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PERFORMANCE PREDICTIONS AND A DESCRIPTION OF THE OXIDE FUEL ELEMENT MODELING CODE BEHAVE-I

F. E. GELHAUS
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PREPARED FOR
THE UNITED STATES ATOMIC ENERGY COMMISSION
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OXIDE FUEL ELEMENT MODELING CODE BEHAVE-I**

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PROGRAM INFORMATION

(Following the ANS Code of Good Practices)

Name or Designation of Program	BEHAVE-I
Computer for which Program is Designed and Others upon which it is Operable	The program was written for and has been run on the General Electric 600-Honeywell 6000 computers.
Nature of Physical Problem Solved	The axial strain profile is calculated for an oxide fuel, fast flux reactor fuel element. The individual contributions of first startup, power increases, fission gas pressure, thermal and irradiation creep, fuel-cladding mechanical interaction, and neutron-induced metal swelling are included in predicting cladding diametral distortion. The program does not include a rigorous accounting of fuel and cladding stresses. The effects of fuel cracks are included implicitly, and detailed crack patterns are not calculated.
Method of Solution	<p>The reactor power is assumed constant for the user-specified period. Program logic solves for the diametral change during this time interval by the following series-sequencing of models:</p> <p>First Startup (FSTART) Fuel Restructuring (RESTRU) Cladding Thickness (CTHICK) Metal Swelling (CSWEL) Gas Pressure (GASP) Cladding Creep (CCREEP) Fuel-Cladding Mechanical Interaction (FCMINT) Power Increase (INCRES)</p> <p>Subroutines FSTART and RESTRU are utilized only once, at first startup.</p>
Restrictions on the Complexity of the Problem	BEHAVE-I requires 27K GE-635 core for execution. There is a limit of 40 power history events per run. The user can specify up to 29 axial nodes.
Typical Running Time	For the modeling of pin F2G (~100,000 MWd/Te), typical total computer time was 0.01 hr.
Unusual Features of the Program	See discussion under "Nature of Physical Problem Solved."
Related and Auxiliary Programs	On the input data, BEHAVE-I requires the axial linear power profile and thermal hydraulic results from other codes. At present, the plotting routine is not an integral part of the code.
Status	BEHAVE-I is in production status and all options have been tested.
References	This document.
Machine Requirements	The input is read from cards and the output requires both the printer and card punch. The program requires 27K of high speed memory.
Programming Language(s) Used	The entirety of BEHAVE-I is written in FORTRAN IV.

GEAP-13741

**Operating System or Monitor under
which Program is Executed**

GECOS III, SAL-3

**Other Programming or Operating
Information or Restrictions**

None

Material Available

**This document, program listing, source deck, and sample problem
(F2H).**

ABSTRACT

The application of the BEHAVE-I modeling code to predicting fuel element diametral changes is detailed for General Electric fuel pins F2G, F2H, and F2V, irradiated in the EBR-II under Task F of the PA-10 contract. Highlighted by these analyses are the speculative mechanisms of in-situ change in the rate of neutron-induced cladding swelling apparently due to precipitate formation, and the presence of a high-pressure central void volume ("gas bottle" effect). A general parameter/sensitivity study is included to show the variation to be expected in the output (prediction) as the result of uncertainties in the input data. Code structure and logic and input-output formats are described to provide a user's manual for execution and modification of the program.

1. INTRODUCTION

THE BEHAVE-I APPROACH TO MODELING

The BEHAVE-I Fuel Performance computer code simulates the integral behavior of a mixed-oxide fuel element throughout its operating history, including power increases and burnout. BEHAVE-I calculates changes in the axial diametral profile of a fuel pin during three modes of operation: (1) first startup with fresh fuel, (2) steady-state operation, and (3) power increase following a period of steady power operation.

There are three general classes of approach to modeling: (1) first principles, (2) semi-empirical, and (3) empirical. In the approach from first principles, the mechanisms are presumed in advance, and the model developed with depth in detail. If the choice of mechanisms proves correct, the completeness of the resulting physical description permits extensive analysis of experimental results, parametric studies and optimization of fuel design. However, at the current level of understanding of the mechanisms contributing to fuel element distortion and of their interactions, the risk of obtaining misleading results from an integral performance model derived from first principles appears to be quite high.

In the semi-empirical approach, the mechanisms are again presumed, but the formulations are rearranged so that only quantities which can be measured in experiments are included as variables. All unmeasurable quantities are grouped into constants. The experimental observations are then compared with the analytical formulations and constants for the grouped unmeasurables derived to track the experimental results. The appearance of an unreasonable value of a constant at this point indicates that either an additional mechanism is involved, or that the interactions between mechanisms are not correctly modeled. The value of this approach is that analytical development of the mechanisms can proceed in parallel with the development of the understanding of their interactions.

The empirical approach makes no presumption of mechanisms. This is best typified by the curve fitting approach to data reduction. Its usefulness lies in predictions within the range of the experimental data, but only to the extent that sufficient data are available within that range to establish reasonable confidence levels.

The semi-empirical method with derivative models is the approach used in BEHAVE-I. In this context, a derivative model is defined as a model which is derived from a detailed investigation of the mechanisms, and which accounts for the significant effects of the mechanism but which suppresses unnecessary complexity. The power history followed by BEHAVE-I is also derivative in nature. The actual history is simulated by a series of well-defined regimes of operation. Within each regime, rigorous attention is focused only on those mechanisms which substantially affect performance. The net result of using derivative models of the mechanisms and of reducing the power history into discrete regimes is a flexible computer code with a short running time.

BEHAVE-I is ideally suited for: (a) extensive simulation and predictive studies of fuel element irradiation experiments; (b) parametric design studies of the effects of controllable fabrication and operating variables; and (c) use as part of larger, more complex studies of overall core performance. The code's logic is not intended to be final or complete. It is the present transient nature of performance modeling that, in large part, motivated the flexible, modular structuring employed in the program.

2. BEHAVE-I MODELING RESULTS

2.1 IN-SITU ENHANCEMENT OF NEUTRON-INDUCED METAL SWELLING

Upon initiating the modeling of the Task F; EBR-II irradiated fuel pins F2H, F2G and F2V, it became apparent that none of the included data correlation equations for metal swelling (CSWEL subroutine) accurately predicted the axial volumetric change profile as determined experimentally by the immersion density technique. These fuel pin data were used in conjunction with the immersion density and transmission electron microscopy examination results on the cladding and materials specimens of Types-316 and -347 stainless steel as a basis for empirically modeling the in-situ enhancement of metal swelling. The salient features of these latter data are:¹

- (1) The void number density is higher in specimens subjected to higher stress and which also suffered more plastic strain
- (2) Associated with the high void density is a high level of intragranular precipitation.
- (3) The incoherent precipitates can act as void nucleation sites.
- (4) The void number density increase saturates.

From their modeling of accelerator-irradiated materials, Bullough and Perrin² have concluded that, in the case of Type-316 stainless steel, the embryonic void density and the dislocation density apparently change with exposure. The former phenomenon is modeled in BEHAVE-I through the inclusion of a creep strain dependent term which effectively increases the nucleation site density (i.e., the embryonic void density) and thus enhances the metal swelling above that predicted by a data correlation equation (e.g., the PNL Theta Equation in CSWEL). This enhancement term has the following form:

$$1 + 100 (1 - e^{-A(\Delta D/D)_c}) \quad (2-1)$$

where

$$A = \text{a material-dependent constant, and}$$

$$\left(\frac{\Delta D}{D}\right)_c = \text{creep strain.}$$

Program logic determines the highest strain rate creep phenomenon in a given time-step and applies the corresponding $\Delta D/D$ to the above expression to enhance the metal swelling. Depending upon the strain rate, the creep strain used is either the sum of thermal and irradiation creep or is equal to the fuel-cladding mechanical interaction creep. No attempt is made to include a temperature dependency in this term since the data correlations available as options in subroutine CSWEL each contain their own temperature variation and since these correlations were derived using data from both stressed and unstressed specimens.

Equation 2-1 is used to modify the user-specified metal swelling equation (chosen through option ICSOPT) if the input parameter ISIMS (Table 4-3) is greater than zero. The results of applying this enhancement term can be seen in Figures 2-1, 2-2 and 2-3.

2.2 CENTER VOID GAS RETENTION

The possibility of central void containment of gaseous fission products has been discussed in conjunction with the post-irradiation analysis of EBR-II irradiated ANL Element 012.³ Somewhat similar fuel behavior was noted for Task F fuel pin F2V, where the fuel column length change was 2.5% as compared to a 1.8% change in the cladding length over the fueled length of the pin. Because of the large available void (smeared density = 86.7%), no fuel-cladding mechanical interaction is predicted for F2V. However, upon comparison with a data correlation equation, the immersion density measurements show that enhancement of metal swelling occurred; this is assumed to indicate that a different cladding straining mechanism was operative in this pin.

Quantitative evidence exists in support of the phenomenon of "gas bottling" of fission products within the center fuel void. Notley and MacEwan's direct pressure measurement⁴ showed that the release from UO_2 can occur in bursts, with discrete pressure increases occurring during reactor shutdown transients. Similar results were obtained by Burley and Freshley from vibrationally compacted mixed-oxide fuel pins.⁵ They noted that the stepwise pressure increases were directly related to the length of time of steady-state operation (up to 200 hr) prior to the reactor shutdown transient.

Figure 2-1. BEHAVE-1 Modeling Results for Fuel Pin F2H

Figure 2-2. BEHAVE-1 Modeling Results for Fuel Pin F2V

Figure 2-3. BEHAVE-1 Modeling Results for Fuel Pin F2G

In addition to considering the pressure resulting from released noble gases, Paulson and Springhorn⁶ have pointed out that the partial pressure of cesium at high temperatures can be 70% of the partial pressures of xenon and krypton. Most of the precursors of cesium are noble gases that migrate up the temperature gradient: this effectively brings cesium to the hot region of the fuel. With a critical temperature of approximately 2050°K, cesium behaves as an ideal gas at typical operating central void temperatures. The gas bottle modeling added to the GASP subroutine assumes the same release behavior for cesium as for the noble gases, an assumption supported by experimental measurements on UO₂.⁷

The central void pressure is computed from the axial-node values of center void temperature and diameter. The fractional yield of Xe + Kr is 0.26 atoms/fission and the cesium yield is 0.184 atoms/fission. The hydrostatic pressure is assumed to act at the fuel plasticity radius, a boundary defined by the isotherm $T = 1650^{\circ}\text{C}$, and is transmitted to the cladding through the cooler structural fuel. Thus, the pressure is given by

$$P_g = \frac{(n_{\text{Xe}} + n_{\text{Kr}} + n_{\text{Cs}}) R}{\sum_{j=1}^{MZ} (V/T)_j} \quad (2-2)$$

where

n_i = moles of specie i released to the center void volume during an extended steady period of operation,

R = the gas constant, and the

denominator = sum of the center void volume divided by center void temperature for each of the MZ axial nodes.

The effective cladding loop stress then is calculated from

$$\bar{\sigma} = (\sqrt{3}/2) P P_p / t_c \quad (2-3)$$

where

$$\begin{aligned}
 P &= P_g - P_{\text{coolant}} \\
 R_p &= \text{fuel plasticity radius (T = 1650°C isotherm),} \\
 t_c &= \text{effective cladding thickness at the time of the gas bottle event, and} \\
 \bar{\sigma} &= \text{effective stress used in thermal and irradiation creep equations (CCREEP).}
 \end{aligned}$$

The user determines the extent of the gas bottle effect through the input parameter GASBOT. If GASBOT = 0 in the STEINP namelist (Table 4-3), the fission gas plenum temperature and volume determine the internal pin pressure. In this case, since the plenum is in communication with the fueled-volume of the pin, cesium is assumed to condense at cooler surfaces (without forming an equilibrium reservoir), and the pressure is determined only from the released moles of noble gas. This is modeled in the code as outlined in Subsection 4.10. For GASBOT $\leq$ 1, the moles of released cesium, xenon, and krypton are accumulated in the central void volume for the totality of the specified steady-state period (DTMT), and Equations 2-2 and 2-3 are used to calculate the internal pressure and cladding stress.

Because a length of time at steady-state conditions is needed to form the sealed central void and because this volume may periodically vent and reseal, the GASBOT = 1 condition probably overpredicts the effect. To permit the user to account for this, the program logic multiplies P_g in Equation 2-2 by GASBOT and uses that product as an "effective" pressure in Equation 2-3. If, in modeling the actual reactor operating power history, the user decides that there was sufficient time for a sealed volume to form and has chosen the gas bottle option: (0 < GASBOT $\leq$ 1), in a particular steady-state period, he must put GASBOT = 0 in the first subsequent STEINP namelist to re-establish plenum control of pin pressure.

2.3 F2H MODELING

The logic discussed in Subsections 2.1 and 2.2 was added to that described in Section 4 as a result of the initial modeling of Task F fuel pins F2H, F2G, and F2V. The modeling results are shown in Figures 2-1, 2-2 and 2-3 for F2H, F2V and F2G, respectively. In each figure, the solid curve represents the post-irradiation outside diameter as measured by profilometry, and the dashed curves are determined by linear segments connecting the 10 axial-node calculated values. Some salient fabrication parameters are given in Table 2-1.

Table 2-1
SUMMARY OF FABRICATION PARAMETERS FOR FUEL PINS F2G, F2H, AND F2V

	F2H	F2G	F2V
Fuel Type	Co-Precipitated solid pellet*		
Fuel O/M	1.978	1.975	1.988
Fuel Density (%TD)	96.1	95.5	89.7
Average Fuel/Cladding Diametral Gap (mil)	2.6	3.0	3.7
Fuel Pin Smeared Density (%)	93.8	92.9	86.7
Annealed Stainless Steel Cladding Type	316	347	316

* Fuel column in F2H and in F2G was axially restrained.

Fuel pin F2H was modeled using the power-time data in Table 2-2; irradiation was terminated with the removal of subassembly XG05. The input used for this calculation is shown in Figure 3-1. The calculated peak burnup is 6.71 at.%, whereas 6.75 at.% was determined by Nd-148 chemical analysis. A 73% release of noble fission gases is calculated and 67.5% is the measured release. The strain-enhanced metal swelling was calculated based upon the PNL Theta Equation using $A = 80$. This enhancement is in agreement with TEM analyses which indicates a higher void density in the F2H cladding than in unstressed Type-316 specimens at similar fluence and temperature. The calculated metal swelling and outside diameter (o.d.) change are compared with the immersion density and profilometry data in Figure 2-1. F2H was used as the basis for the parameter/sensitivity study in Section 3, and the general sensitivity to metal swelling is discussed in Subsection 3.7.

2.4 F2V MODELING

The initial BEHAVE-1 modeling of this lower smear density (86.7%) Type-316 cladding pin gave considerable disparity between the calculated and measured metal swelling. The cladding strain initially predicted was due solely to thermal and irradiation creep under an internal pressure determined by noble gases at the plenum temperature and volume.

To account for the enhanced metal swelling, the gas-bottle model was invoked (GASBOT = 1) during the last two steady-state periods of the X050 irradiation (Table 2-2). There are no quantitative data directly related to F2V which support this modeling; there is, however, the aforementioned qualitative observation that some mechanism moved the fuel axially inside the cladding.

Table 2-2
REACTOR POWER HISTORY FOR BEHAVE-I MODELING OF FUEL PINS F2G, F2H and F2V

Description	EBR-II Power (MW)	Comments
Startup		Strain predicted for F2H only
Steady State (8.7 days)	45	Subassembly XG05
Power Increase	45/45	XG05 rotated 180 degrees
Steady State (140.1 Days)	45	
Steady State (26.2 days)	42.5	Reduced reactor power
Power Increase	42.5/45	Return to full power
Steady State (15 days)	45	
Steady State (2.7 days)	45	Core change stainless steel reflector installed
Power Increase	45/50	Reactor overpower
Steady State (2.7 days)	45	
Steady State (21.3 days)	30	Reduced reactor power
Power Increase	30/45	Return to full power
Steady State (45 days)	45	XG05 removed at the end of this run (F2H out)
Steady State (118.1 days)	50	Subassembly X050
Power Increase	50/52.5	
Steady State (11.4 days)	52.5	Reactor overpower
Power Increase	52.5/54.9	Reactor overpower
Steady State (54 days)	50	
Steady State (26 days)	50	No reactor shutdowns during this period
Steady State (24 days)	50	No reactor shutdowns during this period

The results are shown in Figure 2-2. The calculated (enhanced) metal swelling diameter change is not as large as that determined from the immersion density measurements but it is in better qualitative agreement than the unenhanced PNL Theta Equation curve. Since the PNL Theta Equation is based in part upon Type-304 stainless steel data and since it is known that 304SS and 316SS exhibit different dependencies of swelling, closer agreement is not expected. The peak between eight and nine inches is due to a maximum in the sum of thermal and irradiation creep at that axial node. The over-prediction of the diameter change at the extremes of the fueled region is believed due to misrepresentation of the axial power shape at low reactor power levels. This causes an overprediction of the power-increase strains at these end nodes.

2.5 F2G MODELING

For fuel pin F2G, the entire power-time data of Table 2-2 were used. The calculated peak burnup is 12.1 at.% compared to 10.84 at.% from the neodymium analysis. The burnup is calculated directly from the linear power, and the axial power and temperature profiles are determined from calculations using the fission density profiles in the EBR-II guide. The calculated percent fission gas release remains uncertain between 90 and 100% because of the difference in measured and calculated burnup.

Figure 2-3 shows the improved agreement between the measured and calculated metal swelling effects afforded by the strain enhanced metal swelling. For Type-347 cladding, the PNL "1.71 Equation" is used and the curves shown are for $A = 200$. This larger value for A is in qualitative agreement with the TEM results which showed more intragranular precipitation in Type-347 than in Type-316 stainless steel.⁸ The gas-bottle effect was also invoked for this pin (GASBOT = 0.55). However, since fuel-cladding mechanical interaction is predicted, the center void is believed to contain fission product debris; this type of perturbation is presently not accounted for in the volume/temperature calculations in the model.

2.6 MODELING CONCLUSIONS

The enhancement of void nucleation by in-situ precipitate formation has been repeatedly experimentally verified¹ and analytically predicted. The effect on the total void volume is less accurate, however, because the higher number density specimens tend to contain voids of smaller average size. Modeling this enhancement phenomenon to be dependent upon creep strain (Equation 2-1) was the result of analyzing the data from F2H and F2V for SS316 annealed and from F2G for SS347 annealed. The basis for this dependency needs to be re-evaluated and the data base should be expanded to include more recent experimental data. The analysis to-date indicates that it is credible to include this option in BEHAVE-1.

Considerable experimental evidence points toward there being central void pressurization by fission products. There is a lack of data to unequivocally prove or disprove the phenomenon. When modeling failed fuel pin F2D, it was interesting to note that the diametral strain in the absence of the gas bottle effect was very nearly the same as for F2G when the effect was invoked (peak strain = 5.5%). Modeling the gas bottle effect for F2D exactly as was done for F2G increased the computed peak strain to 7.2%. Albeit large, it is not known that this latter value represents a failure strain for the SS347-clad F2D fuel pin.

Both the in-situ enhancement of metal swelling and the central void containment of fission products must be considered as speculative mechanisms as presently modeled in BEHAVE-1. The user should be aware of this when drawing conclusions based upon the results of using these code options.

3. PARAMETER/SENSITIVITY STUDY

3.1 GENERAL – REFERENCE CASE

The effects that variations in a number of key parameters have on the calculated end-of-life (EOL) axial diametral profile are discussed in the following subsections. Each result was obtained by changing only one input variable, the remainder being held constant at their reference-case values.

The reference case used is the modeling of fuel pin F2H (Subsection 2.3); the reference input is shown in Figure 3-1. The computed total diameter change along the fueled length of this pin is plotted in Figure 3-2; the mechanisms contributing to this total change and the post-irradiation profilometry curve are also shown. The latter curve is repeated on all the figures for reference. The curves are computer plotted using the BEHAVE I card output in a separate plotting routine. The values plotted are those in the last five columns of Table 3-1. Cladding strain at first startup, steady-state fuel swelling during the last 90 days of operation ($SR = 0.45$), enhanced metal swelling (ICSOPT = 9, ISIMS = 1, A = 80), and the four mid-life power increases are calculated to have contributed to the measured profile. Computed thermal and irradiation creep strain (TCREEP and ICREEP) are negligible as shown in Table 3-1.

Table 3-1
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR THE REFERENCE CASE

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.13	0.84	2.03
2.14	53.97	0.373			0.44	1.05	2.66
3.57	57.37	0.407			0.90	1.27	3.21
4.99	59.49	0.424			1.32	1.39	3.71
6.42	59.89	0.427			1.49	1.39	3.85
7.85	58.45	0.416			1.33	1.24	3.57
9.28	55.73	0.386			0.97	0.99	2.99
10.70	51.87	0.351			0.54	0.76	2.14
12.13	47.52	0.308			0.21	0.56	1.65
13.56	45.12	0.266			0.10	0.41	1.17

3.2 AS-FABRICATED FUEL-CLADDING DIAMETRAL GAP

The as-fabricated fuel pellet dimensions for F2H were such that the fuel-cladding diametral gap (DGAPN) was not constant over the active length of the pin. The actual diametral gap varied from approximately 0.0020 in. near the bottom of the pin to slightly greater than 0.0031 in. toward the top (plenum) end. The use of these diametral gap values as the uniform pin fuel-cladding gap results in the curves shown in Figures 3-3 and 3-4, respectively. The 0.0020-in. gap causes an 0.9-mil increase in the computed strain at first startup. This smaller gap decreases the available fuel pin void volume by 7.9%, which causes a 28.9% increase in the average fuel-cladding mechanical interaction strain.

The diameter change with irradiation time for this smaller gap is compared with that for the reference case in Figure 3-5. When compared with the reference, the diameter difference at 150 days is still that due to first startup. The additional 30 days of mechanical interaction sustained by this smaller-gap pin increases the EOL difference in diameter at $z/L = 0.45$ to 1.6 mil above the reference case EOL diameter.

A uniform 0.0031-in. gap eliminates the first-startup strain, and the 11.4% increase in available void is sufficient to limit the occurrence of fuel-cladding mechanical interaction to the last 45 days of operation. The computed diametral profile is shown in Figure 3-4, and the linearized time behavior is included in Figure 3-5.

LISTING OF INPUT FROM FILE 5

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

00

2 3 4 3 3 4 3 3 4 3 3 4 3 1

\$MAINP/ M7=10 , FCL=14.27 , DLE=1.427 , DLTE=1.427
 IHGOPT= 1 , CODN=0.2491 , CIDN=0.2203 , DGAPN=0.0026
 BLT=6.01 , RTD= 10.96 , BDSN=0.933 ,
 FDPN=0.961 , FTD=11.00 , PLL=13.0
 \$

\$STAINP/ NPD=0 , NPS=-1 , ITCOPT=3
 TSI=700. , TSQ= 868 , ITCA= 2 ,
 QL = 12.96, 14.14, 15.08, 15.66, 15.77,
 15.39, 14.67, 13.65, 12.50, 11.86,
 TCA= 880, 911, 940, 966, 985, 999, 1007, 1011, 1013, 1014
 \$

\$STEINP/DTMT=8.7 ,
 STIME=0.0 , SVAL=15.77 , ETIME=8.7 , EVAL=15.73 ,
 ICSOPT=9 , ICCOPT=4 , SP=50
 IETOPT=1 , ITCOPT=3 ,
 FN= 10* 2.14 , FDE= .86 , FLFU= 1.2 ,
 FGT=.78, .87, .95, .99, .997, .97, .90, .82, .72, .62,
 ISIMS = 1, A = 80., SR= .45
 \$

\$PININP/SVALE= 16.86 , FVALS= 15.73 , FRAINC= .7
 \$

\$STEINP/DTMT= 140.1 , FN= 10* 2.29,
 STIME= 8.7 , SVAL= 16.86 , ETIME= 148.8 , EVAL= 16.125
 \$

\$STEINP/DTMT= 26.21 , FN= 10* 2.16,
 STIME= 148.8 , SVAL= 15.228 , ETIME= 175.1 , EVAL= 15.064
 \$

\$PININP/SVALE= 15.95 , FVALS= 15.06, FRAINC= .7
 \$

\$STEINP/DTMT= 15.0 , FN= 10* 2.29,
 STIME= 175.1 , SVAL= 15.950 , ETIME= 190.1 , EVAL= 15.88
 \$

\$STEINP/DTMT= 2.7 , FN= 10* 2.26,
 STIME= 190.1 , SVAL= 15.71 , ETIME= 192.8 , EVAL= 15.708
 \$

\$PININP/SVALE= 17.62 , FVALS= 15.708, FRAINC= .7
 \$

\$STEINP/DTMT= 2.71 , FN= 10* 2.26,
 STIME= 192.8 , SVAL= 15.68 , ETIME= 195.5 , EVAL= 15.67
 \$

\$STEINP/DTMT= 21.30 , FN= 10* 1.51,
 STIME= 195.5 , SVAL= 10.45 , ETIME= 216.8 , EVAL= 10.38
 \$

\$PININP/SVALE= 15.57 , FVALS= 10.38 , FRAINC= .7
 \$

\$STEINP/DTMT= 45.00 , FN= 10* 2.26,
 STIME= 216.8 , SVAL= 15.570 , ETIME= 261.8 , EVAL= 15.34
 \$

Figure 3-1. Input Data for Reference Case

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

Figure 3-2. Axial Diameter Change for the Reference Case

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

- ◆ THERMAL CREEP
- + IRRADIATION CREEP
- x FUEL/CLAD MECHANICAL INTERACTION
- CLAD SWELLING
- | PREDICTED POST IRRADIATION G.D.
- △ MEASURED POST IRRADIATION G.D.

3-4

Figure 3-3. Axial Diameter Change Assuming a 0.0020-In. Uniform As-Fabricated Fuel-Cladding Daimetral Gap

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Figure 3-4. Axial Diameter Change Assuming a 0.0031-In. Uniform As-Fabricated Fuel-Cladding Diametral Gap

Figure 3-5 Effect of As-Fabricated Fuel-Cladding Diametral Gap on Diameter at $Q/L = 0.45$

For these calculations, the startup linear power was 15.77 kW/ft at $\ell/L = 0.45$. The fuel-cladding gap heat transfer coefficient was 1373 Btu/hr-ft²·°F for the 0.0026-in. reference gap and was 1490 and 1289 Btu/hr-ft²·°F for the 0.0020- and 0.0031-in. gaps, respectively (Subsection 3.3). The mechanistic contributions to the total diametral change are given in Table 3-2 for the smaller (3.2a) and larger (3.2b) gap.

Table 3-2
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR

3.2a An 0.0020 in. as-fabricated fuel-cladding gap

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.40	0.334	<0.01	~0.01	0.14	0.85	1.97
2.14	53.67	0.373	↓	↓	1.54	1.06	2.84
3.57	57.05	0.407	↓	↓	1.16	1.31	4.16
4.99	59.16	0.424	↓	↓	1.73	1.46	4.81
6.42	59.56	0.427	↓	↓	1.98	1.47	5.39
7.85	58.13	0.416	↓	↓	1.75	1.30	4.56
9.28	55.43	0.386	↓	↓	1.25	1.03	3.89
10.70	51.58	0.351	↓	↓	0.66	0.77	2.56
12.13	47.26	0.308	↓	↓	0.25	0.56	1.62
13.56	44.87	0.266	↓	↓	0.11	0.41	1.20

3.2b An 0.0031 in. as-fabricated fuel-cladding gap

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.90	0.334	<0.01	~0.01	0.08	0.84	1.98
2.14	54.22	0.373	↓	↓	0.26	1.02	2.18
3.57	57.63	0.407	↓	↓	0.52	1.20	2.62
4.99	59.76	0.424	↓	↓	0.74	1.29	2.86
6.42	60.17	0.427	↓	↓	0.83	1.27	2.91
7.85	58.72	0.416	↓	↓	0.75	1.15	2.76
9.28	55.99	0.386	↓	↓	0.55	0.94	2.39
10.70	52.11	0.351	↓	↓	0.31	0.74	1.93
12.13	47.74	0.308	↓	↓	0.13	0.55	1.44
13.56	45.33	0.266	↓	↓	0.07	0.41	1.15

3.3 STEADY-STATE FUEL-CLADDING GAP HEAT TRANSFER COEFFICIENT

The gap heat transfer coefficient was determined for the reference case from a data correlation equation (Subsection 4.1). This equation is

$$HG = 2011 - 3.114 \times 10^{-4} \cdot (DGAPN) + 2.539 \times 10^{-7} \cdot (DGAPN)^2$$

where

HG = fuel-cladding gap heat transfer coefficient (Btu/hr-ft²·°F), and
DGAPN = as-fabricated fuel-cladding diametral gap (in.).

This equation is used if IHGOPT > 0. If the user wishes to specify a coefficient, then IHGOPT = 0 and the desired HG array is included in the STAINP input. Since this important parameter is not well known, the reference HG = 1373 Btu/hr-ft²·°F was varied by approximately +500 to determine the sensitivity to this parameter. The variation used is large (38%) but is typical of the uncertainty in available experimental data.

The low heat transfer coefficient increased the fuel surface temperature 360°F above the reference case 1700°F at $z/L = 0.45$. As a consequence, fuel melting is calculated for the axial nodes at $z/L = 0.35, 0.45$, and 0.55 . The maximum fuel temperature for the reference case was approximately 4770°F, and a melting temperature of 5090°F is assumed in the code's radial temperature subroutine (FTEMP4). Since the possible movement of molten fuel is not accounted for in BEHAVE-I, the diameter profile of Figure 3-6 is subject to considerable uncertainty.

Table 3-3
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR

3.3a A fuel-cladding gap heat transfer coefficient of 900 Btu/hr-ft²·°F

(Note: fuel melting is predicted, and possible movement of this fuel is not accounted for)

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.15	0.85	2.05
2.14	53.97	0.373	↓	↓	0.36	1.04	2.40
3.57	57.37	0.407	↓	↓	0.60	1.22	2.78
4.99	59.49	0.424	↓	↓	0.79	1.30	3.12
6.42	59.89	0.427	↓	↓	0.86	1.28	3.16
7.85	58.45	0.416	↓	↓	0.79	1.15	2.91
9.28	55.73	0.386	↓	↓	0.62	0.95	2.46
10.70	51.87	0.351	↓	↓	0.40	0.75	1.95
12.13	47.52	0.308	↓	↓	0.21	0.56	1.58
13.56	45.12	0.266	↓	↓	0.13	0.41	1.18

3.3b A fuel-cladding gap heat transfer coefficient of 1900 Btu/hr-ft²·°F

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.90	0.334	<0.01	~0.01	0.05	0.83	1.99
2.14	54.22	0.373	↓	↓	0.25	1.02	2.20
3.57	57.63	0.407	↓	↓	0.62	1.22	2.86
4.99	59.76	0.424	↓	↓	0.99	1.34	3.37
6.42	60.17	0.427	↓	↓	1.14	1.34	3.45
7.85	58.72	0.416	↓	↓	1.00	1.20	3.11
9.28	55.99	0.386	↓	↓	0.69	0.96	2.43
10.70	52.11	0.351	↓	↓	0.33	0.74	1.82
12.13	47.74	0.308	↓	↓	0.10	0.55	1.43
13.56	45.33	0.266	↓	↓	0.04	0.40	1.10

3.4 AS-FABRICATED FUEL DENSITY

The effect of initial fuel density (FDPN) was investigated by decreasing this parameter from the reference value of 96.1% of theoretical density (11.0 gm/cm³) to 93% and 90%. Essentially, the same results were obtained for both of these lower densities and, therefore, only the results for the 93% dense fuel are shown. The lower fuel density prevents strain at first startup; the additional fuel porosity can accommodate all fission products produced and there is no predicted fuel-cladding mechanical interaction. The difference (0.7 to 0.8 mil) between the cladding swelling and total diameter change curves in Figure 3-8 is due to the four power increase events. These results are summarized in Table 3-4.

Figure 3-6. Axial Diameter Change Assuming a Uniform Fuel-Cladding Gap Heat Transfer Coefficient of 900 BTU/hr-ft²-°F

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Figure 3-7. Axial Diameter Change Assuming a Uniform Fuel-Cladding Gap Heat Transfer Coefficient of 1900 BTU/ft²-°F

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BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

Figure 3-8. Axial Diameter Change Assuming a Fuel Density of 93% of Theoretical

Table 3-4
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER
CHANGE FOR A FUEL DENSITY 93% OF THEORETICAL

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	51.33	0.334	<0.01	~0.01	0.	0.83	1.86
2.14	55.77	0.373	↓	↓	↓	0.96	1.91
3.57	59.28	0.407	↓	↓	↓	1.08	1.98
4.99	61.47	0.424	↓	↓	↓	1.10	1.93
6.42	61.89	0.427	↓	↓	↓	1.07	1.82
7.85	60.40	0.416	↓	↓	↓	0.98	1.78
9.28	57.59	0.386	↓	↓	↓	0.83	1.58
10.70	53.60	0.351	↓	↓	↓	0.69	1.37
12.13	49.10	0.308	↓	↓	↓	0.53	1.31
13.56	46.64	0.266	↓	↓	↓	0.40	1.10

3.5 LINEAR POWER AND CLADDING TEMPERATURE

The linear power (QL) and cladding temperature (TCA) arrays used in the reference case result from calculations using the fission density profiles for EBR-II as published in the Guide.⁹ Fuel pin burnups calculated by General Electric using Nd-148 have consistently been ~10% less than predicted from the Guide fission density values. The effects of lowering the linear power (and, therefore, the cladding temperatures) to 94% and 85% of the reference value have been determined.

With the linear power reduced by 6% (15.77 to 14.82 kW/ft peak), there is no predicted strain at first startup. The lower power throughout life decreases the average burnup from 53.9 to 50.7 MWd/KgM. Less fission products accumulate, and this lowers the maximum fuel-cladding mechanical interaction strain from 0.6 to 0.53%. The calculated EOL plenum pressure is 456 psi compared to the reference 490 psi, with a corresponding lower maximum cladding irradiation creep strain of 0.56×10^{-4} . (The thermal creep strain at the same axial node is 0.19×10^{-11} .) The axial profile of Figure 3-9 for this reduced linear power case is in better agreement with the profilometry curve than is the reference curve (Figure 3-2). Comparing the cladding swelling values of Table 3-5 with those of Table 3-1, it is seen that the agreement with the immersion density results presented in Figure 2-1 is preserved.

As shown in Figure 3-10 and Table 3-5, reducing the linear power by 15% decreases the strain due to fuel-cladding mechanical interaction to less than one mil. A comparison of the calculated metal swelling (Table 3-5a and b) shows that the lower cladding temperatures increase this dilation effect more than it is reduced because of decrease in the strain-enhancement effect. The results show the average ΔD decreases by 21% for a 15% reduction in linear power.

3.6 RESTRUCTURING LINEAR POWER

Fuel restructuring in BEHAVE-I is time-invariant. The restructuring subroutine (RESTRU) is called only once at the beginning of the simulated irradiation (by subroutine FSTART). The steady-state fuel porosity distribution calculated for the reference case utilizes the beginning-of-life (BOL) linear power and cladding temperature arrays. If a new steady-state condition is established early in life (e.g., the 180° rotation of assembly XG05 which increased the power of F2H after 8.7 days exposure), it may be more accurate to calculate fuel restructuring based upon the later steady-state conditions.

An option (ITCA = 0) is supplied the user for this purpose. The restructuring linear power array (PL) and cladding temperature array (ATC) can be input as distinct from the BOL arrays (QL and TCA, respectively).

The linear power and cladding temperature arrays input to study this effect were taken from the second (140 days) reference case steady-state period of operation. The peak restructuring linear power was 16.1 kW/ft, or 2.3%

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

Figure 3-9. Axial Diameter Change Assuming Linear Power is 94% of the Reference Value

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

Figure 3-10. Axial Diameter Change Assuming Linear Power is 85% of the Reference Value

Table 3-5
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR

3.5a A six percent reduction in linear power throughout life

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	46.69	0.334	<0.01	~0.01	0.06	0.85	1.88
2.14	50.73	0.373	↓	↓	0.30	1.04	2.46
3.57	53.92	0.407	↓	↓	0.73	1.27	2.89
4.99	55.93	0.424	↓	↓	1.15	1.40	3.40
6.42	56.30	0.427	↓	↓	1.31	1.41	3.48
7.85	54.97	0.416	↓	↓	1.15	1.26	3.25
9.28	52.40	0.386	↓	↓	0.78	1.01	2.70
10.70	48.76	0.351	↓	↓	0.37	0.78	1.94
12.13	44.68	0.308	↓	↓	0.11	0.57	1.57
13.56	42.43	0.266	↓	↓	0.04	0.42	1.15

3.5b A fifteen percent reduction in linear power throughout life

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	42.25	0.334	<0.01	~0.01	0.01	0.85	1.75
2.14	45.89	0.373	↓	↓	0.11	1.03	2.18
3.57	48.78	0.407	↓	↓	0.43	1.26	2.63
4.99	50.58	0.424	↓	↓	0.83	1.41	3.08
6.42	50.91	0.427	↓	↓	0.99	1.43	3.17
7.85	49.69	0.416	↓	↓	0.81	1.28	2.94
9.28	47.39	0.386	↓	↓	0.46	1.02	2.29
10.70	44.09	0.351	↓	↓	0.15	0.80	1.65
12.13	40.38	0.308	↓	↓	0.02	0.61	1.43
13.56	38.36	0.266	↓	↓	0.00	0.45	1.19

greater than the reference case peak power. The cladding temperatures were increased accordingly, resulting in a 9°F higher maximum cladding mid-wall temperature.

These higher power/temperature input conditions resulted in a 100°F higher center fuel temperature. The columnar and equiaxed grain boundary isotherms are at larger radii, and the central void radius increased slightly as shown in Table 3-6. The net effect of restructuring at these increased conditions was to create more internal fuel pin void to accommodate fission products, and the computed fuel-cladding mechanical interaction strain was thus less than the reference case. This decreased the EOL peak $\Delta D/D$ by 0.3 mil; the summary curves are shown in Figure 3-11 and the values which define these curves in Table 3-7. For the case investigated, the time-invariant restructuring logic of the code results in a small change in predicted EOL cladding diameter. It is important that the user be aware of this limitation in applying the code to histories containing large variations in fuel pin power.

3.7 NEUTRON-INDUCED CLADDING SWELLING

The importance of accurately predicting cladding swelling cannot be over-emphasized. The inability of existing data correlation equations to predict the measured EOL metal swelling (in-situ enhancement) is discussed in Subsection 2.1. Because cladding swelling creates additional internal fuel pin volume which can accommodate fission products, an under-prediction of swelling strain will result in an over-prediction of fuel-cladding mechanical interaction

Table 3-6
Columnar (2150°C), Equiaxed (1650°C), and Central Void Radii Assuming Fuel Restructuring is Complete after 8.7 days (startup conditions: reference case) and after 148.8 days (higher power/cladding temperature conditions)

Z/L	Height (in.)	Linear Power (kw/ft)		Equiaxed Radius (mil)		Columnar Radius (mil)		Central Void Radius (mil)	
		8.7 (days)	148.8 (days)	8.7 (days)	148.8 (days)	8.7 (days)	148.8 (days)	8.7 (days)	148.8 (days)
0.05	0.71	12.96	13.32	64.4	67.1	15.9	27.3	6.5	7.5
0.15	2.14	14.14	14.50	72.9	74.9	42.2	46.5	9.2	9.7
0.25	3.57	15.08	15.44	78.3	79.9	53.1	56.3	10.6	11.1
0.35	4.99	15.66	16.02	81.4	82.8	58.5	61.2	11.4	11.7
0.45	6.42	15.77	16.13	82.4	83.7	59.9	62.5	11.6	11.9
0.55	7.85	15.39	15.75	81.4	82.9	58.2	60.8	11.3	11.7
0.65	9.28	14.67	15.03	78.9	80.5	52.9	56.1	10.6	11.1
0.75	10.70	13.65	14.00	74.4	76.4	43.2	47.6	9.4	9.9
0.85	12.13	12.50	12.86	67.7	70.4	24.0	32.6	7.3	8.2
0.95	13.56	11.86	12.21	63.1	66.2	6.1	16.2	6.1	6.7

Table 3-7
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR
RESTRUCTURING COMPLETE AFTER 148.8 DAYS OF OPERATION

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.13	0.85	2.04
2.14	53.97	0.373			0.40	1.04	2.61
3.57	57.37	0.407			0.76	1.25	3.04
4.99	59.49	0.424			1.09	1.35	3.42
6.42	59.89	0.427			1.21	1.34	3.52
7.85	58.45	0.416			1.10	1.20	3.29
9.28	55.73	0.386			0.82	0.97	2.83
10.70	51.87	0.351			0.48	0.76	2.08
12.13	47.53	0.308			0.22	0.56	1.66
13.56	45.12	0.266			0.12	0.41	1.19

strain due to fuel swelling. The increasing availability of immersion density data to define the final state of metal swelling, when used in addition to the profilometry curves for post-irradiation fuel pin outside diameter, has opened up the possibility of individually understanding these two major distortion mechanisms; this has precluded the use of arbitrary assumptions regarding swelling which have often been made in order to calculate the observed diameter profile.

The reference case metal swelling was predicted by the PNL Theta Equation (ICSOPT = 9) with in-situ enhancement based upon creep strain (Equation 2-1 with A = 80). The metal swelling calculated using various options in the CSWEL subroutine are summarized in Table 3-8. It is evident from Table 3-8 and from Figure 3-12 that a swelling-free cladding is not desirable if the fuel pin smear density is high enough to cause fuel-cladding mechanical interaction over an appreciable portion of pin lifetime. Fuel swelling and power increase strains combine to yield the predicted post-irradiation outside diameter; the former mechanism is operative approximately 20 days longer than for

Figure 3-11. Axial Diameter Change Assuming Fuel Restructuring is Complete After 148.8 Days of Operation

Figure 3-12. Axial Diameter Change Assuming No Neutron-Induced Metal Swelling of the Cladding

the reference case (Figure 3-5). The condition of having minimal plastic fuel swelling strain might be achieved by tailoring the cladding material properties so that cladding swelling creates additional fuel pin void for fission products at a rate equal to the volume expansion of the fuel due to these products.

Table 3-8

THE EFFECTS OF DIFFERENT NEUTRON-INDUCED METAL SWELLING CORRELATIONS

(a) PNL Theta Equation with no enhancement

Height (in.)	FUSWELL (mil)	CLSWELL (mil)	Total Diameter (chg mil)
0.71	0.15	0.82	2.03
2.14	0.52	0.96	2.66
3.57	1.07	1.08	3.19
4.99	1.57	1.10	3.67
6.42	1.77	1.07	3.81
7.85	1.58	0.98	3.56
9.28	1.15	0.83	3.02
10.70	0.64	0.69	2.17
12.13	0.25	0.53	1.67
13.56	0.12	0.40	1.19

(b) PNL Theta Equation with enhancement (Reference Case, A = 80)

Height (in.)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	0.13	0.84	2.03
2.14	0.44	1.05	2.66
3.57	0.90	1.27	3.21
4.99	1.32	1.39	3.71
6.42	1.49	1.39	3.85
7.85	1.33	1.24	3.57
9.28	0.97	0.99	2.99
10.70	0.54	0.76	2.14
12.13	0.21	0.56	1.65
13.56	0.10	0.41	1.17

(c) PNL Theta Equation with enhancement (A = 160)

Height (in.)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	0.11	0.86	2.03
2.14	0.38	1.11	2.67
3.57	0.78	1.40	3.24
4.99	1.15	1.60	3.75
6.42	1.29	1.61	3.90
7.85	1.15	1.42	3.59
9.28	0.84	1.11	2.99
10.70	0.47	0.82	2.13
12.13	0.18	0.57	1.65
13.56	0.09	0.41	1.17

Table 3-8 (Continued)

(d) PNL "1.71" Equation with enhancement (A = 80)

Height (in.)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	0.07	1.00	2.05
2.14	0.26	1.28	2.71
3.57	0.53	1.57	3.14
4.99	0.77	1.75	3.51
6.42	0.87	1.77	3.61
7.85	0.77	1.64	3.40
9.28	0.56	1.37	2.97
10.70	0.31	1.11	2.27
12.13	0.12	0.86	1.87
13.56	0.06	0.65	1.38

(e) Minimal Clad Swelling

Height (in.)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	0.29	<0.01	1.34
2.14	1.02		2.19
3.57	2.09		3.14
4.99	3.07		4.08
6.42	3.46		4.46
7.85	3.08		4.12
9.28	2.25		3.30
10.70	1.24		2.09
12.13	0.49		1.38
13.56	0.24		0.90

(f) Swelling for SS321 and CW316 (ICSOPT = 2) with enhancement (A = 80)

Height (in.)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	0.17	0.38	1.61
2.14	0.60	0.55	2.32
3.57	1.24	0.76	3.03
4.99	1.81	0.94	3.75
6.42	2.04	1.03	4.05
7.85	1.82	0.99	3.83
9.28	1.33	0.85	3.21
10.70	0.73	0.69	2.27
12.13	0.29	0.53	1.71
13.56	0.14	0.42	1.22

3.8 NEUTRON FLUX

The reference case fluence-to-burnup ratio is that for the hard-spectrum EBR-II. Demonstration Plant fuel pins will be subjected to a higher ratio (by a factor of 3 to 4) in a softer neutron spectrum, and the performance prediction under these conditions is of major interest. To investigate this effect, the reference $E > 0.1$ MeV fluence-to-burnup ratio was increased by a factor of 3; the results are shown in Table 3-9. The higher flux (Reference FN * 3) causes greater metal swelling and irradiation creep of the cladding, preventing any fuel-cladding mechanical interaction from occurring. The opposite would occur with a flux decrease. In the BEHAVE-I logic, neutron flux is used only in the swelling equation and, therefore, increasing and decreasing this parameter has the effect of modeling a high- and low-swelling material with the same temperature dependence.

Table 3-9
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE ASSUMING A FACTOR
OF THREE GREATER NEUTRON FLUX WITH THE REFERENCE SPECTRUM

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	1.002	<0.01	0.04	0	6.17	7.22
2.14	53.97	1.118		0.04		7.43	8.69
3.57	57.37	1.221		0.04		8.49	9.74
4.99	59.49	1.272		0.04		8.82	9.95
6.42	59.89	1.281		0.04		8.63	9.74
7.85	58.45	1.247		0.04		7.96	9.15
9.28	55.73	1.157		0.04		6.78	7.85
10.70	51.87	1.054		0.03		5.62	6.57
12.13	47.52	0.925		0.03		4.36	5.33
13.56	45.12	0.797		0.03		3.26	3.98

3.9 FUEL SWELLING RATE

The reference fuel swelling rate (SR) is 0.45% per 10^{20} fissions/cc, determined by varying this parameter to obtain agreement with the F2H post-irradiation profilometry curve. Thus, this parameter is model- and assumption-dependent (i.e., the assumption was made that the measured F2H plastic strain not accounted for by the power increases was due entirely to the fuel-cladding mechanical interaction resulting from fuel swelling). Marlowe's¹⁰ theoretical estimates of the contribution of fission gases to fuel swelling (with re-solution) is between 0.21 and 0.27% per 10^{20} fissions/cc. The work by Anselin¹¹ places the contribution of solid fission products between 0.13 and 0.54% per 10^{20} fissions/cc. The reference value is greater than the maximum 0.34 but considerably less than the maximum 0.81 rate determined by summing these effects.

No fuel-cladding mechanical interaction is calculated when a rate of 0.34% per 10^{20} fissions/cc is used, whereas a rate of 0.56 results in that mechanism accounting for dilation equal to the total measured change at $L/L = 0.45$ (Figure 3-13 and Table 3-10). Note from the table the associated increase in the calculated metal swelling due to imposing the strain enhancement effect.

3.10 POWER INCREASE STRAIN

Five power increase events are included in the reference case: (1) first startup; (2) rotation of subassembly XG05 after 8.7 days of operation; (3) a return to full power after a 26-day period of 42.5 mW reactor power operation (175 days); (4) a reactor overpower from 45 to 50 mW (193 days); and (5) a return to full power after a 21-day period of 30 mW reactor power operation (217 days). This power history is detailed in Table 2-2.

The omission of the four midlife power increase events (FRAIN = 0) decreases the calculated EOL diameter by an average of 0.8 mil, as shown in Figure 3-14. The difference between the total diameter change and the sum of the components listed in Table 3-11 for each node is the strain at first startup. This latter strain is small, being about 0.2 mil at the peak power node ($L/L = 0.45$).

Table 3-10
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE
FOR A FUEL SWELLING RATE OF 0.56% PER 10²⁰ FISSIONS/CC

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.31	0.87	2.16
2.14	53.97	0.373	↓	↓	1.07	1.14	3.39
3.57	57.37	0.407	↓	↓	2.18	1.49	4.73
4.99	59.49	0.424	↓	↓	3.20	1.71	5.95
6.42	59.89	0.427	↓	↓	3.61	1.74	6.36
7.85	58.45	0.416	↓	↓	3.22	1.53	5.72
9.28	55.73	0.386	↓	↓	2.35	1.17	4.57
10.70	51.87	0.351	↓	↓	1.30	0.85	2.99
12.13	47.52	0.308	↓	↓	0.51	0.58	1.98
13.56	45.12	0.266	↓	↓	0.25	0.42	1.33

Table 3-11
SUMMARY, BY MECHANISM, OF EOL CLADDING DIAMETER CHANGE FOR THE ASSUMPTIONS OF
(a) No mid-life power increase effects (FRAINC = 0)

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.13	0.84	0.99
2.14	53.97	0.373	↓	↓	0.44	1.05	1.56
3.57	57.37	0.407	↓	↓	0.90	1.27	2.33
4.99	59.49	0.424	↓	↓	1.32	1.39	2.89
6.42	49.89	0.427	↓	↓	1.49	1.39	3.12
7.85	58.45	0.416	↓	↓	1.33	1.24	2.74
9.28	55.73	0.386	↓	↓	0.97	0.99	2.12
10.70	51.87	0.351	↓	↓	0.54	0.76	1.37
12.13	47.52	0.308	↓	↓	0.21	0.56	0.79
13.56	45.12	0.266	↓	↓	0.10	0.41	0.52

(b) Full mid-life power increase effects (FRAINC = 1)

Height (in.)	Burnup (MWd/kg)	Fluence (*E-23)	TCREEP (mil)	ICREEP (mil)	FUSWEL (mil)	CLSWEL (mil)	Total Diameter (chg mil)
0.71	49.67	0.334	<0.01	~0.01	0.13	0.84	2.49
2.14	53.97	0.373	↓	↓	0.44	1.05	3.13
3.57	57.37	0.407	↓	↓	0.90	1.27	3.58
4.99	59.49	0.424	↓	↓	1.32	1.39	4.06
6.42	59.89	0.427	↓	↓	1.49	1.39	4.17
7.85	58.45	0.416	↓	↓	1.33	1.24	3.94
9.28	55.73	0.386	↓	↓	0.97	0.99	3.37
10.70	51.87	0.351	↓	↓	0.54	0.76	2.47
12.13	47.52	0.308	↓	↓	0.21	0.56	2.02
13.56	45.12	0.266	↓	↓	0.10	0.41	1.45

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BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

3-23

Figure 3-13. Axial Diameter Change Assuming a Fuel Swelling Rate of 0.56% per 10^{20} Fissions/cc

GEAP-13741

BEHAVE-1. PARAMETER/SENSITIVITY STUDY F2H

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GEAP-13741

Figure 3-14. Axial Diameter Change Assuming No Mid-Life Power Increase Effects

The power increase differential thermal expansion strain is modeled in subroutine INCRES. General Electric fuel pin FOE was used to calibrate the power increase effect.¹² After 217 days of EBR-II operation in grid position 7A1, a maximum linear power increase from 5.8 to 10 kW/ft occurred due to the introduction of a stainless steel reflector. A second midlife power change occurred upon the return to a reactor power of 45 mW after operation at 30 mW. When the power increase effect with FRAINC = 0.7 was added to the metal swelling strain, the observed post-irradiation diameter increase of FOE was accounted for.

The results obtained when the total strain calculated for each power increase event is included (FRAINC = 1) are also included in Table 3-11.

4. DESCRIPTION OF BEHAVE-I

PROGRAM FLOW

The following subsections detail the method of solution and the various analyses embodied in BEHAVE-I.

4.1 THE EXECUTIVE ROUTINE (SUBROUTINE MAIN)

The computer formulation of BEHAVE-I consists of an executive routine, three linked operating regime subroutines, eight mechanism subroutines, material property FUNCTIONS, and an ACCUMULATOR file, as shown in Figure 4-1. The executive routine (MAIN) reads the input fuel pin identification, sequence of power regimes, and pin fabrication parameters for NAMELIST MAINP (Figure 4-2); the variables are listed in Table 4-1. Typical NAMELIST input format is shown in Figure 3-1.

Using the input diametral fuel-cladding gap (DGAPN — in.), MAIN computes the fuel-cladding gap heat transfer coefficient HG (if IHGOPT = 1) for J = 1, MZ from¹³
$$HG(J) = 2011. - 3.11 \times 10^5 * (DGAPN) + 2.538 \times 10^7 * (DGAPN)^2 \text{ (Btu/hr-ft}^2 \cdot ^\circ\text{F).} \quad (4-1)$$

If a variable fuel-cladding gap (DGAPN) dictates a unique HG for each axial node, IHGOPT is set equal to zero and MZ values of gap coefficient are input. The routine then calculates the fuel pin smear density (FDSN) and defines the location of the axial nodes using input DLE and DLTE.

The executive routine calls one of the operating regime subroutines (FSTART, Subsection 4.2; STEADY, Subsection 4.6; INCRES, Subsection 4.12), following the JOP input sequence. The conditions for each period of operation are input to the operating regime subroutine when it is called and these subroutines, in turn, call the appropriate mechanism subroutines to calculate the changes in fuel element parameters occurring during that period of operation. The new values of the parameters are transferred to the ACCUMULATOR file after each calculation, which file then becomes the source of the initial conditions for the next calculation.

4.2 THE FIRST STARTUP (SUBROUTINE FSTART)

Significant effects occurring during the first startup with fresh fuel are (1) the possible fuel-cladding interference due to differential thermal expansion, and (2) the restructuring of the fuel during the initial period at power. The flow chart and the input operating conditions for FSTART are shown in Figure 4-3 and Table 4-2, respectively.

The model used to calculate fuel-cladding interference during startup has been reported previously.¹⁴ Figure 4-4 shows the mean diameter increase along the fueled length as a function of linear power. It is assumed that the fuel is continually restrained by the high radial stresses characteristic of fuel-cladding mechanical interaction. This model is inappropriate if a condition of radial restraint does not exist. The curve exhibits regions corresponding to four fuel expansion modes. At very low linear power, the brittle fuel will crack under the thermal stresses resulting from the radial temperature gradient. Mode 1, the concave upward portion, reflects the expansion of the fuel before it has reached a center temperature of 1300°C. Fuel below this temperature is relatively strong and will not plastically deform to eliminate the presence of finite cracks. As linear power is increased, the center temperature rises above 1300°C (mode 2), and the slope of the curve decreases as a larger and larger fraction of the thermal expansion of the fuel is absorbed by hot pressing of the porosity in the plastic central core of fuel. The driving force for this porosity decrease is the restraint of the cladding.

When the central core is porous, any load imposed on the outside of the fuel is borne by a thin ring of fuel at the root of the crack. The circumferential or hoop stress in the fuel is maximum at the root of the crack and decreases exponentially with decreasing radius due to the temperature gradient and corresponding gradient in material strength. Because of the thinness of the load bearing area, stresses are very high and the fuel deforms at relatively low temperatures. Analyses utilizing thermally activated creep data indicate that the temperature at the root of the crack will be maintained at about 1300°C during startup. The crack-root temperature is weakly dependent on the rate of power increase during startup and on the fuel physical properties.

Table 4-1
INPUT TO SUBROUTINE MAIN

PINID	=	fuel element identification (up to 59 columns starting in column 2)	Card 1 FORMAT (10A6, A4)
ICASE	=	case number, column 61-64	
JOP	=	power history option sequence 2 = FSTART 3 = STEADY 4 = INGRES 1 = end of power history (limited to 40 sequences per history)	Card 2 FORMAT (40I2)

NAMelist MAINP

MZ	=	number of axial nodes (limit 29)
FCL	=	total fuel column axial length (in.)
DLE	=	axial node length for (MZ-1) nodes (in.)
DLTE	=	length of last axial node (in.)
AC	=	cladding attack option if $\neq 0$, THKINP NAMelist must occur immediately after the first STEINP NAMelist (see CTHICK discussion in Section 4.7)
CODN	=	nominal as-fabricated cladding o.d. (in.)
CIDN	=	nominal as-fabricated cladding i.d. (in.)
DGAPN	=	nominal diametral fuel-cladding gap (in.)
FDPN	=	nominal fuel pellet density (.TD)
FTD	=	fuel theoretical density (g/cm^3)
BTD	=	blanket theoretical density (g/cm^3)
BDSN	=	nominal blanket smeared density (.TD)
BLT	=	total blanket length (upper and lower) (in.)
PLL	=	gas plenum length (in.)
WOC	=	cladding o.d. wear allowance (in.)
WIC	=	cladding i.d. wear allowance (in.)
IHGopt	=	fuel-cladding gap conductance coefficient option = 1 coefficient is computed; $\neq 1$ MZ values of HG must be supplied
HG(MZ)	=	fuel-cladding gap conductance coefficient (if IHGopt $\neq 1$) ($\text{BTU/hr-ft}^2\text{-}^\circ\text{F}$)

Mode 3 occurs when the fuel in the plastic core has hot pressed to virtually 100% of theoretical density. The fuel stress distribution changes markedly. From this point on, the central portion of fuel can expand only by displacing the peripheral fuel. The restraint imposed by the cladding is carried by a relatively large diameter core of weak fuel under a constant hydrostatic pressure. The characteristic temperature at the outside of the 100% dense core is the temperature below which the fuel can resist the hydrostatic pressure without collapse of its porosity. Since the stress on the fuel is much lower than in the mode 2 configuration, the temperature necessary for fuel deformation is higher. The fact that the fuel is deforming under hydrostatic compression rather than a uniaxial load will also tend to increase the characteristic temperature. The net effect is to reduce the effectiveness of the remaining porosity in absorbing the thermal expansion. Thus within mode 3, the curve exhibits a sharp increase slope. Finally in mode 4, the volume expansion upon melting contributes to the volume expansion of the plastic core.

Table 4-2
INPUT TO SUBROUTINE FSTART

NAMELIST STAINP

*QL(MZ) = start-of-life linear power array (kW/ft)
 TSI = core inlet sodium temperature (°F)
 TSO = core outlet sodium temperature (°F)
 TS(MZ) = sodium temperature (°F) (with ITCOPT = 2)
 NPD = detailed printout option (output for each cycle during a power history sequence)
 -1 = print
 0 = no print or plot
 +1 = print and plot (plot not available)
 NPS = summary printout option (output once every power history sequence)
 -1 = print
 0 = no print or plot
 +1 = print and plot (plot not available)
 ITCOPT = cladding temperature option
 1 = calculate TCA(MZ) and CDT(MZ) from input TSI, TSO
 2 = calculate TCA(MZ) and CDT(MZ) from input TS(MZ)
 3 = read TCA(MZ) from input and calculate CDT(MZ)
 4 = use values of TCA(MZ) and CDT(MZ) then stored in the ACCUMULATOR
 TCA(MZ) = mean cladding temperature (°F) (with ITCOPT = 3)
 PL(MZ) = restructuring linear power (kW/ft)
 ATC(MZ) = cladding mean temperature during fuel restructuring (°F)
 ITCA = restructuring mean cladding temperature option
 - 0 PL(MZ) and ATC(MZ) must be input
 = 1 or 2 ATC(K) = TCA(K) K = 1, MZ
 = 2 PL(K) = QL(K)
 FAVBPT = fraction of available void below 1300°C which can accommodate fission products

* arrays of MZ axial nodes

The model described above was developed to predict the mean diameter increase along the length of the pin. Local cladding strain can be predicted if cladding thickness and strength property values are known as a function of local position. The problem is treated in BEHAVE-I by assuming a constant cladding wall thickness and a strain hardening law of the form:

$$\epsilon = k\sigma^4 \quad (4-2)$$

where

ϵ = cladding strain (in./in.),
 σ = cladding stress (psi), and
 k = constant.

Figure 4-1. BEHAVE-1 Flow Diagram

Figure 4-2. Subroutine MAIN Flow Chart

Figure 4-3. Subroutine FSTART Flow Chart

Figure 4-4. Mean Fuel Diametral Increase Predicted by FSTART

Force equilibrium between the cladding hoop stress and the hydrostatic pressure on the 100% dense plastic core of fuel gives for the stress:

$$\sigma = \frac{P_H R_p}{t} \quad (4-3)$$

where

t = thickness of cladding (in.),
 P_H = hydrostatic pressure in plastic core of fuel (psi), and
 R_p = radius of 100% dense plastic core (in.).

Equations 4-2 and 4-3 are solved for the cladding hoop strain at the k th axial node in terms of the mean cladding hoop strain, $\bar{\epsilon}$, and the plastic core radii to give

$$\epsilon(k) = \frac{(R_p(k))^4}{R_p^4} \bar{\epsilon} \quad (4-3)$$

4.3 FUEL RESTRUCTURING (SUBROUTINE RESTRU)

After calculating the cladding strain produced during the first startup and applying this strain to increase cladding diameters, subroutine FSTART calls the fuel restructuring subroutine RESTRU.¹⁵ The restructuring of the fuel is calculated at each axial node for the full power conditions prevailing after the first startup with fresh fuel. If the power and temperature conditions following startup are not maintained for a period sufficient for restructuring to occur, an option is provided (ITCA = 0, Table 4-2) to allow alternate conditions to be input. The use of this option is explained in Subsection 3.6.

The fuel density is initially uniform throughout the pellet, and equal to the as-fabricated value (FDPN). Any effects on the fuel density distribution produced by fuel-cladding interference during the first startup are neglected. Grain growth is assumed to divide the fuel into the following three regions:

Region	Temperature	Characteristics
I	below 1650°C	grain size as fabricated; fuel density as fabricated
II	1650 to 2150°C	equiaxed grain growth; fuel density = 0.97 TD
III	above 2150°C	columnar grain growth; fuel density = 0.99 TD

The 1650°C isotherm (TEG) defines the equiaxed radius (REQ) and the 2150°C isotherm (TCG) defines the columnar radius (RCG).

The central void radius (RCV) is formed from the porosity originally in the now densified equiaxed and columnar grain growth regions. The fuel diameter is set equal to the cladding inside diameter. The fuel-cladding gap is transformed into radial cracks of equivalent volume. The effect of the redistributed gap void on heat transfer is accounted for in the gap coefficient. The restructuring subroutine is called only once during a power history simulation.

At each axial node, the fuel region is divided into 50 radial nodes of equal ΔR from the cladding inside surface (RFS) to the fuel center. The density of each node, ρ_j , is established according to the following:

$$\begin{aligned} R_i &> \text{REG} & \rho_j &= \text{FDPN} \\ \text{REG} &\geq R_i > \text{RCG} & \rho_j &= \rho_{II} = 0.97 \text{ TD} \\ \text{RCG} &\geq R_i > \text{RCV} & \rho_j &= \rho_{III} = 0.99 \text{ TD} \\ R_i &< \text{RCV} & \rho_j &= 0 \end{aligned}$$

where $j = j^{\text{th}}$ radial node,

R_i = fuel radius at i^{th} node interface, and
 $i = j + 1$.

The linear power (QL) is assumed to be constant throughout the axial node, so that the volumetric heat generation rate in each region (Q_V^j) is:

$$Q_V^I = \frac{QL}{\pi(RFS)^2} \left(\frac{W}{cm^3} \right) \quad (4-4)$$

$$Q_V^{II} = Q_V^I * \frac{\rho_{II}}{\rho_I}, \text{ and} \quad (4-5)$$

$$Q_V^{III} = Q_V^I * \frac{\rho_{III}}{\rho_I}. \quad (4-6)$$

These volumetric rates are used in the following solutions to the heat balance equation for the three regions:

Region I

$$\int_{TFS}^{TEG} k(T) dT = \frac{Q_V^I}{4} [(RFS)^2 - (REG)^2] (W/cm) \quad (4-7)$$

Region II

$$\int_{TEG}^{TCG} k(T) dT = \frac{Q_V^{II}}{4} \left\{ [(REG)^2 - (RCG)^2] - 2(REG)^2 \left[1 - \frac{\rho_I}{\rho_{II}} \right] \ln \left(\frac{REG}{RCG} \right) \right\} \quad (4-8)$$

Region III

$$\int_{TCG}^{TFM} k(T) dT = \frac{Q_V^{III}}{4} \left\{ [(RCG)^2 - (RCV)^2] - 2b^2 \ln \left(\frac{RCG}{RCV} \right) \right\} \quad (4-9)$$

$$b^2 = (REG)^2 \left[\frac{\rho_{II} - \rho_I}{\rho_{III}} \right] + (RCG)^2 \left[\frac{\rho_{III} - \rho_{II}}{\rho_{III}} \right]$$

The fuel thermal conductivity as a function of temperature (T in °C) and density (as a fraction of theoretical) is¹⁶

$$k(T, \rho) = 0.011 + \frac{1}{(0.4848 - 0.4465 \rho)T} (W/cm \cdot ^\circ C). \quad (4-10a)$$

This correlation is valid up to 2150°C, above which the conductivity is assumed independent of temperature and equal to

$$k(\rho) = 0.011 + \frac{1}{(0.4848 - 0.4465 \rho) \times 2150} \quad (4-10b)$$

The procedure is to first calculate the fuel surface temperature (TFS) from the input mean cladding temperature (TCA), temperature drop across the cladding (CDT), and fuel-cladding gap heat transfer coefficient (HG):

$$TFS = TCA + \frac{1}{2} * CDT + \frac{QL}{2\pi * RFS * HG} (^\circ C). \quad (4-11)$$

Second, Equations 4-7, 4-10a, and 4-4 are solved for the radius of equiaxed grain growth. Third, Equations 4-8, 4-10a, and 4-5 are solved for the radius of columnar grain growth. Fourth, the central void radius is determined from porosity released from the equiaxed and columnar grain growth regions. Finally, Equations 4-9, 4-10b, and 4-6 are

solved for the maximum fuel temperature (TFM). The fuel density for each of the 50 radial nodes is calculated from these microstructural features. From this known geometry, the radial temperature profile is calculated by subroutine FTEMP4.

4.4 RADIAL TEMPERATURE PROFILE (SUBROUTINE FTEMP4)

Subroutine FTEMP4 is first called by FSTART, and calculates the radial temperature profile in the fuel for each axial node. The procedure is to calculate the temperature rise across each of the 50 radial nodes from the boundary conditions:

- (1) temperature at the outer interface (obtained from the calculation at the previous node), and
- (2) heat flux at the inner interface (obtained from the power generated in the then inner nodes).

The heat transfer equation for an axisymmetric profile with these boundary conditions is:

$$\int_{T(a)}^{T(b)} \frac{1}{k(T)} dT = \frac{Q_{Vj}}{4} [(a)^2 - (b)^2] + C_1 \ln \left[\frac{a}{b} \right] \quad (W/cm) \quad (4-12)$$

where a,b = node outer, inner radius and

$$C_1 = b A_{Aj} - \frac{(b)^2}{2} Q_{Vj}$$

The temperature- and density-dependent thermal conductivity of the mixed-oxide fuel is given by Equation 4-10.

The volumetric heat generation rate in each radial node is determined by assuming the specific power uniform over the axial node.

$$Q_{Vj} = \frac{QL}{\pi(RFS)^2 (FDPN)} * \rho_j \left(\frac{W}{cm^3} \right) \quad (4-13)$$

The fuel density for each radial node, ρ_j , is input to the subroutine. The heat flux (W/cm^2) at the inner node interface is determined by subtracting the power generated in the radial nodes up to and including the latest from the total power being generated at that axial node:

$$Q'_{Aj} = \frac{1}{2\pi R_{j,inner}} QL - \sum_{j=1}^{50} Q'_{Vj} [(R'_{j,outer})^2 - (R'_{j,inner})^2] \quad (4-14)$$

The temperature at the inner node interface is then determined by a numerical solution of Equations 4-10 and 4-12.

4.5 FUEL REGION VOID AVAILABLE TO ACCOMMODATE SWELLING FUEL (IN SUBROUTINE FSTART)

After the call of FTEMP4, control returns to FSTART to calculate available void. The volume within the cladding includes several types of void, each available to accommodate fission products and thus regulate the onset of fuel-cladding mechanical interaction. Three void types are considered in BEHAVE-I: (1) void in the radial cracks; (2) the central void; and (3) distributed porosity in all fuel operating above 1300°C.

Additional fuel region void becomes available as the cladding swells during irradiation. The equations shown are applicable to each of the K axial nodes and the total volume within the cladding is equal to $\sum_K (CRV + CVV + PHV)_K$.

4.5.1 Crack Volume Fraction

The as-fabricated void between the fuel and cladding does not remain a circumferential gap at power. It is assumed that the fuel pieces formed by cracking during startup move radially outward and close the gap. The void that would have formed the gap at power is transformed into radial crack volume. It is assumed that all of the hot gap void

(now radial crack void) is available to accommodate swelling fuel. This volume fraction (CRV) is computed as the difference between the volume available within the expanded cladding (CIVH) and the volume of heated fuel (FVH), with

$$\begin{aligned} \text{CIVH} &= \text{hot cladding inside volume} \\ &= \frac{\pi}{4} (\text{CIDN})^2 \ell \left[1 + \left(\frac{\Delta \ell}{\ell} \right)_c \right]^3 \end{aligned}$$

where

$$\begin{aligned} \text{CIDN} &= \text{cold cladding i.d. (in.)}, \\ \ell &= \text{cold axial node length (in.)}, \text{ and} \\ \left(\frac{\Delta \ell}{\ell} \right)_c &= \text{cladding linear thermal expansion (in./in.)}. \end{aligned}$$

FVH = hot fuel volume,

$$= \sum_{i=1}^N (\text{FVH})_i$$

where

$$\begin{aligned} (\text{FVH})_i &= \text{hot volume of the } i^{\text{th}} \text{ radial fuel node} \\ &= \pi (r_o^2 - r_i^2) \ell \left[1 + \left(\frac{\Delta \ell}{\ell} \right)_F \right]^2 \left[1 + \left(\frac{\Delta \ell}{\ell} \right)_c \right] \end{aligned}$$

r_o, r_i = node outer, inner radius (in.), and

$$\left(\frac{\Delta \ell}{\ell} \right)_F = \text{fuel linear thermal expansion at the node arithmetic mean temperature (in./in.)}.$$

The crack volume fraction is then

$$\begin{aligned} \text{CRV} &= \text{crack volume fraction,} \\ &= \text{hot gap volume fraction} \\ &= (\text{CIVH} - \text{FVH})/\text{CIVH}. \end{aligned}$$

(4-15)

Note that the fuel axial expansion is assumed to be determined by the cladding axial expansion and all effects introduced by this restraint are neglected.

4.5.2 Central Void Volume Fraction

It is assumed that all of the central void is available to accommodate the swelling fuel.

$$\begin{aligned} \text{CVV} &= \text{central void volume fraction} \\ &= \pi (\text{RCV})^2 \ell / \text{CIVH} \end{aligned}$$

(4-16)

where

$$\text{RCV} = \text{central void radius (in.)}.$$

4.5.3 Available Fuel Porosity Volume Fraction

Because of the lack of plasticity in the colder regions of the fuel, there may be less utilization of the distributed porosity in accommodating fission products. A variable, FAVBPT, is provided (input in FSTART) which enables the desired portion of the porosity below the 1300°C radial isotherm (radius = RFP) to be made available to accommodate swelling. This isotherm is obtained from the previously calculated (FTEMP4) fuel radial temperature profile. Account is taken of the densification and corresponding radii calculated (RESTRU) for the equiaxed and columnar grain growth regions. The total available fuel porosity volume fraction is then

$$\begin{aligned}
 PHV = (\pi \ell / CIVH) \{ & [(RFS)^2 - (RFP)^2] * FAVBPT * (1 - FDPN) \\
 & + [(RFP)^2 - (REG)^2] * (1 - FDPN) \\
 & + [(REG)^2 - (RCG)^2] * 0.03 \\
 & + [RCG)^2 - (RCV)^2] * 0.01 \}
 \end{aligned}
 \tag{4-17}$$

The variable definitions are found in Subsection 4.3.

If called for by input option (NPD = -1), the radial temperature profile is printed for each axial node. If NPD = 0 and NPS = -1, a summary for each node is printed containing the strain at first startup, the dimensions of the restructured fuel, and the fuel surface and center void temperatures (Subsection 5.2.1).

4.6 STEADY-STATE OPERATION (SUBROUTINE STEADY)

4.6.1 Description of Calculations

The STEADY subroutine calculates the behavior of the fuel element during periods of steady power operation. The flow chart and the input operating conditions for the STEADY regime are shown in Figure 4-5 and Table 4-3, respectively.

The input time at steady power (DTMT) is divided into one-day subtime intervals. Within the subtime interval, each mechanism is assumed to act independently. A mechanism loading the cladding at a strain rate of $\sim 10^{-5}$ in./in.-hr would produce $\sim 0.02\%$ strain during this interval. The following sequence is repeated for each subtime interval; where specific subroutines are involved, detail is given elsewhere (the results of each step are stored in the ACCUMULATOR):

1. Subroutine CTHICK is called to calculate the change in effective cladding thickness during the interval due to corrosion in the flowing sodium at the outside surface and attack by fuel/fission products at the inside surface. The effective cladding thickness is defined as the thickness of load bearing metal, in contrast to the geometric cladding thickness which determines the cladding dimensions.
2. The burnup increment for the subtime interval (Δt) is calculated at each of the K=1, MZ axial nodes:

$$\Delta B(K) = \frac{7.343 \times 10^{-3} * QL(K) * \Delta t \left(\frac{MWd}{kgm} \right)}{(CIDN)^2 (FSDN) * (FTD)}
 \tag{4-18}$$

Then,

$$\begin{aligned}
 B_j(K) &= \text{burnup at end of } j^{\text{th}} \text{ time interval} \\
 &= B_{j-1}(K) + \Delta B(K).
 \end{aligned}$$

The linear power (QL) in Equation 4-18 is recomputed each subtime interval assuming a linear decrease from SVAL to EVAL (Table 4-3). This permits a simulation of fissile burnout effects.

3. The change in damage fluence during the subtime interval is calculated at each axial node. Damage fluence is defined from neutrons with energy greater than 0.1 MeV.

$$\Delta F(K) = FN(K) * FDF * FGT(K) * \Delta t * 8.64 \times 10^4 \text{ (n/cm}^2\text{)} \text{ (E} > 0.1 \text{ MeV)}
 \tag{4-19}$$

then,

$$\begin{aligned}
 F_j(K) &= \text{damage fluence at the end of the } j^{\text{th}} \text{ time interval} \\
 &= F_{j-1}(K) + \Delta F(K) \text{ (E} > 0.1 \text{ MeV)}
 \end{aligned}$$

Table 4-3
INPUT TO STEADY

NAMELIST STEINP

DTMT	=	length of steady-state power operation (days)
STIME	=	accumulated time of exposure at start of steady-state operation (days)
SVAL	=	peak linear power at time = STIME (kW/ft)
ETIME	=	STIME + DTMT (days)
EVAL	=	peak linear power at time = ETIME (kW/ft)
TSI	=	sodium inlet temperature ($^{\circ}$ F)
SV	=	sodium velocity (ft/sec)
SOX	=	sodium oxygen content (ppm)
TSO	=	sodium outlet temperature ($^{\circ}$ F)
SP	=	mean sodium pressure (psia)
CATN	=	allowance for cladding i.d. attack (in.)
CCTN	=	allowance for cladding o.d. corrosion (in.)
FN(MZ)	=	total neutron flux (10^{15} n/cm ²)
FDF	=	radial fast flux fraction ($E > 0.1$ MeV)
FGT(MZ)	=	axial fast flux fractions ($E > 0.1$ MeV)
FLFU	=	EBR-II fluence correction factor for metal swelling equations (nominally 1.2, but will eliminate swelling if $\geq 10^6$)
A	=	exponential constant for strain enhancement of metal swelling (= 80 for SS316, = 200 for SS347)
QL(MZ)	=	See Table 4-2
HG(MZ)	=	See Table 4-2 (HG is included in STEINP to permit simulation of changes with burnup)
TCA(MZ)	=	See Table 4-2
SR	=	fuel volumetric swelling rate (% per 10^{20} fissions/cc)
GASBOT	=	range: $0 \leq \text{GASBOT} \leq 1$ If $\neq 0$, central void is a closed pressurized volume. If = 0, fission gases communicate with plenum.
NPD	=	See Table 4-2
NPS	=	See Table 4-2
ISIMS	=	enhanced metal swelling option If > 0 , swelling is increased using input value of A (Equation 2.1)
NQLADJ	=	if > 0 , QL array is not changed when subroutine STEADY is called by MAIN
NFNADJ	=	if > 0 , FN array is not changed when subroutine STEADY is called by MAIN
ICCOPT	=	option for clad creep equation 1 = irradiation creep WADCO SS316CW thermal creep WADCO SS316 annealed 2 = irradiation creep WADCO SS316 annealed thermal creep WADCO SS316 annealed 3 = irradiation creep GE SS304/321 annealed thermal creep GE SS304/321 annealed 4 = irradiation creep GE SS316 CW thermal creep GE SS316 annealed
ICSOPT	=	option for cladding swelling equation 1 = WADCO SS316 CS 2 = GE SS321

Table 4-3 (Continued)

- 3 = GE (Sandusky) SS316 Annealed
 4 = GE (Oldberg) SS316/SS304 Annealed
 5 = GE (Appleby) SS304 annealed
 6 = WADCO SS316 annealed ("1.71" Equation)
 7 = GE (Stephen) 347 annealed
 8 = WADCO SS316 CW
 9 = WADCO SS316 Annealed ("θ" Equation)
- IETOPT = effective thickness option
 1, 2, and 3 = uniform thickness reduction at start-of-life from input CATN, CCTN
 2 = calculate o.d. corrosion and i.d. attack from input SV, SOX
 3 = calculate o.d. corrosion and i.d. attack from input constants (AC ≠ 0 and THKINP namelist provided)
- ITCOPT = cladding temperature option (See Table 4-2)
4. Subroutine CSWEL is called to calculate the diametral growth at each node due to cladding swelling during the subtime interval. The void available within the fuel region to accommodate fuel swelling is recalculated in CSWEL to account for the cladding growth.
 5. Subroutine GASP is called to calculate the pressure of the gas inside the fuel pin at the end of the subtime interval. The pressure is assumed to be determined by the plenum temperature and volume unless the gas-bottle option is chosen. In this case, fission products accumulate in the fuel central void, and the pressure is determined from the void surface temperature and void volume.
 6. Subroutine CCREEP is called by GASP to calculate the effective hoop stress and the cladding strain due to creep under the gas pressure load during the subtime interval at each axial node. The pin internal pressure and the effective cladding thickness determined in CTHICK are used in this calculation. A change in cladding dimensions is not calculated at this point. The strain values are stored in the ACCUMULATOR for later comparison with the cladding strain produced by fuel-cladding mechanical interaction.
 7. Subroutine FCMINT is called to calculate the cladding strain produced during the interval as a result of the fuel-cladding mechanical interaction, which occurs after all the void available in the fuel region has been closed. As in the cladding creep calculation, a cladding dimensional change reflecting this strain is not calculated, pending a comparison with the gas pressure creep strain.
 8. A comparison is made, at each axial node, of the cladding strains which are calculated to have been produced by gas pressure creep and by fuel-cladding mechanical interaction, had these phenomena been acting independently during the interval. A creep strain larger than an interaction strain indicates that fuel-cladding mechanical interaction did not occur, and the new cladding diameter is calculated from the creep strain.
- For this case, the fuel region available void is adjusted to account for the cladding growth. When the mechanical interaction strain exceeds the creep strain, it is assumed that any creep enhancement of the interaction is negligible and the diameter increase is calculated from the mechanical interaction strain alone.
9. If called for by input option (NPD = -1), the status of the fuel element at the end of each subtime interval is printed. If NPD = 0 and NDS = -1, output occurs only after the total DTMT days of operation have been modeled.

Figure 4-5. Subroutine STEADY Flow Chart

4.6.2 Material Properties

Several material properties used throughout BEHAVE-I are included in FUNCTION form:

1. Thermal Conductivity of Type-316 Stainless Steel Cladding

(a) FUNCTION CKE(T)

$$CKE = 7.5 + 0.004583T \quad (\text{Btu/hr-ft-}^\circ\text{F})$$

T(=) $^\circ\text{F}$

(b) FUNCTION CKM(T)

$$CKM = 0.01731 [7.5 + 0.004583 (T \cdot 1.8 + 32)] \quad (\text{w/cm-}^\circ\text{C})$$

T(=) $^\circ\text{C}$

These equations are utilized for all the 300 series stainless steels.

2. Thermal Expansion of (U-0.2 Pu)O₂ Fuel

FUNCTION FTE(T)

$$FTE = 0.01 [0.0436 + 6.793 \cdot 10^{-4} \cdot T + 2.92 \cdot 10^{-7} \cdot T^2]$$

FTE (=) fraction $\frac{\Delta L}{L}$ T(=) $^\circ\text{C}$

The thermal expansion for (U-0.25 Pu)O₂ is shown to differ from the above by less than 0.5%.¹⁷ The equation is valid for $25^\circ < T < 2250^\circ\text{C}$, but with lesser confidence at the high temperature end of this range.

3. Thermal Expansion of Type-316 Stainless Steel Cladding

FUNCTION CTEA(T)

$$CTEA = 8.75 \times 10^{-6} + 0.125 \cdot 10^{-8} \cdot T \quad (\text{in./in.-}^\circ\text{F})$$

T(=) $^\circ\text{F}$

This equation gives the mean coefficient of linear thermal expansion from 32°F to T, and is utilized for all the 300 series stainless steels.²²

4.6.3 Cladding Radial Temperature Profile (Subroutine CATEMP)

Subroutine CATEMP calculates at each axial node the cladding mean temperature (TCA) and/or the temperature drop across the cladding (CDT) according to the ITCOPT input option. The options cover the cases typically encountered where the axial linear power and one of the following are known: (1) the sodium inlet and outlet temperatures, (2) the axial sodium temperature profile, or (3) the axial cladding mean temperature profile.

ITCOPT = 1 calculates TCA and CDT from sodium temperatures, input TSI and TSO

ITCOPT = 2 calculates TCA and CDT from input TS

ITCOPT = 3 calculates CDT from input TCA

ITCOPT = 4 CATEMP call is bypassed

ITCOPT = 1

The sodium inlet (TSI) and outlet (TSO) temperatures are used to determine the sodium flow rate (SW) assuming the specific heat is constant ($SCP = 0.302 \text{ Btu/lb}_m\text{-}^\circ\text{F}$)

$$SW = \frac{3412}{SCP \cdot (TSO - TSI)} \sum_{i=1}^{MZ} \left(QL \cdot \frac{\ell}{12} \right)_i \quad (\text{lb/hr}). \quad (4-20)$$

The sodium temperature at the k^{th} axial node is:

$$TS(K) = TS(K-1) + 142.17 \cdot \ell [QL(K-1) + QL(K)] / (SW \cdot SCP) \quad (4-21)$$

and, assuming a constant sodium-cladding heat transfer coefficient ($SH = 20,000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$), the cladding surface temperature (TCS) at any axial node is calculated from

$$TCS(K) = TS(K) + 1.30 \times 10^4 * QL(K)/(CODN * SH). \quad (4-22)$$

The temperature drop throughout the cladding (CDT) is

$$CDT(K) = 543.04 * (QL(K)/CKE(TCS)) \ln (CODN/CIDN). \quad (4-23)$$

The average cladding temperature is calculated from the results of Equations 4-22 and 4-23

$$TCA(K) = TCS(K) + CDT(K)/2. \quad (4-24)$$

ITCOPT = 2

The axial sodium temperature array is specified input, and only Equations 4-22, 4-23 and 4-24 are used.

ITCOPT = 3

The axial cladding mean temperature array is input, and Equation 4-24 is used to calculate the temperature drop through the cladding wall.

4.7 EFFECTIVE CLADDING THICKNESS (SUBROUTINE CTHICK)

The CTHICK subroutine calculates at each axial node the change in effective cladding thickness during the subtime interval. The effective thickness is defined as the thickness of load bearing metal, and is a function of wear during fabrication (scratches, etc.), corrosion of the outside surface in flowing sodium, and attack by fuel and fission products at the inside surface. Three options are available.

OPTION 1 (IETOPT = 1)

A uniform reduction in cladding o.d. is made at the start of life using the largest of the input values of cladding o.d. wear allowance (WOC) and cladding o.d. corrosion allowance (CCTN). A corresponding uniform increase in the i.d. is calculated using the largest of the input values WIC and CATN. With this option, the only computed adjustment to the nominal cladding thickness occurs at the beginning of the first steady-state period of operation.

OPTION 2 (IETOPT = 2)

A reduction of effective thickness is calculated as a function of time in-pile. The correlation for corrosion of the outside cladding surface at temperature TCS in flowing (velocity is SV ft/sec) sodium containing SOX ppm of oxygen is^{1,8}

$$R = 5.906 \times 10^{-6} e^{\left[\sum_{i=1}^7 A_i \right]} \text{ (in./year)} \quad (4-25)$$

with

$$A_1 = 14.15$$

$$A_2 = 0.435 \ln (SV)$$

$$A_6 = 1.156 \ln (SOX)$$

$$A_7 = \frac{-28,827}{(TCS + 460)}$$

$$A_3 = A_4 = A_5 = 0 \text{ at present. The inches of metal lost during the subtime interval } \Delta t \text{ is then } R * \Delta t / 365.$$

This correlation predicts uniform corrosion over the surface, ignoring localized effects. The accumulated metal loss is compared with the outside surface wear allowance (WOC), and the effective o.d. at the end of the time increment is decreased when the accumulated loss exceeds WOC.

The depth of attack by fuel and fission products at the cladding inside surface at temperature TCI is calculated from¹⁹

$$P = C_1 + C_2 * (TCI) + C_3 * (TCI)^2 \quad (4-26)$$

with

$$\begin{aligned} C_1 &= 2.35 \times 10^{-2} \\ C_2 &= -5.03 \times 10^{-5} \\ C_3 &= 2.69 \times 10^{-8} \end{aligned}$$

This correlation is used to predict penetration only when the cladding first reached temperature since it is independent of exposure time. The penetration is compared with the inside surface wear allowance (WIC), and the effective i.d. is then increased using the larger of P and WIC.

OPTION 3 (IETOPT = 3)

Similar to OPTION 2 except that the various rate constants are provided by the user. If this option is chosen, AC in NAMELIST MAINP must be non-zero and a NAMELIST THKINP (Table 4-4) must follow the first STEINP NAMELIST. The assumed form for the o.d. corrosion is

$$C1 * \exp (C2/T) \quad (4-27)$$

where C1 has units of in. per year, and T is in °R.

The cladding i.d. attack is computed from

$$C3 + C4 * T + C5 * T^2 + C6 * \exp (C7/T) \quad (4-28)$$

The units of C3 are also inches/year and temperature is in °R. All constants are input in NAMELIST THKINP.

Table 4-4

INPUT TO CTHICK (this NAMELIST is optional (IETOPT = 3). It is required only if AC ≠ 0 in NAMELIST MAINP (Table 4-1).

NAMELIST THKINP

C1, C2 Defined by Equation 4-27
C3 through C7 Defined by Equation 4-28

4.8 NEUTRON-INDUCED CLADDING SWELLING (SUBROUTINE CSWEL)

Subroutine CSWEL calculates at each axial node the change in cladding dimensions and the change in the void available within the fuel region to accommodate fuel swelling due to cladding swelling during each subtime interval. The current cladding swelling correlation options are listed in Table 4-5.

The procedure is to first calculate the total volumetric swelling of the cladding to the end of the mth time interval (CSVFIN) according to the ICSOPT option selected. A similar calculation is made using the fluence at the beginning of the interval (CSVINT). The change in cladding diametral strain is then calculated assuming the volumetric cladding swelling to be isotropic

$$\Delta \epsilon_m = \frac{1}{3} (CSVFIN - CSVINT) * SIMSC. \quad (4-29)$$

The variable SIMSC in Equation 4-29 is equal to one if the strain enhancement of swelling option is not in effect (i.e., ISIMS = 0), and is calculated using the input constant A (Table 4-3) in

$$\text{SIMSC} = 1 + 100 * (1 - \exp(\Delta\epsilon_c * A)). \quad (4-30)$$

This is Equation 2-1, and a discussion of the terms is found in Subsection 2.1.

Finally, the increase in fuel region volume produced by the swelling cladding is distributed between the crack and the central void volumes. The actual distribution of this void is uncertain, and at present an equal distribution is assumed

$$V_{CR} = V_{CR} + \frac{1}{2} (3\Delta C_m)$$

$$V_{CV} = V_{CV} + \frac{1}{2} (3\Delta C_m)$$

Care must be exercised when analyzing EBR-II irradiations to take into account the discrepancy between the flux values reported by the EBR-II project and the values used in deriving the cladding swelling correlations. This difficulty is handled in BEHAVE-I by including an input factor FLFU (Table 4-3) to modify the fluence in the correlation to be consistent with the derivation. When EBR-II flux values are used, this factor is greater than unity (usually 1.2). When flux values other than reported by the EBR-II project are used, FLFU should be set to unity. The situation of no metal swelling can be achieved simply by setting FLFU to a very large value, since the calculated fluence (F) is divided by this parameter before $\Delta V/V$ is computed.

4.9 FISSION GAS PRESSURE (SUBROUTINE GASP)

This subroutine calculates the pressure of the total accumulated gas inside the fuel element at the end of each subtime increment. There are two options based upon the input value of GASBOT (Table 4-3).

OPTION 1 (GASBOT = 0)

The calculation of the pin internal pressure is based upon the sum of the moles of

- (1) fabrication fill gas,
- (2) released adsorbed gas (e.g., water vapor from fuel), and
- (3) released noble fission gas, assuming that the gas is contained in the plenum at the sodium outlet temperature.

The following fractional release (FR) as a function of burnup is used for noble gases xenon and krypton:²⁷

$$\begin{aligned} \text{FR} &= 0.2 + 0.0117 * B & B < 35.8 \text{ MWd/KgM} \\ \text{FR} &= 0.4 + 0.00611 * B & B \geq 35.8 \text{ MWd/KgM} \end{aligned} \quad (4-31)$$

with the restriction that $\text{FR} \leq 1.0$.

4.9.1 Fabrication Fill Gas

For thermal bonding purposes, fuel pins are typically filled with one atmosphere of helium prior to making the final closure weld. The total moles of fill gas (GI) is calculated for PFAB = 1 atm. and $T = 298^\circ\text{K}$ from

$$\text{GI} = \text{PFAB} * (\text{VVF} + \text{VVB} + \text{PLV}) / (82.06 * 298), \quad (4-32)$$

where

- VVF = effective release void volume in fueled length,
- VVB = effective release void volume in blanket length, and
- PLV = plenum volume.

Table 4-5

CSWEL CLADDING SWELLING CORRELATION OPTIONS

ICSOPT = 1

SS316 COLD WORKED

WADCO

(Ref. 20)

$$\% \frac{\Delta V}{V} = 1.01 \times 10^{-36} F^{1.69} \left[\exp\left(\frac{-7800}{RT}\right) - 5.48 \times 10^3 \exp\left(\frac{-25,300}{RT}\right) \right]$$

$$F = n/\text{cm}^2 \quad (E > 0.1 \text{ MeV})$$

$$T = ^\circ\text{K}$$

$$R = 1.987$$

ICSOPT = 2

SS321/SS316 COLD WORKED

GE (APPLEBY)

(Ref. 21)

$$\% \frac{\Delta V}{V} = 8.9 \times 10^{-2} F^{1.5} e \left[-3.14 + \left(\frac{5.04 \times 10^3}{T} \right) - \left(\frac{1.66 \times 10^6}{T^2} \right) \right]$$

$$F = 10^{22} \text{ n/cm}^2 \quad (E > 0.1 \text{ MeV})$$

$$T = ^\circ\text{C}$$

ICSOPT = 3

SS316 ANNEALED

GE (COMPPELLI)

(Ref. 22)

$$\% \frac{\Delta V}{V} = 3.14 \times 10^{-4} F^{1.81} [(T-360)^{0.683} + (775 - T)^{0.577}]$$

$$F = 10^{22} \text{ n/cm}^2 \quad (E > 0.1 \text{ MeV})$$

$$T = ^\circ\text{C}$$

ICSOPT = 4

SS304/SS316 ANNEALED

GE (OLDBERG)

(Ref. 23)

$$\% \frac{\Delta \rho}{\rho} = -6.7 \times 10^{-5} F^2 e \left[\frac{15,670}{T} - \left(\frac{2710}{T} \right)^2 \right]$$

$$\% \frac{\Delta V}{V} = \frac{-(\Delta \rho / \rho)}{1 + (\Delta \rho / \rho)}$$

$$F = 10^{22} \text{ n/cm}^2 \quad (E > 0.1 \text{ MeV})$$

$$T = ^\circ\text{K}$$

Table 4-5 (Continued)

ICSOPT = 5

SS304 ANNEALED

GE (APPLEBY)

(Ref. 21)

$$\% \frac{\Delta V}{V} = F^{1.57} e \left[-35.01 + \frac{2.968 \times 10^4}{T} - \frac{6.567 \times 10^6}{T^2} \right]$$

$$F = 10^{22} \text{ n/cm}^2 \text{ (E > 0.1 MeV)}$$

$$T = ^\circ\text{C}$$

ICSOPT = 6

SS316 ANNEALED

WADCO

(Ref. 24)

$$\% \frac{\Delta V}{V} = 10^{-48.31} F^{1.71} 10 \left[\frac{1.549 \times 10^4}{T} - \frac{5.988 \times 10^6}{T^2} \right]$$

$$F = \text{n/cm}^2 \text{ (E > 0.1 MeV)}$$

$$T = ^\circ\text{K}$$

ICSOPT = 7

SS347 ANNEALED

Derived from the WADCO SS316 annealed correlation by adjusting the constant to a least squares fit of the F2G immersion density data.

$$\% \frac{\Delta V}{V} = 10^{-48.13} F^{1.71} 10 \left[\frac{1.549 \times 10^4}{T} - \frac{5.988 \times 10^6}{T^2} \right]$$

$$F = \text{n/cm}^2 \text{ (E > 0.1 MeV)}$$

$$T = ^\circ\text{K}$$

ICSOPT = 8

SS316 COLD WORKED

WADCO

(Ref. 25)

$$\% \frac{\Delta V}{V} = 9 \times 10^{-35} F^{1.5} [4.028 - 3.712 \times 10^{-2} T + 1.0145 \times 10^{-4} T^2 - 7.879 \times 10^{-8} T^3]$$

$$F = \text{n/cm}^2 \text{ (E > 0.1 MeV)}$$

$$T = ^\circ\text{C}$$

Table 4-5 (Continued)

ICSOPT = 9

SS316 ANNEALED

WADCO

(Ref. 26)

$$\% \frac{\Delta V}{V} = 10^{-10} (T - 40) F^A e^B$$

$$A = 2.05 - 27/(T-623) + 78/(-623)^2$$

$$B = 32.6 - 5100/T - 0.015T$$

$$F = 10^{22} \text{ n/cm}^2 \text{ (E > 0.1 MeV)}$$

$$T = ^\circ\text{K}$$

4.9.2 Adsorbed Gas

Present fuel processing techniques do not remove all of the adsorbed contamination from the mixed oxide. To account for this gas release, a standard level of contamination of $100 \mu\text{l}$ per gram of fuel is assumed. The number of moles of adsorbed gas (GA) is then

$$GA = 100 * (FMT + BMT) * (270/238)/22.4 \times 10^3 \quad (4-33)$$

where

FMT = total grams of metal in the fuel, and

BMT = total grams of metal in the blanket pellets.

4.9.3 Noble Fission Gases

Xenon and krypton are generated at the combined rate of 0.26 atoms/fission, and the fraction of these gases that is released to the fuel pin plenum (FR) is given by Equation 4-31. Based upon 3.1×10^{16} fissions/mW-sec, the moles of noble gas released (GN) from the K^{th} axial node is

$$GN = 11.56 \times 10^{-5} * FM(K) * FR(K) * B(K) \quad (4-34)$$

where

FM = mass of metal fuel in K^{th} axial node (Kg), and

B = burnup (MWd/KgM)

The pin internal pressure (PGI) is calculated assuming all the gas is in the plenum and that the entire plenum volume (PLV) is at the sodium outlet temperature (TSO). Thus,

$$PGI = 1.206 \times 10^3 * TSO * (GI + GA + GN)/PLV \text{ (psi)} \quad (4-35)$$

Subroutine CCREEP (Subsection 4.10) is called by GASP at this point; control is then returned to subroutine STEADY with no further computation in GASP.

OPTION 2 ($0 < GASBOT \leq 1.0$)

When the gas-bottle option (Subsection 2.2) is selected, the calculations in Subsections 4.9.1 and 4.9.2 are included as shown. The gas-bottle effect is taken to be operative only over the DTMT days of the designated steady-state period of operation. Equation 4-34 is modified accordingly by replacing the total burnup, B, with the

incremental burnup during that period. In addition, a similar equation is included for cesium, for the reasons explained in Subsection 2.2. A generation of 0.184 cesium atoms/fission is employed, and the release fraction computed for the noble gases (Equation 4-31) is assumed to apply to cesium as well, since most of its precursors are isotopes of xenon.

Subroutine FTEM4 (Subsection 4.4) is called by GASP to compute the central void temperature at each axial node. The internal pin pressure (PGI) is calculated from P_g of Equation 2-2 as

$$PGI = P_g * GASBOT \quad (4-36)$$

GASBOT is included as a multiplier to make it possible to account for the fact that the sealed central void may not have existed for the totality of the assumed DTMT days. If the gas-bottle effect is not applicable to subsequent steady-state periods of operation, set GASBOT = 0 and the plenum volume and temperature again determine the pin internal pressure according to Equation 4-35.

When the print option NPD = -1, a summary output is printed from GASP for each subtime interval when GASBOT $\neq$ 0. This summary includes the:

- (1) central void radius and temperature for each axial node,
- (2) accumulative moles of cesium and noble gas released into the central void,
- (3) accumulative burnup for period of central void containment,
- (4) pressure in central void based upon accumulative moles of fission products, and
- (5) prevailing subtime interval central void size and temperature.

When this print option is not chosen, only the conditions at the end of the total steady-state period are output in the standard summary format.

4.10 THERMAL AND IRRADIATION CLADDING CREEP (SUBROUTINE CCREEP)

The CCREEP subroutine is called by GASP and calculates, at each axial node, the cladding diametral strain due to cladding creep under the net internal gas pressure load for each subtime increment. Both thermal- and irradiation-induced creep are included. There are four correlation options, as listed in Table 4-5.

The net gas pressure (PN) is calculated by difference using the internal pressure, PGI, from GASP and the input sodium coolant pressure SP (Table 4-3). If $SP > PGI$, the difference is treated as zero, since creep-down of the cladding onto the fuel is not considered. The effective stress (SGE) is then (Equation 2-3)

$$SGE = (\sqrt{3/2}) * PN * RAD/CHE, \quad (4-37)$$

where

RAD = the average clad radius when GASBOT = 0,

or = the fuel plasticity radius when GASBOT $\neq$ 0.

The GASBOT option is discussed in Subsection 4.9. The effective creep rates for thermal creep (EDTEL) and for irradiation-induced creep (EDIEL) are computed using the equations selected by the ICCOPT option (Tables 4-3 and 4-5). The diametral creep strains are determined for the subtime interval Δt from these creep rates by Equation 4-38:

$$FCX = 20.7846 * EDXEL * \Delta t \quad (4-38)$$

where

X = I for irradiation creep, and

= T for thermal creep.

These diametral strains occurring during the Δt subtime increment are transferred to the ACCUMULATOR. They are compared in the STEADY subroutine with the diametral strain due to fuel-cladding mechanical interaction produced by fuel swelling during the same time increment (EFS from FCMINT; Subsection 4.11), in order to determine which phenomenon caused the increase in cladding diameter.

Table 4-6

CORRELATIONS FOR CLADDING CREEP UNDER GAS PRESSURE

ICCOPT = 1

SS316 COLD WORKED - WADCO

$$EDIEL = C_1 * C_2 * e [1.405 - 0.0027T] * FN * SGE * 3600 \text{ (in./in.-hr)}$$

$$EDTEL = e^{62.48} [\sinh (1.79 \times 10^{-5} * SGE)]^{8.23} e^{-58.840/T} \text{ (in./in.-hr)}$$

C_1 = correction factor for nonconservative-volume creep

= 1.0 at present

$C_2 = 1.93 \times 10^{-30}$, and

T = average cladding temperature ($^{\circ}$ K).

The equation for thermal induced creep is for unirradiated SS316 annealed and is used at present as a conservative estimate for SS316 cold worked.

ICCOPT = 2

SS316 ANNEALED - WADCO

same as option 1 with $C_2 = 1.87 \times 10^{-30}$

ICCOPT = 3

SS321 ANNEALED

The same form of the equations as Option 1 has been programmed, but the constants have not as yet been determined.

ICCOPT = 4

The irradiation-induced creep strain rate equation for SS316 CW is, for temperatures less than 1100° F

$$EDIEL = 2.2 \times 10^{-27} * SGE (3.6 FN) \exp (1.41 - 0.0027T) \text{ (in./in.-hr)}$$

where

FN = fast flux (n/cm²-sec for $E > 0.1$ MeV) and T is the cladding average temperature in $^{\circ}$ K.

The high temperature creep strain rate is for SS316 annealed up to 1400° F and is given by

$$EDTEL = 7.75 \times 10^{21} [\sinh (4.31 \times 10^{-5} SGE)]^{6.16} * \exp (-55359/T)$$

$$[1 - \exp (-0.105 - (10^{22}/FN * \Delta t)^{1.1})] \text{ (in./in.-hr)}$$

4.11 FUEL-CLADDING MECHANICAL INTERACTION (SUBROUTINE FCMINT)

The FCMINT subroutine calculates the cladding diametral strain due to fuel-cladding interaction produced by fuel swelling. The basis of the model has been reported previously.^{28,29} The void in the fuel region (in the form of cracks, fuel porosity, and the central void) that is available to accommodate fuel swelling is evaluated at each time step to determine when void closure occurs. Until all the available void is closed, the fuel is assumed to be weak relative to the cladding and, therefore, to deform inward closing the void rather than outward to produce fuel-cladding mechanical

interaction. The total available void is different at each axial node. The axial movement of fission products is simulated by stipulating that no fuel-cladding mechanical interaction can occur at a given axial location if void is available. Interaction is thus calculated to occur only after the entire fuel element void has been utilized. This is shown diagrammatically in Figure 4-6 for a typical axial section.

In calculating the fuel-cladding mechanical interaction strain it is noted that, owing to high thermally induced creep rates, significant shear stresses cannot be supported by the hot inner region of the fuel; accordingly, stresses are hydrostatic and the pressure is essentially constant in this region. At the plastic radius, fuel behavior changes abruptly from a relatively nonviscous fluid to an almost nondeformable solid. It is assumed that fuel external to the plastic radius is cracked in the radial direction and therefore is unable to support a hoop stress. Thus, a force balance relates the hydrostatic pressure inside the plastic radius to the hoop stress in the cladding, giving

$$\sigma = P_H R_p / t \quad (4-39)$$

where

- σ = cladding hoop stress (psi),
- P_H = hydrostatic pressure in plastic fuel core (psi),
- R_p = radius of plastic fuel core (in.), and
- t = cladding thickness (in.).

The combination of Equation 4-39 with a stress-strain relationship of the form

$$\sigma = K \epsilon^n \quad (4-40)$$

where K is a constant and n is the strain hardening exponent (presently $n=0.10$), yields the following relation between strain and the plastic radius:

$$\epsilon = (P_H / Kt)^{1/n} R_p^{1/n}. \quad (4-41)$$

Assuming no axial fuel swelling, the mean strain is related to the total fission product (V_{FP}) and available void (V_A) volumes for MZ axial nodes by

$$\epsilon_m = \frac{1}{MZ} \sum_{i=1}^{MZ} \frac{1}{2} (V_{FP} - V_A)_i. \quad (4-42)$$

For a constant cladding thickness over the fueled length, the strain at each node is given from Equations 4-41 and 4-42 as

$$EFS = \epsilon_m \frac{R_p^{1/n}}{\bar{R}_p^{1/n}}$$

where $\bar{R}_p^{1/n}$ denotes the average over the axial nodes.

The precise value for the volumetric swelling rate is not known. By considering both the positive volume change resulting from the generation of solid fission products and the negative volume change due to the matrix destruction resulting from the fission process, Anselin¹¹ calculated that the swelling rate could range from 0.13 to 0.54% per 10^{20} fissions/cc. Marlowe¹⁰ has included fission gas re-solution as a factor in his analytical treatment of fission product noble gas (Xe and Kr) bubble kinetics; he calculates that the positive contribution of the fission gases to fuel swelling is 0.21 to 0.27% per 10^{20} fissions/cc. The effect of varying the total fuel volumetric swelling rate within the limits determined by summing the above solid and gaseous rates (0.34 to 0.81% per 10^{22} fissions/cc) is discussed in Subsection 3.9.

Figure 4-6. The BEHAVE-1 Model of Cladding Strain Due to Fuel Swelling

Control is returned to the STEADY subroutine where EFS is compared to the diametral strain due to creep. The larger of these strains is used to calculate the increase in cladding diameter (comment 8, Subsection 4.6).

4.12 POWER INCREASE EFFECT (SUBROUTINE INCRES)

Subroutine INCRES calculates, at each axial node, the cladding strain due to the fuel-cladding interference produced by differential thermal expansion during an increase in power following a period of steady power operation. The input operating conditions and flow chart for INCRES are given in Table 4-7 and Figure 4-8, respectively.

The model used in INCRES postulates the information, after some time at steady power, of a strong unbroken ring (SUR) of fuel. The ring is bounded on the inside by fuel that will flow plastically under the loads expected, and on the outside by fuel with many radial cracks running from the fuel surface to the ring, as shown in Figure 4-7. The location of this ring (i.e., the bounding radii) is a function of temperature.

The formation of the SUR can be pictured as occurring in three steps:³⁰

- (1) Radial cracks form during the rise to the steady-state power.
- (2) The cracks heal out to a "crack closure radius" by sintering under the load imposed by contact with the cladding. Fuel inside the crack radius is considered plastic and weak relative to the cladding.
- (3) The void from the crack radius outward begins to close during the subsequent steady power operation due to vapor transport, sputtering, etc. A continuous ring is thus formed in the fuel which is strong relative to the cladding.

During a power increase, cladding strain is produced by the differential thermal expansion between: (a) this ring and the fingers of fuel, and (b) the cladding. The details of these calculations are included in Figure 4-8. The magnitude of typical power increase strains is discussed in Subsection 3-10.

Table 4-7
INPUT TO INCRES

NAMelist PININP

EVALS	=	EVAL from preceding STEINP NAMelist (See Table 4-3.)
FRAINC	=	fraction of differential thermal expansion strain which is active in deforming the cladding (nominal value = 0.70)
SVALE	=	SVAL from following STEINP NAMelist (See Table 4-3.)

1. RADIAL CRACK FORMATION

2. HEALING TO CRACK RADIUS

3. CRACK FILL

4. ANALYTICAL MODEL

(CRACK SIZE IS EXAGGERATED TO ILLUSTRATE THIS CONDITION)

Figure 4-7. Formation of the Strong Unbroken Ring (SUR) of Fuel

Figure 4-8. Subroutine INCRES Flow Chart

5. INPUT/OUTPUT

5.1 INPUT

Input variables are read into BEHAVE-I in NAMELIST format.³¹ These reads occur as has been described in MAIN (Table 4-1), FSTART (Table 4-2), STEADY (Table 4-3), CTHICK (Table 4-4) and INCRES (Table 4-7). These tables are repeated in Table 5-1. Note that any variable not given as current input maintains its previous value; the encoding assumes an initialization of the core to all zeros. Typical input format is shown in Figures 3-1 and 5-1.

5.2 OUTPUT SUMMARY

With the output print options NPD = 0 and NPS = -1, the printouts received are as shown in Figures 5-2 through 5-7.

5.2.1 FSTART Output

The output shown in Figure 5-2 summarizes startup conditions and gives the strain at first startup (seventh column from left in the figure). The sodium temperature is not calculated when the cladding average temperature is input. The last column on the right of the figure is the diameter profile, which would be compared with profilometry if the irradiation were terminated immediately following the first rise to power.

A summary of the restructured fuel is given in Figure 5-3. The average total void initially available is found from columns 2, 3 and 4 to be 3.87% in this example. This available volume increases during irradiation due to cladding swelling; it is the instantaneous value of this volume which is compared to the increase in fuel volume due to fission product accumulation to calculate fuel-cladding mechanical interaction in the FCMINT Subroutine.

5.2.2 STEADY Output

The summary output is as shown in Figures 5-4 and 5-5. These data represent predicted conditions at the end of a total of 190.1 days of operation, following a steady-state period of 15 days. This output is printed for each separate JOP = 3 input condition.

For each of the MZ axial nodes, the linear power and cladding temperature from the last subtime interval (1 day) of the steady-state period are shown in Figure 5-4. The accumulated values of burnup, fluence, and metal swelling strain are tabulated; the final column indicates the calculated room temperature diametral profile prior to including the creep and fuel-cladding mechanical interaction strains.

A summary of the gas pressure calculations is shown in Figure 5-5; the thermal and irradiation creep strains are accumulative values. The lower table contains information on fuel-cladding mechanical interaction due to fuel swelling. If the NO = 0, YES = 1 matrix contains all ones, FCMINT has computed fuel-cladding mechanical interaction during the last time interval. The comparison of the volumes is also based upon the last time interval, but the swelling strain shown is the cumulative value for each node.

If the experiment had been terminated at this time, the last column gives the diameter profile for comparison with post-irradiation (room temperature) profilometry.

5.2.3 INCRES Output

Following the simulation of a power increase, the output shown in Figure 5-6 is obtained. The accumulative burnup is given for each node, along with the SUR temperature change, cladding strain due to this power increase, the sum of cladding strain from this and previous excursions, and the cladding o.d. (reflected to room temperature) that is predicted at the termination of the event.

5.2.4 Summary Output

Before the job is terminated, MAIN prints the summary shown in Figure 5-7. Cards are punched for plotting purposes at this time; the lower table of values is punched, preceded by a card giving the PINID and the case number (ICASE). These values were input according to Table 4-1.

5.3 OUTPUT - DETAILED

If $NPD = -1$ in STAINP NAMELIST (Table 4-2), then, in addition to Figures 5-2 and 5-3, a fuel temperature profile printout similar to that shown in Figure 5-8 is obtained for each axial node. Thus, by using a number of sequential STAINP inputs ($JOP = 2$ is repeated for the desired number of times), parametric studies can easily be made to investigate restructuring, center temperature, strain at first startup, etc.

If the only detailed output desired is the above, NPD is subsequently set equal to zero in the first STEINP NAMELIST. If $NPD = -1$ remains when STEADY is called, additional printouts are obtained. Output corresponding to Figures 5-4 and 5-5 will occur for every one-day subtime interval during each steady state if $NPD = -1$. This allows the onset of fuel-cladding mechanical interaction to be closely followed; once the steady-state period in which interaction begins is ascertained, use $NPD = 0$ and set $NPD = -1$ in that steady only for detailed output.

When the gas-bottle option is chosen ($GASBOT \neq 0$), if $NPD = -1$, the output shown in Figure 5-9 is printed for one-day intervals. This summarizes the parameter values used to calculate the central void gas pressure, and allows the user to follow the buildup during the steady-state period of time. If this option is not chosen, only the final pressure is printed as in Figure 5-5.

To prevent receiving unwanted output, the user is reminded that, when NPD and NPS are input, those values remain in effect until a new input is received.

Table 5-1
BEHAVE-1 INPUT

NAMELIST MAINP (Table 4-1)

PINID	= fuel element identification (up to 59 columns starting in column 2)	Card 1 FORMAT (10A6, A4)
ICASE	= case number, columns 61-64	
JOP	= power history option sequence. 2 = FSTART 3 = STEADY 4 = INCRES 1 = end of power history (limited to 40 sequences per history)	Card 2 FORMAT (40I2)

NAMELIST MAINP

MZ	= number of axial nodes (limit 29)
FCL	= total fuel column axial length (in.)
DLE	= axial node length for (MZ-1) nodes (in.)
DLTE	= length of last axial node (in.)
AC	= cladding attack option if $\neq 0$, THKINP NAMELIST must occur immediately after the first STEINP NAMELIST (see CTHICK discussion in Section 4.7)
CODN	= nominal as-fabricated cladding o.d. (in.)
CIDN	= nominal as-fabricated cladding i.d. (in.)
DGAPN	= nominal diametral fuel-cladding gap (in.)
EDPN	= nominal fuel pellet density (.TD)
FTD	= fuel theoretical density (g/cm^3)
BTD	= blanket theoretical density (g/cm^3)

Table 5-1 (Continued)

BDSN	=	nominal blanket smeared density (.TD)
BLT	=	total blanket length (upper and lower) (in.)
PLL	=	gas plenum length (in.)
WOC	=	cladding o.d. wear allowance (in.)
WIC	=	cladding i.d. wear allowance (in.)
IHGOPT	=	fuel-cladding gap conductance coefficient option = 1 coefficient is computed; $\neq 1$ MZ values of HG must be supplied
HG(MZ)	=	fuel-clad gap conductance coefficient (if IHGOPT $\neq 1$) (Btu/hr-ft ² · °F)

NAMELIST STAINP (Table 4-2)

*QL(MZ)	=	start-of-life linear power array (kW/ft)
TSI	=	core inlet sodium temperature (°F)
TSO	=	core outlet sodium temperature (°F)
TS(MZ)	=	sodium temperature (°F) (with ITCOPT = 2)
NPD	=	detailed printout option (output for each cycle during a power history sequence) -1 = print 0 = no print or plot +1 = print and plot (plot not available)
NPS	=	summary printout option (output once every power history sequence) -1 = print 0 = no print or plot +1 = print and plot (plot not available)
ITCOPT	=	cladding temperature option 1 = calculate TCA(MZ) and CDT(MZ) from input TSI, TSO 2 = calculate TCA(MZ) and CDT(MZ) from input TS(MZ) 3 = read TCA(MZ) from input and calculate CDT(MZ) 4 = use values of TCA(MZ) and CDT(MZ) then stored in the ACCUMULATOR
TCA(MZ)	=	mean cladding temperature (°F) (with ITCOPT = 3)
PL(MZ)	=	restructuring linear power (kW/ft)
ATC(MZ)	=	cladding mean temperature during fuel restructuring (°F)
ITCA	=	restructuring mean cladding temperature option = 0 PL(MZ) and ATC(MZ) must be input = 1 or 2 ATC(K) = TCA(K) K = 1, MZ = 2 PL(K) = QL(K)
FAVBPT	=	fraction of available void below 1300°C which can accommodate fission products

NAMELIST STEINP (Table 4-3)

DTMT	=	length of steady-state power operation (days)
STIME	=	accumulated time of exposure at start of steady-state operation (days)
SVAL	=	peak linear power at time = STIME (kW/ft)
ETIME	=	STIME + DTMT (days)

* arrays of MZ axial nodes

Table 5-1 (Continued)

EVAL	=	peak linear power at time = ETIME (kW/ft)
TSI	=	sodium inlet temperature ($^{\circ}$ F)
SV	=	sodium velocity (ft/sec)
SOX	=	sodium oxygen content (ppm)
TSO	=	sodium outlet temperature ($^{\circ}$ F)
SP	=	mean sodium pressure (psia)
CATN	=	allowance for cladding i.d. attack (in.)
CCTN	=	allowance for cladding o.d. corrosion (in.)
FN(MZ)	=	total neutron flux (10^{15} n/cm ²)
FDF	=	radial fast flux fraction ($E > 0.1$ MeV)
FGT(MZ)	=	axial fast flux fractions ($E > 0.1$ MeV)
FLFU	=	EBR-II fluence correction factor for metal swelling equations (nominally 1.2, but will eliminate swelling if $\geq 10^6$)
A	=	exponential constant for strain enhancement of metal swelling (= 80 for SS316, = 200 for SS347)
QL(MZ)	=	See Table 4-2
HG(MZ)	=	See Table 4-2 (HG is included in STEINP to permit simulation of changes with burnup)
TCA(MZ)	=	See Table 4-2
SR	=	fuel volumetric swelling rate (% per 10^{20} fissions/cc)
GASBOT	=	range: $0 \leq \text{GASBOT} \leq 1$ If $\neq 0$, central void is a closed pressurized volume. If = 0, fission gases communicate with plenum.
NPD	=	See Table 4-2
NPS	=	See Table 4-2
ISIMS	=	enhanced metal swelling option If > 0 , swelling is increased using input value of A (Equation 2.1)
NQLADJ	=	if > 0 , QL array is not changed when subroutine STEADY is called by MAIN
NFNADJ	=	if > 0 , FN array is not changed when subroutine STEADY is called by MAIN
ICCOPT	=	option for cladding creep equation 1 = irradiation creep WADCO SS316CW thermal creep WADCO SS316 annealed 2 = irradiation creep WADCO SS316 annealed thermal creep WADCO SS316 annealed 3 = irradiation creep GE SS304/321 annealed thermal creep GE SS304/321 annealed 4 = irradiation creep GE SS316 CW thermal creep GE SS316 annealed
ICSOPT	=	Option for cladding swelling equation 1 = WADCO SS316 CS 2 = GE SS321 3 = GE (Sandusky) SS316 Annealed 4 = GE (Oldberg) SS316/SS304 Annealed 5 = GE (Appleby) SS304 annealed 6 = WADCO SS316 annealed ("1.71" Equation) 7 = GE (Stephen) 347 annealed 8 = WADCO SS316 CW 9 = WADCO SS316 Annealed ("0" Equation)

Table 5-1 (Continued)

IETOPT = effective thickness option
 1, 2, and 3 = uniform thickness reduction at start-of-life
 from input CATN, CCTN
 2 = calculate o.d. corrosion and i.d. attack from input SV, SOX
 3 = calculate o.d. corrosion and i.d. attack from input
 constants (AC $\neq$ 0 and THKINP namelist provided)
 ITCOPT cladding temperature option (See Table 4-2.)

NAMelist THKINP (Table 4-4.)

Table 4-4 INPUT TO CTHICK (this NAMelist is optional (IETOPT = 3).
 It is required only if AC $\neq$ 0 in NAMelist MAINP (See Table 4-1)

C1, C2 Defined by Equation 4.27

C3 through C7 Defined by Equation 4.28

NAMelist PININP (Table 4-7)

EVALS = EVAL from preceding STEINP NAMelist (See Table 4-3.)
 FRAINC = fraction of differential thermal expansion strain
 which is active in deforming the clad
 (nominal value = 0.70)
 SVALE = SVAL from following STEINP NAMelist (See Table 4-3.)

TYPICAL BEHAVE-I NAMELIST INPUT FORMAT

2 3 4 3 1

```
$MAINP/MZ=5, FCL=22., DLE=5., DLTE=2., AC=7., CODN=.230,
  CIDN=.210, DGAPN=.003, FDPN=.90, FTD=11.04, BTD=10.96,
  BDSN=.87, BLT=60., PLL=35., WOC=.0005, WIC=.001, IHGOPT=0,
  HG=1375, 1429, 1553, 1416, 1333
$
```

```
$STAINP/QL=13.5, 14.4, 15.2, 14.8, 13.9, TSI=750., TSO=1150.,
  NPD=-1, NPS=0, ITCOPT=3, TCA=758, 837, 959, 1089, 1134,
  ITCA=0, PL=14.9, 15.6, 16.8, 16.2, 15.3,
  ATC=760, 880, 993, 110, 1144, FAVBPT=.25
$
```

```
$STEINP/DTMT=400., STIME=0., SVAL=13.0, ETIME=400.0, EVAL=12.7,
  TSI=738., TSO=1127., SP=66., FN=5*4.07, FDF=.55,
  FGT=.72, .89, .94, .91, .87, FLFU=1.0, SR=.45, GASBOT=0.,
  NPD=0, ISIMS=0, ICCOPT=1, ICSOPT=5, IETOPT=3,
$
```

```
$THKINP/C1=.0003, C2=0., C3=.001, C4=0., C5=0., C6=.0005,
  C7=1460.
$
```

```
$PININP/EVALS=12.7, SVALE=16.5, FRAINC=.7
$
```

```
$STEINP/DTMT=1000.
  STIME=400., SVAL=16.5, ETIME=1400., EVAL=15.9, FN=5*4.83
$
```

Figure 5-1. Typical NAMELIST Input Format (Also See Figure 3-1)

STATUS AFTER FIRST STARTUP

SODIUM INLET TEMP (F) = 700.0000
SODIUM OUTLET TEMP (F) = 868.0000

HEIGHT (IN)	LIN POWER (KW/FT)	SODIUM TEMP(F)	CLAD AV TEMP(F)	CLAD DEL-T (F)	CLAD THERM EXP(IN/IN)	CLAD STRAIN DIF TH EXP	CLAD ID (IN)	CLAD OD HOT(IN)	CLAD OD COLD(IN)
9.71	12.96	0.	880.	75.	0.0084	0.0000	0.2221	0.2512	0.2491
2.14	14.14	0.	911.	81.	0.0087	0.0002	0.2223	0.2513	0.2492
3.57	15.08	0.	940.	85.	0.0090	0.0005	0.2224	0.2515	0.2492
4.99	15.66	0.	966.	88.	0.0093	0.0005	0.2225	0.2516	0.2492
6.42	15.77	0.	985.	88.	0.0095	0.0008	0.2226	0.2517	0.2493
7.85	15.39	0.	999.	85.	0.0097	0.0005	0.2225	0.2516	0.2492
9.28	14.67	0.	1007.	81.	0.0098	0.0005	0.2226	0.2517	0.2492
10.70	13.65	0.	1011.	75.	0.0098	0.0002	0.2225	0.2516	0.2492
12.13	12.50	0.	1013.	69.	0.0098	0.0001	0.2225	0.2516	0.2491
13.56	11.86	0.	1014.	65.	0.0098	0.0000	0.2225	0.2516	0.2491

Figure 5-2. FSTART Output -- First Startup Strain

FUEL RESTRUCTURING AFTER FIRST STARTUP

HEIGHT (IN)	VOL FRACT CRACKS	VOL FRACT AVAILABLE FUEL POROS	VOL FRACT CENTRAL VOID	RADIUS OF FO GRAIN GRD(IN)	RADIUS OF COL GRAIN GRD(IN)	RADIUS OF CFN VOID (IN)	TEMP AT FUEL SUR (F)	MAX TEMP OF FUEL (F)	AT 2372. F PLASTICITY RADIUS(IN)
0.71	0.0092	0.0190	0.0035	0.0644	0.0159	0.0065	1476.	3924.	0.0837
2.14	0.0128	0.0180	0.0069	0.0729	0.0422	0.0092	1560.	4233.	0.0881
3.57	0.0106	0.0182	0.0093	0.0783	0.0531	0.0106	1632.	4485.	0.0926
4.99	0.0130	0.0182	0.0107	0.0814	0.0585	0.0114	1684.	4681.	0.0948
6.42	0.0133	0.0178	0.0110	0.0824	0.0599	0.0116	1707.	4722.	0.0948
7.85	0.0137	0.0183	0.0106	0.0814	0.0582	0.0113	1704.	4674.	0.0948
9.28	0.0114	0.0182	0.0093	0.0789	0.0529	0.0106	1679.	4505.	0.0926
10.70	0.0139	0.0190	0.0073	0.0744	0.0432	0.0094	1636.	4264.	0.0903
12.13	0.0133	0.0206	0.0044	0.0677	0.0240	0.0073	1586.	4007.	0.0881
13.56	0.0129	0.0195	0.0030	0.0631	0.	0.0061	1557.	3780.	0.0837

Figure 5-3. FSTART Output - Fuel Restructuring

STATUS AFTER 15.0 DAYS OF STEADY STATE OPERATION,
TOTAL DAYS OPERATION = 190.1

HEIGHT (IN)	LIN PWR (KW/FT)	CLAD AV TEMP(F)	BURNUP (MWD/KGM)	FLUENCE (N/CM2) (E GT 0.1 MEV)	CLAD DIA STRAIN CLAD SWELLING (IN/IN)	CLAD OD GOLD(IN)
0.714	13.29	884.60	37.67	0.249E 23	0.191E-02	0.24985
2.141	14.38	914.59	40.84	0.278E 23	0.221E-02	0.24997
3.568	15.24	942.55	43.34	0.304E 23	0.246E-02	0.25001
4.995	15.78	968.04	44.90	0.317E 23	0.250E-02	0.25002
6.422	15.88	987.04	45.20	0.319E 23	0.241E-02	0.24998
7.849	15.50	1001.13	44.11	0.310E 23	0.219E-02	0.24993
9.276	14.77	1009.13	42.01	0.288E 23	0.185E-02	0.24995
10.703	13.74	1013.12	39.07	0.262E 23	0.152E-02	0.24972
12.130	12.58	1015.04	35.74	0.230E 23	0.118E-02	0.24959
13.557	11.93	1015.86	33.83	0.198E 23	0.880E-03	0.24943

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Figure 5-4. STEADY Output — Accumulated Metal Swelling

ROD INTERNAL PRESSURE (PSIA) 385.97

GAS COMPOSITION (MOLES)

FABRICATION 0.3647E-03
 ADSORBED 0.5820E-03
 NOBLE FISS GAS 0.2574E-02

HEIGHT (IN)	GAS REL OD FRACT	CORROS ID ALLOW (IN)	ATTACK ALLOW (IN)	EFF CLAD THICK (IN)	CLAD HOOP STRESS (PSI)	THERMAL CREEP STRAIN	IPRAD CREEP STRAIN
0.71	0.6365	0.	0.	0.0144	2738.	0.180E-13	0.316E-04
2.14	0.6564	0.	0.	0.0144	2738.	0.908E-13	0.336E-04
3.57	0.6721	0.	0.	0.0144	2738.	0.386E-12	0.352E-04
4.99	0.6819	0.	0.	0.0144	2738.	0.137E-11	0.353E-04
6.42	0.6838	0.	0.	0.0144	2738.	0.343E-11	0.345E-04
7.85	0.6769	0.	0.	0.0144	2738.	0.664E-11	0.329E-04
9.28	0.6638	0.	0.	0.0144	2738.	0.963E-11	0.301E-04
10.70	0.6453	0.	0.	0.0144	2738.	0.116E-10	0.273E-04
12.13	0.6244	0.	0.	0.0144	2738.	0.127E-10	0.239E-04
13.56	0.6067	0.	0.	0.0144	2738.	0.133E-10	0.206E-04

HEIGHT (IN)	VOLUMETRIC (DV/V) FUEL	FUEL VOID CLOSURE BY SWELLING NO=0 YES=1 GEN TEMP CRACKS	CLAD DIA STRAIN FUEL SWELLING (IN/IN)	CLAD OD. (IN)	CLAD OD COLD(IN)
	SWELLING	AVAILABLE	CLAD PELLET SWELL VOID		
0.71	0.380E-01	0.380E-01	1 1 1 1	0.668E-04	0.2522 0.25007
2.14	0.456E-01	0.456E-01	1 1 1 1	0.233E-03	0.2524 0.25025
3.57	0.476E-01	0.475E-01	1 1 1 1	0.475E-03	0.2526 0.25030
4.99	0.521E-01	0.520E-01	1 1 1 1	0.697E-03	0.2527 0.25040
6.42	0.524E-01	0.523E-01	1 1 1 1	0.785E-03	0.2527 0.25034
7.85	0.518E-01	0.517E-01	1 1 1 1	0.700E-03	0.2527 0.25029
9.28	0.465E-01	0.464E-01	1 1 1 1	0.511E-03	0.2526 0.25018
10.70	0.459E-01	0.459E-01	1 1 1 1	0.282E-03	0.2523 0.24989
12.13	0.424E-01	0.423E-01	1 1 1 1	0.111E-03	0.2522 0.24976
13.56	0.385E-01	0.385E-01	1 1 1 1	0.550E-04	0.2520 0.24954

Figure 5-5. STEADY Output - Creep and Fuel-Cladding Mechanical Interaction

CLAD STRAIN DUE TO POWER INCREASE

PEAK LIN PWR AT BEG OF POWER INCREASE (KW/FT) 15.71
 CORRESPONDING MEAN CLAD TEMP (F) 984.
 PEAK LIN PWR AT END OF POWER INCREASE (KW/FT) 17.62
 CORRESPONDING MEAN CLAD TEMP (F) 1018.

HEIGHT (IN)	BURNUP (MWD/KGM)	SUR TEMP INCR (F)	CLAD STRAIN	TOT CLAD IF STRAIN	CLAD OD (IN)	SUR REL RAD(IN)	CLAD OD COLD(IN)
0.71	38.19	164.	0.0007	0.0018	0.2521	0.8200	0.2501
2.14	41.40	185.	0.0008	0.0020	0.2524	0.8600	0.2502
3.57	43.94	155.	0.0006	0.0016	0.2525	0.8800	0.2502
4.99	45.52	187.	0.0008	0.0017	0.2526	0.9000	0.2503
6.42	45.82	155.	0.0006	0.0015	0.2526	0.9000	0.2502
7.85	44.71	185.	0.0007	0.0017	0.2526	0.9000	0.2502
9.28	42.59	120.	0.0004	0.0018	0.2526	0.8800	0.2501
10.70	39.61	116.	0.0004	0.0012	0.2523	0.8600	0.2499
12.13	36.23	148.	0.0005	0.0012	0.2522	0.8400	0.2497
13.56	34.30	103.	0.0003	0.0008	0.2520	0.8000	0.2495

Figure 5-6. INCRES Output - Power Increase Strain

BEHAVE
FUEL PERFORMANCE SIMULATION

BEHAVE-1 PARAMETER/SENSITIVITY STUDY F2H

CASE- 00

FUEL ELEMENT DESCRIPTION

FUEL COLUMN AXIAL LENGTH(IN)	14.2700
TOTAL BLANKET LENGTH (IN)	6.0100
PLENUM LENGTH (IN)	13.0000
NOMINAL CLAD OUTSIDE DIAMETER	0.2491
NOMINAL CLAD INSIDE DIAMETER	0.2203
NOMINAL FUEL-CLAD GAP(IN)	0.0026
GAP CONDUCT.(BTU/HR-FT ² -DEG F)	1373.0848
NOMINAL PELLETT DENSITY (.TD)	0.9610
FUEL PIN SMEAR DENSITY (.TD)	0.9385

SUMMARY, BY MECHANISM, OF FOL CLAD DIAMETER CHANGES

HEIGHT (IN)	BURNUP MWD/KG	FLUENCE *F-23	ICREEP MILS	ICREEP MILS	FUSWEI MILS	CLSWEI MILS	TOTAL DIA CHG MILS
0.71	49.67	0.334	0.00	0.01	0.13	0.84	2.03
2.14	53.97	0.373	0.00	0.01	0.44	1.05	2.65
3.57	57.37	0.407	0.00	0.02	0.90	1.27	3.20
4.99	59.49	0.424	0.00	0.02	1.32	1.39	3.69
6.42	59.89	0.427	0.00	0.02	1.40	1.39	3.83
7.85	58.45	0.416	0.00	0.01	1.33	1.24	3.55
9.28	55.73	0.386	0.00	0.01	0.97	0.99	2.98
10.70	51.87	0.351	0.00	0.01	0.54	0.76	2.13
12.13	47.52	0.308	0.00	0.01	0.21	0.56	1.65
13.56	45.12	0.266	0.00	0.01	0.10	0.41	1.17

Figure 5-7. EOL Summary Output

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FUEL TEMP PROFILE AFTER RESTRUCTURING

13.56 INCHES FROM BOTTOM OF FUEL COLUMN

MEAN CLAD TEMP (F) 1014.
 CLAD OD (IN) 0.2516
 CLAD ID (IN) 0.2225
 LIN PWR (KW/FT) 11.86
 GAP COEF (BTU/HR-FT²-F) 1373.
 PELLET DEN AS-FAR (.TD) 0.9610

NORMALIZED FUEL RADIUS	NORMALIZED FUEL DENSITY	FUEL TEMP (F)
1.0000	0.9610	1557.
0.9600	0.9610	1701.
0.9200	0.9610	1845.
0.8800	0.9610	1989.
0.8400	0.9610	2133.
0.8000	0.9610	2277.
0.7600	0.9610	2421.
0.7200	0.9610	2565.
0.6800	0.9610	2709.
0.6400	0.9610	2844.
0.6000	0.9610	2970.
0.5600	0.9700	3087.
0.5200	0.9700	3195.
0.4800	0.9700	3303.
0.4400	0.9700	3393.
0.4000	0.9700	3483.
0.3600	0.9700	3555.
0.3200	0.9700	3627.
0.2800	0.9700	3681.
0.2400	0.9700	3726.
0.2000	0.9700	3762.
0.1600	0.9700	3780.
0.1200	0.9700	3780.
0.0800	0.9700	3780.
0.0400	0.	3780.
0.	0.	3780.

Figure 5-8: FSTART Output - Fuel Radial Temperature Profile

***** GAS BOTTLE EFFECT OPERATIVE *****

HEIGHT (IN)	RADIUS OF (IN)	CENT. VOID RELATIVE	PLASTICITY RAD. (IN)	TEMP CENT. VOID (C)
0.71	0.0134	0.119	0.0609	1811.40
2.13	0.0150	0.1332	0.0700	1922.17
3.55	0.0162	0.1432	0.0769	2030.53
4.97	0.0168	0.1480	0.0815	2118.92
6.39	0.0168	0.1487	0.0814	2107.77
7.81	0.0164	0.1453	0.0814	2121.55
9.23	0.0156	0.1383	0.0767	2059.97
10.65	0.0144	0.1281	0.0721	1962.55
12.07	0.0129	0.1148	0.0652	1860.48
13.49	0.0119	0.1058	0.0629	1828.78

MOLES OF CESIUM 0.1118E-04
 MOLES NOBLE GAS 0.1580E-04
 INTERNAL PRESS. 0.441/E 04
 BURNUP (MWD/KGM) 0.1570E 00

***** GAS BOTTLE EFFECT OPERATIVE *****

HEIGHT (IN)	RADIUS OF (IN)	CENT. VOID RELATIVE	PLASTICITY RAD. (IN)	TEMP CENT. VOID (C)
0.71	0.0134	0.1190	0.0609	1811.23
2.13	0.0151	0.1333	0.0700	1922.00
3.55	0.0162	0.1433	0.0769	2030.35
4.97	0.0168	0.1482	0.0815	2118.74
6.39	0.0168	0.1488	0.0814	2107.59
7.81	0.0164	0.1454	0.0814	2121.37
9.23	0.0156	0.1384	0.0767	2054.78
10.65	0.0144	0.1281	0.0721	1962.36
12.07	0.0129	0.1149	0.0652	1855.28
13.49	0.0119	0.1059	0.0629	1828.59

MOLES OF CESIUM 0.1118E-04
 MOLES NOBLE GAS 0.1580E-04
 INTERNAL PRESS. 0.4654E 04
 BURNUP (MWD/KGM) 0.1569E 00

Figure 5-9: GASP Output — Gas Bottle Effect

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