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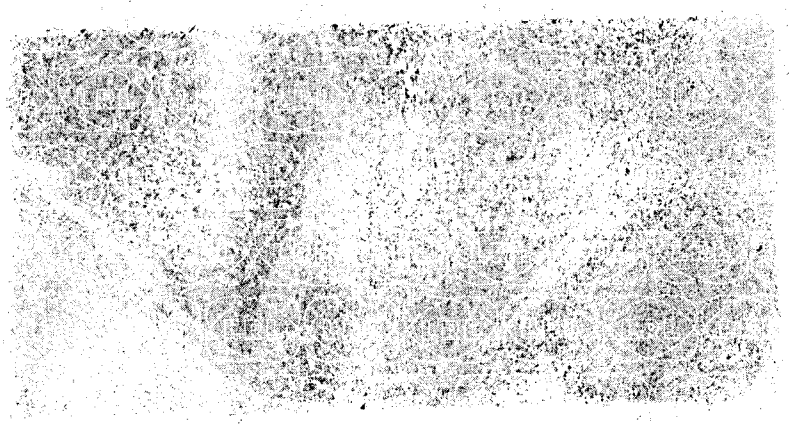

EFFECT OF NATURAL CONVECTION UPON THE TEMPERATURE TRANSIENT FOLLOWING A LOSS-OF-COOLANT ACCIDENT IN A NUCLEAR POWER REACTOR

Berkeley, California

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Research and Development

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Ronald Paul Omberg

(Master's Thesis)

September 1965

Research and Development

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EFFECT OF NATURAL CONVECTION UPON THE TEMPERATURE TRANSIENT FOLLOWING A LOSS-OF-COOLANT ACCIDENT IN A NUCLEAR POWER REACTOR

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ABSTRACT

The effect of natural convection upon the temperature transient following a loss-of-coolant accident was analyzed for a typical power reactor configuration. The system of equations developed was then solved using the design parameters of the Loss-of-Fluid Test Reactor (LOFT). It was found that natural convection will prevent melting of either the fuel or cladding at low power levels. At the maximum power density allowable in the LOFT the time to fuel melting was nearly twice that for an adiabatic transient.

The solution was based on the one-dimensional quasi-steady state flow conservation equations coupled to the lumped capacity transient energy equation for small axial sections of fuel. The time dependence of the decay heat source was considered; the frictional losses and heat transfer were assumed to be adequately represented by the relations for turbulent pipe flow. A constant radial power density was assumed. The system of equations was amenable to machine calculation using an iterative scheme. It was found that natural convection has a pronounced effect, as compared to the adiabatic case, upon the temperature transient. With a constant radial power density, it was found that the cladding will not melt at power levels below 38 Mw and the fuel will not melt at power levels below 68-1/2 Mw.

NOMENCLATURE

A_f:	Cross-sectional area of fuel (cm^2)
A_F:	Cross-sectional area for coolant (cm^2)
c_p:	Specific heat (cal/gm-°C)
d:	Fuel element diameter (cm)
E:	Internal energy per unit mass (cal/gm)
f:	Friction factor
g:	Acceleration of gravity (cm/sec^2)
G:	Mass flow rate (gm/cm^2-sec)
H:	Enthalpy per unit mass (cal/gm)
k:	Thermal conductivity (cal/sec-cm-°C)
ℓ:	Either slab thickness or the exponent in the viscosity-temperature relation
L:	Core height (cm)
L':	Height of core plus stack region above it (cm)
L_{eq}:	Equivalent length of the external piping
p:	Pressure (psia)
P:	Steady state power (watts)
P_w:	Wetted perimeter in core (cm)
P_H:	Heated perimeter in core (cm)
q:	Surface heat flux (cal/cm^2-sec)
Q:	Heat generation rate per unit volume (cal/cm^3-sec)
r_H:	Hydraulic radius, $r_H = A_F/P_w$ where P_w is the wetted perimeter (cm)
r_H':	Heated hydraulic radius, $r_H' = A_F/P_H$ where P_H is the heated perimeter (cm)
t:	Time (sec)

T: Temperature, absolute unless stated otherwise (°K)

u: Velocity of coolant (cm/sec)

x: Distance from the core inlet (cm)

Dimensionless Numbers

$\tilde{G}$: Dimensionless mass flow rate

Nu: Nusselt number

Pr: Prandtl number

$\tilde{Q}$: Dimensionless heat generation rate

St: Stanton number

Greek Letters

α: Thermal diffusivity (cm^2/sec)

β: Coefficient of thermal expansion (1/°K)

δ: Ratio of L_{eq} to L

ξ: Dimensionless length

Θ: Dimensionless temperature

μ: Dynamic viscosity (poise)

ρ: Density (gm/cm^3)

τ: Dimensionless time

Subscripts

c: Cladding

f: Fuel

1: Inlet to core

ex: External piping

I. INTRODUCTION

Most calculations of the temperature transient following a loss-of-coolant accident in a nuclear power reactor neglect the effect of natural convection. A survey of the hazards analyses for six operating power reactors indicated that while heat transfer by conduction and thermal radiation were considered, heat removal due to natural convection was neglected in each case.[1-6] In the case of the Loss-of-Fluid Test Reactor (LOFT), Jensen, et al. considered natural convection; however, as the conduction and radiation mechanisms were emphasized, the treatment of natural convection was simplified.[7] If conduction and thermal radiation are neglected and the natural convection process considered to act alone, a treatment considerably more detailed than that of Ref. (7) is possible. For this case, Olander, et al. have shown that in a low power reactor of the TRIGA type, the temperatures within the core remain significantly below the melting point.[8]

This investigation assessed the effect of heat transfer by natural convection upon the temperature transient in a large power reactor. First, a qualitative evaluation of the buoyant force available was made considering the coolant to be quiescent air and conduction to it as the only mode of heat transfer. This evaluation indicated that a substantial amount of the coolant was being heated to a relatively high temperature prior to melting of the fuel element, and consequently a more detailed analysis was warranted. Thus, an analysis similar to that of Ref. (8) but with temperature dependent properties was made using a typical power reactor configuration considering natural convection to be the sole mode

of heat removal. The specific assumptions were:

(1) The radial power density is constant; the power density varies as a cosine axially and the time dependence of the heat decay is given by Ref. 3. Infinite operation prior to shutdown was assumed.

(2) The flow through the system can be described by the quasi-steady state form of the one-dimensional conservation equations coupled to the lumped capacity transient energy equation for small axial sections of fuel.

(3) The frictional losses and heat transfer can be adequately described by the relations for turbulent pipe flow.

The magnitude of the time derivatives in the differential flow conservation equations were evaluated upon solution and neglecting them was found to be justified for over 90% of the temperature transient.

In the power reactor configuration to which the equations were applied, the break was assumed to take place in the piping external to the pressure vessel. The frictional resistance of this piping and the increase in the buoyant force due to the stack effect in the region above the core were considered. The blowdown time was assumed to be zero. Both air and steam coolants were considered.

The resulting equations were amenable to machine calculation using an iterative scheme. This was done using the design parameters for the LOFT reactor.[7,10] As compared to the adiabatic case, it was found that natural convection has a pronounced effect upon the temperature transient, especially in the case of the steam coolant. At low power levels it alone is enough to prevent melting of either the cladding or the fuel. At the higher power levels it produces a significant increase, as compared to the adiabatic case, in the time to fuel melting.

II. QUALITATIVE EVALUATION OF THE BUOYANT FORCE

Initially it was not known whether the time from the inception of the loss-of-coolant accident until clad melting would be large enough to allow a significant flow rate to be developed. In order to evaluate this the following analysis was made.

If it is assumed that the reactor core is filled with quiescent air immediately following the loss-of-coolant accident, the initial heat loss will be by conduction to the air rather than convection. An estimation of the buoyant force developed can be made by considering conduction to the quiescent air to be the only mode of heat transfer throughout the transient until such time as the melting point of the fuel elements is attained. If at such time the temperature profile has penetrated a significant distance into the surrounding air, it may be concluded that, as a large volume of air is heated, a buoyant force large enough to induce a substantial flow rate will be developed. Consequently, the effect of convection during this period should not have been neglected. However, if the profile has penetrated only a small distance, the flow induced will probably have been small and so neglecting convection may be justified.

Thus, a calculation was made for a fuel element equivalent to that in the LOFT reactor in order to evaluate qualitatively the magnitude of the buoyant force. By equivalent is meant that the LOFT cylindrical fuel element was represented by a semi-infinite slab whose thickness is prescribed so that the surface heat flux is identical to that of a LOFT fuel element during steady state operation. A single semi-infinite slab fuel element in an infinite air medium was then considered and the

following assumptions made:

(1) The fuel element is surrounded by quiescent air for all time.

(2) The initial temperature of the air is 260°C.

(3) The fuel element consists of a homogeneous mixture of fuel and cladding with the interfacial resistance neglected.

(4) The temperature gradient within the fuel element was neglected since the thermal conductivity of the fuel is much greater than that of the air.

Mathematically the problem is expressed by the time dependent conduction Eq. (1) applied to the quiescent air, subject to the initial and boundary conditions (2), (3), and (4). Figure 1 shows the fuel element and pertinent dimensions.

$$\frac{\partial T}{\partial t} = \alpha \frac{\partial^2 T}{\partial x^2}, \quad \alpha = \frac{k}{\rho c_p} \tag{1}$$

$$\text{I.C.:} \quad T(x,o) = T_o \tag{2}$$

$$\text{B.C.:} \quad \lim_{x \to \infty} T(x,t) = T_o \tag{3}$$

$$\frac{Q\ell}{2} = (\rho c_p)_f \frac{\ell}{2} \frac{\partial T}{\partial t}(o,t) - k \frac{\partial T}{\partial x}(o,t) \tag{4}$$

where T_o is the initial temperature of the air, Q is the volumetric decay heat generation rate, which is assumed constant during the time of interest, ℓ is the slab thickness, ρ and c_p are the density and specific heat, respectively, with the subscript f denoting the fuel element. The slab thickness ℓ is determined by the steady state equation

$$q_{cyl} = q_{slab} \tag{5}$$

or

$$\ell = \frac{1}{2} d \tag{6}$$

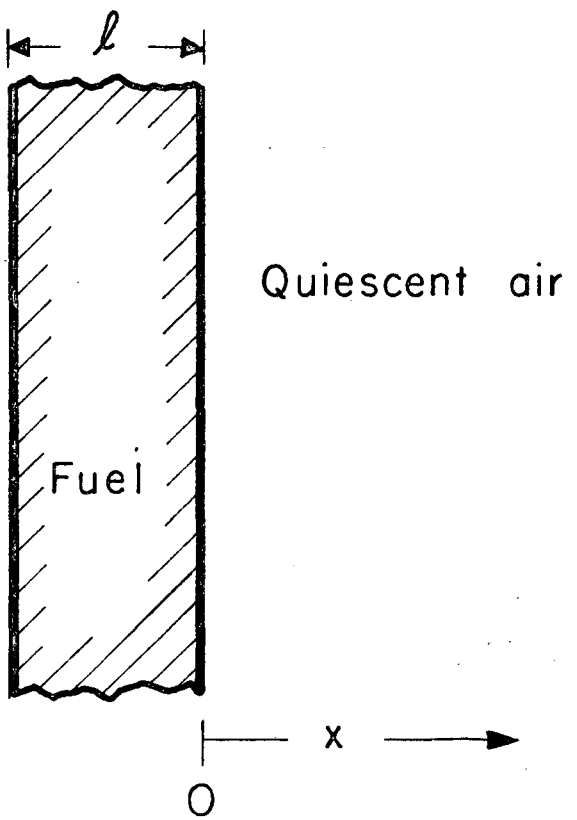

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Fig. 1

where q is the steady state surface heat flux. Thus, from Table II, ℓ for a lumped capacity homogeneous fuel element is 0.490 cm. Equation (1) subject to (2), (3), and (4) can be solved by Laplace Transformation, as shown in Appendix A, to yield:

$$T(x,t) = T_o + \frac{Q}{\alpha(\rho c_p)_f\, b^2} \left\{ \exp\,(2\nu\eta + \nu^2) \cdot \right.$$

$$\operatorname{erfc}\,(\nu + \eta) - (1 + 2\nu\eta)\operatorname{erfc}\,(\eta) +$$

$$\left. \frac{2}{\sqrt{\pi}}\,\nu \exp\,(-\eta^2) \right\} \tag{7}$$

where

$$b = \frac{2k}{\alpha(\rho c_p)_f \ell} \tag{8}$$

$$\eta = \frac{x}{\sqrt{4\,\alpha\, t}} \tag{9}$$

$$\nu = \frac{bx}{2\eta} \tag{10}$$

At the surface of the fuel element, $x = 0$:

$$T(o,t) = T_o + \frac{Q}{\alpha\,(\rho c_p)_f\, b^2} \left\{ \exp\,(\nu^2) \cdot \right.$$

$$\left. \operatorname{erfc}\,(\nu) - 1 + \frac{2}{\sqrt{\pi}}\,\nu \right\} \tag{11}$$

It is difficult to determine precisely the value of Q to be used in (7) and (11). Since Q is a constant in this solution it should be the average value of Q(t) over the time to fuel melting. An estimation of the time to melting can be made by solving the adiabatic Eq. (12) within the fuel using the time dependent heat source (13).[9]

$$(\rho c_p)_f \frac{\partial T}{\partial T} = Q(t) \tag{12}$$

$$Q(t) = \frac{1}{200} PM(t) \tag{13}$$

$$M(t) = \begin{cases} 12.05\, t^{-0.0639} & 0.1 \leq t < 10 \\ 15.31\, t^{-0.1807} & 10 \leq t < 150 \\ 26.02\, t^{-0.2334} & 150 \leq t < 4 \cdot 10^6 \end{cases} \tag{14}$$

Here P is the steady state power in watts and t is the time after the shutdown in seconds. Using the values for the lumped capacity homogeneous fuel element and the properties of the air from Table I, the solution gives the dashed curves shown in Fig. 2. The time over which to average Q(t) for use in (7) and (11) is then determined to be 600 and 2000 seconds for the maximum and average fuel elements, respectively. This is the time at which the fuel melts (assumed at 2500°C) in each case. Average values of M were then computed to be 5.67 and 4.08, respectively. A plot of Eq. (11) for these values is shown by the solid lines in Fig. 2; the curves cross the adiabatic curves because a constant heat generation rate is being used in this case. Equation (7) is now solved to determine the temperature profile in the surrounding air at t = 300 seconds, the time at which the cladding melts (1400°C) in the maximum heat generation case. This profile is shown in Fig. 3. The profile can be seen to penetrate into the air a distance of more than 1.5 cm. As the LOFT fuel elements have a center-to-center spacing of 1.47 cm, this indicates that most of the air surrounding the fuel elements will be at a temperature considerably above T_o. Consequently, it is probable that

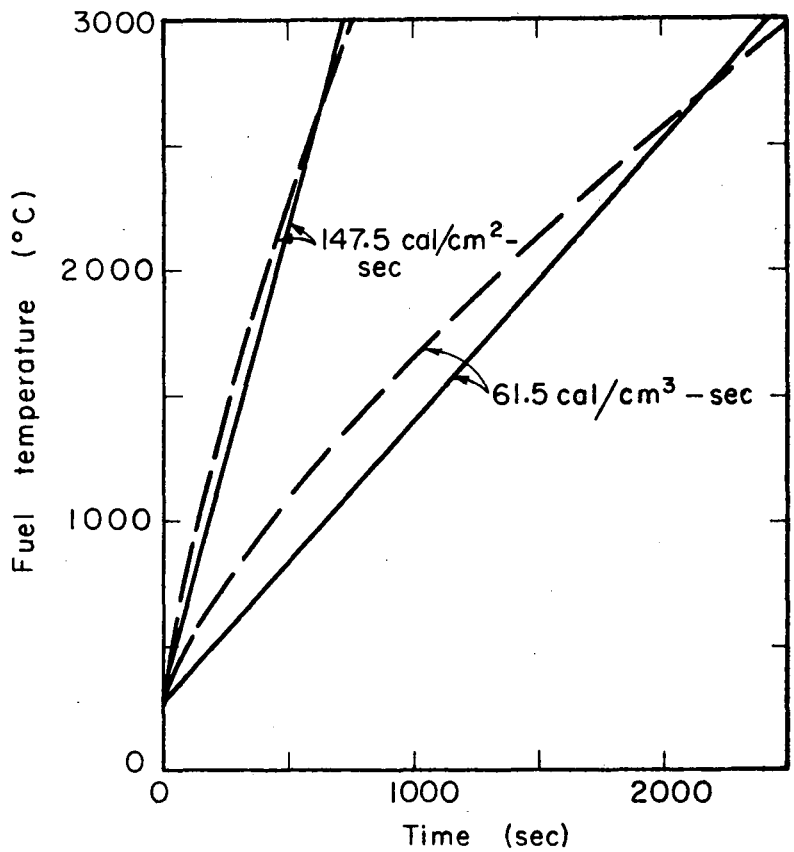

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Fig. 2

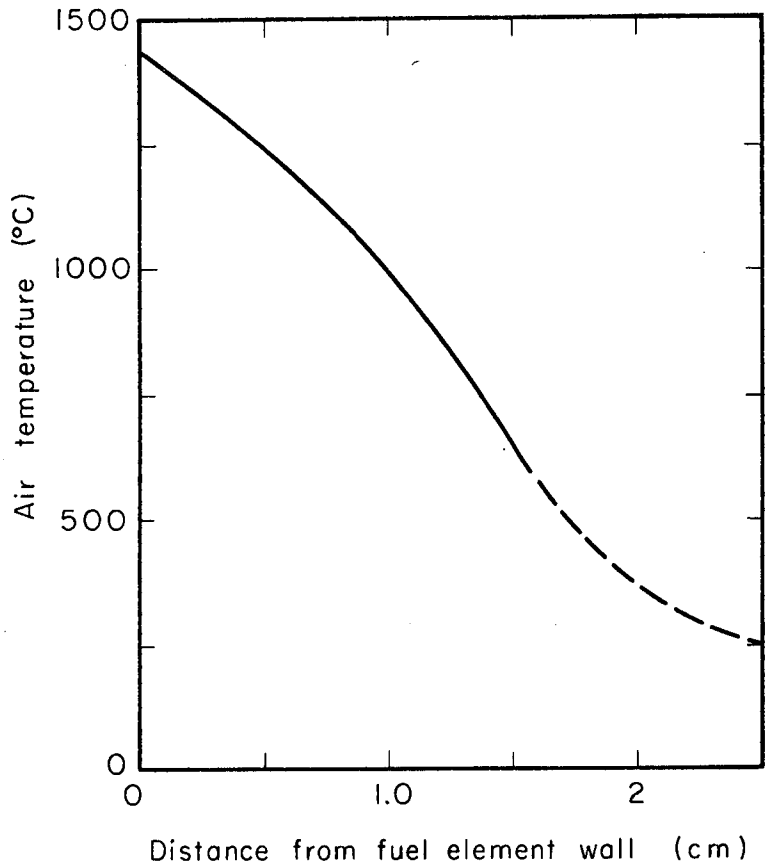

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Fig. 3

Table I. Properties of the Lumped Capacity Homogeneous Slab Fuel Element and Surrounding Air

Fuel Element:	
Material	UO_2
Density, ρ_f (gm/cm^3)	9.45
Specific heat, c_p (cal/gm-°C)	0.115
Thermal conductivity, k_f (cal/sec-cm-°C)	0.0242
Thickness, ℓ(cm)	0.490
Air (260°C):	
Thermal diffusivity, α (cm^2/sec)	0.586
Specific heat, c_p (cal/gm-°C)	0.247
Thermal conductivity, k (cal/sec-cm-°C)	0.0000955

the flow rates induced are large enough so that natural convection will be a significant mode of heat transfer during the temperature transient, and thus a more detailed analysis is warranted.

III. THE NATURAL CONVECTION TEMPERATURE TRANSIENT

As the buoyant force developed appeared large enough to warrant further investigation, the one-dimensional flow conservation equations with a lumped-capacity fuel energy equation were solved for a typical power reactor configuration as shown in Fig. 4. The following assumptions were made:

(1) The radial power density is constant; the power density varies as a cosine axially. The time dependence of the heat decay is given by Eqs. (13) and (14) which assume infinite operation prior to shutdown.

(2) The flow through the system can be described by the quasi-steady state form of the one-dimensional conservation equations coupled to the lumped capacity transient energy equation for small axial sections of fuel.

(3) The frictional losses and heat transfer can be adequately described by the relations for turbulent pipe flow.

The one-dimensional flow equations indicating conservation of mass, momentum, and energy in the coolant are:

$$\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x} (\rho u) = 0 \tag{15}$$

$$\frac{\partial}{\partial t} (\rho u) + \frac{\partial}{\partial x} (\rho u^2) = - \frac{\partial \rho}{\partial x} - \rho g - \frac{f}{r_H} \frac{\rho u^2}{2} \tag{16}$$

$$\frac{\partial}{\partial t} \left[\rho \left(E + \frac{1}{2} u^2 \right) \right] + \frac{\partial}{\partial x} \left[\rho u \left(H + \frac{1}{2} u^2 \right) \right] =$$

$$\frac{q}{r_H} - \rho u g \tag{17}$$

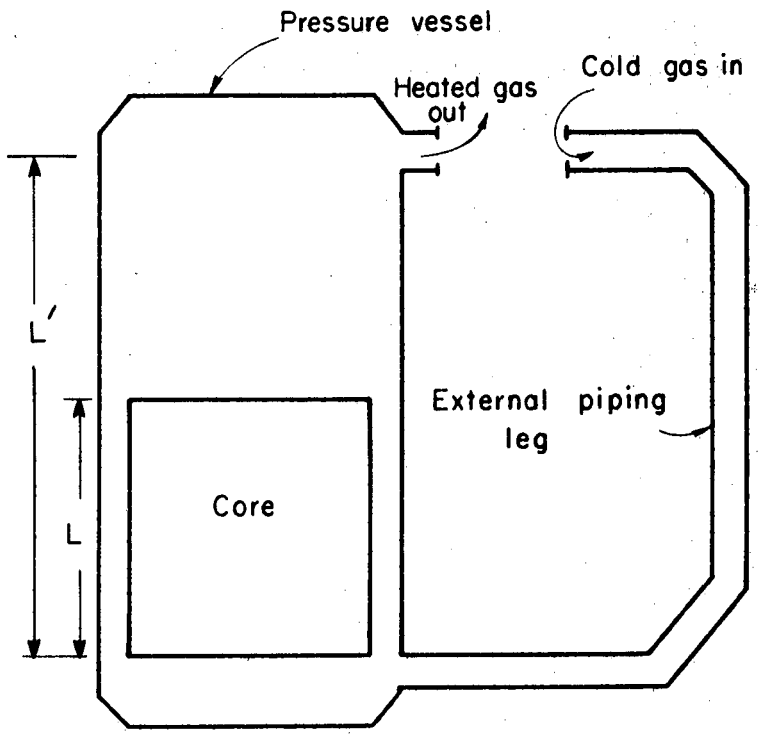

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Fig. 4

where r_H is the hydraulic radius, $r_H = A_F/P_w$, and r_H' is the heated hydraulic radius, $r_H' = A_F/P_H$, where P_H is the heated perimeter.

Defining

$$G = \rho u \tag{18}$$

and neglecting the derivatives with respect to time, and also neglecting the kinetic and potential energy terms in (17), produces a set of ordinary differential equations with the time variable a parameter:

$$\frac{dG}{dx} = 0 \tag{19}$$

$$\frac{dp}{dx} = -\frac{d}{dx}\left(\frac{G^2}{\rho}\right) - \rho g - \frac{fG^2}{2\rho r_H} \tag{20}$$

$$-\frac{d}{dx}(GH) = \frac{q_w}{r_H'} \tag{21}$$

If the subscript 1 denotes the inlet to the core, then Eq. (19) gives

$$G(x,t) = G_1(t) \tag{22}$$

and thus

$$\frac{dp}{dx} = -G_1^2 \frac{d}{dx}\left(\frac{1}{\rho}\right) - \rho g - \frac{fG_1^2}{2\rho r_H} \tag{23}$$

$$G_1 \frac{dH}{dx} = \frac{q_w}{r_H'} \tag{24}$$

Using the ideal gas law (neglecting the slight pressure drop through the core):

$$\rho = \frac{\rho_1 T_1}{T} \tag{25}$$

and the definition of specific heat:

$$dH = c_p dT \tag{26}$$

reduces (23) and (24) to:

$$\frac{dp}{dx} = -\frac{G_1^2}{\rho_1 T_1}\frac{dT}{dx} - \frac{\rho_1 T_1 g}{T} - \frac{f G_1^2 T}{2\rho_1 T_1 r_H} \tag{27}$$

$$c_p G_1 \frac{dT}{dx} = \frac{q_w}{r_H} \tag{28}$$

The lumped-capacity fuel energy equation is:

$$(\rho c_p)_f \frac{dT_f}{dt} = Q(x,t) - \left(\frac{A_T}{A_f}\right)\frac{q_w}{r_H} \tag{29}$$

where the derivative is written as ordinary since conduction within the fuel has been neglected. The heat transfer coefficient h is defined by:

$$q_w = h(T_f - T) \tag{30}$$

and the friction factor is assumed to be represented by

$$f = K\,Re^{-n} \tag{31}$$

where K = 0.046 and n = 0.2 for turbulent flow and the Reynolds Number is expressed:

$$Re = \frac{4\,r_H\,G}{\mu} \tag{32}$$

The local Nusselt Number is assumed to be adequately represented by the Dittus-Boelter relation

$$Nu = 0.023\,Re^{0.8}\,Pr^{0.4} \tag{33}$$

where

$$Nu = \frac{4\,r_H\,h}{k} \tag{34}$$

$$Pr = \frac{c_p\,\mu}{k} \tag{35}$$

and the viscosity is represented by

$$\mu = \mu_1 \left(\frac{T}{T_1}\right)^{\ell} \tag{36}$$

where $\ell = 0.76$ for air. A set of dimensionless variables and parameters is defined:

$$\xi = \frac{x}{L} \tag{37}$$

$$\tau = \left(\frac{k_1 \left(\frac{A_F}{A_f} \right)}{4\, r_H\, r_H'\, (\rho c_p)_f} \right) t \tag{38}$$

$$\Theta = \frac{T}{T_1} \tag{39}$$

$$\Theta_f = \frac{T_f}{T_1} \tag{40}$$

$$\delta = \frac{L_{eq}}{L} \tag{41}$$

$$R = \frac{L'}{L} \tag{42}$$

$$\tilde{Q} = \left(\frac{4\, r_H\, r_H'}{k_1 T_1 \left(\frac{A_F}{A_f} \right)} \right) Q \tag{43}$$

where L is the height of the core, L' is the height of the core plus the stack above it, L_{eq} is an equivalent length for the external piping (includes the resistance of the heat exchanger and coolant pump), and Q is the midplane power density. Using the relations (30) to (43), Eqs. (28) and (29) can be reduced as shown in Appendices C and D, to:

$$\frac{d\Theta}{d\xi} + \frac{L}{r_H'} St(\xi,\tau)\, \Theta(\xi,\tau) = \frac{L}{r_H'} St(\xi,\tau)\, \Theta_f(\xi,\tau) \tag{44}$$

$$\frac{d\Theta_f}{d\xi} + Nu(\xi,\tau)\Theta_f(\xi,\tau) = \tilde{Q}(\tau) \sin(\pi\xi) + Nu(\xi,\tau)\, \Theta(\xi,\tau) \tag{45}$$

Thus the conservation of mass, momentum, and energy of the coolant and the conservation of energy in the fuel is represented by Eqs. (22, 27,

44, and 45) respectively. The boundary and initial conditions for the equations are, respectively:

$$G(0,t) = G_1(t) \tag{46}$$

$$T(0,t) = T_1 \tag{47}$$

$$\Theta(0,\tau) = \Theta_1 = 1 \tag{48}$$

$$\Theta_f(\xi,0) = \Theta_f(\xi) \tag{49}$$

where the functions on the right-hand side must be known. The function $G_1(t)$ is obtained from the solution of the momentum equation while T_1 and Θ_1 were specified as equal to their values at the inlet to the external piping and $\Theta_f(\xi)$ is a parameter which has been calculated for the LOFT reactor to be independent of ξ and equal to 260°C.[7] Blowdown calculations for other reactors, such as Ref. (5), have also shown the final fuel temperature to be relatively independent of position. Equations (22, 27, 44, and 45) are then solved for the reactor configuration shown in Fig. 4. This consists of a pressure vessel with a reactor core located in the lower half of it and an equivalent length of pipe which represents the external piping. The coolant is assumed to enter this piping at a constant temperature T_1 and to pass through it with no temperature increase. After passing through the core, it is assumed to flow upward through the empty region above the core, denoted the stack, at a constant temperature with no wall friction. It then exits as shown. The solution of the above equations for this configuration is shown in Appendices B, C, and D. The resulting equations are:

Momentum equation:

$$\left(\frac{16\, r_H^{\,2}\, \rho_1^{\,2}\, g\, L}{\mu_1^{\,2}}\right) \frac{1}{Re^2(0,\tau)} \left\{ \int_0^1 \frac{1-\Theta(\xi,\tau)}{\Theta(\xi,\tau)}\, d\xi + \frac{1-\Theta(1,\tau)}{\Theta(1,\tau)}\,(R-1) \right\} + \left[\Theta(1,\tau) - 1\right] +$$

$$\frac{KL}{2\, r_H} \quad \frac{1}{Re^n(0,\tau)} \int_0^1 \left[\Theta^{1+n\ell}(\xi,\tau) + \delta\right] d\xi = 0 \qquad (50)$$

The first term represents the buoyant force of the heated coolant both in the core and the stack above it; the second represents the acceleration through the core; and the third represents the friction in the core and inlet piping.

Coolant energy equation:

$$\Theta(\xi,\tau) = \left\{ \Theta(0,\tau) + \frac{L}{r_H'} \int_0^{\xi} \Theta_f\,(\xi',\tau)\, St(\xi',\tau) \left[\exp \frac{L}{r_H'} \int_0^{\xi'} St(\xi'',\tau)\, d\xi''\right] d\xi' \right\} \cdot \left[\exp\left(-\frac{L}{r_H'} \int_0^{\xi} St(\xi',\tau)\, d\xi'\right)\right] \qquad (51)$$

Lumped capacity fuel energy equation:

$$\Theta_f\,(\xi,\tau) = \left\{ \Theta_f(\xi,0) + \int_0^{\tau} \left[\tilde{Q}(\tau')\, \sin\,(\pi\xi) + \Theta(\xi,\tau')\, Nu\,(\xi,\tau') \right] \exp\left(\int_0^{\tau'} Nu(\xi,\tau'')\, d\tau'' \right) \cdot d\tau' \right\} \cdot \left[\exp\left(-\int_0^{\tau} Nu(\xi,\tau')\, d\tau'\right)\right] \qquad (52)$$

where the Stanton number is defined as:

$$St(\xi,\tau) = \frac{Nu(\xi,\tau)}{Pr\ Re(\xi,\tau)} \tag{53}$$

and Eqs. (22), (32), and (36) give the Continuity equation:

$$Re(\xi,\tau) = \frac{Re(0,\tau)}{\Theta^{\ell}(\xi,\tau)} \tag{54}$$

The effect of the extrapolation length was neglected as indicated by the use of $\sin(\pi\xi)$ for axial shape factor in Eq. (52). The error in the heat generation rate at any axial position induced by this approximation will be less than 6 percent for a reactor of the LOFT type which has an axial peaking factor of 1.5.

These equations can be solved by an iterative method. Assuming values for Re, Θ, and Θ_f, Eq. (50) gives a new value for Re, Eq. (51) a new value for Θ, and Eq. (52) a new value for Θ_f. The process is then repeated until convergence is attained. For initial estimates, simplified solutions of Eqs. (23), (28), and (29), neglecting the variation of viscosity and density with temperature except in the buoyancy term where it is represented by

$$\rho = \rho_1 - \beta\rho_1(T-T_1) \tag{55}$$

and neglecting the effect of the external piping were used. These equations are solved in Appendix E. The resulting initial estimates are:

$$Re(\tau) = [AB(2R-1)\ \tilde{Q}(\tau)]^{0.357} \tag{56}$$

$$\Theta(\xi,\tau) = 1 + B\ \frac{\tilde{Q}(\tau)}{Re(\tau)}(1 - \cos\pi\xi) \tag{57}$$

$$\Theta_f(\xi,\tau) = \left\{\Theta_f(\xi,0) + \int_0^\tau \left[\exp(Nu(0)s)\right] \cdot \right.$$

$$\left[Nu(0)\ \Theta(\xi,s) + \tilde{Q}(s)(\sin \pi\ \xi)\right] \cdot$$

$$\left. ds \right\} \cdot \left[\exp(-Nu(0)\tau)\right] \tag{58}$$

where the Nusselt number is given by Eq. (33) and A and B are the dimensionless coefficients:

$$A = \frac{32\ \rho_1^{\ 2}\ g\ \beta\ T_1\ r_H^{\ 3}}{K\ \mu_1^{\ 2}} \tag{59}$$

$$B = \frac{k_1\ L}{\mu_1\ c_p\ r_H} \tag{60}$$

IV. THERMAL AND GEOMETRIC PARAMETERS

To evaluate the effect of natural convection upon a temperature transient, a specific reactor design must be chosen. It has been proposed[7,10] that a reactor whose characteristics are typical of currently operating power reactors be constructed and deliberately subjected to a loss-of-coolant accident. This reactor, a 50 Mw, UO_2 fueled, pressurized water reactor, designated the Loss-of-Fluid Test Reactor (LOFT), furnished the thermal and geometric parameters used in this analysis. It was chosen because these parameters are supposedly typical of presently operating pressurized water reactors. The parameters are shown in Table II.

Based upon the values in Table II, the parameters for a homogenized fuel element were calculated with $\bar{\rho}_f$ and $\bar{c}_p$ defined by:

$$\bar{\rho}_f \equiv \frac{\rho_f A_f + \rho_c A_c}{A_f + A_c} \tag{61}$$

$$\bar{c}_{p_f} \equiv \frac{\rho_f A_f c_{p_f} + \rho_c A_c c_{p_c}}{(A_f + A_c)\,\bar{\rho}_f} \tag{62}$$

$$\bar{A}_f \equiv A_f + A_c \tag{63}$$

$$\bar{k}_f \equiv \frac{k_f A_f + k_c A_c}{A_f + A_c} \tag{64}$$

The parameters used in the solution of Eqs. (50) to (54) are shown in Table III.

Table II. Loft Design Parameters

Core height, L (cm)	91
Cross-sectional area of fuel, A_f (cm^2)	1770
Cross-sectional area of cladding, A_c (cm^2)	380
Cross-sectional area for coolant, A_F (cm^2)	5130
Rod spacing (center-to-center) (cm)	1.47
Fuel	
Density, ρ_f (gm/cm^3)	10.2
Specific heat, c_{p_f} (cal/gm-°C)	0.08
Thermal conductivity, k_f (cal/cm-sec-°C)	0.010
Pellet diameter (cm)	0.910
Cladding	
Thickness (cm)	0.038
Material	Stainless steel
Density, ρ_c (gm/cm^3)	8.04
Specific heat, c_{p_c} (cal/gm-°C)	0.18
Average fuel power density, Q	256 kw/liter or 61.5 cal/cm^3-sec
Peaking factor	
Radial	1.6
Axial	1.5
Total	2.4
Hydraulic radius, r_H (cm)	0.480
Heated hydraulic radius, r_H' (cm)	0.496
Pressure loss through core (psi)	6-1/2
Pressure rise across pumps (psi)	35

Table III. Parameters Used in the Solution

Core		
Height, L (cm)		91
Hydraulic radius, r_H (cm)		0.48
Heated hydraulic radius, r_H' (cm)		0.496
Cross-sectional area for coolant, A_F (cm^2)		5130
Cross-sectional area for fuel, $\bar{A}_f$ (cm^2)		2150
Equivalent length of external piping*, L_{eq} (cm)		400
Height of core plus stack region, L' (cm)		182
Fuel		
Density, $\bar{\rho}_f$ (gm/cm^3)		9.45
Specific heat, $\bar{c}_{pf}$ (cal/gm-°C)		0.115
Thermal conductivity, $\bar{k}_f$ (cal/sec-cm-°C)		0.0242
Initial fuel temperature, T_{f_1} (independent of ξ) (°C)		260
Coolant (at inlet)	Air	Steam
Pressure (psia)	14.7	35
Temperature T_1 (°C)	25	127
Density, ρ_1 (gm/cm^3)	0.0012	0.00135
Viscosity, μ_1 (poise)	0.0002	0.000138
Thermal conductivity, k_1 (cal/sec-cm-°C)	$7.6 \cdot 10^{-5}$	$6.8 \cdot 10^{-5}$
Coefficient of thermal expansion, β_1 (1/°K)	0.0034	0.0025
Specific heat, c_{p_1} (cal/gm-°C)	0.24	0.52
Prandtl Number, Pr	0.70	1.05
Viscosity exponent, ℓ	0.76	0.98
$\frac{16\, r_H^2 \rho_1^2 g L}{\mu_1^2}$	$1.18 \cdot 10^7$	$3.14 \cdot 10^7$

* The method for calculating L_{eq} is shown in Appendix B

V. THE PREDICTED NATURAL CONVECTION TEMPERATURE TRANSIENT FOR THE LOFT POWER REACTOR

A machine program was written which solves Eqs. (50-52, 54) using an iterative method. It is shown in Appendix G. The temperature transient was then calculated using the design parameters shown in Table III as input to the program. Two coolants were considered: air at atmospheric temperature and pressure, and steam at 127°C and 35 psia; the steam conditions are identical to those assumed by Jensen, et al.[7] Four midplane operating power densities, 24.5, 61.5, 98.5, and 147.5 cal/cm^3-sec were considered for the air coolant case with the last three being considered in the steam coolant case. These can be equated to the power level of the reactor, assuming a constant radial power density, by the relation $P = Q\,(A_f + A_c)\,L/1.5$ where Q is the midplane power density. Thus these power densities correspond to power levels of 13-1/2, 33-1/2, 54, and 80 Mw, respectively.

The maximum axial fuel temperature as a function of time is shown in Figs. 5 and 6 for air and steam coolants, respectively. It can be seen that the steam coolant gives temperatures approximately a factor of two below those for an air coolant. There are two reasons for this: one, the dimensionless coefficient multiplying the buoyant force term in Eq. (5), $(16\, r_H^2 \rho_1^2 gL/\mu_1^2)$ is more than a factor of two higher for steam as shown in Table III. This is entirely due to the difference in the kinematic viscosities. Consequently higher Reynolds numbers will exist with steam thereby raising the heat transfer coefficient. That higher Reynolds numbers will in fact be calculated for steam is verified in Figs. 11 and 12. Secondly, the specific heat of steam is a factor of two higher than that of air giving lower coolant temperatures for equal amounts

of heat transferred. As the fuel temperature is a function of the coolant temperature and the heat transfer coefficient, lower fuel temperatures will be obtained with steam. At the maxima of the curves all of the heat generated is being removed by natural convection. In Figs. 7 and 8 the initial portion of the same curves are plotted with the adiabatic case shown for comparison. The time to melting of the fuel and cladding obtained from these curves is shown in Table IV (the melting points of the cladding and fuel are assumed to be 1400°C and 2800°C, respectively)*; the percentage increase over the adiabatic case in the time to clad melting varies from 12 percent to an infinite amount while that for the fuel varies from 25 percent to an infinite amount. The data from Table IV is shown graphically in Fig. 9. When one or more of the points exist at infinity, the shape of the curve was difficult to determine exactly and dashed lines were drawn in these regions.

Figure 10 shows the maximum fuel temperature occurring during the transient as a function of the operating power density. It is composed of the locus of the maxima from Figs. 5 and 6. It can be seen in the steam coolant case that the cladding melts at 70 cal/cm^3-sec and the fuel melts at 126 cal/cm^3-sec. By the previous equation, these are equivalent to power levels of 38 and 68-1/2 Mw, respectively. For air, similar values for melting of the cladding and fuel are 24 and 43 Mw, respectively.

---

* A value of 2500°C was used for the fuel melting point in the slab case; however, 2800°C appears to be a more generally accepted value and so will be used here.

Figures 11 and 12 show the Reynolds number at the core inlet and exit. For the air case the Reynolds numbers are decidedly below the critical value of 2000 for smooth tubes; however, for non-circular ducts, departure from the laminar regime has been observed at Reynolds numbers on the order of 1000 with the turbulent regime fully established at Reynolds numbers of 1800.[11-12] The core length is approximately 48 hydraulic diameters and has several spacer grids (used to maintain the fuel elements in their proper orientation) across the core perpendicular to the flow direction. Thus the calming length may be large enough that turbulent flow will persist throughout the core length. For the steam case shown in Fig. 11, the Reynolds numbers are on the order of 1900 and so there is little doubt that the flow is turbulent at the core inlet. Again the complex flow path is assumed to maintain the turbulent regime throughout the core length. A single calculation was made assuming laminar flow to exist throughout the transient and the results are discussed in the last paragraph of this section. The Reynolds number at the core inlet in Figs. 11 and 12 does not increase monotonically with power density as might be expected. This is because the average coolant temperature level increases with increasing power density causing the hotter gas to experience less of a change in buoyant force per incremental rise. Also, the hotter gas is more viscous and less dense than the colder gas at the lower power density. Thus, as the coolant temperature increases, the forces which retard the flow increase at a faster rate than the buoyant force which induces the flow, thereby causing the Reynolds number to pass through a maximum. It is this effect that causes the maximum fuel temperature for air to rise rapidly above 60 cal/cm^3-sec as shown in Fig. 9. For steam, the effect occurs at a higher power density,

primarily because the higher specific heat of steam results in a lower coolant temperature for the same power density.

Figures 13 and 14 show the maximum coolant temperature transient for the cases considered. It usually, though not always, occurs at the outlet of the core. This departure of the maximum from the outlet occurs when the coolant becomes hotter than the fuel causing heat transfer from the coolant to the fuel. This is to be expected since the heat generation at the core exit is zero and axial conduction has been neglected. Its effect, however, was always small.

The magnitude of the terms dropped in the derivation were evaluated by the computer program upon solution and, with one exception, found to be less than one percent of the terms retained for times greater than 25 seconds. The exception was the axial conduction term; the axial conduction amounted to about 5 percent of the radial conduction rate.

It was attempted to determine if the assumption of a constant radial heat generation rate at the peak power density is conservative. An analysis similar to that used the derivation of Eqs. (50-54) was made on a system consisting of two parallel flow paths attached to a length of inlet piping. Each path contained a fuel element generating heat at a constant rate. The system was assumed to be in a steady state. The effect of reducing the heat generation rate in one of the fuel elements was investigated; however, the equations became too complex to be easily solved. The analysis is shown in Appendix F.

If an actual loss-of-coolant accident were to occur, the containment vessel would be filled with a mixture of air and steam. Thus the specific heat of the mixture would be lower than that of steam which would result in higher fuel temperatures than for pure steam. However, the density of

the mixture will be greater than that for steam alone; reference to the momentum equation (27) shows that this will result in an increase in the buoyant force along with a decrease in the acceleration and frictional terms. Thus higher Reynolds numbers should exist and hence the two effects are compensatory.

A single calculation with the machine program was made to indicate the effect of the external piping. The result is shown in Fig. 15 for a steam coolant with a heat generation rate of 147.5 cal/cm^3-sec. It can be seen that the Reynolds numbers are about 50 percent higher and the fuel temperatures about 30 percent lower than the case with the external piping in the circuit. The fuel does not melt in this case.

A single calculation was also made with laminar flow assumed to exist throughout the transient. The principle difficulty is, of course, to determine relations for the friction factor and Nusselt number in the geometrically complex LOFT reactor core. As an approximation, it was assumed that the core could be represented by a circular pipe. The friction factor is then given by Eq. (31) with K = 16 and n = 2. The local Nusselt number was assumed to be given by the asymptotic constant wall heat flux case, that is Nu = 4.364.[13] The results for a power density of 147.5 cal/cm^3-sec and the steam coolant are shown in Fig. 16. It can be seen that there is little difference in the fuel temperatures. The Reynolds number in the laminar case rises less steeply because the laminar relation gives a higher friction factor at low Reynolds numbers. At high Reynolds numbers it drops more steeply because the increasing temperature level of the coolant affects the friction factor, through the kinematic viscosity, to the first power rather than the 0.2 power. In changing from the turbulent to the laminar analysis, the length-averaged friction factor has been increased by a factor of approximately

1.3 and the length-averaged Nusselt Number reduced by a factor of approximately 2. Both changes tend to increase the maximum fuel temperature. The maximum fuel temperature increased by less than 10 percent thus indicating the fuel temperature is relatively insensitive to changes in the friction factor and Nusselt Number. It is believed that this insensitivity is caused by the large stack region and also possibly the inlet piping. The significant point, however, is that the fuel temperatures differ only slightly and that the inlet Reynolds number quickly rises to the critical value. Thus the fact that natural convection existed during the initial portion of the transient, and possibly near the core outlet, did not significantly affect the fuel temperature in this case.

Table IV. Time to Fuel and Clad Melting for the Loft Reactor

Power Density (cal/cm^3-sec)	Coolant	Time to clad melting (sec)	Time to fuel melting (sec)	Percentage increase in the time to clad melting over the adiabatic case (%)	Percentage increase in the time to fuel melting over the adiabatic case (%)
61.5	Adiabatic	770	2450	-	-
61.5	Air	1160	-	51	∞
61.5	Steam	∞	∞	∞	∞
98.5	Adiabatic	410	1200	-	-
98.5	Air	490	1870	20	56
98.5	Steam	600	∞	46	∞
147.5	Adiabatic	240	690	-	-
147.5	Air	270	860	12	25
147.5	Steam	290	1200	21	74

The melting points of the cladding and fuel are assumed to be 1400°C and 2800°C, respectively.

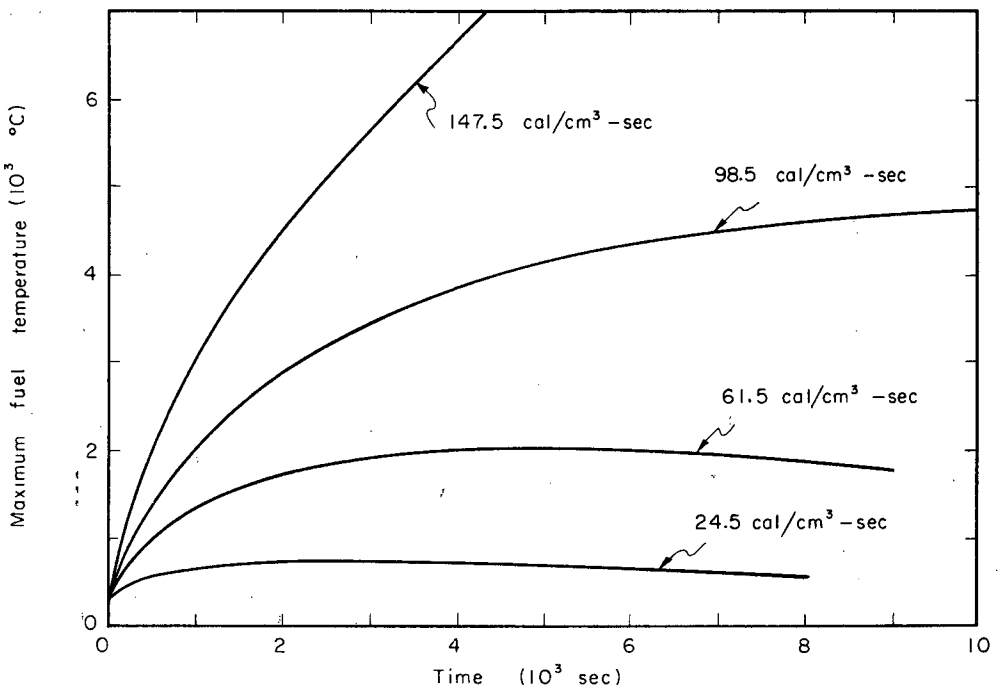
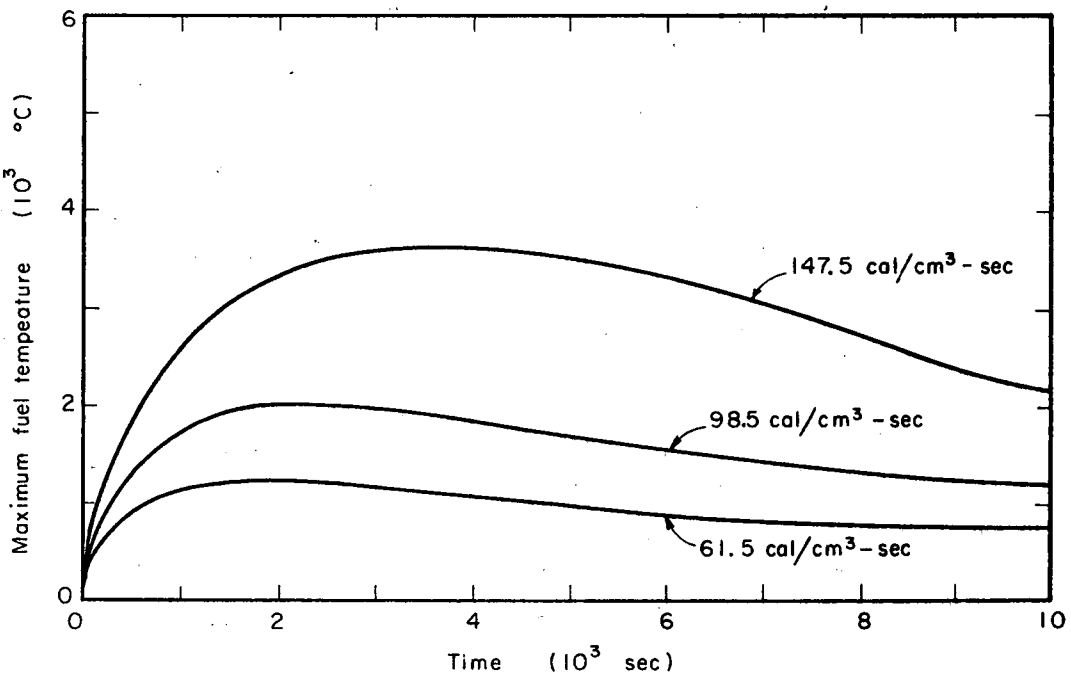
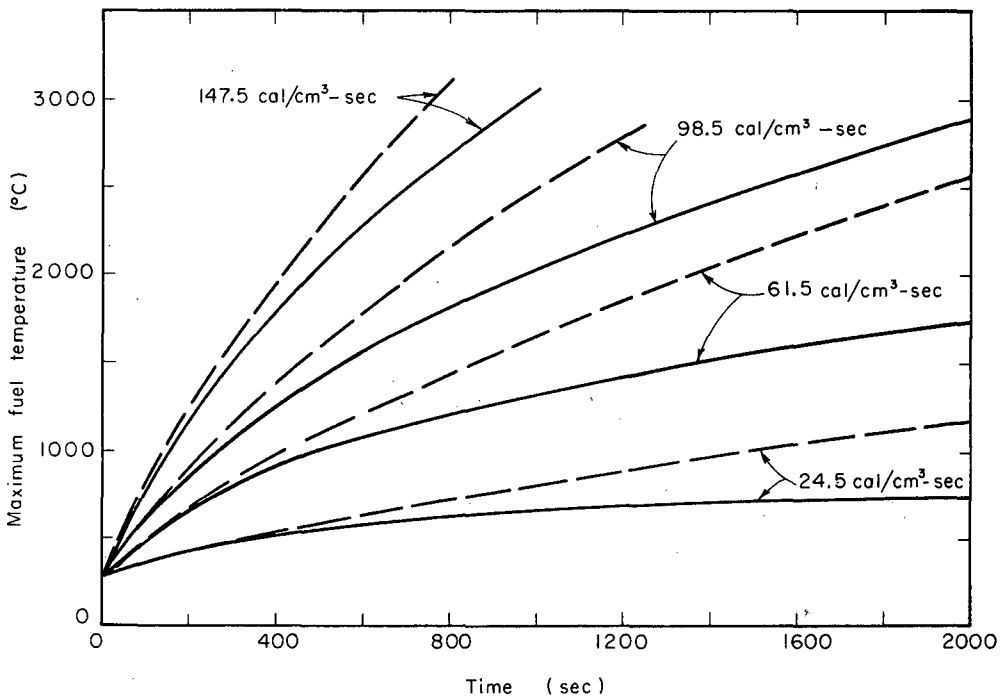

147.5 cal/cm³ -sec
98.5 cal/cm³ -sec
61.5 cal/cm³ -sec
24.5 cal/cm³ -sec
Maximum fuel temperature (10³ °C)
Time (10³ sec)
0
2
4
6
8
10
MU-36725

Fig. 5



MU-36726

Fig. 6



MU-36727

Fig. 7

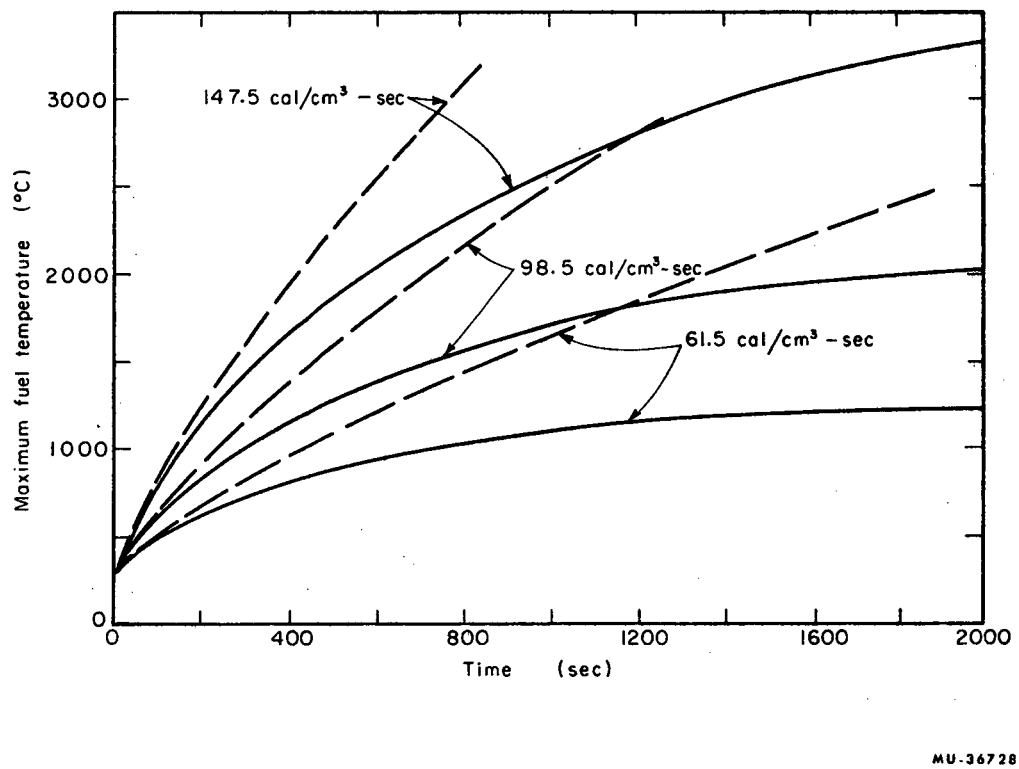
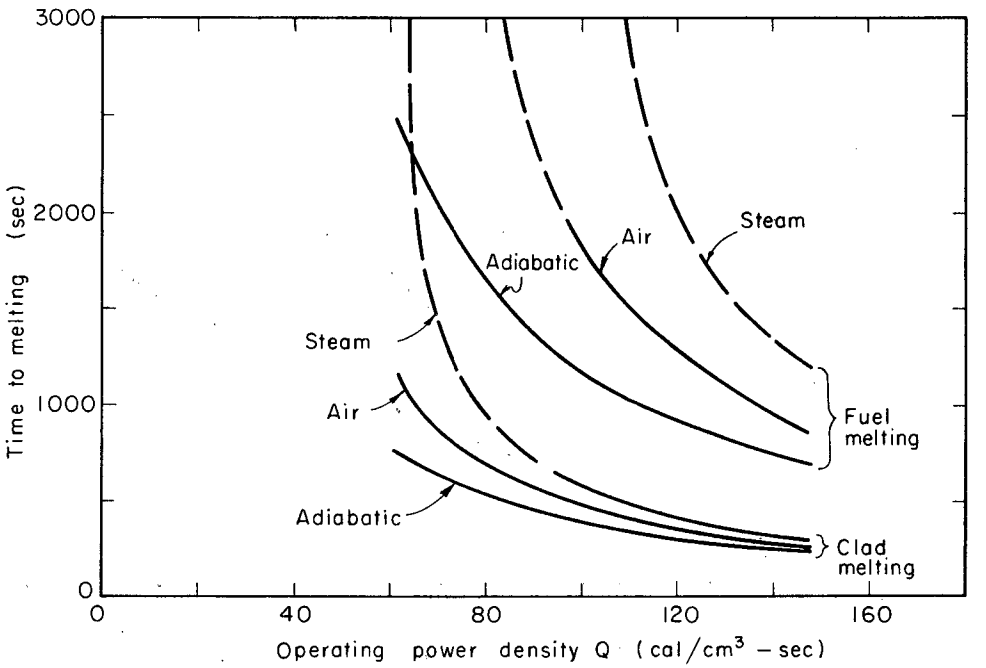
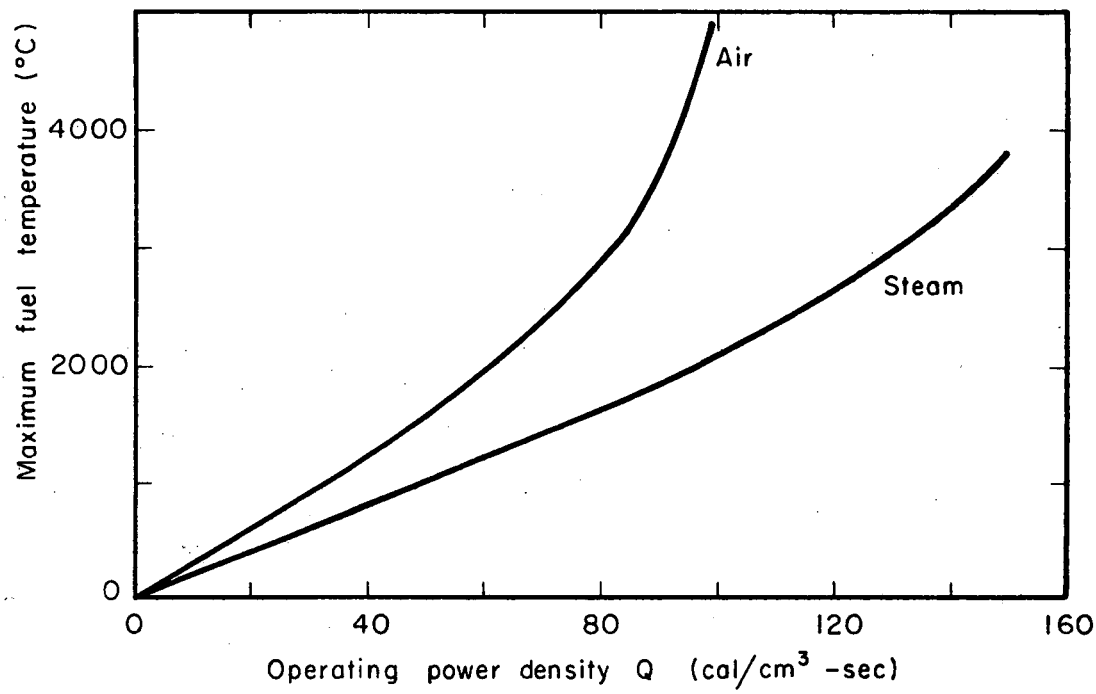

147.5 cal/cm³ – sec
98.5 cal/cm³-sec
61.5 cal/cm³ – sec
Maximum fuel temperature (°C)
3000
2000
1000
0
0
400
800
1200
1600
2000
Time (sec)
MU-36728

Fig. 8



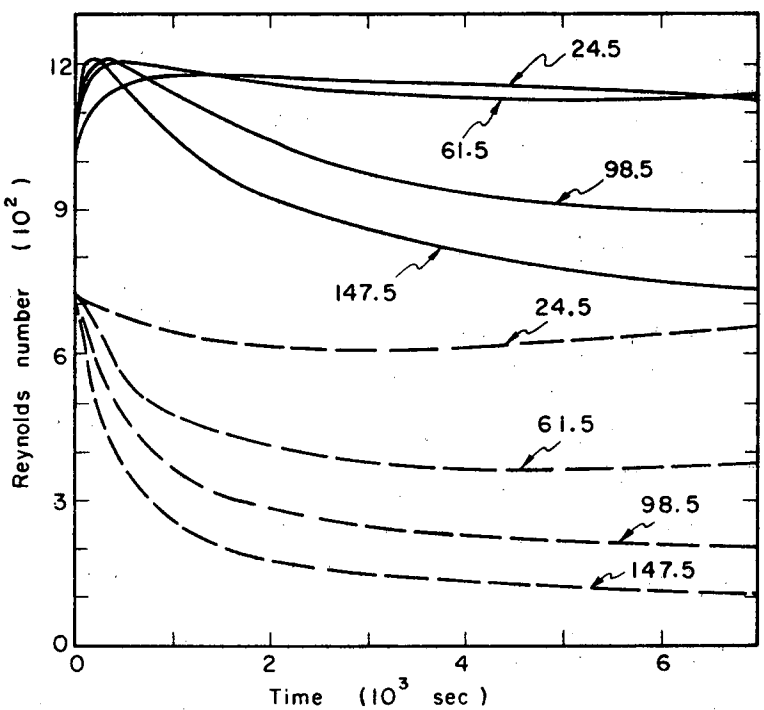
MU-36729

Fig. 9



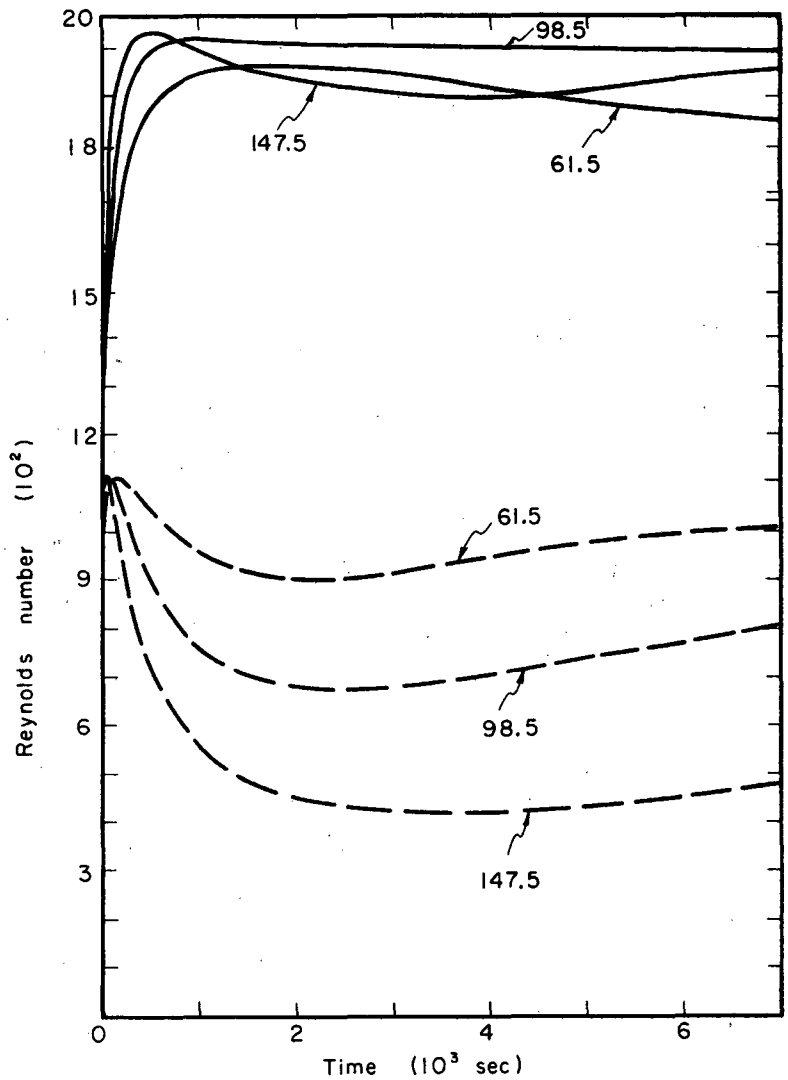
MU-36741

Fig. 10



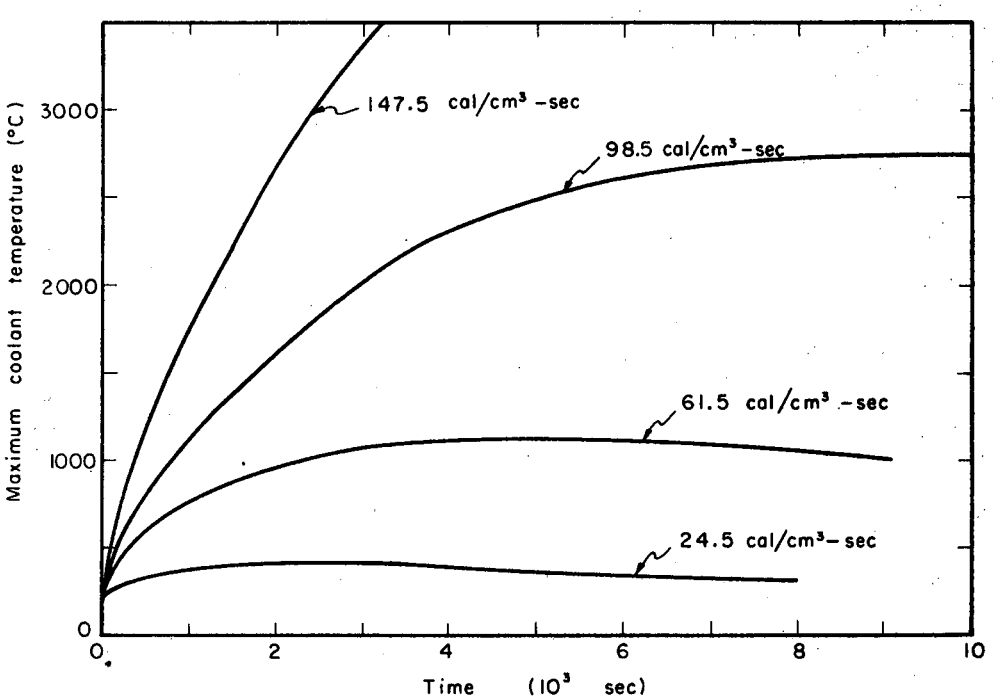
MU-36730

Fig. 11



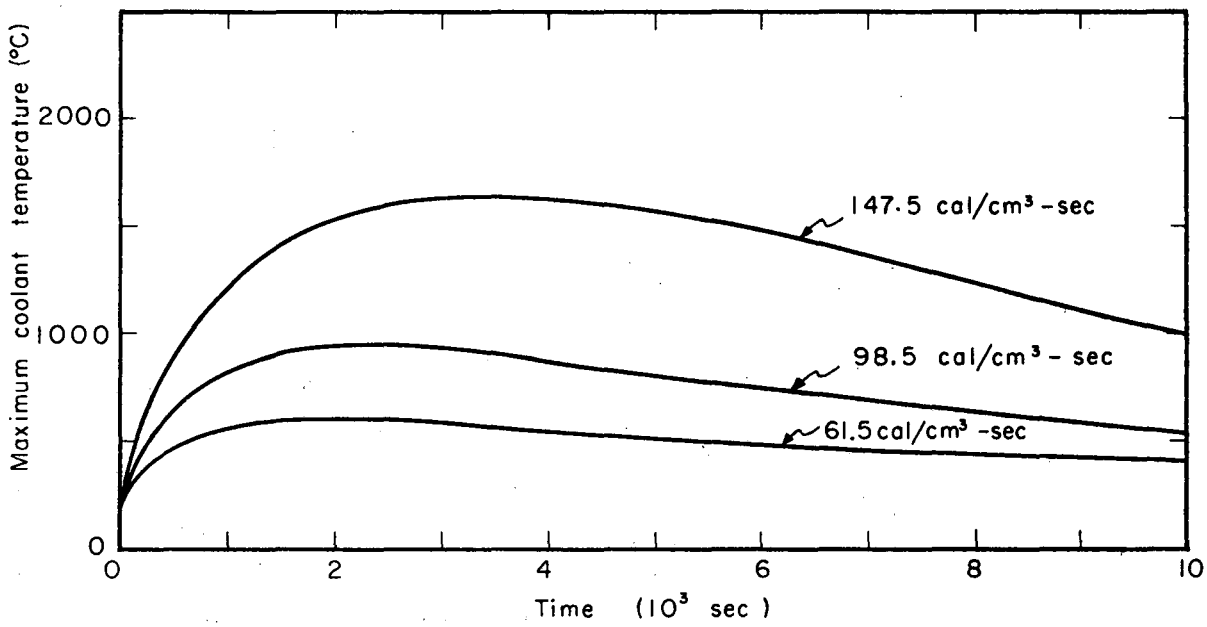
MU-36731

Fig. 12



MU-36732

Fig. 13



MU-36733

Fig. 14

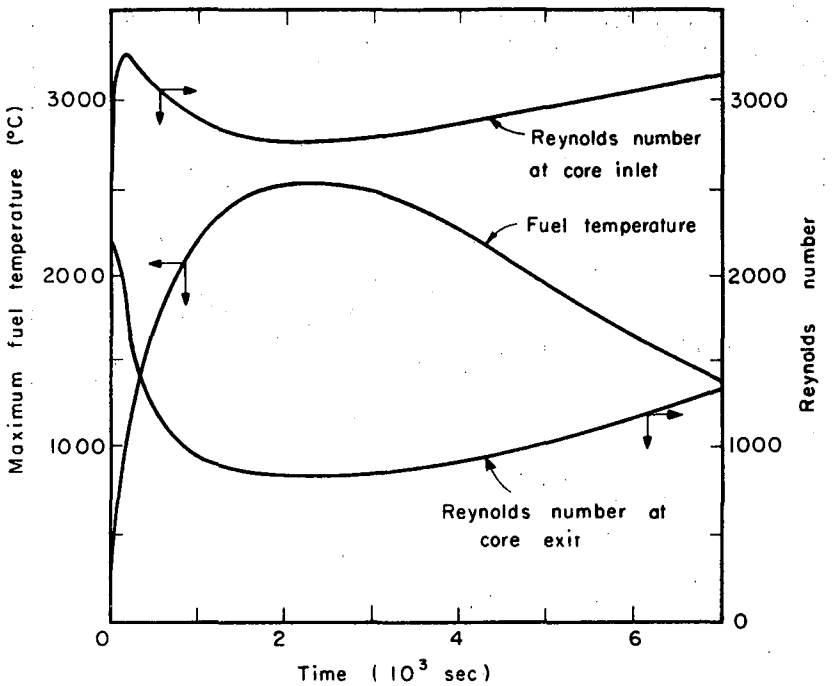

Maximum fuel temperature (°C)
3000
2000
1000
Reynolds number
3000
2000
1000
0
Reynolds number at core inlet
Fuel temperature
Reynolds number at core exit
0
2
4
6
Time (10^3 sec)
MU-36734

Fig. 15

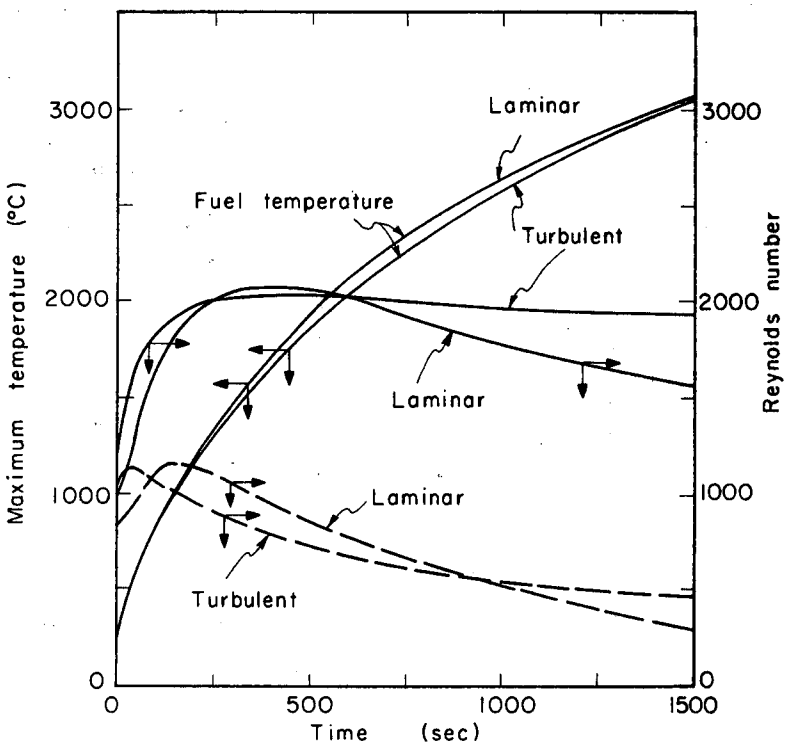

Maximum temperature (°C)
Reynolds number
Time (sec)
Fuel temperature
Laminar
Turbulent
Laminar
Laminar
Turbulent
0
1000
2000
3000
0
500
1000
1500
MU-36735

Fig. 16

VI. CONCLUSION

The effect of natural convection upon the temperature transient in a reactor of the LOFT type is significant. If the coolant is assumed to consist of steam alone and the reactor operates at a midplane power density of 61.5 cal/cm^3-sec before shutdown, neither the fuel nor the cladding will melt. The maximum temperature attained during the transient is 1250°C. At 98.5 cal/cm^3-sec, the time to reach the melting point of the cladding has been extended from the adiabatic value of 410 sec to 600 sec; the fuel melting was not reached. At the highest power density allowable in the LOFT, 147.5 cal/cm^3-sec, the effect of natural convection is to delay the onset of clad melting by 50 sec and fuel melting by 510 sec. The melting point of the cladding and the fuel will not be attained at power densities below 70 and 126 cal/cm^3-sec, respectively. As a constant radial power density has been assumed, these correspond to power levels of 38 and 68-1/2 Mw, respectively. If a double break occurs (simultaneous rupture of the coolant piping to and from the vessel), the maximum fuel temperature attained during the transient at 147.5 cal/cm^3-sec is 2500°C and thus the fuel will not melt. This corresponds to a power level of 80 Mw.

In summary, these calculations have indicated that natural convection would prevent melting of the LOFT fuel following a loss-of-coolant accident at low power levels. At the peak power density in LOFT, the time to fuel melting is nearly twice that for an adiabatic transient.

ACKNOWLEDGMENTS

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APPENDIX A

SOLUTION OF THE CONDUCTION PROBLEM

The problem is to calculate the temperature profile which would exist if the fuel element were transferring heat by conduction only to a quiescent atmosphere. Since the thermal conductivity of the lumped capacity homogeneous fuel element is much greater than that of the air, the temperature gradient within the fuel will be assumed to be zero. Also, the cylindrical LOFT fuel element will be represented by an infinite slab as the solution is less tedious in the rectangular coordinate system. The error induced in the temperature profile should be small for short distances from the fuel element surface. The thickness of the slab will be determined so that its surface heat flux will be identical to that existing in the LOFT reactor.

The slab thickness is calculated using

$$q_{slab} = q_{cyl} \tag{1-A}$$

where

$$q_{slab} = \frac{1}{2} Q \ell \tag{2-A}$$

and

$$q_{cyl} = Q \left(\frac{1}{4} \pi d^2\right) / \left(\pi d\right) \tag{3-A}$$

giving

$$\ell = \frac{1}{2} d \tag{4-A}$$

Reference to Table II shows ℓ to be 0.490 cm where the diameter d is assumed to include fuel and cladding.

The time dependent conduction equation for the quiescent air is

$$\frac{\partial T}{\partial t} = \alpha \frac{\partial^2 T}{\partial x^2}, \quad \alpha = \frac{k}{\rho c_p} \tag{5-A}$$

subject to the initial and boundary conditions:

$$\text{I. C.:} \quad T(x,0) = T_0 \tag{6-A}$$

$$\text{B. C.:} \quad \lim_{x \to \infty} T(x,t) = T_0 \tag{7-A}$$

$$\frac{Q\ell}{2} = (\rho\, c_p)_f \frac{\ell}{2} \frac{\partial T}{\partial t}(0,t) - k \frac{\partial T}{\partial x}(0,t) \tag{8-A}$$

The geometry of the problem is shown in Fig. 1. Defining

$$\theta(x,t) = T(x,t) - T_0 \tag{9-A}$$

gives

$$\frac{\partial \theta}{\partial t} = \alpha \frac{\partial^2 \theta}{\partial x^2} \tag{10-A}$$

with

$$\text{I. C.:} \quad \theta(x,0) = 0 \tag{11-A}$$

$$\text{B. C.:} \quad \lim_{x \to \infty} \theta(x,t) = 0 \tag{12-A}$$

$$\frac{Q\ell}{2} = (\rho\, c_p)_f \frac{\ell}{2} \frac{\partial \theta}{\partial t}(0,t) - k \frac{\partial \theta}{\partial x}(0,t) \tag{13-A}$$

where the unsubscripted properties refer to the air and are evaluated at T_0. Taking the Laplace Transform with respect to the variable t gives:

$$\frac{d^2\bar{\theta}}{dx^2} - \frac{s}{\alpha}\bar{\theta} = 0 \tag{14-A}$$

with

$$\text{B. C.:} \quad \lim_{x \to \infty} \bar{\theta}(x,s) = 0 \tag{15-A}$$

$$\frac{Q\ell}{2s} = (\rho\, c_p)_f \frac{\ell}{2} s\, \bar{\theta}(0,s) - k \frac{d}{dx}\bar{\theta}(0,s) \tag{16-A}$$

and solving the differential Eq. (14-A) for $\bar{\theta}$:

$$\bar{\theta}(x,s) = C_1(s) \exp\left(\sqrt{\frac{s}{\alpha}}\, x\right) + C_2(s) \exp\left(\sqrt{\frac{s}{\alpha}}\, x\right) \tag{17-A}$$

By the first boundary condition

$$C_1(s) = 0 \tag{18-A}$$

and by the second

$$C_2(s) = \frac{Q\ell}{2s} \frac{1}{k\sqrt{\frac{s}{\alpha}} + (\rho\, c_p)_f \frac{\ell}{2} s} \tag{19-A}$$

and thus

$$\bar{\theta}(x,s) = \frac{Q\ell}{2} \frac{\exp\left(-\sqrt{\frac{s}{\alpha}}\, x\right)}{s\left(k\sqrt{\frac{s}{\alpha}} + (\rho\, c_p)_f \frac{\ell}{2} s\right)} \tag{20-A}$$

This can be inverted to give:

$$\theta(x,t) = \frac{Q}{\alpha(\rho\, c_p)_f b^2} \left\{ \exp(2\nu\eta + \nu^2). \right.$$

$$\operatorname{erfc}(\nu+\eta) - (1 + 2\nu\eta).$$

$$\left. \operatorname{erfc}(\eta) + \frac{2}{\sqrt{\pi}}\, \nu \exp(-\eta^2) \right\} \tag{21-A}$$

where

$$b = \frac{2k}{\alpha(\rho\, c_p)_f \ell} \tag{22-A}$$

$$\eta = \frac{x}{\sqrt{4\alpha t}} \tag{23-A}$$

$$\nu = \frac{bx}{2\eta} \tag{24-A}$$

Using Eq. (9-A) with Eq. (21-A) then gives Eq. (7) in the body of this report.

The values used in plotting Figs. 2 and 3 were obtained by using the series expansions for the exponential and complementary error function and terminating them after the second term. Thus

$$e^{y} \approx 1 + y \tag{25-A}$$

$$\operatorname{erfc}(y) \approx 1 - \frac{2}{\sqrt{\pi}}\, y \tag{26-A}$$

for $y \ll 1$. Hence, at $x = 0$ and for $t < 10^4$ seconds

$$T(x,t) \approx T_o + \frac{Q}{\alpha(\rho\, c_p)_f b^2} \left\{ \nu^2 \left(1 - \frac{2}{\sqrt{\pi}}\, \nu \right) \right\} \tag{27-A}$$

from which Fig. 2 is obtained. For $t < 10^4$ seconds and $x < 2\text{-}1/2$ cm

$$T(x,t) \approx T_o + \frac{Q}{\alpha(\rho\, c_p)_f b^2} \left\{ \frac{2}{\sqrt{\pi}}\, \nu \left[(1-\eta^2) - (1+2\nu\eta)\left(1 - \frac{\sqrt{\pi}}{2}\, \nu + \nu\eta + \nu^2\right) \right] \right\} \tag{28-A}$$

from which Fig. 3 is obtained.

APPENDIX B

SOLUTION OF THE MOMENTUM EQUATION

The momentum equation in the form of (27) is solved for a nuclear reactor configuration as shown in Fig. 4. Using the dimensionless variables (37) to (43), (27) becomes:

$$\frac{dp}{d\xi} = -\frac{G_1^{\,2}}{\rho_1}\,\frac{d\Theta}{d\xi} - \frac{\rho_1 g L}{\Theta}\,\frac{f G_1^{\,2} L \Theta}{2\rho_1\, r_H} \tag{1-B}$$

Integrating Eq. (1-B) over the reactor core ($0 \leq x \leq L$ or $0 \leq \xi \leq 1$):

$$p(1,\tau) - p(0,\tau) = -\frac{G_1^{\,2}}{\rho_1}\left[\Theta(1,\tau) - \Theta(0,\tau)\right]$$
$$-\rho_1\, g\, L \int_0^1 \frac{1}{\Theta}\, d\xi - \frac{G_1^{\,2} L}{2\rho_1 r_H}\int_0^1 f\,\Theta\, d\xi \tag{2-B}$$

In the stack above the core the heat sources and frictional resistance are neglected and so Eq. (1-B) reduces to:

$$\frac{dp}{d\xi} = -\frac{\rho_1\, g\, L}{\Theta(1,\tau)} \tag{3-B}$$

Integrating Eq. (3-B) over the stack ($L \leq x \leq L'$ or $1 \leq \xi \leq R$):

$$p(R,\tau) - p(1,\tau) = -\frac{\rho_1\, g\, L}{\Theta(1,\tau)}\,(R-1) \tag{4-B}$$

where R is defined by (42).

In the external piping, Eq. (1-B) can be written

$$\frac{dp}{dx} = -\rho_1\, g - \frac{f_{ex}\, G_{ex}^{\,2}}{2\rho_1\, r_{H_{ex}}} \tag{5-B}$$

where the dimensionless variables are not used, and upon integrating over the external piping ($-L_p \leq x \leq 0$):

$$p(0,\tau) - p_a = \rho_1\, g\, L' - \frac{f_{ex}\, G_{ex}^{\,2}}{2\rho_1\, r_{H_{ex}}}\, L_p \tag{6-B}$$

where L' in the second term arises from the fact that the buoyant force contributes to the flow only in the vertical sections; also $p(-L_p, \tau) = p_a$ was used. During steady-state operation (6-B) must still apply, and the right-hand side must be equal to the pressure rise across the pump minus the pressure loss across the core. Neglecting the buoyancy term, as most of the loss in this case should be frictional, gives:

$$\Delta p_{pumps} - \Delta p_{core} \approx \frac{f_{ex} G_{ex}^2 L_p}{2 \rho_1 r_{H_{ex}}} = \frac{f G_1^2 L_{eq}}{2 \rho_1 r_H} \tag{7-B}$$

where the third term is definition of L_{eq}; the other values in the third term are core parameters. Now the pressure drop across the core during steady state operation is approximated by:

$$\Delta p_{core} \approx \frac{f G_1^2 L}{2 \rho_1 r_H} \tag{8-B}$$

and thus dividing Eq. (7-B) by Eq. (8-B) gives:

$$\delta = \frac{L_{eq}}{L} = \frac{\Delta p_{pumps} - \Delta p_{core}}{\Delta p_{core}} \tag{9-B}$$

and therefore δ can be calculated by the data in Table III. Equation (6-B) then becomes:

$$p(0,\tau) - p_a = \rho_1 g L' - \frac{f G_1^2 L_{eq}}{2 \rho_1 r_H} \tag{10-B}$$

Now $p(R,\tau) = p_a$ and thus adding Eq. (2-B), (4-B), and (6-B) gives

$$0 = -\frac{G_1^2}{\rho_1}\left[\Theta(1,\tau) - \Theta(0,\tau)\right] - \rho_1 g L \cdot \int_0^1 \frac{1}{\Theta}\, d\xi - \frac{G_1^2 L}{2\rho_1 r_H}\int_0^1 f\,\Theta\, d\xi - \frac{\rho_1 g L}{\Theta(1,\tau)}(R-1) + \rho_1 g L R - \frac{f G_1^2 L\delta}{2 \rho_1 r_H} \tag{11-B}$$

Combining terms and using Eqs. (31, 32 and 36) and Eq. (13-B) below, plus the fact that $\Theta(0,\tau) = 1$ gives

$$\left(\frac{16\, r_H^{\,2}\, \rho_1^{\,2}\, g\, L}{\mu_1^{\,2}}\right) \frac{1}{Re^2(0,\tau)} \left\{ \int_0^1 \frac{1-\Theta(\xi,\tau)}{\Theta(\xi,\tau)}\, d\xi + \frac{1-\Theta(1,\tau)}{\Theta(1,\tau)}\,(R-1) \right\} + \left[\Theta(1,\tau) - 1\right] + \frac{K\,L}{2r_H} \cdot$$

$$\frac{1}{Re^n(0,\tau)} \int_0^1 \left[\Theta^{1+n\ell}(\xi,\tau) + \delta\right] d\xi = 0 \qquad \text{(12-B)}$$

which is identical with Eq. (50). By the continuity equation (22), the Reynolds number at any position in the core is given in terms of its value at the inlet by:

$$Re(\xi,\tau) = \frac{Re(0,\tau)}{\Theta^{\ell}(\xi,\tau)} \qquad \text{(13-B)}$$

APPENDIX C

SOLUTION OF THE COOLANT ENERGY EQUATION

The coolant energy equation (28) using the dimensionless definitions (37) to (43) can be written

$$\frac{c_p\ G_1\ T_1}{L}\ \frac{d\Theta}{d\xi} = \frac{q_w}{r_H'} \tag{1-C}$$

$$q_w = h\ T_1(\Theta_f - \Theta) \tag{2-C}$$

Using Eqs. (32, 34 and 35) gives

$$\frac{d\Theta}{d\xi} + \frac{L}{r_H'}\ \frac{Nu}{Pr\ Re}\ (\Theta_f - \Theta) \tag{3-C}$$

The Stanton number is defined

$$St(\xi,\tau) = \frac{Nu(\xi,\tau)}{Pr\ Re(\xi,\tau)} \tag{4-C}$$

and so Eq. (3-C) becomes

$$\frac{d\Theta}{d\xi} + \frac{L}{r_H'}\ St(\xi,\tau)\Theta = \frac{L}{r_H'}\ St(\xi,\tau)\Theta_f \tag{5-C}$$

Assuming the right-hand side to be known, as it will be if an iterative method of solution is used, Eq. (5-C) can then be solved to yield Eq. (51).

APPENDIX D

SOLUTION OF THE FUEL ENERGY EQUATION

Using the dimensionless definitions (37) to (43), the fuel energy equation (29) can be written

$$\frac{d\Theta_f}{d\tau} = \tilde{Q}(\tau)f(\xi) - \frac{4\ r_H}{k_1 T_1}\ q_w \tag{1-D}$$

where $f(\xi)$ describes the axial power variation and $Q(\tau)$ is the midplane power density. By Eqs. (30,34, and 35),Eq.(1-D) can be written

$$\frac{d\Theta_f}{d\tau} = \tilde{Q}(\tau)f(\xi) - Nu(\Theta_f - \Theta) \tag{2-D}$$

or, with $f(\xi) = \sin(\pi\xi)$

$$\frac{d\Theta_f}{d\tau} + Nu\ \Theta_f = \tilde{Q}(\tau)\ \sin(\pi\xi) + Nu\ \Theta \tag{3-D}$$

If the right-hand side is known, as it will be if an iterative method is used, then Eq. (3-D) can be solved to give Eq. (52).

APPENDIX E

SOLUTION OF THE INITIALIZING EQUATIONS

The solution of the initializing equations is begun with Eqs. (23), (28), and (29) which, when written in dimensionless form, are

$$\frac{dp}{d\xi} = - G_1^{\,2} \frac{d}{d\xi}\left(\frac{1}{\rho}\right) - \rho\, g\, L - \frac{f\, G_1^{\,2} L}{2\rho\, r_H} \tag{1-E}$$

$$c_p\, G_1\, T_1 \frac{d\Theta}{d\xi} = \frac{L}{r_H'}\, q_w \tag{2-E}$$

$$\frac{d\Theta_f}{d\tau} = \tilde{Q}(\tau) f(\xi) - \frac{4\, r_H}{k_1 T_1}\, q_w \tag{3-E}$$

Neglecting the variation of density with temperature in all terms of Eq. (1-E) with the exception of the body force term, and representing it there as

$$\rho = \rho_1 - \beta \rho_1\, T_1 (\Theta - 1) \tag{4-E}$$

gives

$$\frac{dp}{d\xi} = - \rho_1\, g\, L + \rho_1\, g\, L\, \beta\, T_1 (\Theta - 1) - \frac{f\, G_1^{\,2} L}{2\, \rho_1\, r_H} \tag{5-E}$$

Integrating Eq. (5-E) over the core ($0 \leq x \leq L$ or $0 \leq \xi \leq 1$):

$$p(1,\tau) - p(0,\tau) = - \rho_1\, g\, L + \rho_1\, g\, L\, \beta\, T_1 \cdot \int_0^1 (\Theta - 1)\, d\xi - \frac{f\, G_1^{\,2} L}{2\, \rho_1\, r_H} \tag{6-E}$$

Integrating Eq. (5-E) over the stack region ($L \leq x \leq L'$ or $1 \leq \xi \leq R$) and neglecting the frictional resistance therein:

$$p(R,\tau) - p(1,\tau) = - \rho_1\, g\, L\, (R-1) + \rho_1\, g\, L\, \beta\, T_1\, (\Theta(1,\tau) - 1)(R-1) \tag{7-E}$$

If the external piping is neglected then the hydrostatic balance gives:

$$p(R,\tau) - p(0,\tau) = - \rho_1 \, g \, L \, R \tag{8-E}$$

Thus, adding Eqs. (6-E), (7-E), and (8-E) and using the definition of the Reynolds number, Eq. (32) gives:

$$0 = \rho_1 \, g \, L \, \beta \, T_1 \, (\Theta(1-\tau)-1)(R-1) + \rho_1 \, g \, L \, \beta \, T_1 \int_0^1 (\Theta-1) \, d\xi - \frac{K \, \mu_1^{\,2} \, L \, Re^{2-n}}{32 \, \rho_1 \, r_H^{\,3}} \tag{9-E}$$

where viscosity is assumed independent of temperature and the friction factor is given by Eq. (31). Rewriting gives

$$Re^{2-n}(\tau) = A\left[(R-1)\left(\Theta(1-\tau)-1\right) + \int_0^1 (\Theta-1) \, d\xi\right] \tag{10-E}$$

where A is the dimensionless coefficient

$$A = \frac{32 \, \rho_1^{\,2} \, g \, \beta \, T_1 \, r_H^{\,3}}{K \, \mu_1^{\,2}} \tag{11-E}$$

If it is assumed as an approximation in solving Eq. (2-E) that

$$\tilde{Q}(\tau) f(\xi) \approx \frac{4 \, r_H}{k_1 \, T_1} \, q_w \tag{12-E}$$

the effect of which is to assume that all heat generated is transferred to the coolant, then

$$\frac{d\Theta}{d\xi} = B \, \frac{\tilde{Q}(\tau) f(\xi)}{Re(\tau)} \tag{13-E}$$

where B is the dimensionless coefficient

$$B = \frac{k_1 L}{\mu_1 c_p r_H'} \tag{14-E}$$

Integrating gives:

$$\Theta(\xi,\tau) = 1 + \frac{B}{\pi} \frac{\tilde{Q}(\tau)}{Re(\tau)} (1-\cos \pi \xi) \tag{15-E}$$

Using Eqs. (30) and (34), Eq. (3-E) can be written

$$\frac{d\Theta_f}{d\tau} = \tilde{Q}(\tau)f(\xi) - Nu(\tau)(\Theta_f-\Theta) \tag{16-E}$$

Assuming the Nusselt number can be approximated by its initial value:

$$Nu(\tau) \approx Nu(0) \tag{17-E}$$

gives

$$\frac{d\Theta_f}{d\tau} + Nu(0)\ \Theta_f = \tilde{Q}(\tau)f(\xi) + Nu(0)\Theta \tag{18-E}$$

Solving by use of the integrating factor p

$$p = \exp\ (Nu(0)\tau) \tag{19-E}$$

gives:

$$\Theta_f(\xi,\tau) = \left\{\Theta_f(\xi,0) + \int_0^\tau \left[\exp\ (Nu(0)s)\right] \cdot \left[Nu(0)\ \Theta(\xi,s) + \tilde{Q}(s)f(\xi)\right] ds\right\} \cdot \left[\exp\ (-Nu(0)\tau)\right] \tag{20-E}$$

By Eq. (15-E), Eq. (10-E) can be reduced to:

$$Re(\tau) = \left[A \cdot B\left(\frac{2R-1}{\pi}\right) \tilde{Q}(\tau)\right]^{1/(3-n)} \tag{21-E}$$

With $f(\xi) = \sin(\pi\xi)$, Eqs. (21-E), (15-E), and (20-E) coincide with Eqs. (56), (57), and (58).

APPENDIX F

CONSEQUENCES OF THE CONSTANT RADIAL POWER ASSUMPTION

It is important to know if the assumption of a constant radial power density results in fuel temperatures which are conservative. A simplified model can be used to determine if this is true. Consider the parallel flow path system consisting of two fuel elements with a common inlet length as illustrated in Fig. 1-F. An analysis identical to that in the body of this report will be used on this system. The only differences will be that the viscosity is considered constant, the system is a steady state one, and that the volumetric heat generation in either element is a constant, though not necessarily equal. The conservation equations applicable to this system are:

$$\frac{dG}{dx} = 0 \tag{1-F}$$

$$\frac{d}{dx}\left(\frac{G^2}{\rho}\right) = -\frac{dp}{dx} - \rho g - \frac{f\,G^2}{2\rho r_H} \tag{2-F}$$

$$\frac{d}{dx}(GH) = \frac{q_w}{r_H'} \tag{3-F}$$

$$q_w = Q\,r_H'\left(\frac{A_f}{A_F}\right) \tag{4-F}$$

$$q_w = h(T_f - T) \tag{5-F}$$

$$Nu = 0.023\,Pr^{0.4}Re^{0.8} \tag{6-F}$$

The ideal gas law (25) will also be used and the following dimensionless quantities defined:

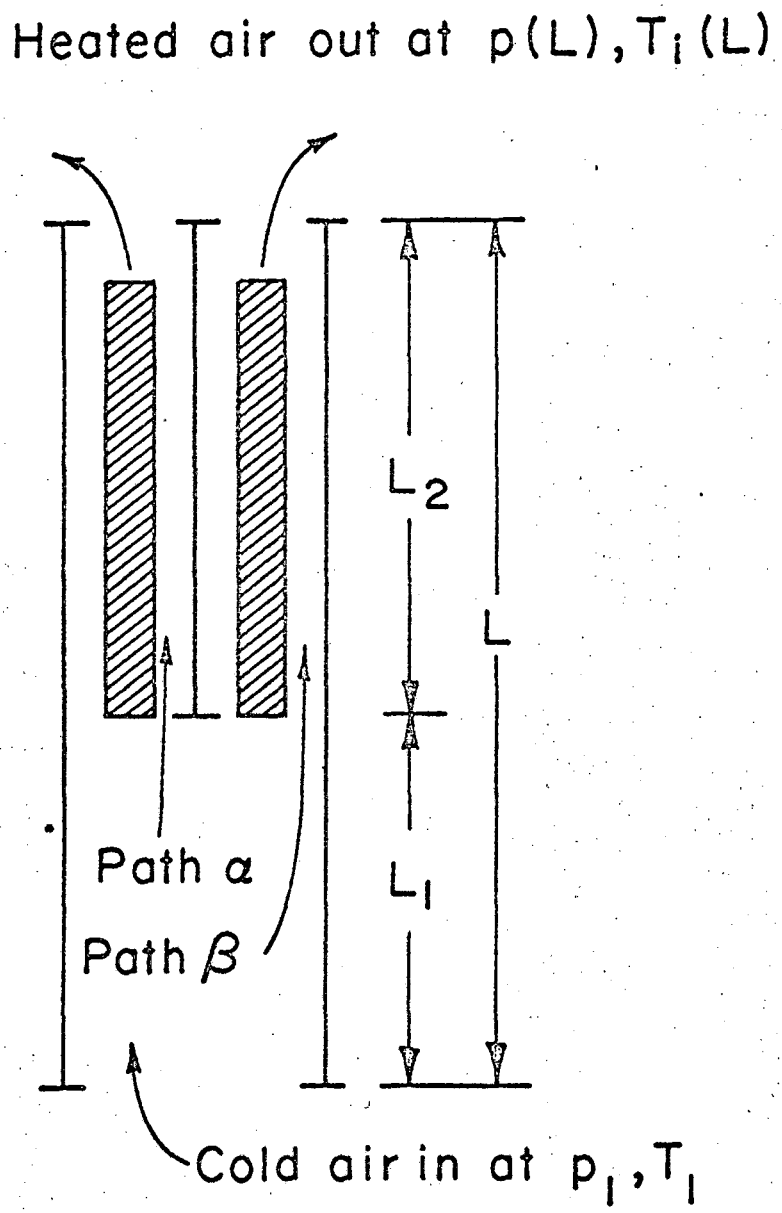

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Fig. 1-F. Parallel-flow path system used to evaluate the consequences of the constant-radial-power assumption.

$$a = \frac{A_F}{A_F} \tag{7-F}$$

$$\Theta = \frac{T - T_1}{T_1} \tag{8-F}$$

$$\tilde{G} = \frac{G}{\rho_1 \sqrt{L_2 g}} \tag{9-F}$$

$$\tilde{Q} = \frac{a \sqrt{L_2}\, Q}{\rho_1\, c_p\, T_1 \sqrt{g}} \tag{10-F}$$

Also, for simplicity, the left-hand path will be considered identical to the right-hand path in all aspects except that the heat generation rates may be controlled independently. The conditions imposed by the system on the conservation equations are:

$$p_\alpha(L_1) = p_\beta(L_1) = p(L_1) \tag{11-F}$$

$$p_\alpha(L) = p_\beta(L) = p(L) \tag{12-F}$$

$$p(L) = p_1 - \rho_1\, g\, L \tag{13-F}$$

$$G_1 = \frac{1}{2}\,(G_\alpha + G_\beta) \tag{14-F}$$

where α denotes the left-hand path and β denotes the right-hand path. The solution of the momentum equation can be shown to be in the heated region:

$$p_i(L) - p_i(L_1) = \frac{a\, L_2\, G_i\, Q_i}{\rho_1\, T_1\, c_p} - \frac{\rho_1\, T_1\, c_p\, g\, G_i}{a\, Q_i} \cdot$$

$$\ln\left(1 + \frac{a\, L_2\, Q_i}{T_1\, c_p\, G_i}\right) - \frac{f_i\, L_2\, G_i^{\,2}}{2\rho_1\, r_H}\left(1 + \frac{a\, L_2\, Q_i}{2\, c_p\, T_1\, G_i}\right) \tag{15-F}$$

where $i = \alpha, \beta$ and in the inlet region:

$$p(L_1) - p_1 = - \rho_1 \, g \, L_1 - \frac{f_1 \, L_{eq} \, G_1^{\,2}}{2 \, \rho_1 \, r_H} \tag{16-F}$$

The solution of the energy equation is:

$$T_i(L) - T_1 = \frac{a \, Q_i \, L_2}{c_p \, G_i} \tag{17-F}$$

The fuel temperature is given by:

$$T_{f_i} = \frac{4 \, r_H \, r_H' \, a \, Q_i}{k \, Nu} + T_i \tag{18-F}$$

Applying the conditions (11-F) through (14-F) to (15-F) and (16-F) and using the dimensionless definitions gives:

$$0 = (\tilde{G}_\alpha \tilde{Q}_\alpha - \tilde{G}_\beta \tilde{Q}_\beta) + \left[\frac{\tilde{G}_\alpha}{\tilde{Q}_\alpha} \operatorname{Ln} \left(1 + \frac{\tilde{Q}_\alpha}{\tilde{G}_\alpha}\right) - \frac{\tilde{G}_\beta}{\tilde{Q}_\beta} \operatorname{Ln} \left(1 + \frac{\tilde{Q}_\beta}{\tilde{G}_\beta}\right) \right] + A \left[\tilde{G}_\alpha^{1.8} \left(1 + \frac{\tilde{Q}_\alpha}{\tilde{G}_\alpha}\right) - \tilde{G}_\beta^{1.8} \left(1 + \frac{\tilde{Q}_\beta}{\tilde{G}_\beta}\right) \right] \tag{19-F}$$

$$1 = B(G_\alpha + G_\beta)^{1.8} + \frac{1}{2} \left\{ \left(\tilde{G}_\alpha \tilde{Q}_\alpha + \tilde{G}_\beta \tilde{Q}_\beta\right) + \left[\frac{\tilde{G}_\alpha}{\tilde{Q}_\alpha} \operatorname{Ln} \left(1 + \frac{\tilde{Q}_\alpha}{\tilde{G}_\alpha}\right) + \frac{\tilde{G}_\beta}{\tilde{Q}_\beta} \operatorname{Ln} \left(1 + \frac{\tilde{Q}_\beta}{\tilde{G}_\beta}\right) \right] + A \left[\tilde{G}_\alpha^{1.8} \left(1 + \frac{\tilde{Q}_\alpha}{\tilde{G}_\alpha}\right) + \tilde{G}_\beta^{1.8} \left(1 + \frac{\tilde{Q}_\beta}{\tilde{G}_\beta}\right) \right] \right\} \tag{20-F}$$

where A and B are the dimensionless coefficients:

$$A = \frac{L_2 \; C_1}{2 \; r_H} \tag{21-F}$$

$$B = \frac{L_{eq} \; C_1}{8 \; r_H} \tag{22-F}$$

$$C_1 = K \left(\frac{4 \; r_H \; \rho_1 \sqrt{L_2 \; g}}{\mu_1} \right)^{-0.2} \tag{23-F}$$

The coolant energy equation in dimensionless form is:

$$\Theta_i = \frac{\tilde{Q}_i}{\tilde{G}_i} \tag{24-F}$$

and the fuel energy equation is:

$$\Theta_{f_i} = \Theta_i + \frac{D \; \tilde{Q}}{Nu} \tag{25-F}$$

where D is the dimensionless coefficient

$$D = \frac{4 \; r_H \; r_H' \rho_1 c_p \sqrt{g}}{k \; \sqrt{L_2}} \tag{26-F}$$

A solution of equations (19-F) to (26-F) does not appear to be readily available; however, the equations can be simplified by considering two cases:

$$\text{Case I:} \quad \tilde{Q}_x = \tilde{Q}_\beta \equiv \tilde{Q}_\alpha \tag{27-F}$$

implying

$$\tilde{G}_\alpha = \tilde{G}_\beta \equiv \tilde{G} \tag{28-F}$$

$$\Theta_\alpha = \Theta_\beta \equiv \Theta \tag{29-F}$$

$$\Theta_{f_\alpha} = \Theta_{f_\beta} \equiv \Theta_f \tag{30-F}$$

$$\text{Case II:} \quad \tilde{Q}_\alpha = \tilde{Q}, \; \tilde{Q}_\beta = 0 \tag{31-F}$$

For Case I the equations then reduce to:

$$1 = (A + 2^{1.8}B)\,\tilde{G}^{1.8} + \left[\tilde{Q} + \frac{1}{\tilde{Q}}\,\mathrm{Ln}\cdot\left(1 + \frac{\tilde{Q}}{\tilde{G}}\right)\right]\tilde{G} + A\,\tilde{Q}\,\tilde{G}^{0.8} \tag{32-F}$$

$$\Theta = \frac{\tilde{Q}}{\tilde{G}} \tag{33-F}$$

$$\Theta_f = \Theta + \frac{D\tilde{Q}}{\mathrm{Nu}} \tag{34-F}$$

For Case II we have:

$$1 = \left[A + (1 + f)^{1.8}\,B\right]\tilde{G}_\alpha^{\,1.8} + \left[\tilde{Q} + \frac{1}{\tilde{Q}}\,\mathrm{Ln}\cdot\left(1 + \frac{\tilde{Q}}{\tilde{G}_\alpha}\right)\right]\tilde{G}_\alpha + A\,\tilde{Q}\,\tilde{G}_\alpha^{\,0.8} \tag{35-F}$$

where

$$f = \frac{\tilde{G}_\beta}{\tilde{G}_\alpha} \tag{36-F}$$

$$0 = A\left(1 + \frac{\tilde{Q}}{\tilde{G}_\alpha} - f^{1.8}\right)\tilde{G}_\alpha^{\,1.8} + \left[\frac{1}{\tilde{Q}}\,\mathrm{Ln}\cdot\left(1 + \frac{\tilde{Q}}{\tilde{G}_\alpha}\right) - f\right]\tilde{G}_\alpha + \tilde{Q}\,\tilde{G}_\alpha \tag{37-F}$$

$$\Theta_\alpha = \frac{\tilde{Q}}{\tilde{G}_\alpha} \tag{38-F}$$

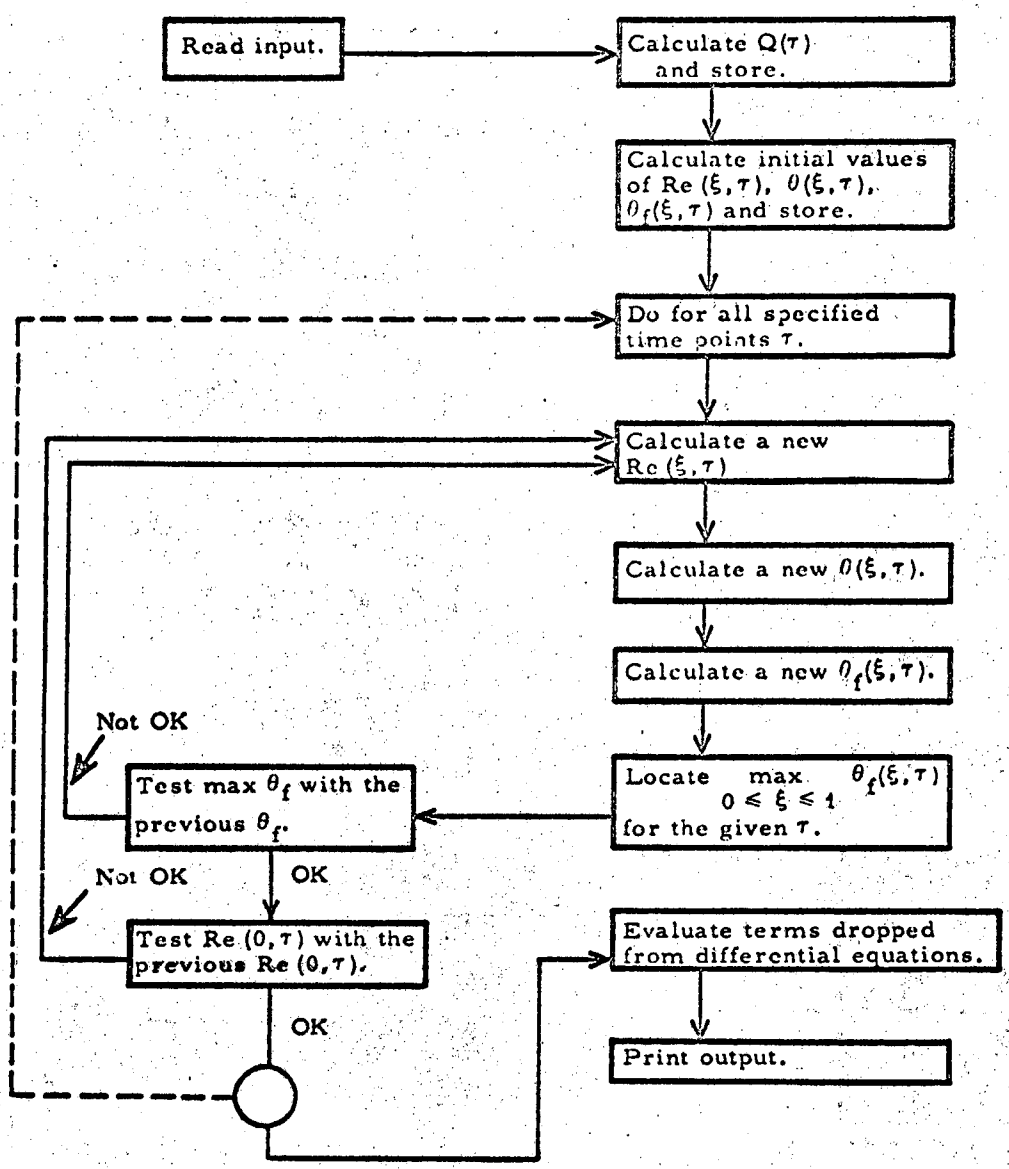
$$\Theta_{f_\alpha} = \Theta_\alpha + \frac{D\tilde{Q}}{\mathrm{Nu}} \tag{39-F}$$

If the assumption of constant radial power density is conservative, then Θ_{f_α} should be less than Θ_f. But again there appears to be no easy method of determination if this is true. Possibly the best approach would be to solve the Eqs. (32-F) to (39-F) on a machine for a sequence of values of A, B, C_1, D and $\tilde{Q}$.

APPENDIX G

THE MACHINE PROGRAM

Outline of Program



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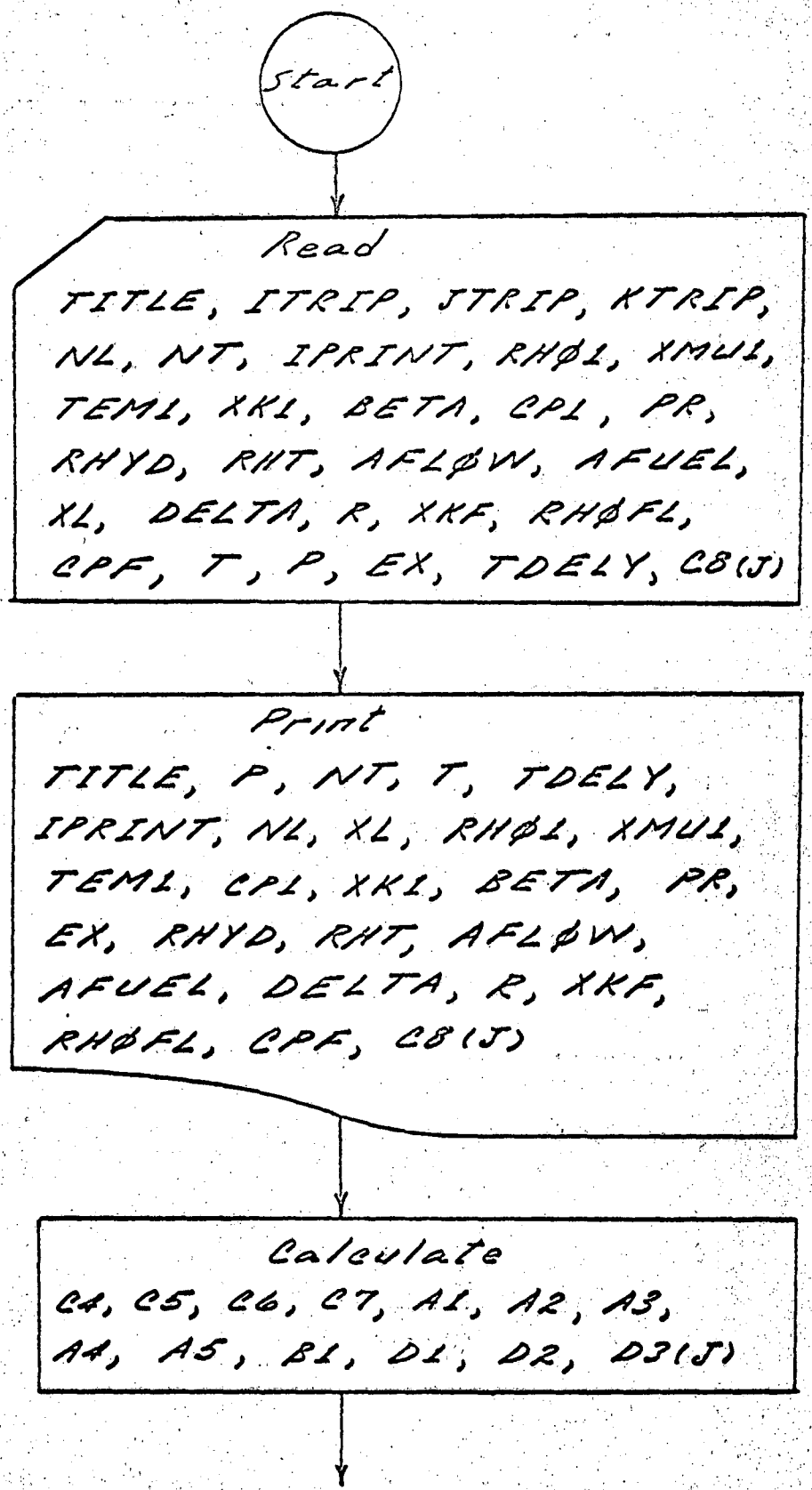
Program Nomenclature

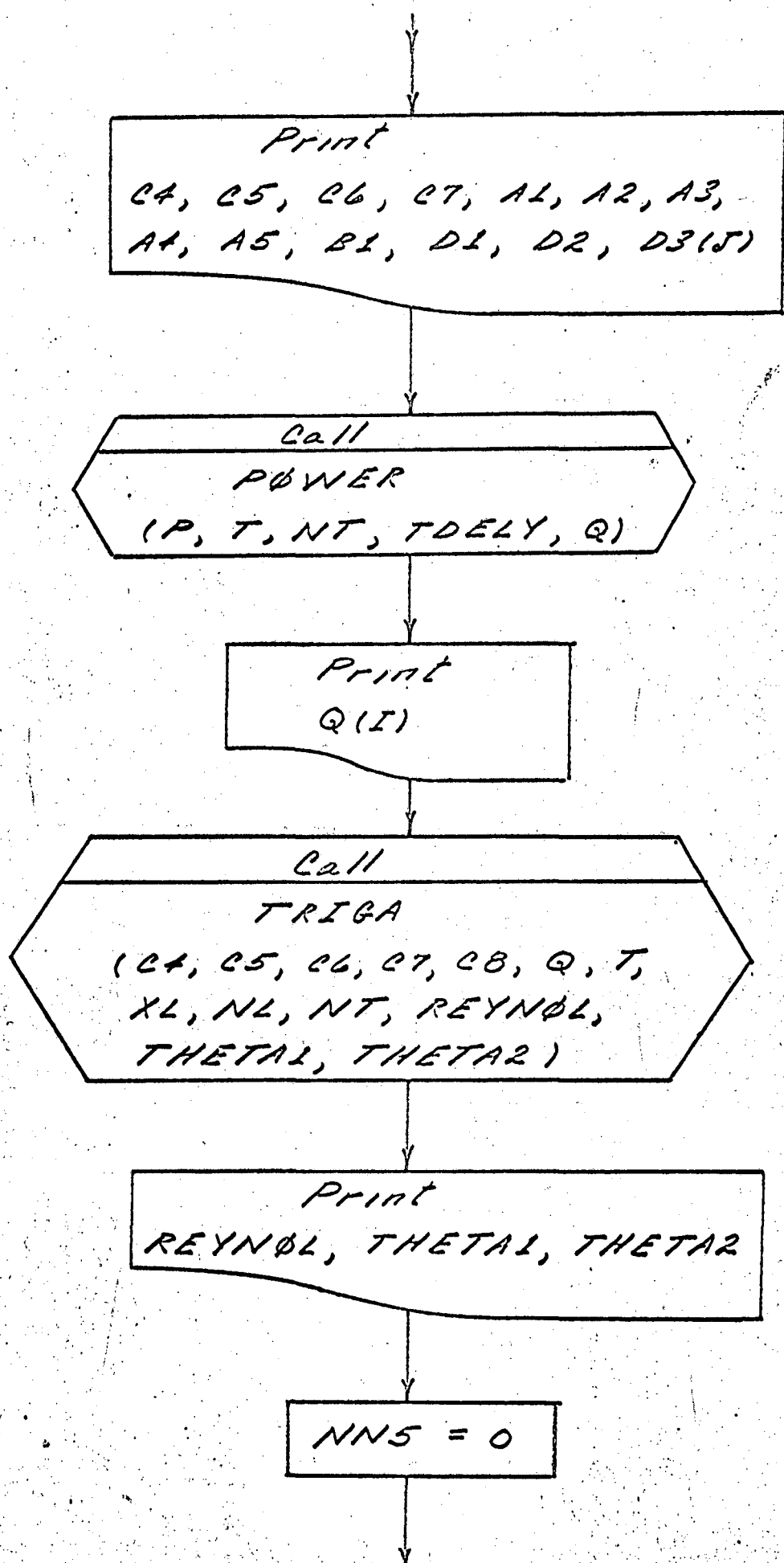
AFLOW:	Cross-sectional area of coolant, A_F
AFUEL:	Cross-sectional area of fuel, A_f
BETA:	Coefficient of thermal expansion, β
C8(J):	Initial fuel temperature, $\Theta_f(\xi)$
CP1:	Specific heat of coolant at inlet, c_{p_1}
CPF:	Specific heat of fuel, c_{p_f}
DELTA:	Ratio of L_{eq} to L, δ
EX:	Exponent in the viscosity-temperature relation, ℓ
FR1:	Coefficient in the friction factor relation
FR2:	Exponent in the friction factor relation
ICHEK:	A parameter determining whether the turbulent or laminar heat transfer correlation is used; zero if laminar, one if turbulent
IPRINT:	Variable controlling quantity of output printed
ITRIP:	Variable controlling quantity of output printed
JTRIP:	Variable controlling amount of output printed
KTRIP:	Variable controlling amount of output printed
NL:	One plus the number of length intervals into which core length XL is divided
NT:	One plus the number of time intervals into which the transient duration T is divided
P:	Operating power density
PR:	Prandtl number, Pr
Q:	Volumetric decay heat generation rate, Q
R:	Ratio of core plus stack height to core height, R

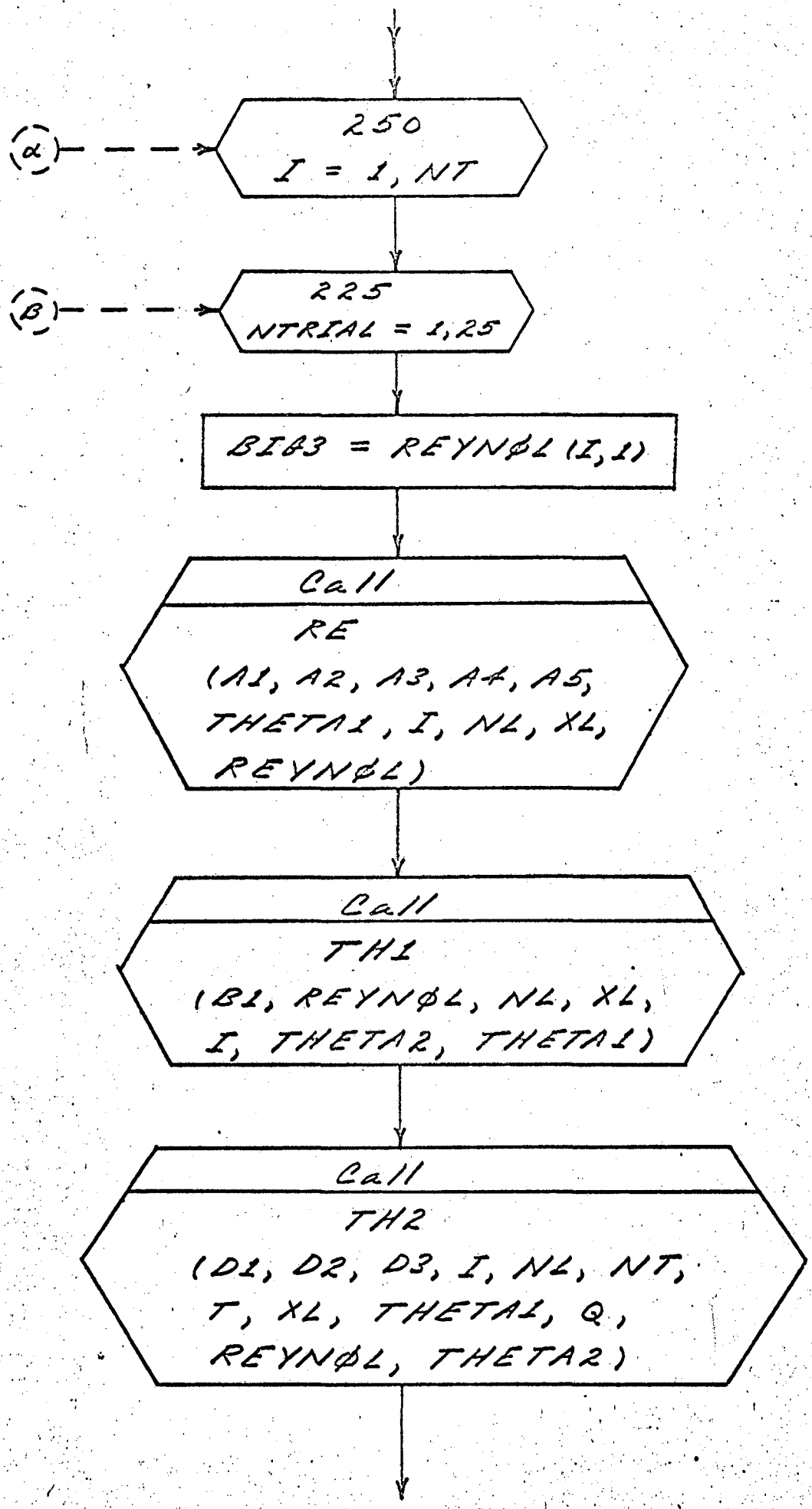
REYNOL:	Reynolds number, Re
RHO1:	Coolant density at inlet, ρ_1
RHOFL:	Fuel density, ρ_f
RHT:	Heated hydraulic radius, r_H'
RHYD:	Hydraulic radius, r_H
ST:	Stanton number, St
T:	Time span over which temperature transient is to be evaluated
TEM1:	Coolant temperature at inlet, T_1
TDELY:	Delay time between reactor shutdown and initiation of the temperature transient, i.e. blowdown time
THETA1:	Dimensionless coolant temperature, Θ
THETA2:	Dimensionless fuel temperature, Θ_f
TITLE:	Job title of run
XK1:	Thermal conductivity of coolant at inlet, k_1
XKF:	Thermal conductivity of fuel, k_f
XL:	Length of core, L
XMU1:	Coolant viscosity at inlet, μ_1
XNU:	Nusselt number

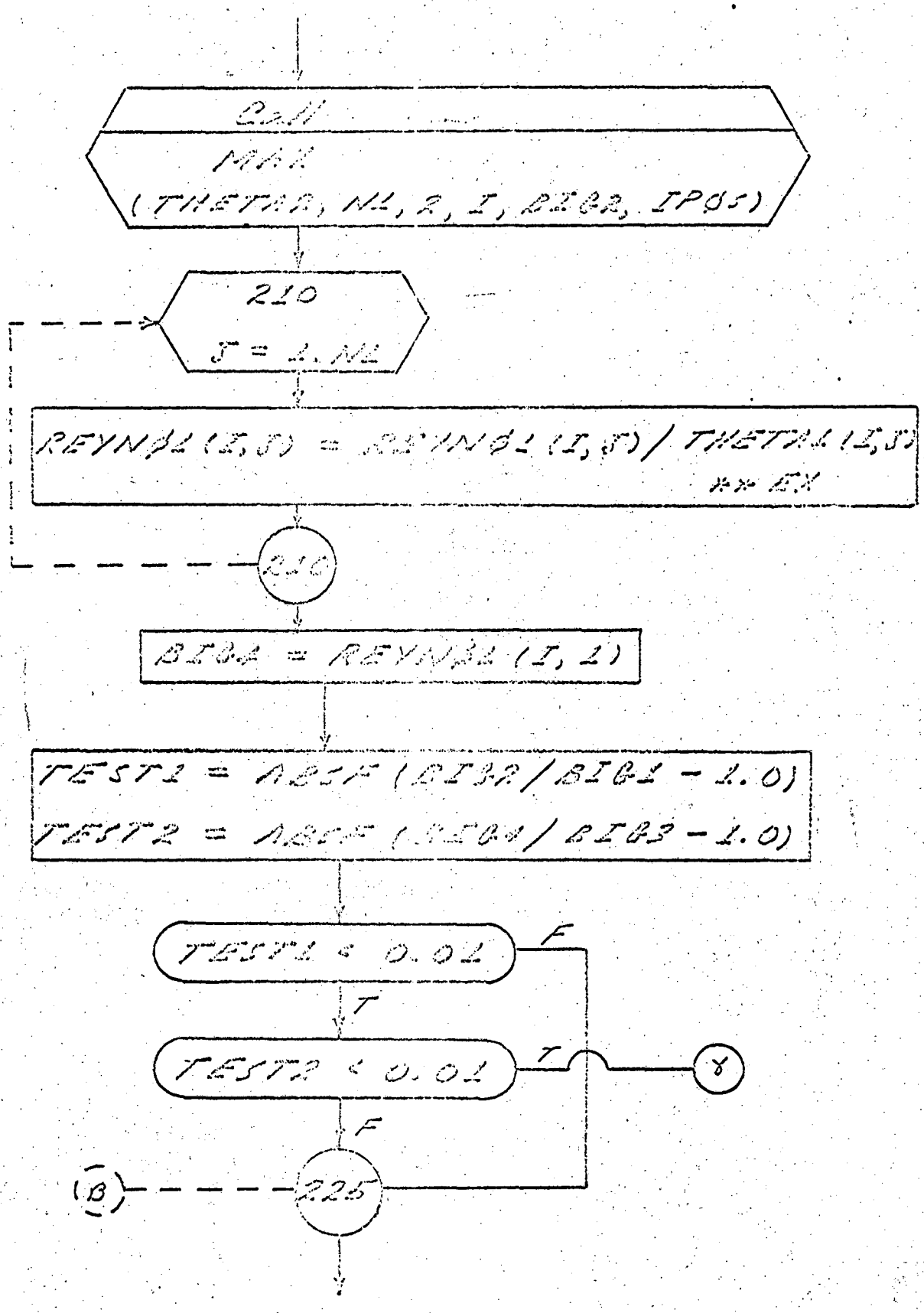
All values must be in the CGS system for use in the program. All other variables used in the program are defined in terms of these within the program.

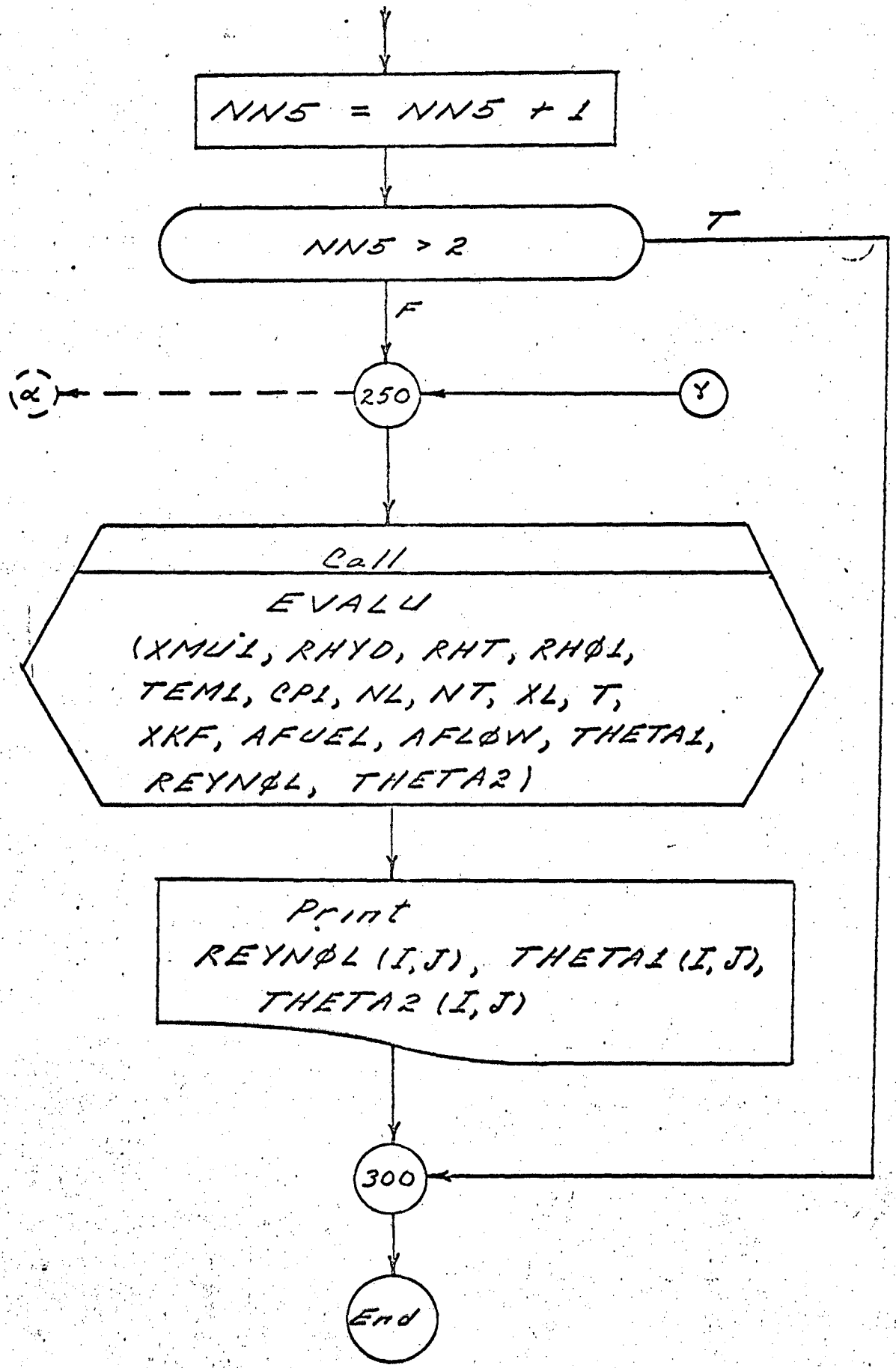
FLOW DIAGRAM

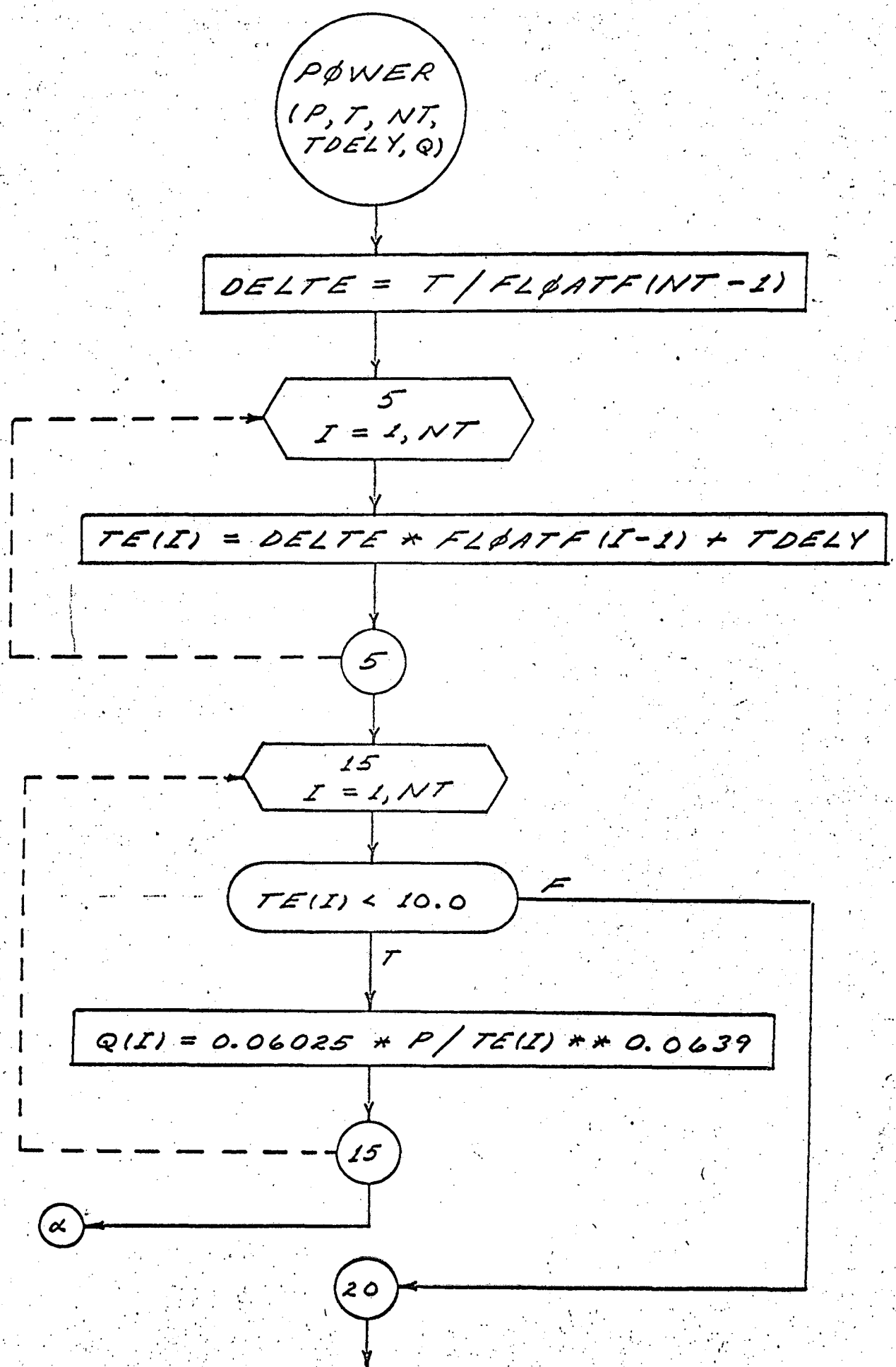


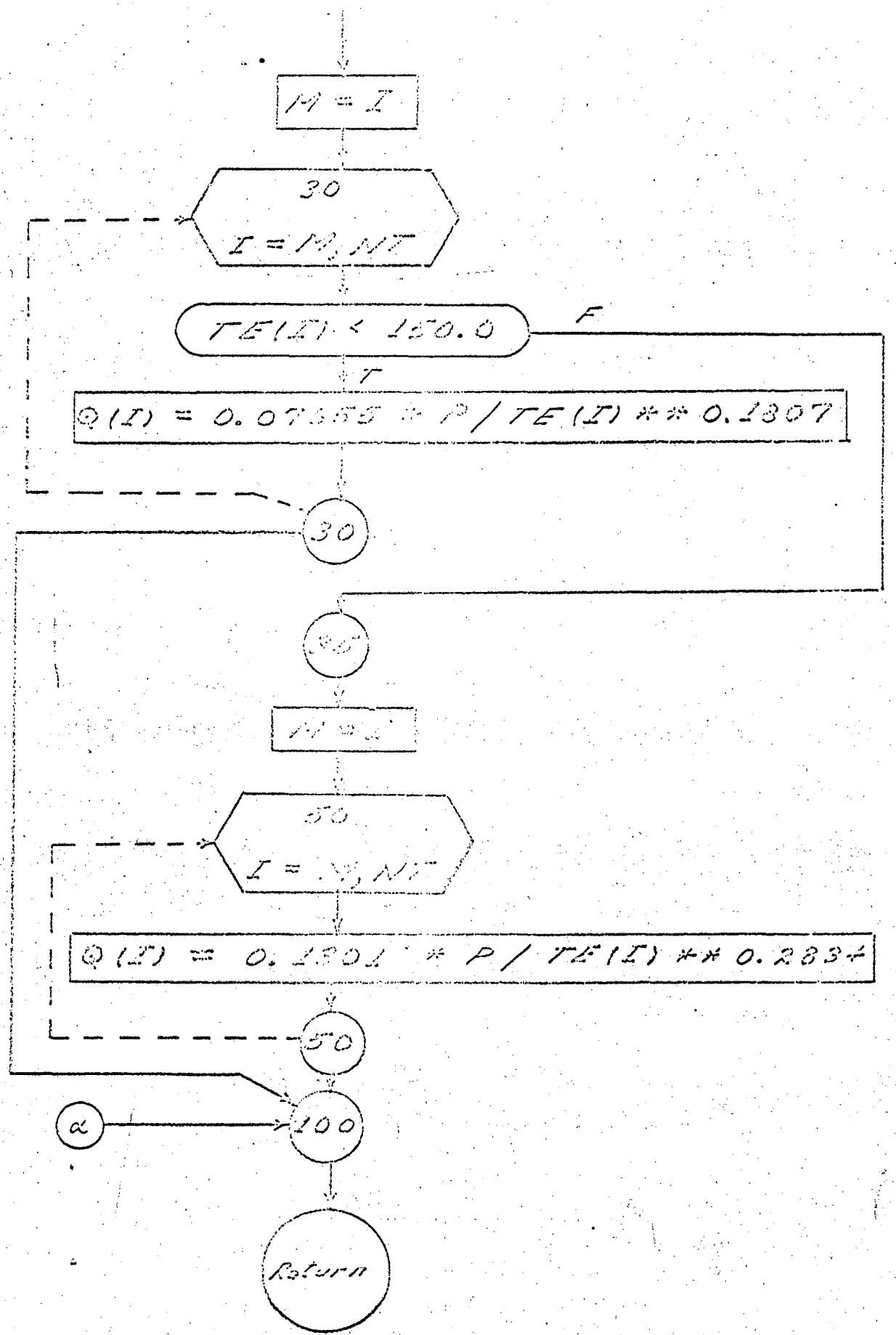

Print
C4, C5, C6, C7, A1, A2, A3,
A4, A5, B1, D1, D2, D3(5)
Call
PØWER
(P, T, NT, TDELY, Q)
Print
Q(I)
Call
TRIGA
(C4, C5, C6, C7, C8, Q, T,
XL, NL, NT, REYNØL,
THETA1, THETA2)
Print
REYNØL, THETA1, THETA2
NN5 = 0

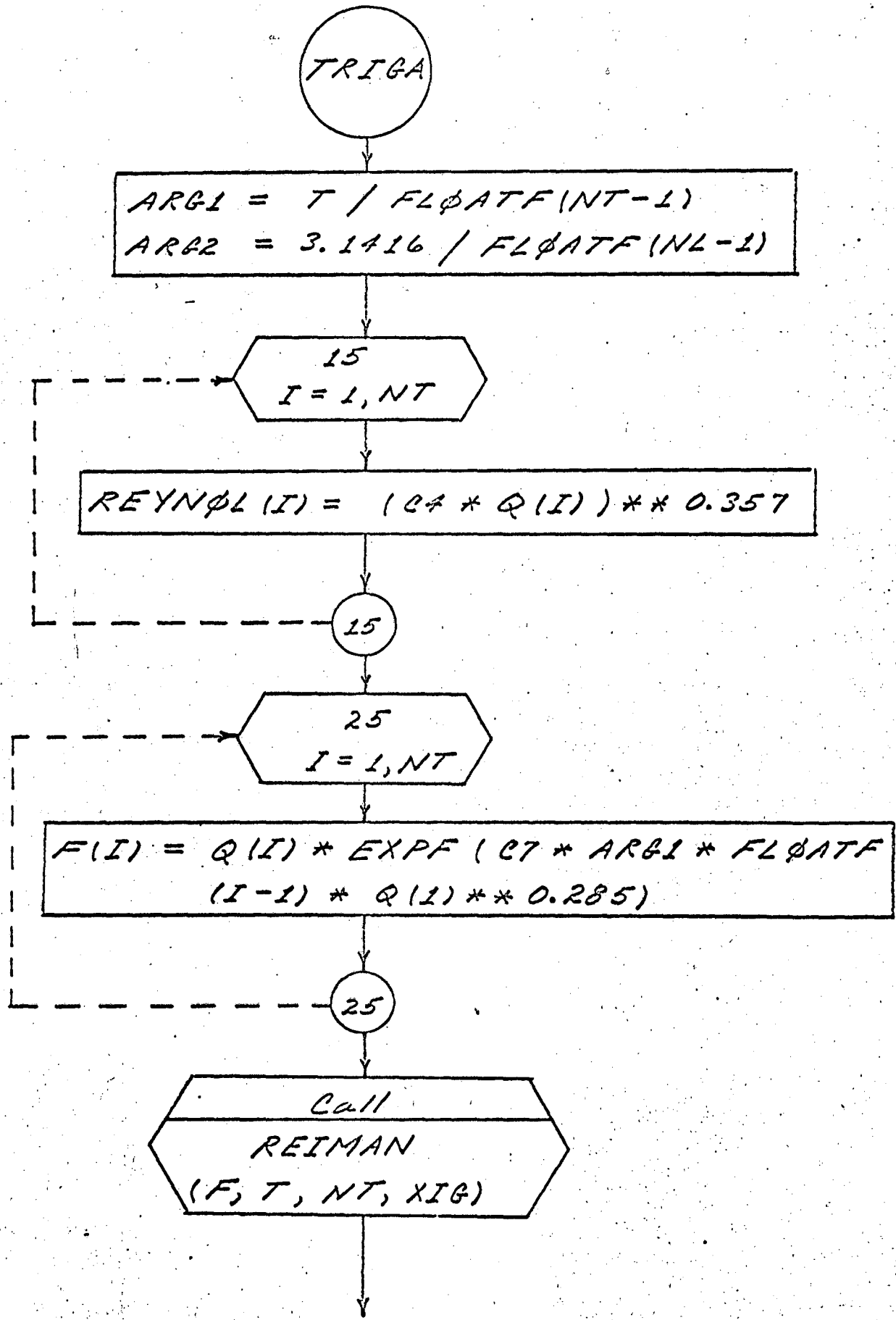

α
250
I = 1, NT
β
225
NTRIAL = 1,25
BIB3 = REYNØL (I,1)
Call
RE
(A1, A2, A3, A4, A5,
THETA1, I, NL, XL,
REYNØL)
Call
TH1
(B1, REYNØL, NL, XL,
I, THETA2, THETA1)
Call
TH2
(D1, D2, D3, I, NL, NT,
T, XL, THETA1, Q,
REYNØL, THETA2)

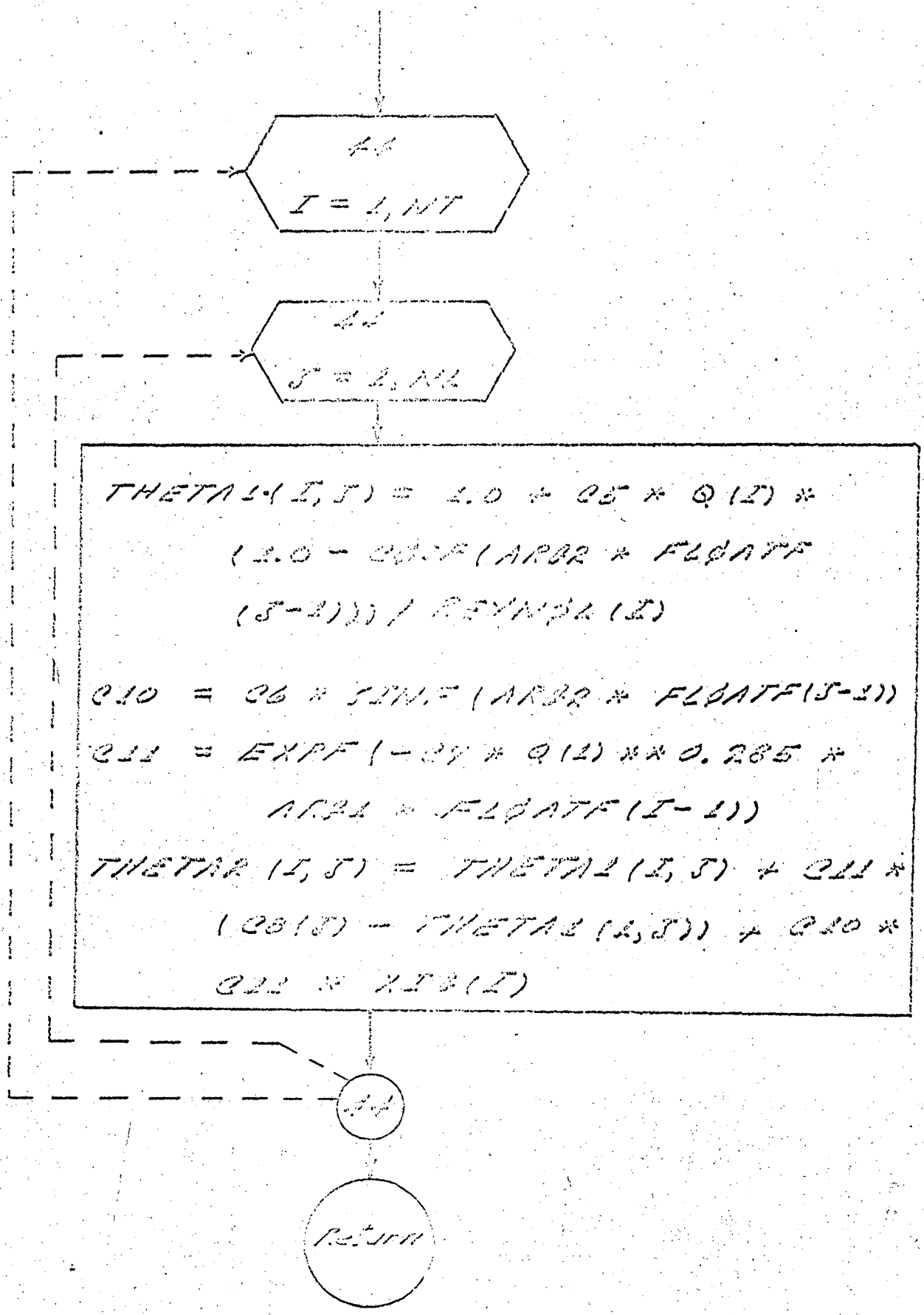

CALL
MAX
(THETAR, NL, R, I, BIBR, IPOS)
210
J = 1, NL
REYNBL (I, J) = REYNBL (I, J) / THETAR (I, J) ** EX
210
BIBA = REYNBL (I, 1)
TEST1 = ABSF (BIBR / BIBA - 1.0)
TEST2 = ABSF (BIBA / BIBB - 1.0)
TEST1 < 0.01
F
T
TEST2 < 0.01
T
γ
F
B
225

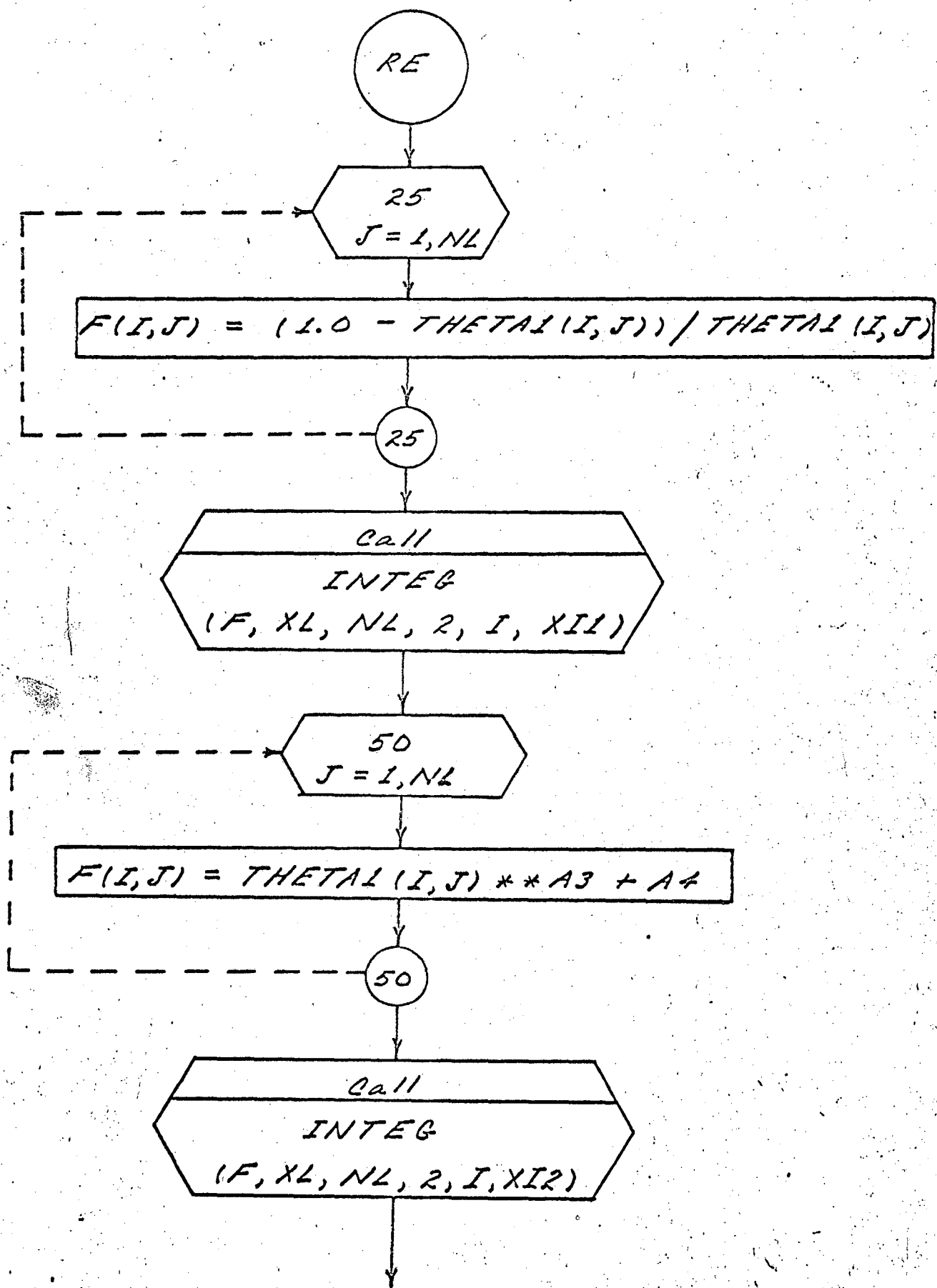

NN5 = NN5 + 1
NN5 > 2
T
F
250
α
γ
Call
EVALU
(XMUIL, RHYD, RHT, RHØL,
TEML, CPL, NL, NT, XL, T,
XKF, AFUEL, AFLØW, THETA1,
REYNØL, THETA2)
Print
REYNØL (I,J), THETA1 (I,J),
THETA2 (I,J)
300
End

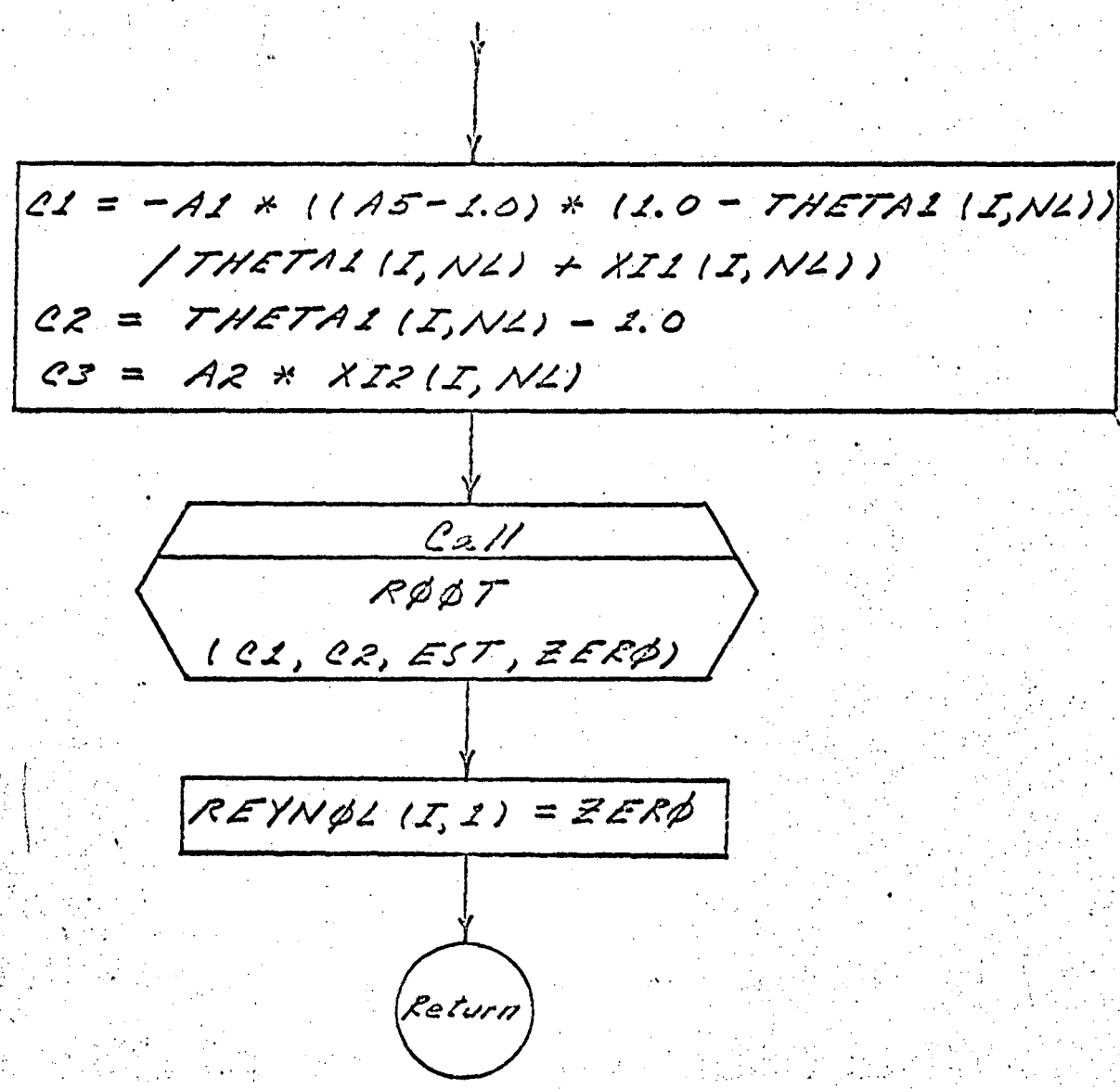

PØWER
(P, T, NT,
TDELY, Q)
DELTE = T / FLØATF(NT-1)
5
I = 1, NT
TE(I) = DELTE * FLØATF(I-1) + TDELY
5
15
I = 1, NT
TE(I) < 10.0
F
T
Q(I) = 0.06025 * P / TE(I) ** 0.0639
15
α
20

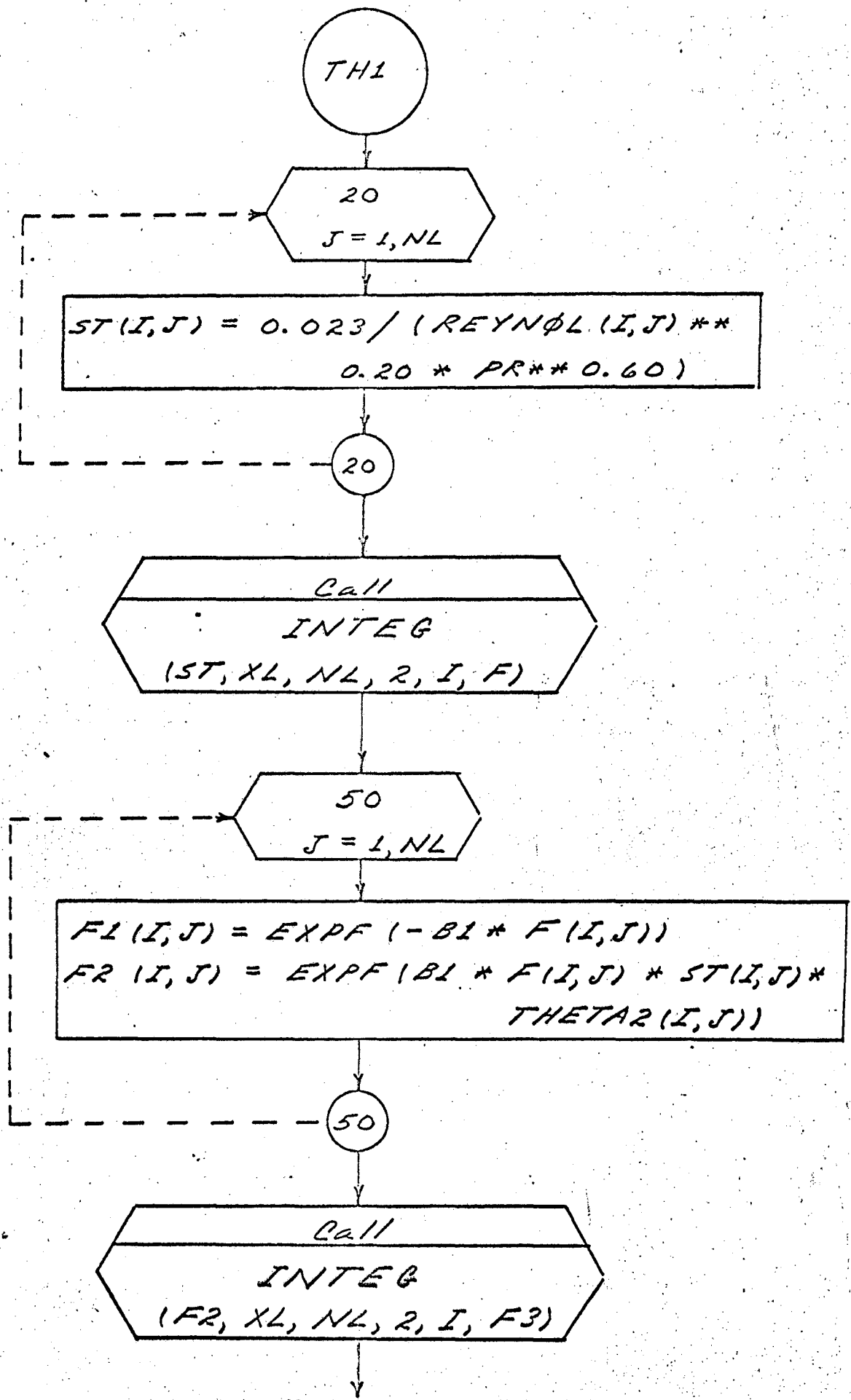

M = I
30
I = M, NT
TE(I) < 150.0
F
T
Q(I) = 0.07355 * P / TE(I) ** 0.1307
30
M = 1
50
I = M, NT
Q(I) = 0.1501 * P / TE(I) ** 0.2834
50
α
100
Return

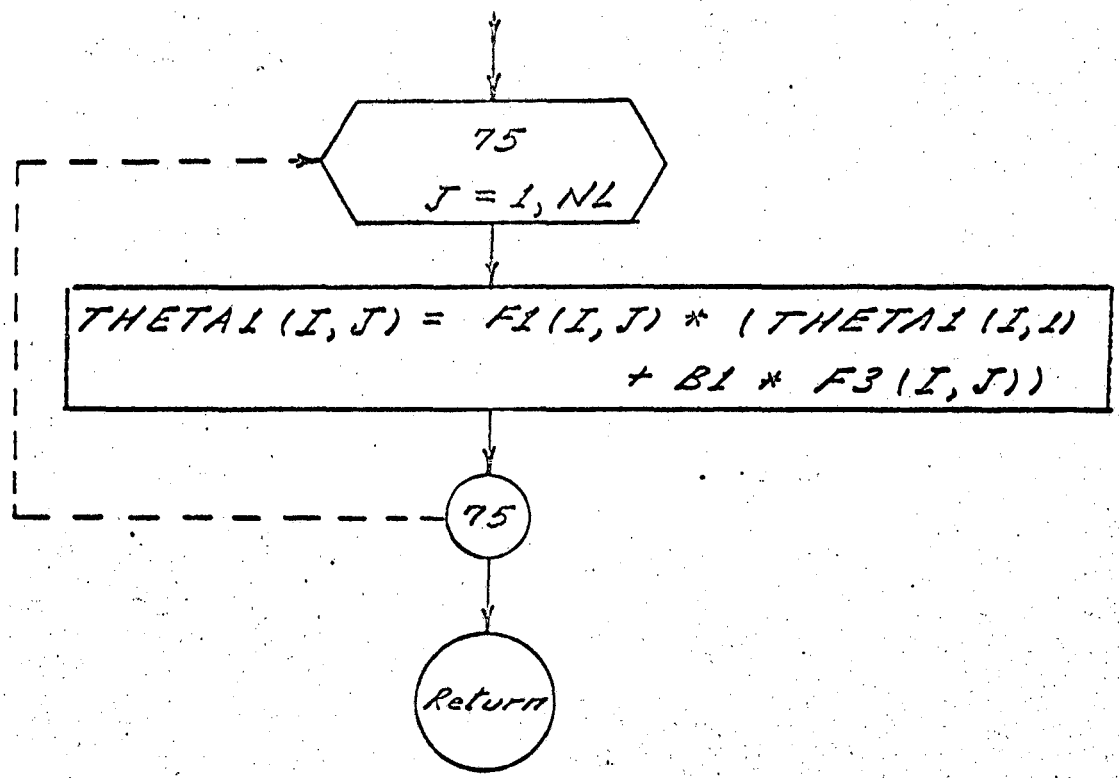

TRIGA
ARG1 = T / FLØATF(NT-1)
ARG2 = 3.1416 / FLØATF(NL-1)
15
I = 1, NT
REYNØL(I) = (C4 * Q(I))** 0.357
15
25
I = 1, NT
F(I) = Q(I) * EXPF (C7 * ARG1 * FLØATF (I-1) * Q(1)** 0.285)
25
Call
REIMAN
(F, T, NT, XIG)

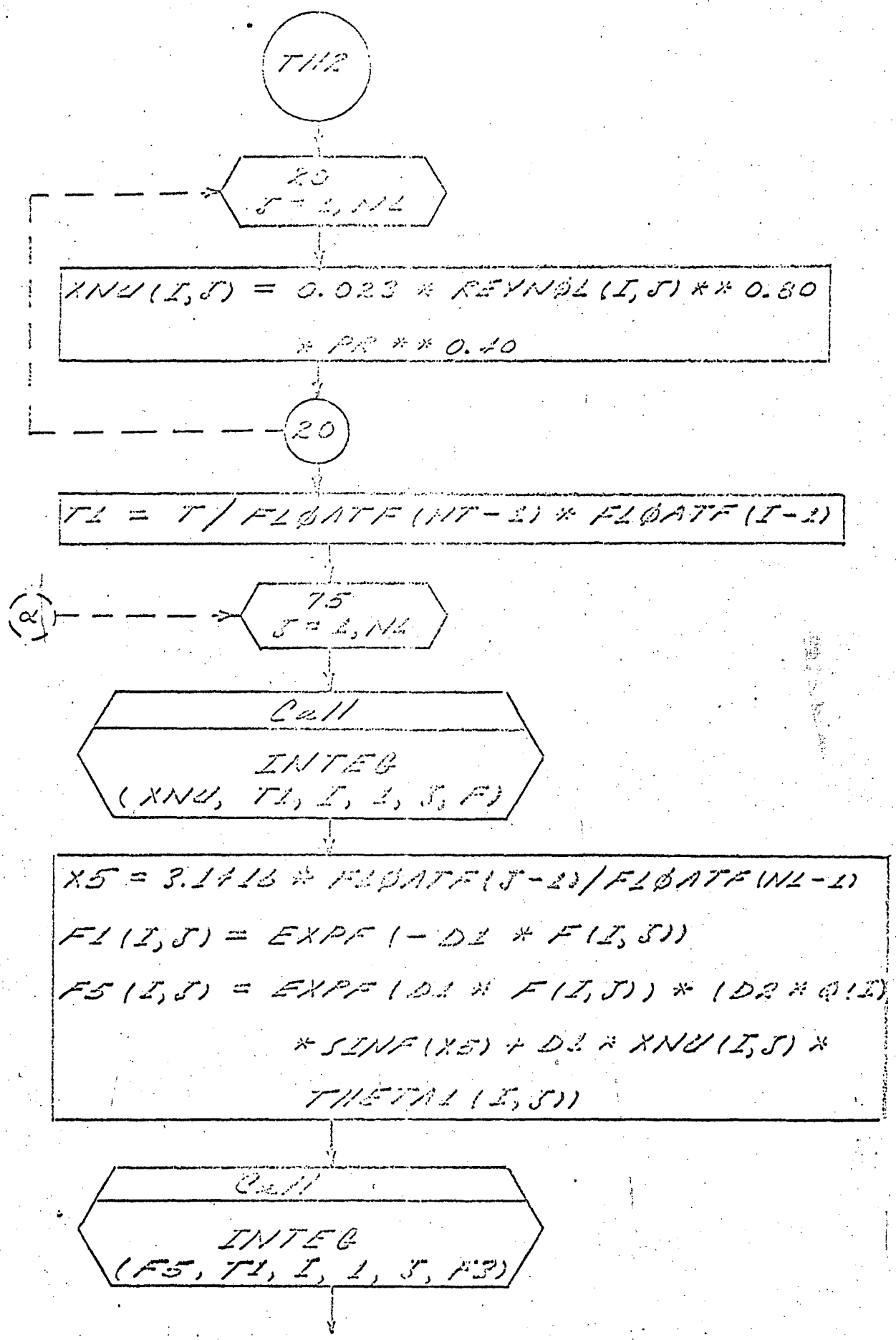

DO
I = 1, NT
DO
J = 2, NL
THETA1(I, J) = 1.0 + C5 * Q(I) *
(1.0 - COSF(ARR2 * FLOATF
(J-1))) / RSYNB2(I)
C10 = C6 * SINF(ARR2 * FLOATF(J-1))
C11 = EXPF(-C9 * Q(I) ** 0.285 *
ARR1 * FLOATF(I-1))
THETA2(I, J) = THETA1(I, J) + C11 *
(C8(I) - THETA1(I, J)) + C10 *
C11 * XTB(I)
CONTINUE
Return

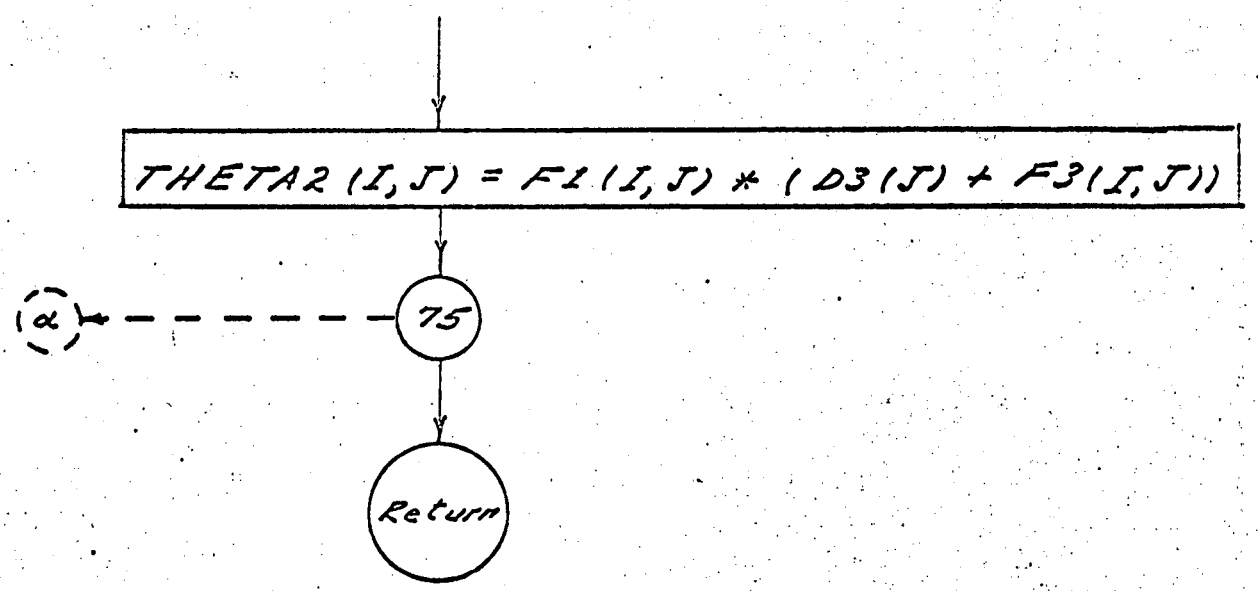

RE
25
J = 1, NL
F(I,J) = (1.0 - THETA1(I,J)) / THETA1(I,J)
25
Call
INTEG
(F, XL, NL, 2, I, XI1)
50
J = 1, NL
F(I,J) = THETA1(I,J) ** A3 + A4
50
Call
INTEG
(F, XL, NL, 2, I, XI2)

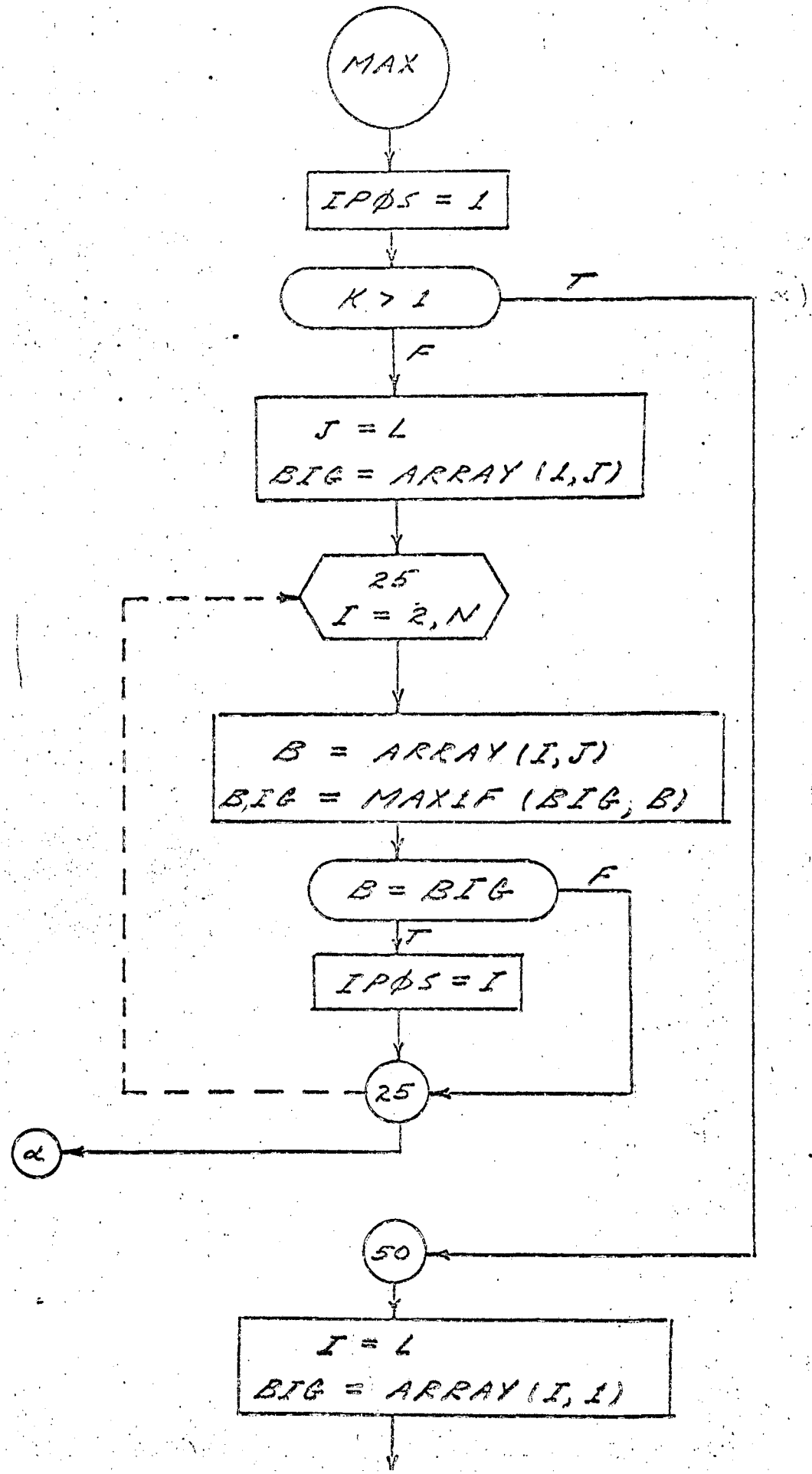

C1 = -A1 * ((A5-1.0) * (1.0 - THETA1 (I,NL))
/THETA1 (I,NL) + XI1 (I,NL))
C2 = THETA1 (I,NL) - 1.0
C3 = A2 * XI2 (I,NL)
Call
RØØT
(C1, C2, EST, ZERØ)
REYNØL (I,1) = ZERØ
Return

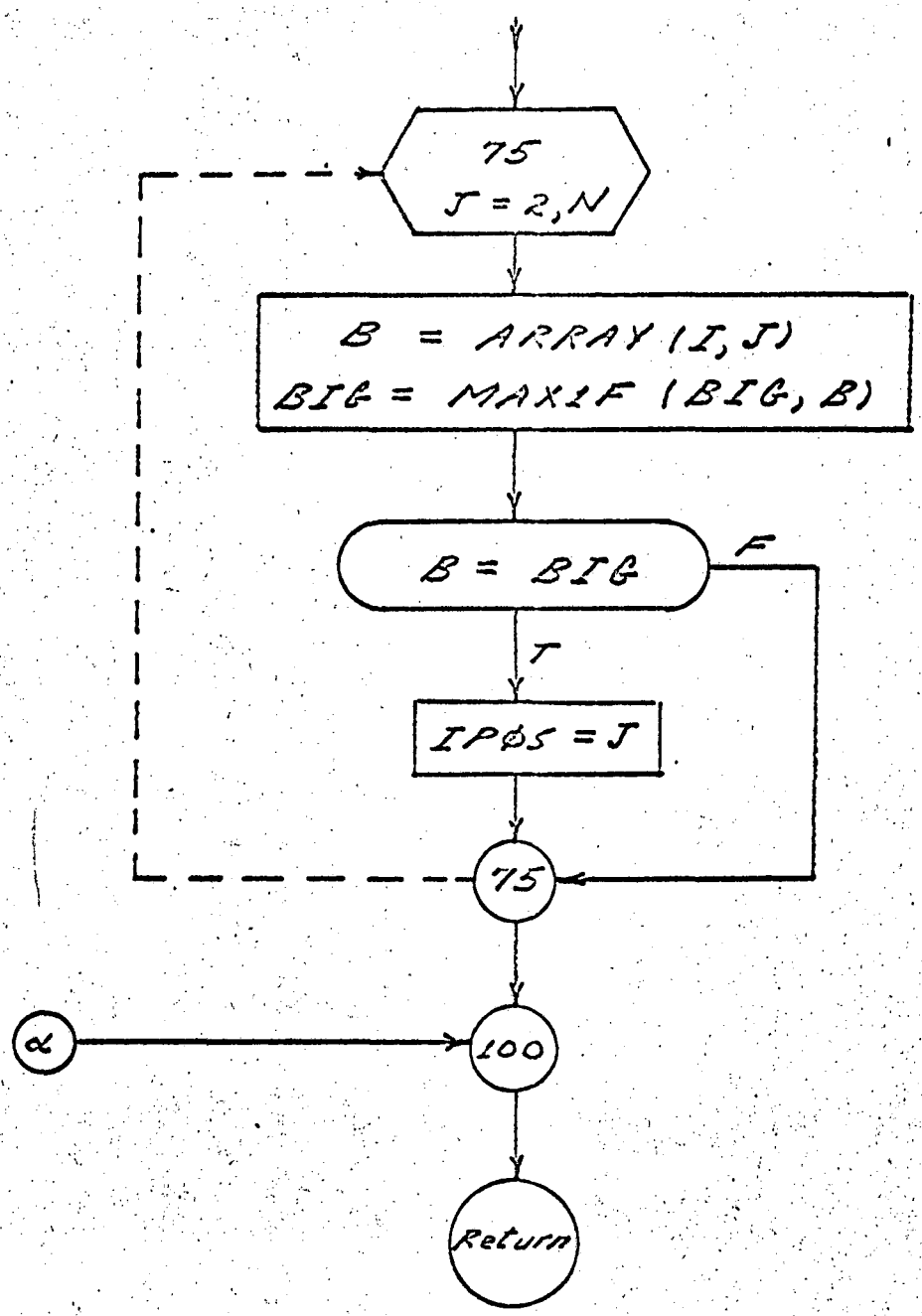

TH1
20
J = 1, NL
ST(I,J) = 0.023/(REYNØL(I,J)**
0.20 * PR**0.60)
20
Call
INTEG
(ST, XL, NL, 2, I, F)
50
J = 1, NL
F1(I,J) = EXPF(-B1 * F(I,J))
F2(I,J) = EXPF(B1 * F(I,J) * ST(I,J)*
THETA2(I,J))
50
Call
INTEG
(F2, XL, NL, 2, I, F3)

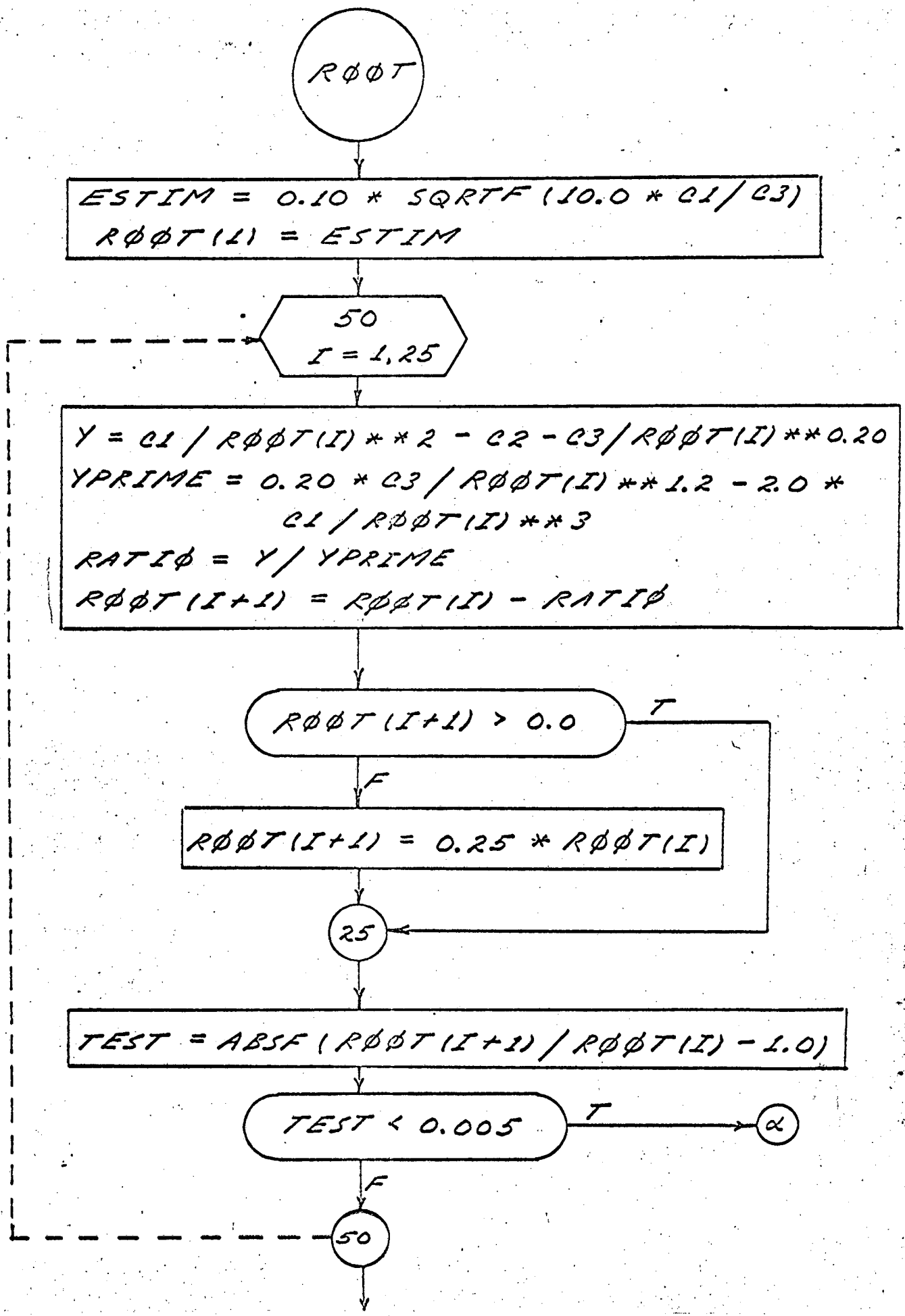

75
J = 1, NL
THETA1 (I, J) = F1 (I, J) * (THETA1 (I, 1)
+ B1 * F3 (I, J))
75
Return

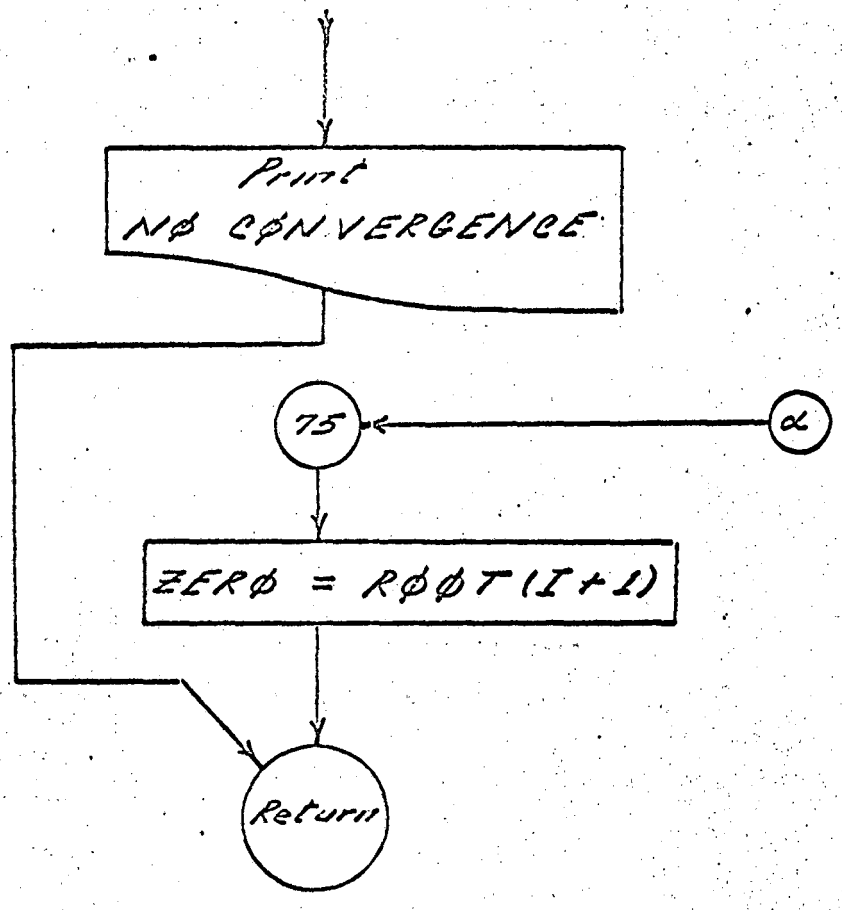

THR
20
J = 2, NL
XNU(I,J) = 0.023 * REYNOL(I,J) ** 0.80
* PR ** 0.40
20
TL = T/FLOATF(NT-1) * FLOATF(I-1)
α
75
J = 2, NL
Call
INTEG
(XNU, TL, I, L, J, F)
XJ = 3.1416 * FLOATF(J-1)/FLOATF(NL-1)
FL(I,J) = EXPF(-DL * F(I,J))
FS(I,J) = EXPF(DL * F(I,J)) * (DR * COS
* SINF(XJ) + DL * XNU(I,J) *
THETAL(I,J))
Call
INTEG
(FS, TL, I, L, J, FIJ)

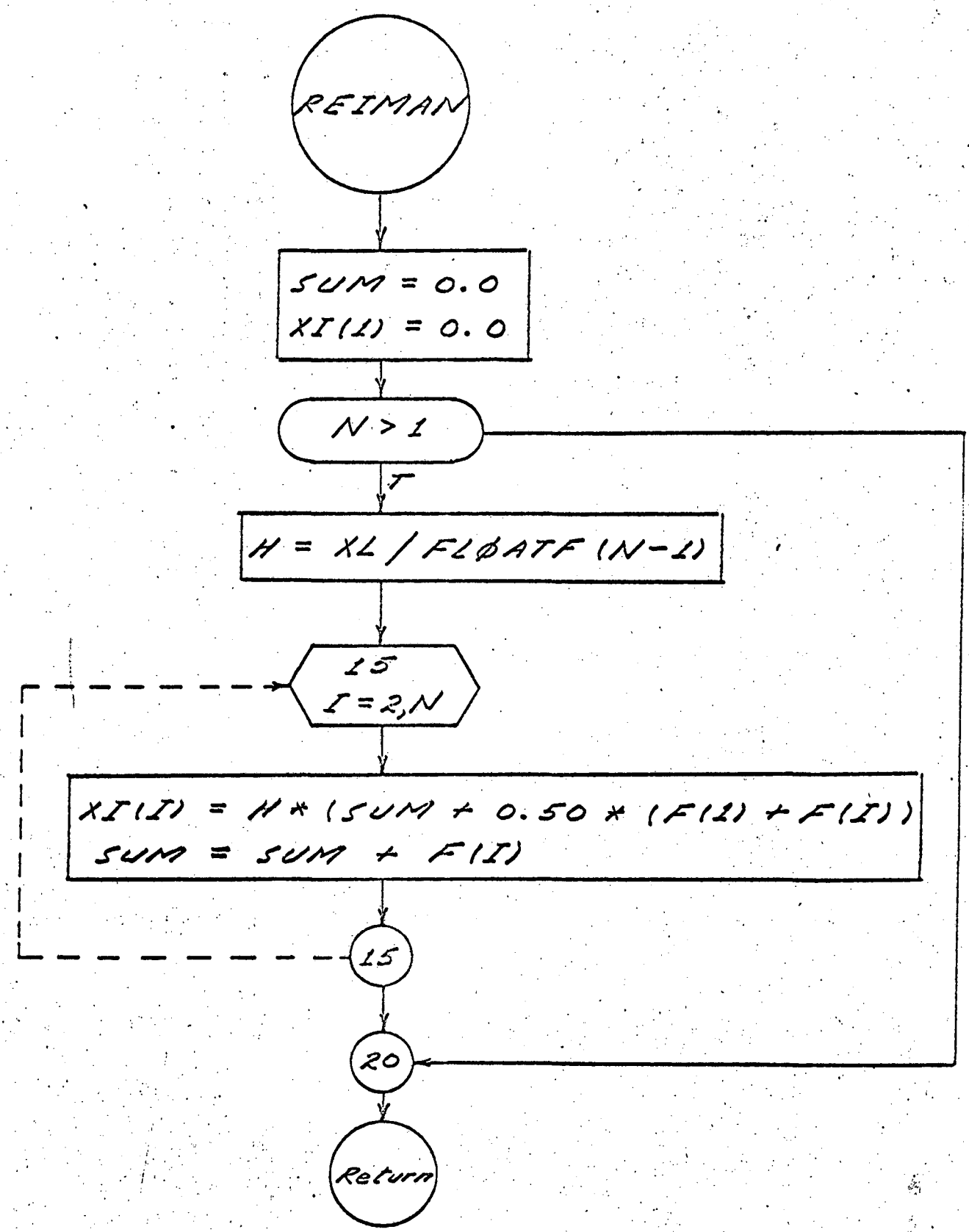

THETA2 (I, J) = F1 (I, J) * (D3 (J) + F3 (I, J))
α
75
Return

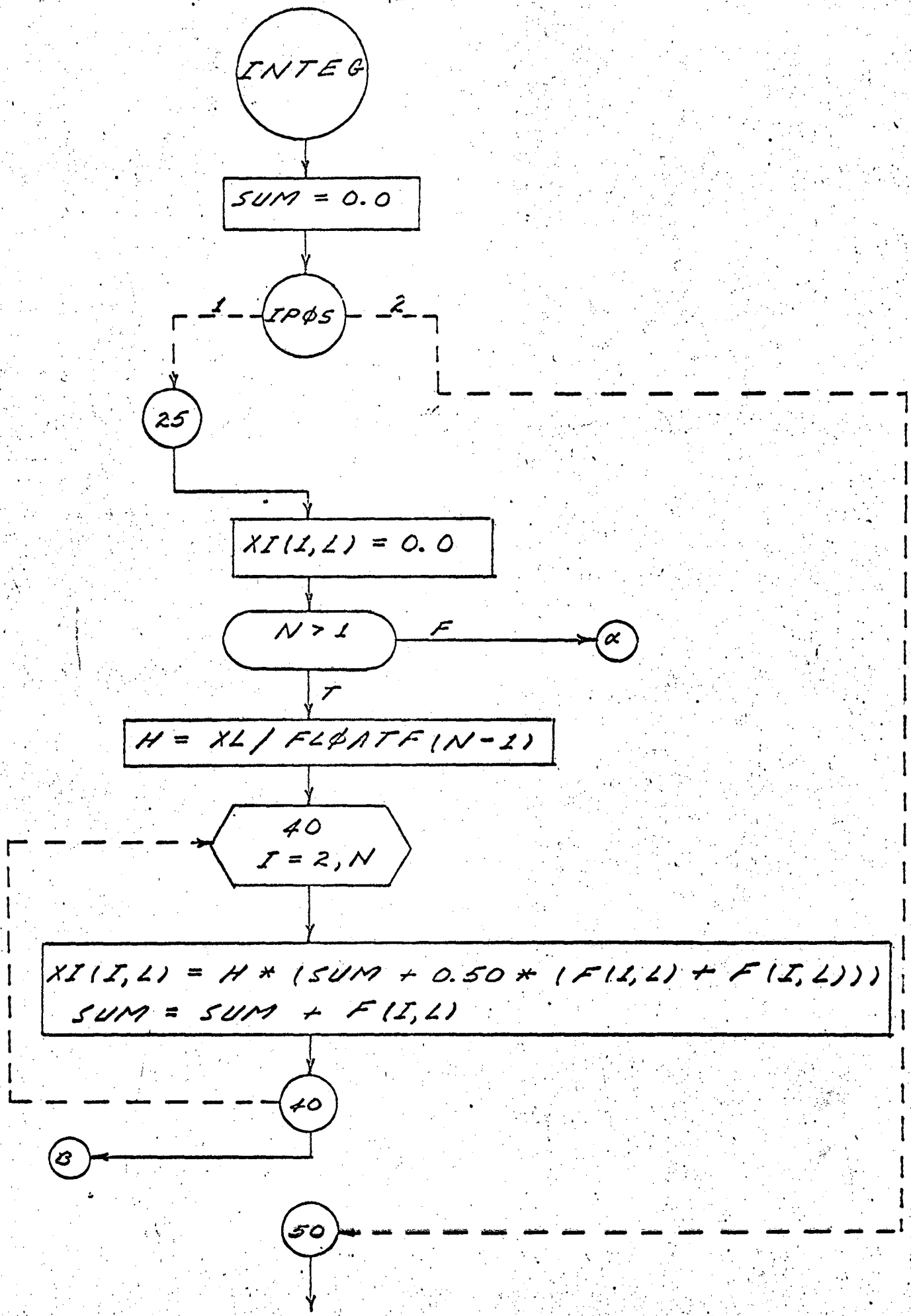

MAX
IPØS = 1
K > 1
T
F
J = L
BIG = ARRAY (L, J)
25
I = 2, N
B = ARRAY (I, J)
BIG = MAXIF (BIG, B)
B = BIG
F
T
IPØS = I
25
α
50
I = L
BIG = ARRAY (I, 1)

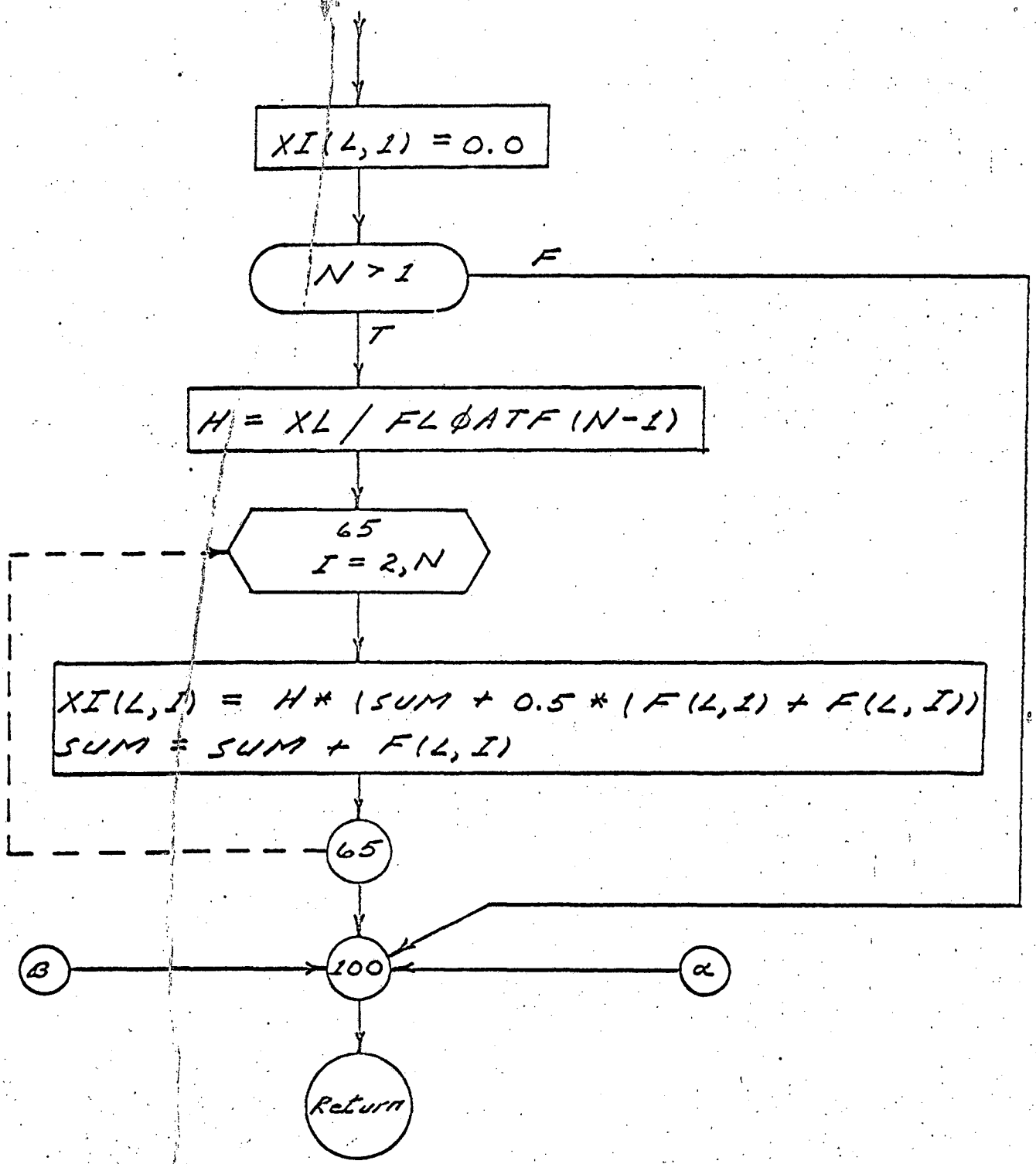

75
J = 2,N
B = ARRAY (I, J)
BIG = MAX1F (BIG, B)
B = BIG
F
T
IPØS = J
75
α
100
Return

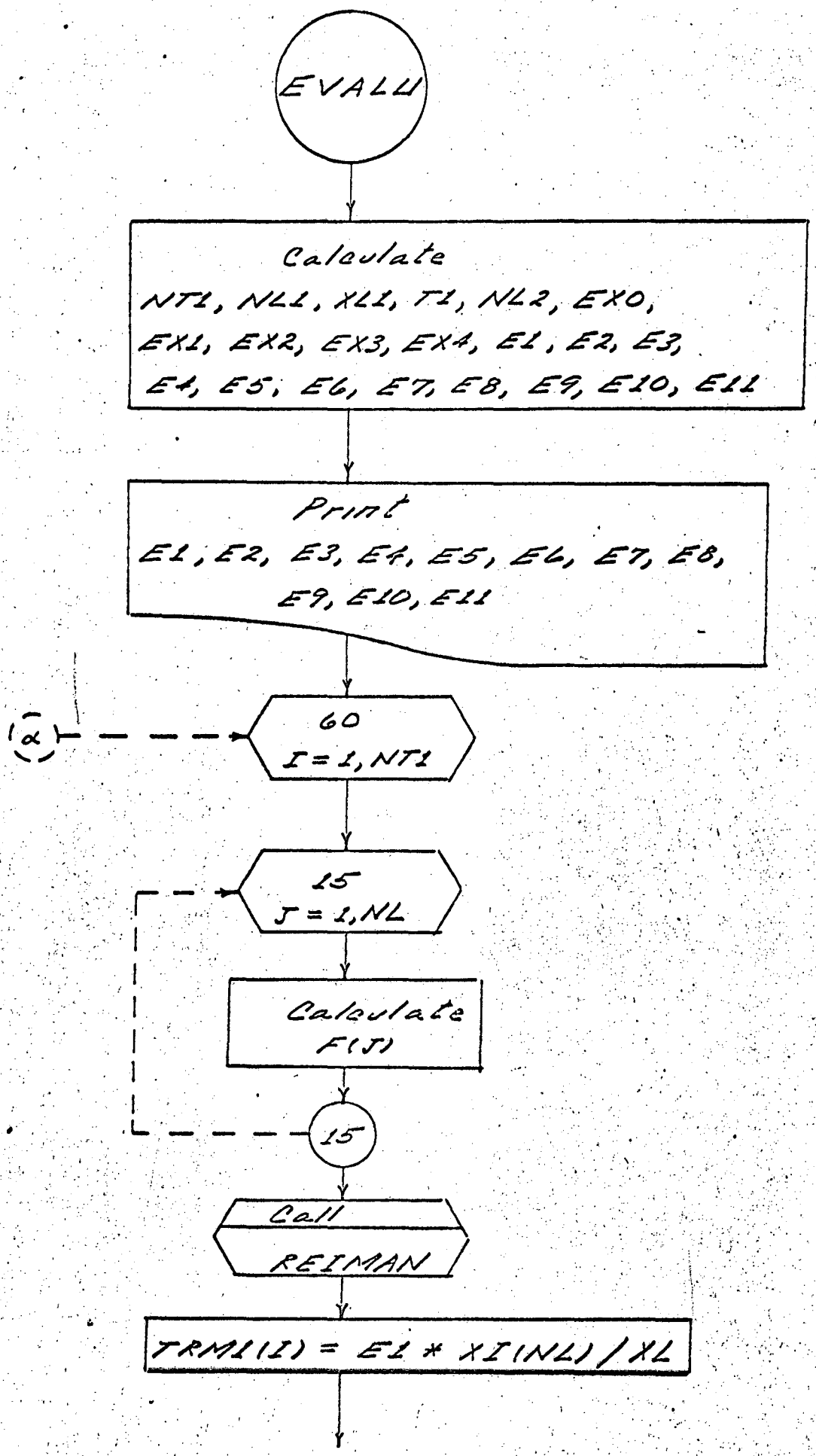

RØØT
ESTIM = 0.10 * SQRTF (10.0 * C1/C3)
RØØT(1) = ESTIM
50
I = 1,25
Y = C1 / RØØT(I) **2 - C2 - C3/RØØT(I) **0.20
YPRIME = 0.20 * C3 / RØØT(I) **1.2 - 2.0 *
C1 / RØØT(I) **3
RATIØ = Y / YPRIME
RØØT(I+1) = RØØT(I) - RATIØ
RØØT(I+1) > 0.0
T
F
RØØT(I+1) = 0.25 * RØØT(I)
25
TEST = ABSF (RØØT(I+1) / RØØT(I) - 1.0)
TEST < 0.005
T
α
F
50

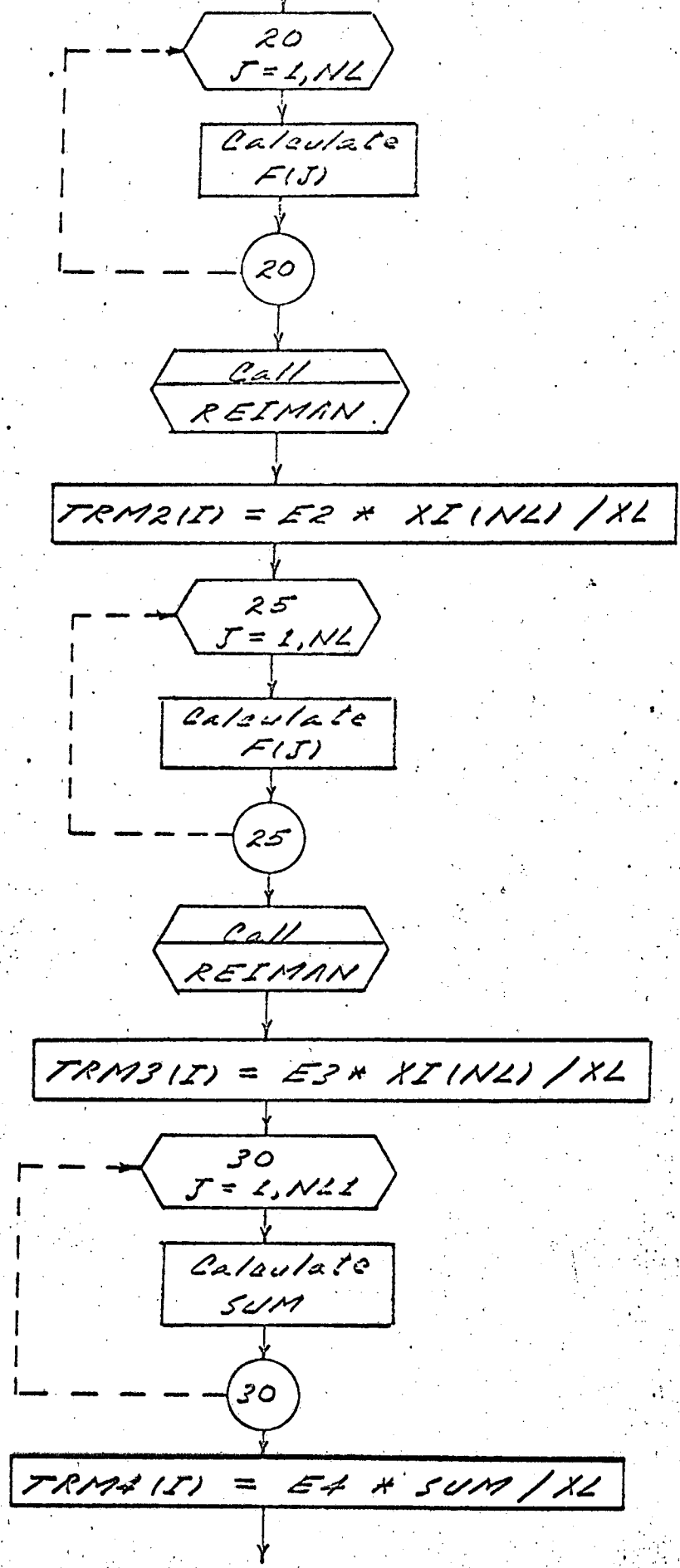

Print
NØ CØNVERGENCE
75
α
ZERØ = RØØT(I+1)
Return

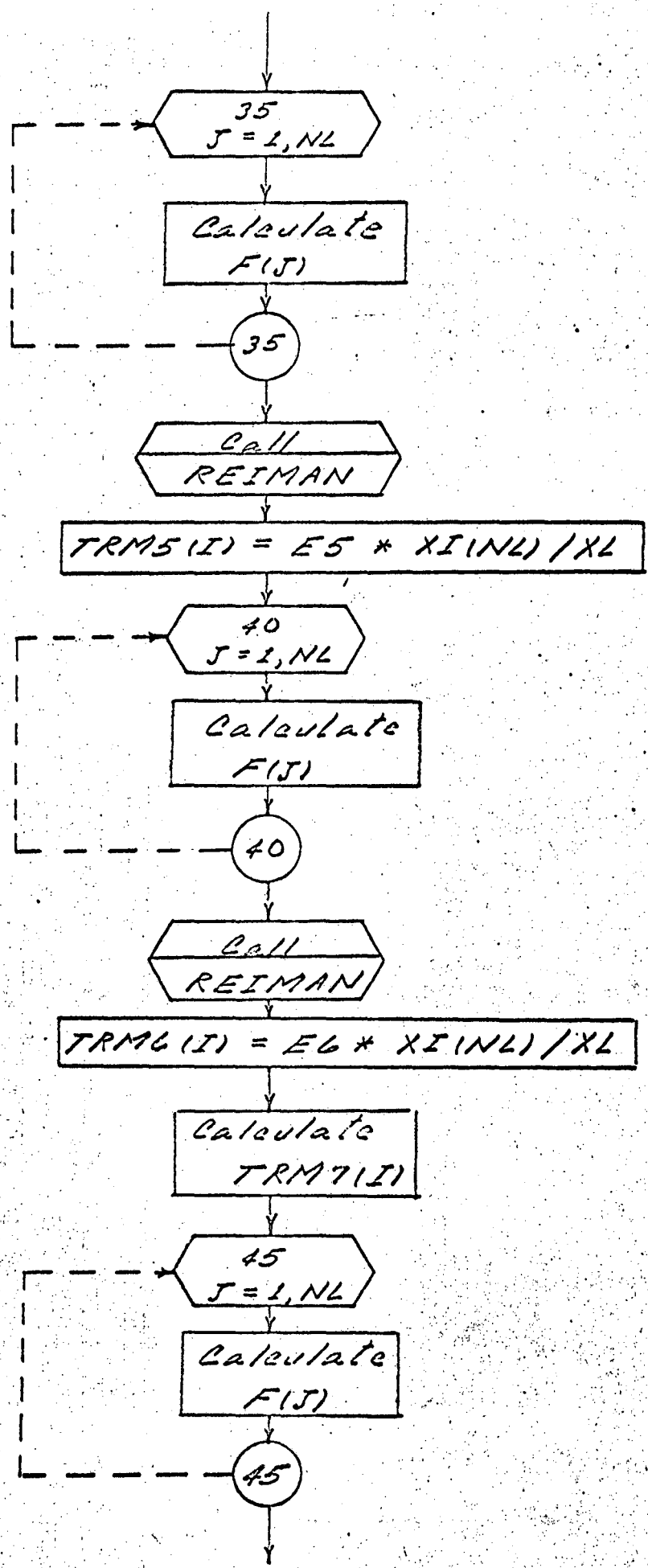

REIMAN
SUM = 0.0
XI(1) = 0.0
N > 1
T
H = XL / FLØATF (N-1)
15
I = 2,N
XI(I) = H * (SUM + 0.50 * (F(1) + F(I)))
SUM = SUM + F(I)
15
20
Return

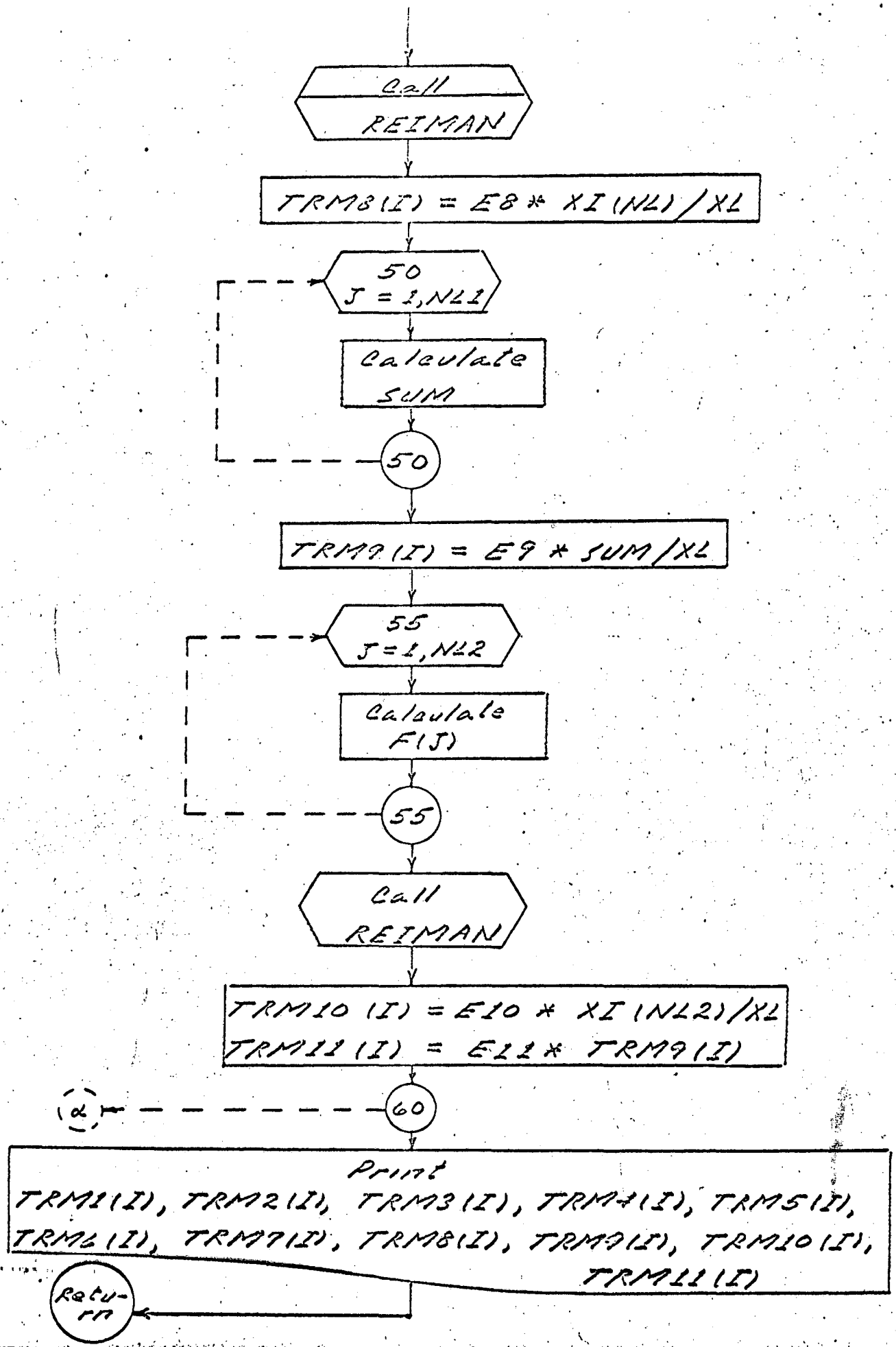

INTEG
SUM = 0.0
IPØS
1
2
25
XI(1,L) = 0.0
N > 1
F
α
T
H = XL / FLØATF(N-1)
40
I = 2,N
XI(I,L) = H * (SUM + 0.50 * (F(1,L) + F(I,L)))
SUM = SUM + F(I,L)
40
β
50


XI(L,1) = 0.0
N > 1
F
T
H = XL / FLØATF(N-1)
65
I = 2,N
XI(L,I) = H* (SUM + 0.5 * (F(L,1) + F(L,I))
SUM = SUM + F(L,I)
65
β
100
α
Return


EVALU
Calculate
NT1, NL1, XL1, T1, NL2, EX0,
EX1, EX2, EX3, EX4, E1, E2, E3,
E4, E5, E6, E7, E8, E9, E10, E11
Print
E1, E2, E3, E4, E5, E6, E7, E8,
E9, E10, E11
α
60
I = 1, NT1
15
J = 1, NL
Calculate
F(J)
15
Call
REIMAN
TRML(I) = E1 * XI(NL) / XL


20
J=1,NL
Calculate
F(J)
20
Call
REIMAN.
TRM2(I) = E2 * XI(NL) / XL
25
J=1,NL
Calculate
F(J)
25
Call
REIMAN
TRM3(I) = E3 * XI(NL) / XL
30
J=1,NL1
Calculate
SUM
30
TRM4(I) = E4 * SUM / XL


35
J = 1, NL
Calculate
F(J)
35
Call
REIMAN
TRM5(I) = E5 * XI(NL) / XL
40
J = 1, NL
Calculate
F(J)
40
Call
REIMAN
TRM6(I) = E6 * XI(NL) / XL
Calculate
TRM7(I)
45
J = 1, NL
Calculate
F(J)
45


Call
REIMAN
TRM8(I) = E8 * XI(NL)/XL
50
J = 1,NL1
Calculate
SUM
50
TRM9(I) = E9 * SUM/XL
55
J = 1,NL2
Calculate
F(J)
55
Call
REIMAN
TRM10 (I) = E10 * XI (NL2)/XL
TRM11 (I) = E11 * TRM9(I)
α
60
Print
TRM1(I), TRM2(I), TRM3(I), TRM4(I), TRM5(I),
TRM6(I), TRM7(I), TRM8(I), TRM9(I), TRM10(I),
TRM11(I)
Return

```
$FMS
*     NOGO
*     DECKS
*     LABEL
*     FORTRAN
CMAIN
C     MAIN CONTROLLING PROGRAM
      DIMENSION TITLE(16), C8(11), D3(11), REYNOX(201), REY(201,11),
     1 THE1(201,11), XNU(201,11)
      DIMENSION Q(201), REYNOL(201,11), THETA1(201,11), THETA2(201,11)
      DIMENSION Z(12000)
      COMMON Z
      EQUIVALENCE (XNU(1),Z(6660))
      COMMON IPRINT, EX
      SENSE LIGHT 0
      READ 10, (TITLE(K),K=1,12)
      READ 12, ITRIP, JTRIP, KTRIP
      IF (ITRIP)5,5,7
    5 CONTINUE
      SENSE LIGHT 1
    7 CONTINUE
   10 FORMAT(12A5)
      READ 12, NL,NT
      READ 12, IPRINT
   12 FORMAT(3I5)
      READ 14, RHO1, XMU1, TEM1, XK1, BETA, CP1, PR
      READ 14, RHYD, RHT, AFLOW, AFUEL
      READ 14, XL, DELTA, R
      READ 14, XKF, RHOFL, CPF
      READ 14, T, P
      READ 14, EX, TDELY, FR1, FR2, ICHEK
   14 FORMAT(7F10.5)
      PR04 = PR**0.40
      PR06 = PR**0.60
      IF (ICHEK)17,17,18
   17 SENSE LIGHT 3
   18 CONTINUE
C     IF ICHEK IS ZERO THE FLOW IS LAMINAR, IF IT IS ONE THE FLOW IS
C     TURBULENT
      PRINT 25
   25 FORMAT(23H1INITIATION OF PRINTOUT)
      PRINT 26
   26 FORMAT(1H0)
      PRINT 198
      PRINT 27, (TITLE(K),K=1,12)
   27 FORMAT(1H0,16A5)
      PRINT 198
      PRINT 28
   28 FORMAT(13H0THE INPUT IS)
      PRINT 30, P
   30 FORMAT(38H0THE POWER DENSITY P IN CAL/CC/SEC IS ,E15.8)
      PRINT 31, NT
   31 FORMAT(30H THE MAXIMUM TIME INDEX NT IS ,I3)
      PRINT 32, T
   32 FORMAT(33H THE TIME INTERVAL IN SECONDS IS ,E15.8)
      PRINT 63, TDELY
   63 FORMAT(30H THE DELAY TIME IN SECONDS IS ,E15.8)
      PRINT 33, IPRINT
   33 FORMAT(24H THE VALUE OF IPRINT IS ,I3)
```

```
      PRINT 34, NL
   34 FORMAT(34H THE MAXIMUM DISTANCE INDEX NL IS ,I3)
      PRINT 36, XL
   36 FORMAT(29H THE CORE LENGTH XL IN CM IS ,E15.8)
      PRINT 38, RHO1
   38 FORMAT(36H THE INLET DENSITY RHO1 IN GM/CC IS ,E15.8)
      PRINT 40, XMU1
   40 FORMAT(38H THE INLET VISCOSITY XMU1 IN POISE IS ,E15.8)
      PRINT 42, TEM1
   42 FORMAT(36H THE INLET TEMPERATURE TEM1 IN K IS ,E15.8)
      PRINT 43, CP1
   43 FORMAT(32H THE INLET SPECIFIC HEAT CP1 IS ,E15.8)
      PRINT 44, XK1
   44 FORMAT(39H THE INLET THERMAL CONDUCTIVITY XK1 IS ,E15.8)
      PRINT 46, BETA
   46 FORMAT(46H THE COEFFICIENT OF THERMAL EXPANSION BETA IS ,E15.8)
      PRINT 45, PR
   45 FORMAT(29H THE INLET PRANDTL NUMBER IS ,E15.8)
      PRINT 47, EX
   47 FORMAT(43H THE EXPONENT IN THE VISCOSITY RELATION IS ,E15.8)
      PRINT 49, FR1
   49 FORMAT(52H THE COEFFICIENT IN THE FRICTION FACTOR RELATION IS ,
     1 E15.8)
      PRINT 51, FR2
   51 FORMAT(49H THE EXPONENT IN THE FRICTION FACTOR RELATION IS ,E15.8)
      PRINT 48, RHYD
   48 FORMAT(30H THE HYDRAULIC RADIUS RHYD IS ,E15.8)
      PRINT 50, RHT
   50 FORMAT(33H THE HEAT TRANSFER RADIUS RHT IS ,E15.8)
      PRINT 52, AFLOW
   52 FORMAT(40H THE FLOW CROSS SECTIONAL AREA AFLOW IS ,E15.8)
      PRINT 54, AFUEL
   54 FORMAT(40H THE FUEL CROSS SECTIONAL AREA AFUEL IS ,E15.8)
      PRINT 56, DELTA
   56 FORMAT(47H THE EQUIVALENT EXTERNAL LENGTH RATIO DELTA IS ,E15.8)
      PRINT 58, R
   58 FORMAT(40H THE RATIO OF TOTAL TO CORE LENGTH R IS ,E15.8)
      PRINT 60, XKF
   60 FORMAT(38H THE FUEL THERMAL CONDUCTIVITY XKF IS ,E15.8)
      PRINT 62, RHOFL
   62 FORMAT(27H THE FUEL DENSITY RHOFL IS ,E15.8)
      PRINT 64, CPF
   64 FORMAT(31H THE FUEL SPECIFIC HEAT CPF IS ,E15.8)
      C4 = 32.0 * RHO1**2 * 980.0 * BETA * TEM1 * RHYD**3 / FR1 / XMU1
     1 **2
      C5 = XK1 * XL / XMU1 / CP1 / RHT
      C6 = XK1 * AFLOW / AFUEL / 4.0 / RHYD / RHT / RHOFL / CPF
      C7 = 4.0 * RHYD * RHT / XK1 / TEM1 / AFLOW * AFUEL
      READ 65, (C8(J),J=1,NL)
   65 FORMAT(7F10.2)
      DO 68 J = 1,NL
      D3(J) = C8(J)
   68 CONTINUE
      A1 = 16.0 * RHYD**2 * RHO1**2 * 980.0 * XL / XMU1**2
      A2 = FR1 * XL / 2.0 / RHYD
      A3 = 1.0 + FR2 * EX
      A4 = DELTA
      A5 = R
      B1 = XL / RHT
      D1 = 0.25 * AFLOW * XK1 / AFUEL / RHOFL / CPF / RHYD / RHT
```

```
      D2 = 1.0 / TEM1 / CPF / RHOFL
      PRINT 70
   70 FORMAT(29H0THE CALCULATED CONSTANTS ARE)
      PRINT 26
      PRINT 72, C4, C5, C6, C7
   72 FORMAT(5H0C4 =,E15.8,5X,4HC5 =,E15.8,5X,4HC6 =,E15.8,5X,4HC7 =,
     1 E15.8)
      PRINT 74, (C8(J),J=1,NL)
   74 FORMAT(6H0C8(J)/(5E20.8))
      PRINT 76, A1, A2, A3, A4, A5
   76 FORMAT(5H0A1 =,E15.8,5X,4HA2 =,E15.8,5X,4HA3 =,E15.8,5X,4HA4 =,
     1 E15.8,5X,4HA5 =,E15.8)
      PRINT 78, B1, D1, D2
   78 FORMAT(5H0B1 =,E15.8,5X,4HD1 =,E15.8,5X,4HD2 =,E15.8)
      PRINT 80,(D3(J),J=1,NL)
   80 FORMAT(6H0D3(J)/(5E20.8))
      PRINT 26
      PRINT 100
  100 FORMAT(14H0THE OUTPUT IS)
      CALL POWER(P, T, NT, Q, TDELY)
      PRINT 105, (Q(K),K=1,NT)
  105 FORMAT(48H0THE DECAY HEAT GENERATION RATE IN CAL/CC/SEC IS/
     1 (5E20.8))
      CALL TRIGA(C4, C5, C6, C7, C8, Q, T, XL, NL, NT, REYNOX, THETA1,
     1 THETA2, FR2, ICHEK, PR04)
      DO 115 K = 1,NT
      DO 115 J = 1,NL
      REY(K,J) = REYNOX(K)
      THE1(K,J) = THETA1(K,J)
      REYNOL(K,J) = REYNOX(K)
  115 CONTINUE
      PRINT 120
  120 FORMAT(48H0THE INITIAL ESTIMATES FROM SUBROUTINE TRIGA ARE)
      DO 130 I = 1,NT,IPRINT
      PRINT 122, I, (REYNOL(I,J),J=1,NL)
  122 FORMAT(12H0REYNOL(I,J),5X,3HI =,I3/(5E20.8))
      PRINT 124, I, (THETA1(I,J),J=1,NL)
  124 FORMAT(12H0THETA1(I,J),5X,3HI =,I3/(5E20.8))
      PRINT 126, I, (THETA2(I,J),J=1,NL)
  126 FORMAT(12H0THETA2(I,J),5X,3HI =,I3/(5E20.8))
  130 CONTINUE
      PRINT 198
      PRINT 26
      PRINT 150
  150 FORMAT(58H0THE VALUES CALCULATED BY SUBROUTINES RE, TH1, AND TH2 A
     1RE)
  198 FORMAT(41H0*************************************,41H***********
     1****************************,20H*******************)
      NN5 = 0
      DO 250 I = 1,NT
      BIG1 = 1.0
      BIG2 = 10.0
      TEST2 = 0.0
  200 FORMAT(19H0THE TIME INDEX IS ,I3)
      DO 225 NTRIAL = 1,25
      BIG3 = REYNOL(I,1)
      EST = REYNOL(I,1)
      STORE = XL
      XL = 1.0
```

```
      CALL RE(A1, A2, A3, A4, A5, THETA1, I, NL, XL, FR2, REY)
      CALL TH1(B1, REYNOL, NL, XL, I, THETA2, THE1, PR06)
      CALL TH2(D1, D2, D3, I, NL, NT, T, XL, THETA1, Q, REYNOL, THETA2,
     1 PR04)
      CALL MAX(THETA2, NL, 2, I, BIG2, IPOS)
      XL = STORE
      DO 210 J = 1,NL
      THETA1(I,J) = THE1(I,J)
      REYNOL(I,J) = REY(I,1) / THETA1(I,J)**EX
  210 CONTINUE
      BIG4 = REYNOL(I,1)
      TEST = ABSF(BIG2/BIG1 - 1.0)
  212 FORMAT(6H0      ,5X,6HBIG1 =,E15.8,5X,6HBIG2 =,E15.8,5X,6HTEST =,
     1 E15.8)
      IF (0.01 - TEST)215,214,214
  214 TEST2 = ABSF(BIG4 / BIG3 - 1.0)
      IF (0.01 - TEST2)215,230,230
  215 CONTINUE
      IF (JTRIP)205,205,225
  205 CONTINUE
      IF(XMODF(I,IPRINT) - 1)225,216,225
  216 PRINT 200, I
      PRINT 217, NTRIAL
  217 FORMAT(14HOTRIAL NUMBER ,I2)
      PRINT 212, BIG1, BIG2, TEST
      PRINT 218, BIG3, BIG4, TEST2
  218 FORMAT(1H0,10X,6HBIG3 =,E15.8,5X,6HBIG4 =,E15.8,5X,7HTEST2 =,
     1 E15.8)
      PRINT 122, I, (REYNOL(I,J),J=1,NL)
      PRINT 124, I, (THETA1(I,J),J=1,NL)
      PRINT 126, I, (THETA2(I,J),J=1,NL)
  225 BIG1 = BIG2
      PRINT 226, I
  226 FORMAT(24HOTHE ITTERATION FOR I = ,I3,23H HAS FAILED TO CONVERGE)
      NN5 = NN5 + 1
      IF (NN5 - 2)227,227,300
  227 GO TO 250
  230 CONTINUE
      IF(KTRIP)206,206,250
  206 CONTINUE
      IF(XMODF(I,IPRINT) - 1)250,231,250
  231 PRINT 26
      PRINT 198
      PRINT 232, I, NTRIAL
  232 FORMAT(38HOTHE ITTERATION HAS CONVERGED FOR I = ,I3,14H AND NTRIAL
     1 = ,I2,16H THE RESULTS ARE)
      PRINT 122, I, (REYNOL(I,J),J=1,NL)
      PRINT 234, I, (XNU(I,J),J=1,NL)
  234 FORMAT(8HONU(I,J),5X,3HI =,I3/(5E20.8))
      PRINT 124, I, (THETA1(I,J),J=1,NL)
      PRINT 126, I, (THETA2(I,J),J=1,NL)
      PRINT 198
  250 CONTINUE
      PRINT 26
      PRINT 26
      PRINT 198
      PRINT 198
      CALL EVALU(XMU1, RHYD, RHT, RHO1, TEM1, CP1, NL, NT, XL, T,
     1 XKF, AFUEL, AFLOW, THETA1, REYNOL, THETA2, FR1, FR2)
      PRINT 26
```

```
      PRINT 150
      PRINT 251
  251 FORMAT(58H0THE THETAS ARE THE TEMPERATURE RISE IN DEGREES CENTIGRA
     1DE)
      DO 275 I = 1,NT,IPRINT
      T1 = T * FLOATF(I-1) / FLOATF(NT-1)
      PRINT 260, T1
  260 FORMAT(8H0TIME = ,E15.8,12X,1HX,7X,6HREYNOL,14X,6HTHETA1,14X,
     1 6HTHETA2)
      DO 275 J = 1,NL
      XX1 = FLOATF(J-1) / FLOATF(NL-1)
      THETA1(I,J) = TEM1 * (THETA1(I,J) - 1.0)
      THETA2(I,J) = TEM1 * (THETA2(I,J) - 1.0)
      PRINT 265, XX1, REYNOL(I,J), THETA1(I,J), THETA2(I,J)
  265 FORMAT(33X,F4.2,3E20.8)
  275 CONTINUE
  300 CONTINUE
      PRINT 310
  310 FORMAT(16H0END OF PRINTOUT)
      CALL EXIT
      END
```

```
*       LABEL
*       FORTRAN
        SUBROUTINE POWER(P, T, N, Q, TDELY)
C       THIS SUBROUTINE CALCULATES THE DECAY HEAT GENERATION Q IN TERMS OF
C       THE OPERATING POWER P IN THE SAME UNITS FOR A SPECIFIED NUMBER OF
C       TIME INTERVALS  N  OVER A GIVEN TIME SPAN T IN SECONDS, THE TOTAL
C       NUMBER OF CALCULATED VALUES IS N, T MUST BE LESS THAN 4*10**6
C       SECONDS
        DIMENSION Q(201), TE(201), Z(12000)
        COMMON Z
        EQUIVALENCE (Z(1),TE(1))
        DELTE = T / FLOATF(N-1)
        DO 5 I = 1,N
        TE(I) = DELTE * FLOATF(I-1) + TDELY
      5 CONTINUE
        DO 15 I = 1,N
        IF (TE(I) - 10.0)10,20,20
     10 Q(I) = 0.06025 * P / TE(I)**0.0639
     15 CONTINUE
        GO TO 100
     20 M = I
        DO 30 I = M,N
        IF (TE(I) - 150.0)25,35,35
     25 Q(I) = 0.07655 * P / TE(I)**0.1807
     30 CONTINUE
        GO TO 100
     35 M = I
        DO 50 I = M,N
        Q(I) = 0.1301 * P / TE(I)**0.2834
     50 CONTINUE
    100 CONTINUE
        RETURN
        END
```

```
*     LABEL
*     FORTRAN
CTRIGA
C     SUBROUTINE FOR TRIGA TYPE QUASI-STEADY STATE ANALYSIS
      SUBROUTINE TRIGA(C4, C5, C6, C7, C8, H, T, XL, NL, NT, REYNOL,
     1 THETA1, THETA2, FR2, ICHEK, PR04)
C     THIS SUBROUTINE CALCULATES REYNOL, THETA1, AND THETA2 FOR A
C     GIVEN TIME SPAN T AND DISTANCE SPAN XL FOR RANGES 2,NT AND 1,NL
C     RESPECTIVELY, 1 AND 2 DENOTE COOLANT AND FUEL
C     RESPECTIVELY, H DENOTES POWER DENSITY
      DIMENSION H(201), REYNOL(201), THETA1(201,11), THETA2(201,11),
     1 F(201), XIG(201), C8(11),  Z(12000)
      COMMON Z
      EQUIVALENCE (Z(1),F(1)), (Z(205),XIG(1))
      ARG1 = T / FLOATF(NT-1)
      ARG2 = 3.14159265 / FLOATF(NL-1)
      XL = XL
      C10 = C4 * C5 * C7 * 3.0 / 3.1416
      C12 = 1.0 / (3.0 - FR2)
      DO 15 I = 1,NT
      REYNOL(I) = (C10 * H(I))**C12
   15 CONTINUE
      IF (ICHEK)16,16,18
   16 XNU = 4.364
      GO TO 19
   18 XNU = 0.023 * REYNOL(1)**0.8 * PR04
   19 CONTINUE
      IF (SENSE LIGHT 1)20,22
   20 PRINT 21, (REYNOL(I),I=1,NT)
   21 FORMAT(6HODEBUG,5X,6HREYNOL/(5E20.8))
      SENSE LIGHT 1
   22 CONTINUE
      C14 = C6 * ARG1
      DO 75 J = 1,NL
      X1 = ARG2 * FLOATF(J-1)
      DO 44 I = 1,NT
      T1 = ARG1 * FLOATF(I-1)
      F(I) = EXPF(XNU * C6 * T1)
      THETA1(I,J) = 1.0 + C5 * H(I) * C7 / REYNOL(I) * (1.0 - COSF(X1))
     1 / 3.1416
      F(I) = F(I) * (XNU * THETA1(I,J) + C7 * H(I)* SINF(X1))
   44 CONTINUE
      IF (SENSE LIGHT 1)36,38
   36 PRINT 37, (F(I),I=1,NT)
   37 FORMAT(6HODEBUG,5X,4HF(I)/(5E20.8))
      SENSE LIGHT 1
   38 CONTINUE
      SUM = 0.0
      XIG(1) = 0.0
      DO 40 I = 2,NT
      XIG(I) = C14    * (SUM + 0.5 * (F(1) + F(I)))
      SUM = SUM + F(I)
   40 CONTINUE
      IF (SENSE LIGHT 1)41,43
   41 PRINT 42, (XIG(I),I=1,NT)
   42 FORMAT(6HODEBUG,5X,3HXIG/(5E20.8))
      SENSE LIGHT 1
   43 CONTINUE
      DO 70 I = 1,NT
```

```
      T1 = ARG1 * FLOATF(I-1)
      THETA2(I,J) = EXPF(-XNU * C6 * T1) * (C8(J) + XIG(I))
   70 CONTINUE
   75 CONTINUE
      IF (SENSE LIGHT 1)45,50
   45 PRINT 46, ((THETA1(I,J),J=1,NL),I=1,NT)
   46 FORMAT(6H0DEBUG,5X,6HTHETA1/(5E20.8))
      PRINT 47, ((THETA2(I,J),J=1,NL),I=1,NT)
   47 FORMAT(6H0DEBUG,5X,6HTHETA2/(5E20.8))
      SENSE LIGHT 1
   50 CONTINUE
      IF QUOTIENT OVERFLOW 52,54
   52 PRINT 53
   53 FORMAT(42H0A QUOTIENT OVERFLOW HAS OCCURRED IN TRIGA)
   54 IF DIVIDE CHECK 56,60
   56 PRINT 57
   57 FORMAT(37H0A DIVIDE CHECK HAS OCCURRED IN TRIGA)
   60 CONTINUE
      RETURN
      END
```

```
*       LABEL
*       FORTRAN
        SUBROUTINE RE(A1, A2, A3, A4, A5, THETA1, I, NL, XL, EST, REYNOL)
C       THIS PROGRAM SOLVES THE MOMENTUM EQUATION TO OBTAIN REYNOL(I,1)
C       WHERE I IS A TIME INDEX
        DIMENSION REYNOL(201,11), THETA1(201,11), F(201,11), XI1(201,11),
       1 XI2(201,11), Z(12000)
        COMMON Z
        EQUIVALENCE (Z(1),F(1)), (Z(2220),XI1(1)), (Z(4440),XI2(1))
        DO 25 J = 1,NL
        F(I,J) = (1.0 - THETA1(I,J)) / THETA1(I,J)
   25   CONTINUE
        IF (SENSE LIGHT 1)30,35
   30   PRINT 32, I, (F(I,J),J=1,NL)
   32   FORMAT(6H0DEBUG,5X,6HF(I,J),7X,3HI =,I5/(5E20.8))
        SENSE LIGHT 1
   35   CONTINUE
        CALL INTEG(F, XL, NL, 2, I, XI1)
        DO 50 J = 1,NL
        F(I,J) = THETA1(I,J)**A3 + A4
   50   CONTINUE
        IF (SENSE LIGHT 1)55,60
   55   PRINT 32, I, (F(I,J),J=1,NL)
        SENSE LIGHT 1
   60   CONTINUE
        CALL INTEG(F, XL, NL, 2, I, XI2)
        C1 = A1 * ((A5 - 1.0) * (1.0 - THETA1(I,NL)) / THETA1(I,NL) +
       1 XI1(I,NL))
        C1 = -C1
        C2 = THETA1(I,NL) - 1.0
        C3 = A2 * XI2(I, NL)
        IF (SENSE LIGHT 1)65,70
   65   PRINT 67, C1, C2, C3
   67   FORMAT(6H0DEBUG,5X,4HC1 =,E15.8,5X,4HC2 =,E15.8,5X,4HC3 =,E15.8)
        SENSE LIGHT 1
   70   CONTINUE
        IF QUOTIENT OVERFLOW 80,85
   80   PRINT 82
   82   FORMAT(39H0A QUOTIENT OVERFLOW HAS OCCURRED IN RE)
   85   IF DIVIDE CHECK 90,95
   90   PRINT 92
   92   FORMAT(34H0A DIVIDE CHECK HAS OCCURRED IN RE)
   95   CONTINUE
        CALL ROOT(C1, C2, C3, EST, ZERO)
        REYNOL(I,1)    = ZERO
        RETURN
        END
```

```
*     LABEL
*     FORTRAN
      SUBROUTINE TH1(B1, REYNOL, NL, XL, I, THETA2, THETA1, PR06)
C     THIS SUBROUTINE SOLVES THE COOLANT ENERGY EQUATION TO OBTAIN
C     THETA1(I,J) WHERE I IS THE TIME INDEX
      DIMENSION REYNOL(201,11), THETA1(201,11), THETA2(201,11),
     1 ST(201,11), F(201,11), F1(201,11), F2(201,11), F3(201,11),
     2 Z(12000)
      COMMON Z
      EQUIVALENCE (Z(1),F(1)), (Z(2220),F1(1)), (Z(4440),F2(1)),
     1 (Z(6660),F3(1)), (Z(8880),ST(1))
      IF(SENSE LIGHT 3)15,18
   15 DO 16 J = 1,NL
   16 ST(I,J) = 4.364 / REYNOL(I,J) / PR06**(1.0/0.60)
      SENSE LIGHT 3
      GO TO 21
   18 CONTINUE
      DO 20 J = 1,NL
      ST(I,J) = 0.023 / (REYNOL(I,J)**0.2 * PR06)
   20 CONTINUE
   21 CONTINUE
      IF (SENSE LIGHT 1)30,35
   30 PRINT 32, I, (ST(I,J),J=1,NL)
   32 FORMAT(6H0DEBUG,5X,7HST(I,J),5X,3HI =,I5/(5E20.8))
      SENSE LIGHT 1
   35 CONTINUE
      CALL INTEG(ST, XL, NL, 2, I, F)
      DO 50 J = 1,NL
      TEMP = F(I,J)
      F1(I,J) = EXPF(-B1 * TEMP)
      F2(I,J) = EXPF(B1 * TEMP)
      F2(I,J) = THETA2(I,J) * ST(I,J) * F2(I,J)
   50 CONTINUE
      CALL INTEG(F2, XL, NL, 2, I, F3)
      TEMP = THETA1(I,1)
      DO 75 J = 1,NL
      THETA1(I,J)    = F1(I,J) * (TEMP + B1 * F3(I,J))
   75 CONTINUE
      RETURN
      END
```

```
      LABEL
      FORTRAN
      SUBROUTINE TH2(D1, D2, D3, I, NL, NT, T, XL, THETA1, H, REYNOL,
     1 THETA2, PR04)
      THIS SUBROUTINE SOLVES THE FUEL ENERGY EQUATION TO OBTAIN
      THETA2(I,J) WHERE I IS THE TIME INDEX
      DIMENSION REYNOL(201,11), THETA1(201,11), THETA2(201,11), H(201),
     1 D3(11),  XNU(201,11), F1(201,11), F3(201,11), F5(201,11),
     2 F(201,11)
      DIMENSION Z(12000)
      COMMON Z
      EQUIVALENCE (Z(1),F3(1)), (Z(2220),F5(1)), (Z(4440),F(1)),
     1 (Z(6660),XNU(1)), (Z(8880),F1(1))
      IF(SENSE LIGHT 3)18,22
   18 DO 19 J = 1,NL
   19 XNU(I,J) = 4.364
      SENSE LIGHT 3
      GO TO 25
   22 CONTINUE
      DO 20 J = 1,NL
      XNU(I,J) = 0.023 * REYNOL(I,J)**0.8 * PR04
   20 CONTINUE
   25 CONTINUE
      IF (SENSE LIGHT 1)30,35
   30 PRINT 32, I, (XNU(I,J),J=1,NL)
   32 FORMAT(6H0DEBUG,5X,7HNU(I,J),5X,3HI =,I5/(5E20.8))
      PRINT 33
   33 FORMAT(1H )
      SENSE LIGHT 1
   35 CONTINUE
      T1 = T / FLOATF(NT-1) * FLOATF(I-1)
      DO 75 J = 1,NL
      CALL INTEG(XNU, T1, I, 1, J, F)
      X5 =       FLOATF(J-1) * 3.1416 / FLOATF(NL-1)
      XL = XL
      TEMP = F(I,J)
      F1(I,J) = EXPF(-D1 * TEMP)
      F5(I,J) = EXPF(D1 * TEMP)
      F5(I,J) = F5(I,J) * (D2 * H(I) * SINF(X5) + D1 * XNU(I,J) *
     1 THETA1(I,J))
      CALL INTEG(F5, T1, I, 1, J, F3)
      THETA2(I,J) = F1(I,J) * (D3(J) + F3(I,J))
      IF (SENSE LIGHT 1)50,55
   50 PRINT 52, TEMP, F1(I,J), F5(I,J), F3(I,J)
   52 FORMAT(6H DEBUG,5X,6HTEMP =,E15.8,5X,4HF1 =,E15.8,5X,4HF5 =,E15.8,
     1 5X,4HF3 =,E15.8)
      SENSE LIGHT 1
   55 CONTINUE
   75 CONTINUE
      RETURN
      END
```

```
      LABEL
      FORTRAN
      SUBROUTINE ROOT(C1, C2, C3, FR2, ZERO)
      THIS SUBROUTINE FINDS THE REAL ROOT OF THE EQUATION Y = C1/X**2 -
      C2 - C3/X**FR2, ZERO IS THE VALUE OF THE ROOT, ESTIM IS THE
      ESTIMATED VALUE.
      DIMENSION ROOT(30)
      ESTIM = 0.1 * SQRTF(10.0 * C1 / C3)
      ROOT(1) = ESTIM
      DO 50 I = 1,25
      Y = C1 / ROOT(I)**2 - C2 - C3 / ROOT(I)**FR2
      YPRIME = FR2 * C3 / ROOT(I)**(1.0 + FR2) - 2.0 * C1 / ROOT(I)**3
      RATIO = Y / YPRIME
      ROOT(I+1) = ROOT(I) - RATIO
      CHECK = ROOT(I+1)
      IF (CHECK)20,25,25
   20 ROOT(I+1) = 0.25 * ROOT(I)
   25 CONTINUE
      TEST = ROOT(I+1) / ROOT(I) - 1.0
      TEST = ABSF(TEST)
      IF (SENSE LIGHT 1)40,45
   40 PRINT 42, I, Y, ROOT(I+1), TEST
   42 FORMAT(6H DEBUG,5X,3HI =,I3,5X,3HY =,E15.8,5X,11HROOT(I+1) =,E15.8
     1 ,5X,6HTEST =,E15.8)
      SENSE LIGHT 1
   45 CONTINUE
      IF (0.005 - TEST)50,75,75
   50 CONTINUE
      PRINT 52
   52 FORMAT(39H0SUBROUTINE ROOT HAS FAILED TO CONVERGE)
   75 CONTINUE
      IF QUOTIENT OVERFLOW 80,85
   80 PRINT 82
   82 FORMAT(41H0A QUOTIENT OVERFLOW HAS OCCURRED IN ROOT)
   85 IF DIVIDE CHECK 90,95
   90 PRINT 92
   92 FORMAT(36H0A DIVIDE CHECK HAS OCCURRED IN ROOT)
   95 ZERO = ROOT(I+1)
      RETURN
      END
```

```
*     LABEL
*     FORTRAN
      SUBROUTINE MAX(ARRAY, N, K, L, BIG, IPOS)
C     THIS SUBROUTINE IS DESIGNED TO DETERMINE THE MAXIMUM OF A TWO DI-
C     MENSIONAL ARRAY ARRAY(I,J) WITH RESPECT TO EITHER VARIABLE, THE
C     MAX IS CALLED BIG, IF K = 1, THE FIRST VARIABLE IS SCANNED, IF
C     K = 2, THE SECOND IS SCANNED, N IS THE MAXIMUM VALUE OF THE INDEX
C     BEING SCANNED, L IS THE VALUE OF THE VARIABLE HELD FIXED, IPOS
C     IS THE POSITION OF BIG IN THE ARRAY.
      DIMENSION ARRAY(201,11)
      IPOS = 1
      IF (K - 1)10,10,50
   10 J = L
      BIG = ARRAY(1,J)
      DO 25 I = 2,N
      B = ARRAY(I,J)
      BIG = MAX1F(BIG,B)
      IF (B - BIG)25,22,25
   22 IPOS = I
   25 CONTINUE
      GO TO 100
   50 I = L
      BIG = ARRAY(I,1)
      DO 75 J = 2,N
      B = ARRAY(I,J)
      BIG = MAX1F(BIG,B)
      IF (B - BIG)75,72,75
   72 IPOS = J
   75 CONTINUE
  100 RETURN
      END
```

```
*       LABEL
*       FORTRAN
        SUBROUTINE INTEG (F, XL, N, IPOS, L, XI)
C       THIS SUBROUTINE INTEGRATES A FUNCTION F(I,J) WITH RESPECT TO
C       EITHER INDEX USING THE TRAPEZOIDAL RULE, XL IS THE INTERVAL, N IS
C       THE MAXIMUM VALUE OF THE INDEX INTEGRATED OVER, IPOS DENOTES ITS
C       POSITION, L IS THE VALUE OF THE FIXED INDEX, XI IS THE VALUE OF
C       THE INTEGRAL.
        DIMENSION F(201,11), XI(201,11)
        SUM = 0.0
   20   GO TO (25,50),IPOS
   25   XI(1,L) = 0.0
        IF (N-1)30,28,30
   28   GO TO 100
   30   CONTINUE
        H = XL / FLOATF(N-1)
        DO 40 I = 2,N
        XI(I,L) = H * (SUM + 0.5 * (F(1,L) + F(I,L)))
        SUM = SUM + F(I,L)
   40   CONTINUE
        GO TO 100
   50   XI(L,1) = 0.0
        IF (N-1)60,58,60
   58   GO TO 100
   60   CONTINUE
        H = XL / FLOATF(N-1)
        DO 65 I = 2,N
        XI(L,I) = H * (SUM + 0.5 * (F(L,1) + F(L,I)))
        SUM = SUM + F(L,I)
   65   CONTINUE
  100   RETURN
        END
```

```
*       LABEL
*       FORTRAN
        SUBROUTINE REIMAN(F, XL, N, XI)
C       THIS SUBROUTINE INTEGRATES A FUNCTION F(I) FROM I = 1 TO N WHERE
C       XL IS THE INTERVAL AND XI(I) IS THE VALUE OF THE INTEGRAL
        DIMENSION F(201), XI(201)
        SUM = 0.0
        XI(1) = 0.0
        IF (N - 1)20,20,5
      5 H = XL / FLOATF(N-1)
        DO 15 I = 2,N
        XI(I) = H * (SUM + 0.50 * (F(1) + F(I)))
        SUM = SUM + F(I)
     15 CONTINUE
     20 RETURN
        END
```

```
*     LABEL
*     FORTRAN
      SUBROUTINE EVALU(XMU1, RHYD, RHT, RHO1, TEM1, CP, NL0, NT0, XL0,T,
     1 XKF, AFUEL, AFLOW, TH, RE, TH1, FR1, FR2)
      DIMENSION TH(201,11), RE(201,11), TRM1(201), TRM2(201), TRM3(201),
     1 TRM4(201), TRM5(201), TRM6(201), TRM7(201), TRM8(201), TRM9(201),
     2 TRM10(201), TRM11(201)
      DIMENSION Z(12000), F(11), XI(11), TH1(201,11)
      COMMON Z
      EQUIVALENCE (Z(1),TRM1(1)), (Z(205),TRM2(1)), (Z(410),TRM3(1)),
     1 (Z(615),TRM4(1)), (Z(820),TRM5(1)), (Z(1025),TRM6(1)),
     2 (Z(1230),TRM7(1)), (Z(1435),TRM8(1)), (Z(1640),TRM9(1)),
     3 (Z(1845),TRM10(1)), (Z(2050),TRM11(1))
      COMMON IPRINT, EX
      NL = NL0
      NT = NT0
      XL = XL0
      NT1 = NT - 1
      NL1 = NL - 1
      XL1 = XL / FLOATF(NL1)
      T1 = T / FLOATF(NT1)
      NL2 = NL - 2
      SUM = 0.0
      EX0 = EX
      EX1 = 1.0 - EX
      EX2 = 2.0 * EX
      EX3 = EX2 + 1.0
      EX4 = 3.0 * EX + 2.0
      E1 = XMU1 / 4.0 / RHYD
      E2 = XMU1 / 2.0 / RHYD
      E3 = 980.0 * RHO1
      E4 = XMU1**2 / 16.0 / RHYD**2 / RHO1
      E5 = FR1 * XMU1**2 / 32.0 / RHYD**3 / RHO1
      E6 = XMU1**2 / 32.0 / RHO1 / RHYD**2
      E7 = XMU1**3 / 128.0 / RHO1**2 / RHYD**3
      E8 = 4.2*10.0**7*TEM1 * RHO1 * CP
      E9 = 4.2*10.0**7 * TEM1 * XMU1 * CP / 4.0 /RHYD
      E10 = TEM1 * XKF
      E11 = 1.0 /(4.2 * 10.0**7)
      PRINT 10, E1, E2, E3, E4, E5, E6, E7, E8, E9, E10, E11
   10 FORMAT(2H0E/(5E20.8))
      DO 60 I = 1,NT1
      DO 15 J = 1,NL
   15 F(J) = (TH(I+1,J)**EX0  * RE(I+1,J) - TH(I,J)**EX0  * RE(I,J)) /T1
      CALL REIMAN(F, XL, NL, XI)
      TRM1(I) = E1 * XI(NL) / XL
      DO 20 J = 1,NL
   20 F(J) = 0.50 * (RE(I+1,J) / TH(I+1,J)**EX1  + RE(I,J) / TH(I,J)**
     1 EX1)  * (TH(I+1,J) - TH(I,J)) / T1
      CALL REIMAN(F, XL, NL, XI)
      TRM2(I) = E2 * XI(NL) / XL
      DO 25 J = 1,NL
   25 F(J) = 0.50 * (1.0 / TH(I+1,J) + 1.0 / TH(I,J))
      CALL REIMAN(F, XL, NL, XI)
      TRM3(I) = E3 * XI(NL) / XL
      DO 30 J = 1,NL1
      F(J) = 0.50 * TH(I+1,J)**EX2 * RE(I+1,J)**2 * (TH(I+1,J+1) -
     1 TH(I+1,J)) / XL1
      F(J) = F(J) + 0.50 * TH(I,J)**EX2 * RE(I,J)**2 * (TH(I,J+1) -
```

```
  1 TH(I,J)) / XL1
    XI(J) = XL1 * F(J)
 30 SUM = SUM + XI(J)
    TRM4(I) = E4 * SUM / XL
    SUM = 0.0
    DO 35 J = 1,NL
 35 F(J) = 0.50 * (TH(I+1,J)**EX3 * RE(I+1,J)**(2.0-FR2) + TH(I,J)**
  1 EX3 * RE(I,J)**(2.0-FR2))
    CALL REIMAN(F, XL, NL, XI)
    TRM5(I) = E5 * XI(NL) / XL
    DO 40 J = 1,NL
 40 F(J) = (TH(I+1,J)**EX3 * RE(I+1,J)**2 - TH(I,J)**EX3 * RE(I,J)
  1 **2) / T1
    CALL REIMAN(F, XL, NL, XI)
    TRM6(I) = E6 * XI(NL) / XL
    TRM7(I) = TH(I+1,NL)**EX4  * RE(I+1,NL)**3 - TH(I+1,1)**EX4  * RE
  1 (I+1,1)**3
    TRM7(I) = TRM7(I) + TH(I,NL)**EX4  * RE(I,NL)**3 - TH(I,1)**EX4 *
  1 RE(I,1)**3
    TRM7(I) = 0.50 * TRM7(I)
    TRM7(I) = E7 * TRM7(I) / XL
    DO 45 J = 1,NL
 45 F(J) = LOGF(TH(I+1,J) / TH(I,J)) / T1
    CALL REIMAN(F, XL, NL, XI)
    TRM8(I) = E8 * XI(NL) / XL
    DO 50 J = 1,NL1
    F(J) = 0.50 * TH(I+1,J)**EX0  * RE(I+1,J) *(TH(I+1,J+1) - TH
  1 (I+1,J)) / XL1
    F(J) = F(J) + 0.50 * TH(I,J)**EX0  * RE(I,J) * (TH(I,J+1) - TH(
  1 I,J)) / XL1
    XI(J) = XL1 * F(J)
 50 SUM = SUM + XI(J)
    TRM9(I) = E9 * SUM / XL
    SUM = 0.0
    DO 55 J = 1,NL2
    F(J) = 0.50 * (TH1(I+1,J+2) - 2.0 * TH1(I+1,J+1) + TH1(I+1,J))/
  1 XL1**2
 55 F(J) = 0.50 * (TH1(I,J+2) - 2.0 * TH1(I,J+1) + TH1(I,J)) / XL1**2
  1 + F(J)
    CALL REIMAN(F, XL, NL2, XI)
    TRM10(I) = E10 * XI(NL2) / XL
    TRM11(I) = E11 * TRM9(I)
 60 CONTINUE
    PRINT 101
101 FORMAT(88H0THE ABSOLUTE VALUE OF TRM1 AND TRM2 SHOULD BE MUCH LESS
  1 THAN EITHER TRM3, TRM4, OR TRM5)
    PRINT 102
102 FORMAT(71H THE ABSOLUTE VALUE OF TRM6, TRM7, AND TRM8 MUST BE MUCH
  1 LESS THAN TRM9)
    PRINT 103
103 FORMAT(57H THE ABSOLUTE VALUE OF TRM10 MUST BE MUCH LESS THAN TRM1
  11)
    PRINT 105
105 FORMAT(52H0THE DIFFERENTIAL TERMS IN THE MOMENTUM EQUATION ARE)
    PRINT 106
106 FORMAT(3H0 I,8X,4HTRM1,16X,4HTRM2,16X,4HTRM3,16X,4HTRM4,16X,4HTRM5
  1)
    DO 110 K = 1,NT1,IPRINT
    PRINT 108, K, TRM1(K), TRM2(K), TRM3(K), TRM4(K), TRM5(K)
```

```
108 FORMAT(1X,I3,5E20.8)
110 CONTINUE
    PRINT 115
115 FORMAT(50HOTHE DIFFERENTIAL TERMS IN THE ENERGY EQUATION ARE)
    PRINT 116
116 FORMAT(3HO I,8X,4HTRM6,16X,4HTRM7,16X,4HTRM8,16X,4HTRM9)
    DO 120 K = 1,NT1,IPRINT
    PRINT 118, K, TRM6(K), TRM7(K), TRM8(K), TRM9(K)
118 FORMAT(1X,I3,4E20.8)
120 CONTINUE
    PRINT 125
125 FORMAT(48HOTHE DIFFERENTIAL TERMS IN THE FUEL EQUATION ARE)
    PRINT 127
127 FORMAT(3HO I,8X,5HTRM10,15X,    5HTRM11)
    DO 130 K = 1,NT1,IPRINT
    PRINT 129, K, TRM10(K), TRM11(K)
129 FORMAT(1X,I3,2E20.8)
130 CONTINUE
    RETURN
    END
```

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FIGURE CAPTIONS

Fig. 1 Infinite slab fuel element.

Fig. 2 Slab fuel element temperature transient.
— — Adiabatic with time-dependent heat source.
—— Heat removal by conduction only with a constant heat source.

Fig. 3 Temperature profile surrounding the slab fuel element with heat removal only by conduction to quiescent air (t = 300 sec).

Fig. 4 Nuclear reactor configuration to which the conservation equations are applied.

Fig. 5 Natural convection with air temperature transient.

Fig. 6 Natural convection with steam temperature transient.

Fig. 7 Comparison of natural convection with air and adiabatic temperature transients. — — Adiabatic, —— Natural convection.

Fig. 8 Comparison of natural convection with steam and adiabatic temperature transients.

Fig. 9 Time to fuel and clad melting versus operating power density.

Fig. 10 Maximum fuel temperature occurring during the transient versus power density.

Fig. 11 Reynolds number at core inlet and exit for air coolant.
—— Inlet, — — Exit.

Fig. 12 Reynolds number at core inlet and exit for steam coolant.
—— Inlet, — — Exit.

Fig. 13 Maximum coolant temperature for air coolant transient.

Fig. 14 Maximum coolant temperature for steam coolant transient.

Fig. 15 Natural convection temperature transient without external piping.
Coolant: Steam; Q: 147.5 cal/cm^3-sec.

Fig. 16 Comparison of maximum fuel temperatures and Reynolds numbers for the laminar and turbulent regimes.
Coolant: Steam; Q: 147.5 cal/cm^3-sec; L_{eq}: 400 cm.
—— Inlet, — — Exit.

Fig. 1-F Parallel-flow path system used to evaluate the consequences of the constant-radial-power assumption.

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