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CODE DEVELOPMENT AND ANALYSIS PROGRAM

FRAPCON-1: A COMPUTER CODE FOR THE STEADY STATE ANALYSIS OF OXIDE FUEL RODS

August 1978

EG&G Idaho, Inc.

IDAHO NATIONAL ENGINEERING LABORATORY

DEPARTMENT OF ENERGY

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FRAPCON-1:
A COMPUTER CODE FOR THE STEADY-STATE
ANALYSIS OF OXIDE FUEL RODS

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ABSTRACT

FRAPCON is a FORTRAN IV computer code which predicts the steady state long-term burnup response of a light water reactor fuel rod. The coupled effects of fuel and cladding deformation, temperature, and internal gas pressure on the behavior of the fuel rod are considered in determining fuel rod response. The cladding deformation model includes multi-axial, elasto-plastic analysis and considers both primary and secondary creep. The fuel temperature model considers the effects of fuel cracking and relocation in determining the fuel temperature distribution. Burnup dependent fission gas generation and release is included in calculating fuel rod internal pressure. An integral fuel rod failure subcode determines failure and failure modes based on the operating conditions at each timestep.

The material property subcode, MATPRO, provides gas, fuel and cladding properties to the computational subcodes in FRAPCON. No material properties need to be supplied by the code user.

FRAPCON is a completely modular code with each major computational subcode isolated within the code and coupled to the main code by subroutine calls and data transfer through argument lists. FRAPCON is soft-coupled to the transient fuel rod code, FRAP-T, to provide initial conditions to initiate analysis of such off-normal transients as a loss-of-coolant accident. The code is presently programmed and running on a CDC 7600 computer.

SUMMARY

FRAPCON is a FORTRAN IV computer code developed to predict the steady state behavior of nuclear fuel rods during long-term burnup operations. FRAPCON includes the coupled effects of thermal, mechanical, and internal gas pressure in the analysis of fuel rod behavior. This code is part of a continuing development program sponsored by the Nuclear Regulatory Commission designed to produce analytical tools for accurate prediction of nuclear reactor system behavior during normal and off-normal operating conditions. The code described in this document, FRAPCON-1, is the first in the FRAPCON series of fuel rod codes planned for periodic release, with each succeeding version incorporating the most recent advancements that have been made in fuel rod response modeling. The development of FRAPCON is a combined effort of the Idaho National Engineering Laboratory (INEL) and Pacific Northwest Laboratory (PNL). The code is a development combining features from FRAP-S^[1] and GAPCON-THERMAL^[2].

FRAPCON is presently programmed and running on the CDC 7600 computer. The transient fuel rod analysis code, FRAP-T, can accept history dependent initial conditions from FRAPCON to initiate an off-normal transient. Both codes are being developed with common subcodes and compatible input-output features.

FRAPCON is a modular code. Each type of computation, such as the internal gas pressure computation, is performed by an analytical model programmed in a separate subcode or subroutine. This configuration is designed to allow maximum versatility in developing and modifying the code with minimum impact on the unmodified portion of the code, and permits the exchange of "best estimate" models with "conservative" models as analysis needs dictate.

A major subcode of FRAPCON is the material properties subcode MATPRO. This subcode is composed of modular function subprograms and subroutines which define the material properties required by the computational subcodes of FRAPCON. Each function subprogram or subroutine defines only one material property.

Each successive version of FRAPCON is checked by two forms of verification. Developmental verification is performed during the development process and is limited in scope, whereas independent verification compares the code predictions with a large experimental data base.

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I. INTRODUCTION

FRAPCON is a FORTRAN IV computer code developed to describe the steady state long-term burnup response of oxide fuel rods in light water reactors (LWR). In addition, the code is designed to generate initial conditions for transient fuel rod accident analysis using versions of the transient analysis code FRAP-T^[3]. This effort is part of the safety analysis code development program being sponsored by the Office of Water Reactor Safety Research of the Nuclear Regulatory Commission (NRC).

The code described in this report (FRAPCON-1) is the first of a series of steady state fuel rod codes planned for release at yearly intervals with each succeeding version incorporating the most recent advancements made in fuel rod response analysis models.

FRAPCON is a modular code with each type of computation and analytical model, such as internal gas pressure, contained in a separate module or subroutine. This configuration is designed to allow maximum versatility in development and to permit modification of a module without impacting the unmodified portion of the code.

A major subcode of FRAPCON is MATPRO^[4], see Appendix E. This subcode is comprised of modular function subprograms and subroutines which define the material properties required by the computational subcodes of FRAPCON. Each function subprogram or subroutine defines a single material property.

The FRAPCON developmental process includes two separate verification efforts designed both to provide configuration control and to test the analytical capability of the code. They are Developmental Verification and Independent Verification. Developmental Verification is an ongoing verification process whereby model additions and changes are checked for both model correctness and for any perturbation of the

overall computer code. Independent Verification is a verification process which takes place after a code version has been frozen to further changes and involves comparison with a large amount of fuel rod response data. The results of the comparison are presented in a FRAPCON verification report released at a later date.

The basis of the FRAPCON-1 CODE is FRAP-S3^[1] modified for dynamic dimensioning and with the addition of the fuel temperature subcode from the GAPCON-THERMAL^[2] computer code. In addition FRAPCON contains other improvements over FRAP-S3. These improvements are summarized in Table I. Improvements in FRAPCON are made on a continuing basis. If programming errors are detected in the use of this version of FRAPCON, notification of one of the authors would be greatly appreciated.

This document describes the FRAPCON-1 code, the models used in the code, the input instructions and examples of code input and output. Section II describes the code, the input information needed, the general methods by which the solution is achieved, and the output information a user would expect to obtain. Section III describes the individual models divided into three categories: temperature response, pressure response, and deformation response. Appendices contain a user input manual (Appendix A), a FRAPCON example problem (Appendix B), a description of the configuration control procedure used at EG&G Idaho, Inc. to document analytical codes (Appendix C), additional temperature subcode information (Appendix D), and a description of the material properties used (Appendix E).

TABLE I
DIFFERENCES BETWEEN FRAPCON AND FRAP-S3

	FRAP-S3	FRAPCON
Heat Conduction	Stacked 1-D radial using $\rho k d T$, effective fuel thermal conductivity	Stacked 1-D radial using Method of Weighted Residuals, effective fuel thermal conductivity
Fuel, cladding and gas properties	MATPRO-9	MATPRO-10A
FRAP-T Links	FRAP-S3/T2,T3,T4	FRAPCON/T5
Programming features	Modular coding	Modular coding, Dynamically dimensioned with respect to nodalization and power-time steps, modified code iteration structure to optimize efficiency

II. GENERAL CODE DESCRIPTION

FRAPCON has been developed to predict the behavior of fuel rods during long-term irradiation and to calculate initial conditions for transient accident analyses performed using the FRAP-T code. The code predicts temperatures, internal gas pressure, and fuel and cladding deformation information at each of the user specified axial nodes. The user specifies fuel rod fabrication information, power history and profile, and coolant boundary conditions. Calculations can be performed for rods which are expected to experience varying axial power distributions. The code also predicts failure probability. The input information, calculational scheme, and output information available are described below.

1. INPUT INFORMATION

The input required for using the FRAPCON code consists of the data describing the fuel rod fabrication, core thermal and hydraulic conditions, and the expected irradiation history, namely:

- (1) Fuel rod initial fabrication information (UO_2 or PUO_2 enriched fuel):
 - (a) Cladding material
 - (b) Cladding outside diameter
 - (c) Cladding inside diameter
 - (d) Pellet diameter
 - (e) Percent of fuel theoretical density (based on 10.97 gm/cc as theoretical)

- (f) Fuel enrichment
 - (g) Fuel stack length
 - (h) Pellet height
 - (i) Dish depth
 - (j) Dish shoulder width
 - (k) Plenum length
 - (l) Hold-down spring (coil) diameter
 - (m) Hold-down spring wire diameter
 - (n) Total number of spring turns
 - (o) Fill gas pressure and makeup.
- (2) Core coolant boundary conditions:
- (a) Primary loop system pressure as a function of time
 - (b) Coolant mass flow rate as a function of time
 - (c) Heated diameter of the coolant channel
 - (d) Core inlet water temperature as a function of time.
- (3) Rod power:
- (a) Axial power distribution as a function of time
 - (b) Rod average power as a function of time.

This set of input information provides a basic set upon which the user can expand if he has more information available (see "Optional Input" of Appendix A, Section 2).

2. CALCULATION AND NUMERICAL PROCEDURE

The FRAPCON code iteratively calculates the interrelated effects of fuel and cladding temperature, rod internal pressure, fuel and cladding deformation, release of fission product gases, fuel swelling and densification, cladding thermal expansion and irradiation-induced growth, cladding corrosion, and crud deposition as functions of time and specific power.

The fuel rod power history is approximated by a series of steady state power levels with instantaneous jumps from one power level to another. The length of the rod is divided into a user specified number of axial segments called nodes, each assumed to operate at a single set of conditions over its length. The axial flux (power) shape is input and may be varied as a function of time. Fuel and cladding temperatures, fuel swelling, densification, thermal expansion, cladding thermal and irradiation induced growth, cladding stresses and strains, and fission gas releases are calculated separately for each axial node. The fission gas release and fuel and cladding deformations are then integrated over the length of the fuel rod and added to previous power step values to obtain the rod internal pressure. This pressure is fed back into the fuel and cladding elastic and plastic deflection calculations in subsequent iterations for purposes of evaluating thermal expansion, fuel swelling, densification, and fission gas release, within each axial node.

This section describes the program numerical sequence and the convergence checks made at various points in FRAPCON. A flow diagram of the code is given in Table II.

TABLE II FRAPCON flow diagram

Due to the large number of interactions in the physical behavior of a fuel rod, several iterative processes are nested in the calculations. The major iterative loops are:

- (1) At each axial node the gap and fuel temperatures, the fuel thermal expansion, and the cladding strain, are iterated upon until convergence is obtained,
- (2) At each specified time step the entire rod conditions are recalculated, with iterations continued until gas release and rod internal pressure are unchanged.

The code calculations begin with bulk coolant temperatures determined by a single-channel enthalpy rise evaluation. Then, for each given axial node of the rod, the cladding outside surface temperature is calculated using either the Dittus-Boelter^[5] formula for subcooled heat transfer or the Jens-Lottes^[6] correlation if nucleate boiling is present. A crud temperature rise and an oxide temperature rise are also considered. The cladding corrosion rate (oxide thickness) calculated in MATPRO is dependent on system conditions, boiling or subcooled liquid. The temperature rise across the cladding is calculated as a function of power level and cladding dimensions. The gap conductivity between the pellet surface and the cladding inside diameter is calculated as a function of the composition and pressure of the internal gas mixture and the contact pressure between the fuel and cladding. The fuel surface temperature is calculated using this conductivity. The radial temperature distribution through the pellet is calculated using the Method of Weighted Residuals with the fuel surface temperature for one boundary condition and zero slope at the fuel center for the other. The fuel conductivity is corrected to account for fuel cracking.

After the temperatures in the fuel are calculated, the fission gas fractional release is calculated using the diffusion trapping

model of Weisman and MacDonald^[7]. The quantity of generated fission gases in the fuel is calculated as a function of fuel burnup (the burnup is calculated for each subvolume from the input power/time and axial flux shapes and from the burnup dependent pellet radial flux depression). The dish, pellet-to-cladding gap, fuel crack, fuel open porosity, and plenum volumes are calculated considering the various fuel and cladding thermal, stress induced, and fission product induced deformation. Then the total fission product gas release and the rod internal pressure is calculated using the ideal gas law. The plenum temperature calculation considers heat flow from the top of the fuel stack, from the coolant channel, and gamma heating in the hold-down spring.

The new pressure and gas composition are used in the next iteration to recalculate the heat transfer across the pellet to cladding gap. The cladding stresses, strains, and interface pressures or gaps are computed by the FRACAS^[8] subcode.

3. OUTPUT INFORMATION

FRAPCON calculates the following quantities at each input power-time step:

- (1) Coolant bulk and cladding axial temperature distribution
- (2) Temperature distribution in the fuel pellets at eleven radial nodes and all axial nodes
- (3) Zircaloy cladding corrosion and hydriding at each axial node
- (4) Fuel swelling, densification, restructuring, and thermal expansion at each axial and radial node
- (5) Irradiation-induced cladding growth at each axial node

- (6) Pellet-cladding gap or contact pressure at each axial node
- (7) Pellet to cladding gap at each axial node
- (8) Cladding stresses and strains at each axial node including cladding creep
- (9) Krypton, xenon, and helium generation and release at each node
- (10) Plenum temperature
- (11) Total fuel rod internal pressure.

This information as well as many other associated quantities are included in optional code output.

4. FRAPCON/FRAP-T LINK

For realistic evaluation of fuel rod response during a reactor accident transient such as a loss-of-coolant accident (LOCA), a realistic set of initial conditions, based on a real or hypothetical reactor history, is required to initiate the transient analysis. Therefore, one of the primary reasons for developing FRAPCON is to provide a computational tool which is compatible with the transient accident analysis code FRAP-T^[3]. Consistent computational models in the two codes are required for a smooth transition between the predictions of the two codes at initiation of the transient.

The link between FRAPCON and FRAP-T is accomplished by having FRAPCON generate a magnetic tape containing the necessary information in the format consistent with FRAP-T requirements. The following data are stored on tape and passed to FRAP-T as initial conditions:

- (1) Gap pressure (all axial nodes)

- (2) Radial gap thickness, thermal (all axial nodes)
- (3) Interfacial pressure (all axial nodes)
- (4) Fuel outside radius (all axial nodes)
- (5) Total moles of gas in the rod
- (6) Temperature distribution (all axial nodes)
- (7) Node location (all axial nodes)
- (8) Fuel surface displacement due to densification and swelling (all axial nodes)
- (9) Mole fractions of fuel rod gases
- (10) Local burnup (all axial nodes)
- (11) Peak average cladding temperature attained (all axial nodes)
- (12) Permanent cladding strains (all axial nodes)
- (13) Volume fraction of open porosity (all axial nodes)
- (14) Zircaloy oxide thickness (all axial nodes)

These quantities provide for a smooth coupling of the steady state and transient codes at any of the FRAPCON computed power-time steps. This same mechanism is also used in links to appropriate versions of RELAP^[9].

III. DESCRIPTION OF FRAPCON MODELS

The major interactive response regimes considered in FRAPCON are temperature response, pressure response, and mechanical response. These responses are closely coupled. Two iterative loops are required to find an equilibrium solution between the predictions of each regime. The failure subcode uses the information from the equilibrium solution to establish failures and failure probabilities. The models and equations used to describe these response regimes are presented in this section.

1. FUEL ROD TEMPERATURE RESPONSE

The temperature calculations are based on models which predict coolant, cladding and fuel temperature as well as the plenum temperature. Models for temperature drops across the coolant film, crud, zircaloy-oxide, and the gap are included. Figure 1 shows the temperature as it drops from the fuel centerline, T_{fc} to the bulk coolant, T_b .

Fig. 1 Fuel rod temperature distribution

1.1 Assumptions

The calculation of the temperature distribution is based on the following assumptions:

- (1) Axial heat conduction is negligible
- (2) circumferential heat conduction is negligible
- (3) Steady state boundary conditions during each time step
- (4) Gamma heating effects are negligible.

1.2 Fuel Rod Surface Temperature

The fuel rod surface temperature is calculated by first finding the coolant temperature and then summing the temperature drops across the coolant film, crud, and zircaloy-oxide.

1.2.1 Coolant Conditions. FRAPCON performs a single, closed channel coolant temperature calculation at each axial node along the rod. Bulk temperatures at these positions are determined according to the following expression:

$$T_b(z) = T_{in} + \int_0^z \left[\frac{4\dot{q}''(z)}{C_p G D_e} \right] dz \quad (1)$$

where

$T_b(z)$ = bulk coolant temperature at axial position (K)

T_{in} = inlet coolant temperature (K)

$\dot{q}''(z)$ = rod surface heat flux at axial position (W/m^2)

C_p = heat capacity of the coolant (J/kg-K)

G = coolant mass flux (kg/s-m²)

D_e = coolant channel heated diameter (m)

The rod surface temperature at axial elevation z is taken the minimum value of the following expressions:

$$T_w(z) = T_b(z) + \Delta T_f(z) + \Delta T_c(z) \quad (2)$$

$$T_w(z) = T_{sat} + \Delta T_{JL} \quad (3)$$

where

$T_w(z)$ = rod surface temperature at z (K)

$\Delta T_f(z)$ = forced convection film temperature drop at z (K)

$\Delta T_c(z)$ = crud temperature drop (K)

T_{sat} = coolant saturation temperature (K)

ΔT_{JL} = nucleate boiling temperature drop (K)

The forced convection temperature drop across the coolant film layer at the rod surface is based on the expression

$$\Delta T_f(z) = \dot{q}''(z)/h_f \quad (4)$$

where h_f is the Dittus-Boelter^[5] film conductance given by

$$h_f = (0.023k/D_e) R_e^{0.8} P_r^{0.4} \quad (5)$$

where

h_f = conductance (W/m^2-K)

k = conductivity of the water ($W/m-K$)

D_e = coolant channel heated diameter (m)

Re = Reynold's number

Pr = Prandtl number

The crud temperature drop is computed from

$$\Delta T_c = \dot{q}''(z) \frac{\delta_c}{k_c} \quad (6)$$

where

δ_c = crud thickness (m)

k_c = crud thermal conductivity ($0.8648 W/m-K$)

The nucleate boiling temperature drop is based on the Jens-Lottes^[6] formulation:

$$\Delta T_{JL}(z) = 60 [\dot{q}''(z)/10^6]^{0.25} / e^{(P/6.2 \times 10^6)} \quad (7)$$

where

P = system pressure (pascals)

No additional temperature rise occurs due to crud deposition because the coolant is assumed to boil through the crud blanket when nucleate boiling occurs.

1.2.2 ZrO₂ Temperature Drop. For zircaloy clad rods, the temperature drop across the zirconium oxide layer at axial station z is determined by the expression

$$\Delta T_{\text{oxide}}(z) = \frac{\dot{q}''(z) \delta_{\text{oxide}}(z)}{k_{\text{oxide}}} \quad (8)$$

where

$\Delta T_{\text{oxide}}(z)$ = oxide temperature drop (K)

$\delta_{\text{oxide}}(z)$ = oxide thickness at axial position (m)

k_{oxide} = oxide thermal conductivity (W/m-K)

1.3 Cladding Temperature Drop

The cladding temperature drop for each axial location is calculated according to the expression for steady state heat transfer through a cylinder with uniform thermal conductivity:

$$\Delta T_{\text{clad}} = \dot{q}''(z) r_o \ln(r_o / r_i) / k_{\text{clad}} \quad (9)$$

where

ΔT_{clad} = cladding temperature drop (K)

r_o = cladding outside radius (m)

r_i = cladding inside radius (m)

k_{clad} = temperature and material dependent thermal conductivity of the cladding (W/m-K).

1.4 Gas Gap Temperature Drop

FRAPCON contains two different gap conductance options. The first is based on a cracked pellet geometry^[10] model and the second is a modification of the Ross and Stoute^[11] model.

1.4.1 Cracked Pellet Model. This gap conductance model computes an effective gap conductance by accounting for open gap and closed gap contributions. The model has been compared with experimental data to determine the correct value of the various constants.

(1) Open Gap. If the fuel and cladding are not in contact an eccentric cladding-fuel geometry is modeled. The fraction of the pellet circumference in contact with the cladding is modeled by the following empirical equation:

$$F = \frac{1}{a_1 \left[\frac{100 \Delta D}{D_F} \right]^{a_2} + a_3} + \left(1 - \frac{1}{a_3} \right) \quad (10)$$

where

F = fraction of pellet in contact with the cladding

ΔD = diametral gap (m)

D_F = hot diameter of the fuel pellet (m)

a_1 , a_2 , and a_3 = empirical constants determined from data.

The form of Equation (10) permits a very large fraction of pellet-cladding contact for a small calculated diametral gap. The

justification for this form of the equations is that the cracks (radial and circumferential) that form in the fuel pellets will not close appreciably until the gas gap is closed. Thus the actual gas gap will close before the calculated gap.

Heat transfer across the gas gap is considered to occur by conduction through the gas in the open gap plus zero pressure contact conductance times that fraction of the gap circumference with pellet cladding contact. Radiation heat transfer across the open gap is assumed negligible, since reasonable gaps are always small and thus sufficiently large temperature differences between the fuel and cladding do not exist. The effective heat transfer coefficient for the gas gap is therefore calculated from the equation

$$h_{\text{gap}} = (1 - F) h_1 + F h_2 \quad (11)$$

where

$$h_{\text{gap}} = \text{net gap conductance (W/m}^2\text{-K)}$$

$$h_1 = \text{open gap conductance (W/m}^2\text{-K)}$$

$$h_2 = \text{zero pressure contact conductance (W/m}^2\text{-K)}$$

When the gap is open, the gap conductance is determined from equations

$$h_1 = \frac{k_{\text{mix}}}{\Delta r' + \delta} \quad (12)$$

and

$$\Delta r' = r_1 - r_2 \quad (13)$$

where

$$k_{\text{mix}} = \text{thermal conductivity of gas mixture (W/m-K)}$$

$\Delta r'$ = average radial gap thickness of eccentric gas gap (m)

δ = root mean square of the fuel cladding surface roughness (m). A value of 4.39×10^{-4} m is used

r_1 = hot calculated inside radius of cladding (m)

r_2 = hot calculated radius of fuel (m).

(2) Closed Gap. When the fuel and cladding are in contact, the theory of thermal contact conductance for ceramic fuel elements developed by Jacobs and Todreas^[12], is used. The governing equation for contact conductance is taken as

$$h_{\text{gap}} = C_1 p^n + \frac{k_{\text{mix}}}{\delta} \quad (14)$$

where

h_{gap} = net gap conductance ($\text{W/m}^2\text{-K}$)

C_1 = 7155.1 for zircaloy-2 and zircaloy-4 cladding

p = pellet-cladding contact pressure (pascals)

n = 1.0 for $0.0 \leq p \leq 6.89475 \times 10^6$ (pascals) and
0.219 for $p \geq 6.89475 \times 10^6$ (pascals).

The value of the exponent, n , is governed by the material behavior at the interface of the fuel and cladding contact points. An exponent of 1.0 is consistent with the Ross and Stoute^[11] theory of contact conductance only if the surface peaks of one of the materials are flowing plastically. If the contact points of both materials are

behaving elastically the value for the exponent, n , is approximately 0.219. The experimental results of Fenech and Rohsenow^[13] support this value and also indicate that for metal-ceramic pairs the transition pressure from elastic to plastic flow is approximately 6.8948×10^6 pascals. The parameter k_{mix}/δ accounts for the heat conduction through the gas in the gaps between contact points.

1.4.2 Ross and Stoute Model. The model is a modification of the Ross and Stoute^[11] work. It considers heat transfer across a uniform annular gap. Separate equations are used for the open and closed gap situations.

(1) Open Gap. If the fuel and cladding are not in contact, heat is transferred across the gas gap by conduction through the gas and by radiation. Heat transfer across the gas gap is considered to be governed by the equation

$$h_g = \frac{k_g}{t_g + c(R_f + R_c) + (g_1 + g_2)} + h_r \quad (15)$$

where

h_g = gap conductance ($\text{W/m}^2\text{-K}$)

k_g = conductivity of gas in gap (W/m-K)

t_g = gap thickness (m)

g_1 = temperature jump distance at cladding inside surface (m)

g_2 = temperature jump distance at fuel outside surface (m)

$$c = 1.98$$

$$R_c = \text{arithmetic mean roughness of the cladding (m)}$$

$$R_f = \text{arithmetic mean roughness of the fuel (m)}$$

$$h_r = \text{radiant heat transfer conductance (W/m}^2\text{-K)}.$$

Radiant heat transfer conductance is computed using the following equation:

$$h_r = \sigma F_e (T_f^2 + T_c^2)(T_f + T_c) \quad (16)$$

where

$$h_r = \text{radiant heat transfer conductance (W/m}^2\text{-K)}$$

$$\sigma = \text{Stefan-Boltzman constant (W/m}^2\text{-K}^4)$$

$$F_e = \text{emissivity factor}$$

$$T_f = \text{temperature of outside surface of fuel (K)}$$

$$T_c = \text{temperature of inside surface of cladding (K)}.$$

The emissivity factor for two concentric cylinders is computed by the equation (See Reference 14)

$$F_e = \left[\frac{1}{e_f} + \frac{r_f}{r_c} \left(\frac{1}{e_c} - 1 \right) \right]^{-1} \quad (17)$$

where

$$F_e = \text{emissivity factor}$$

- e_f = emissivity of fuel surface
 e_c = emissivity of cladding inside surface
 r_f = outside radius of fuel (m)
 r_c = inside radius of cladding (m).

The temperature jump distance is computed by an empirically derived equation presented in the GAPCON code report^[2]. The equation is

$$g_1 + g_2 = 3.756 \times 10^4 \frac{\mu}{P} \left(\frac{T}{M}\right)^{1/2} \quad (18)$$

where

- $g_1 + g_2$ = jump distance (m)
 μ = viscosity of gas (gm/cm-sec)
 P = pressure of gas (pascals)
 T = temperature of gas (K)
 M = molecular weight of gas.

(2) Closed Gap. If the fuel and cladding are in contact, the GAPCON code equation for contact conductance is used. This equation agrees with gap conductance data presented by Ross and Stoute^[11]. The equation is

$$h_g = \frac{K_m P_i}{a_o R^{0.5} H} + \frac{k_g}{C(R_f + R_c) + (g_1 + g_2)} + h_r \quad (19)$$

where

$$h_g = \text{gap conductance (W/m}^2\text{-K)}$$

$$K_m = \frac{2k_f k_c}{k_f + k_c}$$

$$k_f = \text{fuel conductivity (W/m-K)}$$

$$k_c = \text{cladding conductivity (W/m-K)}$$

$$P_i = \text{interfacial pressure between fuel and cladding (pascals)}$$

$$a_o = \text{a constant -- } 0.005(\text{m}^{1/2})$$

$$R = \frac{(R_f^2 + R_c^2)^{1/2}}{2} \text{ (m)}$$

$$k_g = \text{conductivity of gas in gap (W/m-K)}$$

$$h_r = \text{radiant heat transfer conductance (W/m}^2\text{-K)}$$

$$R_c = \text{arithmetic mean roughness of cladding (m)}$$

$$R_f = \text{arithmetic mean roughness of fuel (m)}$$

$$H = \text{Meyer-Hardness of cladding (pascals).}$$

The coefficient C in Equation (19) is computed by the empirical equation

$$C = 1.98 e^{-12.5P_i} \quad (20)$$

where

P_i = interfacial pressure between fuel and cladding
(kg/m²).

1.5 Pellet Heat Conduction

The pellet temperature distribution is calculated in subroutine DDLF (developed by Don D. Lanning) using the Method of Weighted Residuals. The method is described below.

1.5.1 General Procedure. This subroutine solves the steady state radial heat transfer problem in the fuel. The fuel is envisioned to be a homogeneous, continuous, right circular cylinder with a constant surface temperature about its perimeter. The method used can easily be extended to handle a restructured zone and even azimuthally varying surface temperature or heat generation rate, but the present version does not extend to these complications. The radial form of the heat transfer equation in cylindrical coordinates is:

$$\frac{d^2T(r)}{dr^2} + \frac{1}{r} \frac{dT}{dr} + \frac{1}{K(T)} \frac{dK}{dT} \left(\frac{dT}{dr} \right)^2 + \frac{Q(r)}{K} = 0 \quad (21)$$

where $Q(r)$, is the volumetric heat generation rate and K is the conductivity. The equation is non-linear because of the temperature dependence of the conductivity. The boundary conditions are:

$$\left. \frac{dT}{dr} \right|_{r=r_I} = 0 \quad (22)$$

and

$$T|_{r=r_{FS}} = T_{FS} \quad (23)$$

where r_I and r_{FS} are the fuel inner and outer radii, respectively, and T_{FS} is the fuel surface temperature.

There are several ways of solving equation (21). The method used in DDLF is a collocation technique using the Method of Weighted Residuals, and has the following advantages: 1) the solution time on a computer is very short; 2) coefficients are produced which permit calculation of correct temperatures at any point in the fuel without resolving the entire problem. The specific steps used are a slight variation of the method proposed by B. A. Finlayson¹⁵, and proceed as follows:

- (1) A trial solution of the form $T = \sum_{j=1}^{N+2} d_j r^{j-1}$ is assumed, where d_j are unknown coefficients.
- (2) To find estimates of the d_j , equation (21) is forced to be satisfied at N "collocation" points within the fuel. This requirement plus the boundary condition results in $N+2$ simultaneous equations for the $(N+2)$ d_j . Fourth order (4 point) collocation has been found to yield accuracy within 1°C compared to exact solutions. Thus $4+2=6$ coefficients are generated*.
- (3) In order to solve the 6 simultaneous equations, the terms involving conductivity are considered "known" by evaluating the conductivity using temperatures from the previous iteration.
- (4) The procedure is repeated to convergence. The temperatures for the very first iteration are calculated using a constant typical value for the conductivity as discussed in Appendix D.

* The "residual" is the function formed by substitution of the trial function into equation (21). Forcing the residual to be zero at certain points weights this function, hence the name "weighted residual." As Finlayson suggests, the roots of the N th order shifted Legendre polynomial are the most efficient collocation points. This is true for a general class of equations of which the radial heat transfer equation is a member.

1.5.2 Formulation of Equations for d_j . To avoid reformulating the equations for each gap conductance iteration, it is convenient to perform the calculations in normalized coordinate

$y = r/r_{FS}$. With this variable change, equation (21) becomes:

$$\frac{d^2 T(y)}{dy^2} + \frac{1}{y} \frac{dT}{dy} + \frac{1}{K(T)} \frac{dK}{dT} \left(\frac{dT}{dy} \right)^2 + \frac{Q(y)r_{FS}^2}{K} = 0 \quad (24)$$

with boundary conditions:

$$\left. \frac{dT}{dy} \right|_{y=y_K} = 0 \quad (25)$$

$$T|_{y=1} = T_{FS} \quad (26)$$

Using the collocation technique, we formulate six simultaneous equations as follows:

$$\begin{aligned} \frac{dT_n}{dy} &= 0 & i &= 1 \\ \left. \frac{d^2 T_n}{dy^2} \right|_{y_i} + \frac{1}{y_i} \frac{dT_n}{dy} \Big|_{y_i} &= \frac{1}{K_{n-1}} \left[r_{FS}^2 Q_i + \left(\frac{dK_{n-1}}{dT_i} \right) \left(\left. \frac{dT_{n-1}}{dy} \right|_{y_i} \right)^2 \right] & i &= 2-5 \\ T_n|_{y=1} &= T_{FS} & i &= 6 \end{aligned}$$

where subscripts n and $n-1$ refer to the current and previous iteration, respectively, and

$$\begin{aligned} Q_i &= Q(y_i), \quad K_{n-1} = K(T_{n-1}) \\ \text{and } \frac{dK_{n-1}}{dT_i} &= \left. \frac{dK}{dT} \right|_{T_{n-1}(y_i)} \end{aligned}$$

Now equations for $i = 2-5$ need to be modified by the elimination of the term

$$\left(\frac{dT_{n-1}}{dy} \Big|_{y_i} \right)^2$$

The most accurate way to do this is to appeal to the relationship

$$\begin{aligned} -K \frac{dT}{dr} &= \text{surface heat flux at } r \\ &= 2\pi \int_{r_I}^r Q(r) r dr / 2\pi r \end{aligned} \quad (27)$$

Now $Q(r)$ is expressed as $Q(r) = N(Z + YYr^2 + Wr^4)$ where N is a normalization constant defined later. Equation (27) is thus

$$\begin{aligned} -K \frac{dT}{dr} &= \frac{-N}{r} \left[\frac{Z}{2} (r^2 - r_I^2) \right. \\ &\quad \left. + \frac{YY}{4} (r^4 - r_I^4) + \frac{W}{6} (r^6 - r_I^6) \right]. \end{aligned} \quad (28)$$

Transforming to normalized coordinates, and solving for $\frac{dT}{dy} \Big|_{y_i}$ we have,

$$\begin{aligned} \frac{dT}{dy} \Big|_{y_i} &= - \left(\frac{N r_{FS}^2}{y_i K} \right) \left[\frac{Z}{2} (y_i^2 - y_I^2) + \frac{YY}{4} (y_i^4 - y_I^4) + \frac{W}{6} (y_i^6 - y_I^6) \right] \\ &\equiv \left[- \frac{N r_{FS}^2}{y_i K} \right] C_i \equiv I_i. \end{aligned} \quad (29)$$

So the collocation equations ($i = 2-5$) can be written as

$$\frac{d^2 T}{dy^2} \Big|_{y_i} + \frac{1}{y_i} \frac{dT}{dy} \Big|_{y_i} = \frac{-1}{K_{n-1}} \left[r_{FS}^2 Q_i + \left(\frac{dK_{n-1}}{dT_i} \right) I_i^2 \right] \quad (30)$$

with I_i defined as above. Substituting in the expansion

$$T_n(y_i) = \sum_{j=1}^6 d_j y_i^{j-1}$$

We have the following six equations:

$$\sum_{j=1}^6 d_j \left[(j-1)y_1^{j-2} \right] = 0 \quad y_1 = y_I$$

$$\sum_{j=1}^6 d_j \left[(j^2 - 2j + 1)y_i^{j-3} \right] = \frac{-1}{K_{n-1}} \left[r_{FS}^2 Q_i + \left(\frac{dK_{n-1}}{dT_i} \right) I_i^2 \right] \quad i=2-5$$

$$\sum_{j=1}^6 d_j = T_{FS}$$

In matrix form, the above set can be written as follows:

$$[A] \begin{pmatrix} d_1 \\ d_2 \\ d_3 \\ d_4 \\ d_5 \\ d_6 \end{pmatrix} = \begin{pmatrix} 0.0 \\ B_2 \\ B_3 \\ B_4 \\ B_5 \\ T_{FS} \end{pmatrix}$$

where

$$\left. \begin{aligned} A_{ij} &= (j-1) y_I^{j-2}, & i &= 1 \\ &= (j^2 - 2j + 1)y_i^{j-3}, & i &= 2-5 \\ &= 1.0 & i &= 6 \end{aligned} \right\} \quad j = 1, 6$$

$$\text{and} \quad B_i = \frac{-1}{K_{n-1}} \left[r_{FS}^2 Q_i + \left(\frac{dK_{n-1}}{dT_i} \right) I_i^2 \right]$$

Notice that in succeeding iterations the matrix A need not be recalculated, but only the B_i .

Additional details of the temperature solution in DDLF and the flux depression calculation are presented in Appendix D.

1.6 Plenum Gas Temperature

The plenum gas temperature is calculated based on energy transfer between the top of the pellet stack and the plenum gas, between the coolant channel and the plenum gas, and between the spring and the plenum gas. The three contributions are discussed in the following pages.

Natural convection from the top of the fuel stack is calculated based on heat transfer coefficients from McAdams^[16] for laminar or turbulent natural convection from flat plates.

The heat transfer coefficient is calculated from

$$h_p = \frac{k}{D} Nu \quad (31)$$

where

h_p = the heat transfer coefficient from the top of the pellet stack to the plenum gas (W/m^2-K)

Nu = Nusselt number

D = inside diameter of the cladding of the top node (m)

k = conductivity of the plenum gas (W/m^2-K).

and the Nusselt number is computed using the equation,

$$Nu = C(GrPr)^m \quad (32)$$

where

$Gr = \leq$ the Grashof number

$Pr = >$ the Prandtl number

and for

$$GrPr \leq 2.0 \times 10^7 \quad C = 0.54 \text{ and } m = 0.25$$

or

$$GrPr > 2.0 \times 10^7 \quad C = 0.14 \text{ and } m = 0.33.$$

The overall effective conductivity from the coolant to the plenum is defined as the inverse of the sum of the individual heat flow resistances. The three encountered here are: 1) across the inside surface film, 2) across the cladding, and 3) across the outside surface film. The overall conductivity is therefore found as follows:

$$U_c = \frac{1.0}{\frac{2.0}{D h_f} + \frac{\ln \left(\frac{D_o}{D_i} \right)}{k_{clad}} + \frac{1.0}{D_o (1.0 + \alpha \Delta T) h_{DB}}} \quad (33)$$

where

U_c = overall effective conductivity from the coolant to the plenum gas (W/m-K)

D = hot state inside cladding diameter (m)

h_f	=	cladding inside surface film coefficient (W/m-K)
D_o	=	cold state outside cladding diameter (m)
D_i	=	cold state inside cladding diameter (m)
k_{clad}	=	cladding thermal conductivity (W/m ² -K)
α	=	coefficient of thermal expansion of the cladding (1/K)
ΔT	=	temperature difference between cladding average temperature and datum temperature (K)
h_{DB}	=	heat transfer coefficient between the coolant and the cladding (W/m-K).

Gamma heating in the hold-down spring is calculated at a volumetric rate of 3.76 W/m³ for every W/m² of rod average heat flux

$$Q_{sp} = 3.76 \dot{q}'' V_s \quad (34)$$

where

Q_{sp}	=	energy generated in the spring (W)
$\dot{q}''$	=	average heat flux of the rod (W/m)
V_s	=	volume of the spring (m ³).

The plenum temperature is then approximated from the following equation:

$$T_{\text{plen}} = \frac{Q_{\text{sp}} + U_c \frac{V_p}{D^2} T_{\text{BLK}} + T_{\text{pa}} h_p \pi D^2 / 4}{U_c \frac{V_p}{D^2} + \frac{h_p \pi D^2}{4}} \quad (35)$$

where

T_{plen} = plenum temperature (K)

V_p = volume of the plenum (m^3)

T_{BLK} = bulk coolant temperature at the top axial node (K)

T_{pa} = temperature associated with the insulator or top pellet (K).

1.7 Effective Fuel Conductivity

The thermal conductivity of cracked fuel cannot be the same as that of uncracked fuel. In order to compute the correct effective thermal conductivity, a relocation factor, R , is applied to the uncracked conductivity as shown below:

$$k_{\text{eff}} = R k_{\text{lab}}$$

where k_{eff} = effective thermal conductivity ($\frac{\text{W}}{\text{m} \cdot \text{K}}$)

k_{lab} = uncracked fuel thermal conductivity as a function of temperature ($\frac{\text{W}}{\text{m} \cdot \text{K}}$)

The relocation factor R is an empirical correlation which accounts for the presence of cracks in the fuel. This correlation was developed from fuel centerline and off-center thermocouple data taken as part of the Gap Conductance Tests performed in the Power Burst Facility at the Idaho National Engineering Laboratory. The equation for R is:

$$R = 1.0 - 7\pi \left(\frac{\delta_s - \delta_T}{\delta_o - 0.005 r_p} \right) \left(\frac{\delta_o}{r_p} \right) \left(1.0 - \frac{k_g}{k_{lab}} \right) \quad (36)$$

where

k_g = gas thermal conductivity ($\frac{W}{m \cdot K}$)

δ_o = as fabricated gap (m)

δ_s = gap assuming no fuel surface relocation (m)

δ_T = gap assuming fuel surface relocation (m)

r_p = as fabricated pellet radius (m)

The terms $\frac{\delta_o}{r_p}$ is a measure of volume initially available for the pellets to crack and relocate into the first ramp to power. The term $(1 - k_g/k_{lab})$ accounts for the fact that the gas in the cracks in the fuel has a different thermal conductivity than the fuel itself, and degrades the effective conductivity of the fuel. The term

$\frac{\delta_s - \delta_T}{\delta_o - 0.005 r_p}$ is a measure of the instantaneous volume available for cracking. This term decreases as the fuel to cladding gap closes. Recognizing that the cracked fuel can never fully reconsolidate, this term is never allowed to be less than 0.75. The constant 7π was chosen to best first the experimental data base for rods containing He, Xe, and Ar.

Crack healing which occurs because of fuel resintering at a transition temperature at or near the fabrication sintering temperature is modeled by enforcing the following:

$$R = 1.0 \text{ when } T_r \geq T_{\text{trans}}$$

where T_f = local fuel temperature (K)

T_{trans} = fuel transition temperature (K)

One assumption made is that cracks instantaneously heal or re crack when fuel regions are above or below the transition temperature respectively. Another assumption is that the relative gas and fuel conductivities are lab values.

1.8 Stored Energy

The stored energy in the fuel rod is calculated by summing the energy of each pellet ring calculated at that ring temperature.

$$E = \frac{\sum_{i=1}^N m_i \int_{298 \text{ K}}^{T_i} C_p(T) dT}{m} \quad (37)$$

where

E = stored energy (J/Kg)

m_i = mass of ring segment i (kg)

T_i = temperature of ring segment i (K)

$C_p(T)$ = specific heat (J/Kg-K)

m = total mass of the axial node (kg)

N = number of annular rings

The stored energy is computed for each axial node and is output as a information item.

2. FUEL ROD INTERNAL GAS PRESSURE RESPONSE

2.1 Assumptions

The fuel rod internal pressure model is based on the following assumptions:

- (1) Perfect gas law holds ($PV = NRT$)
- (2) Gas pressure is constant throughout the fuel rod
- (3) Gas in the fuel cracks is at the average fuel temperature.

2.2 Fuel Rod Internal Pressure

Fuel rod internal gas pressure is computed from the perfect gas law modified to permit different volumes of gas at different temperatures, ie:

$$P = \frac{NR}{\sum \frac{V_i}{T_i}} \quad (38)$$

where V_i are the different volumes and T_i are the corresponding temperatures. In FRAPCON, the different volumes considered are dish volume, crack volume, porosity volume, plenum volume, gap volume, and roughness volume.

In detail the gas law becomes:

$$P_g = \frac{M_g R}{\frac{V_p}{T_p} + \sum_{n=1}^N \left[\frac{(r_{cn}^2 - r_{fn}^2) \Delta z_n}{T_{G_n}} + \frac{V_c \Delta z_n}{T_c} + \frac{V_{por} \Delta z_n}{T_{por}} + \frac{V_{dsh} \Delta z_n}{T_{dsh}} + \frac{V_{rf} \Delta z_n}{V_{rf}} \right]} \quad (39)$$

where

P_g = internal fuel rod pressure (pascals)

M_g = moles of gas in fuel rod (gm-moles)

R = universal gas constant $\left(\frac{n - m}{k\text{-gm-mole}} \right)$

V_p = plenum volume (m^3)

n = axial node number

T_p = temperature of gas in plenum (K)

N = number of axial nodes into which fuel rod is divided for numerical solution

r_{cn} = radius of inside surface of fuel at axial node n (m)

r_{fn} = radius of outside surface of fuel at axial node n (m)

T_{G_n} = temperature of gas in gas gap at axial node n (K)

Δz_n = fuel rod length associated with axial node n (m)

V_c = fuel crack volume per unit length of node n (m^2)

T_c = temperature of the crack volume (K)

V_{por} = open porosity volume per unit length of node n (m^2)

T_{por} = temperature of node n porosity volume (K)

V_{dsh} = dish volume per unit length of node n (m^2)

T_{dsh} = temperature of node n dish volume (K)

V_{rf} = roughness volume per unit length of node n (m^2)

T_{rf} = temperature of node n roughness volume (K).

The gas pressure calculation therefor requires information on the gas inventory, void volumes, and the void temperature which is provided by the following supportive models.

2.2.1 Fission Gas Production. Given production rates for the major diffusing gases, the burnup dependent total fission gas generated at axial station z is calculated as follows:

$$GPT(z) = \frac{BU(z) VF(z)}{100 A_v} (PR_{\text{krypton}} + PR_{\text{helium}} + PR_{\text{xenon}}) \quad (40)$$

where

$GPT(z)$ = total fission gas produced at z (gm-moles)

$BU(z)$ = burnup at z (fiss/cc)

$VF(z)$ = fuel volume (cc)

A_V = Avogadro's Number

PR_e = Fission gas production rate (atoms/100 fissions)
for krypton, xenon, and helium, respectively, 30,
30, and 0.3.

All the fission gas produced, however, is not released. A portion is trapped in the fuel and a portion is released to the gas gap volume. Only that portion released is used to compute the rod internal pressure. The gas release fraction is computed as shown in the following sections.

2.2.2 Fission Gas Release. The fission gas release model used in FRAPCON considers the release to be determined by escape of gas from the fuel matrix and release of trapped gas from grain boundaries or dislocations. If k^1 represents the portion of fission gas that escapes without being trapped, then

$$dn_1 = k^1 p dt \quad (41)$$

where

dn_1 = the moles of gas released directly in time dt

dt = the time increment

p = the gas production rate.

If the probability of trapped particle release per unit time is k and the number of moles trapped is C , then the trapped moles released in dt is $dn_2 = k C dt$. Only a fraction, k^1 , of the gas released from traps reaches the surface, thus the total gas released is
 $dn = k^1 k C dt + k^1 p dt$.

If C is replaced by (pt - n) and integration is performed, one obtains:

$$n = p \quad t - \frac{1 - k^1}{k^1 k} \left\{ [1 - \exp(-(k^1 k t))] \right\} . \quad (42)$$

At constant power, the total fractional release is

$$F = n/(pt) = 1 - (1 - k^1) \frac{1 - e^{-Kt}}{Kt} \quad (43)$$

where $K = k^1 k$. The constants, K and k^1 , have been evaluated from data as functions of fuel temperature and density in the form

$$k, k^1 = Ae(-B/T - Cd + D) \quad (44)$$

where

T = fuel temperature (K)

d = fuel density(% theoretical)

and for K and k^1 respectively:

A = 0.25, 1.0

B = 11894.0, 6916.7

C = 0.0, 0.333

D = 0.0, 33.95

The preceding formulation [Equation (42)] is extended to variable power time histories by assuming reactor operation is described by a series of constant power steps. The number of moles released, Δn_i , during the i^{th} interval is then

$$\Delta n_i = n_i - n_{i-1} = P_i \left\{ \Delta t_i - \frac{1 - k_i^1}{K_i} \left[1 - \exp(-K_i \Delta t_i) \right] \right\} + C_{i-1} \left[1 - \exp(-K_i \Delta t_i) \right] \quad (45)$$

The first two terms, identical with Equation (42) represent the release during Δt_i , had the initial concentration been zero. The last term is additional release due to previously produced gas. Since the total release from time zero is Δn_i , the fraction of total gas produced which is released is

$$F = \left(\sum_{i=1}^m \Delta n_i \right) / \left(\sum_{i=1}^m P_i \Delta t_i \right). \quad (46)$$

This fraction is used along with the total gas production presented earlier to obtain the gas inventory.

2.2.3 Nitrogen Release. The release of nitrogen initially present in fuel material from fabrication occurs as a result of a diffusion transport mechanism. From the model proposed by Booth^[17], given the assumptions that

- (1) The initial concentration of diffusing substance C_0 is assumed to be uniform throughout a sphere of radius a
- (2) Transport of material does not occur from the external phase (gaseous nitrogen) back into the initial carrier medium, the following diffusion expression holds:

$$r \frac{\partial C}{\partial t} = D \frac{\partial^2 (Cr)}{\partial r^2} \quad (47)$$

where

r = radial location

C = concentration of diffusing substance

t = time

D = diffusion coefficient

and

$C = 0.0$ when $r = a$

$C = C_0$ when $t = 0$.

By applying a series solution method, the fractional release of the diffusing substance (nitrogen) can be approximated by the following, based on the value of B .

If

$$B = \pi^2 D_{N_2}(T)t \quad (48)$$

where

$D_{N_2}(T)$ = temperature dependent diffusion coefficient for nitrogen

t = time (sec) from the start of diffusion

then, when $B > 1$, the fraction of nitrogen released as of time t , equals:

$$F_{N_2} = 1 - 6 e^{-B/\pi^2} \quad (49)$$

and when $B \leq 1$

$$F_{N_2} = 6 \left[D_{N_2}(T)t/\pi \right]^{0.5} - 3D_{N_2}(T)t \quad (50)$$

From the experimental data of Ferrari [18,19]:

$$D_{N_2}(t) = (1 \times 10^{-12}) e^G \quad (51)$$

where

$$G = 20202.0 \left(\frac{1.0}{1673.0} - \frac{1.0}{T} \right) \quad (52)$$

and T is in degrees kelvin.

2.2.4 Void Volumes.

(1) Pellet Dish Volume. The volume between pellets is calculated and included as part of the overall volume in the internal pressure model. The interpellet volume is calculated at each time step as the difference between the cold pellet geometry and the hot pellet operating geometry.

Figure 2 shows: (A) a cold pellet interface configuration; and (B) an exaggerated hot pellet interface configuration. The volume available for expansion of internal fill gas is defined by the cross hatched areas (A and B) in the figure.

(2) Gap Volume. The gap volume is calculated assuming the pellet diameter is the relocated value which results in a simply calculated area between two concentric cylinders that is often zero.

(3) Fuel Crack Volume. As the fuel expands, extensive cracking occurs due to the high thermally-induced stresses resulting in a relocated fuel surface. This crack volume is computed as

$$V_c = V_{eg} - V_{TX} - V_g \quad (53)$$

where

V_c = crack volume (m^3)

V_{TX} = fuel volume defined by expanded radial nodes including thermal expansion, swelling, and densification (m^3)

V_g = the computed gap volume based on the relocated fuel surface (m^3)

V_{eg} = the volume within the thermally expanded cladding (m^3)

(a) Cold Pellet Interface

(b) Hot Pellet Interface

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Fig. 2 Pellet expansion

(4) Plenum Volume. The plenum volume is calculated from geometry considerations of the thermally expanded cladding and the thermal expansion, densification, and swelling of the fuel. The volume of the hold-down spring is considered.

(5) Open Porosity Volume. A portion of the initial fabrication porosity is open to free gas flow which is computed as follows:

$$\begin{aligned}
 V_{\text{poros}} &= 0.0 && \text{when } G_{\text{den}} \geq 94.0 \quad (54) \\
 &= 1.97 \times 10^{-8} (94.0 - G_{\text{den}}) && \text{when } 91.25 < G_{\text{den}} < 94.0 \\
 &= 2.77 \times 10^{-4} - 3.818 G_{\text{den}} \\
 &\quad - 1.43 \times 10^{-8} G_{\text{den}}^2 + 2.497 \times 10^{-10} G_{\text{den}}^3 && \text{when } G_{\text{den}} < 91.25
 \end{aligned}$$

where

$$V_{\text{poros}} = \text{porosity volume (m}^3\text{)}$$

$$G_{\text{den}} = \text{DEN} - 1.25$$

$$\text{DEN} = \text{percent of theoretical density}$$

(6) Roughness Volume. The roughness of the surfaces of the fuel and cladding result in a small void volume accounted for in the following.

$$V_{\text{rough}} = \frac{5.27 \times 10^{-5} \pi D_p}{V_f} \quad (55)$$

where

$$V_{\text{rough}} = \text{roughness volume (m}^3\text{)}$$

D_p = pellet diameter (m)

V_f = geometric fuel volume (m^3)

The pressure response resulting from the above models thus feed back into the mechanical response models.

3. FUEL ROD MECHANICAL RESPONSE

3.1 Cladding Deformation.

3.1.1 Assumptions. The cladding deformation model is based upon the following assumptions:

- (1) Incremental theory of plasticity
- (2) Prandtl-Reuss flow rule holds for time-independent plasticity and creep
- (3) Isotropic work-hardening assumption holds for creep under varying stress level
- (4) Thin wall cladding (stress, strain, and temperature are neglected through cladding thickness)
- (5) If fuel and cladding are in contact, no slippage occurs at fuel-cladding interface
- (6) Bending strains and stresses in cladding are neglected
- (7) Axisymmetric loading and deformation of cladding
- (8) Incompressible fuel.

This section describes the models which perform the mechanical analysis of the cladding. The models analyze the elastic and plastic deformation of the cladding due to thermal and pressure loadings, as well as loads due to mechanical interaction between the cladding and the fuel pellets.

The Fuel Rod and Cladding Analysis Subcode (FRACAS) consists of a set of individual subroutines, each of which is independent of the others. Hence, the model contained in each subroutine can be modified or replaced without requiring changes in any other part of the subcode.

Deformation and stresses in the cladding when the fuel-cladding gap is open are computed in subroutine CLADF. The model considered is that of a thin cylindrical shell with specified internal and external pressures and a prescribed uniform temperature.

Calculations when the fuel cladding gap is closed are made in subroutine COUPLE. The model considered is a thin cylindrical shell with prescribed external pressure and a prescribed radial displacement of its inside surface. The prescribed displacement is obtained from the fuel displacement models contained in FRAPCON. Further, since no slip is assumed to take place when the fuel and cladding are in contact, the axial expansion of the fuel is transmitted directly to the cladding, and hence, the change in axial strain in the shell is also prescribed.

A third deformation regime is considered in which a section of fuel is trapped between the lower end of the fuel rod and a pellet which is in firm contact with the cladding. Here, the axial expansion of the fuel is imparted to the cladding even through the fuel-cladding gap is open. This regime is called the "Trapped Stack" regime of cladding deformation.

Calculations for the trapped stack regime are made in subroutine STACK. The model considered is a thin cylindrical shell with prescribed internal and external pressures and a prescribed total change in length of the cylinder. In contrast to CLADF and COUPLE, which solve for the stresses and strains at only one axial location at a time, subroutine STACK simultaneously solves for the stresses and strains in all axial nodes which are being strained axially by the trapped stack of fuel pellets.

The decision as to whether or not the gap is open or closed, and whether to call COUPLE, STACK or CLADF is made in the executive subroutine, FCMI, (Fuel-Clad Mechanical Interaction). This is the only subroutine which must be called to initiate the fuel-cladding interaction analysis. At the completion of this analysis, FCMI returns either a new gap size or a new interface pressure between fuel and cladding for use in the next iteration of the thermal calculations.

In each of COUPLE, STACK, and CLADF, either an elastic-plastic (time-independent) solution or a creep-stress relaxation (time-dependent) solution is obtained, depending on the value of an input flag.

For the plasticity calculations, two additional subroutines, STRAIN and STRESS, compute changes in yield stress with work-hardening, given a uniaxial stress-strain curve. This stress-strain curve will be obtained from the material properties package subcode, MATPRO^[4]. Subroutine STRAIN computes the effective total strain and new effective plastic strain given a value of effective stress and the effective plastic strain at the end of the last loading increment. Subroutine STRESS computes the effective stress given an increment of plastic strain and the effective plastic strain at the end of the last loading increment. Depending on the work-hardened value of yield stress, loading can be either elastic or plastic, while unloading is constrained to occur elastically. (Isotropic work-hardening is assumed in these calculations).

For the creep and stress relaxation analysis, the creep rate (at the appropriate stress and temperature) is obtained from subroutine CREPR, while the creep stress corresponding to a given creep rate is obtained from subroutine CREEP. The creep calculations are based on the so-called "equation of state" approach in which the numerical calculations closely parallel those in the incremental plasticity calculations.

Section 3.1.2 of this report presents a discussion of the general problem of elastic-plastic analysis in biaxial stress fields. It will be indicated there how the formulation of the problem naturally leads to the Method of Successive Substitutions as a means of obtaining a solution to the coupled, nonlinear elastic-plastic continuum equations. Section 3.1.3 describes the general multiaxial creep-stress relaxation problem, and describes the method of extending an elastic-plastic incremental solution to consider creep deformation. Section 3.1.4 describes the individual subroutines and specific equations which are solved.

3.1.2 General Considerations in Elasto-Plastic Analysis. Problems involving elastic-plastic deformation and multiaxial states of stress involve a number of questions which do not need to be considered in a uniaxial problem. In the following, an attempt is made to briefly outline the structure of incremental plasticity, and to outline the Method of Successive Substitutions (also called the Method of Successive Elastic Solutions) which has been used so successfully in treating multiaxial elastic-plastic problems [19].

In a problem involving only uniaxial stress ϵ_1 , the strain δ_1 is related to the stress by an experimentally determined stress-strain curve as shown in Figure 3, and Hooke's law is taken as

$$\epsilon_1 = \frac{\sigma_1}{E} + \epsilon_1^P + \int \alpha dT$$

Fig. 3 Typical isothermal stress-strain curve

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where ϵ_1^P is the plastic strain. The onset of yielding occurs at the yield stress, which can be determined directly from Figure 3. Given a load (stress) history, the resulting deformation can be determined in a simple fashion. Increase of yield stress with work-hardening is easily computed directly from Figure 3.

In a problem involving multiaxial states of stress, however, the situation is not so clear cut. Here one must have a method of relating the onset of plastic deformation to the results of a uniaxial test, and further, when plastic deformation occurs, one must have some means of determining how much plastic deformation has occurred, and how it is distributed among the individual components of strain. These two complications are taken into account by use of the so-called "Yield Function" and "Flow Rule," respectively.

A considerable wealth of experimental evidence exists on the onset of yielding in a multiaxial stress state. The bulk of this evidence supports the von Mises yield criteria, which asserts that yielding occurs when the stress state is such that:

$$0.5 \left[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \right] = \sigma_y^2 \quad (56)$$

where σ_i are the principal stresses, and σ_y is the yield stress as determined in a uniaxial stress-strain test. The square root of the left-hand side of this equation is referred to as the effective stress, σ_e , and this effective stress is one commonly used type of yield function.

To determine how the yield stress changes with permanent deformation, it is hypothesized that the yield stress is a function of the effective plastic strain, ϵ^P . An increment of effective plastic strain is determined at each loadstep and ϵ^P is defined as the sum of all increments incurred:

$$\epsilon^P \triangleq \int d\epsilon^P. \quad (57)$$

Each increment of effective plastic strain is related to the individual plastic strain components by

$$d\epsilon^P = \sqrt{\frac{2}{3}} \left[(d\epsilon_1^P)^2 + (d\epsilon_2^P)^2 + (d\epsilon_3^P)^2 \right]^{1/2} \quad (58)$$

where the $d\epsilon_i^P$ are the plastic strain components in principal coordinates. It is a well known experimental result that at pressures on the order of the yield stress, plastic deformation occurs with no change in volume. This implies that

$$d\epsilon_1^P + d\epsilon_2^P + d\epsilon_3^P = 0 \quad (59)$$

and hence in a uniaxial test with $\sigma_1 = \sigma$, $\sigma_2 = \sigma_3 = 0$, the plastic strain increments are

$$d\epsilon_2^P = d\epsilon_3^P = -1/2 d\epsilon_1^P$$

so that in a uniaxial test, Equations (56) and (58) reduce to

$$\begin{aligned}\sigma_e &= \sigma \\ d\epsilon^P &= d\epsilon_1^P.\end{aligned}\tag{60}$$

Thus, when it is assumed that the yield stress is a function of the total effective plastic strain (called the Strain-Hardening Hypothesis), the functional relationship between yield stress and plastic strain can be taken directly from a uniaxial stress-strain curve by virtue of Equation (60).

The relationship between the magnitudes of the plastic strain increments and the effective plastic strain increment is provided by the Prandtl-Reuss Flow Rule:

$$d\epsilon_i^P = \frac{3}{2} \frac{d\epsilon^P}{\sigma_e} S_i \quad i = 1, 3 \tag{61}$$

where S_i are the deviatoric stress components (in principal coordinates) defined by

$$S_i = \sigma_i - \frac{1}{3} (\sigma_1 + \sigma_2 + \sigma_3) \quad i = 1, 3 \tag{62}$$

Equation (71) embodies the fundamental observation of plastic deformation that the plastic strain increments are proportional to the deviatoric stresses. It may be shown^[20] that the constant of proportionality is determined by the choice of the yield function. Direct substitution shows that Equations (57), (59), (60), (61), and (62) are consistent with one another.

Once the plastic strain increments have been determined for a given loadstep, the total strains are determined from a generalized form of Hooke's law given by:

$$\begin{aligned}
\epsilon_1 &= \frac{1}{E} \left[\sigma_1 - \nu(\sigma_2 + \sigma_3) \right] + \epsilon_1^P + d\epsilon_1^P + \int \alpha_1 dT \\
\epsilon_2 &= \frac{1}{E} \left[\sigma_2 - \nu(\sigma_1 + \sigma_3) \right] + \epsilon_2^P + d\epsilon_2^P + \int \alpha_2 dT \\
\epsilon_3 &= \frac{1}{E} \left[\sigma_3 - \nu(\sigma_2 + \sigma_1) \right] + \epsilon_3^P + d\epsilon_3^P + \int \alpha_3 dT
\end{aligned} \tag{63}$$

in which ϵ_1^P , ϵ_2^P , and ϵ_3^P are the total plastic strain components at the end of the previous load increment and ν is Poisson's ratio.

The remaining continuum field equations of equilibrium, strain displacement, and strain compatibility are unchanged. The complete set of governing equations is presented in Table III, written in terms of rectangular Cartesian coordinates and employing the usual indicial notation in which a repeated Latin index implies summation. This set of equations is augmented by an experimentally determined uniaxial stress-strain relation.

(1) Method of Solution. When the problem under consideration is statically determinate, so that the stresses can be found from equilibrium conditions alone, the resulting plastic deformation can easily be determined. However, when the problem is statically indeterminate, and the stresses and deformation must be found simultaneously, then the full set of plasticity equations proves to be quite formidable even in the case of simple loadings and geometries.

One numerical procedure which has been used with considerable success is the Method of Successive Substitutions. This can be applied to any problem for which an elastic solution can be obtained, either in closed form or numerically. A full discussion of this technique including a number of technologically useful examples is contained in Reference 20.

Briefly, the method involves breaking the loading path up into a number of small increments. (For example, in the present application,

the loads are external pressure, temperature, and either internal pressure of prescribed displacement or the inside surface of the cladding. These loads all vary during the operating history of the fuel rod). For each new increment of the loading, the solution to all the plasticity equations listed in Table III is obtained as follows:

First, an initial estimate of the plastic strain increments $d\epsilon_{ij}^P$ is made. Based on these values, the equations of Equilibrium, Hooke's law, Strain-Displacement and compatibility are solved as for any elastic problem. From the stresses so obtained, the deviatoric stresses S_{ij} may be computed. This represents one path in the computational scheme.

Independently, using the assumed $d\epsilon_{ij}^P$ values, the increment of effective plastic strain $d\epsilon^P$ may be computed, and from this and the stress-strain curve, a value of the effective stress σ_e is obtained.

Finally, a new estimate of the plastic strain increments is obtained from the Prandtl-Reuss Flow Rule:

$$d\epsilon_{ij}^P = \frac{3}{2} \frac{d\epsilon^P}{\sigma_e} S_{ij}$$

and the entire process is continued until the $d\epsilon_{ij}^P$ converge. A schematic of the iteration scheme is shown in Figure 4.

The mechanism by which improved estimates of $d\epsilon_{ij}^P$ are obtained results from the fact that the effective stress obtained from $d\epsilon^P$ and the stress-strain curve will not be equal to the effective stress which would be obtained from the stresses from the elastic solution. They will only agree when convergence is obtained.

TABLE III
SUMMARY OF GOVERNING EQUATIONS

Equilibrium

$$\sigma_{ji,j} + \rho f_i = 0$$

where

σ = stress tensor

ρ = mass density

f_i = components of body force per unit mass

Stress-Strain

$$\epsilon_{ij} = \frac{1+\nu}{E} \sigma_{ij} - \delta_{ij} \left(\frac{\nu}{E} \sigma_{kk} - \int \alpha dT \right) + \epsilon_{ij}^P + d\epsilon_{ij}^P$$

Strain Displacement

$$\epsilon_{ij} = \frac{1}{2} (u_{i,j} + u_{j,i})$$

Compatibility

$$\epsilon_{ij,kl} + \epsilon_{kl,ij} - \epsilon_{ik,jl} - \epsilon_{jl,ik} = 0$$

Definitions used in Plasticity

$$\sigma_e \triangleq \sqrt{\frac{3}{2} S_{ij} S_{ij}}$$

$$S_{ij} \triangleq \sigma_{ij} - \frac{1}{3} \sigma_{kk} \delta_{ij}$$

$$d\epsilon^P \triangleq \sqrt{\frac{2}{3} d\epsilon_{ij}^P d\epsilon_{ij}^P}$$

Prandtl-Reuss Flow Rule

$$d\epsilon_{ij}^P = \frac{3}{2} \frac{d\epsilon^P}{\sigma_e} S_{ij}$$

Fig. 4 Schematic of the Method of Successive Elastic Solutions

The question of convergence is one that cannot, in general, be answered a priori. However, it can be shown^[20] that convergence will be obtained for sufficiently small load increments. Furthermore, lack of convergence can usually be traced to the flatness of the stress-strain curve in the plastic region. Problems with convergence are accentuated when the material is at high temperatures (approximately 922K and above for zircaloy), due to the fact that work-hardening effects decrease with temperature.

(2) Modified Iteration Scheme. To avoid (or at least minimize) such convergence problems, an alternate iteration scheme was devised by Mendelson and his co-workers. This scheme is based on the von Mises yield function and the Prandtl-Reuss flow rule, so no new hypotheses or assumptions are involved. The iteration scheme is essentially the same as the Prandtl-Reuss iteration scheme described

above, except that the iterated variables σ_e and $d\epsilon_{ij}^p$ (which are related by the highly nonlinear stress-strain law) are replaced by a new pair of variables which are related to $d\epsilon^p$ in a more nearly linear fashion. The new variables are ϵ_{et} , the equivalent modified total strain, and ϵ'_{ij} , the modified deviator strains, and in terms of these new variables, the Prandtl-Reuss flow rule becomes

$$d\epsilon_{ij}^P = \frac{d\epsilon^P}{\epsilon_{et}} \epsilon'_{ij} \quad (64)$$

Thus, ϵ_{et} plays the role of σ_e , and ϵ'_{ij} replaces the deviator stresses S_{ij} . At each step in the iteration, ϵ_{et} is calculated from the strains obtained from the pseudoelastic solution based on the previous values of the plastic strain increments, and then $d\epsilon^P$ is related to ϵ_{et} via the uniaxial stress-strain curve by the expression

$$d\epsilon^P = \frac{\epsilon_{et} - \frac{2}{3} \left(\frac{1+\nu}{E} \right) \sigma_e^{old}}{1 + \frac{2}{3} \left(\frac{1+\nu}{E} \right) \left(\frac{d\sigma_e}{d\epsilon^P} \right)_{old}} \quad (65)$$

in which σ_e^{old} is the value of the effective stress at the end of the last converged loadstep, and $\left(\frac{d\sigma_e}{d\epsilon^P} \right)_{old}$ is the slope of the stress-plastic strain curve at the end of the last converged loadstep. Since $\left(\frac{d\sigma_e}{d\epsilon^P} \right)_{old}$ is usually at least an order of magnitude smaller than the elastic modulus E , it can be seen that the denominator in Equation (65) is nearly unity, and thus $d\epsilon^P$ is very nearly linearly related to ϵ_{et} . Thus, the convergence difficulties associated with the nonlinearity of the stress-strain curve can be avoided.

Only the appropriate equations which are used in this Plastic Strain-Total Strain iteration scheme will be given here. The full derivation is presented in Mendelson's book^[20], Sections 7-9 and 9-1. The modified strains are obtained from

$$\epsilon'_{ij} = \epsilon_{ij}^e + d\epsilon_{ij}^P \quad (66)$$

in which ϵ_{ij}^e are the elastic strains defined by

$$\epsilon_{ij}^e = \frac{1+\nu}{E} \sigma_{ij} - \frac{\nu}{E} \sigma_{kk} \delta_{ij} \quad (67)$$

Fig. 5 Flow chart for Plastic strain-Total strain iteration scheme.

The modified deviator strains are then obtained from Equation (66) as

$$e'_{ij} = e_{ij}^e + d\epsilon_{ij}^P \quad (68)$$

where e_{ij}^e are the elastic deviator strains, and the fact that $d\epsilon_{ij}^P = 0$ has been used. The equivalent modified total strain is defined as

$$\epsilon_{et} \triangleq \sqrt{\frac{2}{3}} \left[(\epsilon'_{11} - \epsilon'_{22})^2 + (\epsilon'_{22} - \epsilon'_{33})^2 + (\epsilon'_{33} - \epsilon'_{11})^2 + 6(\epsilon'_{12})^2 + 6(\epsilon'_{23})^2 + 6(\epsilon'_{31})^2 \right]^{1/2}. \quad (69)$$

A schematic outline of the Plastic Strain-Total Strain iteration scheme is shown in Figure 5. The similarity with the previous iteration scheme based directly on the Prandtl-Reuss flow relations is evident.

At the start of each loadstep calculation, initial values of $d\epsilon_{ij}^P$ are assumed. Using these values, the pseudo-elastic equations are solved for new stresses and strains. From the strains so calculated ϵ_{et} and ϵ_{ij} are obtained. Then, using the uniaxial stress-strain curve, a value of $d\epsilon^P$ corresponding to ϵ_{et} is obtained using Equation (65). Finally, new estimates of $d\epsilon_{ij}^P$ are obtained from the modified flow rule

$$d\epsilon_{ij}^P = \frac{d\epsilon^P}{\epsilon_{et}} e'_{ij}$$

and the process is repeated until convergence is obtained.

This modified iteration procedure differs from the previous iteration scheme in one important aspect. In the previous scheme, if one of the nodes at which the plastic strains are being sought is actually only strained elastically, then in the course of the iteration process, the plastic strain increments will approach zero, and the correct elastic solution will be obtained automatically. In the modified iteration scheme, however, the iteration process will not converge to the appropriate elastic solution, but will, in fact, generally diverge in this case. Thus, at each step in the iteration process it is necessary to compute the effective stress and compare it with the current, work-hardened yield stress. If the yield stress has not been exceeded, then the plastic strain increments are set to zero.

Not only will the modified iteration scheme described above permit convergent plasticity calculations for higher temperatures and larger loadsteps, it also converges much more rapidly than the previous iteration scheme. Hence, the modified iteration scheme is utilized in all the (time-dependent) plasticity calculations.

3.1.3 General Considerations in Creep-Stress Relaxation Analysis.

(1) Uniaxial Behavior. Creep can broadly be defined as the inelastic time-dependent change in stress and strain in a body under constant load. For most common structural metals, high temperatures are required. Shown in Figure 6 are typical creep curves taken on a uniaxial specimen at constant stress for zircaloy-2 at 573 K taken from a correlation by Ibrahim^[21].

Two features are notable. First, doubling the stress more than doubles the strain. Second, the strain rate decreases as time goes on. A typical uniaxial creep test taken to failure is shown in Figure 7. From time $t = 0$ to $t = t_1$, the creep rate (slope of ϵ versus t) is continually decreasing. This is called the period of primary, or transient creep. From time t_1 to t_2 , the creep rate is approximately constant (although it does depend on the stress level, temperature, radiation flux, etc.). The region of constant creep rate is called the region of secondary or steady state creep.

The final region is one in which the creep rate begins increasing and is called the region of tertiary creep. In this region, the specimen usually exhibits cracks or other nonuniform deformation, and hence, the usual continuum theory is not applicable. Thus, in a structural analysis, attention is restricted to the primary and secondary stages of creep.

One approximation that is often used in creep analysis is to consider only the steady state portion of creep, so that the creep rate is independent of time. This considerably simplifies any such analysis. However, as can be seen from Figure 6, the creep strain incurred during the period of transient creep cannot be neglected in comparison to the creep incurred during the period of steady state creep (at least for hold times of less than 3000 hours) so that both transient and secondary creep must be included. This consideration is

Fig. 6 Creep curves for zircaloy-2 at 300 C (572°F) (correlations from Ibrahim, Reference 14)

Fig. 7 Typical creep test taken to failure

especially important when comparisons are to be made between code predictions and laboratory experiments which are usually of limited duration compared to anticipated service life.

The strains induced during creep are permanent, and it is a basic assumption of creep analysis that the constitutive law can be written:

$$\epsilon_1 = \frac{1}{E} [\sigma_1 - \nu(\sigma_2 + \sigma_3)] + \epsilon_1^P + \epsilon_1^C + d\epsilon_1^C \quad (70)$$

where

ϵ_1^P = accumulated plastic strain

ϵ_1^C = accumulated creep strain

$d\epsilon_1^C$ = increment of creep strain.

Although both plastic strains and creep strains are permanent, there is a fundamental difference between plasticity, which does not depend on time, and creep which is explicitly time-dependent. That is, the magnitude of the plastic strains resulting from the application of a sequence of loads to a body does not depend on the time intervals between the applications of the individual loads. The magnitude of the resulting creep strain, however, is greatly affected by the time history of the application of the loads.

Since creep is explicitly a time-dependent process, it would be most appropriate to use a theory in which the time rates of strain are related to the rates of stress, i.e., a visco-elastic theory. And, in fact, there are simple, linear rheological models available which can qualitatively reproduce all the effects observed in a uniaxial creep test. However, as pointed out in regard to Figure 6, the creep response of metals is a nonlinear function of stress. Hence, a nonlinear theory of visco-elasticity would have to be used to accurately predict creep strains. While such a nonlinear theory does exist (in the Frechet Expansion Theorem), its implementation is currently beyond

the scope of present day computers. Further, the theory requires experimental data which do not exist to date, and which would be relatively difficult to obtain. Thus, the use of a nonlinear viscoelastic theory is not within the current state of the art. For a further discussion of this, reference is made to C. E. Pugh et al^[22].

(2) The Equation of State Approach. A workable alternative to a visco-elastic creep analysis is to use the so-called "equation of state" approach. Here, analytical expressions are fit to the experimental uniaxial creep strain versus time curves measured at constant applied stress, giving the functional relation:

$$\epsilon = f(\sigma, t, T, R) \quad (71)$$

where

σ = applied constant stress

t = time

T = temperature

R = irradiation flux parameter.

The creep strain rate is obtained analytically by differentiating Equation (71) to obtain

$$\dot{\epsilon} = g(\sigma, t, T, R). \quad (72)$$

Now it must be kept in mind that Equations (71) and (72) are strictly valid for constant stress only. To extend their use to problems involving continuously varying stress, certain assumptions must be made. Consider the two creep curves in Figure 8 obtained at two different stress levels. Suppose that creep strain has been accumulating

Fig. 8 Time hardening and Strain hardening hypotheses for creep with varying stress

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at stress σ_1 until time t_1 , and then stress is changed to σ_2 . The question to be answered is: at what point on the $\sigma = \sigma_2$ creep curve do we start our calculations for further creep analysis? One of two hypotheses is commonly made:

- (1) Time-hardening Hypothesis: Here it is assumed that it is the elapsed time which is the determining factor, so that in making the transition from stress σ_1 to σ_2 , one moves from point O to point A in Figure 8. Hence, creep strain rates are determined directly from Equation (72).
- (2) The Strain-hardening Hypothesis: Here it is assumed that it is the accumulated creep strain which is the determining factor. Thus, one moves from point O to point B in Figure 8. Analytically, this is accomplished by eliminating time, t , from Equation (72) by use of Equation (71) to obtain

$$\dot{\epsilon} = h(\sigma, \epsilon, T, R) \quad (73)$$

and thus the subsequent creep strain rates are determined by the accumulated creep strain.

In general, experimental data agree better with analytical predictions based on the strain-hardening assumption, so this is the assumption which is incorporated into the FRACAS subcode. This is in accordance with the recommendations of Reference 22.

When the creep function, Equation (71), is a complicated function of its arguments, it is often not possible to eliminate time analytically between Equations (71) and (72). In these cases it is necessary to perform the elimination of time numerically. This can be done for arbitrary functional forms of Equations (71) and (72) and thus, complete generality can be maintained. However, this is achieved at the cost of having to solve a pair of highly nonlinear, coupled algebraic equations at each step of the creep iteration scheme, which is a time consuming procedure.

In order to optimize the computation time for creep calculations in FRACAS, the following procedure is utilized. It is assumed that the creep law, Equation (71), is such that time can be explicitly evaluated so to obtain Equation (73), and that Equation (73) is available to FRACAS from a subroutine denoted CREPR. Secondly, it is assumed that Equation (73) can be written in the form

$$\sigma = \sigma(\dot{\epsilon}, \epsilon, T) \quad (74)$$

and that this equation is available to FRACAS from a subroutine denoted CREEP. In the majority of cases, subroutine CREEP will use an iteration scheme to obtain σ given $\dot{\epsilon}$ and ϵ , but this can usually be done very efficiently. For use in the FRAP codes, subroutine CREPR is obtained from the material properties subcode MATPRO. Subroutine CREEP uses the analytic expression for creep rate from MATPRO and the method of successive substitutions. This results in minimizing the creep calculation time.

(3) Extension to Multiaxial Creep Problems. All of the foregoing discussion is applied to uniaxial loading behavior only. To extend this to creep and stress relaxation in multiaxial states of stress, we proceed in a fashion similar to that used in plasticity, and introduce an effective creep strain increment defined by

$$d\epsilon^C \triangleq \sqrt{\frac{2}{3}} \, d\epsilon_{ij}^C \, d\epsilon_{ij}^C \quad (75)$$

which is, of course, directly analogous to the effective plastic strain defined for the plasticity calculations. Then the effective creep strain rate is computed from

$$\dot{\epsilon}^C = \frac{d\epsilon^C}{dt} = \frac{1}{\Delta t} \sqrt{\frac{2}{3}} \, d\epsilon_{ij}^C \, d\epsilon_{ij}^C. \quad (76)$$

The same effective stress is utilized:

$$\sigma_e \triangleq \sqrt{\frac{3}{2}} \, \sigma_{ij} \, \sigma_{ij}.$$

It is then assumed that the effective stress, σ_e , and the effective creep strain rate, $\dot{\epsilon}^C$, are related by the uniaxial creep strain function, Equation (73). For any given problem, the equilibrium and kinematic equations are identical with those used in the plasticity calculations.

The external loading (temperatures, pressures, displacements etc.,) are assumed to be applied stepwise. At each time that these loads are incremented, a (time-independent) plasticity solution is obtained as described in the previous sections. The time periods in between (at constant load) are subdivided into a number of small time increments, Δt , and a creep analysis is performed. For each time increment, Δt , the Method of Successive Elastic Solutions is applied in the usual fashion, except that the effective stress, σ_e , is obtained from the uniaxial creep curve at the iterated value of the

effective creep rate, $\dot{\epsilon}^C$, instead of from the uniaxial stress-strain curve. Thus, for each time increment, Δt , the creep solution is obtained as follows:

(1) Assume the creep strain increments, $d\epsilon_{ij}^C$

(2) Compute the effective creep strain rate

$$\dot{\epsilon}^C = \frac{1}{\Delta t} \sqrt{\frac{2}{3} d\epsilon_{ij}^C d\epsilon_{ij}^C}$$

(3) Obtain the effective stress from the uniaxial creep curve, using the strain-hardening hypothesis

$$\sigma_e = h(\dot{\epsilon}^C, \epsilon^C, T, R)$$

where ϵ^C is the total accumulated effective creep strain, including $d\epsilon^C = \dot{\epsilon}^C \Delta t$ incurred during the current time increment

(4) Obtain the deviator stresses from the elastic solution, with Hooke's law taken as

$$\epsilon_{11} = \frac{1}{E} \left[\sigma_{11} - \nu(\sigma_{22} + \sigma_{33}) \right] + \epsilon_{11}^P + \epsilon_{11}^C + d\epsilon_{11}^C$$

$$\epsilon_{12} = \frac{1+\nu}{E} \sigma_{12} + \epsilon_{12}^P + \epsilon_{12}^C + d\epsilon_{12}^C$$

where

etc.

ϵ_{ij}^P = accumulated plastic strains at end of last load step

ϵ_{ij}^C = accumulated creep strains at end of last Δt time increment

$d\epsilon_{ij}^c$ = increments of creep strain during current Δt
time increment

- (5) Obtain new estimates of the creep strain increments from the Prandtl-Ruess flow rule:

$$d\epsilon_{ij}^c = \frac{3}{2} \frac{\dot{\epsilon}^c \Delta t}{\sigma_e} S_{ij}.$$

- (6) Repeat until convergence.

3.1.4 Description of Individual Subroutines. The package of subroutines which perform the fuel-clad mechanical interaction analysis consists of four main subroutines. FCMI is the executive subroutine, and it calls either COUPLE, STACK, or CLADF as appropriate. STRESS and STRAIN are called by either CLADF, STACK, or COUPLE to obtain the necessary mechanical properties. Subroutine CLOSE is an interpolation routine called by FCMI to locate the point at which the gap closes for each node. These subroutines are described in detail below.

(1) Subroutine FCMI. Subroutine FCMI performs the basic function of determining whether or not the fuel pellets and the cladding are in contact. The radial expansion of the fuel is obtained from models contained externally to the fuel-clad interaction subroutines, and is passed to FCMI in the calling sequence. Stress effects on the fuel expansion are known to be small relative to thermal expansion and swelling, and so the fuel expansion is uncoupled from the cladding deformation. (Stress-induced creep is important, however, and is being considered separately.)

The decision as to whether or not the fuel is in contact with the cladding is made by comparing the radial displacement of the fuel with the radial displacement which would occur in the cladding due to the

prescribed external (coolant) pressure and the prescribed internal (fission gas) pressure. Both of these values are passed to FCMI through the calling sequence. This free cladding radial displacement is obtained in CLADF. Then if

$$u_r^{\text{fuel}} \geq u_r^{\text{clad}} + \delta \quad (77)$$

where δ is the initial (as-fabricated) gap between the fuel and the cladding, the fuel is determined to be in contact with the cladding. The as-fabricated gap δ is a constant which does not change throughout the loading history of the rod. The loading history enters into this decision by virtue of the permanent plastic cladding strains which are used in the CLADF solution, and which are updated at each call to CLADF or COUPLE. These plastic strains (and total effective plastic strain ϵ^P) are stored in the main calling program, and are passed to FCMI through the calling sequence.

If the fuel and cladding displacements are such that Equation (77) is not satisfied, the gap has not closed during the current loadstep, and the solution obtained by CLADF is the appropriate solution. the current value of the gap is computed and passed back to the main calling program. The plastic strain values may be changed in the solution obtained by CLADF if additional plastic straining has occurred.

If Equation (77) is satisfied, however, the fuel and cladding are in contact during the current loading step. If the gap was open at the end of the last loadstep, then subroutine CLOSE is called. This routine linearly interpolates between the current load values and the last load values to determine exactly when the gap closed. For that portion of the loadstep in which the gap is open, subroutine CLADF is called. For the remaining portion of the loadstep, in which the cladding and the fuel are in contact, radial continuity at the contact interface requires that

$$\frac{u}{r}{}^{clad} = \frac{u}{r}{}^{fuel} - \delta \quad (78)$$

while in the axial direction it is assumed that no-slip occurs between the fuel and the cladding. Since a thin shell theory is used for the cladding, it is assumed that the strain induced in the cladding by the axial expansion of the fuel is taken up entirely by straining of the middle surface of the cladding.

Note that only the additional strain which occurs in the fuel after lock-up has occurred is transferred to the cladding. Thus, if $\epsilon_{z,0}^{clad}$ is the axial strain in the cladding just prior to contact and $\epsilon_{z,0}^{fuel}$ is the corresponding axial strain in the fuel, then the no-slip condition in the axial direction becomes

$$\epsilon_z^{clad} - \epsilon_{z,0}^{clad} = \epsilon_z^{fuel} - \epsilon_{z,0}^{fuel} \quad (79)$$

The values of the prestrains $\epsilon_{z,0}^{fuel}$ and $\epsilon_{z,0}^{clad}$ are set equal to the values of the strains which existed in the fuel and the cladding at the last load increment during which the gap was open. These values are stored in the main calling program, and passed to FCMI in the calling sequence. They are updated at the end of any load increment during which the gap closes.

After u_z^{clad} and ϵ_z^{clad} have been computed in FCMI, they are passed to subroutine COUPLE, which considers a thin cylindrical shell with prescribed axial strain, external pressure, and prescribed radial displacement of its inside surface. After the solution to this problem is obtained in COUPLE, subroutine FCMI passes a value of the interface pressure back to the main calling program, along with new plastic strains and stresses.

Fig. 9 Axial gap trapped stack configurations

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After all the nodes in the fuel rod have been examined for open or closed gaps as described above, all those nodes with open gaps are re-examined to consider axial compatibility. This is necessary because in the course of normal power histories it is possible for gaps to exist between the fuel pellets. The gaps can, in the case of collapsible cladding, give rise to local high strain concentrations. As a means of following the generation and development of such axial gaps, models are included in FCMI to consider the behavior of the cladding in the situations depicted in Figure 9a or Figure 9b. In both of these figures, nodes J* and J-1 are nodes which have been determined to be in firm contact with the cladding. Nodes J to J*-1 are nodes which have been determined not to be in contact with the cladding.

Figure 9a depicts the case where, due to the pressure differences and thermal expansion, the length of the enclosed column of pellets is less than the length of the cladding, and hence, an axial gap exists. For the case of vertical fuel rods, the gap will occur at the top of the uppermost pellet. In this situation, the deformation of the cladding is determined by the internal (fission gas) and external (coolant) pressures, and the axial temperature distribution in the cladding.

By contrast, it can happen that the axial expansion of the column of fuel pellets is great enough so that no gap can exist, and in fact, the expansion of the fuel pellets trapped between nodes J-1 and J* actually stretches the cladding axially, as depicted in Figure 9b. This situation is denoted as the "Trapped Stack" regime.

Models for the determination of the elastic-plastic deformation of the cladding for the two regimes discussed above are contained in the two subroutines CLADF and STACK, which are described in detail later in this report. In the following, the assumptions and logic used to determine which deformation regime is appropriate are discussed.

Consider the situation shown in Figure 9a, wherein nodes J* and J-1 have been determined to be in firm contact. Nodes J through J*-1 are nodes with open radial gaps, and thus, for these nodes, the fuel is not in contact with the cladding. Let $\epsilon^K(i)$ denote the axial gap between node i and node i + 1 at the end of the loadstep k. (It is assumed that there is never any gap between the lowest pellet and the bottom of the fuel rod.) Then, at the start of loadstep k + 1, it is assumed that all the fuel pellets consolidate at the bottom of the stack, so that all axial gaps are zero except the gap between the top node in the free stack and the next locked node J*, that is

$$\left\{ \begin{array}{l} \xi^{k+1}(J^*-1) = \sum_{i=j-1}^{J^*-1} \xi^k(i) \\ \xi^{k+1}(i) = 0 \text{ for } i = j-1 \text{ to } J^*-2. \end{array} \right.$$

The change in length of the stack of fuel pellets is determined from the prescribed axial fuel strains, using

$$\Delta \ell_{\text{fuel}}^{k+1} = \sum_{i=j}^{J^*-1} \begin{pmatrix} \epsilon_{z, \text{fuel}}^{k+1} & - \epsilon_{z, \text{fuel}}^k \end{pmatrix} dz(i) \quad (80)$$

where the summation is taken over all nodes in the stack, and the superscript indicates the loadstep number.

The length of the trapped cladding can now be computed assuming that the fuel and cladding do interact, and that changes in the length of the trapped cladding are determined by changes in the length of the stack of fuel pellets. The trapped cladding length, assuming axial interaction, is given by

$$\ell_{\text{clad}}^{(k+1)} = \ell_{\text{clad}}^{(k)} + \Delta \ell_{\text{fuel}}^{(k+1)} - \xi^{k+1}(J^*-1) \quad (81)$$

where

$\ell_{\text{clad}}^{(k)}$ = length of trapped cladding at the end of the last loadstep

$\Delta \ell_{\text{fuel}}^{(k+1)}$ = change in length of column of fuel pellets during current loadstep

$\xi^{k+1}(j^*-1)$ = axial gap between column of fuel pellets and upper end of trapped cladding.

The trapped cladding length computed above is compared with the length that the cladding would assume due to the internal and external pressures and temperature gradients acting alone, as in the absence of

axial fuel-cladding interaction. This free length can be obtained from the strains computed by subroutine CLADF, using

$$\ell_{\text{free}}^{(k+1)} = \sum_{i=j}^{J^*-1} \left[1 + \epsilon_{z, \text{CLADF}}^{(k+1)} \right] dz(i). \quad (82)$$

The decision as to whether or not fuel-cladding axial interaction takes place can now be made. If the length that the cladding would assume under the action of pressure and thermal gradients alone is greater than the length the cladding would assume due to axial interaction, then no axial interaction is possible and, in general, an axial gap will exist. Otherwise, axial interaction must have taken place. The two cases are considered separately.

Case A:

If $\ell_{\text{free}}^{(k+1)} > \ell_{\text{free}}^{(k)}$, then a gap has opened between the fuel pellet stack and the cladding and the stresses and strains are set equal to those computed in CLADF. The new axial gap (at the top of the fuel pellet stack) can be computed from

$$\xi^{k+1}(J^*-1) = \xi^k(j^*-1) + \Delta \ell_{\text{clad}}^{(k+1)} - \Delta \ell_{\text{fuel}}^{(k+1)} \quad (83)$$

in which $\Delta \ell_{\text{clad}}^{(k+1)}$ is the change in length of the trapped cladding during the current loadstep, and is obtained from

$$\Delta \ell_{\text{clad}}^{(k+1)} = \Delta \ell_{\text{free}}^{(k+1)} - \ell^{(k)} \quad (84)$$

where $\ell^{(k)}$ is the length of the trapped cladding at the end of the last loadstep. The axial gaps between all other axial nodes in the stack of fuel pellets (including the gap beneath the lowest pellet in the stack) are set equal to zero.

Case B:

If $\ell_{\text{clad}}^{(k+1)} > \ell_{\text{clad}}^{(k+1)}$, then axial interaction has taken place and no axial gap will exist. The change in length of the trapped cladding is determined by the preexisting axial gap and the change in the fuel pellet stack length via

$$\Delta \ell_{\text{clad}}^{(k+1)} = \Delta \ell_{\text{fuel}}^{(k+1)} - \xi^k(J^*-1) \quad (85)$$

This quantity is then passed to subroutine STACK, after which all the axial gaps in the fuel stack are set equal to zero.

Finally, if the stack of pellets with open radial gaps occurs at the top of the fuel rod, then the solution obtained in CLADF is the appropriate solution, and all axial gaps are set to zero. This done since it is assumed that there is always a plenum volume containing a hold-down spring at the top of the fuel rod, and hence that the upper-most pellet can never contact the top of the fuel rod cladding.

(2) Subroutine CLADF. This subroutine considers a thin cylindrical shell loaded by both internal and external pressures. Axisymmetric loading and deformation are assumed. Loading is also restricted to be uniform in the axial direction, and no bending is considered. The geometry and coordinates are shown in Figure 10. The displacements of the midplane of the shell are u and w in the radial and axial directions respectively.

Then, as is well known, the equilibrium equations simplify considerably, and are identically satisfied by

$$\sigma_{\theta} = \frac{r_i p_i - r_o p_o}{t} \quad (86)$$

$$\sigma_z = \frac{r_i^2 p_i - r_o^2 p_o}{r_o^2 - r_i^2} \quad (87)$$

Fig. 10 Fuel rod geometry and coordinates

For membrane shell theory^[21], the strains are related to the midplane displacements by

$$\epsilon_z = \frac{\partial w}{\partial z} \quad (88)$$

$$\epsilon_\theta = \frac{u}{\bar{r}} \quad (89)$$

where $\bar{r}$ is the radius of the midplane. Strain across the thickness of the shell will be allowed. In shell theory, since the radial stress can be neglected, and since the hoop stress, σ_r , and axial stress, σ_z , are uniform across the thickness when bending is not considered,

the radial strain is due only to Poisson's effect, and is uniform across the thickness. (Normally, radial strains are not considered in a shell theory, but when plastic deformations are to be considered, plastic radial strains must be included.)

The stress-strain relations are written in the incremental form:

$$\epsilon_{\theta} = \frac{1}{E} \left[\sigma_{\theta} - \nu \sigma_z \right] + \epsilon_{\theta}^P + d\epsilon_{\theta}^P + \int_{T_0}^T \alpha_{\theta} dT \quad (90)$$

$$\epsilon_z = \frac{1}{E} \left[\sigma_z - \nu \sigma_{\theta} \right] + \epsilon_z^P + d\epsilon_z^P + \int_{T_0}^T \alpha_z dT \quad (91)$$

$$\epsilon_r = -\frac{\nu}{E} \left[\sigma_{\theta} + \sigma_z \right] + \epsilon_r^P + d\epsilon_r^P + \int_{T_0}^T \alpha_r dT \quad (92)$$

where

T_0 = strain-free reference temperature

$\alpha_{\theta}, \alpha_z, \alpha_r$ = coefficients of thermal expansion

T = current average clad temperature.

The terms ϵ_{θ}^P , ϵ_z^P , and ϵ_r^P are the plastic strains at the end of the last load increment, and $d\epsilon_{\theta}^P$, $d\epsilon_r^P$, and $d\epsilon_z^P$ are the additional plastic strain increments which occur due to the new load increment.

As discussed in Section 3.1.2, the magnitudes of the additional plastic strain increments are determined by the effective stress and the Prandtl-Reuss flow rule, namely:

$$\sigma_e = \frac{1}{\sqrt{2}} \left[(\sigma_\theta - \sigma_z)^2 + (\sigma_z)^2 + (\sigma_\theta)^2 \right]^{1/2} \quad (93)$$

$$d\epsilon_\theta^P = \frac{3}{2} \frac{S_\theta}{\sigma_e} d\epsilon^P$$

$$d\epsilon_z^P = \frac{3}{2} \frac{S_z}{\sigma_e} d\epsilon^P \quad (94)$$

$$d\epsilon_r^P = -d\epsilon_\theta^P - d\epsilon_z^P$$

$$S_\theta = \sigma_\theta - \frac{1}{3} (\sigma_\theta + \sigma_z)$$

$$S_z = \sigma_z - \frac{1}{3} (\sigma_\theta + \sigma_z) \quad (95)$$

$$S_r = \frac{1}{3} (\sigma_\theta + \sigma_z).$$

Once $d\epsilon^P$ is determined, the individual plastic strain components are found from Equations (94), and the total strain components are obtained from Equations (90) through (92).

The displacement of the inside surface of the shell must be determined so that a new gap width can be computed. The radial displacement of the inside surface is given by

$$u(r_i) = \bar{r} \epsilon_\theta - \frac{t}{2} \epsilon_r \quad (96)$$

where the first term is the radial displacement of midplane [from Equation (89) and] ϵ_r is the uniform strain across the thickness, t .

The final step performed by CLADF prior to returning control to FCMI is to add the plastic strain increments to the previous plastic strain values as in

$$(\epsilon^P)_{\text{new}} = (\epsilon^P_{\theta})_{\text{old}} + d\epsilon^P_{\theta}$$

$$(\epsilon^P_z)_{\text{new}} = (\epsilon^P_z)_{\text{old}} + d\epsilon^P_z$$

$$(\epsilon^P_r)_{\text{new}} = (\epsilon^P_r)_{\text{old}} + d\epsilon^P_r$$

$$(\epsilon^P)_{\text{new}} = (\epsilon^P)_{\text{old}} + d\epsilon^P$$

and these values are returned to FCMI for use at the next load increment.

Thus, all the stresses and strains can be computed directly since, in this case, the stresses are determinate. In the case of the driven cladding displacement, the stresses depend on the displacement, and such a straightforward solution is not possible.

The solution in CLADF proceeds as follows. At the end of the last load increment the plastic strain components ϵ^P_r , ϵ^P_{θ} , and ϵ^P_z , are known, and also the total effective plastic strain ϵ^P , is known.

The loading is now incremented with new values of P_i , P_o , and T prescribed. The new stresses can be determined immediately from Equations (86) and (87), and a new value of effective stress is obtained from Equation (93).

The increment of effective plastic strain $d\epsilon^P$, which results from the current increment of loading can now be determined from the uniaxial stress-strain curve at the new value of σ_e , as shown in Figure 11. (The new elastic loading curve depends on the value of ϵ^P .) This computation is performed by subroutine STRAIN.

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Fig. 11 Calculation of effective stress σ_e from $d\epsilon^p$.

(3) Subroutine COUPLE. This subroutine considers the problem of a cylindrical shell for which the radial displacement of the inside surface and axial strain are prescribed. Here the stresses cannot be computed directly since the pressure at the inside surface (the interface pressure) must be determined as part of the solution.

As in CLADF, the displacement at the inside surface is given by

$$u(r_i) = u - \frac{t}{2} \epsilon_r$$

where u is the radial displacement of the midplane. From Equation (63), $u = \bar{r}\epsilon_\theta$ and hence:

$$u(r_i) = \bar{r} \epsilon_\theta - \frac{t}{2} r. \quad (97)$$

Thus, prescribing the displacement of the inside surface of the shell is equivalent to a constraining relation between ϵ_θ and ϵ_r . As before, Hooke's law is taken in the form

$$\epsilon_\theta = \frac{1}{E} (\sigma_\theta - \nu \sigma_z) + \epsilon_\theta^P + d\epsilon_\theta^P + \int_{T_0}^T \alpha_\theta dT \quad (98)$$

$$\epsilon_z = \frac{1}{E} (\sigma_z - \nu \sigma_\theta) + \epsilon_z^P + d\epsilon_z^P + \int_{T_0}^T \alpha_z dT \quad (99)$$

$$\epsilon_r = -\frac{\nu}{E} (\sigma_\theta + \sigma_z) + \epsilon_r^P + d\epsilon_r^P + \int_{T_0}^T \alpha_r dT \quad (100)$$

Using Equations (97) and (100) in Equation (98) yields a relation between the stresses σ_θ , σ_z , and the prescribed displacement $u(r_i)$:

$$\begin{aligned} & \frac{u(r_i)}{\bar{r}} + \frac{1}{2} \frac{t}{\bar{r}} \left(\epsilon_r^P + d\epsilon_r^P + \int_{T_0}^T \alpha_r dT \right) \\ & - \left(\epsilon_\theta^P + d\epsilon_\theta^P + \int_{T_0}^T \alpha_\theta dT \right) = \frac{1}{E} \left[\left(1 + \frac{\nu}{2} \frac{t}{\bar{r}} \right) \sigma_\theta \right. \\ & \left. + \nu \left(\frac{1}{2} \frac{t}{\bar{r}} - 1 \right) \sigma_z \right] \quad (101) \end{aligned}$$

Equations (99) and (101) are now a pair of simultaneous algebraic equations for the stresses σ_θ and σ_z , which may be written as

$$\begin{bmatrix} A_{11} & A_{12} \\ A_{21} & A_{22} \end{bmatrix} \begin{bmatrix} \sigma_\theta \\ \sigma_z \end{bmatrix} = \begin{bmatrix} B_1 \\ B_2 \end{bmatrix}$$

where

$$A_{11} = 1 + \frac{u}{2} \frac{t}{r}$$

$$A_{12} = u \left(\frac{1}{2} \frac{t}{r} - 1 \right)$$

$$A_{21} = -u$$

$$A_{22} = 1$$

$$B_1 = E \frac{u(r_i)}{\bar{r}} + \frac{E}{2} \frac{t}{\bar{r}} \left[\epsilon_r^P + d\epsilon_r^P + \int_{T_0}^T \alpha_r dT \right]$$

$$- E \left[\epsilon_\theta^P + d\epsilon_\theta^P + \int_{T_0}^T \alpha_\theta dt \right]$$

$$B_2 = E \epsilon_z - E \left[\epsilon_z^P + d\epsilon_z^P + \int_{T_0}^T \alpha_z dT \right].$$

Then the stresses can be written explicitly as

$$\sigma_\theta = \frac{B_1 A_{22} - B_2 A_{12}}{A_{11} A_{22} - A_{12} A_{21}} \quad (102)$$

$$\sigma_z = \frac{B_2 A_{11} - B_1 A_{21}}{A_{11} A_{22} - A_{12} A_{21}}. \quad (103)$$

These equations relate the stresses to $u(r_i)$ and ϵ_z which are prescribed and to $d\epsilon_r^P$, $d\epsilon_z^P$, and $d\epsilon_\theta^P$, which are to be determined. The remaining equations which must be satisfied are

$$\epsilon_{et} = \sqrt{\frac{2}{3}} \left[(\epsilon_\theta' - \epsilon_z')^2 + (\epsilon_z' - \epsilon_r')^2 + (\epsilon_r' - \epsilon_\theta')^2 \right]^{1/2} \quad (104)$$

The modified Prandtl-Reuss flow equations are

$$\begin{aligned} d\epsilon^P &= \frac{d\epsilon^P}{\epsilon_{et}} \\ d\epsilon_z^P &= \frac{d\epsilon^P}{\epsilon_{et}} e'_z \\ d\epsilon_r^P &= -d\epsilon_\theta^P - d\epsilon_z^P \end{aligned} \quad (105)$$

in which e'_θ and e'_z are the modified deviator strains defined by Equation (68). The equivalent modified total strain ϵ_{et} is related to the plastic strain increment $d\epsilon^P$ using Equation (69) and the uniaxial stress-strain law. Equations (102) through (105) must be simultaneously satisfied for each loading increment.

As discussed in Section 3.1.1, a straightforward numerical solution to these equations can be obtained via the Method of Successive Substitutions. Here, one initially assumes arbitrary values for the increments of plastic strain, and uses Equations (101) through (105) to obtain improved estimates of the plastic strain components. The steps performed by COUPLE for each increment of load are as follows:

- (1) Values of $d\epsilon^P$, $d\epsilon_\theta^P$, and $d\epsilon_z^P$ are assumed
- (2) From Hooke's law, using the assumed plastic strain increments and the prescribed values of $u(r_i)$ and ϵ_z , values for the stresses can be obtained from Equations (101) and (102)
- (3) From the pseudoelastic solution in Step 2, compute the modified strains and modified deviator strains, and hence obtain ϵ_{et} from Equation (104)
- (4) Obtain $d\epsilon^P$ from ϵ_{et} and the uniaxial stress-strain curve

- (5) Compute new values of the plastic strain increments using the modified Prandtl-Reuss equations
- (6) The old and new values of $d\epsilon_{\theta}^P$, $d\epsilon_z^P$, and $d\epsilon_r^P$ are compared and the process continued until convergence is obtained
- (7) Once convergence has been obtained, the interface pressure is computed from Equation (86):

$$P_{int} = \frac{t\sigma_{\theta} + r_o P_o}{r_i} \quad (106)$$

When steps 1 through 7 have been accomplished, the solution is complete, provided that the interface pressure is not less than the local gas pressure.

Due to unequal amounts of plastic straining in the hoop and axial direction, however, it often happens on unloading that the interface pressure as obtained in Step 7 is less than the gas pressure even though the gap has not opened. When this situation occurs, the frictional locking mechanism which is assumed to constrain the cladding axial deformation to equal the fuel axial deformation can no longer act. The axial strain and stress adjust themselves so that the interface pressure just equals the gas pressure, at which point the axial strain is again locked. Thus, on further unloading, the axial strain and the hoop and axial stresses continually readjust themselves so as to maintain the interface pressure equal to the gas pressure until the gap opens. Because the unloading occurs elastically, a solution for this portion of the fuel-cladding interaction problem can be obtained directly as follows.

Since the external pressure and the interface pressure are known, the hoop stress is obtained from Equation (86) as:

$$\sigma_{\theta} = \frac{r_i p_{int} - r_o p_o}{t} \quad (107)$$

From Equation (96) we can write:

$$\theta = \frac{u_r^{fuel} - \delta + t/2 \epsilon_r}{\bar{r}} \quad (108)$$

Substituting ϵ_{θ} and ϵ_r as given by Equations (98) and (100) into Equation (118) gives an explicit equation for σ_z as:

$$\begin{aligned} v r_i \sigma_z = & (\bar{r} + vt/2) \sigma_{\theta} + \bar{r} E \left[\int \alpha_{\theta} dT + \epsilon_{\theta}^P \right] \\ & - \frac{t}{2} E \left[\int \alpha_r dT + \epsilon_r^P \right] - E u(r_i) \end{aligned} \quad (109)$$

in which σ_{θ} is known from Equation (107). With σ_z and σ_{θ} known, the strains may be computed from Hooke's law, Equations (98) through (100). This set of equations is included in subroutine COUPLE and is automatically invoked when a value of P_{int} less than the local gas pressure is computed.

As in CLADF, the last step performed by COUPLE before returning control to FCMI is to set the plastic strain components and total effective strain ϵ^P equal to their new values by adding in the computed increments $d\epsilon_i^P$ and $d\epsilon^P$.

(4) Subroutine STACK. Subroutine STACK is called when one or more fuel pellet nodes are trapped between the lower end of the cladding and a pellet in firm contact with the cladding, as shown in Figure 12. In this case the axial expansion of the fuel will be imparted to the cladding even though the cladding and fuel are not in radial contact.

Fig. 12 Schematic of trapped stack

The total change in length of the trapped cladding is computed in FCMI, and passed to STACK in the calling sequence. For each axial node in the trapped cladding, the axial strain is given by

$$\epsilon_z(i) = \frac{1}{E(i)} \left[\sigma_z - \nu(i) \sigma_\theta(i) \right] + \epsilon_z^P(i) + d\epsilon_z^P(i) + \int_{T_0}^{T(i)} \alpha_z dT \quad (110)$$

in which i denotes the axial node number. Axial force equilibrium requires that ϵ_z be the same in each node. Since the total length change is prescribed, one can write

$$\Delta l = \sum_{i=1}^n \left[\epsilon_z(i) - \epsilon_z^0(i) \right] dz(i)$$

in which $dz(i)$ are the axial cladding node lengths, and $\epsilon_z^0(i)$ are the axial strains in the cladding at the end of the last loadstep. Inserting Equation (110) in the above equation yields

$$\sigma_z = \left[\sum_{i=1}^N \frac{E(i)}{dz(i)} \right] \left\{ \Delta l + \sum_{i=1}^N dz(i) \left[\frac{\nu(i) \sigma_\theta(i)}{E(i)} + \epsilon_z^0(i) - \epsilon_z^P(i) - d\epsilon_z^P(i) - \int_{T_0}^{T(i)} \alpha_z dT \right] \right\} \quad (111)$$

For each node, the plasticity equations are

$$\epsilon_{et}(i) = \sqrt{\frac{2}{3}} \left\{ \left[\epsilon_z'(i) - \epsilon_r'(i) \right]^2 + \left[\epsilon_r'(i) - \epsilon_\theta'(i) \right]^2 + \left[\epsilon_\theta'(i) - \epsilon_z'(i) \right]^2 \right\}^{1/2} \quad (112)$$

$$e_j'(i) = e_j^e(i) + d\epsilon_j^P(i) \quad j = r, \theta, z \quad (113)$$

and the modified Prandtl-Reuss equations:

$$\left\{ \begin{array}{l} d\epsilon_z^P(i) = \frac{d\epsilon^P(i)}{\epsilon_{et}(i)} e_z'(i) \\ d\epsilon_\theta^P(i) = \frac{d\epsilon^P(i)}{\epsilon_{et}(i)} e_\theta'(i) \\ d\epsilon_r^P(i) = -d\epsilon_\theta^P(i) - d\epsilon_z^P(i). \end{array} \right. \quad (114)$$

Equations (111) through (114) must be simultaneously satisfied for all the trapped axial cladding nodes. And since the nodes may have different temperatures, different stress-strain curves are used at different nodes.

As before, the Method of Successive Elastic Solutions is used. In contrast to subroutine COUPLE, however, the method is applied simultaneously to several axial nodes. Because more than one node is being considered, an additional possibility arises.

This is the possibility that, due to the axial stretching and Poisson's effect, some (or all) of the cladding nodes may come into contact with the fuel pellets, although contact would not occur due to internal and external pressures alone. In this case, the hoop stress in Equation (111) is no longer given by Equation (86), but now depends on ϵ_z and the radial displacement of the fuel. While contact occurs, however, radial compatibility as expressed in Equation (96) requires that

$$\bar{r} \epsilon_\theta(i) - 0.5 t \epsilon_r(i) = u_r^{\text{fuel}}(i) - \delta.$$

Substituting for $\epsilon_\theta(i)$ and $\epsilon_r(i)$ from Hooke's law, Equations (90) and (92) there results a single equation relating $\sigma_\theta(i)$ at each node to the axial stress σ_z , which can be solved for $\sigma_\theta(i)$ explicitly to obtain

$$\begin{aligned}
& \left[\frac{\bar{r}}{E(i)} + \frac{0.5v(i)t}{E(i)} \right] \sigma_{\theta}(i) = u_r^{fuel}(i) - \delta \\
& - r \left[\frac{-v(i)}{E(i)} \sigma_z + \epsilon_{\theta}^P(i) + d\epsilon_{\theta}^P(i) + \int_{T_0}^{T(i)} \alpha_{\theta} dT \right] \quad (115) \\
& + \frac{t}{2} \left[-\frac{v(i)}{E(i)} \sigma_z + \epsilon_r^P(i) + d\epsilon_r^P(i) + \int_{T_0}^{T(i)} \alpha_r dT \right]
\end{aligned}$$

Which applies at each node where contact has occurred. Finally, Equation (115) is used to eliminate $\sigma_{\theta}(i)$ from Equation (111) for those nodes at which contact has occurred. Thus, we obtain an equation for σ_z involving summations over all nodes not in contact plus summations over all nodes, denoted J^* , where contact has occurred. This equation, solved explicitly for σ_z , is shown below:

$$\begin{aligned}
& \left\{ \sum_i \frac{dz(i)}{E(i)} - \sum_{i \in J^*} \frac{dz(i)v(i)}{E(i)} \left[\frac{(\bar{r} - 0.5t)v(i)}{\bar{r} + 0.5tv(i)} \right] \right\} \sigma_z \\
& = \delta l + \sum_i \epsilon_z^0(i) dz(i) \\
& - \sum_i \left[\epsilon_z^P(i) + d\epsilon_z^P(i) + \int_{T_0}^{T(i)} \alpha_z dT \right] dz(i) \\
& + \sum_{i \in J^*} \frac{v(i)}{E(i)} \left[\frac{p_i(i)r_i - p_0(i)r_0}{r_0 - r_i} \right] dz(i) \quad (116) \\
& + \sum_{i \in J^*} \frac{v(i)dz(i)}{r + 0.5tv(i)} \left\{ u_r^{fuel}(i) - \delta - \bar{r} \left[\epsilon_{\theta}^P(i) + d\epsilon_{\theta}^P(i) \right. \right. \\
& \left. \left. + \int_{T_0}^{T(i)} \alpha_{\theta} dT \right] + 0.5t \left[\epsilon_r^P(i) + d\epsilon_r^P(i) + \int_{T_0}^{T(i)} \alpha_r dT \right] \right\}
\end{aligned}$$

This modified equation for σ_z allows for an arbitrary number of contacting nodes, and is solved for σ_z at each step in the iteration for the plastic strain increments. Of course, it is not known a priori which nodes may be in contact.

However, for given values of the plastic strain increments (the iterates in the Method of Successive Elastic Solutions) the governing equations are all linear. Thus, one can solve for σ_z assuming no pellets are in contact, then compute the gaps, and if any negative gaps are found, recompute σ_z with those nodes now assumed to be in contact. This process is repeated until all calculated gaps are either positive or zero. At most, N steps are required since the equations are linear, where N is the number of nodes in the stack.

Thus, in this application, the Method of Successive Elastic Solutions becomes an iteration within an iteration, and one in which the set of variables iterated upon is determined as the solution progresses.

(5) Subroutines STRAIN and STRESS. These two subroutines are called by COUPLE and CLADF to relate stress and plastic strain, taking into consideration the direction of loading and the previous plastic deformation. A typical stress-strain curve is shown in Figure 13. This curve represents the results of a uniaxial stress-strain experiment, and may be interpreted (beyond initial yield) as the locus of work-hardened yield stresses. The equation of the curve is provided by MATPRO^[4] at each temperature.

To utilize this information, the usual idealizations of the mechanical behavior of metals are made. Thus, we assume linear elastic behavior until a sharply defined yield stress is reached, after which plastic (irrecoverable) deformation occurs. Unloading from a state of stress beyond the initial yield stress, σ_y , is

assumed to occur along a straight line having the elastic modulus for its slope. When the (uniaxial) stress is removed completely, a residual plastic strain remains, and this plastic strain completely determines the subsequent yield stress. That is, it is assumed that when the specimen is loaded again, loading will occur along line BA, and no additional plastic deformation will occur until Point A is again reached. Point A is the subsequent yield stress. If $\sigma = f(\epsilon)$ is the equation of the plastic portion of the stress-strain curve (YAC), then for a given value of plastic strain, the subsequent yield stress is found by solving simultaneously the pair of equations:

Fig. 13 Idealized σ - ϵ behavior

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$$\begin{cases} \sigma = f(\epsilon) \\ \sigma = E(\epsilon - \epsilon^P) \end{cases}$$

which may be written as

$$\sigma = f\left(\frac{\sigma}{E} + \epsilon^P\right).$$

The solution to this nonlinear equation may be computed very efficiently by Newton's Iteration Scheme:

$$\sigma^{(m+1)} = f\left(\frac{\sigma^{(m)}}{E} + \epsilon^P\right) \quad m = 0, 1, 2, \dots \quad (117)$$

The initial iterate $\sigma^{(0)}$ is arbitrary, and without loss of generality, is taken as 3.447×10^7 pascals. It can be proven that, for any monotonically increasing stress-plastic strain relation, the iteration scheme in Equation (117) converges uniformly and absolutely. Furthermore, it converges so rapidly that it is sufficient to perform the iteration no more than 30 times to obtain 8 decimal accuracy. Thus, no convergence or error criteria are necessary in utilizing this scheme.

The computations in STRAIN and STRESS are described below. It is to be noted that STRESS is only called when additional plastic deformation has occurred.

(1) Subroutine STRAIN. Values of plastic strain, ϵ^P , temperature and stress are passed to STRAIN through the calling sequence.

(a) For given temperature, obtain $\sigma = f(\epsilon)$ from MATPRO

(b) Obtain yield stress σ_y for given ϵ^P from Equation (117)

(c) For given value of stress, σ ,

$$(i) \text{ if } \sigma < \sigma_y, \quad \epsilon = \frac{\sigma}{E} + \epsilon^P$$

$$\epsilon_{new}^P = \epsilon_{old}^P$$

$$d\epsilon^P = 0$$

$$(ii) \text{ if } \sigma > \sigma_y, \quad \epsilon = f(\sigma)$$

$$\epsilon_{new}^P = \epsilon - \sigma/E$$

$$d\epsilon^P = \epsilon_{new}^P - \epsilon_{old}^P$$

(d) Return.

(2) Subroutine STRESS. Values of plastic strain, ϵ^P , temperature and plastic strain increment $d\epsilon^P$ are passed to STRESS through the calling sequence.

(a) For given temperature, obtain $\sigma = f(\epsilon)$ from MATPRO

(b) Obtain yield stress σ_y for given ϵ^P from Equation (117)

(c) Given $d\epsilon^P$ (see Figure 14)

$$\epsilon_{new}^P = \epsilon_{old}^P + d\epsilon^P$$

Fig. 14 Computations in subroutine STRESS

Since $d\epsilon^p > 0$, the new value of stress and strain must lie on the plastic portion of the stress-strain curve $\sigma = f(\epsilon)$. So σ, ϵ are obtained by simultaneously solving, as before,

$$\begin{cases} \sigma = f(\epsilon) \\ \sigma = E(\epsilon - \epsilon_{new}^p) \end{cases}$$

(d) Return.

(6) Subroutines CLOSE and GAPT. These two subroutines are called by FCMI when one of the node gaps has been found to be closed between two successive power changes. It is assumed that all the applied loads vary linearly between the two loadsteps, so that

$$P_c(\lambda) = P_c(i-1) + \lambda[P_c(i) - P_c(i-1)]$$

$$P_g(\lambda) = P_g(i-1) + \lambda[P_g(i) - P_g(i-1)] \quad (118)$$

$$T(\lambda) = T(i-1) + \lambda[T(i) - T(i-1)]$$

$$u^{\text{fuel}}(\lambda) = u^{\text{fuel}}(i-1) + \lambda[u^{\text{fuel}}(i) - u^{\text{fuel}}(i-1)]$$

where the index $i-1$ denotes the loads as the last power step, i denotes the current load values and λ varies between zero and unity. Subroutine CLOSE iterates on λ to find the exact value of λ at which the radial gap closes. Given the old and new load values for any given value of λ , subroutine GAPT computes the size of the radial gap by calling subroutine CLADF, and passing to it the linearly interpolated values computed from Equation (118). The method used is the Modified Newton-Raphson iteration scheme which solves for the roots of the equation:

$$\text{Radial gap} - f(\lambda) = 0$$

by use of the recurrence relation:

$$\lambda^{(m+1)} = \lambda^{(m)} - \frac{f[\lambda^{(m)}]}{g[\lambda^{(m)}, \lambda^{(m-1)}]} \quad (119)$$

$$g[\lambda^{(m)}, \lambda^{(m-1)}] = \frac{f[\lambda^{(m)}] - f[\lambda^{(m-1)}]}{\lambda^{(m)} - \lambda^{(m-1)}} \quad (120)$$

in which $f(\lambda)$ is the gap thickness as a function of the interpolating parameter λ as given by subroutine GAPT. The iteration procedure is performed until

$$|f(\lambda)| < \text{CUT} \quad (121)$$

where CUT is the tolerance value which currently set (internally) to the value 1.0×10^{-8} inches. If, for any reason, the iteration scheme cannot converge to a gap value less than CUT, an error message is printed out, and the last computed value of λ is returned. However, experience has shown that convergence has always occurred for values of CUT greater than 10^{-10} .

3.2 Fuel Mechanical Response

Deformation of the fuel is calculated as a function of thermal expansion, irradiation swelling, and densification. The deformation model is described here with irradiation swelling calculations described in Section IV.

3.2.1 Assumptions.

- (1) No pellet deformation from stresses induced by fuel-cladding contact or thermal stresses
- (2) No pellet creep under induced stresses
- (3) Free ring thermal expansion (i.e., each individual fuel ring is assumed to expand without restraint from any other ring and the total expansion is the summation of the individual expansions).

3.2.2 Radial Expansion. Radial expansion of the pellet due to temperature, irradiation swelling, and densification is calculated by a free ring expansion model. The governing equation for this model is

$$R_H = \sum_{i=1}^N \Delta r_i (1 + \alpha_{T_i} T_i + \epsilon s_i + \epsilon d_i) \quad (122)$$

where

R_H = hot pellet radius (m)

α_{T_i} = coefficient of thermal expansion of i^{th} radial
for temperature T_i (1/K)

ΔT_i = average temperature of i^{th} radial ring (K)

Δr_i = width of i^{th} radial ring (m)

N = number of radial rings

ϵs_i = swelling strain

ϵd_i = densification strain.

3.2.3 Axial Expansion. Axial expansion of the fuel stack is calculated by summing the maximum ring axial expansions of each pellet. Maximum ring axial expansion of each pellet is calculated as the difference between the length of the ring with the maximum overall hot length and the cold length of that ring.

The calculation of overall ring height includes consideration of the depression of a central dish. The fuel stack length is thus calculated by

$$L_f = \sum_{j=1}^M (1 + \alpha_{T_j} T_j + \epsilon s_j + \epsilon d_j) \ell_j \quad (123)$$

where

i is the ring with the maximum axial length of the j^{th} node

and

L_f = hot length of the fuel stack (m)

M = number of axial nodes

ℓ_i = length of i^{th} radial ring (m)

3.2.4 Fuel Surface Relocation. Two closely related models are used to compute the effect of relocation. The fuel surface relocation model results in an effective pellet-cladding gap which is used in computing the gap conductance, the thermal gap. The space made available for cracks based on this model is distributed inside the pellet. This leads to the second relocation model which calculates an effective thermal conductivity across this cracked pellet. The effective thermal conductivity model was discussed in 1.7.

The amount of fuel surface relocation necessary to result in the proper gap closure versus power was studied by Coleman using FRAP-T3. These results are presented in Reference 23. The model which was developed from these results provides FRAP with an effective fuel strain which is a function of cold state geometry. This strain is treated as any of the other fuel radial strains (thermal expansion, swelling, and densification).

$$\left(\frac{\Delta r}{r}\right)_{\text{rel}} = 0.0 \quad \text{when, } \frac{\delta}{r_p} \leq 0.005$$

$$= \frac{\delta}{r_p} - 0.005 \quad \text{when, } \frac{\delta}{r_p} > 0.005$$

where, $\left(\frac{\Delta r}{r}\right)_{\text{rel}}$ = relocation strain (m/m)

Fig. 15 Fuel surface relocation

δ = cold state radial gap (m)

r_p = cold state pellet radius (m)

This model is shown graphically in Figure 15.

4. FUEL ROD FAILURE MODELS

A subcode has been developed for the FRAP codes to predict failure of the zircaloy cladding of light water reactor fuels. This subcode, FRAIL (FRAP Integrity Limits), is a modular routine that predicts the probability of failure of fuel cladding under a variety of steady state and transient conditions for FRAPCON and FRAP-T, respectively.

The FRAIL subcode is composed of several subroutines, each of which has the capability of predicting a different mode of zircaloy fuel rod cladding failure. The subroutines are distinguished from one another by mode of failure. A probability of failure is calculated for each mode, and these probabilities are then appropriately combined to yield a general probability of failure of the fuel rod cladding. The cladding failure modes that are currently operable in FRAIL include:

- (1) Cladding melt
- (2) oxide layer wall thinning
- (3) ballooning (without rupture)
- (4) eutectic melt
- (5) collapse

(6) overstress failure

(7) cumulative stress damage

(8) overstrain failure

For more detail on specific models within the FRAIL subcode and verification of them see reference (24).

VII. REFERENCES

1. J. A. Dearien et al, FRAP-S3 Analytical Models and Input Manual
FRAP-S3 A Computer Code for the Steady-State Analysis of Oxide
Fuel Rods, Volume 1, Revision 2, TFBP-TR-164 (March 1978).
2. C. R. Hann, C. E. Beyer, and L. J. Parchen, GAPCON-THERMAL-1: A
computer Program for Calculating the Gap Conductance in Oxide
Fuel Pins, BNWL-1778 (September 1973).
3. J. A. Dearien et al, FRAP-T4: A Computer Code for the Transient
Analysis of Oxide Fuel Rods, CDAP-TR-78-027 (July 1978).
4. P. E. MacDonald et al, MATPRO: A Handbook of Materials Proper-
ties For Use in the Analysis of Light Water Reactor Fuel Rod
Behavior, ANCR-1263, NRC-3 (February 1976).
5. F. W. Dittus and L. M. K. Boelter, "Heat Transfer in Automobile
Radiators of the Tubular Type," University of California Publica-
tions in Engineering, 2, 13 (1930) pp 443-461.
6. W. H. Jens and P. A. Lottes, Analysis of Heat Transfer, Burnout,
Pressure Drop, and Density Data for High-Pressure Water, ANL-4627
(1951).
7. J. Weisman et al, "Fission Gas Release from UO_2 Fuel Rods with
Tim Varying Power Histories," ANS Transactions, 12, 2
(November 1969).
8. M. P. Bohn, FRACAS -- A Subcode for the Analysis of Fuel Pellet-
Cladding Mechanical Interaction, TREE-NUREG-1029 (April 1977).
9. R. J. Sand et al, Fuel Model Development of RELAP4/MOD7
VERSION 2, CDAP-TR-78-025, June 1978.

10. P. E. MacDonald et al, MATPRO-Version 09 - A Handbook of Materials Properties for Use in the Analysis of Light Water Reactor Fuel Rod Behavior, TREE-NUREG-1005, (December 1976).
11. A. M. Ross and R. L. Stoute, Heat Transfer Coefficient Between UO_2 and Zircaloy-2, AECL-1552 (June 1962).
12. C. Jacobs and N. Todreas, "Thermal Contact Conductance in Reactor Fuel Elements," Nuclear Science and Engineering, 50, 3 (March 1973) pp 283-290.
13. H. Fenech and W. M. Rohsenow, "A Prediction of Thermal Conductance of Metallic Surfaces in Contact," Transactions of the ASME, Series C, Journal of Heat Transfer, 85, 1 (February 1963) pp 15-24 (ASME paper 62-HT-32).
14. J. P. Holman, Heat Transfer, McGraw-Hill, Inc., 1968.
15. B. Finlayson, The Method of Weighted Residuals and Variational Principles, Academic Press, New York, N. Y., 1972.
16. W. H. McAdams, Heat Transmission, 34d ed., New York: McGraw-Hill Book Company, Inc., 1954.
17. A. H. Booth, A Method of Calculating Fission Gas Diffusion from UO_2 Fuel and Its Application to the X-2 Loop Test, AECL-496 (CRDC-721), (1957).
18. H. M. Ferrari, "Nitrogen Release from UO_2 Pellets at Elevated Temperatures," Nuclear Science and Engineering, 17, 4 (December 1963).
19. H. M. Ferrari, "Diffusion of Nitrogen in Uranium Dioxide," Journal of Nuclear Materials, 12, 2 (1964).

20. A Mendelson, Plasticity: Theory and Applications, New York: The MacMillan Company, 1968.
21. E. F. Ibrahim, An Equation for Creep of Cold-Worked Zircaloy Pressure Tube Material, AECL-2528 (December 1965).
22. C. E. Pugh et al; Currently Recommended Constitutive Equations for Inelastic Design Analysis of FFTF Components, ORNL-TM-3602 (September 1973).
23. D. R. Coleman and E. T. Laats, FRAP-T3: A Computer Code for Transient Analysis of Oxide Fuel Rods, Volume II - Model Verification Report, TFBP-TR-180 (1977).
24. J. D. Kerrigan, FRAIL 3: A Fuel Rod Failure Subcode, TFBP-TR-189, April 1977.

APPENDIX A

FRAPCON INPUT INSTRUCTIONS

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FRAPCON INPUT INSTRUCTIONS

The required input to FRAPCON is presented in this appendix. Section 1 describes the use of NAMELIST, the free form input used. Section 2 presents the problem size, standard, and optional input with a listing of each mnemonic variable, a description of the variable, and any restrictions or options associated with the variable. This is followed by the plot subcode input requirements. Section 3 presents the job control language required to run FRAPCON on the Idaho National Engineering Laboratory (INEL) CDC 7600.

1. NAMELIST INPUT SPECIFICATION

Input data must be in a special form in order to be read using a NAMELIST list. The first character in each record (card) to be read must be blank. The second character in the first record of a group of data records must be a \$, immediately followed by the appropriate NAMELIST name. Two NAMELIST names are used - FRPCN for the problem size input and FRPCON for the general input. The NAMELIST name must be followed by a blank and must not contain any embedded blanks. This name is followed by data items separated by commas. (A comma after the last item is optional.) The end of a data group is signaled by \$END.

The form of the data items in an input record is:

symbolic name = constant

The symbolic name may be an array element name or a variable name. Subscripts must be integer constants. The constant may be an integer, real, literal, complex, or logical. (If the constants are logical, they may be in the form T or .TRUE. and F or .FALSE.)

array name = set of constants (separated by commas)

The set of constants consists of constants of the type integer, real, literal, complex, or logical. The number of constants must be less than or equal to the number of elements in the array.

Successive occurrences of the same constant can be represented in the form k*constant, where k is a nonzero integer constant specifying the number of times the constant is to occur.

Each data record (card) must begin with a blank followed by a complete variable or array name or constant. Embedded blanks are not permitted in names or constants. Trailing blanks after integers and exponents are treated as zeros. The order of the data cards between the NAMELIST name card and the \$END card is not significant.

2. INPUT DATA

2.1 Problem Size Input

The first card which must be included is a job title card. The first 72 columns are available for the title. The following input must be supplied to specify the problem size. These follow \$FRPCN and are followed by \$END.

<u>Variable</u>	<u>Description</u>	<u>Restrictions and Options</u>
IM	Number of power-time steps	must equal number of time steps (maximum of 100)
NA	Number of equal length axial nodes	must be odd interger $3 < NA < 21$
NR	Number of radial nodes	must be equal to 11 for FRAPCON-1

2.2 General Input

The following input must be supplied unless an optional input card replaces it. This input is preceded by \$FRPCØN and followed by \$END.

Standard Input

<u>Variable</u>	<u>Description</u>	<u>Restrictions and Options</u>
CPL	Plenum length (in., m)	none
DCI	Diameter of cladding, inside (in., m)	none
DCØ	Diameter of cladding, inside (in., m)	none
DE	Equivalent heated diameter (in., m)	none
DEN	Pellet true density (% theoretical density - 10.97 gm/cc)	none
DISHSD	Dish shoulder width (pellet radius minus dish radius) (in., m)	none
DP	Diameter of pellet (in., m)	none
DSPG	Diameter of spring, outside (in., m)	none

DSPGW	Diameter of spring wire (in., m)	none
ENRCH	Fuel enrichment, used in flux depression estimate	
FGPAV	Initial gas pressure (psia, N/m ²)	none
GØ	Mass flow rate (lb/hr-ft ² , kg/s-m ²)	= 0, cladding surface temperature = TW at all axial nodes. If NSP = 1, input one value for each power-time step.
HDISH	Height of pellet end dish (in., m)	none
HPLT	Height of pellet (in., m)	none
ICM	Index for cladding material	= 2, zircaloy-2 cladding = 4, zircaloy-4 cladding
IDXGAS	Index for initial fill gas	= 1, helium = 2, air = 3, nitrogen = 4, fission gas = 5, argon = 6, user specifies mole fractions of the above (see AMFAIR, etc. in the optional input section). If fission products are input, user should consider input of initial burnup, BUIN

IQ	Axial power shape index	= 0, shape input = 1, cosine shape
JDLPR	Index for axial nodes to be output	= 1, and NOPT = 0, peak power node ≠ 1, and NOPT = 0, all nodes
JN	Number of entries in each set of QF and X tables	omit if IQ = 1. Maximum of 40. If more than one shape JST must be input. Number of values must equal number of axial shapes used.
NUNITS	Units to be input (defaults to SI units)	= 0, SI units are input = 1, British units are input
P2	System pressure (psia, N/m^2)	if NSP = 1, input one value for each power-time step
QF	Pointwise axial heat flux normalization factors	number of entries must equal JN for each axial shape. $1 < Q < 41$ for each shape up to 8 shapes. If more than one shape, normalize all shapes to the average
QMPY	Heat flux at each power ($BTu/hr-ft^2$) or ($kW/ft, W/cm$)	= kW/ft or W/cm if first value is less than 100. = $Btu/hr-ft^2$ if first value is greater than 100.

TIME	Table of accumulated times. (end of step times) Corresponding to QMPY entries (hours)	= seconds if first value greater than 17.0 = hours if first value > 0.01 hour but, < 1.0 hour
TØTL	Fuel stack height (ft, m)	none
TW	Inlet water temperature (°F, K)	if GØ = 0.0, TW is clad surface temperature. If NSP = 1, input one value for each power-time step
VS	Total number of spring turns	none
X	Table of axial stations corresponding to QF entries (ft, m)	number of entries must equal JN for each axial shape. $1 < X < 41$ for each shape up to 8 shapes. (First value must be 0.0 and last value must be equal to the total length TØTL for each shape)

The following variables are predefined in the code. It is not necessary to include these cards in the input data deck unless a value other than that listed below is desired. The "additional" factors (i.e., AFAL, AFCR, etc.) may be used in parametric or scoping studies, since they are multipliers on the appropriate parameters.

Optional Input

<u>Variable</u>	<u>Default Value</u>	<u>Description</u>	<u>Restrictions and Options</u>
AFAL	1.0	Additional thermal expansion factor	$\neq 0$

AFCR	1.0	Additional creep acceleration factor	$\neq 0$
AFDN	1.0	Additional densifi- cation factor	none
AFGR	1.0	Additional frac- tional gas release multiplication factor	$\neq 0$
AFTC	1.0	Additional fuel ther- mal conductivity multiplication factor	$\neq 0$
AFSW	1.0	Additional fuel swelling multipli- cation factor	$\neq 0$
AMFAIR	0.0	Absolute mole frac- tion of air	use only if IDXGAS = 6
AMFARG	0.0	Absolute mole frac- tion of argon	use only if IDXGAS = 6
AMFFG	0.0	Absolute mole frac- tion of fission gas	use only if IDXGAS = 6
AMFKRY	0.0	Absolute mole frac- tion of krypton	use only if IDXGAS = 6 and AMFFG = 0.0
AMFXE	0.0	Absolute mole frac- tion of xenon	use only if IDXGAS = 6 and AMFFG = 0.0

AMFHE	0.0	Absolute mole fraction of helium	use only if IDXGAS = 6
AMFH2	0.0	Absolute mole fraction of hydrogen	use only if IDXGAS = 6
AMFH2Ø	0.0	Absolute mole fraction of steam	use only if IDXGAS = 6
AMFN2	0.0	Absolute mole fraction of nitrogen	use only if IDXGAS = 6
BUIN	0.0	Initial fuel burnup (MWd/MtU, MWs/kg)	should consider including input of fission product mole fractions
CATEXF	0.05	Texture factor (fraction of cladding cells with basal poles parallel to the tube axis)	$0.0 \leq \text{CATEXF} \leq 1.0$
CØLDWK	0.1	Cold work of the cladding	none
CØMP	0.0	Weight percent of PuØ_2 in mixed oxide fuel	$0.0 \leq \text{CØMP} \leq 100.0$
CRDT	1.0	Initial crud thickness (mil, m)	none
CRDTR	1.1415525×10^{-4}	Crud buildup rate (mil/hr, m/s)	used only if ICØR = 2

DENG	0.75	Porosity correction pellet density (%) emersion (true) den- sity to geometric density	none
FA	1.0	axial hot node peak- ing factor (peak/ average)	= 1.0, if QF table is normalized to the average, and QMPY = average heat flux. > 1.0, if IQ = 1 and QMPY = peak heat flux. > 1.0, if IQ table is normalized to the peak, and QMPY = peak heat flux
FLUX	6×10^{17}	Fast neutron flux experienced by rod	none
FQE	1.0	Multiplication factor on QMPY values	none
GRNSZ	5.0 microns	Initial grain size	none
ICØR	0	Index for crud model	= 0, constant crud, no crud temperature drop if boiling. = 1, constant crud, crud temperature drop if boiling. = 2, varying crud, crud temperature drop if boiling

JST	IM*1	Indicates types of axial power shape to be used for each time step	IQ must = 0. Must be one type number for each time step, (the first QF and X array is Type 1, the second is Type 2, etc.), maximum of eight types. The axial shapes must be normalized to the average
NFRØD	1	Number of like fuel rods being analyzed	none
NGAPC	1	Gap conductance model selector	= 0, cracked pellet model = 1, annular gap model
NØFAIL	0	Failure Subcode switch	= 0, failure subcode is called and results output = 1, failure subcode is bypassed
NØPT	3	Printout selector	= 0, full output = 3, short (tabular) output
NPCYCL	0	Number of previous power cycles	none
NPLTAB	0	Parameter on the plot abscissa	= 0 - time = 1 - rod average power = 2 - local burnup
NREAD	0	FRAPCON restart read index	= 0, no restart = 1, read restart tape

NRESTR	0	FRAPCON restart index	= 0, no restart write = 1, write restart tape
NSP	0	Varying system parameter	= 0, constant system parameter (P2, TW, GØ) = 1, varying system parameters
NTAPE	0	FRAP-T restart index	= 0, no restart data stored = 1, restart data stored for FRAP-T use at end of each power-time step
PPMH2Ø	0.0	Fuel initial water content (PPM)	none
PPMN2	15.0	Fuel initial nitrogen content (PPM)	none
QEND	0.3	Normalized heat flux at top of fuel stack	must be one value for each axial shape. Used to determine heat flow into the plenum
RC	0.0	Pellet core radius (in., m)	none
RØUGHC	4.5×10^{-5}	Arithmetic mean roughness of cladding (in., m)	none
RØUGHF	8.5×10^{-5}	Arithmetic mean roughness of fuel (in., m)	none

SGAPF	30	Fission gas atoms per 100 fissions	none
TSINT	2912 F	Fuel sintering tem- perature (°F, K)	none
UMELT	MATPRO computed value	User specified clad- ding failure melt temperature criterion	none
UØFD	17.0%	User specified clad- ding oxide failure depth criterion	none

2.3 Plot Input

The following is the input data for plotting. If no plots are desired, follow the "\$END" card of the preceding input with card 1 below. If plots are desired, a full set of plot data is required.

Card No. 1

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 5	I	NPLTNØ	Number of axial nodes at which plots are desired. If no plots wanted, input the number 0.

Card No. 2 - Specification of axial nodes at which plots are wanted

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 5	I	IAPLT(1)	Number of an axial node at which plots are wanted.

6 - 10	I	IAPLT(2)	Number of an axial node at which plots are wanted.
--------	---	----------	---

Repeat as necessary for IAPLT(K) (K being an axial node number)

Card No. 3 - Time axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TSTART	Minimum time on time axis (hr, sec).
11 - 20	F	TEND	Maximum time on time axis (hr, sec).
21 - 30	F	AXLT	Length of time axis (in.)
31 - 70	A	LABLT	Label to be given time axis.

Card No. 4 - Cladding surface temperature axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TSMIN	Minimum cladding surface temperature on axis (°F, K).
11 - 20	F	TSMAX	Maximum cladding surface temperature in axis (°F, K).
21 - 30	F	AXLTS	Length of surface temperature axis (in.).
31 - 70	A	LABLTS	Label to be given surface temperature axis.

Card No. 5 - Fuel centerline temperature axis

1 - 10	F	TOLMIN	Minimum fuel centerline temperature on axis ($^{\circ}\text{F}$, K).
11 - 20	F	TOLMAX	Maximum fuel centerline temperature on axis ($^{\circ}\text{F}$, K).
21 - 30	F	AXLTMP	Length of centerline temperature axis (in.).
31 - 70	A	LABLTM	Label to be given centerline temperature axis.

Card No. 6 - Gas pressure axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	PMIN	Minimum gas gap pressure on axis (psia, N/m^2).
11 - 20	F	PMAX	Maximum gas gap pressure on axis (psia, N/m^2).
21 - 30	F	AXLP	Length of gas gap pressure axis (in.).
31 - 70	A	LABLP	Label to be given gas gap pressure axis.

Card No. 7 - Cladding hoop strain axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	EPSMIN	Minimum cladding hoop strain on axis (dimensionless).

11 - 20	F	EPSMAX	Maximum cladding hoop strain on axis (dimensionless).
21 - 30	F	AXLEPS	Length of cladding hoop strain axis (in.).
31 - 70	A	TABLE	Label to be given cladding hoop strain.

Card No. 8 - Fuel axial displacement axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	UZFMIN	Minimum fuel axial displacement on axis (ft, m).
11 - 20	F	UZFMAX	Maximum fuel axial displacement on axis (ft, m).
21 - 30	F	AXLUZF	Length of fuel axial displacement axis (in.).
31 - 70	A	LABLUF	Label to be given fuel axial displacement axis.

Card No. 9 - Cladding axial displacement axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	UZCMIN	Minimum cladding axial displacement on axis (ft, m).
11 - 20	F	UZCMAX	Maximum cladding axial displacement on axis (ft, m).

21 - 30	F	AXLUZC	Length of cladding axial displacement axis (in.).
31 - 70	A	LABLUC	Label to be given cladding axial displacement axis.

Card No. 10 - Fuel rod power axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	PMIN	Minimum linear fuel rod power on axis (kW/ft, kW/m).
11 - 20	F	PMAX	Maximum linear fuel rod power on axis (kW/ft, kW/m).
21 - 30	F	PLEN	Length of linear fuel rod power axis (in.).
31 - 70	A	PLABL	Label to be given fuel rod power axis.

Card No. 11 - Fuel surface temperature axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TFSMIN	Minimum fuel surface temperature on axis (°F, K).
11 - 20	F	TFSMAX	Maximum fuel surface temperature on axis (°F, K).
21 - 30	F	TFSLEN	Length of fuel surface temperature axis (in.).

31 - 70 A TFSLAB Label to be given fuel surface temperature axis.

Card No. 12 - Gap heat transfer coefficient axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	HGMIN	Minimum gap heat transfer coefficient on axis (Btu/hr-°F-ft ² , W/m ² -K).
11 - 20	F	HGMAX	Maximum gap heat transfer coefficient on axis (Btu/hr-°F-ft ² , W/m ² -K).
21 - 30	F	HGLEN	Length of gap heat transfer coefficient axis (in.).
31 - 70	A	HGLABL	Label to be given gap heat transfer coefficient axis.

Card No. 13 - Surface heat transfer coefficient axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	HSMIN	Minimum surface heat transfer coefficient on axis (Btu/hr-°F-ft ² , W/m ² -K).
11 - 20	F	HSMAX	Maximum surface heat transfer coefficient on axis (Btu/hr-°F-ft ² , W/m ² -K).

21 - 30	F	HSLEN	Length of surface heat transfer coefficient axis (in.).
31 - 70	A	HSLAB	Label to be given surface heat transfer coefficient axis.

Card No. 14 - Average cladding temperature axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TAMIN	Minimum average cladding temperature on axis (°F, K).
11 - 20	F	TAMAX	Maximum average cladding temperature on axis (°F, K).
21 - 30	F	TALEN	Length of average cladding temperature axis (in.).
31 - 70	A	TALABL	Label to be given average cladding temperature axis.

Card No. 15 - Zircaloy-Oxide thickness axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	ZOMIN	Minimum ZrO_2 thickness (in., mm).
11 - 20	F	ZOMAX	Maximum ZrO_2 thickness (in., mm).
21 - 30	F	ZOLEN	Length of ZrO_2 thickness (in.).
31 - 70	A	ZOLABL	Label to be given zircaloy-oxide thickness axis.

Card No. 16 - Mole fraction of helium

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	MFMIN	Minimum helium mole fraction on axis.
11 - 20	F	MFMAX	Maximum helium mole fraction on axis.
21 - 30	F	MFLEN	Length of helium mole fraction axis (in.).
31 - 70	F	MFLABL	Label to be given helium mole fraction axis.

Card No. 17 - Plenum temperature axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TPMIN	Minimum plenum temperature on axis (°F or K).
11 - 20	F	TPMAX	Maximum plenum temperature on axis (°F or K).
21 - 30	F	TPLEN	Length of plenum temperature axis (in.).
31 - 70	A	TPLABL	Label to be given plenum temperature axis.

Card No. 18 - Rod gas increase axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	RGMIN	Minimum gas increase on axis (% of initial).
11 - 20	F	RGMAX	Maximum gas increase on axis (% of initial).
21 - 30	F	RGLEN	Length of gas increase axis (in.).
31 - 70	A	RGLABL	Label to be given gas increase axis.

Card No. 19 - Mass flux axis (plot of average mass flux in coolant channels surrounding fuel rod)

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	GMIN	Minimum mass flux on axis (lbm/ft ² -hr or kg/m ² -s).
11 - 20	F	GMAX	Maximum mass flux on axis (lbm/ft ² -hr or kg/m ² -s).
21 - 30	F	GLEN	Length of mass flux axis (in.).
31 - 70	A	GLABL	Label to be given mass flux axis.

Card No. 20 - Fuel stored energy axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	SEMIN	Minimum stored energy on axis (Btu/lbm, Cal/kg).

11 - 20	F	SEMAX	Maximum stored energy on axis (Btu/lbm, Cal/kg).
21 - 30	F	SELEN	Length of average stored energy axis (in.).
31 - 70	A	SELABL	Label to be given average stored energy axis.

Card No. 21 - Coolant pressure axis (plot of average pressure in coolant channel surrounding fuel rod)

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	PCMIN	Minimum pressure on axis (psia or N/m^2).
11 - 20	F	PCMAX	Maximum pressure on axis (psia or N/m^2).
21 - 30	F	PCLLEN	Length of pressure axis (in.).
31 - 70	A	PCLABL	Label to be given coolant pressure axis.

Card No. 22 - Gap thickness axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	THKMIN	Minimum gap thickness on axis (mil or m).
11 - 20	F	THKMAX	Maximum gap thickness on axis (mil or m).

21 - 30	F	THKLEN	Length of bulk temperature axis (in.).
31 - 70	A	THKLAB	Label to be given gap thickness axis.

Card No. 23 - Bulk coolant temperature axis

<u>Columns</u>	<u>Format</u>	<u>Name</u>	<u>Quantity</u>
1 - 10	F	TBMIN	Minimum bulk temperature on axis (°F or K).
11 - 20	F	TBMAX	Maximum bulk temperature on axis (°F or K).
21 - 30	F	TBLEN	Length of bulk temperature axis (in.).
31 - 70	A	TBLAB	Label to be given bulk temperature axis.

3. JOB CONTROL LANGUAGE (JCL)

3.1 JCL Cards for Creating a Set of FRAPCON Object Decks on the
INEL CDC 7600 Computer

Card No.

- 1 Job Card
- 2 Account Card
- 3 STAGE, INF, PE, E, VSN=AXXXXX.
- 4 STAGE, ØUTF, PE, E, PØST.
- 5 CØPYP, INF, ØLDPL.
- 6 UPDATE, C, F.

```

7  FTN,I=CØMPILE.
8  CØPYP,INF,DUM.
9  RETURN,DUM.
10 CØPYP,INF,ØLDPL.
11 UPDATE,C,F.
12 FTN,I=CØMPILE,B=LGX.
13 REWIND,LGX,LGØ.
14 CØPYP,LGX,ØUTF.
15 CØPYP,LGØ,ØUTF.
16 CØPYP,INF,DUM.
17 CØPYP,INF,ØLDPL.
18 UPDATE,C,F.
19 FTN,I=CØMPILE,B=PLT.
20 REWIND,PLT,ØUTF.
21 CØPY,PLT,ØUTF.

```

3.2 JCL Cards for Executing the Load Module on the INEL CDC 7600

Card No.

```

1  Job Card
2  Account Card
3  STAGE,ØUTF,PE,E,VSN=AXXXXX.
4  CØPYP,ØUTF,LGØ.
5  CØPYP,ØUTF,LGX.
6  REWIND,LGØ,LGX.
7  RFL=160000.
8  SEGLØAD.
9  LØAD,LGØ,LGX.
10 EXECUTE,FRPCØN,PL=50000.
11 EØR CARD
12      TREE FRPCØN-FTBNUL

```

```

13  FTBNUL GLØBAL FAST
14      END FRPCØN
15  EØR CARD
    C    INPUT  DECK
16  EØF CARD

```

NOTE: Tape AXXXXX was created in 3.1.

3.3 Additional JCL Cards for Plotting FRAPCON Results

<u>Card</u>	<u>Position in 3.2 JCL Cards</u>
STAGE,TAPE10,NT,HD,PØST.	between 2 and 3
ATTACH,ENVRL.	between 2 and 3
LIBRARY,ENVRL.	between 2 and 3
FILE,TAPE10,RT=F,FL=80,BT=K,RB=3.	between 3 and 4
CØPYP,ØUTF,PLT.	between 5 and 6
REWIND,TAPE17,PLT.	between 10 and 11
LØAD,PLT.	between 10 and 11
EXECUTE,FRPLT.	between 10 and 11

APPENDIX B

EXAMPLE PROBLEM INPUT AND OUTPUT

APPENDIX B

EXAMPLE PROBLEM INPUT AND OUTPUT

This section presents the general format used for input to FRAPCON and the output format showing the information a user would expect to receive from a run.

(1) Example Problem Input

A set of data for an example problem is presented in Table B-I and B-II.

(2) Example Problem Output

- (a) Initial problem parameters and history information appear in the first three pages of output as shown in the first three pages of Table B-III.
- (b) A typical set of output information for one axial station is shown in the fourth page of Table B-III.
- (c) A power step summary of the entire rod is presented on the fifth page of Table B-III.
- (d) A problem summary of information calculated at the peak power axial level along with other information provided at a problem's completion are shown on the seventh and eighth pages of Table B-III.

TABLE B-I

EXAMPLE PROBLEM DATA (PCM-20 PBF ROD)

Coolant Conditions:

mass flux, 1.0×10^6 (lbm/hr-ft²) - 1356.3 (kg/sec-m²)
inlet temperature, 642 (°F) - 612 (K)
heated diameter, 0.348 (in.) - 0.00884 (m)
pressure, 2250 (psia) - 1.55×10^7 (pascals)

Fuel Rod Geometry:

fuel stack length, 3.0 (ft) - 0.9144 (m)
fuel rod outside diameter, 0.422 (in.) - 0.0107 (m)
fuel pellet diameter, 0.366 (in.) - 0.0093 (m)
plenum length, 2.0 (in.) - 0.0508(m)

Fuel Initial Conditions:

density, 93.5 (% theoretical density)
pressure, 300 (psia) - 2.068×10^6 (pascals)
enrichment, 35 (%)

Power History:

a power ramp from 5 kW/ft to 10 kW/ft in 10 hours or from
16.4 kW/m to 32.8 kW/m in 3.6×10^4 sec

TABLE B-III

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****          X
X    STEADY-STATE FUEL ROD ANALYSIS CODE    X
X    MOD 001 VERS 001    MAT PRO MODULE MOD10A    X
X    RUN DATE = 07/19/78    OPTIONS 71000000 PAGE    1    X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          PBF SINGLE ROD PCM-20          X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

```

	STEP	TIME(HRS)	QMPY(KW/FT)	P2(PSSI)	TW(F)	GO(LBM/HR-FT2)
CLAD MATERIAL IS ZIRCALOY-4						
COLD CLAD O.D. = .422000(IN)	1	.01	5.000	2250.0	642.00	1000000.0
COLD CLAD I.D. = .374000(IN)	2	10.00	10.000	2250.0	642.00	1000000.0
COLD CLAD THICKNESS = .0240 (IN)	3	0.00	0.000	0.0	0.00	0.0
	4	0.00	0.000	0.0	0.00	0.0
COLD DIAMETRAL GAP = 8.0000(MILS)	5	0.00	0.000	0.0	0.00	0.0
	6	0.00	0.000	0.0	0.00	0.0
COLD INTERNAL HELIUM PRESSURE = 300.00(PSSIA)	7	0.00	0.000	0.0	0.00	0.0
	8	0.00	0.000	0.0	0.00	0.0
	9	0.00	0.000	0.0	0.00	0.0
COLD PELLET DIAMETER = .366000(IN)	10	0.00	0.000	0.0	0.00	0.0
COLD PELLET LENGTH = .65450(IN)	11	0.00	0.000	0.0	0.00	0.0
PELLET TRUE DENSITY = 93.500(PERCENT)	12	0.00	0.000	0.0	0.00	0.0
ENRICHMENT = 35.0000(WEIGHT PERCENT)	13	0.00	0.000	0.0	0.00	0.0
FUEL STACK HEIGHT = 3.0000(FT)	14	0.00	0.000	0.0	0.00	0.0
DISH SPHERICAL RADIUS = .49553(IN)	15	0.00	0.000	0.0	0.00	0.0
DISH DEPTH = .61500(IN)	16	0.00	0.000	0.0	0.00	0.0
DISH SHOULDER WIDTH = .06200(IN)	17	0.00	0.000	0.0	0.00	0.0
PELLET CORE RADIUS = 0.00000(IN)	18	0.00	0.000	0.0	0.00	0.0
COLD FUEL VOLUME = 3.74937(CU.IN.)	19	0.00	0.000	0.0	0.00	0.0
DISH VOLUME FRACTION OF FUEL = .01007	20	0.00	0.000	0.0	0.00	0.0
FUEL ROUGHNESS VOLUME = .0019(PERCENT)	21	0.00	0.000	0.0	0.00	0.0
FUEL SINTERING TEMP = 2912.0 (F)	22	0.00	0.000	0.0	0.00	0.0
COLD PLENUM LENGTH = 2.0000(IN)	23	0.00	0.000	0.0	0.00	0.0
SPRING DIAMETER = .35500(IN)	24	0.00	0.000	0.0	0.00	0.0
SPRING WIRE DIAMETER = .04000(IN)	25	0.00	0.000	0.0	0.00	0.0
TOTAL SPRING TURNS = 17.00000	26	0.00	0.000	0.0	0.00	0.0
INITIAL BURNUP = 0. (MWD/MTU)	27	0.00	0.000	0.0	0.00	0.0
SPRING VOLUME = .02114(CU.IN.)	28	0.00	0.000	0.0	0.00	0.0
PLENUM VOLUME = .19858(CU.IN.)	29	0.00	0.000	0.0	0.00	0.0
TOTAL COLD VOID VOLUME = .41914(CU.IN.)	30	0.00	0.000	0.0	0.00	0.0
ARITHMETIC MEAN ROUGHNESS(FUEL) = .0000900(IN)	31	0.00	0.000	0.0	0.00	0.0
ARITHMETIC MEAN ROUGHNESS(CLAD) = .0000500(IN)	32	0.00	0.000	0.0	0.00	0.0
CHANNEL EQUIVALENT DIAMETER = .02900(FT)	33	0.00	0.000	0.0	0.00	0.0
FISSION GAS ATOMS PER 100 FISSIONS = 30.0	34	0.00	0.000	0.0	0.00	0.0
INITIAL WATER CONCENTRATION = 0.00(PPM)	35	0.00	0.000	0.0	0.00	0.0
INITIAL NITROGEN CONCENTRATION = 15.00(PPM)	36	0.00	0.000	0.0	0.00	0.0
	37	0.00	0.000	0.0	0.00	0.0
AFGR = 1.0000	38	0.00	0.000	0.0	0.00	0.0
AFGR = 1.0000	39	0.00	0.000	0.0	0.00	0.0
AFGR = 1.0000	40	0.00	0.000	0.0	0.00	0.0
EXC = 1.0000	41	0.00	0.000	0.0	0.00	0.0
FA = 1.0000	42	0.00	0.000	0.0	0.00	0.0
AFDN = 1.0000	43	0.00	0.000	0.0	0.00	0.0
FLXDP = .70319	44	0.00	0.000	0.0	0.00	0.0
BUCRIT = 0.0						

TABLE B-III (continued)

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****          X
X    STEADY-STATE FUEL ROD ANALYSIS CODE    X
X  MOD 001 VERS 001  MAT PRO MODULE MOD10A  X
X  RUN DATE = 07/19/78  OPTIONS 71600000 PAGE 2 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

```

ANGI = .0003349 H2DMI = 0.0000000

IPLANT = 0

ICOR = 1

CRUD THICKNESS = 1.00 (MILS)

CRUD RATE = 0.00 (MILS/C.Y.)

.25400E-04 (M)

BRITISH UNITS ARE USED

GAP CONDUCTANCE = 1

PLOTS NOT REQUESTED

45	0.00	0.000	0.0	0.00	0.0
46	0.00	0.000	0.0	0.00	0.00
47	0.00	0.000	0.0	0.00	0.00
48	0.00	0.000	0.0	0.00	0.00
49	0.00	0.000	0.0	0.00	0.00
50	0.00	0.000	0.0	0.00	0.00
51	0.00	0.000	0.0	0.00	0.00
52	0.00	0.000	0.0	0.00	0.00
53	0.00	0.000	0.0	0.00	0.00
54	0.00	0.000	0.0	0.00	0.00
55	0.00	0.000	0.0	0.00	0.00

TABLE B-III (continued)

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X      *** FRAPCON ***      X
X      STEADY-STATE FUEL ROD ANALYSIS CODE      X
X      MOD 001 VERS 001 MAT PRO MODULE MOD10A      X
X      RUN DATE = 07/19/78 OPTIONS 71000000 PAGE 3 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
XXXXXXXXXXXXXXXXXX AXIAL POWER SHAPE INFORMATION XXXXXXXXXXXXXXXXXXXX

```

AXIAL DISTRIBUTION IS INPUT

ROD AVERAGE POWER IS INPUT
X'S IN FEET

NUMBER OF AXIAL NODES = 9

XXXXXXXXXXXXXXXXXXXX INPUT AXIAL SHAPE NUMBER 1 XXXXXXXXXXXXXXXXXXXX

X(1) = 0.0000	X(2) = .1667	X(3) = .3333	X(4) = .5000	X(5) = .6667	X(6) = .8333	X(7) = 1.0000
X(8) = 1.1667	X(9) = 1.3333	X(10) = 1.5000	X(11) = 1.6667	X(12) = 1.8333	X(13) = 2.0000	X(14) = 2.1667
X(15) = 2.3333	X(16) = 2.5000	X(17) = 2.6667	X(18) = 2.8333	X(19) = 3.0000		
QF(1) = .6200	QF(2) = .7050	QF(3) = .8750	QF(4) = 1.0650	QF(5) = 1.2150	QF(6) = 1.3100	QF(7) = 1.3700
QF(8) = 1.3800	QF(9) = 1.3750	QF(10) = 1.3350	QF(11) = 1.2750	QF(12) = 1.1800	QF(13) = 1.0600	QF(14) = .9400
QF(15) = .8150	QF(16) = .6850	QF(17) = .5500	QF(18) = .4400	QF(19) = .3800		

INCREMENT	AXIAL STATION FEET	METERS	UNNORMALIZED HEAT FLUX
1	.1667	.05080	.7262
2	.5000	.15240	1.0550
3	.8333	.25400	1.3013
4	1.1667	.35560	1.3762
5	1.5000	.45720	1.3300
6	1.8333	.55880	1.1737
7	2.1667	.66040	.9388
8	2.5000	.76200	.6838
9	2.8333	.86360	.4525

AVGQI = 1.004167

NORMALIZED HEAT FLUX AT TOP OF STACK = .3000

XXXXXXXXXXXX INITIAL PLENUM GASES (MOLES) XXXXXXXXXXXX

AIR = 0.

NITROGEN = 0.

ARGON = 0.

FISSION GAS = 0.

HELIUM = .57931E-02

ROD AVE BURNUP AT END OF LIFE (AMWDI) = 22.7
 ROD TIME AVE HEAT FLUX (QBAR) = 308768.

TABLE B-III (continued)

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****          X
X          STEADY-STATE FUEL ROD ANALYSIS CODE          X
X          MOD 001 VERS C01          MAT PRO MODULE MOD10A          X
X          RUN DATE = 07/19/78          OPTIONS 71000000 PAGE 7 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

```

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXX AXIAL NODE 4 XXXXXXXXXXXX POWER LEVEL 1 XXXXXXXXXXXX GAS ITERATION 2 XXXXXXXXXXXXXXXXXXXXXXX

```

```

START STEP AT .00 HOURS 1. SECONDS END STEP AT .01 HOURS 36. SECONDS
POWER DURING STEP 6.883 KW/FT 22581.7 W/M HEAT FLUX DURING STEP 212579.99 BTU/HR-FT2 .12063E+07 W/M**2

BURNUP AT START OF STEP .00 MWD/MTU .6548E-04 MWS/KG BURNUP DURING STEP .02 MWD/MTU .1315E+01 MWS/KG
COGLANT CONDITIONS *****
INLET TEMP. 642.00 (F) BULK TEMP. 652.75 (F) AVERAGE BULK TEMP. 652.75 (F) COOLANT DENSITY 37.73 (LB/FT3)
612.04 (K) 618.01 (K) 618.01 (K) 618.01 (K) 604.4 (KG/M**3)

FILM, CRUD, AND OXIDE CONDITIONS *****
DITUIS-BOELTER FILM COEF. 3267.33 (BTU/HR-FT2-F) FORCED CONVECTION FILM COEF. 2115.38 (BTU/HR-FT2-F)
.18540E+05 (W/M**2-K) .12004E+05 (W/M**2-K)
DITUIS-BOELTER TEMP. DROP 65.06 (F) 36.15 (K) FORCED CONVECTION TEMP. DROP 100.49 (F) 55.83 (K)
JENS-LOTTES TEMP. DROP 3.35 (F) 1.86 (K) SATURATION TEMP. 652.75 (F) 618.01 (K)
CRUD THICKNESS 1.0000 (MILS) .2540E-04 (M) CRUD TEMPERATURE DROP 35.43 (F) 19.68 (K)
ZR-OXIDE... SURFACE TEMP. 691.53 (F) 639.55 (K), THICKNESS .0000018 (IN) .4569E-07 (M), WEIGHT GAIN .67 (MGMS/DM2)
HYDROGEN 10.22 (PPM) TEMP. DROP .027 (F) .01518 (K)

CLADDING CONDITIONS *****
INSIDE DIAM. 0.00000 (IN) 0.0000000 (M) VOLUME .4429 (CU.IN.) .7258E-05 (CU.M.)
O.D. TEMP. 691.56 (F) 639.57 (K) AVG. TEMP. 714.40 (F) 652.26 (K) I.D. TEMP. 737.24 (F) 664.95 (K)
HUOP: STRESS -13231. (PSIA) AXIAL: STRESS -7410. (PSIA) RADIAL: STRESS 0. (PSIA)
-.13231E+05 (N/M**2) -.74096E+04 (N/M**2) 0. (N/M**2)
STRAIN .0013 (PCT) STRAIN .0013 (PCT) STRAIN 0.0028 (PCT)
BOS EFFECTIVE CREEP STRAIN 0. AXIAL NODE LENGTH 4.0000 (IN) .101600 (M) AXIAL GAP 0.0000 (IN) 0.000000 (M)
GAP CONDITIONS *****
DIAMETRAL GAP 0.0000 (MIL) CONDUCTANCE, 6277. (BTU/HR-FT2-F) AVG. TEMP. 757.35 (F) TEMP. DROP 40.22 (F) VOLUME 0.0000IN**3
0. (M) 35618. (W/M**2-K) 676.12 (K) 22.34 (K) 0. M**3
CONTACT PRESSURE 0. (PSIA) 0. (N/M**2) GAS CONDUCTIVITY .15130 (BTU/HR-FT-F) .26170 (W/M-K)
FUEL CONDITIONS *****
OUTSIDE DIAM. .36879 (IN) .009367 (M) SURFACE TEMP. 777.46 (F) 687.29 (K)
STORED ENERGY AT TAVE = 1334.07 (F) 996.52 (K) IS 87.53 (BTU/LB) 48.63 (CAL/GM)

```

RING RADIUS (IN)	TEMPERATURE (F)	TEMPERATURE (K)	EXPANSION (PCT)	SWELLING (PCT)	POROSITY (PCT)	DENSIFICATION (PCT)	GRAIN SIZE (MICRONS)
0.000000	2095.05	1419.29	0.000000	0.000000	0.000000	0.000000	5.00
0.058478	1905.96	1314.24	1.051106	0.000000	2.1667	0.000000	5.00
0.082669	1729.12	1216.00	1.920369	0.000000	2.1667	0.000000	5.00
0.101210	1565.90	1125.31	2.804282	0.000000	2.1667	0.000000	5.00
0.116825	1416.04	1042.06	3.701963	0.000000	2.1667	0.000000	5.00
0.130970	1279.20	966.04	4.612062	0.000000	2.1667	0.000000	5.00
0.142986	1155.00	897.04	5.533332	0.000000	2.1667	0.000000	5.00
0.154397	1043.08	834.86	6.464660	0.000000	2.1667	0.000000	5.00
0.165011	943.08	779.31	7.405064	0.000000	2.1667	0.000000	5.00
0.174975	854.65	730.18	8.353683	0.000000	2.1667	0.000000	5.00
0.184395	777.46	667.29	9.309763	0.000000	2.1667	0.000000	5.00

```

VOLUMES (CU.IN.) ... FUEL .4255 DISH .0040 CRACK .0134 OPEN POROSITY .0007
(CU.M.) ... .6973E-05 .6598E-07 .2188E-06 .1149E-07
NITROGEN CONCENTRATION .37197E-04 (MOLES) PERMANENT FUEL RADIAL DISPLACEMENT = 0.00000 (IN) 0.000000 (M)
RADIUS OF COLUMNAR GRAIN ZONE = 0.000000 (IN) 0.000000 (M) THRESHOLD TEMP. = 4037.0 (F) 2498.1 (K)
COLUMNAR GRAIN ZONE DENSITY = .935
TO OBTAIN TEMPERATURE AT ANY RADIUS-R, USE THE FOLLOWING COEFFICIENTS IN THE EQUATION T = A*R**3 + B*R**2 + C*R + D (F)
A = .22154E+06 B = -.86322E+05 C = .12390E+04 D = .20950E+04

```

TABLE B-III (continued)

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XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****          X
X          STEADY-STATE FUEL ROD ANALYSIS CODE          X
X          MOD 001 VERS 001          MAT PRO MODULE MOD10A          X
X          RUN DATE = 07/19/78          OPTIONS 71000000 PAGE 23 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
POWER LEVEL 2          XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX          AXIAL SHAPE NUMBER 1          XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

BURNUP TO END OF STEP 45. (MWD/MTU)          BURNUP DURING STEP 23. (MWD/MTU)          ROD AVERAGE HEAT FLUX 308927. (BTU/HR-FT2)
          39177E+04. (MWS/KG)          .19579E+04 (MWS/KG)          .17530E+07 (W/M)
RAD. + AX. CLAD AVG TEMP 741.41 (F) 667.27 (K) CLAD VOL .2542 CU.IN. .416E-05 CU.M. POROS VOL .0215 CU.IN. .3507E-06 CU.M.
RAD. + AX. GAP AVG TEMP 802.42 (F) 701.16 (K) GAP VOL .0004 CU.IN. .570E-08 CU.M. DISH VOL .0362 CU.IN. .5933E-06 CU.M.
RAD. + AX. FUEL AVG TEMP 1647.52 (F) 1170.66 (K) FUEL VOL 3.8657 CU.IN. .633E-04 CU.M. ROUGH VOL .0072 CU.IN. .1172E-06 CU.M.
AVG. FUEL SURFACE TEMP. 830.54 (F) 716.78 (K) CRACK VOL .0944 CU.IN. .154E-05 CU.M.
STACK HEIGHT 36.275 (IN) .92140 (M) PCT. INCREASE .7646619 ROD LENGTH 36.049 (IN) .91566 (M) PCT. INCREASE .1372576
PERMANENT FUEL STACK DISPLACEMENT = .0000 (IN) .0000 (M)
NITROGEN CONCENTRATION .32528E-03 (MOLES) HELIUM PRODUCTION .42111E-07 (MOLES) FISSION GAS PRODUCTION .42181E-05 (MOLES)

GASES RELEASED -- WATER NITROGEN HELIUM FISSION
          (MOLES) 0. .96141E-05 .89913E-09 .33142E-06
          (FRACTION) 0. .28708E-01 .21352E-01 .78570E-01

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
MOLE FRACTIONS OF GAS
AIR 0. HYDROGEN 0. WATER 0.
ARGON C. HELIUM .99829E+00 NITROGEN C.
KRYPTON .85666E-05 XENON .48544E-04 FISSION .57111E-04

CUMULATIVE QUANTITY OF GAS IN GAP .58031E-02 (MOLES)

PLENUM LENGTH 1.7900 (IN) .C4547 (M) PLENUM VOLUME .1761 CU.IN. .2886E-05 CU.M.
GRASHOFF NUMBER OF PLENUM .15121E+05 HEAT FLOW FROM TOP OF STACK 5.7644 (BTU/HR) .1688E+01 (W)
HEAT GENERATED IN SPRING 4.3303 (BTU/HR) .1268E+01 (W) PLENUM TEMPERATURE 704.80 (F) 646.93 (K)
COOLANT SATURATION TEMP. 652.75 (F) 618.01 (K) TOTAL HOT VOID VOLUME .3358 CU.IN. .5503E-05 CU.M.
INTERNAL PRESSURE = (GASOL) (40.8) / ((HPV / PLEN +460.) + SUMDH + SUMGP + SUMCK + SUMRG + SUMPOR)
          .0058031 .1761 705. .0000178 .0000003 .0000484 .0000058 .0000092 = 1017.14

CLADDING TEMP. IN DEG F , STRESSES & INTERFACIAL PRESSURE IN PSIA , & RADIAL GAP IN INCHES
XXXXXXXXXXXXXXXXXXXX CLAD TEMP., AXIAL & HOOP STRESS, STRAIN, & PLASTIC STRAIN, GAP WIDTH, AND INTERFACIAL PRESSURE XXXXXXXXXXXXXXXXXXXX
J I CLAD SIG HOOP SIG AXIAL EPS HOOP EPS AXIAL EPP HOOP EPP AXIAL GAP PINT
1 .71787E+03 -.11941E+05 -.68033E+04 .13902E-02 .13196E-02 0. 0. .29915E-02 0.
2 .74581E+03 -.11941E+05 -.68033E+04 .14817E-02 .13820E-02 0. 0. .27273E-02 0.
3 .76660E+03 -.11941E+05 -.68033E+04 .15501E-02 .14294E-02 0. 0. .23868E-02 0.
4 .77289E+03 -.11941E+05 -.68033E+04 .15709E-02 .14439E-02 0. 0. .23332E-02 0.
5 .76901E+03 -.11941E+05 -.68033E+04 .15581E-02 .14349E-02 0. 0. .23668E-02 0.
6 .75587E+03 -.11941E+05 -.68033E+04 .15147E-02 .14046E-02 0. 0. .26073E-02 0.
7 .73600E+03 -.11941E+05 -.68033E+04 .14496E-02 .13601E-02 0. 0. .28306E-02 0.
8 .71427E+03 -.11941E+05 -.68033E+04 .13784E-02 .13115E-02 0. 0. .30266E-02 0.
9 .69437E+03 -.11941E+05 -.68033E+04 .13131E-02 .12669E-02 0. 0. .31662E-02 0.

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TABLE B-III (continued)

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XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****          X
X          STEADY-STATE FUEL ROD ANALYSIS CODE          X
X          MOD 001 VERS OC1          MAT PRO MODULE MOD10A          X
X          RUN DATE = 07/19/78          OPTIONS 71000000 PAGE 24 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

```

CLADDING TEMP. IN DEG F , STRESSES & INTERFACIAL PRESSURE IN PSIA , & RADIAL GAP IN INCHES									
XXXXXXXXXXXXXXXXXXXX CLAD TEMP., AXIAL & HOOP STRESS, STRAIN, & PLASTIC STRAIN, GAP WIDTH, AND INTERFACIAL PRESSURE XXXXXXXXXXXXXXXXXXXX									
J	T CLAD	SIG HOOP	SIG AXIAL	EPS HOOP	EPS AXIAL	EPP HOOP	EPP AXIAL	GAP	PINT
1	.71787E+03	-.11941E+05	-.68033E+04	.47098E-03	.12299E-02	-.91927E-03	-.89653E-04	.27964E-02	0.
2	.74581E+03	-.11941E+05	-.68033E+04	.16855E-03	.12539E-02	-.13132E-02	-.12807E-03	.24486E-02	0.
3	.76666E+03	-.11941E+05	-.68033E+04	-.14415E-03	.12641E-02	-.18943E-02	-.16524E-03	.20273E-02	0.
4	.77289E+03	-.11941E+05	-.68033E+04	-.25610E-03	.12657E-02	-.18270E-02	-.17818E-03	.19456E-02	0.
5	.76901E+03	-.11941E+05	-.68033E+04	-.18607E-03	.12648E-02	-.17442E-02	-.17010E-03	.19968E-02	0.
6	.75587E+03	-.11941E+05	-.68033E+04	.27500E-04	.12596E-02	-.14872E-02	-.14504E-03	.22918E-02	0.
7	.73600E+03	-.11941E+05	-.68033E+04	.28878E-03	.12469E-02	-.11609E-02	-.11322E-03	.25843E-02	0.
8	.71427E+03	-.11941E+05	-.68033E+04	.50153E-03	.12260E-02	-.87691E-03	-.85522E-04	.28406E-02	0.
9	.69437E+03	-.11941E+05	-.68033E+04	.64104E-03	.12014E-02	-.67205E-03	-.65543E-04	.30236E-02	0.
CHANGE IN CLADDING LENGTH =				.44850E-01 (IN)	LENGTH CHANGE RELATIVE TO THE FUEL CHANGE =				-.23044E+00 (IN)
				.11392E-02 (IN)					-.58530E-02 (IN)

TABLE B-III (continued)

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****                                X
X          STEADY-STATE FUEL ROD ANALYSIS CODE                X
X          MOD 001 VERS 001  HAT PRO MODULE MOD10A           X
X          RUN DATE = 07/19/78  OPTIONS 71000000 PAGE 25 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          PBF SINGLE ROD PCM-20
X          ***** PEAK POWER INCREMENT OUTPUT *****

```

TIME	BURNUP	POWER	CLAD TEMP (F)			GAP	GAP	FUEL TEMP (F)			CONT.	CLAD STRESS		STRAIN	FUEL OD	GAP	FGAS	ZR02	H2	
HOURS	MWD/TU	KW/FT	OD.	AVG	ID.	MILS	(F)	OD.	AVG	CEN	PSI	HOOP	AXIAL	PCT	INCH	CONDUCT	PSI	HIL	PPM	
1	0.	0.	6.86	692.	714.	737.	0.00	757.	777.	1348.	2095.	0.	-13231.	-7410.	.0013	.36879	6277.	841.	.00	10.2
2	1C.	31.	13.77	728.	773.	818.	0.00	854.	890.	2069.	3639.	0.	-11941.	-6803.	.0016	.37167	6623.	1017.	.02	12.8

TABLE B-III (continued)

```

XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX
X          **** FRAPCON ****                      X
X    STEADY-STATE FUEL ROD ANALYSIS CODE          X
X    MOD 001 VERS 001  MAT PRO MODULE MOD10A      X
X    RUN DATE = 07/19/78  OPTIONS 71000000 PAGE 26 X
XXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXXX

```

END OF LIFE STRAIN RANGE (PCT) = .0010

FISSION GAS CUMULATIVE FRACTION RELEASE = .078570

ZRO2 WEIGHT GAIN , (MG/DM2) = 7.55

APPENDIX C

CONFIGURATION CONTROL PROCEDURE

APPENDIX C

CONFIGURATION CONTROL PROCEDURE

A Configuration Control Procedure (CCP) has been defined to maintain a traceability of results from developing computer codes. During the development process of a computer code, requirements exist for using the code for generating both checkout results and production results, depending on the stage of development.

The Configuration Control Procedure consists of a method by which changes can be made to the code and tracability of results maintained. Any time a modification to the code is made, the following data are taken:

- (1) Version of code to which modification was made
- (2) Reason for modification
- (3) Results affected by modification
- (4) Date of modification
- (5) Person responsible for modification
- (6) The change cards used to modify the original version of the code.

A tape update routine is used to modify the code. This routine requires only those computer cards defining new statements or deleting old statements. These "change cards" are kept on file so any version of the code can be reproduced if necessary.

A new identification number is assigned to the modified version of the code and this new number is programmed into the code where it will be listed at the top of each page of output and on each plot produced by the code.

APPENDIX D

FUEL TEMPERATURE SUBCODE DETAILS

APPENDIX D

FUEL TEMPERATURE SUBCODE DETAILS

This appendix contains the details of the solution within DDLF and the description and details of the flux depression subroutine used by DDLF.

Solution Steps and Nomenclature

From the formulation described in the pellet heat conduction section, the programming steps are fairly obvious. They are:

1. Determine the collocation points.
2. Translate linear heat rating, relative heat generation, and geometry to heat generation rates Q_i at the collocation points.
3. Get initial collocation point temperatures.
4. Calculate the A_{ij} .
5. Calculate the I_i .
6. Do an iteration to temperature convergence. Within each iteration is a definition of the B_i and a call to SIMO to solve the equations for d_j . These are used to calculate collocation point temperatures for the next iteration.

These steps will be considered in turn. Table D-I provides a translation between the nomenclature used in this description and the major FORTRAN variables used in the subroutine.

1. Definition of the collocation points (first loop)

The collocation points are the four roots of the shifted fourth order Legendre Polynomial. The general roots $RT(I)$ are given in a data statement and mapped into the specific range y_I to 1.0.

2. Definition of Q_i (second loop)

The volumetric heat generation rate Q_i is described as

$N(Z + Yr^2 + Wr^4)$. N is found from the relation $N = P/2\pi \int_r^{r_{FS}} Q(r) r dr$, where P is the linear heat generation rate.

Because of its occurrence later the actual normalization

constant used is $RN = r_{FS}^2 N$.

3. Initial Temperatures at Collocation Points (second loop)

The initial temperatures for the first gap conductance iteration are found from the parabolic solution for a fixed conductivity and heat generation: $T = \frac{P}{4\pi K} (1-y^2) + T_{FS}$.

A constant conductivity of 3.0 W/m°C is assigned. After the first gap conductance iteration the initial temperatures are taken to be the converged temperatures from the previous iteration (BTOLD).

4. Definition of A_{ij} (fourth and fifth loop)

Since powers of the collocation points are used throughout a matrix of points and their powers is formed (third loop).

The fourth and fifth loops define the A_{ij} at the boundaries and the collocation points respectively. Because the call to SIMQ destroys the A_{ij} , two matrices are defined; AS (I,J) is set equal to A (I,J) at each internal iteration, and A (I,J) goes into the call to SIMQ.

5. Define the I_i (sixth loop)

The final definition of I_i is done in the iteration loop but the temperature independent portion (labelled C_i in equation (29) of the main text is calculated in the sixth loop just ahead of the iteration loop.

6. The Iteration Loop (seventh loop)

This loop is structured such that an external subroutine provides the conductivity K_{n-1} and its derivative $\frac{dK_{n-1}}{dT_i}$.

The B_i are defined just ahead of the call to SIMQ. Subroutine SIMQ returns $B(I)$, $I=1,6$ which are the d_j in our nomenclature. These are used to calculate boundary and collocation point temperatures. The convergence criterion is that the sum of absolute deviations and between present and current values of these six temperatures not exceed 1.0 degrees C.

TABLE D-I DDLF NOMENCLATURE

<u>Symbol</u>	<u>Equivalent FORTRAN Variable</u>	<u>Meaning</u>
A_{ij}	AS(I,J) and A(I,J)	Coefficients for d_j in the 6 simultaneous equations.
B_i	B(I) going into SIMQ	Right-hand sides of collocation equations.
C_i	TTRM(I)	Temperature-independent portion of I_i
d_j	B(I) out of SIMQ	Expansion coefficients
I_i	TFAC	See text
K	RK	Conductivity
$\frac{dK}{dT}$	DKDT	Temperature derivative of conductivity
Nr_{FS}^2	RN	Normalization constant for Q
P	POW	Linear heat generation rate
Qr_{FS}^2	Q(I)	Volumetric heat generation rate (times r_{FS}^2)
r_I	RII	Fuel inner radius

TABLE D-I DDLF NOMENCLATURE (cont'd)

<u>Symbol</u>	<u>Equivalent FORTRAN Variable</u>	<u>Meaning</u>
r_{FS}	RD	Fuel outer radius
T	T(I)	Collocation temperatures
y_i^j	RC(I,J)	Normalized boundaries and collocation points. (jth power thereof)

DESCRIPTION OF SUBROUTINE FLUXD

(Authorship attributed to G. R. Horn)

This subroutine produces three constants (A,B,C) which are used to express the volumetric heat generation rate, Q, according to the expression

$$Q = A + Br^2 + Cr^4 \quad (1)$$

The three terms in the above expression are calculated as being the first three terms of the power series expansion of $I_0(Kr)$, where I_0 is the modified Bessel function of zero order and K is the inverse diffusion length for neutrons in the fuel. The basis for this expression for Q is given in reference (D-1) (I_0 is a transport theory solution for thermal neutron flux in a cylindrical fuel rod). The calculation of K is taken from an approximate expression in reference (D-2):

$$K^2 = 3 \Sigma_a \Sigma_T \left[1 - \frac{4}{5} \frac{\Sigma_a}{\Sigma_T} \right] \quad (2)$$

where Σ_a and Σ_T are the absorption and total macroscopic cross sections for the fuel, respectively.

Σ_a and Σ_T are given by

$$\Sigma_a = N_{238} \sigma_{238}^a + N_{235} \sigma_{235}^a + N_o \sigma_o^a$$

$$\Sigma_T = N_{238} \sigma_{238}^T + N_{235} \sigma_{235}^T + N_o \sigma_o^T$$

and N_{238} , N_{235} and N_o are atom concentrations (atoms/cm³) of ^{238}U , ^{235}U , and oxygen, respectively and σ_{238} , σ_{235} and σ_o

Fig. D-1 Flux depression from FLUXD

are the respective microcrosssections (barns). Appendix 2 of Reference (D-3) gives spectrum-averaged cross sections for UO_2 constituents at $40^\circ C$, and these are used in FLUXD.

One would think a temperature correction should be applied to these cross sections, but the method is so approximate that this hardly seems worthwhile. To emphasize this point, we have plotted in Figure D-1 the results of a BRT-I^[D-4] transport code calculation for heat generation in a 0.534 cm radius, 95% TD, 3% enriched UO_2 pellet in $240^\circ C$ D_2O . Also shown is the FLUXD calculation for this case. Both curves are normalized to a linear heat generation of 300 W/cm, such that

$$2\pi \int_0^R Q(r) r dr = 300 \text{ W/cm in each case.}$$

Note that the BRT-1 result is steeper than that for FLUXD near the edge of the fuel, indicating a larger K-value should have been used. But the temperature correction to $240^\circ C$ would have made the K-value even smaller (by a factor of 1.3). So the expression used for K is seriously in error. Our solution is to force the K values to yield a correct expression for the centerline temperature, since this will bring the entire temperature profile into line. The centerline temperature is found from the expression

$$\int_{T_{FS}}^{T_0} \frac{1}{C} dt = \left[\frac{2 \int_0^R \frac{dr}{r} \int_0^r r' Q(r') dr'}{\int_0^R r Q(r) dr} \right] \frac{P}{4\pi} = Pf/4\pi \quad (3)$$

where T_0, T_{FS} = fuel centerline and surface temperatures

P = linear heat generation

C = fuel conductivity

The idea is to find what K value will yield the same "f" in equation (3) as a curve-fit to the BRT-I results.

For a 3%, 0.534 cm radius pellet, a curve fit to the BRT results yielded

$$A = 0.4485$$

$$B = 0.12352$$

$$C = 0.19389$$

Inserting this into equation (3), the target "f" is 0.974. The expression for "f" from reference (D-2) is

$$\frac{2(I_0 (K r_{FS}) - 1)}{K r_{FS} I_1 (K r_{FS})}.$$

By trial, the proper K value is 0.9 cm^{-1} . The FLUXD calculated value, on the other hand, is 0.466, as shown below:

$$N_5 = 10.41 \times 0.03 \times 0.002250 = 7.055 - 04$$

$$N_8 = 10.41 \times 0.97 \times 0.0022308 = 2.525 - 02$$

$$N_0 = 2 (N_5 + N_8) = 4.646 - 02$$

$$\Sigma_a = [7.055 - 4 (578) + 2.252 - 2 (2.33)] = 0.46025$$

$$\Sigma_T = [7.055 - 4 (586.5) + 2.252 - 2 (10.8) + 4.646 - 2 (4.2)]$$

$$= 0.8394$$

$$K^2 = (0.4602)(0.8394) \left[1.0 - 0.8 \left(\frac{0.4602}{0.8394} \right) \right] = 0.216$$

$$K = 0.466$$

The ratio between the two is 1.9 and this ratio holds for other low enrichments. Accordingly, the K values calculated by FLUXD are multiplied by the factor 1.9. It is recommended that FLUXD not be used for calculations when the enrichment exceeds 4%.

Table D-II provides translation between the nomenclature in this report and the major FORTRAN variables in the subroutine.

TABLE D-II FLUXD NOMENCLATURE

Symbol	FORTRAN Variable	Meaning
K	CAPPA	Inverse diffusion length
N_{235} , N_{238} , N_O	EN5, EN8, ENO	Atom concentrations of U-235, U-238, and oxygen
σ_{235}^a , σ_{238}^a , σ_O^a	SIGA5, SIGA8, SIGAO	Absorption micro cross sections
σ_{235}^T , σ_{238}^T , σ_O^T	SIGT5, SIGT8, SIGTO	Total micro cross sections
Σ_a , Σ_T	SIGA, SIGT	Fuel macroscopic absorption and total cross sections
ρ	RHO	Fuel density (grams/cm ³)

APPENDIX D REFERENCES

- D-1. S. A. Kushneriuk, Neutron Capture by Long Cylindrical Bodies Surrounded by Predominantly Scattering Media. AECL Report CRT-712, 1957.
- D-2. C. H. Westcott, Effective Cross Section Values for Well-Moderated Thermal Reactor Spectra. CRRP-787, 1958.
- D-3. J. A. L. Robertson, Integral K_{eff} in Fuel Irradiations. CRFD-835, 1959.
- D-4. L. J. Page and D. R. Skeen, THERMOS/Battelle: The Battelle Version of the THERMOS Code. BNL-516, 1967.

APPENDIX E

MATERIALS PROPERTIES CORRELATIONS EMPLOYED BY FRAPCON

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MATERIALS PROPERTIES CORRELATIONS EMPLOYED BY FRAPCON

A materials properties subcode is used to provide the computational subcodes of FRAPCON with gas, fuel, and cladding materials properties. Those properties used by FRAPCON from the two references below are presented in Table E-I and are from the following reference.

1. G. A. Reymann et al, MATPRO-VERSION 10 A Handbook of Materials properties for Use in the Analysis of Light Water Reactor Fuel Rod Behavior, TREE-NUREG-1180 (February 1978).
2. P. E. MacDonald et al, MATPRO - Version 09 - A Handbook of Materials Properties for Use in the Analysis of Light Water Reactor Fuel Rod Behavior, TREE-NUREG-1005 (December 1976).

TABLE E-I

PROPERTIES INCLUDED IN MATPRO USED BY FRAPCON

<u>Property</u>	<u>Reference</u>	<u>Subcode</u>
<u>Fuel Material Properties</u>		
1. Specific Heat Capacity	1	FCP
2. Thermal Conductivity	1	FTHCON
3. Emissivity	1	FEMISS
4. Thermal Expansion	1	FTHEXP
5. Fuel Swelling	1	FSWELL
6. Densification	2	FUDENS
7. Restructuring	1	FRESTR
<u>Cladding Material Properties</u>		
1. Axial Growth	1	CAGROW
2. Thermal Conductivity	1	CTHCON
3. Zr-Oxide Thermal Conductivity	1	ZOTCON
4. Axial Thermal Expansion	1	CATHEX
5. Diametral Thermal Expansion	1	CDTHEX
6. Elastic Modulus	1	CELMOD
7. Strain versus Stress	1	CSTRAN
8. Stress versus Strain	1	CSTRES
9. Poisson's Ratio	1	CPOIR
10. Meyer Hardness	1	CMHARD
<u>Gas and Fuel Rod Material Properties</u>		
1. Gas Thermal Conductivity	1	GTHCON
2. Gas Viscosity	1	GVISCO
3. Physical Properties	1	PHYPRO