlet Temperature

Pressure Loss Coefficients

Steam Condensation Coefficient (H_0)

Estimate of Steam Quality at the Void Departure Point

Flow Rate Estimate

Allowable Errors:

Steam Quality
Wall Temperature
Flow Rate

3) The value of the steam condensation coefficient was determined from the experimental data of Jordan and Leppert.⁽¹⁷⁾ A description of the method used to determine this coefficient is given in Appendix C.

4) An estimate of the steam quality at the void departure point is necessary to start the void fraction calculation. This quality is X_k in Equation (3-17) for the first increment in two-phase flow. The value of this input variable is chosen to make the void fraction profile smooth at the beginning.

5) The allowable errors are used as convergence tests for the void fraction, wall temperature, and flow rate iterations.

Since significant uncertainties exist in some of these input values, the sensitivity of program results to certain input errors is investigated in the next chapter, where the results of this thesis are presented.

Chapter 5

RESULTS

This chapter presents the thermal-hydraulic results from the NAFQ program for the 1.0 MW NRAD design core, along with comparisons of program results to previous analyses and program sensitivity results. All the cases analyzed assume an axial peaking factor of 1.26, which was determined from core neutronics calculations.⁽²⁾ In all the cases considered, the heat transfer mode from fuel element surface to coolant was fully developed nucleate boiling; thus the convergence error for wall temperature is zero. It should be noted that all of the results presented here represent conditions in the hottest channel.

5.1. Comparison to Previous Analyses

In this section, the accuracy of the NAFQ program is investigated by comparing NAFQ results to the results of previous analyses, which are reported in the NRAD final safety analysis report (FSAR).⁽²⁾ Two comparison cases were performed: a 0.3 MW NRAD core calculation and a 2.0 MW PRNC core calculation. These calculations were also performed in the FSAR. For the comparisons, the input to the NAFQ program was chosen to match the

FSAR conditions as closely as reasonably possible. Since in many cases the assumptions and methods used in the FSAR are not reported, these comparisons are only approximate.

5.1.1. Comparison for 0.3 MW NRAD Case. The input to the NAFQ program for the 0.3 MW NRAD case is shown in Table (5-1). The inlet and outlet pressures were calculated for a tank temperature of 98.6°F, which was measured in the existing NRAD tank at full power.⁽¹⁸⁾ The inlet temperature, radial peaking factor, and number of fuel elements are from Reference 2. The inlet temperature is the water temperature at the inlet to the core flow channel. To make the maximum heat flux calculated by the NAFQ program equal to the FSAR value, the reactor power used was adjusted to 0.312 MW. Since the available computer time was limited, the number of axial increments was chosen as 35, which gives the minimum computer time without sacrificing accuracy. The allowable errors were also chosen to minimize computer time; the quality error, in particular, is relatively large because the void fraction subroutine tends to be slow to converge.

The NAFQ results for this case are compared in Table (5-2) to the calculations from the FSAR. Although the flow rates are much different, the calculation method

Table 5-1
Input for 0.3 MW NRAD Case

Inlet Pressure (psia)	16.302
Outlet Pressure (psia)	15.086
Inlet Temperature (°F)	90
Reactor Power (MW)	0.312
Radial Peaking Factor	1.59
Axial Peaking Factor	1.26
Number of Fuel Elements	60
Number of Axial Increments	35
Maximum Flow Rate Convergency Error (lb _m /hr)	0.01
Maximum Steam Quality Convergency Error (%)	1.0

Table 5-2
Comparison of NAFQ Results to FSAR
for 0.3 MW NRAD Case

	NAFQ	FSAR
Mass Flow Rate (lbm/hr)	397	518
Maximum Heat Flux (BTU/hr-ft ²)	7.69 x 10 ⁵	7.67 x 10 ⁵
Minimum Subcooling (°F)	52.1	55.8
Maximum Void Fraction (%)	0	<0.5
Minimum DNBR	6.32	5.4

used in the FSAR is not described, and, therefore, a large uncertainty in this comparison exists. The core temperature rise, as reflected in the minimum subcooling, is nearly the same in both cases, in spite of the large difference in flow rates. This discrepancy appears to exist because the temperature rise was calculated in the FSAR by applying the maximum heat flux to the whole channel length. The NAFQ program predicts single-phase flow throughout the channel; therefore no bubble departure was calculated. Since the void fraction calculated in the NAFQ program is a detached void fraction, a significant amount of attached bubbles could be present on the fuel element surfaces. The void fraction calculated in the FSAR is actually a bounding value; thus, the void fraction agreement between the two calculations is good. The last comparison is for the departure from nucleate boiling ratio (DNBR). Both calculations use the same critical heat flux equation; the reason for the difference in DNBR might be attributed to the way the equations are applied. The critical heat flux given in the FSAR appears to be very conservative.

Although the agreement between NAFQ and FSAR results is not satisfactory for all parameters, the FSAR results seem to be unrealistic and inconsistent. Therefore, a close comparison of these results is not warranted.

5.1.2. Comparison for 2.0 MW PRNC Core. The input to the NAFQ program for the 2.0 MW PRNC case is shown in Table (5-3). The inlet and outlet pressures were calculated for a tank temperature of 110°F.⁽¹⁰⁾ The inlet temperature and number of fuel elements are taken from Reference 2. The radial peaking factor was calculated to give a maximum heat flux equal to that given in the FSAR. The remaining input is the same as in the previous case.

The NAFQ results for this case are compared in Table (5-4) to the calculations from the FSAR. The agreement for this case is better than in the previous case. The FSAR results for this case were calculated by a computer program written by General Atomic. The NAFQ program uses the same general methodology as the General Atomic program; therefore, the differences between the NAFQ and FSAR results for this case are primarily due to differences in the correlations used in each program and in the input. A particular note must be made of the void fraction calculations. The NAFQ void fraction calculation predicts a void fraction which increases with axial height to the end of the heated length; this will be shown in the next section. The General Atomic program, on the other hand, by relying completely on the data of Jordan and Leppert⁽¹⁷⁾ tends to predict a decreasing void fraction in the topmost

Table 5-3
Input for 2.0 MW PRNC Case

Inlet Pressure (psia)	25.157
Outlet Pressure (psia)	23.945
Inlet Temperature (°F)	90
Reactor Power (MW)	2.0
Radial Peaking Factor	1.54
Axial Peaking Factor	1.26
Number of Fuel Elements	95
Number of Axial Increments	35
Maximum Flow Rate Convergence Error (lb _m /hr)	0.01
Maximum Steam Quality Convergency Error (%)	1.0

Table 5-4
Comparison of NAFQ Results to FSAR
for 2.0 MW PRNC Case

	NAFQ	FSAR
Mass Flow Rate (lb_m/hr)	797	769
Maximum Heat Flux ($\text{BTU}/\text{hr}\text{-ft}^2$)	3.02×10^5	3.02×10^5
Minimum Subcooling ($^\circ\text{F}$)	8.78	5.04
Maximum Void Fraction (%)	10.5	6
Minimum DNBR	2.28	1.37

part of the heated length, where the void fraction is usually largest. Because of this difference, the maximum void fraction calculated by the NAFQ program is inherently larger than that given in the FSAR, even if the channel average void fractions are the same. In light of this fact, the agreement between the void fraction values in Table (5-4) is good.

Based on the two comparisons given in this section it is concluded that the NAFQ program agrees with the previous analyses to within the modeling, input, and roundoff uncertainties.

5.2. Results for the 1.0 MW NRAD Core

Two cases for the 1.0 MW NRAD core were analyzed: a case using the tank temperature and inlet temperature expected to occur with the current heat removal system design and a case which assumes an upgraded heat removal system. The results for these two cases are discussed in this section.

5.2.1. The 1.0 MW NRAD Design Case. The first case considered in this section is the 1.0 MW NRAD design case. This case is a prediction of the thermal-hydraulic conditions in the hottest channel of the NRAD core at a power of 1.0 MW with a tank water temperature of 140°F and an inlet temperature of 120°F. These two temperatures were calculated by the Argonne-West staff,

based on a reactor power of 1.0 MW and the heat removal capacity of the existing heat removal system.⁽¹⁸⁾ This is considered the 1.0 MW NRAD design case.

The NAFQ input for this case is shown in Table (5-5). As in the last section, an axial peaking factor of 1.26 was assumed. The fuel element powers were provided by Mr. Wade Richards of Argonne-West.⁽¹⁸⁾ These powers were computed by workers at Texas A&M University using a core neutronics computer program. Since the channel power was input, it was not necessary to input the number of fuel elements.

The calculated results for this case are given in Table (5-6). The minimum subcooling and maximum void fraction calculated for this case are approximately the same as these calculated by the NAFQ program for the 2.0 MW PRNC core (Table 5-4). This means that the present 1.0 MW design is likely to experience the same power oscillation problems as experienced with the PRNC reactor. The DNBR is reasonably high, and is larger than that in the PRNC, which never experienced critical heat flux problems.⁽²⁾ Therefore, this critical heat flux margin should be adequate. Finally, the maximum clad temperature of 259°F is far below the technical specification limit of 1652°F given in Chapter 1.

Table 5-5
Input For 1.0 MW NRAD Case

Inlet Pressure (psia)	16.260
Outlet Pressure (psia)	15.057
Inlet Temperature (°F)	120
Reactor Power (MW)	1.0
Channel Power (KW)	17.338*
Axial Peaking Factor	1.26
Number of Axial Increments	35
Maximum Flow Rate Convergence Error (lb _m /hr)	0.01
Maximum Steam Quality Convergency Error (%)	1.0

* Represents the average of the powers of the four fuel elements surrounding the hottest channel; these four power levels are: 16.15, 16.96, 17.11, and 19.13 KW/element.

Table 5-6
NAFQ Results for 1.0 MW NRAD Case

Mass Flow Rate (lb_m/hr)	684
Maximum Heat Flux ($\text{BTU}/\text{hr}\text{-ft}^2$)	1.61×10^5
Minimum Subcooling ($^\circ\text{F}$)	6.82
Maximum Void Fraction (%)	9.14
Minimum DNBR	3.95
Maximum Cladding Temperature ($^\circ\text{F}$)	259

The axial variation of the heat flux, bulk liquid temperature, wall temperature, and void fraction is shown in Figures (5-1) to (5-4). In these figures, the axial distance is the distance from the beginning of the heated length. The increase in void fraction with axial height can be seen in Figure (5-4).

The results for this case will be compared in Section 5.3 to the results for the PRNC reactor at a power of 1.2 MW.

5.2.2. The 1.0 MW NRAD Core with Upgraded Heat Removal. This case investigates the effects on the calculated results from Section 5.2.1 of an upgraded heat removal system for the NRAD reactor. The calculation presented here was performed by assuming that all the input parameters from the 1.0 MW NRAD design case were the same, except for the tank water temperature and the inlet temperature. For this case, the heat removal system was assumed to have been upgraded to a capacity capable of maintaining a 98.6°F tank temperature and a 90°F inlet temperature. These are the temperatures in the present 0.3 MW NRAD reactor at full power. The NAFQ input for this case is shown in Table (5-7) and the calculated results are shown in Table (5-8). As can be seen in Table (5-8), the maximum void fraction for this case is much less than in the 1.0 MW NRAD design case.

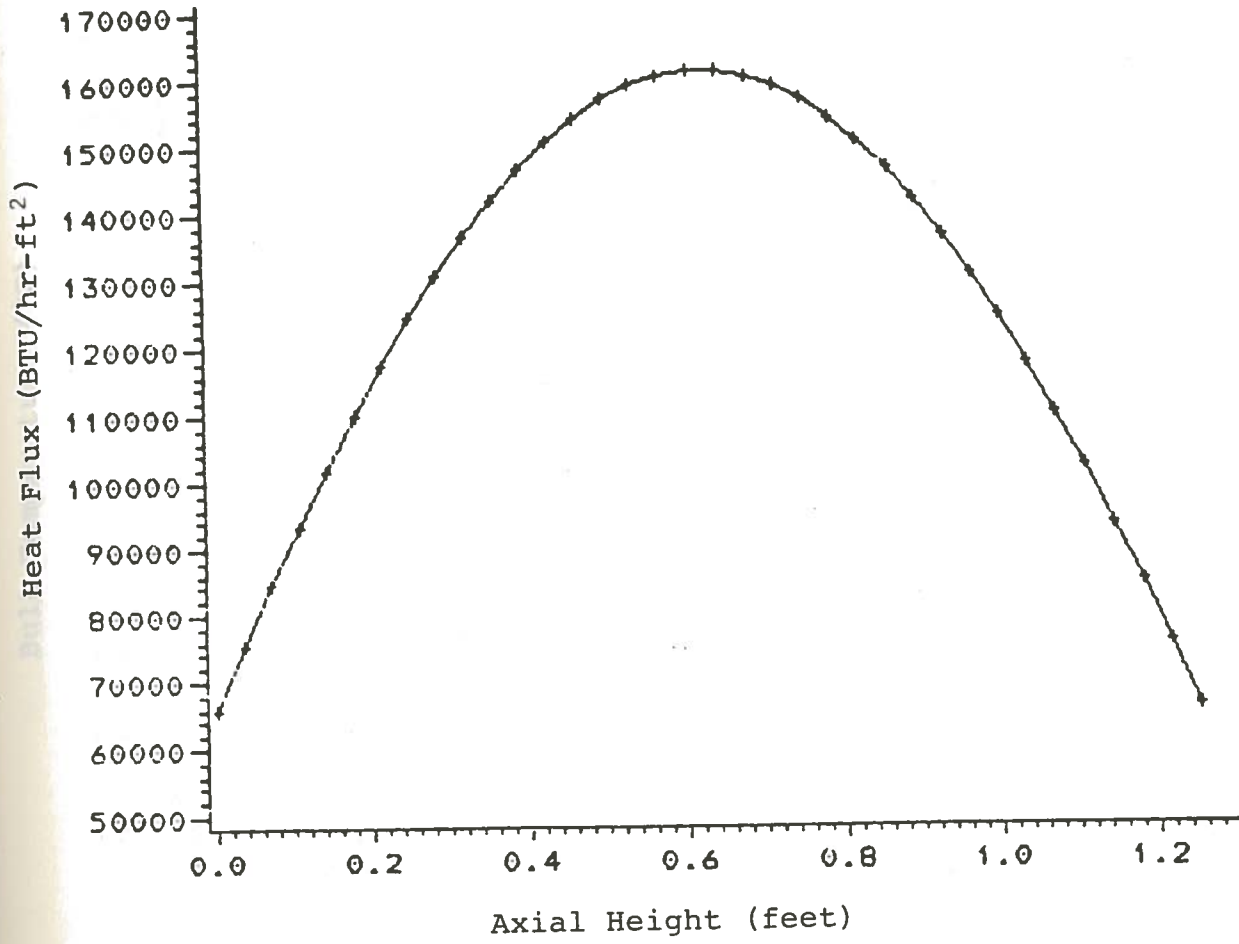


Figure 5-1
Heat Flux Profile

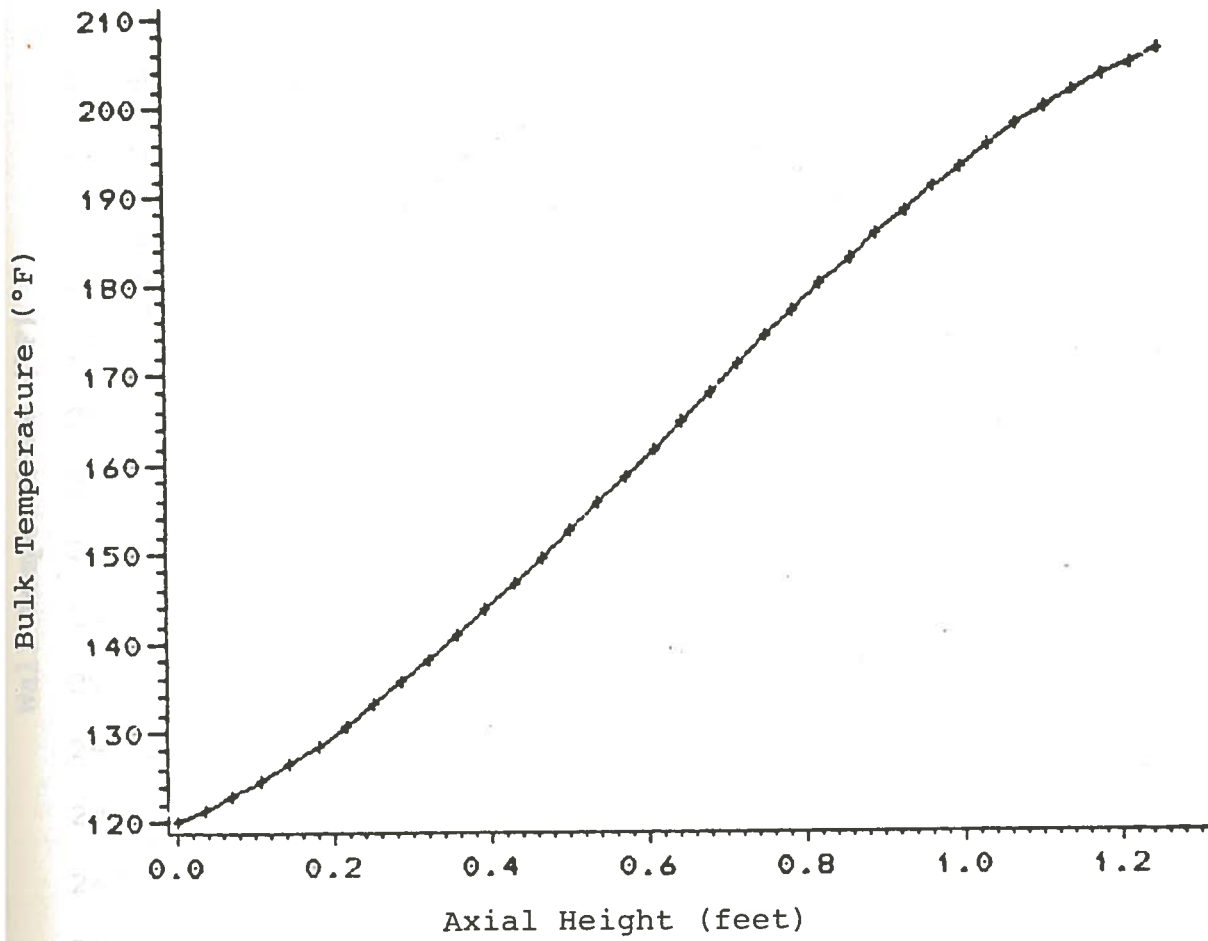


Figure 5-2
Bulk Temperature

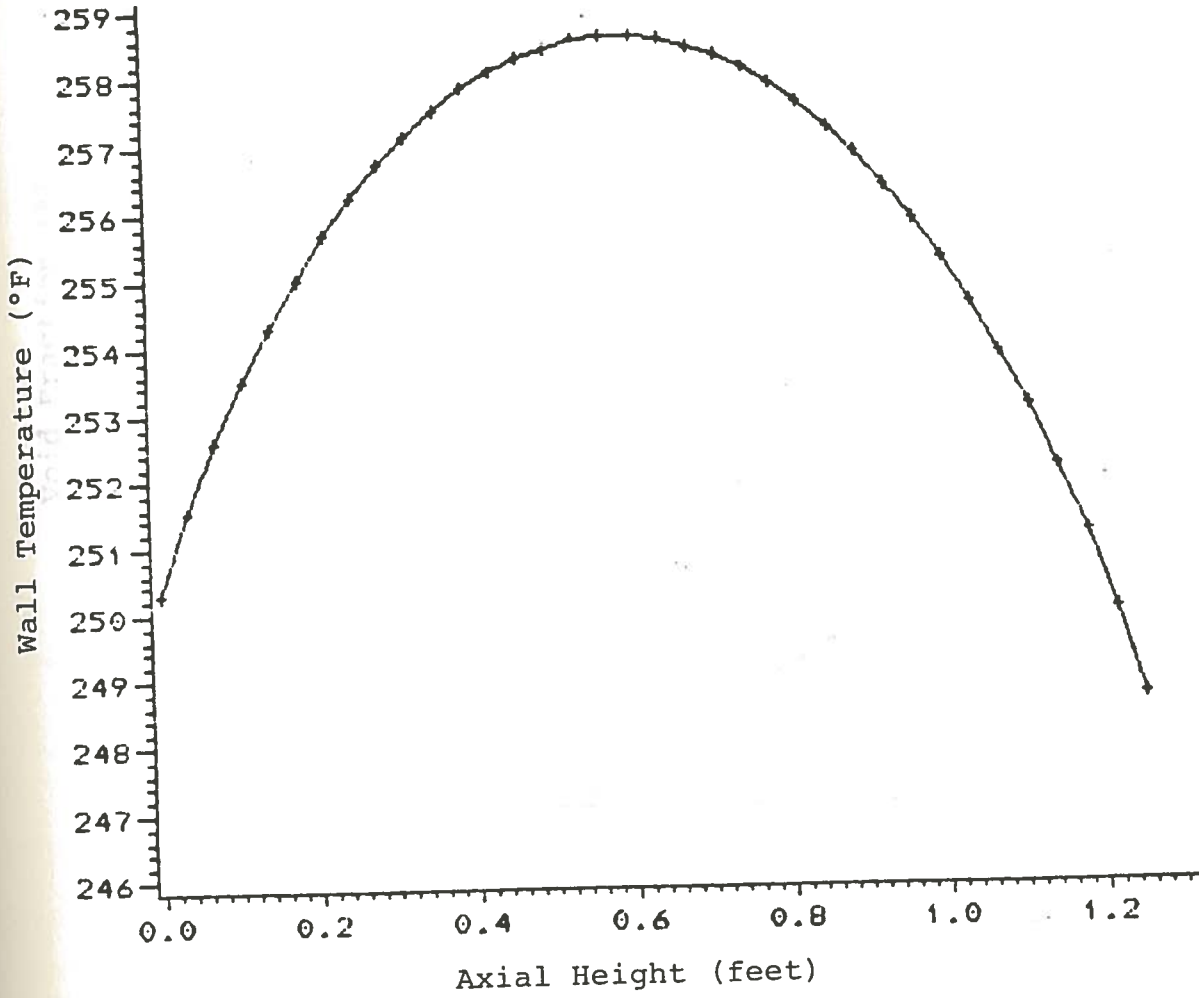


Figure 5-3
Wall Temperature Profile

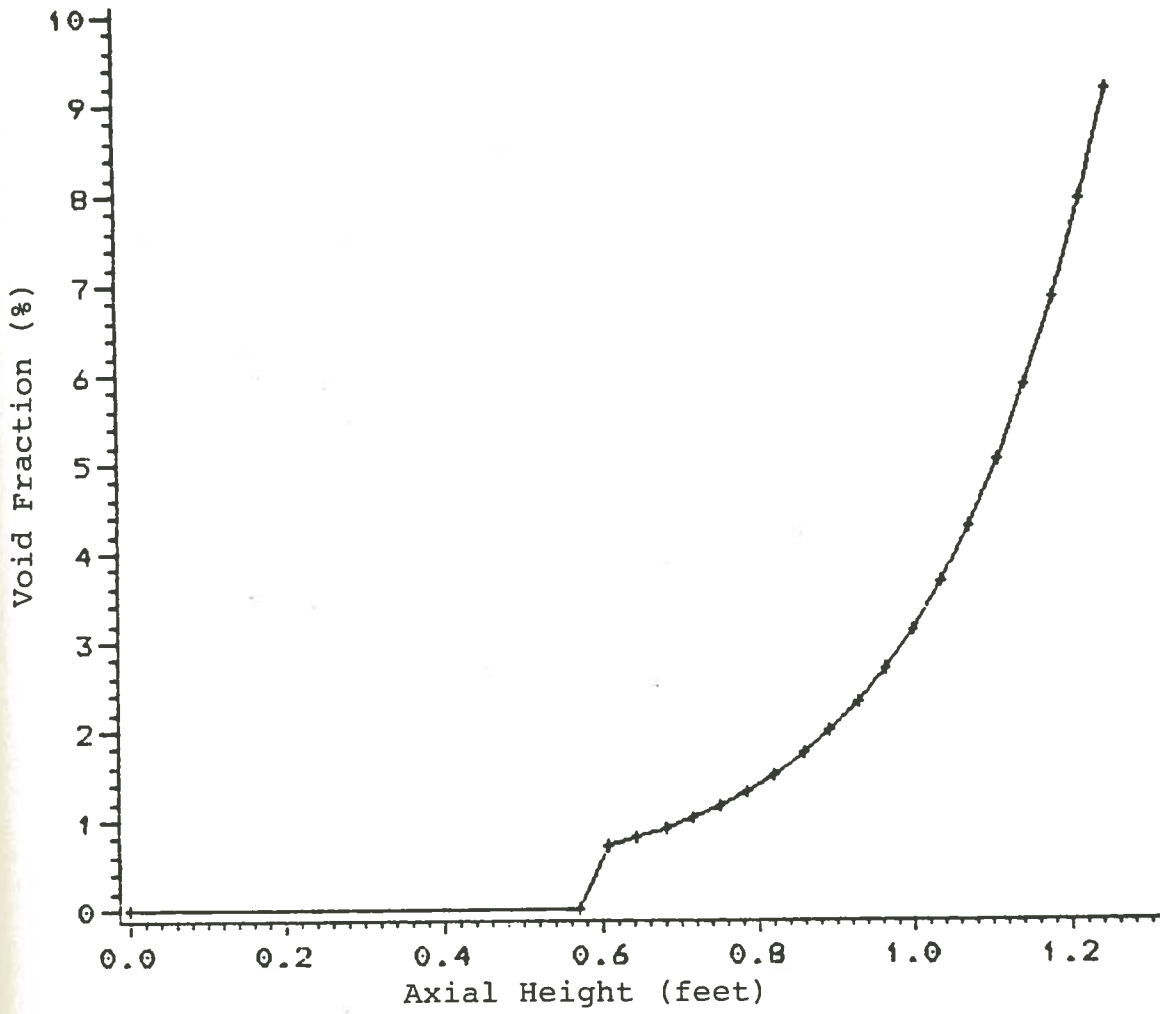


Figure 5-4
Void Fraction Profile

Table 5-7
Input for 1.0 MW Upgraded NRAD Case

Inlet Pressure (psia)	16.302
Outlet Pressure (psia)	15.086
Inlet Temperature (°F)	90
Reactor Power (MW)	1.0
Channel Power (KW)	17.338
Axial Peaking Factor	1.26
Number of Axial Increments	35
Maximum Flow Rate Convergency Error (lb_m/hr)	0.01
Maximum Steam Quality Convergency Error (%)	1.0

Table 5-8

NAFQ Results for 1.0 MW Upgraded NRAD Case

Mass Flow Rate (lb_m/hr)	574
Minimum Subcooling ($^{\circ}\text{F}$)	20.2
Maximum Void Fraction (%)	1.88
Minimum DNBR	3.62
Maximum Cladding Temperature ($^{\circ}\text{F}$)	259

These results will be compared in the next section to the results for the 1.2 MW PRNC case.

5.3. Comparison of 1.0 MW NRAD and 1.2 MW PRNC Results

The results from Section 5.2 for the 1.0 MW NRAD core are compared in this section to the results calculated by the NAFQ program for the PRNC core at a power of 1.2 MW, the power at which the PRNC reactor experienced power oscillations. The input parameters to the NAFQ program for the 1.2 MW PRNC calculation are given in Table (5-9). The only difference between this input and the input for the 2.0 MW PRNC case, given in Section 5.1.2, is the reactor power.

In Table (5-10), the NAFQ results for the 1.2 MW PRNC case are compared to the NAFQ results from Section 5.2.1 for the 1.0 MW NRAD design case. The maximum void fraction in the NRAD case is more than six times as large as in the PRNC case. This is strong evidence that the 1.0 MW NRAD reactor with the present heat removal system will suffer from power oscillations at a power well below the design power of 1.0 MW. The primary reasons for this much larger void fraction are the differences in inlet temperature, tank temperature, and saturation temperature between the NRAD and PRNC reactors. The effects on the NAFQ results of variations in these parameters will be shown in Section 5.4.

Table 5-9
Input For 1.2 MW PRNC Case

Inlet Pressure (psia)	25.157
Outlet Pressure (psia)	23.945
Inlet Temperature (°F)	90
Reactor Power (MW)	1.2
Radial Peaking Factor	1.54
Axial Peaking Factor	1.26
Number of Fuel Elements	95
Number of Axial Increments	35
Maximum Flow Rate Convergency Error (lb _m /hr)	0.01
Maximum Steam Quality Convergency Error (%)	1.0

Table 5-10

Comparison of 1.0 MW NRAD Case to 1.2 MW PRNC Case

	NRAD	PRNC
Reactor Power (MW)	1.0	1.2
Inlet Temperature (°F)	120	90
Mass Flow Rate (lb _m /hr)	684	541
Maximum Heat Flux (BTU/hr-ft ²)	1.61 x 10 ⁵	1.81 x 10 ⁵
Minimum Subcooling (°F)	6.82	24.7
Maximum Void Fraction (%)	9.14	1.46
Average Saturation Temperature (°F)	215	239
Minimum DNBR	3.95	3.12

The results for the 1.0 MW NRAD reactor with upgraded heat removal system are compared in Table (5-11) to the results for the 1.2 MW PRNC case. The upgraded heat removal system results in a maximum void fraction in the NRAD core about the same as that in the 1.2 MW PRNC core. Therefore, with the tank and inlet temperatures assumed for the upgraded heat removal system, the 1.0 MW NRAD core is predicted to operate at the threshold of power oscillations.

5.4. Sensitivity Results

The sensitivity of the NAFQ program to selected input errors is investigated in this section. The procedure used is to vary the input parameters one at a time and compare the perturbed results for each input variation with the results for an unperturbed case. The case used as a base for this sensitivity study is the 1.0 MW NRAD design case, presented in Section 5.2.1.

The input parameters changed and the calculated results for each variation are given in Table (5-12). These results are compared to the base, or unperturbed, case in Table (5-13). From the results in Table (5-13), it can be seen immediately that the number of axial nodes has very little effect on the calculated results, at least in this range of nodalization. The remaining input variations represent the maximum expected

Table 5-11
Comparison of 1.0 MW Upgraded NRAD
Case to 1.2 MW PRNC Case

	NRAD	PRNC
Reactor Power (MW)	1.0	1.2
Inlet Temperature (°F)	90	90
Mass Flow Rate (lb _m /hr)	574	541
Minimum Subcooling (°F)	20.2	24.7
Maximum Void Fraction (%)	1.88	1.46
Average Saturation Temperature (°F)	215	239
Minimum DNBR	3.62	3.12

Table 5-12

Sensitivity Results

Parameter Changed	Input Value	Flow Rate (lb _m /hr)	Minimum Subcooling (°F)	Maximum Void Fraction (%)	Minimum DNBR
Power	+20%	818	4.48	15.4	3.52
Loss	+10%				
Coefficients	for all	682	6.46	9.64	3.94
Condensation Coefficients	+25%	676	5.80	9.27	3.93
Pressure	- 1 psia	706	6.01	10.4	4.01
Axial Peaking Factor	-10%	685	6.89	10.3	3.95
Inlet Temperature	+10°F	766	6.00	10.8	4.18
Tank Temperature	+20°F	673	5.35	11.4	3.92
Axial Nodes	30	684	6.80	9.16	3.95
Axial Nodes	40	684	6.79	9.18	3.95
Axial Nodes	45	684	6.81	9.16	3.95

Table 5-13

Sensitivity Results in Percent Difference

Parameter Changed	Input Value	Flow Rate	Percent Differences*		
			Minimum Subcooling	Maximum Void Fraction	Minimum DNBR
Power	+20%	+20	-34	+68	-11
Loss	+10%				
Coefficients	for all	-0.3	-5	+5	-0.3
Condensation Coefficient	+25%	-1	-15	+1	-0.5
Pressure	-1 psia	+3	-12	+14	+2
Axial Peaking Factor	-10%	+0.1	+1	+13	0
Inlet Temperature	+10°F	+12	-12	+18	+6
Tank Temperature	+20°F	-2	-22	+25	-0.8
Axial Nodes	30	0	-0.4	+0.2	0
Axial Nodes	40	0	-0.4	+0.4	0
Axial Nodes	45	0	-0.1	+0.2	0

* These percent differences are with respect to the 1.0 MW NRAD design case.

uncertainties for each input parameter. It can be seen that each of these input variations caused the subcooling to decrease and the void fraction to increase. Thus, input errors of the same arithmetic sign as the input variations in Table (5-13) are conservative. These results are consistent with the expected behavior of the NAFQ program.

As mentioned in Section 5.3, changes in the inlet temperature, tank temperature, and saturation temperature strongly influence the calculated void fraction. In addition, changes in the reactor power have a strong effect on the void fraction. It should be remembered that a 20% increase in the reactor power means a greater increase in the hot channel power, because of the power peaking in the core.

Overall, the NAFQ program is not overly sensitive to input errors, with the exception that the maximum void fraction is very sensitive to power. None of the sensitivities presented here are strong enough to change the conclusions drawn in the previous sections.

CONCLUSIONS AND RECOMMENDATIONS

Based on the results presented in Chapter 5, it is concluded that the NAFQ program predicts approximately the thermal-hydraulic conditions in low power nuclear reactor cores in natural circulation at near atmospheric pressures. Since the program results agree reasonably well with the FSAR results, and FSAR calculations are always conservative, the NAFQ program can be used for safety analysis calculations.

With respect to the 1.0 MW NRAD design, it is concluded that the NRAD reactor as currently designed will not be able to operate at 1.0 MW without serious power oscillations caused by relatively large void fractions in the high power channels. The maximum cladding temperature and minimum DNBR limits will not be violated in the 1.0 MW core. It is recommended that the currently proposed configuration for the 1.0 MW core be changed. The NAFQ program can be used in designing a NRAD reactor system which will be free of power oscillations.

Finally, more program verification is recommended. Several studies are needed: first, comparison of NAFQ results to results from other core

thermal-hydraulic computer programs; second, measurement of the maximum flow rate, channel exit temperature, and void fraction in the existing 0.3 MW NRAD core and comparison of these measurements to the NAFQ results for the 0.3 MW NRAD case given in Section 5.1.1; and third, comparison of the NAFQ void fraction calculation to experimental data at the flow rates, pressures, and heat fluxes present in the NRAD core.

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Appendix A

NOMENCLATURE

A	Factor in Equation (3-42)
A	Channel cross-sectional area, ft^2
A_f	Flow area in fuel element channel, ft^2
A_1	Flow area upstream of area change, ft^2
A_2	Flow area downstream of area change, ft^2
A_j	Smaller of areas A_1 and A_2 , ft^2
b	Exponent in Equation (3-26)
C_p	Liquid specific heat, $\text{BTU}/\text{lb}_m\text{-}^\circ\text{F}$
C_o	Void concentration coefficient
D	Fuel element diameter, ft
ΔQ_b	Power into steam generation, BTU/hr
ΔQ_c	Power into steam condensation, BTU/hr
Δp	Pressure drop, lb_f/ft^2
Δz	Length of an axial increment, ft
D_H	Hydraulic diameter, ft
DNBR	Departure from nucleate boiling ratio
e_v	Irreversible pressure loss coefficient
f	Friction factor
F_{XY}^O	Radial peaking factor
F_Z^O	Axial peaking factor
g	Acceleration due to gravity, $4.17 \times 10^8 \text{ ft}/\text{hr}^2$
G	Mass flux, $\text{lb}_m/\text{ft}^2\text{-hr}$

g_c	Gravitational conversion factor, 4.17×10^8 $\frac{\text{lb}_m\text{-ft}}{\text{lb}_f\text{-hr}^2}$
Gr	Grashof number
H_{core}	Height of core flow channel, ft
h_{fg}	Latent heat, BTU/lb _m
H_{SP}	Heat transfer coefficient, BTU/ft ² -hr-°F
H_o	Condensation coefficient in Equation (3-22), 1/°F
k	Thermal conductivity, BTU/ft-hr-°F
N	Number of axial increments
p	Absolute pressure, lb _f /ft ²
P_H	Flow channel heated perimeter, ft
q"	Heat flux, BTU/ft ² -hr
Q_{channel}	Thermal power in flow channel, BTU/hr
Q_{element}	Thermal power in fuel element, BTU/hr
Re	Reynold's number
RHF	Relative heat flux
T	Bulk liquid temperature, °F
T_s	Saturation temperature, °F
T_w	Wall (cladding) temperature, °F
V	Volume, ft ³
V_{fg}	Difference between liquid and vapor specific volumes, ft ³ /lb _m
V_{gj}	Drift velocity, ft/hr
v'	Momentum specific volume, ft ³ /lb _m
W	Mass flow rate, lb _m /hr

X	Steam quality
Z	Axial distance from beginning of heated length, ft

Greek Letters

α	Void fraction
β	Steam volumetric flow fraction
β	Thermal expansion coefficient, $1/^\circ\text{F}$
μ	Liquid viscosity, $\text{lb}_m/\text{ft}\cdot\text{hr}$
Ω	Mass flux correction factor
ϕ_{fo}^2	Two-phase friction factor
$\phi_{fo}^{2'}$	Two-phase friction factor for $G = 10^6 \text{ lb}_m/\text{ft}^2\cdot\text{hr}$
ρ	Density, lb_m/ft^3
$\bar{\rho}$	Two-phase density, lb_m/ft^3
σ	Surface tension, lb_f/ft
θ	Liquid subcooling, $^\circ\text{F}$

Subscripts

a	Acceleration
avg	Average
b	Bulk
cond	Condensation
crit	Critical
d	Void departure point
e	Elevation
f	Friction
f	Film

g	Saturated vapor phase
i	Increment i
in	Inlet
iso	Isothermal
j	Area change number
k	Beginning of increment i
k+1	End of increment i
l	Local
l	Liquid
LH	Heated length
LR	Lower reflector
max	Maximum
out	Outlet
SCB	Subcooled boiling
tot	Total
TP	Two-phase
UR	Upper reflector

Appendix B

DERIVATION OF HEAT FLUX EQUATION

The heat flux equation given by Tong and Weisman is

$$q'' = q''_{\text{mas}} \cos \left(\frac{\pi Z'}{L_0} \right), \quad (\text{B-1})$$

in which Z' is the axial distance from the center of the fuel element and L_0 is the extrapolated length of a half-cycle of the cosine curve.⁽⁵⁾ This equation is transformed to a more useful form by making use of the ratio L'/L_0 , where L' is the difference between the actual heated length and the extrapolated length. These terms are shown in Figure (B-1).

Letting

$$A = \frac{L'}{L_0} \quad (\text{B-2})$$

gives

$$L' = A L_0. \quad (\text{B-3})$$

Since

$$L_H = L_0 + L', \quad (\text{B-4})$$

the variable L' from Equation (B-3) is substituted into

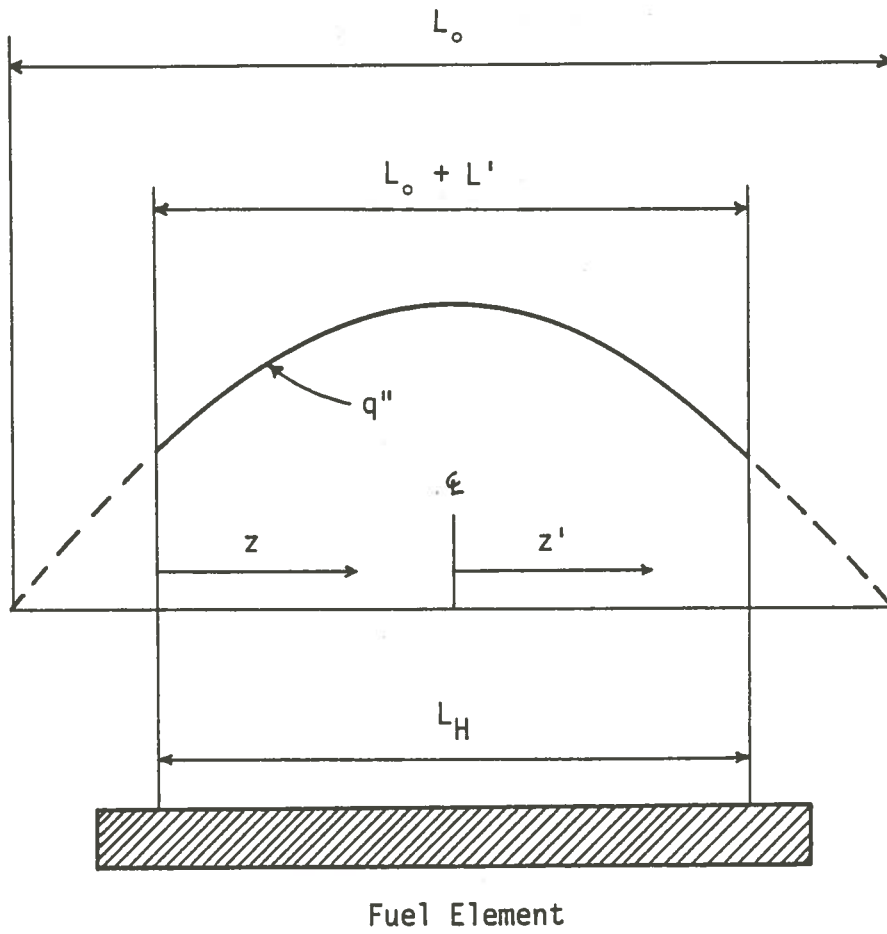


Figure B-1
Axial Heat Flux Geometry

Equation (B-4) to yield

$$L_H = L_O + AL_O, \quad (B-5)$$

or

$$L_O = \frac{L_H}{1+A}. \quad (B-6)$$

By definition:

$$F_Z^Q = \frac{q''_{\max}}{q''_{\text{avg}}}, \quad (B-7)$$

or equivalently:

$$q''_{\max} = F_Z^Q q''_{\text{avg}}. \quad (B-8)$$

Substituting Equations (B-6) and (B-8) into Equation (B-1) gives

$$q'' = F_Z^Q q''_{\text{avg}} \cos \left[\frac{\pi(1+A)Z'}{L_H} \right]. \quad (B-9)$$

The variable Z' is defined as

$$Z' = Z - \frac{L_H}{2}, \quad (B-10)$$

in which Z is the axial distance from the beginning of the heated length. By substituting Equation (B-10) into Equation (B-9), the equation given in Chapter 3 for

calculating the local heat flux results:

$$q'' = F_Z^Q q'' \cos \left[\frac{\pi (1+A) (Z-L_H/2)}{L_H} \right]. \quad (\text{B-11})$$

The ratio A is calculated from the equation: ⁽⁵⁾

$$F_Z^Q = \frac{\pi (1+A)}{2 \sin \left[\frac{\pi}{2} (1+A) \right]} \quad (\text{B-12})$$

by solving for A.

Appendix C

DETERMINATION OF CONDENSATION COEFFICIENT

The value of the condensation coefficient in the void fraction calculation, H_o in Equation (3-20), was determined from the experimental data of Jordan and Leppert.⁽¹⁷⁾ This data is generally not useful for practical void fraction calculations; instead, it is used to find an approximate value of the condensation coefficient. The data used is for a heat flux of 5×10^5 BTU/ft²-hr, a flow velocity of 4 ft/sec, a pressure of from 15 to 21 psia, and a bulk liquid temperature ranging from 86°F to 121°F. For these conditions, the void fraction is a nearly constant 0.69% over the heated length. The geometry and thermal-hydraulic conditions from this data were input to the NAFQ program and the condensation coefficient was varied until the maximum calculated void fraction was slightly less than that given by the Jordan and Leppert data. The value of the coefficient determined in this manner was 8700 1/°F, for a void fraction which varies from 0.32% to 0.67%. Since the condensation coefficient is over-estimated for this data, the void fraction calculated by the NAFQ program should be conservative.

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