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## ABSTRACT

The tasks of the Gas-Cooled Fast Breeder Reactor (GCFR) program that are supported by the U.S. Energy Research and Development Administration include: development of GCFR fuel, blanket, and control elements; fuels and materials development; nuclear analysis and reactor physics for GCFR core design; development of the pressure-equalization system for fuel; structural-thermal-flow tests of core assemblies; an in-pile loop facility test program plan; reactor engineering to assess the thermal, hydraulic, and structural performance of the core and core support structure, to evaluate and develop the reactor control-rod, shutdown-rod, and core components locking mechanisms, and to evaluate the capability of PCRV internal structures for postaccident fuel containment; to evaluate the pressure-boundary integrity of the prestressed concrete reactor vessel (PCRVR); reactor safety; refueling-system development; component development engineering; shielding requirements of the GCFR; and fuel-rod engineering to evaluate the steady-state and transient performance of the GCFR fuel and blanket rods.



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## I. INTRODUCTION

The various tasks of the Gas-Cooled Fast Breeder Reactor (GCFR) program sponsored by the U.S. Energy Research and Development Administration are discussed in the following sections of this quarterly progress report.

The GCFR Utility Program, which is supported by a large number of electric utility companies, rural electric cooperatives, and General Atomic Company, is primarily directed toward the development of a GCFR demonstration plant. This utility-sponsored work and the ERDA-sponsored work are complementary.

To establish the basis for the design of the GCFR fuel, blanket, and control element assemblies, analytical, experimental, and fabrication development is being done under the core-element development task. Analytical methods development for structural and thermal-hydraulic analyses is discussed. The results of structural analysis of the fuel-assembly components and thermal-hydraulic analysis of the blanket element during low power are presented. Current progress on the rod-spacer interaction tests, fuel-element seismic and vibration test planning, and the development of assembly fabrication techniques is also presented. The various subtasks of the core-element development and the work accomplished during this reporting period are discussed in Section II.

In the fuels and materials development program, thermal-flux and fast-flux irradiation programs are being conducted to establish conditions and design features specific to GCFR fuel rods, such as vented fuel, fission-product traps, and surface-roughened cladding. In addition, a test program is being conducted of GCFR cladding specimens, both smooth and surface-roughened, to determine materials behavior in an irradiation environment. These fuels and materials tests and analytical studies and the results to date are presented in Section III.

The validation and verification of the nuclear design methods that will be applied to the GCFR core design are the objectives of the nuclear analysis and reactor physics task. A critical experiments program will be carried out to evaluate the methods developed. The critical assembly design and analysis and methods development are presented in Section IV.

The technology to support the design and construction of the pressure-equalization system for GCFR fuel is being developed. This includes the development of design models and computer codes that will be verified by test programs and testing of materials and seals and developing fabrication processes for the pressure-equalization system, as discussed in Section V.

Program plans for out-of-pile and in-pile tests of GCFR element assemblies are discussed in Sections VI and VII. The out-of-pile heat-transfer and fluid-flow tests are to demonstrate the ability of the GCFR assemblies to meet design goals and to verify analytical predictions that describe design operation and postulated accident behavior.

In the reactor engineering program, analytical methods and models are being evaluated and developed to assess the thermal, hydraulic, and structural performance of the GCFR core and core support; a program to develop the control rods, shutdown rods, and core component locking mechanisms is being carried out; and materials behavior models are being developed to assess the capability of the PCRV internal structures to serve as postaccident fuel containment. The work on these tasks is discussed in Section VIII.

An evaluation of the design of the prestressed concrete reactor vessel (PCRV) pressure-boundary system is being made to determine the integrity of the system, which includes the PCRV liner and thermal and pressure barriers. This program is discussed in Section IX.

The reactor safety task, which is discussed in Section X, is to study the safety aspects of the GCFR using logical probabilistic methods to determine the probabilities associated with accident initiation and progression sequences.

In the development of the GCFR refueling system, an evaluation of the reference design and design verification of the refueling equipment are being conducted. This task includes analysis of system requirements for spent-fuel cooling and shielding. Program plans for the development and testing of the GCFR refueling system are being developed. This task is discussed in Section XI.

The development of the main components of the GCFR nuclear steam supply system is being carried out under the component engineering task. Initial work on this task was the preparation of development program plans for the steam generator, helium circulator, and PCR. These program plans are discussed in Section XII.

The verification of the physics and engineering analytical methods and data for the design of the GCFR shields and an evaluation of the effectiveness of various shield configurations are to be conducted under the shielding requirements task. The results of radial shield analyses and the work being done on structural analysis are presented in Section XIII.

Under the fuel-rod engineering task, the steady-state and transient performance of the fuel and blanket rods is being evaluated to determine performance characteristics, operating limits, and design criteria. In addition, surveillance of the fuel-rod and blanket-rod technology of other programs is being carried out. These studies are presented in Section XIV.



## II. CORE-ELEMENT DEVELOPMENT (189a No. 13125)

### 2.1. FUEL- AND CONTROL-ASSEMBLIES DEVELOPMENT

The analytical basis, in conjunction with experimental results, for the design and development of the GCFR fuel and control assemblies is being developed. Because complete prototype in-pile tests will not be conducted, a strong analytical base, supported by development tests, is required to design the core assemblies. The current emphasis is to develop an adequate steady-state and transient analysis capability and to perform both thermal-hydrodynamic and structural transient analyses, which will provide a basis for the assembly design criteria and for specific test requirements. Although some transient analyses have been performed previously and some of the analyses to be performed are extensions of the previous work, much of the work for the current year will be based on newly developed methods; therefore, the completion of the analytical work is dependent on the adequacy of those methods.

During the previous quarterly period, initial calculations were made with the newly developed COBRA-IV code for helium, the basic equations of the FLOMAX thermal analysis code were derived, and a viscoelastic dilation model was developed to calculate the dilation of the hexagonal fuel-assembly duct.

During this quarterly period, the FLOMAX equations were developed further and the fundamental solution of the equations was obtained; a small computer program of the solution showed excellent agreement with corresponding COBRA calculations. Except for comparison with the FLOMAX solution, there were no further calculations with COBRA-IV. There have been difficulties with the transient execution of COBRA, but these will be resolved in cooperation with Battelle Pacific Northwest Laboratory (PNL). The dilation model was coded for computer calculation of the duct dilation throughout the core.

### 2.1.1. Development of the Thermal Analysis Code FLOMAX

The development of FLOMAX, which was initiated during the last quarterly period, was continued. Previously, the differential equations were derived and scaled for their solution by the method of matched asymptotic expansions. During this period, the scaled equations were discretized in the transverse coordinates by an integral method, which resulted in a coupled system of subchannel differential equations in the axial coordinate and in time. To complete the system of equations, an empirical turbulence closure model is required. The literature on turbulent mixing in rod bundles was reviewed and a closure model selected. The solution of the equations proceeded by expanding the variables in perturbation series and the lowest-order, or fundamental, equations were solved by an iterative scheme. The fundamental solutions compared well with appropriate COBRA-IV results and the iterative scheme exhibited excellent convergence properties.

2.1.1.1. Summary of Equations. The describing three-dimensional differential equations were integrated in the transverse coordinates by the use of Green's Theorem in the Plane and by approximating the dependent variables by constants over small regions in the flow field, or subchannels; derivatives of variables are then transformed to fluxes at the subchannel boundaries. In this way, the following system of dimensionless subchannel equations were obtained.

Conservation of mass:

$$\delta \psi_i + \frac{\partial}{\partial x} (\rho u A)_i + C_{ik} (\rho v s)_k = 0 \quad . \quad (2.1)$$

Conservation of axial momentum:

$$\begin{aligned} \epsilon \frac{\partial}{\partial x} (\rho u^2 A)_i + [\rho u^2 (Pf + \frac{1}{2} \epsilon K_\ell \delta_\ell)]_i \\ + C_{ik} [\rho \bar{u} \Delta u (X_M s \sqrt{f} + \epsilon M_\ell \delta_\ell) + \epsilon \rho v u s]_k \\ = - \frac{\partial}{\partial x} (p A)_i + \beta (p A)_i \quad . \end{aligned} \quad (2.2)$$

Conservation of transverse momentum:

$$\Delta p_k = O(\epsilon d_c^2 / L_c^2) \quad . \quad (2.3)$$

Conservation of energy:

$$\begin{aligned} & \epsilon \frac{\partial}{\partial x} (\rho u T A)_i + C_{ik} [\rho \bar{u} \Delta T (X_H s \sqrt{f} + \epsilon H_\ell \delta_\ell) + \epsilon \rho v T s]_k \\ & = \epsilon (P^h_q)_i \quad . \end{aligned} \quad (2.4)$$

Equation of state:

$$\rho_i = \left( \frac{1 + \gamma p}{1 + \alpha T} \right)_i \quad (2.5)$$

In these equations, the only unsteady term appears in Eq. (2.1) and is defined by

$$\psi_i \equiv \frac{\partial \rho_i}{\partial t} + \frac{\rho_i}{\rho_c} \frac{d\rho_c}{dt} \quad ;$$

the 0 in Eq. (2.3) means "the order of magnitude of." All the variables in Eqs. (2.1) through (2.5) are nondimensional and have been scaled in magnitude to suit the reactor fuel-assembly conditions. These variables and the characteristic values by which they have been scaled (denoted by subscript c) are listed in Table 2.1. Other parameters used for scaling are defined in Table 2.2. The perturbation parameters previously introduced have been adjusted to correspond to the subchannel equations; their definition and typical values for the GCFR fuel assembly are shown in Table 2.3.

**2.1.1.2. Review of Turbulent Mixing.** The system of equations given above was completed by specifying the Reynolds stress (momentum flux),  $\tau_R$ , and Reynolds heat flux,  $q_R$ , in terms of the velocities and temperatures. A valid approximation of these fluxes for flow in rod bundles is to specify them in terms of the eddy diffusivities of momentum,  $K_\tau$ , and heat,  $K_q$ . Thus, in dimensional variables,

Table 2.1

## SUMMARY OF VARIABLES USED IN THE FLOMAX SUBCHANNEL EQUATIONS

| Scaled Variables (Dimensionless) |                               | Scaling Parameters |                        |                               |                                            |
|----------------------------------|-------------------------------|--------------------|------------------------|-------------------------------|--------------------------------------------|
| Symbol                           | Description                   | Symbol             | Dimension <sup>a</sup> | Definition                    | Description                                |
| A                                | Subchannel flow area          | $A_c$              | $L^2$                  | $A_T/n_s$                     | Average subchannel flow area               |
| f                                | Friction factor               | $f_c$              | 1                      |                               | Characteristic Darcy friction factor       |
| p                                | Static pressure               | $\Delta p_c$       | $M/LT^2$               | $\rho_c u_c^2 f_c L_c / 2D_H$ | Assembly frictional pressure drop          |
| P                                | Subchannel friction perimeter | $P_c$              | L                      | $P_T/n_s$                     | Average friction perimeter                 |
| $\infty$ $P^h$                   | Subchannel heated perimeter   | $P_c^h$            | L                      | $\pi D_n / n_s$               | Average heated perimeter per subchannel    |
| q                                | Rod-surface heat flux         | $q_c$              | $M/T^3$                | $Q/\pi D$                     | Average rod heat flux                      |
| $\rho$                           | Density                       | $\rho_c$           | $M/L^3$                | $\rho_{in}$                   | Inlet density                              |
| s                                | Inter-subchannel gap width    | $s_c$              | L                      |                               | Characteristic gap width                   |
| t                                | Time                          | $t_c$              | T                      |                               | Characteristic time of boundary conditions |
| T                                | Subchannel temperature        | $\Delta T_c$       | $\theta$               | $QL/\rho_c C_p u_c A_r$       | Average coolant temperature rise           |
| u                                | Axial velocity                | $u_c$              | L/T                    | $W/\rho_c A_c$                | Average axial velocity                     |
| v                                | Crossflow velocity            | $v_c$              | L/T                    | $u_c d_c / L_c$               | Characteristic crossflow velocity          |
| x                                | Axial coordinate              | $L_c$              | L                      |                               | Length of assembly                         |

<sup>a</sup>The dimension symbols are: M = mass, L = length, T = time,  $\theta$  = temperature, 1 = dimensionless.

Table 2.2  
OTHER PARAMETERS

| Symbol        | Definition         | Dimension       | Description                                                                                  |
|---------------|--------------------|-----------------|----------------------------------------------------------------------------------------------|
| $A_r$         | $A_T/n_r$          | $L^2$           | Average flow area per fuel rod                                                               |
| $A_T$         |                    | $L^2$           | Total flow area                                                                              |
| $C_{ik}$      |                    | 1               | Subchannel connection matrix                                                                 |
| $C_p$         |                    | $L^2/T^2\theta$ | Constant pressure specific heat                                                              |
| $d_c$         | $A_c/s_c$          | L               | Characteristic transverse length                                                             |
| D             |                    | L               | Rod diameter                                                                                 |
| $D_H$         |                    | L               | Average hydraulic diameter                                                                   |
| $\delta_\ell$ |                    | 1               | Dirac $\delta$ -function ( $\delta_\ell = \delta(x-a_\ell)$ ),<br>$a_\ell$ = spacer location |
| H             | $4A_T/P_T$         | 1               | Spacer heat mixing factor                                                                    |
| K             |                    | 1               | Spacer pressure-loss coefficient                                                             |
| M             |                    | 1               | Spacer momentum mixing factor                                                                |
| $n_r$         |                    | 1               | Number of rods                                                                               |
| $n_s$         |                    | 1               | Number of subchannels                                                                        |
| $P_T$         |                    | L               | Total friction perimeter                                                                     |
| Q             |                    | ML/T            | Average heat generation per rod per unit length                                              |
| W             |                    | M/T             | Total mass flow                                                                              |
| $X_H$         | $X_M K_q / K_\tau$ | 1               | Turbulent heat mixing factor                                                                 |
| $X_M$         |                    | 1               | Turbulent momentum mixing factor                                                             |

Table 2.3  
PERTURBATION PARAMETERS

| Symbol     | Definition          | Description                            | Value for GCFR Fuel Assembly                      |
|------------|---------------------|----------------------------------------|---------------------------------------------------|
| $\epsilon$ | $2D_H/f_c L_c$      | Length-scale friction-factor parameter | 0.067                                             |
| $\delta$   | $L_c/u_c t_c$       | Time-scale parameter                   | 0.278 (peak), 0.01 (avg.)                         |
| $\gamma$   | $\Delta p_c/p_{in}$ | Pressure-drop ratio                    | 0.032                                             |
| $\alpha$   | $\Delta T_c/T_{in}$ | Temperature-rise ratio                 | 0.381                                             |
| $\beta$    | $2gD_H/f_c u_c^2$   | Gravitation friction-factor parameter  | 0.006 (20% flow),<br>10 <sup>-4</sup> (100% flow) |

$$\tau_R = -\rho K_\tau \frac{\partial u}{\partial z} , \quad (2.6)$$

$$q_R = -\rho C_p K_q \frac{\partial T}{\partial z} . \quad (2.7)$$

The literature review showed that for flows in both circular tubes and rectangular channels, the above diffusivities ( $K_\tau \approx K_q$ ) could be calculated from

$$K_\tau = 0.2 u^* h , \quad (2.8)$$

where  $u^*$  = shear velocity ( $\equiv \sqrt{\tau_w/\rho}$ ),

$h$  = an appropriate length ( $\approx D_h/4$ ),

$D_h$  = hydraulic diameter.

By measuring directly the velocity gradient and the Reynolds stress, Kjellström<sup>(1)\*</sup> showed that the above formulation is valid also for rod bundles. Similarly, Roidt, et al.,<sup>(2)</sup> confirmed the formulation by measuring mass transport and concentration gradients in their tracer injection experiments. Equations (2.6) and (2.7) are required only at the boundaries

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\*References are listed at the end of each section.

of the subchannels; thus the velocity gradient, for example, between subchannels  $i$  and  $j$ , can be written as  $\partial u / \partial z \approx (u_j - u_i) / z_{ij}$ , where  $z_{ij}$  is a representative distance between subchannels. With this approximation, the turbulent mixing model is identical to the one used by Kattchee and Reynolds in the HECTIC code.<sup>(3)</sup> Combining Eq. (2.8) with Eqs. (2.6) and (2.7) and expressing  $u^*$  in terms of the friction factor,  $u^* = u \sqrt{f/8}$ , we obtain, in dimensional variables,

$$\tau_R = 0.2 \rho \left( \frac{h}{z} \right)_{ij} \bar{u} \Delta u \sqrt{f/8} = 0.1 \rho \left( \frac{h}{z} \right)_{ij} \sqrt{f/8} (u_i^2 - u_j^2) \quad (2.9)$$

Scaling Eq. (2.9) by the characteristic shear stress

$$\tau_c = \rho_c u_c^2 f_c / 8$$

results in

$$\frac{s_c}{p_c} \tau'_R = X_M \rho' u' \Delta u' \sqrt{f'} \quad (2.10)$$

where

$$X_M = \frac{0.2}{\sqrt{f_c/8}} \left( \frac{s_c}{p_c} \right) \left( \frac{h}{z} \right)_{ij} = O(1)$$

is the momentum mixing parameter. Dropping the primes, which signify scaled variables, Eq. (2.10) is the term appearing in Eq. (2.2) in the system of subchannel equations. In a completely analogous manner, using the variables in Table 2.1, the Reynolds heat flux formulation of Eq. (2.4) was derived, with  $X_H = X_M K_q / K_\tau$ . It can be seen from these preceding equations that the turbulent mixing increases with the square root of the friction factor. Thus, for a friction factor multiplier of 4 in the surface-roughened section of the GCFR fuel-rod assembly, the turbulent mixing should increase by a factor of two from the value for smooth rods.

2.1.1.3. Solution of the Fundamental Equations. The fundamental equations are obtained from the system Eqs. (2.1) through (2.5) in the limit as  $\epsilon$ ,  $\delta$ ,  $\gamma$ , and  $\beta$  approach zero. From Eq. (2.3), it is then seen that the pressures are equal in all subchannels; from Eq. (2.4), the temperatures are equal in this limit; and from Eq. (2.5), the densities are also equal. Thus, the fundamental equations are

$$\frac{d}{dx} (GA)_i + C_{ik} (\rho_o v_o s)_k = 0 \quad , \quad (2.11)$$

$$(G^2 Pf)_i + C_{ik} (\bar{G} \Delta G X_M s \sqrt{f})_k = -\rho_o A_i \frac{dp_o}{dx} \quad , \quad (2.12)$$

where  $G_i = (\rho_o u_o)_i$  is the dimensionless mass velocity and where the subscript  $o$  denotes the lowest-order fundamental variables. In these equations, the terms represented by  $C_{ik} ( )_k$  are fluxes of mass and momentum between the subchannels; it is clear that the summation of these fluxes over all subchannels must be zero, i.e.,

$$\sum_{i=1}^{n_s} C_{ik} ( )_k = 0 \quad .$$

Then, from Eqs. (2.4), (2.11), and (2.12), we obtain the auxiliary equations

$$\sum_{i=1}^{n_s} (GA)_i \frac{dT_o}{dx} = \sum_{i=1}^{n_s} (P^h q)_i \quad , \quad (2.13)$$

$$\frac{d}{dx} \sum_{i=1}^{n_s} (GA)_i = 0 \quad , \quad (2.14)$$

$$\sum_{i=1}^{n_s} (G^2 Pf)_i = -\rho_o \frac{dp_o}{dx} \sum_{i=1}^{n_s} A_i = -n_s \rho_o \frac{dp_o}{dx} \quad . \quad (2.15)$$

Equation (2.14) states that the summation of all axial mass flows is a constant. In fact, because of the scaling used,

$$\sum_{i=1}^{n_s} (GA)_i = n_s \quad . \quad (2.16)$$

Equation (2.13) then defines  $T_o(x)$ , and hence  $\rho_o(x)$ . To the lowest order, we consider only constant areas,  $A_i$ , so Eq. (2.16) implies that each  $G_i$  is a constant. We will restrict the friction factors,  $f_i$ , to be functions of the Reynolds numbers only and not explicit functions of  $x$ ; then, suppressing the weak temperature dependence of the viscosity in the Reynolds number, the left side of Eq. (2.15) must be a constant, say  $cn_s$ . Thus, from Eqs. (2.12) and (2.15), we then obtain

$$F_i \equiv (G^2 Pf)_i + C_{ik} (\bar{G} \Delta G X_M s \sqrt{f})_k - c A_i = 0 \quad , \quad (2.17)$$

where

$$c = \frac{1}{n_s} \sum_{i=1}^{n_s} (G^2 Pf)_i \quad (2.18)$$

and where the  $G_i$ 's must satisfy Eq. (2.16). These equations, Eqs. (2.16) and (2.17), are the fundamental, or lowest-order or reduced, equations to be solved for  $G_i$  and  $c$ . Since the  $G_i$ 's are constants, Eq. (2.11) is satisfied by setting  $v_{ok} = 0$ . Because the FLOMAX code is designed to solve very large rod-bundle problems, Eq. (2.17) represents a large system of nonlinear algebraic equations that must be solved carefully for an efficient code.

It can be seen that for the case of constant friction factors, Eq. (2.17) can be written in the matrix form  $\underline{A} \underline{y} = \underline{b}$ , where  $y_i = G_i^2$ . Thus, for a given  $c$ , the  $G_i$ 's could be determined from the solution of the linear system, and  $G_i = \sqrt{y_i}$ . However, the evaluation of square roots on the computer takes over fifty times as long as arithmetic operations; therefore,

Eqs. (2.16) through (2.18) were solved by an iterative scheme. By expanding the  $G_i$ 's in Eq. (2.17) in a Taylor series about an initial guess,  $G_i^{(0)}$ , and assuming a value of  $c$ , a linear system is obtained to solve for the new mass velocities,  $G_i^{(1)}$ :

$$J_{ij}^{(0)} G_j^{(1)} = b_i^{(0)} \equiv J_{ij}^{(0)} G_j^{(0)} - F_i^{(0)}, \quad (2.19)$$

where  $J$  is the Jacobean matrix,  $J_{ij} = \partial F_i / \partial G_j$ . The Jacobian of this system is sparse, and it has the desirable property of being diagonally dominant; physically this means that the subchannel flows are determined mostly by the shear stresses at the rod surfaces, and not so much by the Reynolds stresses between the subchannels. Since Eq. (2.19) is diagonally dominant, it can be solved iteratively by the successive over-relaxation method (SOR). Thus, we obtain the recursive relationship

$$G_i^{(1)} = \frac{0.5 b_i^{(0)} + \sum_j S_{ij} (G_j^{(0)} G_i^{(1)})_j}{(G^{(0)} Pf)_i + G_i^{(0)} \sum_j S_{ij}}, \quad (2.20)$$

where

$$S_{ij} = \frac{1}{2} (sX_M \sqrt{f})_{ij}$$

and where the summations on  $j$  are over the subchannels connected to subchannel  $i$ .

With the  $G_i^{(1)}$ 's determined from the inner iterations in Eq. (2.20), it is then necessary to reevaluate  $c$  and to satisfy Eq. (2.16). This was done by defining  $G_i^{(2)} = G_i^{(1)} / k$ , where, from Eq. (2.16),

$$k = \frac{1}{n_s} \sum_{i=1}^{n_s} (G_i^{(1)})_A, \quad ,$$

and the new  $c$  is determined from Eq. (2.18) using the  $G_i^{(2)}$ 's. Then, the

Jacobian, the vector  $b_i$ , and the  $S_{ij}$ 's are evaluated with the  $G_i^{(2)}$ 's and so on until convergence of all the equations. In evaluating the Jacobian, derivatives were not taken of the friction factors; in this way, arbitrary friction-factor functions can be used and no numerical differentiation is required.

To evaluate the above solution scheme, a small program was written for solving the flows in the BR-2 assembly, which is shown in Fig. 2.1. This assembly was chosen as a test case because numerical calculations had already been performed with the COBRA finite-difference code, as reported in the last quarterly report.<sup>(4)</sup>

It was found that about five inner iterations (Eq. (2.20)) were sufficient for convergence; thus, every fifth iteration the various functions were reevaluated in terms of the new  $G_i$ 's. The convergence of a typical case is shown in Fig. 2.2 (for subchannels 1 and 25 of Fig. 2.1). It can be seen that the iterative scheme converges very well. The jumps in the curves at 5 and 10 iterations are due to function reevaluations. Over or under relaxation was not employed in the above scheme, so it really corresponds to the Gauss-Seidell iteration for the inner iterations and to the Newton-Raphson iteration for the outer iterations.

The results from several calculations are summarized in Table 2.4. For comparison, COBRA calculations were made without heat generation in the rods and with constant and variable friction factors. The same turbulent mixing factors were used in FLOMAX and COBRA, and there is excellent agreement between the results. In Case 1, with  $f = 0.02$  for all subchannels, the velocities decrease from subchannel 1 to subchannel 25, as expected. For higher friction factors, Case 3, the velocities in central subchannels are increased and the edge subchannel velocities are decreased. For Reynolds-number-dependent friction factors, Case 2, the velocities in central subchannels are still higher, as they should be. In Case 4, another velocity distribution was obtained by selecting different friction factors. Cases 5 and 6 are identical to Case 1 except that heat was generated in the rods. In these COBRA calculations, it was found that the  $G_i$ 's were very nearly constant from inlet to outlet and the cross flows

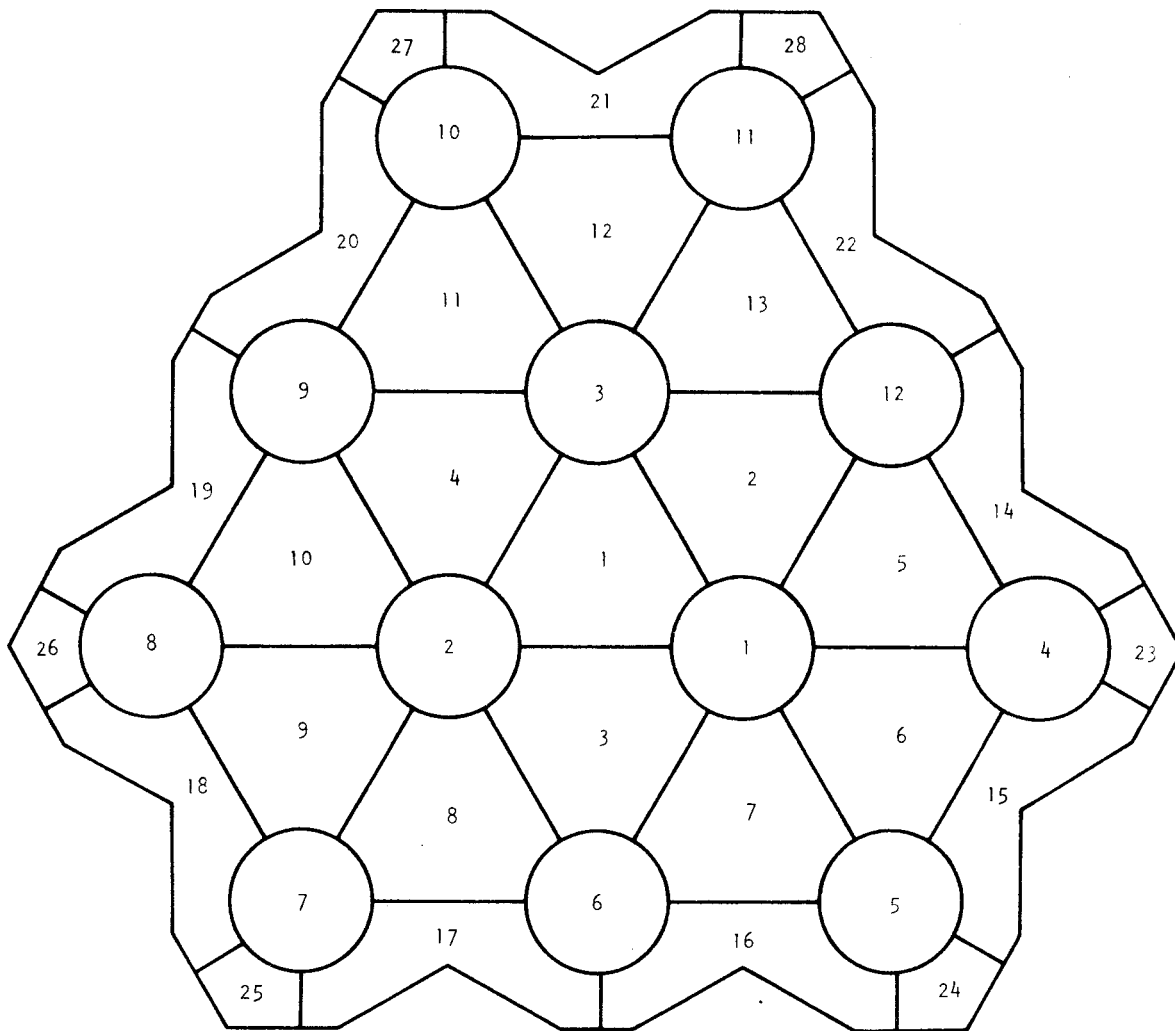


Fig. 2.1 Subchannel layout of an experimental fuel assembly (not to scale)

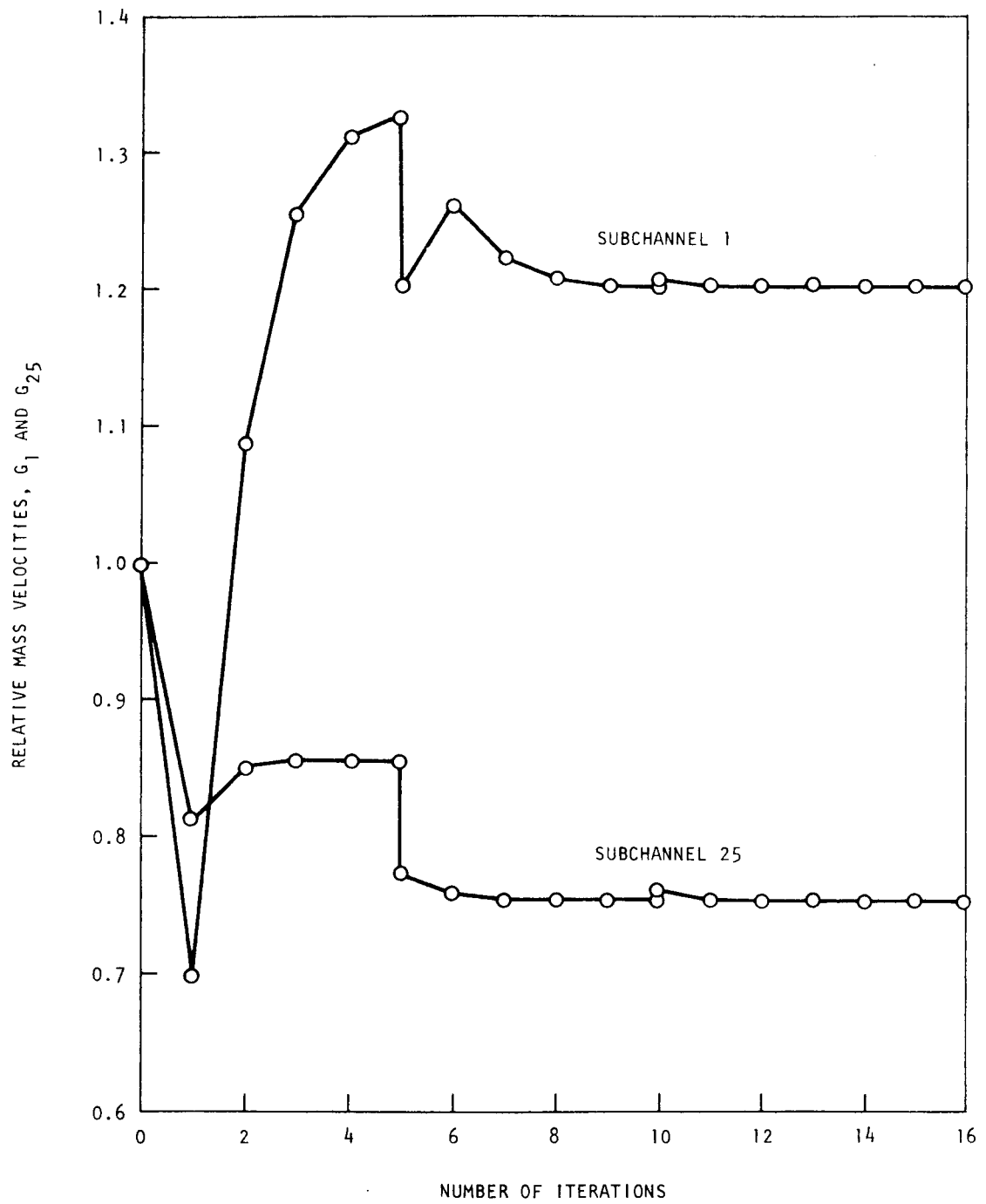


Fig. 2.2 Convergence behavior of the FLOMAX solution scheme

Table 2.4

COMPARISON OF SCALED MASS-VELOCITIES FROM FLOMAX AND COBRA CALCULATIONS

| Case | Code          | $G_1$  | $G_3$  | $G_8$  | $G_9$  | $G_{17}$ | $G_{18}$ | $G_{25}$ | Comments                                                                                                                                 |
|------|---------------|--------|--------|--------|--------|----------|----------|----------|------------------------------------------------------------------------------------------------------------------------------------------|
| 1    | FLOMAX        | 1.1542 | 1.1461 | 1.1138 | 1.1091 | 0.8818   | 0.8768   | 0.8074   | Constant friction factors,<br>$f = 0.02$ for all subchannels                                                                             |
|      | COBRA         | 1.1557 | 1.1495 | 1.1286 | 1.1267 | 0.8643   | 0.8614   | 0.7895   |                                                                                                                                          |
| 2    | FLOMAX        | 1.2049 | 1.1928 | 1.1483 | 1.1414 | 0.8451   | 0.8387   | 0.7487   | Reynolds-number-dependent<br>friction factors,<br>$f_i = 0.184 \text{Re}_i^{-0.2}$ ,<br>$i = 1, 25$ .                                    |
|      | COBRA         | 1.2099 | 1.2013 | 1.1707 | 1.1679 | 0.8238   | 0.8209   | 0.7288   |                                                                                                                                          |
| 3    | FLOMAX        | 1.1560 | 1.1527 | 1.1318 | 1.1300 | 0.8651   | 0.8628   | 0.7902   | Constant friction factors,<br>$f = 0.10$ for all subchannels                                                                             |
|      | COBRA         | 1.1543 | 1.1514 | 1.1457 | 1.1457 | 0.8495   | 0.8490   | 0.7771   |                                                                                                                                          |
| 4    | FLOMAX        | 0.9906 | 0.9909 | 0.9932 | 0.9931 | 1.0213   | 1.0183   | 0.9590   | Different constant friction<br>factors; $f_i = 0.1$ , $i = 1$ ,<br>13; $f_i = 0.02$ , $i = 14, 25$                                       |
|      | COBRA         | 0.9905 | 0.9886 | 0.9890 | 0.9890 | 1.0233   | 1.0224   | 0.9448   |                                                                                                                                          |
| 5    | COBRA<br>only | 1.1495 | 1.1419 | 1.1200 | 1.1171 | 0.8705   | 0.8681   | 0.8100   | Constant friction factors,<br>$f = 0.02$ for all subchannels;<br>but uniform heat generation<br>in all rods for the COBRA<br>calculation |
| 6    | COBRA<br>only | 0.9405 | 1.1243 | 1.1300 | 1.1333 | 0.8838   | 0.8814   | 0.8214   | Same as Case 5, but twice<br>the heat generation in rods<br>1, 2, and 3.                                                                 |

were very small. This is in agreement with Eq. (2.14), which states that to the lowest order, the  $G_i$ 's are constants for arbitrary heat generation.

In conclusion, the lowest-order solution of the FLOMAX subchannel equations has been obtained using the preceding iterative scheme. For further comparison, COBRA calculations were made for two distributions of heat generation in the rods, as listed in Table 2.4. It can be seen that there is a readjustment of the mass velocities, especially in Case 6 where the three central rods were generating twice the heat of the other rods. This effect will be included in FLOMAX in the first-order correction to the fundamental solution. Cases 5 and 6 were for constant friction factors. Probably viscosity changes with temperature will cause a slight axial variation of the mass velocities, but this will be included in the first-order FLOMAX equations. In the FLOMAX development, it is now necessary to code the fundamental solution for general problems and to develop the first-order corrections. This work is currently under way.

#### 2.1.2. Structural Analysis of Fuel-assembly Components

A computer program was written to calculate the dilation of the assembly duct due to internal pressure and irradiation creep and swelling. The code, which is not operational, is based on the correspondence principle of viscoelasticity where a time-dependent deformation can be obtained from the elastic solution by replacing Young's modulus,  $E$ , with an equivalent modulus,  $E + (1/J)$ . The creep compliance,  $J$ , is given by

$$J = \frac{\epsilon(t)}{\sigma} = \text{evaluated at the time of interest,}$$

$\epsilon(t)$  = the time-dependent strain, independent of  $\sigma$ ,  
 $\sigma$  = the stress.

A comparison given in the previous quarterly report has shown justification for using this method by reason of its excellent agreement with finite methods. The duct wall was modeled by a simple cantilever beam fixed at both ends, subject to pressure on one surface, to a constant temperature (no  $\Delta T$  through or along the beam), and to a constant neutron flux. The  $J$  modulus is obtained from the LIFE-III interim equation for irradiation creep, neglecting the dependency on swelling. An additional deformation is evaluated from the LIFE-III swelling correlation and added

separately. Consequently, the output of the code consists of an elastic deformation due to pressure alone, an elastic plus irradiation creep deformation, an elastic plus creep plus swelling deformation, and the maximum stress for the axial locations specified.

Initially, the elements subjected to the highest flux in each of the seven radial rows of the core were analyzed. Dilations at 12,000 hr and 18,000 hr were computed. The maximum deformations at twenty-four axial points along each element were obtained. Of these, the greatest dilation that occurred in each element is given in Table 2.5 for both 12,000 and 18,000 hr. The total dilation listed is the elastic plus creep plus swelling distortion. The  $\delta$  is the distortion, in inches, and the axial position is the distance from the top of the element at which this distortion is observed. Note that most of the dilation can be attributed to irradiation-induced creep of the element wall under the pressure loading. These values will be evaluated with respect to possible interference of adjacent elements for potential improvement in the fuel management sequence. Later, a more detailed analysis across the core will be made and combined with the element bowing analysis to obtain core deformation maps.

For calculations of the fuel-assembly bowing, the BEAM-CREEP code is now fully operational and partially verified. It has been applied to the fuel assembly experiencing the greatest flux gradient in the outer row. The initial run for the assembly was extended to 18,000 hr in three 6,000-hr intervals. The cooled condition of each element was recorded on a data file (at 6,000 and 12,000 hr) for subsequent restart to assess various rotation sequences. The initial thermal displacement of the lower end of the fuel assembly was 8.1 mm, which, after 6,000 hr of operation, increased to 8.6 mm and at 12,000 hr to 22.2 mm. Of this, 14.1 mm was permanent distortion. If operations were continued to 18,000 hr without rotation, a total displacement of 48.3 mm would occur, of which 40.2 mm is permanent. If the element is rotated 180° and then run to 18,000 hr, the final distortion will be 18.8 mm, 10.7 mm of which is permanent.

## 2.2. BLANKET ASSEMBLY ANALYSIS

Analytical methods are being developed to perform the thermal-hydraulic analysis of the GCFR blanket element. The methods should be capable of

Table 2.5  
MAXIMUM DILATION OF FUEL-ELEMENT DUCT WALL

| Radial<br>Position |                                  | 12,000 Hr                |                                              | 18,000 Hr                |                                              |
|--------------------|----------------------------------|--------------------------|----------------------------------------------|--------------------------|----------------------------------------------|
|                    |                                  | Elastic<br>plus<br>Creep | Elastic<br>plus<br>Creep<br>plus<br>Swelling | Elastic<br>plus<br>Creep | Elastic<br>plus<br>Creep<br>plus<br>Swelling |
| 1                  | $\delta$ , in.                   | 0.073                    | 0.119                                        | 0.106                    | 0.207                                        |
|                    | Axial position, in. <sup>a</sup> | 50.                      | 66.                                          | 50.                      | 66.                                          |
| 2                  | $\delta$ , in.                   | 0.072                    | 0.115                                        | 0.104                    | 0.200                                        |
|                    | Axial position, in.              | 50.                      | 66.                                          | 50.                      | 66.                                          |
| 3                  | $\delta$ , in.                   | 0.072                    | 0.116                                        | 0.104                    | 0.203                                        |
|                    | Axial position, in.              | 50.                      | 66.                                          | 50.                      | 66.                                          |
| 4                  | $\delta$ , in.                   | 0.068                    | 0.099                                        | 0.098                    | 0.175                                        |
|                    | Axial position, in.              | 50.                      | 66.                                          | 50.                      | 66.                                          |
| 5                  | $\delta$ , in.                   | 0.067                    | 0.101                                        | 0.096                    | 0.179                                        |
|                    | Axial position, in.              | 50.                      | 64.                                          | 50.                      | 66.                                          |
| 6                  | $\delta$ , in.                   | 0.055                    | 0.065                                        | 0.079                    | 0.114                                        |
|                    | Axial position, in.              | 50.                      | 62.                                          | 50.                      | 66.                                          |
| 7                  | $\delta$ , in.                   | 0.050                    | 0.059                                        | 0.071                    | 0.107                                        |
|                    | Axial position, in.              | 50.                      | 62.                                          | 50.                      | 66.                                          |

<sup>a</sup>Axial position is from top of element.

analyzing the unique geometry (closely spaced rods and flow baffles) and the large thermal gradients to which the blanket element is subjected.

During the previous quarterly period, some of the available methods for steady-state thermal-hydraulic analysis were reviewed. Among the heat-exchanger codes and the network codes, the network codes were found to be more suitable and gave excellent agreement between experiments and analysis.<sup>(4)</sup> The review of available methods was continued with a review of COBRA-IV and THI-3D<sup>(5)</sup> codes.

#### 2.2.1. Review of COBRA-IV for Steady-state Thermal-Hydraulic Analysis

The COBRA-IV code was used to perform a preliminary thermal analysis of the blanket element for the hottest element during full-flow conditions. The model consisted of a central strip from the blanket element with 26 subchannels (see Fig. 2.3). Three situations were studied: (1) a blanket element without baffles at a bulk outlet temperature of 550°C, (2) a blanket element without baffles in an overcooled condition with a bulk outlet temperature of 525°C, and (3) a blanket element with five disc and doughnut baffles.

2.2.1.1. Blanket Element Without Baffles. The temperature distribution of the coolant in the blanket element and the existing radial and axial power distribution is shown in Figs. 2.4 and 2.5, respectively. The average bulk temperature at the outlet is equal to 550°C. The maximum temperature of 658°C in the hottest channel is unacceptable because with the hot-spot factors the hot-spot cladding temperature will exceed the design limit of 700°C. Subsequent methods could be used to reduce the maximum temperatures.

2.2.1.2. Overcooled Blanket Element. The effect of 12% overcooling is to reduce the maximum temperature of the coolant to 620°C. For this condition, the maximum hot-spot cladding temperature will be within the safe limit. However, overcooling also reduces the bulk outlet temperature.

Approximately 10% of the total reactor coolant flows through the blanket region. Hence, 12% overcooling will reduce the average core

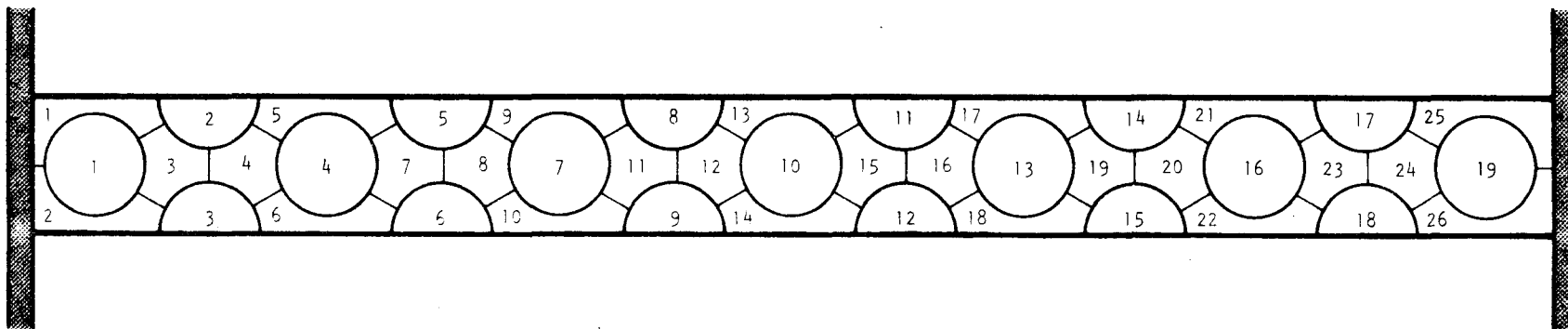


Fig. 2.3 Central strip of the blanket assembly

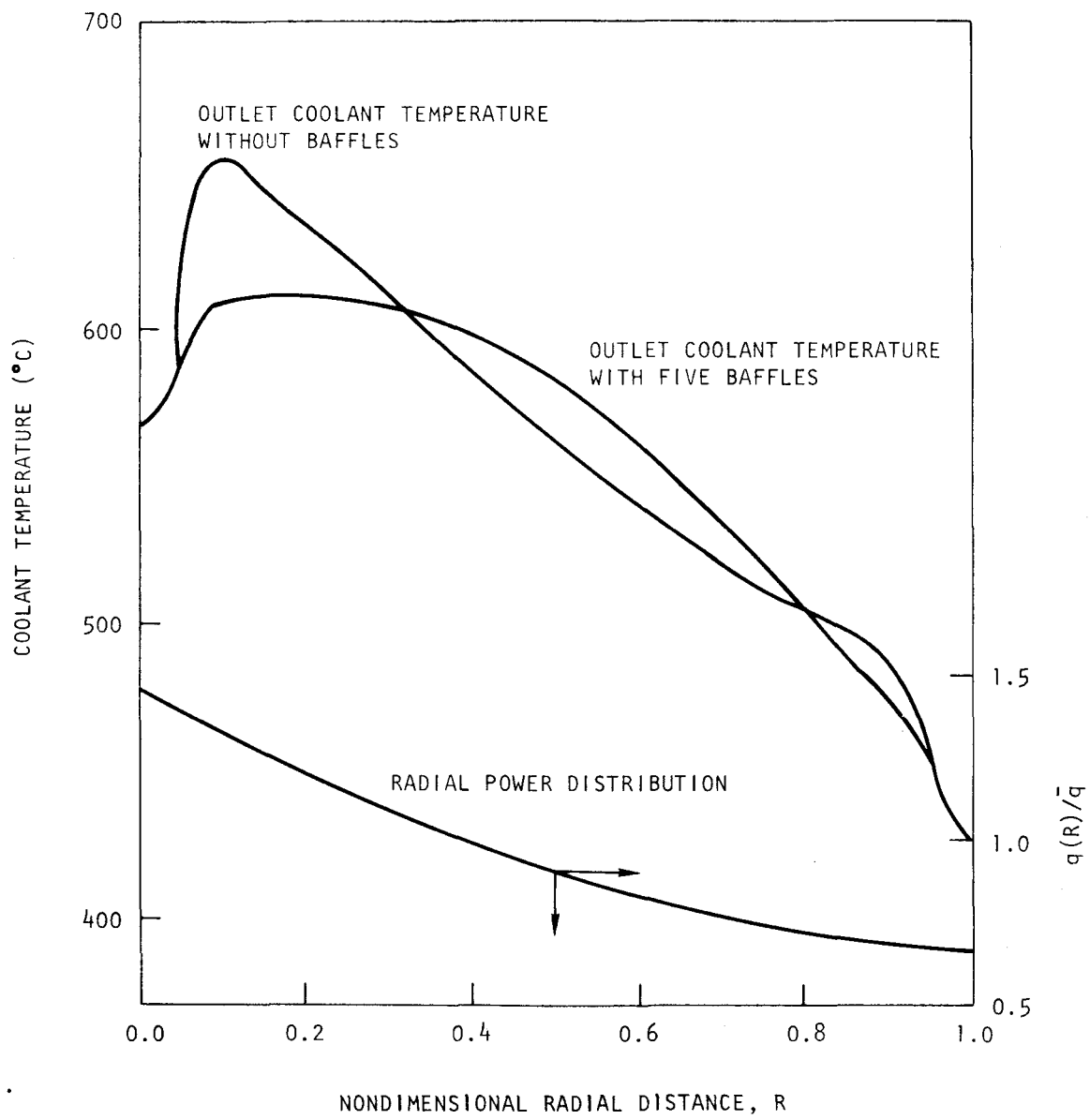


Fig. 2.4 Radial power distribution and temperature from the outlet of the blanket element

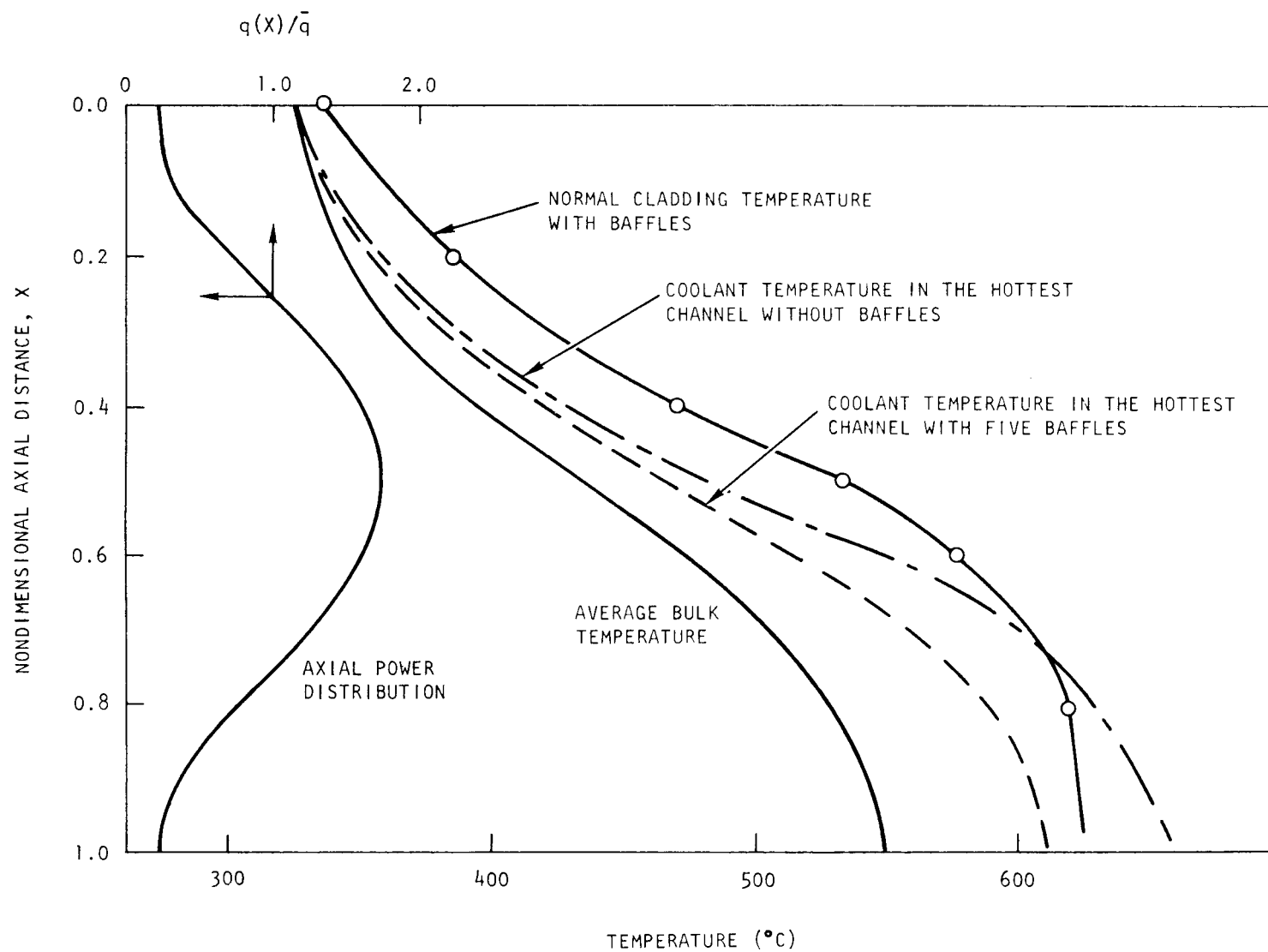


Fig. 2.5 Axial power distribution and temperature from the outlet of the blanket element

outlet temperature by 3°C and will degrade the overall reactor thermal performance.

2.2.1.3. Use of Baffles. Flow baffles will reduce the maximum coolant temperature without altering the bulk outlet temperature from the blanket element. In the GCFR reference design, asymmetrical baffles are used. Since these baffles are not symmetrical about the axis of the blanket element, they will restrict rotating the blanket element. An alternative design under consideration is the use of disc and doughnut baffles. These baffles are symmetrical about the center and are shaped like a disc and doughnut at alternate axial locations. Disc and doughnut baffles have the advantage of symmetry about the center of the blanket element and thus do not have any restriction on rotating and shuffling the blanket elements.

The results of using 5 disc and doughnut baffles are shown in Figs. 2.4 and 2.5. The peak fluid temperature has been reduced by 45°C.

In conclusion, COBRA-IV appears to be capable of analyzing the baffled geometry of the blanket assembly. At the present time, some convergence difficulty was experienced, which resulted in large loss coefficients. If this is overcome by modifications, COBRA-IV would be a satisfactory method for thermal-hydraulic analysis of the radial blanket element.

#### 2.2.2. Review of THI-3D Code

The THI-3D code,<sup>(5)</sup> which was developed at Argonne National Laboratory (ANL), is a steady-state subchannel code. The code solves the subchannel conservation equations as a boundary value problem and is capable of accounting for a diversion cross flow due to grids or baffles. Although a complete cross-flow momentum model is used in the formulation, this option is not operational yet. The code does not converge for low cross-flow resistance.

The THI-3D code will need considerable development and comparison with experiments before it can be used for blanket-element analysis.

#### 2.2.3. Thermal-Hydraulic Analysis of the Blanket Element During Low-power Operation

Because of the contents of the blanket assemblies and their location

at the perimeter of the core, the heat-generation rate in the blanket elements is much lower than the heat-generation in the fuel elements. Also, because of the steep neutron gradients in the blanket-element region, power generation occurs at a rapidly decreasing rate in the radial direction. Within each element, the power-generation rate and power gradient is constantly changing because of the buildup of fissile material. The operating flow conditions to which blanket elements are subjected cover all three coolant flow regimes: turbulent, transition, and laminar. During low-flow conditions, the possibility exists that the natural convection effects acting in a direction opposite to the forced convection may cause considerable flow distortion and, in extreme cases, flow reversals.

A review of the literature indicates that no analytical or experimental results exist for the combined forced convection and natural convection, which has buoyancy forces that are in a direction opposite to the flow, in the presence of radially varying heat generation. Hence this analysis was undertaken.

In this analysis, attention was focused on the flow region where natural-convection effects dominate. For the purpose of this analysis, a central portion of the fuel-rod bundle was approximated by a narrow rectangular strip and two-dimensional behavior was assumed. For the vertical channel considered for the analysis, the energy added to the fluid can be represented by

$$q'''(x, y) = q'''_{\max} \cos[c_1(c_2 - y)] e^{-\gamma x} . \quad (2.21)$$

For two-dimensional laminar flow conditions, with constant properties (except variation of density in the buoyancy term), the conservation equations are:

$$\frac{\partial^2 \psi}{\partial X^2} + \frac{\partial^2 \psi}{\partial Y^2} = -\omega , \quad (2.22)$$

$$\frac{\partial(U\omega)}{\partial X} + \frac{\partial(V\omega)}{\partial Y} = \frac{1}{\text{Re}} \left( \frac{\partial^2 \omega}{\partial X^2} + \frac{\partial^2 \omega}{\partial Y^2} \right) + \frac{\text{Gr}}{\text{Re}^2} \frac{\partial \theta}{\partial X} - \frac{32}{\text{Re}} \frac{\partial^2 \psi}{\partial X^2} , \quad (2.23)$$

$$\frac{\partial(U\theta)}{\partial X} + \frac{\partial(V\theta)}{\partial Y} = \frac{1}{Pe} \left( \frac{\partial^2 \theta}{\partial X^2} + \frac{\partial^2 \theta}{\partial Y^2} \right) + \frac{q'''(X,Y)}{\bar{q}'''A_y}, \quad (2.24)$$

where  $A_y$  = nondimensional height of the model,  
 $c_1, c_2$  = constants,  
 $Gr$  = Grashof number,  
 $Pe$  = Peclet number,  $Re Pr$ ,  
 $Pr$  = Prandtl number,  $\nu/\alpha$ ,  
 $q'''$  = heat generation per unit volume,  
 $\bar{q}'''$  = average heat generation per unit volume,  
 $r$  = radial power skew,  $q'''_{max}/q'''_{min}$ ,  
 $Re$  = Reynolds number,  
 $U$  = nondimensional velocity along X-coordinate,  
 $V$  = nondimensional velocity along Y-coordinate,  
 $X, Y$  = nondimensional coordinates,  
 $\theta$  = nondimensional temperature,  
 $\psi$  = nondimensional stream function,  
 $\omega$  = nondimensional vorticity.

The coupled Eqs. (2.22) through (2.24) are elliptic in nature. A number of methods exist to solve such a system numerically. One of these, suggested first by Courant, et al.,<sup>(6)</sup> and later modified by Gosman, et al.,<sup>(7)</sup> was adopted for the present study.

The governing Eqs. (2.22) through (2.24) indicate that the nondimensional parameters governing the flow and temperature distribution in the radial blanket are the Reynolds number,  $Re$ , the ratio of Grashof number to Reynolds number squared,  $Gr/Re^2$ , and the power skew  $q'''_{max}/q'''_{min}$ . The variation of the Prandtl number is negligible.

2.2.3.1. Effect of Buoyancy Forces. Observation of Eq. (2.23) shows that the effect of buoyancy forces is described by the term  $Gr/Re^2$ . During

normal power operation of the reactor, this quantity is of the order of  $10^{-5}$ . However, during low-flow operating conditions,  $Gr/Re^2$  will increase by several orders of magnitude.

The effects of variation of  $Gr/Re^2$  for a  $Gr = 10^4$  and a power skew of 1.5:1 are shown in Figs. 2.6 and 2.7. At large values of  $Gr/Re^2$ , the velocity profile is considerably distorted. Since the distortion of the velocity profile causes more flow in the area with less generation and vice versa, the temperature distribution exhibits a skew greater than the power skew. However, no flow reversal was observed in the expected operating range of the GCFR blanket element. The effect of buoyancy forces on the distortion of velocity and temperature profiles decreases with  $Gr/Re^2$ . The effect is negligible for  $Gr/Re^2$  less than 0.003. Below this value of the  $Gr/Re^2$  parameter, the velocity is almost uniform and the temperature distribution follows the power skew.

2.2.3.2. Effect of Radial Power Skew. The effects of radial power skew on the velocity and temperature profiles at  $Re = 1000$  and  $Gr = 10^4$  are shown in Figs. 2.8 and 2.9. As can be expected, distortion of the velocity and temperature profiles is more at larger power skews. The distortion of the velocity profile, which can be considered as a measure of the effect of buoyancy forces, is quite small for a power skew less than 1.3:1.

2.2.3.3. Effect of Axial Power Skew. A comparison was made of the velocity and temperature profiles at the outlet of the blanket assembly for a given Grashof number, Reynolds number, and power skew for a chopped cosine and uniform axial power distribution. No significant effect on the outlet condition due to a change in axial distribution of power was observed. This result is of significance in planning the flow tests with heated rod bundles.

In the present analysis, the effect of baffles or any such mechanism used for inducing mixing were not considered. Theoretically, such effects could be considered by the addition of suitable "loss terms" in the y momentum equation. However, these effects presented convergence difficulties in the present numerical solution.

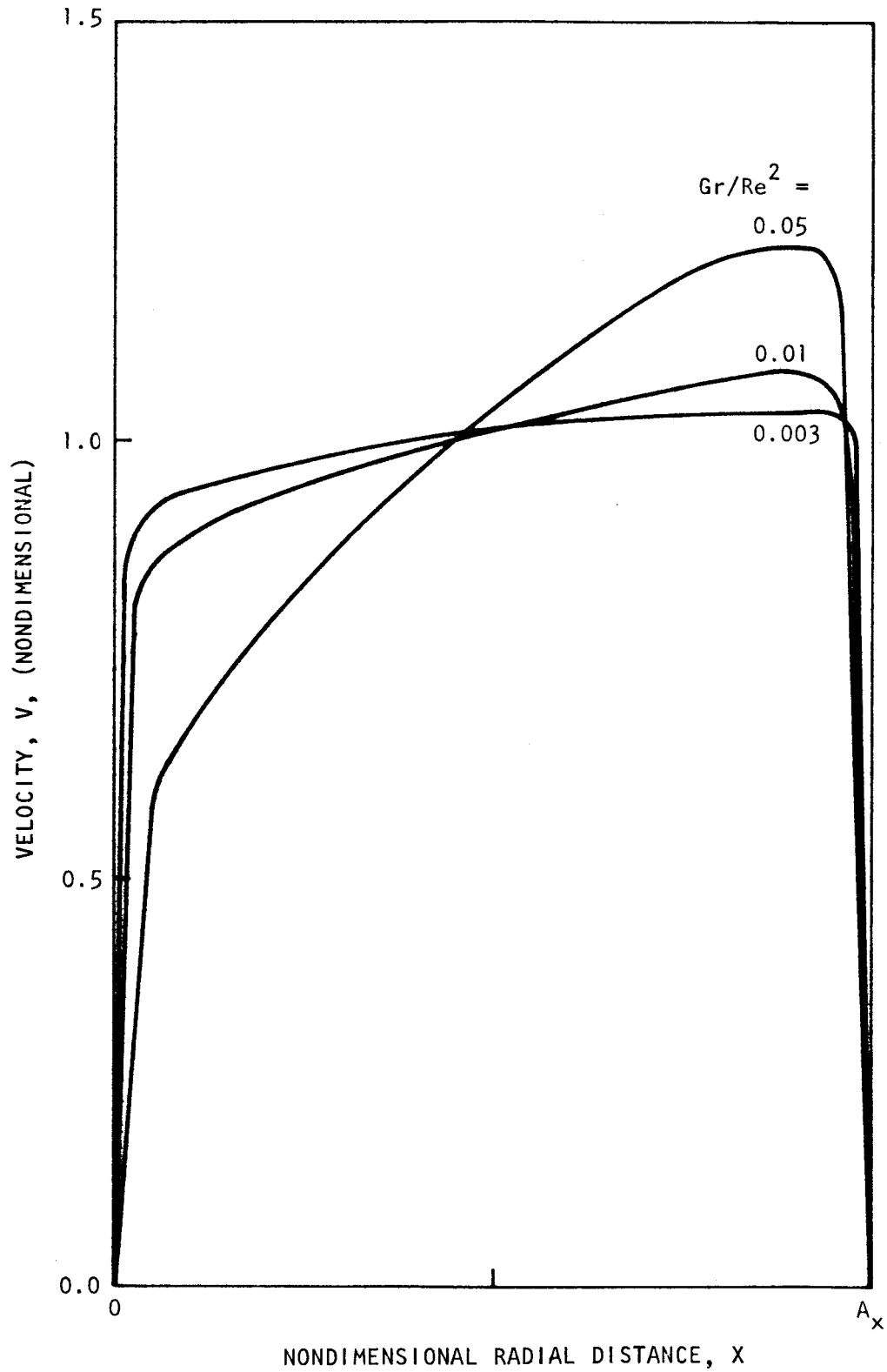


Fig. 2.6 Effect of buoyancy forces on velocity distribution at the blanket element outlet for  $Gr = 10^4$  and radial power skew = 1.5:1

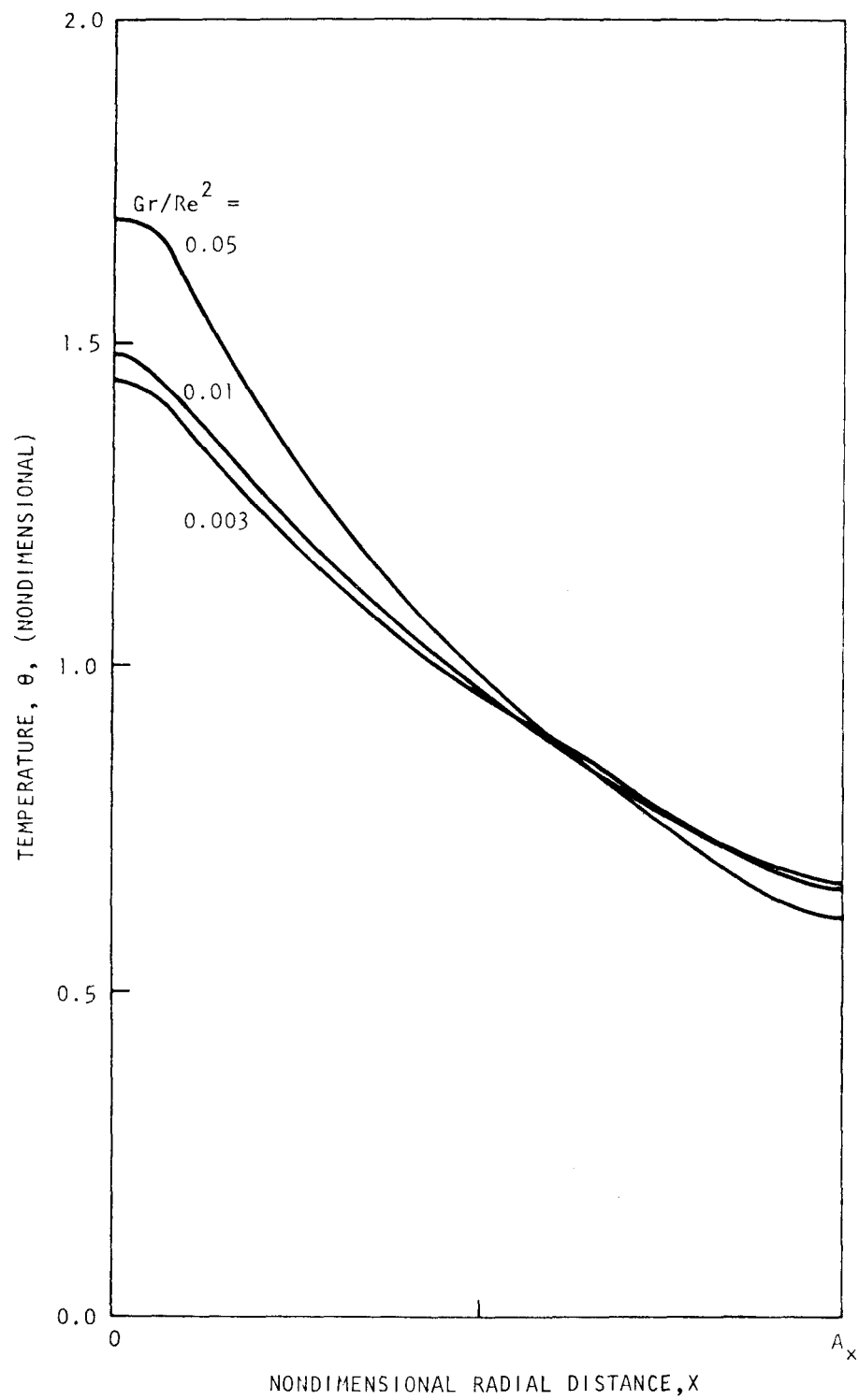


Fig. 2.7 Effect of buoyancy forces on temperature distribution at the blanket element outlet for  $Gr = 10^4$  and radial power skew = 1.5:1

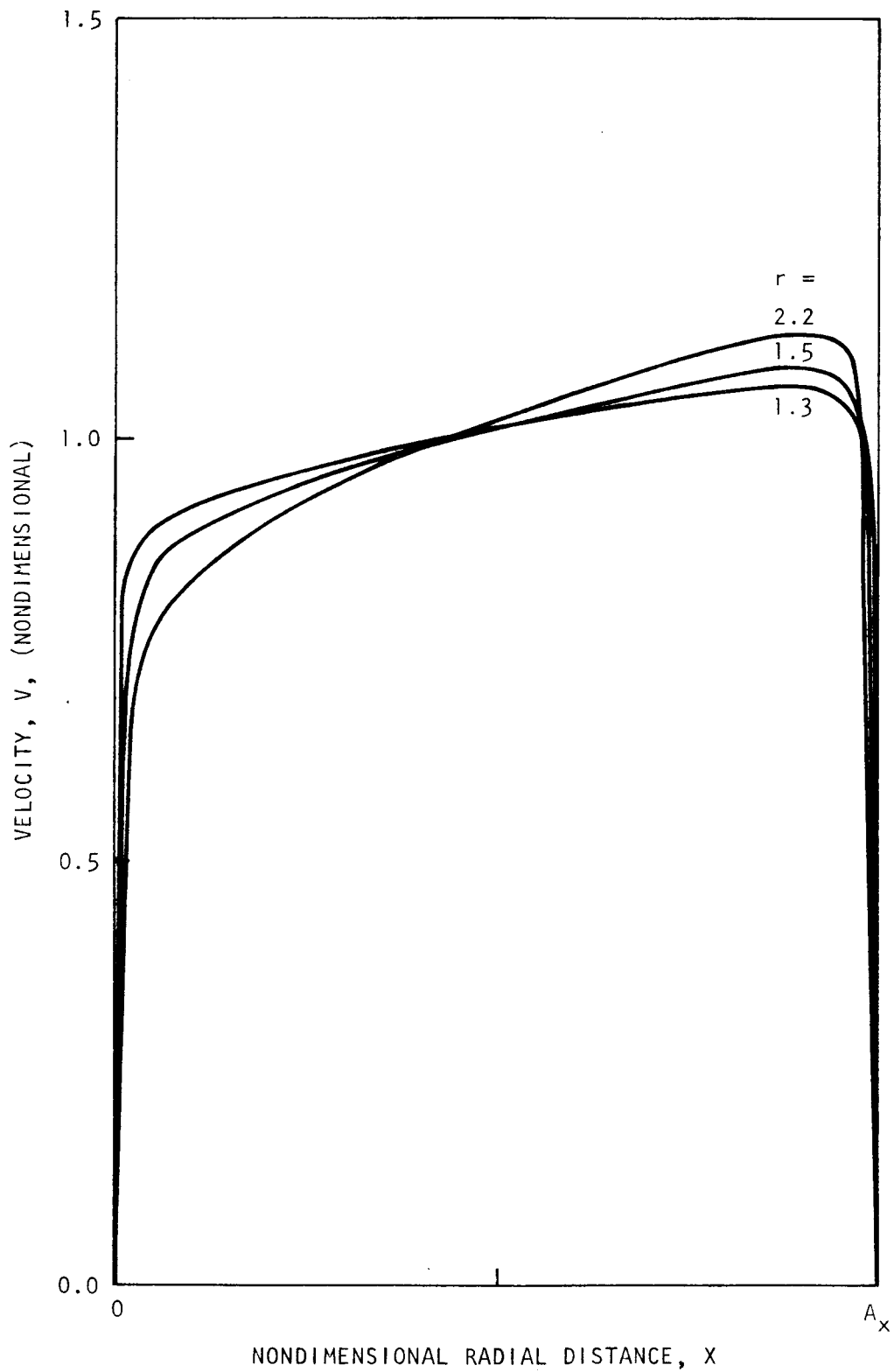


Fig. 2.8 Effect of power skew on velocity distributions at the blanket element outlet for  $Gr = 10^4$  and  $Re = 1000$

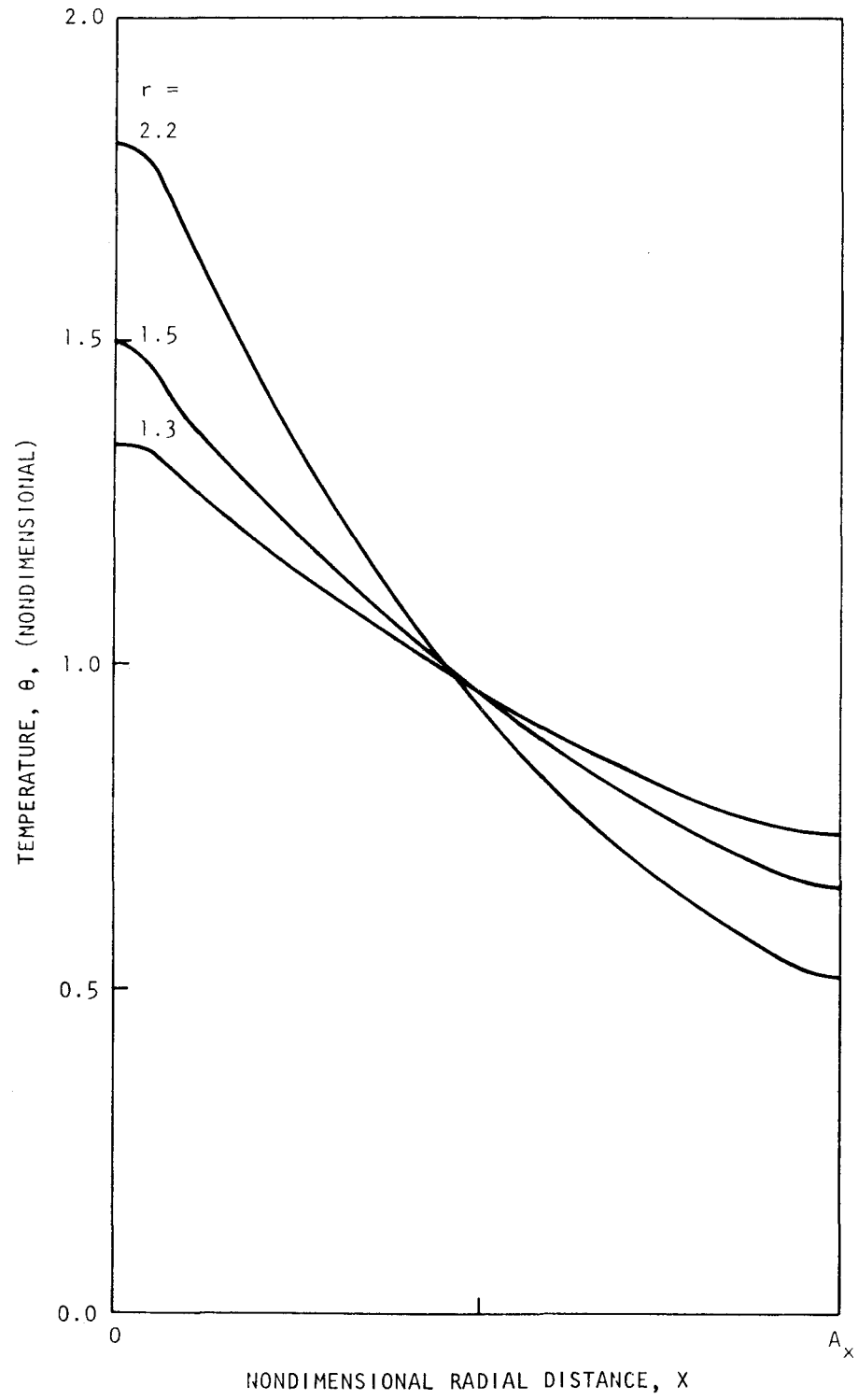


Fig. 2.9 Effect of power skew on temperature distribution at the blanket element outlet for  $Gr = 10^4$  and  $Re = 1000$

### 2.3. ASSEMBLY MECHANICAL TESTING

The current component mechanical testing phase being conducted is the fuel-rod-spacer-grid interaction tests, and preliminary planning for the seismic and flow-induced-vibration tests on core element assemblies has been initiated. The rod-spacer interaction test includes evaluation of the adhesion, friction, and wear characteristics between the fuel rod and spacer as a function of environmental and material surface conditions. A program plan has been prepared and a preliminary analysis has been made for the seismic and flow-induced-vibration tests.

#### 2.3.1. Rod-Spacer Interaction Tests

The purpose of the rod-spacer interaction tests is the evaluation of the interaction between the rod and the spacer in the range of temperature and impurity levels (primarily water and hydrogen) expected in the GCFR helium coolant. These interactions arise from the relative motion between the rod and the spacer due to a normal operating load and to various reactor transients that cause contact of the rod and spacer. This interaction leads to friction and wear and possibly adhesion between the rod and the spacer.

The combination of pressed spacers with convex contact surfaces and smooth or ribbed rods was tested initially. Both spacers and rods were of Type 316 stainless steel. Adhesion was observed in tests on smooth rods in an environment containing 900/90  $\mu\text{atm}$  of  $\text{H}_2/\text{H}_2\text{O}$  in helium. No adhesion was observed when the  $\text{H}_2/\text{H}_2\text{O}$  ratio was increased to 100 at the same absolute level of  $\text{H}_2\text{O}$ . Additional tests were conducted using spacers fabricated by the electrodischarge machining (EDM) technique. These spacers had flat or concave contact surfaces with an average surface roughness in the region of 2  $\mu\text{m}$ . No adhesion was observed under the same conditions at which pressed spacers showed adhesion. In addition, adhesion has not been observed with Inconel 718 and Inconel 625 pressed spacers and Type 316 stainless steel smooth rods at a  $\text{H}_2/\text{H}_2\text{O}$  ratio of 10. No adhesion has been observed when ribbed rods were used with any type of spacers.

Tests conducted in the present quarterly period were designed to evaluate the effects of a range of impurity levels as well as the absolute levels

of  $H_2O$  of interest to the GCFR. Since adhesion is clearly not acceptable, these tests were designed to map the entire region of impurity levels from the point of view of adhesion. Previous tests have showed that the problem of adhesion exists only with smooth rods. For this reason, a combination of EDM spacers made at GA and smooth rods were used in these tests. The spacers and rods were of 20% cold-worked Type 316 stainless steel. The tests were conducted at 525°C, which is the highest temperature for smooth rods in the GCFR. No adhesion was observed. The test results are presented in Table 2.6.

The frictional behavior in the first eight tests was somewhat unusual. The coefficient of friction values ranged from a low of 0.3 to a high of 2.4. Deep grooves ( $\sim 0.05$  mm), or wear tracks, on the rods were associated with the high coefficient of friction. The coefficient of friction from previous tests, with the same combination of spacers and rods at a  $H_2/H_2O$  ratio of 10, was in the range of 0.4 to 0.8. The present results did not indicate any correlation between the coefficient of friction and the impurity levels. Hardness measurements on both the previous and the present spacers did not show any difference. The only other parameter that was different in these tests was the roughness of the contact surface of the spacer. Measurements of roughness using the surface analyzer gave an average value of  $\sim 10$   $\mu m$  for the present spacers as compared to 2  $\mu m$  for the previous spacers. Upon investigation, it was found that the present spacers were machined using a copper-tungsten electrode and a mixture of two dielectric solutions, whereas the previous spacers were machined using a copper-graphite electrode and a standard dielectric solution. The same current densities were used for both the previous and present spacers and were expected to yield a No. 27 EDM surface finish, which is equivalent to a surface roughness of 2  $\mu m$ . Four more tests were conducted at  $H_2/H_2O$  levels of 90,000/9,000  $\mu atm$  and 9,000/9,000  $\mu atm$  using spacers machined to a No. 27 EDM surface finish (2  $\mu m$  average). The coefficient of friction ranged from 0.35 to 0.7, indicating that the unusual frictional behavior is related to surface roughness.

Table 2.6

RESULTS OF ROD-SPACER INTERACTION TESTS ON SMOOTH TUBES AND EDM SPACERS AT 525°C

| Impurity Level<br>( $\mu\text{atm}$ ) |                      | Stroke<br>Length<br>(mm) | Total<br>Linear<br>Travel<br>(mm) | Roughness of<br>Spacer Contact<br>Surface<br>( $\mu\text{m}$ ) | Coefficient of<br>Friction |         | Remarks                            |
|---------------------------------------|----------------------|--------------------------|-----------------------------------|----------------------------------------------------------------|----------------------------|---------|------------------------------------|
| $\text{H}_2$                          | $\text{H}_2\text{O}$ |                          |                                   |                                                                | Peak                       | Average |                                    |
| 90                                    | 90                   | 3.81                     | 381                               | 10                                                             | 0.6                        | 0.35    | Deep grooves or wear tracks on rod |
| 90                                    | 90                   | 0.76                     | 3048                              | 10                                                             | 1.1                        | 1.1     |                                    |
| 900                                   | 900                  | 3.81                     | 381                               | 10                                                             | 2.3                        | 1.2     |                                    |
| 900                                   | 900                  | 0.76                     | 3048                              | 10                                                             | 0.9                        | 0.5     |                                    |
| 9,000                                 | 9,000                | 3.81                     | 381                               | 10                                                             | 0.5                        | 0.4     |                                    |
| 9,000                                 | 9,000                | 0.76                     | 3048                              | 10                                                             | 0.5                        | 0.3     | Deep grooves or wear tracks on rod |
| 90,000                                | 9,000                | 3.81                     | 381                               | 10                                                             | 0.7                        | 0.7     |                                    |
| 90,000                                | 9,000                | 0.76                     | 3048                              | 10                                                             | 2.4                        | 1.6     |                                    |
| 90,000                                | 9,000                | 3.81                     | 381                               | 2                                                              | 0.6                        | 0.6     |                                    |
| 90,000                                | 9,000                | 0.76                     | 3048                              | 2                                                              | 0.7                        | 0.6     |                                    |
| 9,000                                 | 9,000                | 3.81                     | 381                               | 2                                                              | 0.35                       | 0.35    |                                    |
| 9,000                                 | 9,000                | 0.76                     | 3048                              | 2                                                              | 0.55                       | 0.35    |                                    |

### 2.3.2. Seismic and Vibration Test Support

The work on the seismic and flow-induced vibration tests of GCFR core elements includes a preliminary analysis and preparation of a test program plan. During the quarter, the review of correlations for flow-induced vibration and the analyses of the effect of the assumption of the rod-support conditions and rod bowing and the potential effects of acoustic-induced vibrations were continued. Development of the model for seismic analysis also continued. The test program plan was completed, which included recommendations for a future test facility utilizing existing equipment.

2.3.2.1. Flow-induced Vibration Correlations. A number of correlations for predicting the amplitude of parallel-flow induced vibrations were previously selected for possible use in determining the response of GCFR fuel rods. These correlations were subsequently compared with test data to determine which, if any, would be suitable for the vibration analysis of GCFR rods.

Although all of the selected correlations appear to have sound analytic derivations and they each give excellent agreement with their respective investigator's own test data, rather poor agreement often results when they are compared with the test data of others. Indeed, when the correlations were applied to determine GCFR fuel-rod response, widely differing response predictions resulted. It was thus apparent that some of the correlations were not suitable for GCFR use. Furthermore, since all of the correlations were derived from the results of water-flow tests, there was the question of whether any of the predictions were valid for gas flows.

To resolve these questions, the various correlations were first used to predict the response of a full-scale test assembly simulating an Advanced Gas-Cooled Reactor (AGR) fuel element, for which test results are available in the literature.<sup>(8,9)</sup> Pertinent parameters of the AGR test are given in Table 2.7 and the corresponding GCFR values are shown for comparison. Although there are some differences between the AGR test assembly and a GCFR fuel assembly, the two are similar enough for one to assume that those correlations that are in reasonable agreement with the AGR test results will also yield reasonably accurate predictions of GCFR fuel-rod response.

Table 2.7

## AGR TEST PARAMETERS

| Parameter                                            | AGR Test             | 300-MW(e)<br>GCFR <sup>a</sup> |
|------------------------------------------------------|----------------------|--------------------------------|
| Coolant density, $\rho$ , kg/m <sup>3</sup>          | 20                   | 5                              |
| Dynamic viscosity, $\mu$ , kg/m-sec                  | $1.6 \times 10^{-5}$ | $3.9 \times 10^{-5}$           |
| Distance between spacers, $\ell$ , m                 | 0.3                  | 0.25                           |
| Rod O.D., $d$ , mm                                   | 15.0                 | 7.16                           |
| Coolant velocity, maximum, $V$ , m/sec               | 36                   | 108                            |
| Coolant virtual mass per unit rod length, $M$ , kg/m | $5.3 \times 10^{-3}$ | $2.0 \times 10^{-4}$           |
| Rod mass per unit length, $m$ , kg/m                 | 1.8                  | 0.37                           |
| Rod dynamic stiffness, $EI$ , N-m <sup>2</sup>       | 395                  | 8.2                            |
| Bundle hydraulic diameter, $d_H$ , mm                | 30                   | 7.63                           |
| Reynolds number, $Re$                                | $1.4 \times 10^6$    | $1.0 \times 10^5$              |

<sup>a</sup>At outlet of highest-powered elements.

The results of using various correlations are compared in Table 2.8. Note that since some correlations allow adjustment of a constant to account for differing degrees of flow noise, a range of values is shown where applicable. The "modified Paidoussis formula" is the correlation of Ref. 10 with the noise factor,  $K$ , set to 50. This was done because the test data of Paidoussis shows that the correlation considerably underestimates low-amplitude rod vibrations when in a rod-bundle environment. It can be seen from Table 2.8 that the modified Paidoussis correlation and the correlation due to Basile, et al.,<sup>(11)</sup> give the best results. The formula of Chen<sup>(12)</sup> also gives good results, provided the correct noise level is assumed.

Since the correlation of Basile, et al., requires neither modifications nor specification of arbitrary constants to obtain good results in gas-cooled rod bundles, and since the results so obtained are somewhat

Table 2.8  
COMPARISON OF VARIOUS CORRELATIONS

| <u>Correlation</u>                         | <u>Predicted Response</u><br><u>Measured Response</u> |
|--------------------------------------------|-------------------------------------------------------|
| Paidoussis (Ref. 10) . . . . .             | 0.02 - 0.09                                           |
| Modified Paidoussis <sup>a</sup> . . . . . | 0.9                                                   |
| Basile-Faure-Ohlmer (Ref. 11) . . . . .    | 1.7                                                   |
| Burgreen (Ref. 13) . . . . .               | 12                                                    |
| Reaves (Ref. 14) . . . . .                 | 6 - 24                                                |
| Chen (Ref. 12) . . . . .                   | 1.6 - 25                                              |

<sup>a</sup>The Paidoussis correlation appears to underestimate the value of very small amplitude vibrations in bundles. For this reason, the value of the noise factor, K, was set to 50 for this calculation.

conservative, it appears to be the empirical correlation most useful for the prediction of GCFR fuel-rod response. As more information becomes available on the flow-induced vibration response of gas-cooled reactor internals, the correlations will continue to be reevaluated in this manner to verify their suitability for GCFR use.

2.3.2.2. Effect of Rod-support Conditions. Analysis was done to determine the effect of various rod-support conditions on the flow-induced response of GCFR fuel rods.

One of the most serious drawbacks of the empirical correlations for predicting flow-induced vibration response is that they were developed from experiments involving single-span rods, whereas most applications of interest have rods that have multiple supports. Unfortunately, no analytical means are yet available for predicting the effect of the intermediate supports. But since the correlations represent the only analytical means for making any predictions regarding rod response, they must be used even with their shortcomings. To do this requires transforming the problem of a multiply supported rod bundle to a problem of an "equivalent" single-span bundle. In the process it is necessary to determine an effective unsupported length, which is the length of a single-span rod that

would behave similar to the multiple-span rods. The proper choice of this length is important since it strongly influences the magnitude of predicted rod response. The choice is influenced by rod bowing, initial misalignment, and the amount of free play between rod and support.

Ordinarily, the effective unsupported length is taken to be the distance between rod spacers. However, this is strictly true only if the intermediate spacers act as simple supports. In the GCFR, the spacers are designed with approximately 0.13 mm ( $\sim 5$  mils) diametrical clearance between rod and spacer. Since the maximum amplitude of vibration is expected to be only a few hundredths of a millimeter, if there were perfect alignment between rod and spacer and no rod bowing, the rod could vibrate without touching the spacer at all. This would mean that the effective unsupported length would have to be taken as the total rod length. In the reactor, however, this possibility is very remote because of initial misalignment and rod bowing. It is more likely that the rod will be pressed up against one side of each spacer. If this is the case and if during the course of vibration the rods do not lift off of their respective spacers, then the approximation that the rod is simply supported at every spacer is legitimate and the effective unsupported length is the distance between spacers. Calculations have shown that for the majority of the fuel rods in the core, the reactions due to rod bowing will be sufficient to overcome the tendency for the rods to lift off. For a few elements located near the core center, it has been found that the reactions due to rod bowing alone may not be sufficient to overcome the reactions of the vibrating rod, so in the absence of initial misalignment, lift-off may be expected. Further calculations (which, however, neglected the considerable damping that would be present during the lift-off and recycle) showed that a potential fivefold increase in rod response may occur in these elements. It is expected that the inclusion of damping would lower this number considerably.

Because of the numerous simplifications made here, these calculations should be considered tentative. Future work in this area is planned.

2.3.2.3. Effect of Noise on Rod Response. As a first step toward analyzing the effect of circulator noise on the flow-induced response of

GCFR fuel and blanket rods, a review of experimental data available in the literature of a noise and flow test of an AGR fuel element was made.<sup>(8)</sup>

The CO<sub>2</sub> circulators in an AGR are expected to radiate 160 dB (referenced to  $2 \times 10^{-5}$  Pa)\* of broad-band noise into the inlet plenum region. To determine the effect of this noise on flow-induced vibrations, flow tests of full-scale fuel elements were conducted in which the noise was injected by means of a loudspeaker. It was pessimistically assumed that the full 160 dB was concentrated in a narrow frequency band that was swept over a wide range during the test.

In the range of frequencies that excited the rods the most, the rod response increased by a factor of 3 over the response due to flow alone. It was determined that this increased excitation was due to direct mechanical transmission of the noise through the support and spacer grids. Since the acoustic impedance ( $\rho c$ ) for high-pressure helium is slightly higher than that for the CO<sub>2</sub> used in these tests, the potential for acoustic excitation is slightly greater with helium than with CO<sub>2</sub> for a given sound-pressure level. However, because of differences in design, the SPL of a helium circulator will be less than that of a similar CO<sub>2</sub> circulator.<sup>(9)</sup> In particular, the GCFR circulators are expected to have an overall SPL of about 147 dB,<sup>(15)</sup> which means the actual sound pressure level is only about 22% of that used in the AGR tests. Near the frequency range where GCFR fuel-rod response is expected to be highest (~100 Hz), the SPL is only about 115 dB, or about 0.6% of the SPL used in the tests.

Thus, it appears that the amplification of flow-induced vibrations due to circulator noise should be very small in a GCFR - certainly significantly less than the factor of 3 found for an AGR and possibly not noticeable at all. This is a tentative conclusion, as future work in this area is planned.

### 2.3.3. Seismic Analysis of Core Elements

The seismic analysis of GCFR core elements is continuing with the

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\*Sound-pressure level (SPL) in dB  $\equiv 20 \log_{10} \left( \frac{\text{Sound pressure level}}{\text{Reference pressure level}} \right)$ .

development of updated and expanded models of the fuel and blanket elements. Whereas the previous fuel element model consisted of a system of 11 beams and 12 nodal points, the newly developed models each contain 30 beams and 33 nodal points, which allows greater detail to be incorporated. In particular, the rod bundle is now modeled separately from the assembly duct; this permits the evaluation both of rod stresses and displacements and duct stresses and displacements. Axial temperature profiles of the duct and cladding were tentatively assumed, and their effect on the material elastic properties was included.

To date, only modal extractions have been performed on the models. It has been found that the fuel-assembly frequencies calculated with the present model do not differ markedly from those calculated previously. The new values of the fuel-element fundamental frequencies are 9.3 Hz for a pinned-pinned connection at the grid plate and 11.2 Hz for a clamped-clamped connection. The values of the blanket-element frequencies are 7.7 Hz for the pinned-pinned connection and 9.4 Hz for the clamped-clamped connection.

A by-product of the increased detail used in the new analysis is that revised estimates of the weights of the fuel and blanket assemblies were obtained. The gross weight of the fuel element is now calculated to be 2,760 N (620 lb) and the gross weight of a radial blanket assembly is 4,040 N (910 lb).

These models, together with a model of a control fuel assembly yet to be developed, will be incorporated into the seismic model of the GCFR. Modal synthesis will be used to simultaneously evaluate the motions of each and every element in the reactor core during a seismic event.

#### 2.3.4. PNL Seismic and Vibration Studies

The preliminary test plan has been finalized by GA and PNL and a report is being issued by PNL. One result of the PNL study has been to recommend existing helium facilities and equipment that might be utilized for flow-induced vibration testing. A facility that is available is the helium compressor and storage building called the 115 KW building in the K area at the Hanford site. Low-pressure storage tanks are available outside

the building. These tanks are hydrostatically tested to 200 psig and are used for 50-psi storage. A railroad helium tank car loading dock is behind the storage tanks.

Inside the building is a helium compressor and the high-pressure storage tanks. The helium compressor is shown in Fig. 2.10. The compressor is rated at 170 STP ft<sup>3</sup>/min at 0 psig inlet and 1500 psig outlet. The high-pressure helium storage tanks are inside the building, as shown in Fig. 2.11. There are thirty tanks, which are 10 in. in diameter by 40 ft long and have a total storage capacity of 3,450 ft<sup>3</sup> of helium at 1500 psig. The helium compressor is located at the right of these tanks.

Additional high-pressure helium storage is available at other areas at Hanford and could be moved to the building area. There are twenty-three tanks, which are 2 ft in diameter by 80 ft long, and have a total storage capacity of 7690 ft<sup>3</sup> of helium.

The facilities and equipment are surplus at the Hanford site and could be made available for GCFR flow-induced vibration testing. They could be used for continuous helium flow of the single-rod vibration test and for a blowdown test of the 19-rod bundle and a full-sized core element assembly.

#### 2.4. ASSEMBLY FABRICATION DEVELOPMENT

The GCFR core-element assembly designs are being evaluated to determine the fabricability and feasibility of assembly of the element components. A review of the previous GCFR reference designs resulted in consideration of new conceptual designs. The survey of manufacturing techniques for the core-element components reference design was continued. The following methods for joining parts of the assembly are being considered: plasma arc, electron beam, and laser welding. The methods being considered for fabricating the rod spacers are EDM and milling with numerical control.

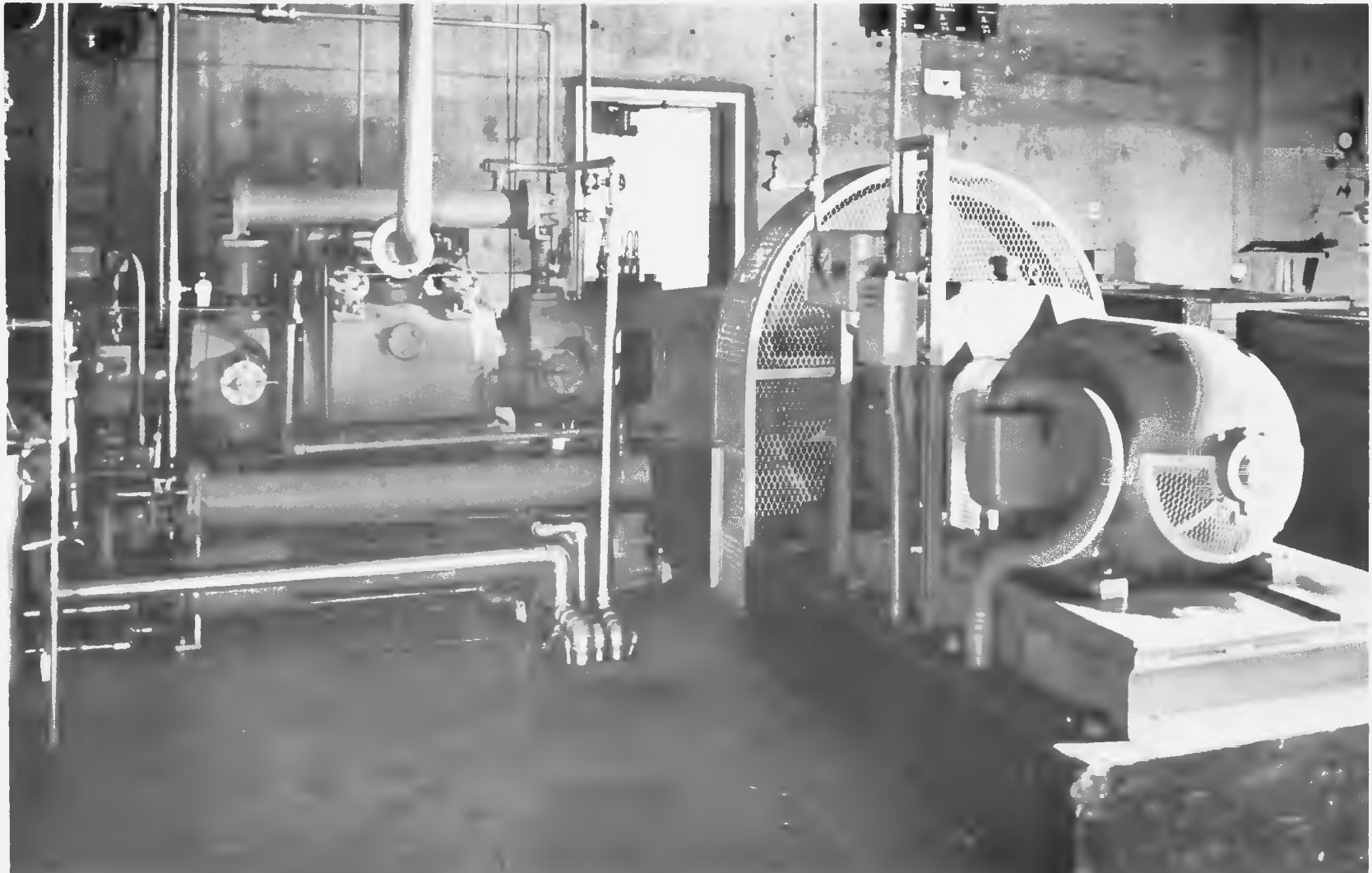


Fig. 2.10 Helium compressor inside 115 KW building at K area at Hanford

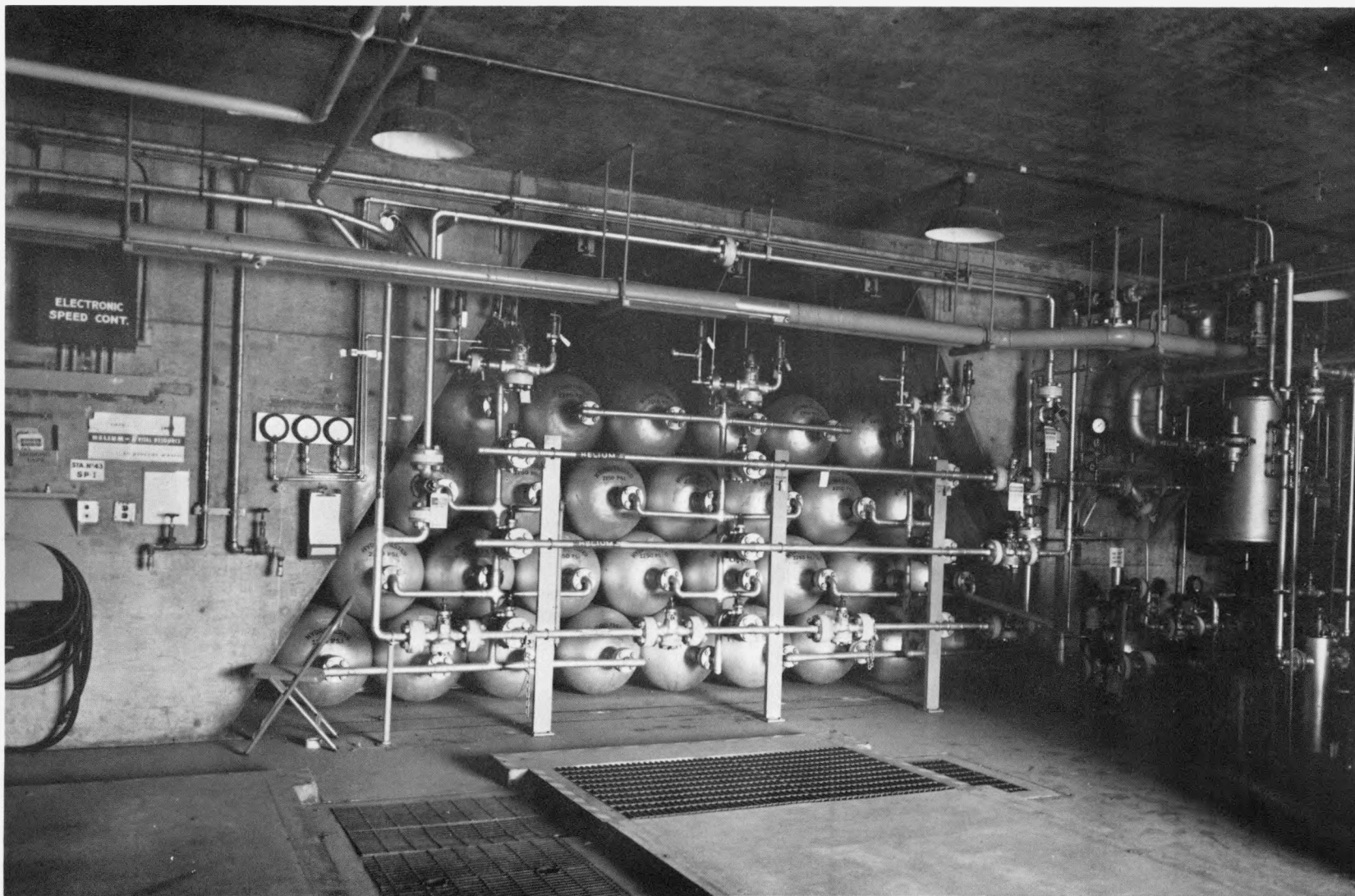


Fig. 2.11 Helium storage tanks inside 115 KW building at Hanford K area

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### III. FUELS AND MATERIALS DEVELOPMENT (189a No. 13126)

#### 3.1. FISSION-PRODUCT RELEASE AND TRANSPORT

The release and transport of fission products in GCFR fuel are being measured and studied in capsule GB-10, which is being irradiated in the Oak Ridge Research Reactor (ORR). As of January 26, 1975, the burnup of the pressure-equalized and vented fuel rod in capsule GB-10 reached approximately 64 MWd/kg of the initial exposure goal of 75 MWd/kg. The first 27 MWd/kg was accumulated at a heat generation rate of 400 W/cm (12 kW/ft) at a cladding outside surface temperature of 565°C; the remainder has been accumulated at 440 W/cm (13.5 kW/ft) at a cladding outside surface temperature of 630°C. Irradiation of capsule GB-10 is continuing to the new burnup goal of 100 MWd/kg.

Discussed below are the continuing development of the tritium monitoring instrumentation to be installed on the sweep-gas sampling line of irradiation capsule GB-10, the changes in the flow resistance of the fuel rod, presumably as a result of transport and deposition of fission products, and the revisions in the burnup schedule and the special tests to be conducted over the remainder of the GB-10 capsule experiment.

##### 3.1.1. Tritium-monitoring Instrumentation

The conceptual design of the tritium-monitoring system being developed for the GB-10 experiment was described in an earlier quarterly report.<sup>(1)</sup> An attempt was made to separate tritium from other gases in laboratory tests using either an Ag-Pd diffusion membrane or chromatographic sorption in a liquid-nitrogen-cooled charcoal bed. The tests to separate tritium (and hydrogen) from other impurities using a silver palladium membrane were described in the last quarterly report.<sup>(2)</sup> Although the membrane technique gave excellent results with tritium-helium mixtures, anomalous results were observed when micro and macro amounts of H<sub>2</sub> were added to the helium carrier gas; therefore, the effort on separation techniques was directed

toward chromatographic sorption using liquid-nitrogen-cooled charcoal traps.

During the present quarterly reporting period, measurements were made of the dynamic adsorption coefficients of tritium on charcoal as a function of (a) charcoal mass, (b) helium sweep flow rate, (c) length-to-diameter ratio,  $l/D$ , of the charcoal bed, (d) continuous versus "pulsed" loading of the bed, (e) trap design (U-tube and annular cylinder designs), and (f) hydrogen additions to the sweep gas. The data are summarized in Table 3.1. The results have been averaged over all the flow rates for each of the traps.

Table 3.1  
DYNAMIC ADSORPTION COEFFICIENTS OF TRITIUM

| Trap Parameters           |       | Actual $\text{cm}^3$ He<br>per Gram Carbon | Number<br>of Values<br>Averaged |
|---------------------------|-------|--------------------------------------------|---------------------------------|
| Charcoal<br>Weight<br>(g) | $l/d$ |                                            |                                 |
| 0.25                      | 1.43  | 1253                                       | 4                               |
| 0.75                      | 23    | 730 $\pm 90$                               | 4                               |
| 1.0                       | 5.3   | 548 $\pm 67$                               | 4                               |
| 2.0                       | 10.5  | 1188 $\pm 256$                             | 4                               |
| 0.25                      | 2.8   | 992 $\pm 74$                               | 2                               |
| 1.0                       | 11    | 835                                        | 1                               |

Burnette and Lofing have measured the static and dynamic adsorption isotherm for  $\text{H}_2$  and HT on charcoal at liquid-nitrogen temperatures in high-pressure helium. <sup>(3)</sup> They obtained adsorption coefficient values of 2787 and 1977  $\text{cm}^3$  He/g-carbon for the static and dynamic condition, respectively. They obtained an adsorption coefficient value of 846  $\text{cm}^3$  He/g-carbon when the  $\text{H}_2$  was at the 10,000 ppm level.

The extrapolated value from Antill's plot of  $\ln \phi$  versus  $1/T$  yields a dynamic adsorption coefficient,  $K$ , of  $\sim 20,000 \text{ cm}^3$  He/g-carbon;\* however, the raw data points all lie in the range of 4000 to 8000  $\text{cm}^3$  He/g-carbon. A comparison of the various  $K$  values obtained are summarized in Table 3.2.

\* $\phi$  = adsorption coefficient in  $(\text{mols}/\text{cm}^3 \text{ of charcoal bed})/(\text{mmols}/\text{cm}^3 \text{ in the gas phase})$  and  $K$  = adsorption coefficient in  $\text{cm}^3 \text{ STP/g of charcoal at 1 atm.}$

Table 3.2  
A COMPARISON OF HYDROGEN DYNAMIC ADSORPTION  
COEFFICIENT FOR CHARCOAL AT 77°K

| <u>Source</u>                                                                          | <u>Dynamic<br/>Adsorption<br/>Coefficient<br/>(cm<sup>3</sup>He/g-carbon)</u> |
|----------------------------------------------------------------------------------------|-------------------------------------------------------------------------------|
| GCFR average values in Table 3.1 . . . . .                                             | 924 <u>+487</u>                                                               |
| Burnette and Lofing, from static isotherm<br>(450 psia He) . . . . .                   | 2,787                                                                         |
| Burnette and Lofing, from dynamic data, HT only<br>(450 psia He) . . . . .             | 1,977                                                                         |
| Burnette and Lofing, 10,000 ppm H <sub>2</sub> dynamic data<br>(450 psia He) . . . . . | 846                                                                           |
| Antill's raw data . . . . .                                                            | 4,000-8,000                                                                   |
| Antill's extrapolated data . . . . .                                                   | 20,000                                                                        |

As can be seen, there is considerable variation in the data; however, they are adequate for the design of the charcoal trap for separation of tritium from impurities activated to radioactive argon and from krypton and xenon fission gases.

### 3.1.2. H<sub>2</sub>O Converter

A life test of the magnesium converter to be used for the reduction of H<sub>2</sub>O to H<sub>2</sub> in GB-10 was terminated after 18 days of operation. The magnesium bed consisted of a mixture of 20 mesh granules and metal chips (~3 mm) in a 14-mm diameter by 200-mm long stainless-steel tube. The conversion efficiency of this unit remained constant throughout the test period, with an inlet H<sub>2</sub>/H<sub>2</sub>O ratio of 455 +25 and an outlet ratio of 52,000, which indicate a reduction >99% of the influent H<sub>2</sub>O. The system helium pressure was 2 atm and the flow was 200 ml/min.

A gradually increasing pressure differential (at constant flow rate) across the converter was observed with time. Examination of the magnesium bed during disassembly of the converter showed that the leading 1.2 cm (8%) of the bed had been converted to magnesium oxide. The observed flow rate reduction was attributed to the dense packing of the magnesium oxide.

(An excessive consumption of magnesium for the measured H<sub>2</sub>O level was observed and is suspected to be due to a leak following the change of the helium cylinder.) The design of the converter for GB-10 should therefore take this phenomenon into account by (1) providing for a light loading of magnesium at the inlet of the converter and (2) designing the system to permit replacement of the converter if the pressure differential becomes excessive.

Tritium (or hydrogen) would be expected to permeate the stainless-steel tube containing the magnesium at its operating temperature of 475° to 545°C unless transport is inhibited by an oxide film on the steel. Studies have been made of tritium recovery by operating the magnesium converter unit with tritium in helium at 100 cm<sup>3</sup>/min at 2 atm for times up to 4 hr and intermittently bypassing the unit for reference measurements of the tritium level. The results indicate that the tritium losses, if any, are estimated to be less than 2%. Nevertheless, tritium loss measurements are needed to improve the precision of these measurements.

### 3.1.3. Flow-resistance Changes

As reported previously,<sup>(2)</sup> the flow resistance in the capsule GB-10 fuel rod has been rising with increasing burnup. The flow resistance of the rod and the sweep-gas line between the two measuring pressure gauges has been measured and the values have been normalized to the preirradiation resistance. Flow resistance of the lines alone is only a small part of the total resistance. The normalized resistance is

$$R = (\Delta p)P/qp_0r,$$

where  $\Delta p$  = measured pressure drop, MPa (psi),

$P$  = operating pressure, MPa (psig),

$q$  = volume metric flow rate at temperature and pressure (ml/min),

$r$  = normalization factor ( $r = R$  at the preirradiation condition, 0.01786 MPa-min/ml),

$p_0$  = 0.101 MPa (14.7 psig) (atmospheric pressure).

The rod flow resistances and the normalized flow resistances over the irradiation period of ~2.5 yr and more than 60 MWd/kg of burnup are given in Table 3.3. It may be seen that after about 45 MWd/kg, the flow

Table 3.3  
FLOW RESISTANCE OF GCFR FUEL ROD IN IRRADIATION CAPSULE GB-10

| Date Taken | Operating Conditions                       | Pressure Drop (MPa), (psi) | Flow Rate, q (ml STP/min) | Normalized Resistance |
|------------|--------------------------------------------|----------------------------|---------------------------|-----------------------|
| 8/27/72    | Preirradiation                             | 0.31 (45)                  | 1185                      | 1.0                   |
| 8/29/72    | Initial startup                            | 0.53 (77)                  | 949                       | 2.1                   |
|            | Typical long time at low burnup            | 0.14 (20)                  | 1000                      | 0.53                  |
| 5/16/74    | Low pressure (300 psi)                     | 0.50 (73)                  | 240                       | 2.4                   |
| 6/13/74    | Low pressure (300 psi)                     | 0.50 (73)                  | 230                       | 2.5                   |
| 6/26/74    |                                            | 0.75 (109)                 | 1000                      | 2.9                   |
| 7/2/74     | Burnup at 50 MWd/kg                        | 1.07 (155)                 | 980                       | 4.2                   |
| 7/11/74    |                                            | 0.83 (121)                 | 610                       | 5.2                   |
| 8/1/74     |                                            | 1.62 (235)                 | 740                       | 8.4                   |
| 8/11/74    |                                            | 1.21 (175)                 | 315                       | 14.6                  |
| 8/22/74    | Shutdown                                   | 6.32 (47)                  | 1005                      | 1.2                   |
| 9/3/74     | Return to power (440 W/cm)<br>(13.5 kW/ft) | 1.45 (210)                 | 480                       | 11.5                  |
| 9/10/74    |                                            | 1.10 (159)                 | 397                       | 9.55                  |
| 9/12/74    | Shutdown                                   | 0.15 (22)                  | 530                       | 0.99                  |
| 9/12/74    | Full power (440 W/cm)<br>(13.5 kW/ft)      | 1.15 (167)                 | 425                       | 7.60                  |
| 10/16/74   | Full power (440 W/cm)<br>(13.5 kW/ft)      | 1.69 (245)                 | 240                       | 25.8                  |
| 10/28/74   | Shutdown                                   | 0.44 (64)                  | 990                       | 1.7                   |
| 11/15/74   | Full power (440 W/cm)<br>(13.5 kW/ft)      | 1.72 (250)                 | 125                       | 52.7                  |
| 11/21/74   | Full power (440 W/cm)<br>(13.5 kW/ft)      | 1.49 (216)                 | 90                        | 63.2                  |
| 11/22/74   | Power level (245 W/cm)<br>(7.5 kW/ft)      | 0.07 (10)                  | 400                       | 0.66                  |
|            | Power level (310 W/cm)<br>(9.5 kW/ft)      | 0.14 (20)                  | 400                       | 1.32                  |
|            | Power level (370 W/cm)<br>(11.25 kW/ft)    | 0.24 (35)                  | 380                       | 2.43                  |
|            | Power level (440 W/cm)<br>(13.5 kW/ft)     | 1.45 (210)                 | 160                       | 34.6                  |
| 12/3/74    | Shutdown                                   | 0.53 (77)                  | 1050                      | 1.93                  |
| 12/13/74   | Full power (440 W/cm)<br>(13.5 kW/ft)      | 1.34 (195)                 | 55                        | 93.4                  |
| 1/20/75    | Full power at 64 MWd/kg                    | 1.49 (216)                 | 32                        | 178.                  |

NOTE: Normalization factor, r, for the flow resistances is  $0.01786 \text{ MPa-min/cm}^3$  STP (2.583 psi-min/ml STP).

resistance began to increase rapidly with burnup and thus far has reached a value of 178 times the preirradiation value. However, when the capsule is shut down, the resistance is essentially unchanged from its preirradiation value. Following shutdowns, the resistance appears to be slightly lower than just prior to the shutdown, to decrease gradually over the initial period at power following a shutdown, and then to increase very rapidly again. The flow resistance may also be seen to be a function of the linear heat rating, or power level, of the operating rod. Little change in the flow resistance occurs between the shutdown condition and about 377 W/cm (11.5 kW/ft). Above 377 W/cm, the flow resistance rises rapidly.

The flow resistance has been correlated with the buildup of fission products, particularly cesium at the fuel-blanket interface, in other irradiation tests. In the postirradiation examination of capsule GB-9, flow testing of the rod showed that the flow coefficients had not changed during the irradiation except at the fuel-blanket interface where gamma scanning revealed large peaks of cesium and iodine to be present.<sup>(5)</sup> Similar results have been obtained from GCFR fuel rods irradiated in the EBR-II to burnups of approximately 55 MWd/kg. These rods also showed cesium peaks in gamma-scanning and cesium compound formations in the fuel-blanket interface region, where markedly increased flow resistance was also located and measured.

#### 3.1.4. Rescheduling of Burnup and Special Tests for Capsule GB-10

The design burnup of capsule GB-10 is 75 MWd/kg. However, to perform a number of special tests that are planned in capsule GB-10, the capability of the capsule to sustain its power level beyond 75 MWd/kg was evaluated at HNL-OR. The original design calculations were reviewed and compared with the experience in moving capsule GB-10 into the ORR reflector flux peak in order to maintain constant power over the burnup period. From this, HNL-OR determined that it should be possible to take capsule GB-10 to 100 MWd/kg at a power level of 485 W/cm (14.8 kW/ft) if desired. Thus, there appears to be sufficient residual burnup capability in the capsule to permit a number of special tests to be performed—namely, (1) tritium release, cladding permeation, and venting, (2) simulated-leak flow-rate tests, (3) rapid depressurization tests, and (4) power-cycling (breathing)

tests. A revised burnup schedule and special test plan is being developed. The tritium test program is expected to begin in March 1975. The final test will be the power-cycling (breathing) test, which has been scheduled last in order to minimize the risk to the capsule from a cyclic failure. The simulated-leak flow-rate test is expected to be conducted early in the next quarterly period. Depressurization testing is also expected to be conducted prior to the beginning of the tritium testing.

### 3.1.5. Minimum Fuel-rod Flow-rate Analysis

Because of the increasing flow resistance in capsule GB-10, as described above, an analysis was performed to estimate the minimum flow rate that would be required in order to conduct the tritium measurements planned. The results of that analysis are presented here.

The minimum flow rate of sweep gas through the fuel rod of capsule GB-10 necessary for measuring the release of tritium from the solid-state fuel matrix was determined as follows.

The rate of generation of fission gases from the fuel rods in the GCFR demonstration plant has been calculated to be  $4.5 \times 10^{-5} \text{ cm}^3 \text{ STP/sec}$ . Correcting for the difference in the fission rates, the generation rate is

$$4.5 \times 10^{-5} \text{ cm}^3 \text{ STP/sec} \times 3600 \text{ sec/hr} \frac{13 \text{ kWt(GB-10)}}{39 \text{ kWt(GCFR)}} = 0.054 \text{ cm}^3 \text{ STP/hr} ,$$

$$(0.054 \text{ cm}^3 \text{ STP/hr}) \left( \frac{1 \text{ atm}}{68 \text{ atm}} \right) \left( \frac{975^\circ\text{K}}{273^\circ\text{K}} \right) = 2.83 \times 10^{-3} \text{ cm}^3/\text{hr}$$

The gas space in the fuel rod may be estimated from the dimensions and the smear density in the fuel:

$$V = \frac{\pi}{4} d^2 \ell (1 - \rho) = \frac{\pi}{4} [0.301(2.54)]^2 [13(2.54) (1 - 0.85)] = 2.27 \text{ cm}^3 .$$

Now, assuming slug flow of the fission gases, the transport time is

$$t_{fg} = \frac{V}{F} = \frac{2.27 \text{ cm}^3}{2.88 \times 10^{-3} \text{ cm}^3/\text{hr}} = 802 \text{ hr} .$$

The gaseous diffusion of hydrogen and tritium through high-pressure helium is much faster than the fission-gas slug flow. This is calculated as follows:

$$t_H = \frac{L^2}{6D} = \left[ \frac{(13(2.54))^2 \text{ cm}^2}{6(0.107) \text{ cm}^2/\text{sec}} \right] \left( \frac{1 \text{ hr}}{3600 \text{ sec}} \right) \left( \frac{68 \text{ atm}}{85 \text{ atm}} \right) = 0.366 \text{ hr} ,$$

where L = length of fuel,

D = diffusion coefficient for tritium in 68 atm of helium at 700°C.

In the above equation, a correction was made for the pressure difference between capsule GB-10 at 68 atm and the GCFR at 85 atm. The minimum acceptable flow rate is one that will transport the tritium by convection at least as fast as it is transported by diffusion. This limit is found by scaling the fission-gas flow rate by the ratio to the transport times for fission-gas slug flow and tritium diffusion:

$$F_{\min} = \left( \frac{802}{0.366} \right) (0.054 \text{ cm}^3 \text{ STP/hr}) \left( \frac{1 \text{ hr}}{60 \text{ min}} \right) = 1.97 \text{ cm}^3 \text{ STP/min} .$$

It is thus found that the minimum flow rate necessary to compete with the gaseous diffusion transport is a few standard cubic centimeters per minute.

Several additional considerations that are important in the flow-rate measurements are discussed below.

1. The long half-life of tritium eliminates decay as a factor in release and transport to and through the tritium monitors.
2. As the fuel-region flow rate decreases, the gas concentration increases; thus, tritium concentration is no problem in the measurements.
3. By putting a known amount of tritium in the sweep gas, the permeation loss can be measured even at very low flows, and hence the release fractions will still be known by measurement and calculation to account for permeation.
4. The breakthrough time of hydrogen and tritium in the trap are inversely proportional to the sweep-gas flow rate. At 200 cm<sup>3</sup> STP/min, breakthrough times of ~10 min are expected for 1-g charcoal traps (L/D = 5). At 20 cm<sup>3</sup> STP/min, the times would reach 100.0 min, or say 2 hr. This

would require significantly longer times to conduct the experimental program.

5. Reasonable trap breakthrough times can be assured and long test times avoided regardless of the flow rate in the flow mode in through the bottom of the fuel and out through the top of the trap (BF-TT) by operating the flow modes in through the bottom of the fuel and in and out through the top of the trap (TT-TT) simultaneously. The two sweep-gas streams converge downstream of the capsule but upstream of the monitoring instrumentation. The flow rate through the charcoal trap would be constant and could be set to provide breakthrough times in the range of 1 to 10 min. This would remove the breakthrough time as a factor in the program. The choice of flow rates must also take into consideration the reduced trap life or regeneration time at high flow rates.

6. It would be of significant experimental advantage to be able to partition the sweep stream continuously in known amounts between pure BF-TT and pure TT-TT flow modes. This would require an additional flow-meter, which could be mounted upstream of the capsule after separation of the two streams.

7. The continuous variation of the fraction of sweep gas passing through the fuel rod while maintaining the total sweep-gas flow rate constant will (1) provide an alternative (not a substitute) method to tritiating the sweep gas flowing through the fuel rod and (2) make immediate interpretation of data possible since the change in the amount of tritium measured will be the only variable.

#### 3.1.6. Tritium Test Plan

Proposed tritium tests and the test sequence have been defined for planning, preparation, and coordination purposes. These tests are designed to acquire data with respect to the generation of tritium in the fission process, its release from the solid state of the fuel, its permeation of the fuel-rod cladding into the coolant, and its venting from a pressure-equalized and vented fuel rod. Also, to be determined is the tritium composition as  $T_2$ , HT, or HTO. The tritium monitoring instrumentation has been described previously.<sup>(2)</sup> The tests defined and their sequence are shown in Table 3.4.

Table 3.4

TRITIUM INSTRUMENTATION TESTS AND DATA ACQUISITION SEQUENCE  
(At 395 W/cm linear heat-generation rate)

| Experiment Number | Type of Experiment             | Flow Mode                    | Flow Rate (cm <sup>3</sup> STP) | Gas                                 | Notes                                                                                |
|-------------------|--------------------------------|------------------------------|---------------------------------|-------------------------------------|--------------------------------------------------------------------------------------|
| 1                 | Zero baseline <sup>a</sup>     | External bypass <sup>b</sup> | 200                             | Helium                              | Determine background radiation levels                                                |
| 2                 | In-line response of instrument | External                     | 200                             | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP | Check laboratory calibration; check HTO/HT of calibration gas                        |
| 3a                | Line contamination check       | Capsule bypass <sup>c</sup>  | 200                             | Helium                              | } Check capsule bypass line for tritium losses due to interactions with contaminants |
| 3b                | Line contamination check       | Capsule bypass               | 200                             | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP |                                                                                      |
| 3c                | Line contamination check       | Capsule bypass               | 200                             | 1% H <sub>2</sub>                   |                                                                                      |
| 4a                | Vented activity check          | TT-TT                        | 200                             | Helium                              | } Determine vented and released activity under present conditions                    |
| 4b                | Released activity              | BF-TT                        | 200-1200                        | Helium                              |                                                                                      |
| 5a                | Zero baseline                  | External bypass              | 200                             | Helium                              | Recheck performance due to possible contamination of magnesium converter and lines   |
| 5b                | Calibration check              | External bypass              | 200                             | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP |                                                                                      |
| 6a                | In-line calibration            | TT-TT                        | 200                             | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP | } Determine losses of tritium along entire TT-TT and BF-TT flow circuit              |
| 6b                | In-line calibration            | BF-TT                        | 200-1200                        | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP |                                                                                      |
| 6c                | Repeat experiment 6a           | TT-TT                        | 200                             | 0.01 $\mu\text{Ci}/\text{cm}^3$ STP | Determine effect of BF-TT flow on TT-TT operation                                    |

Table 3.4 (Continued)

| Experiment Number | Type of Experiment                                                           | Flow Mode                  | Flow Rate (cm <sup>3</sup> STP) | Gas                                           | Notes                                                                                         |
|-------------------|------------------------------------------------------------------------------|----------------------------|---------------------------------|-----------------------------------------------|-----------------------------------------------------------------------------------------------|
| 7a                | Hydrogen displacement <sup>a</sup>                                           | TT-TT                      | 200                             | H <sub>2</sub> (variable)                     | Determine extent of reaction of T with contaminants in line                                   |
| 7b                | Hydrogen displacement                                                        | TT-TT                      | 200                             | H <sub>2</sub> (variable + HT)                |                                                                                               |
| 8                 | Repeat experiments 6a and 6b                                                 |                            |                                 |                                               |                                                                                               |
| 9a                | Hydrogen displacement                                                        | BF-TT                      | 200-1200                        | H <sub>2</sub> (variable)                     | Determine effect of H on T losses in rod                                                      |
| 9b                | Hydrogen displacement                                                        | BF-TT                      | 200-1200                        | H <sub>2</sub> (variable + HT)                |                                                                                               |
| 10                | Vary H <sub>2</sub> O/H <sub>2</sub> and H <sub>2</sub> O level              | Both bypass and TT-TT mode | 200-1200                        | H <sub>2</sub> O, H <sub>2</sub> <sup>d</sup> | Evaluate chemistry of reaction of T with contaminants in lines, also determine exchange rate  |
| 11                | H <sub>2</sub> O/H <sub>2</sub> and H <sub>2</sub> O level                   | BF-TT                      | 200-1200                        | H <sub>2</sub> O, H <sub>2</sub>              | Evaluate reaction and losses of T in the rod and effect of oxidation potential, exchange rate |
| 12                | Repeat of experiment 9b with variable H <sub>2</sub> O/H <sub>2</sub> + HT   |                            |                                 |                                               |                                                                                               |
| 13-15             | Repeat of major tests at 440 and 485 W/cm (13.5 and 14.8 kW/ft) power levels |                            |                                 |                                               |                                                                                               |

<sup>a</sup>The magnesium converter will be operated both on-stream and off-stream in each test.

<sup>b</sup>The external bypass is a line that bypasses all capsule lines, i.e., those that connect the valve box to the capsule and shuts off any possible venting of tritium through static but open lines to the capsule.

<sup>c</sup>The capsule bypass refers to operation with valve HV-194 closed and valves HV-195 and -199 or HV-196 and -198 open. These lines are not isolated from the capsule, although they may be "static" with reference to the sweep-gas flow.

<sup>d</sup>Maximum H<sub>2</sub> concentration will be determined from tritium response found in experiments 6a, 6b, 7a, and 7b. H<sub>2</sub>O will be varied to give H<sub>2</sub>O/H<sub>2</sub> ratios from 10<sup>-4</sup> to 10<sup>-2</sup>; then H<sub>2</sub> will be varied to get higher H<sub>2</sub>O/H<sub>2</sub> ratios.

## 3.2. INTEGRAL FUEL-ROD AND BLANKET-ROD BEHAVIOR

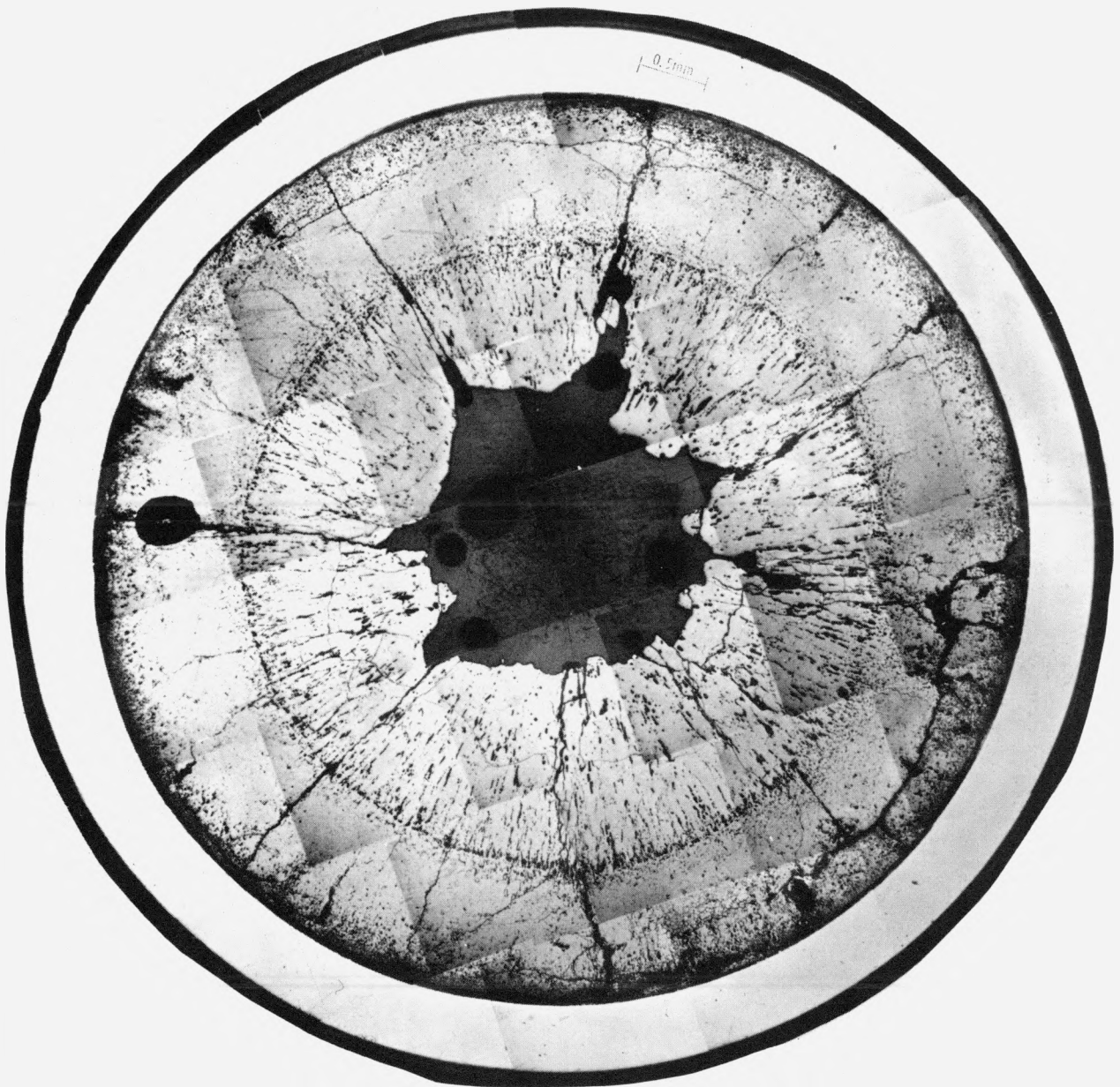
### 3.2.1. Fast-flux Irradiation Experiment F-1 (X094)

Irradiation of the encapsulated seven-fuel-rod F-1 (X094B) experiment has now achieved a maximum burnup exposure of 85 MWd/kg. Irradiation will continue to a maximum burnup of 125 MWd/kg, which is the burnup goal for the experiment.

Postirradiation examination of the F-1 rods removed during the second interim examination at 52 MWd/kg continues at ANL, and some information on the destructive postirradiation examination of fuel rod G-2 has been received from ANL. From the composite photographs of the metallographic sections (unetched) from fuel rod G-2 (Figs. 3.1 through 3.5), no cladding attack is observable. The most significant feature of the sections of the G-2 rod shown in these photographs is their similarity to the sections previously obtained from rod G-1. Apparently, rod G-2 also underwent fuel melting at some time during its irradiation (see Figs. 3.1, 3.2, and 3.4). It is interesting to note that rod G-2 operated at powers very similar to G-1 during the X094A segment of the irradiation (Runs 56 through 61), based on the run-by-run power history generated from the EBR-II fission rates. Detailed metallographic examination of the sections of fuel rod G-2 is under way at ANL.

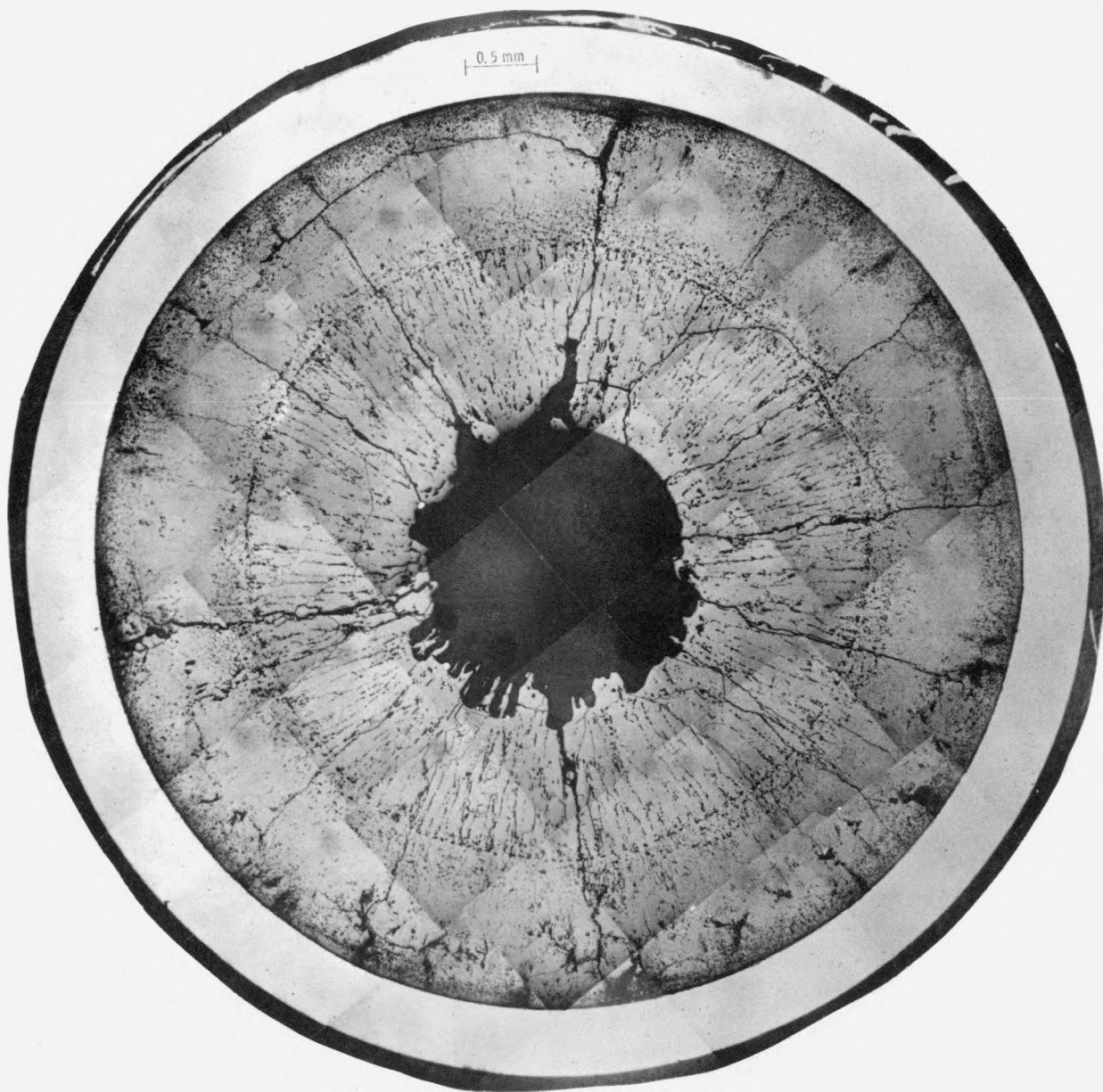
The room temperature and hot-flow test on fuel rod G-7, which had been irradiated to an exposure of ~52 MWd/kg, have been completed at ANL. Previous flow testing of the fuel-blanket interface region of rod G-7 had indicated that no flow was measurable,<sup>(1)</sup> and it was suggested that blockage may have resulted from the cesium transport and deposition in the fuel-blanket interface region. Since those tests were conducted at room temperature, a hot-flow test was planned to determine whether metallic cesium was responsible for the blockage.

The additional flow tests at the upper fuel-blanket interface of fuel rod G-7 were performed with an improved apparatus at pressures up to 50 psi and temperatures of about 105°C. Improvements in the apparatus included using copper tubing with soldered joints and a compression-type fitting on the rod (see Fig. 3.6), rather than a saddle valve. Tests of the



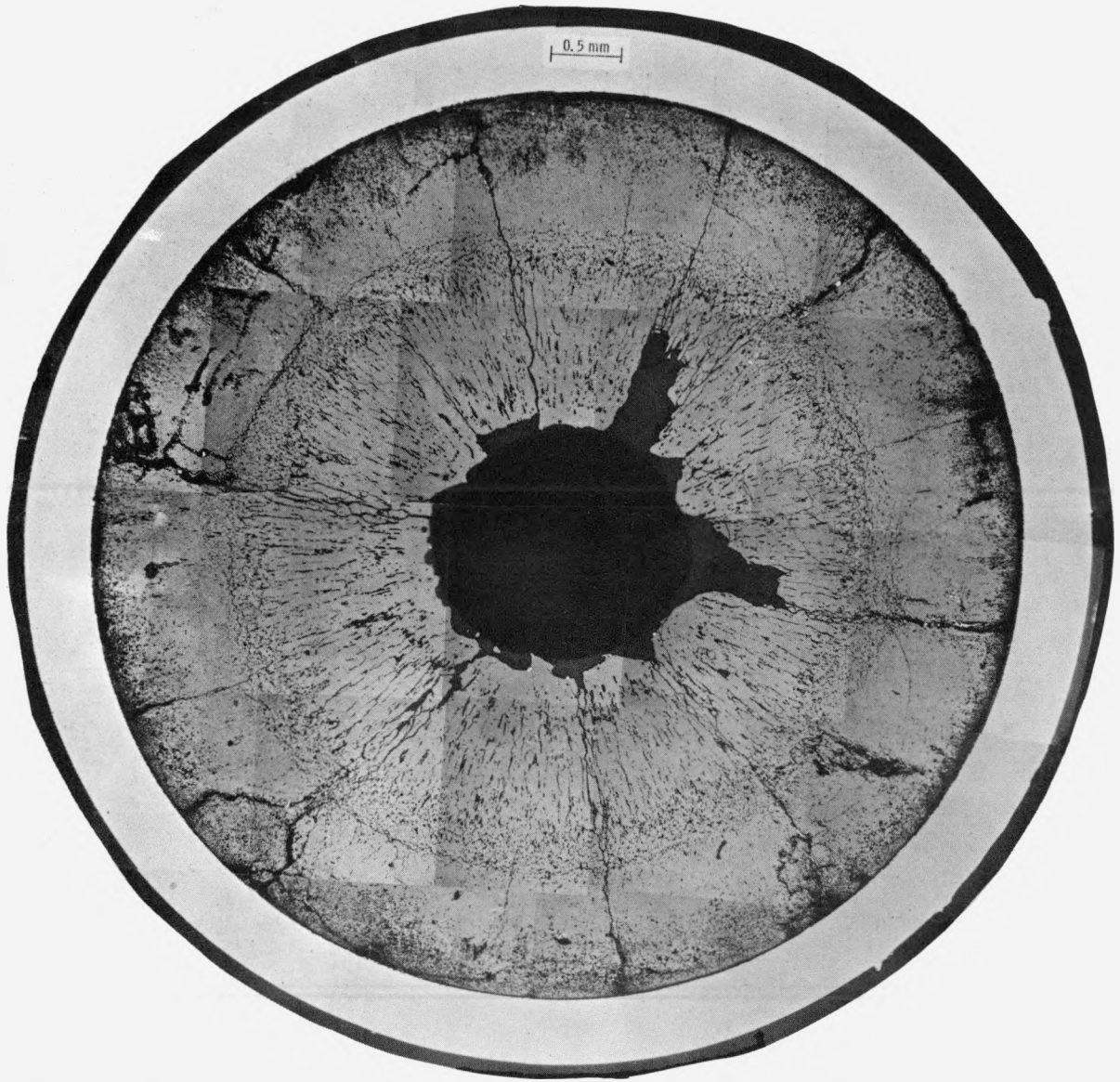
MSD-181386

Fig. 3.1 Transverse section of rod G-2 3-1/4 in. above bottom of core (cladding inside temperature, 690°C; 13.7 kW/ft power; 5.1 at-% burnup)



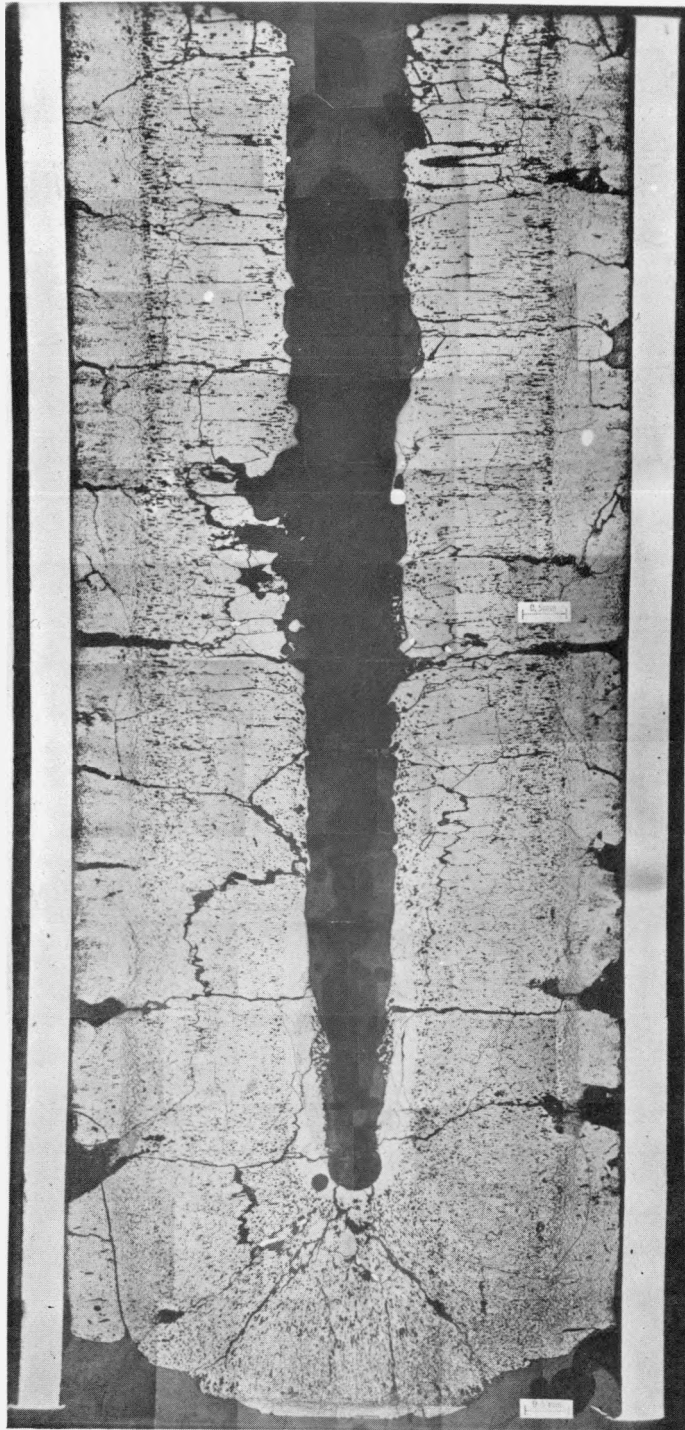
MSD-181385

Fig. 3.2 Transverse section of rod G-2 at midplane of core (cladding inside temperature, 730°C; 14.4 kW/ft power; 5.3 at-% burnup)



MSD-181384

Fig. 3.3 Transverse section of rod G-2 11-1/4 in. above bottom of core (cladding inside temperature, 730°C; 12.9 kW/ft power; 4.8 at-% burnup)



MSD 181377

Fig. 3.4 Longitudinal section at bottom of fuel column of rod G-2 showing indications of fuel melting (cladding inside temperature, 650°C; 12.6 kW/ft power; 4.7 at-% burnup)



MSD-181376

Fig. 3.5 Longitudinal section at top of fuel column of rod G-2 (cladding inside temperature, 730°C; 12.3 kW/ft power; 4.6 at-% burnup)

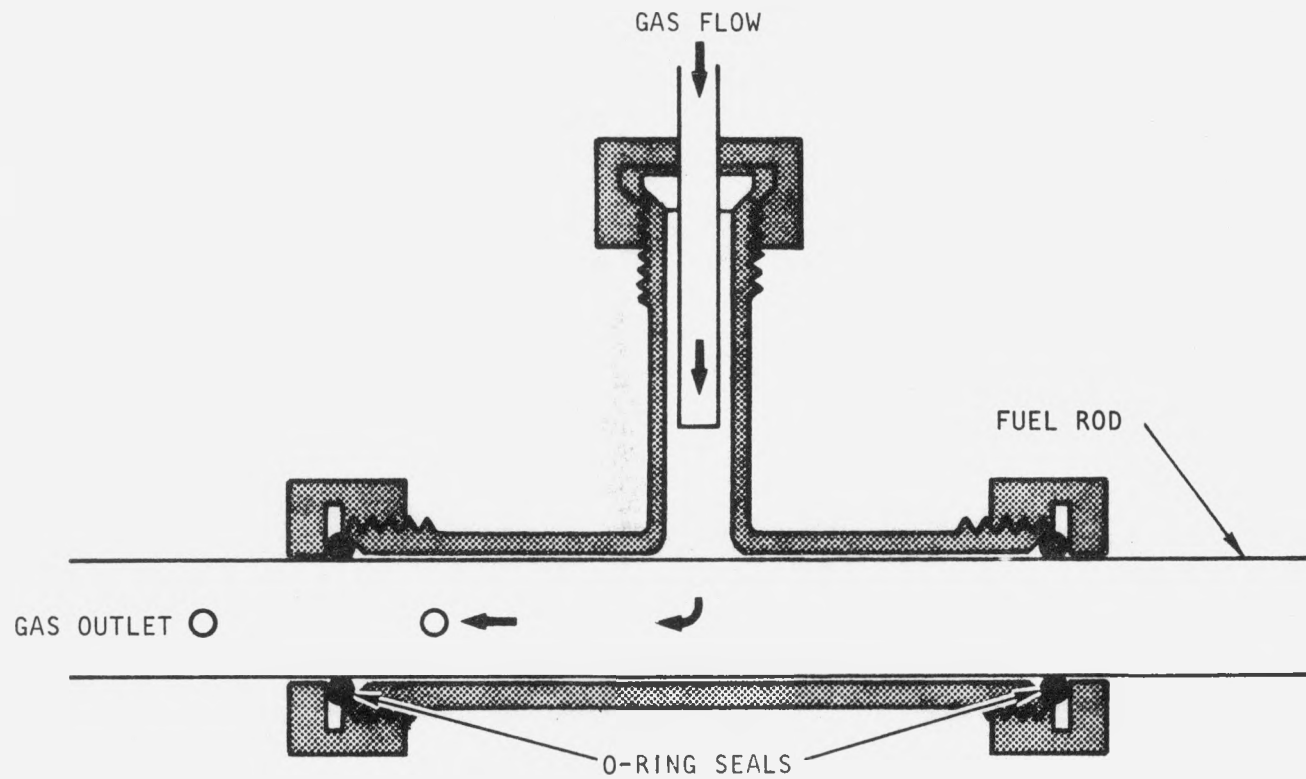


Fig. 3.6 Compression seal used in higher pressure flow tests on fuel rod G-7 at the upper fuel-blanket interface

apparatus, including the seals on the rod, show the system to be essentially leaktight. The holes from the previous flow tests were sealed with shrinkable tubing held in place with hose clamps.

Flow tests through the rod at the upper fuel-blanket interface were made after removing the shrinkable tubing over the hole just above the interface. The results of these flow tests at ambient temperature are shown by the solid lines in Fig. 3.7. The low-pressure flow tests revealed significant flow through the interface region,  $\sim 1$  ml/sec at a pressure of 40 in. of water. This is a higher flow rate than had been measured previously at the interface regions (top or bottom) of the other F-1 fuel rods. The reason for the change in the flow condition of the interface from a blockage when flow tests were run in March 1974 to a measurable flow rate now is uncertain. Two obvious factors are changes that may have occurred in the rod as a result of storage after the original tests and the difference in the flow-testing apparatus, particularly the seal at the rod. The saddle valve may have required greater force on the rod to obtain a seal than the compression fitting with an O-ring seal.

Flow testing was performed at  $\sim 105^\circ\text{C}$  even though the flow restriction was no longer apparent. The fuel-blanket interface region of rod G-7 was heated with a heat lamp while the flow rate was low ( $\sim 0.3$  ml/sec). The temperature was measured using a thermometer placed under the rod. As the temperature rose to about  $90^\circ\text{C}$ , the pressure rose from  $\sim 15$  in. of  $\text{H}_2\text{O}$  to 50 in. of  $\text{H}_2\text{O}$  for a short time and then it dropped back to about 30 in. of  $\text{H}_2\text{O}$  as the temperature reached about  $100^\circ\text{C}$ . The rod temperature was stabilized at about  $105^\circ\text{C}$  and the flow tests were run at higher pressures. The results, which are plotted in Fig. 3.8, showed that the flow rates during heating were significantly lower than before the rod was heated. The rod was allowed to cool over night and the flow tests were again run at ambient temperature (see dashed line in Fig. 3.7). The results showed a lower flow rate at ambient temperature after heating with a flow of only 6 ml/sec at 36 psi versus about 16.5 ml/sec at 36 psi before heating. The difference between the tests at the elevated temperature and the tests at ambient temperature after heating was measurable— $\sim 6$  ml/sec at ambient versus 4.75 ml/sec at  $105^\circ\text{C}$  with a pressure of 36 psi—but was not as great as between the first ambient temperature test and the heated tests.

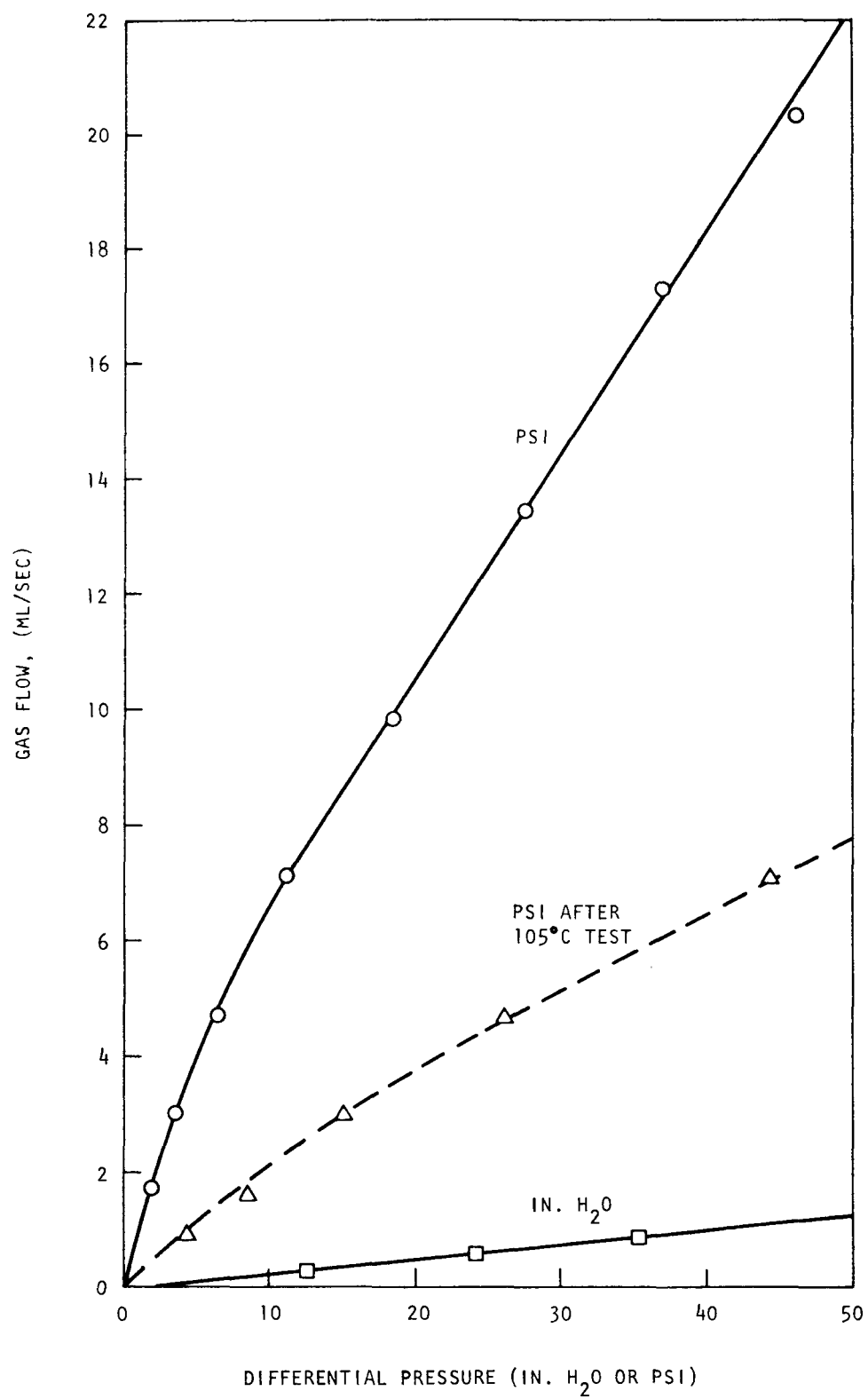


Fig. 3.7 Flow tests through upper fuel-blanket interface of rod G-7 at ambient temperature

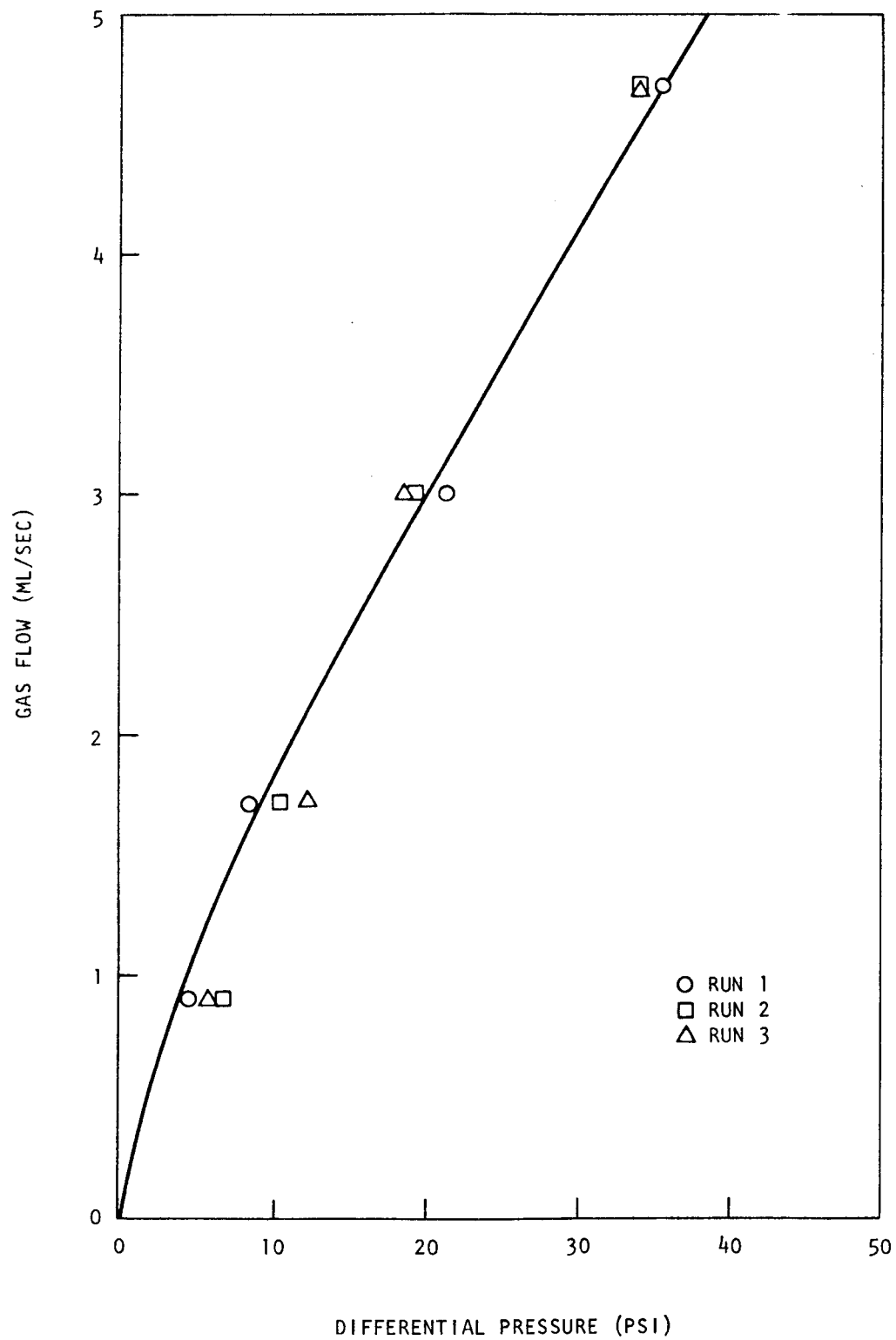


Fig. 3.8 Flow tests through upper fuel-blanket interface of rod G-7 at ~105°C

Two more tests were run at  $\sim 105^{\circ}\text{C}$  with results very similar to the first test at the elevated temperature. The rod was maintained at  $\sim 105^{\circ}\text{C}$  for 4 hr before the last test at the elevated temperature to allow the temperature at the interface to come as near to equilibrium as possible. The effect of flowing ambient-temperature ( $25^{\circ}\text{C}$ ) gas through the heated rod is unknown. No changes in temperature as measured by the thermometer were noted.

The results show that the flow test results are affected by temperature. The uncertainties caused by the storage of rod G-7 and the changes in apparatus make explanation of the data difficult.

The shipping cask containing the dosimetry, charcoal traps, cladding samples, and temperature monitors from F-1 rods G-1, G-2, and G-6 was received from ANL at the GA Hot Cell Facility in November 1974. Unfortunately, samples from rod G-7 (particularly the charcoal traps) were not included in the shipment. The shipping cask was unloaded and the low-activity-level dosimetry and cladding samples have been removed from the Hot Cell Facility and transferred to the radiochemical facility.

Fuel particles included in the dosimeters of rods G-6 and G-7 were examined to determine whether they had survived the fast neutron fluence associated with irradiation of these rods to  $\sim 50$  MWd/kg. The ability of particle coatings to sustain this fast-neutron fluence and remain intact is essential to the success of the planned measurements of the tritium yield from the fission of  $\text{U}^{233}$  and  $\text{Pu}^{239}$  in the F-3 irradiation experiment (see Section 3.2.2). Fuel particles in the two axial positions above and below the upper fuel-blanket interface of rod G-7 and in the axial position above the upper fuel-blanket interface of rod G-6 were estimated to have received essentially the same fast fluence of  $2.2 \times 10^{22} \text{ n/cm}^2$  ( $>0.1 \text{ MeV}$ ) that the particles in the dosimeters of F-3 rods G-25 and G-26 will receive at a burnup of 50 MWd/kg.

The fuel particles from rod G-7 were TRISO-coated\*  $(\text{Th}, \text{U}^{234})\text{C}_2$  whereas those in rod G-6 above the upper fuel-blanket interface contained TRISO-

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\* TRISO is an acronym that denotes the coating has three types of layers: low-density pyrolytic carbon around the kernel to act as a buffer, and this is surrounded by a layer of SiC and then a layer of high-density isotropic carbon to retain the fission products.

coated fully enriched  $U^{235}C_2$  particles. The particles were retrieved from each position and were examined under a low-power microscope. No particles with failed coatings were observed. This examination would, of course, detect only failure of the outer pyrolytic carbon coating. However, failure of the silicon carbide layer, except by the amoeba effect, is unlikely without concomitant failure of the outer pyrolytic carbon. The prospects for the success of the program to measure the tritium yields of all five fissile and fertile species in fast fission are enhanced by this observation.\*

Preparations for gamma scanning, cutting the traps from fuel-rod G-6, and removing the charcoal samples from the fuel-rod traps are continuing in the GA hot cell.

### 3.2.2. Fast-flux Irradiation Experiment F-3

The irradiation of the F-3 experiment started in EBR-II Run 75 on December 13, 1974, and has reached an exposure of 4 MWd/kg to date. As previously reported, these capsules have been designed for irradiation in an EBR-II core position (row 4) and share a type J19A subassembly (designated X206) with an ANL Group-08 high-temperature chemistry experiment. The experiment will be irradiated to a burnup of 50 MWd/kg burnup, or 300 full power days, at which time a planned interim examination will occur (about June 1976). The final maximum burnup goal for the experiment is 11 at-% ( $1.5 \times 10^{23}$  n/cm<sup>2</sup>). The experiment is being irradiated in location 4B3 of the EBR-II core.

The flow strips containing the thermocouples, which were installed on the subassembly to make the coolant flow more uniformly in the channels between the subassembly wall and the fuel-rod capsules, were not positioned as requested in the F-3 experiment data package. The flow strips as installed are located in a counterclockwise arrangement starting with flow strip 25, which is located between capsules G-15 and G-18. This is a mirror image of the specified arrangement. This arrangement will not have any significant effect on the objectives of the experiment as the neutron

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\*These results should not be extrapolated to the conclusion that these particles will survive a fast fluence of  $\sim 2 \times 10^{22}$  nvt ( $E > 0.1$  MeV) under HTGR conditions. These irradiations in EBR-II were at  $\sim 550^\circ C$  sodium temperature and the particles sustained only about  $\sim 5\%$  FIMA.

fluence for the thermocouples will not be changed and the flow channel temperatures are similar on opposite sides of the subassembly.

The revised Request for Approval in Principle for the F-3 experiment has been approved by RRD. Authorization was requested for (1) measurements of the tritium yield in fast (EBR-II spectrum) fission of  $U^{235}$ ,  $U^{234}$ , Th,  $U^{233}$ , and  $Pu^{239}$ , and (2) studies of the use of H-451 graphite in GCFR fission-products traps for use in the F-3 replacement rods to be inserted when the experiment achieves an exposure of 50 MWd/kg.

Neutron radiography and gamma-scanning of fuel rods in the F-1 (X094) fast-flux irradiation experiment have shown large volumetric shrinkages of the charcoal in both the sealed and active charcoal traps. Measurement of the neutron radiographs obtained during the 50 MWd/kg interim examination showed shrinkages in the range 40% to 60% by volume. These observations led to consideration of the use of graphite as an alternative trapping material because graphite would be expected to expand (slightly) rather than shrink under irradiation. In addition, graphite is known to be a good sorbant for metallic fission products even at elevated temperature. (6)

The F-3 traps will be located above and as close as possible to the fuel column to maximize communication for the fission products between the fuel and trap. The traps will be oriented so that the graphite rod will be in the upper level of the trap in rod G-25 and in the lower level of the trap in rod G-26 so as to determine any differences in the fission-product sorption of solid and granulated graphite.

The objective of the inclusion of dosimetry particles in replacement rods G-25 and G-26 is to enable measurements of the tritium yield in ternary fission, in the EBR-II spectrum, from all fissile and fertile species in the F-1 and F-3 irradiation experiments. The unique tritium retention capability of the TRISO-coated fuel particles will yield this important data quite inexpensively and, hopefully, with good precision. (7,8)

Fuel particles containing  $U^{235}$ ,  $U^{238}$ , and thorium are already included in dosimetry capsules in the fuel rods of both the F-1 and F-3 experiments. Recently, examination of particles irradiated in rods G-6 and G-7 of the

F-1 experiment has shown that the particles survived an exposure of  $\sim 50$  MWd/kg and a fast fluence of  $\sim 2 \times 10^{22}$  nvt ( $E > 0.1$  MeV) in EBR-II (see Section 3.2.1). Thus, by inclusion of particles containing  $U^{233}$  and  $Pu^{239}$  in dosimeters of the replacement rods for the F-3 experiment to be fabricated this fiscal year, a complete set of fast-fission yields for all the species of interest could be obtained.

Fortunately, archive samples of TRISO-coated sol-gel  $U^{233}O_2$  particles and TRISO-coated  $Pu^{239}O_{1.81}$  particles were available at ORNL. Additionally, the isotopic contents of  $U^{232}$  and  $Pu^{240}$  in these samples were low, which will enhance the value of the data and ease the handling problems associated with these materials.

The design of the traps in F-3 rods G-25 and G-26 has been changed to incorporate graphite in the traps and coated particles containing  $Pu^{239}$  and  $U^{233}$ . Granulated H-451 graphite and solid graphite rods, instead of charcoal as in the other fuel rods of the F-3 experiment, will be employed as the sorbant. These traps will be of the flow-through (active) design. Needle tubing containing coated fuel particles for tritium-yield measurement have been incorporated in the dosimeters. The appropriate drawings have been changed and have been sent to ANL for review. The revised test matrix for the F-3 experiment is shown in Table 3.5.

The work on the fabrication and assembly F-3 replacement rods has been scheduled by ANL to start on February 1, 1975. Two months will be required to set up the fuel-rod-fabrication glove boxes to the requirements established for the F-3 rods, and another three months will be required to build a new sodium-bonding apparatus. (The sodium bonding apparatus previously used was not moved to the new location for fabrication and assembly.) It has been decided that for the F-3 replacement rods, the previous sodium-bond design was preferable to a proposed flow-through design (as used in the ANL Group-08 fuel rods). Two of the reasons for the decision were: (1) the flow-through design would lower the peak cladding temperatures by  $10^\circ\text{C}$  and (2) the risk of bubble entrapment causing hot spots would be increased.

Analysis of one of two archive samples treated in the same manner as the trap charcoal loaded into the initial capsules of the F-3 experiment

Table 3.5

## F-3 FAST-FLUX IRRADIATION CAPSULE EXPERIMENT

| Fuel Rod<br>Capsule<br>Number | F-3 Irradiation Conditions                    |                      |                           |                            |                    |     |                   |                                |                                           | Design<br>Variations <sup>c</sup> |   |   |    |   |   |
|-------------------------------|-----------------------------------------------|----------------------|---------------------------|----------------------------|--------------------|-----|-------------------|--------------------------------|-------------------------------------------|-----------------------------------|---|---|----|---|---|
|                               | Cladding<br>Temperature<br>Calculated<br>(°C) | Fuel<br>O/M<br>Ratio | Fuel<br>Density<br>(% TD) | Fuel<br>Pellet<br>Geometry | Burnup<br>(MWd/kg) |     | Charcoal Trap     |                                |                                           |                                   |   |   |    |   |   |
|                               |                                               |                      |                           |                            | 50                 | 100 | Type <sup>a</sup> | Location <sup>b</sup><br>(in.) | Neutron<br>Fluence<br>x 10 <sup>-22</sup> |                                   |   |   |    |   |   |
|                               |                                               |                      |                           |                            |                    |     |                   |                                |                                           |                                   |   |   |    |   |   |
| Initial Loading               |                                               |                      |                           |                            |                    |     |                   |                                |                                           |                                   |   |   |    |   |   |
| G-14                          | 676                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 9.875                          | 2.4                                       | G                                 | M | E |    |   |   |
| G-15                          | 685                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.55                           | 7.0                                       |                                   |   | E |    |   |   |
| G-16                          | 688                                           | 1.94                 | 88                        | Solid                      |                    |     | S                 | 7.55                           | 7.0                                       |                                   |   | E |    | T |   |
| G-17                          | 690                                           | 1.98                 | 88                        | Solid                      |                    |     | A                 | 7.55                           | 7.0                                       |                                   |   | E |    | T |   |
| G-18                          | 689                                           | 1.98                 | 88                        | Solid                      |                    |     | S                 | 9.875                          | 4.7                                       | G                                 | E |   |    |   |   |
| G-19                          | 749                                           | 1.94                 | 92                        | Annular                    |                    |     | A                 | 9.875                          | 2.4                                       | M                                 | A |   |    |   |   |
| G-20                          | 749                                           | 1.98                 | 92                        | Annular                    |                    |     | A                 | 9.875                          | 2.4                                       |                                   | M | E |    |   |   |
| G-21                          | 728                                           | 1.94                 | 92                        | Annular                    |                    |     | A                 | 9.875                          | 4.7                                       |                                   |   | E |    |   |   |
| G-22                          | 743                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.55                           | 7.0                                       |                                   |   | E |    |   |   |
| G-23                          | 743                                           | 1.98                 | 88                        | Solid                      |                    |     | A                 | 9.875                          | 4.7                                       |                                   | E |   |    |   |   |
| Replacement Rods              |                                               |                      |                           |                            |                    |     |                   |                                |                                           |                                   |   |   |    |   |   |
| G-24                          | 676                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.27                           | 3.7                                       | G                                 | M | E |    |   | C |
| G-25                          | 749                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.55                           | 3.5                                       |                                   | M | A | GT |   |   |
| G-26                          | 749                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.55                           | 3.5                                       |                                   | M | A | GT |   |   |
| Spare Rods                    |                                               |                      |                           |                            |                    |     |                   |                                |                                           |                                   |   |   |    |   |   |
| G-27                          | 685                                           | 1.94                 | 88                        | Solid                      |                    |     | A                 | 7.55                           |                                           |                                   |   | E |    |   |   |
| G-28                          | 743                                           | 1.98                 | 88                        | Solid                      |                    |     | A                 | 9.875                          |                                           |                                   |   | E |    |   |   |

<sup>a</sup>A = active; S = sealed.<sup>b</sup>Distance from core centerline to trap centerline.<sup>c</sup>G - contains graphite sample,

M - contains shield material samples,

T - no getter ring, transient test rod,

E - GA-etched surface roughening,

A - Alpha-cut surface roughening,

C - charcoal trap between blanket pellets 1 and 2,

GT - contains granulated graphite instead of charcoal  
and U<sup>233</sup>O<sub>2</sub> and Pu encapsulated coated particles.

has been completed. This analysis, carried out using a mass spectrometric technique for gas analysis and a volume measurement for quantification, yielded the following gas release from 0.87 g of charcoal at 600°C:

|                            |                        |
|----------------------------|------------------------|
| CO <sub>2</sub> . . . . .  | 0.018 cm <sup>3</sup>  |
| CO . . . . .               | 0.0042 cm <sup>3</sup> |
| H <sub>2</sub> O . . . . . | 0.0037 cm <sup>3</sup> |

This gas volume is acceptably low for the purposes of the F-3 experiment.

### 3.3. CLADDING MATERIALS BEHAVIOR

#### 3.3.1. GCFR Cladding Irradiation Tests

Inclusion of GCFR ribbed and smooth cladding specimens in a materials test experiment that was designed and fabricated by Hanford Engineering Development Laboratories (HEDL) and is now being irradiated was reported in a previous quarterly report.<sup>(1)</sup> The ribs on the samples were formed by etching. Additional cladding samples ribbed by electrochemical grinding will be included in a material capsule that will be assembled by HEDL in mid-1975. This cladding material was ribbed by the Swiss Federal Institute for Reactor Research (EIR). Twenty-eight test specimens have been machined from the material and are undergoing quality control inspection.

#### 3.3.2. Helium Loop Test Program at PNL

The primary objective of this program is to permit comparison of mechanical properties in recirculating helium to those in a quasi-static helium; these will be measured in the Mechanical Properties Testing Program at ANL. The work scope has been agreed upon and the evaluation of the techniques for detection of pin-hole leaks and ruptures has been completed.

Work during this quarterly period has been directed toward modifying the loop for impurity control and unattended operation. A Panametrics moisture-monitoring unit with three sensing probes was received at PNL. This unit is being tested for long-term drift and stability. The delivery of the Thermox meter for monitoring H<sub>2</sub>/H<sub>2</sub>O ratio is due in early 1975. The rpm meter for the helium circulator, which was defective when it was received, was repaired and returned by the supplier. The authorization

for the capital equipment funding has been received by PNL. Authorization for the rest of operating funds for FY-75 has been processed and sent to PNL.

### 3.3.3. Support of ANL Studies of Geometry Effects and Impurity Effects in Helium on GCFR Cladding

A study was initiated at ANL in mid FY-75 to determine the effects of rib geometry and the effects of impurities on the behavior of GCFR fuel-rod cladding in a quasi-static helium. Although the work at ANL is in a planning stage, ~90 in. of cladding ribbed by etching, ~100 in. of cladding ribbed by mechanical grinding, and ~130 in. of smooth cladding were supplied by GA to ANL for initial test purposes.

A tentative initial test matrix for the ANL mechanical testing program was received from ANL. Comments on this initial test matrix were discussed with ANL personnel and agreement was reached on the following test matrix. Initial testing will be carried out at 700°C in pure helium.

| Tube Type                          | Length (in.) | Number of Specimens |        | Stress (ksi) | End-cap Design |
|------------------------------------|--------------|---------------------|--------|--------------|----------------|
|                                    |              | Ribbed              | Smooth |              |                |
| Ground tubing by Superior Tube Co. | 1.75         | 4                   | ---    | 36           | EBR-II         |
|                                    | 1.75         | 4                   | ---    | 36           | HEDL           |
| Tubing etched by GA                | 1.75         | 4                   | 4      | 36           | (see text)     |
|                                    | 1.75         | 4                   | 4      | 36           |                |
|                                    | 4.0          | 4                   | 4      | 12           |                |
|                                    | 4.0          | 4                   | 4      | 12           |                |

The first part of this test matrix will attempt to determine the effect of the end-cap design using mechanically ground tubing fabricated by Superior Tube Co. The most favorable of the two end-cap designs will be used in the latter part of the test matrix.

The second part of the test matrix is designed to determine the difference between smooth and ribbed tubing and the effect of specimen length. These tests will use tubing prepared by etching at GA.

A factorial design test matrix for biaxial creep testing of GCFR cladding at ANL was received from ANL in accordance with the agreements and commitments made earlier. This matrix is being reviewed at present for possible improvements based on a statistical design to achieve the defined objectives of this program. The environmental conditions for this test are under review at present.

#### REFERENCES

1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period May 1, 1974 through July 31, 1974," USAEC, Report GA-A13148, General Atomic Company, September 26, 1974.
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#### IV. NUCLEAR ANALYSIS AND REACTOR PHYSICS (189a No. 13127)

The scope of activities planned under this task encompasses the validation and verification of the nuclear design methods that will be applied to the GCFR core design. This will be done primarily by direct evaluation of the methods with a critical experiment program specifically directed toward GCFR development. Program planning and coordination activities, critical assembly design and analysis, and the necessary methods development will be carried out during the course of this program.

During the previous quarter, the preanalysis of the Phase I core was completed and comparison with ANL values was made. Preliminary specifications of the overall program plan, and experimental details of Phase I, were jointly agreed to by GA and ANL. Work continues on adaptation of the VARI-8 code at GA for use in sensitivity analyses.

##### 4.1. PREPARATION FOR THE PHASE I CRITICAL ASSEMBLY

Program approval from RRD was received in November, 1974, at a joint meeting among GA, ANL, and RRD at Washington. General approval of the six-phase program plan for benchmark assemblies was granted, subject to individual approval of a detailed work plan in advance of each phase. Such a work plan was approved for the first phase of the program, and experimental details were extensively discussed. The finalized work plan has been issued by ANL.<sup>(1)</sup>

The final precritical prediction of the critical radius for the Phase I core has been made with both one-dimensional and two-dimensional diffusion theory codes (see Fig. 4.1). The results of these calculations indicate a critical radius of 90.35 cm. In the one-dimensional calculation using the GAZE-2 code, an appropriate transverse buckling was entered in a radial calculation. The two-dimensional calculations were made using the ADGAUGE code and include, on option, corrections for neutron streaming in the

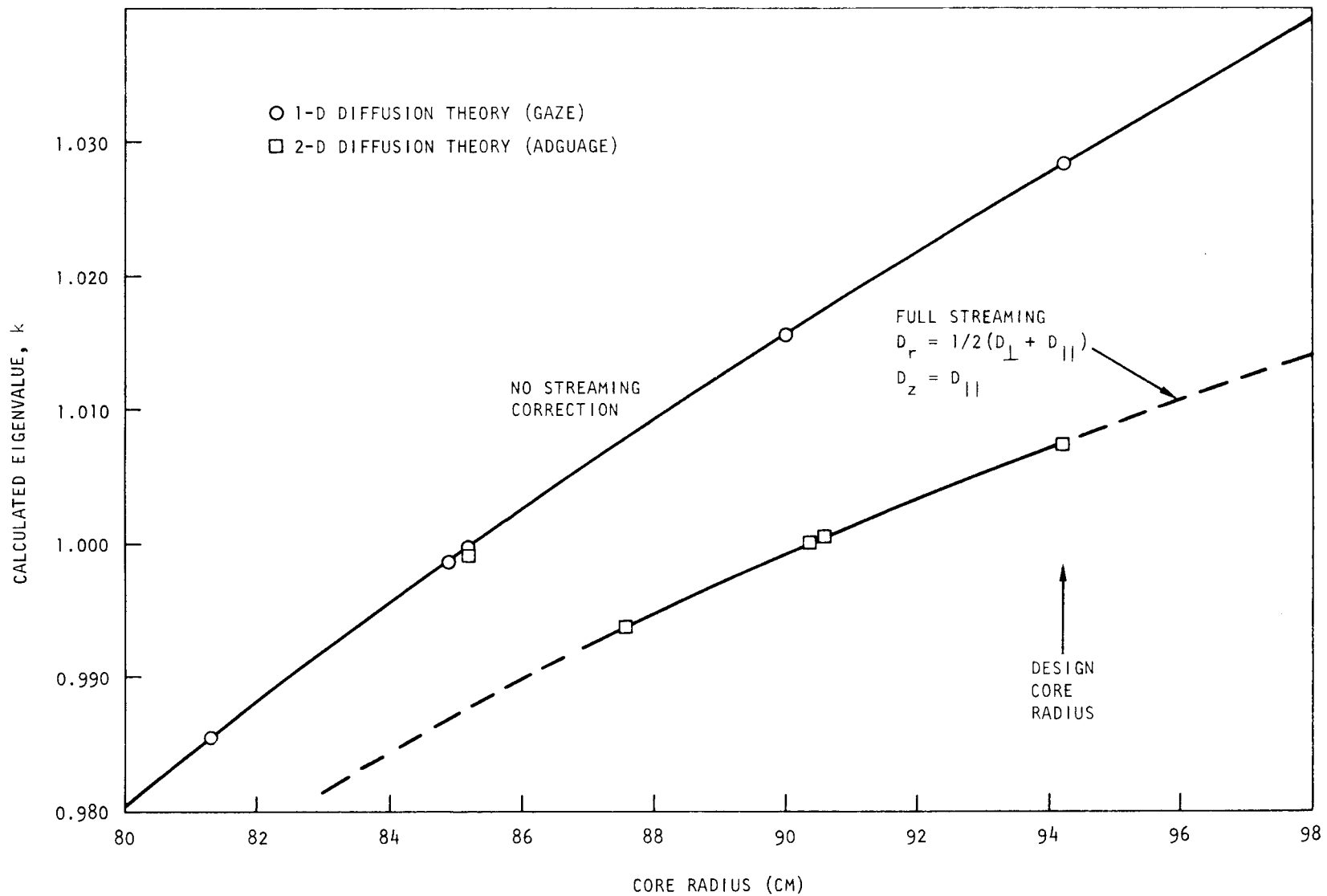


Fig. 4.1 Variation of k-effective with core radius in Phase I GCFR assembly

radial and axial directions. Although the eigenvalue depends rather sensitively on the specific form of the streaming correction, it was believed that the approach shown on Fig. 4.1 would be appropriate. In this approach, the radial streaming correction is changed to be the arithmetic average of the corrections for the r and z directions. This is also approximately equivalent to the arithmetic average of the corrections for the x and y directions in an x-y calculation and thus should be appropriate for radial streaming. The axial streaming treatment is unchanged.

#### 4.2. PREANALYSIS FOR THE PHASE II CRITICAL ASSEMBLY

A start has been made in defining the Phase II critical assembly. This assembly has the same composition as Phase I, but the void volume fraction has been reduced from 55% to 43%. Bidirectional diffusion coefficients utilizing this smaller void fraction have been derived for use in subsequent diffusion calculations. Also, the effect of collapsing the void entirely was investigated. The results are shown in Table 4.1 in terms of the bidirectional modifiers, which are the ratios of the directional diffusion coefficients to the isotropic diffusion coefficients.

The coefficients uniformly approach the limiting value as the void fraction decreases. The limiting value, however, does not correspond to the isotropic diffusion coefficient (where the ratio is 1.00), but it still reflects the heterogeneous structure of the ZPR unit cell.

#### 4.3. METHODS DEVELOPMENT

A study of a simplified reactor model was undertaken in order to examine the effect of different methods of cross-section preparation on the eigenvalue. The study was prompted by a discrepancy between GA and ANL calculated eigenvalues for the GCFR Phase I critical assembly. This discrepancy appeared to be traceable to the differences in the transport cross sections that determine the diffusion coefficients.

In previous GGC-5 calculations, the  $B_3$  solution option was used for the spectrum equations, whereas the derivation of the microscopic transport cross section was calculated according to the so-called "definition 2."<sup>(2)</sup> A recent thorough investigation<sup>(3)</sup> has shown that this procedure for the calculation of  $\sigma_{tr}$  is different from that used at ANL. For this reason,

Table 4.1  
 BIDIRECTIONAL DIFFUSION COEFFICIENT MODIFIERS FOR PHASE I  
 AND PHASE II OF THE GCFR CRITICAL EXPERIMENT

| Energy<br>Group | $D_{\perp}/D_{\text{ISO}}$ |          |         | $D_{\parallel}/D_{\text{ISO}}$ |          |         |
|-----------------|----------------------------|----------|---------|--------------------------------|----------|---------|
|                 | Phase I                    | Phase II | No Void | Phase I                        | Phase II | No Void |
| 1               | 1.0020                     | 1.0017   | 1.0003  | 1.0620                         | 1.0575   | 1.0037  |
| 2               | 1.0028                     | 1.0022   | 1.0003  | 1.0744                         | 1.0661   | 1.0039  |
| 3               | 1.0045                     | 1.0032   | 1.0004  | 1.1003                         | 1.0858   | 1.0038  |
| 4               | 1.0097                     | 1.0068   | 1.0009  | 1.1524                         | 1.1294   | 1.0068  |
| 5               | 1.0120                     | 1.0083   | 1.0011  | 1.1563                         | 1.1301   | 1.0061  |
| 6               | 1.0179                     | 1.0123   | 1.0017  | 1.1792                         | 1.1480   | 1.0082  |
| 7               | 1.0164                     | 1.0114   | 1.0016  | 1.1739                         | 1.1432   | 1.0091  |
| 8               | 1.0258                     | 1.0181   | 1.0032  | 1.2132                         | 1.1780   | 1.0129  |
| 9               | 1.0306                     | 1.0215   | 1.0044  | 1.2348                         | 1.1948   | 1.0168  |
| 10              | 1.0486                     | 1.0362   | 1.0091  | 1.2782                         | 1.2324   | 1.0314  |

the effect of using other transport cross-section definitions was also investigated.

The model selected was a four-nuclide spherical homogeneous reactor with no blanket or reflector. The composition was intended to simulate that of the uniform core of the Phase I critical experiment. Therefore, the fissile nuclide,  $\text{Pu}^{239}$ , was chosen to have an atom density equal to the sum of all fissile nuclides. The atom density of the fertile nuclide,  $\text{U}^{238}$ , was the sum of all fertile nuclides in the Phase I core. Similarly,  $\text{Fe}^{56}$  represented all structural nuclides, whereas  $\text{O}^{16}$ , as the principal light scatterer, remained at the same atom density as in the critical assembly. The numerical densities are given in Table 4.2.

These nuclides were used in a series of homogeneous GGC-5 calculations with a constant input buckling of  $8.85 \times 10^{-4}$  corresponding to a critical radius of 101.79 cm. GAROL and GANDY treatments were utilized in the resolved and unresolved resonance regions, respectively; ENDF-B Version 3 nuclear data were used. Twenty-seven-group cross sections in the ANL group

Table 4.2  
SYNTHETIC REACTOR ATOM DENSITIES

| <u>Nuclide</u>              | <u>Density<br/>(<math>10^{24}/\text{cm}^3</math>)</u> |
|-----------------------------|-------------------------------------------------------|
| $\text{O}^{16}$ . . . . .   | 0.01066                                               |
| $\text{Fe}^{56}$ . . . . .  | 0.018531                                              |
| $\text{U}^{238}$ . . . . .  | 0.0042676                                             |
| $\text{Pu}^{239}$ . . . . . | 0.0009302                                             |

structure were generated for six cases. These cross sections were used in GAZE for the calculation of the eigenvalue; the results of this investigation are presented in Table 4.3.

In Cases 1 through 3, all cross sections, including transport cross sections, were entered in GAZE as microscopic data for each nuclide. In Cases 4 through 6, the macroscopic cross sections obtained from GGC-5 were used with GAZE.

With respect to the calculation of microscopic or macroscopic cross sections by GGC-5, the following should be noted: GGC-5 calculates the macroscopic transport cross section first and then the  $\sigma_{\text{tr}}$ 's are obtained for the individual nuclides. These  $\sigma_{\text{tr}}$ 's depend not only on the atomic density of the nuclide but also on the option used in the calculation of  $\sigma_{\text{tr}}$  (definition 1 or 2). In cases 4 through 6 in Table 4.3, "macro" means that the  $\Sigma_{\text{tr}}$ 's were calculated for GAZE based on atomic densities of the individual nuclides. The cross sections other than  $\sigma_{\text{tr}}$  or  $\Sigma_{\text{tr}}$  are the same in all six cases.

The maximum spread in the eigenvalue is approximately 0.6%. It is concluded that the cause of the discrepancy between GA and ANL calculated eigenvalues cannot be entirely attributed to the difference in the definition of the transport cross section. However, as a result of this study, "definition 1" will be utilized for future calculations due to the better theoretical foundation.

Table 4.3  
TRANSPORT CROSS-SECTION OPTIONS IN DIFFUSION THEORY

| Case | Cross Sections from |                           | GAZE-2 Calculation |         |         |                  |                         |
|------|---------------------|---------------------------|--------------------|---------|---------|------------------|-------------------------|
|      |                     |                           | Leakage            | Capture | Fission | $k_{\text{eff}}$ | $\Delta k/k \times 100$ |
| 1    | GGC-5               | Def. 1 - $B_3$<br>(Micro) | 0.3743             | 0.2868  | 0.3392  | 0.99359          | +0.38                   |
| 2    | GGC-5               | Def. 2 - $B_3$<br>(Micro) | 0.3773             | 0.2851  | 0.3378  | 0.98965          | Standard                |
| 3    | GGC-5               | Def. 1 - $P_1$<br>(Micro) | 0.3788             | 0.2844  | 0.3371  | 0.98750          | -0.22                   |
| 4    | GGC-5               | Def. 1 - $B_3$<br>(Macro) | 0.3751             | 0.2864  | 0.3388  | 0.99238          | +0.27                   |
| 5    | GGC-5               | Def. 2 - $B_3$<br>(Macro) | 0.3751             | 0.2864  | 0.3388  | 0.99238          | +0.27                   |
| 6    | GGC-5               | Def. 1 - $P_1$<br>(Macro) | 0.3788             | 0.2844  | 0.3371  | 0.98750          | -0.22                   |

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## V. PRESSURE-EQUALIZATION SYSTEM FOR FUEL (189a No. 13715)

### 5.1. FUEL-ELEMENT AND VENT-CONNECTION-SEAL TEST PROGRAM

In the GCFR reference design, the fuel, control, and blanket elements and their vent connections are sealed to the grid plate by clamping the conical surface of the elements to the matching surfaces in the grid plate with a force sufficient to effect a seal and to support the elements, which are cantilevered from the grid plate. These element seals must function at the coolant-pressure difference between the reactor core inlet and exit plenums. The effectiveness of the seals over the life of the core is uncertain, not only because each element may be rotated or relocated several times over its useful life, but also because the seals must be effective in a high-purity, high-temperature helium environment while subject to mechanical, vibrational, and thermal effects. Most of the uncertainties are expected to be resolved in a two-part test program: (1) a materials screening test program for the prevention of static adhesion of matched fuel-element and grid-plate parts and (2) leakage tests of fuel-element and vent-connection seals to the grid plate. Current progress in these activities is given below.

#### 5.1.1. Static Adhesion Tests

The first phase of the GCFR element seal evaluation includes an investigation of the extent of adhesion between small seal samples of the conical seal parts clamped together with loads simulating the GCFR fuel-element and grid-plate design conditions. The samples are Type 316 stainless steel and the environment is helium at 300°C with impurities of 9,000  $\mu\text{atm}$  of  $\text{H}_2$  and 90  $\mu\text{atm}$  of  $\text{H}_2\text{O}$ .

The first 3,000-hr test was completed on January 26, 1975; the assemblies are shown in Fig. 5.1. The evaluation of the samples will be conducted during the next quarterly period. An addition to the procedure for evaluating the test parts was written, reviewed, and approved. In

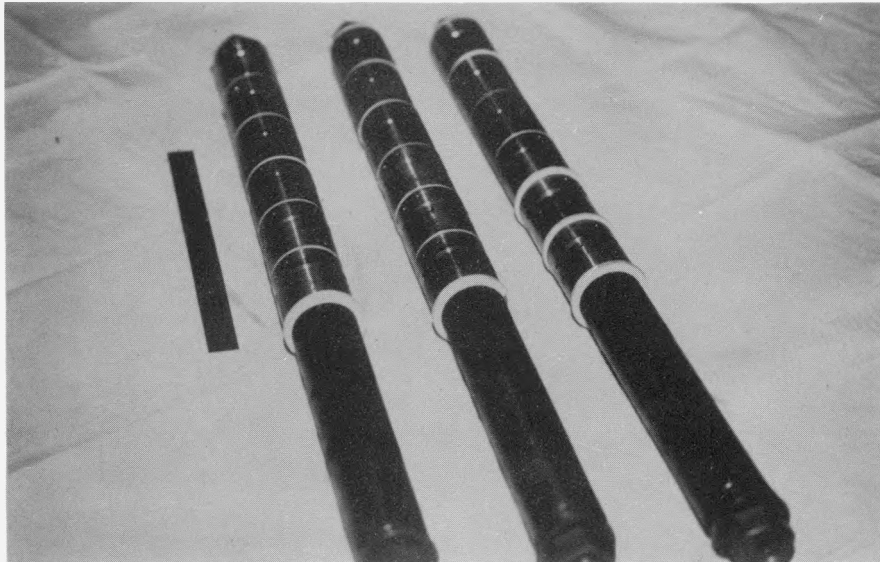
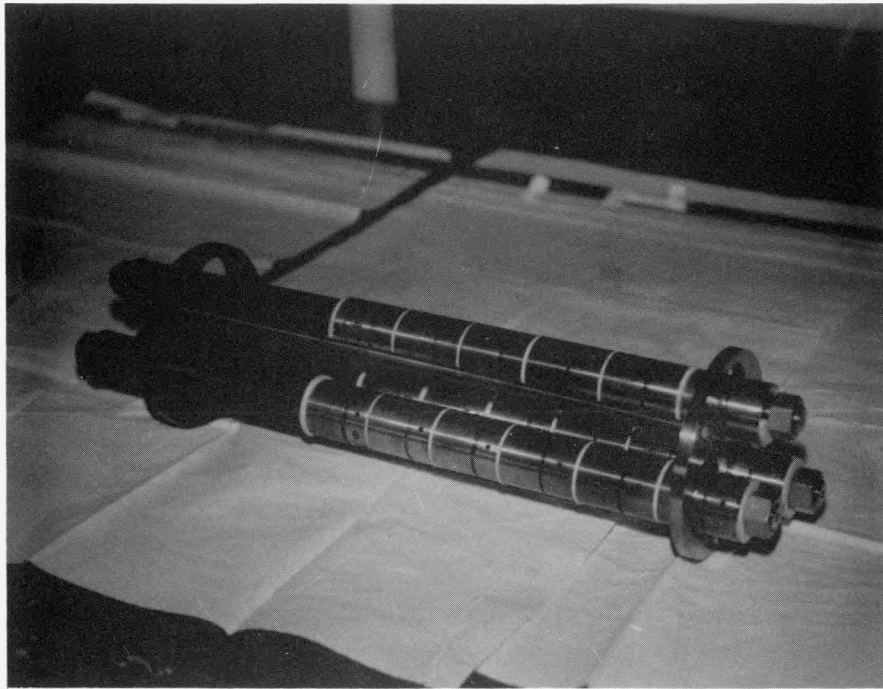


Fig. 5.1 Static adhesion test assemblies removed from furnace after 3,000-hr test

general, the procedure is to repeat the various examinations made prior to initiating the test. These examinations include: (1) photographing the parts as they are removed from the test furnace, (2) weighing the couples on an analytical balance, (3) leak-checking on a helium mass-spectrometer leak detector, (4) photographing the surface of the parts with a scanning electron microscope, and (5) weighing the parts on an analytical balance.

An additional evaluation step that will be included, if applicable, is to measure the force required to pull the conical seal samples apart. It is expected that they may be adhered to each other to some degree so the force will be measured if it exceeds normal handling forces.

The material for static adhesion testing of alternative materials has been received. The tests will be deferred until funding is available.

#### 5.1.2. Seal Leak Testing

The main part of the GCFR element seal evaluation is the leakage measurement test using full-size element and grid-plate parts at reactor operating temperature. The environment used is helium from commercial cylinders at a pressure of between 10 and 60 psi. The parts are clamped together by a central acting axial force to obtain the required net conical seal surface pressure forces required for sealing.

The helium leaking through the interface gap is measured using calibrated Brooks Rotameters in units of standard cubic feet per hour. The use of volumetric units permits a direct correlation between data at room temperature and pressure and at elevated temperature and pressure. This correlation is discussed below under data evaluation.

During the previous quarterly period, preliminary leakage measurements were conducted on two sets of parts at room temperature and at 300°C. The parts were then sent to the GA machine shop to be remachined with a concentricity and ovality tolerance of 0.0051 mm (+0.0002 in.) on the conical seal surface. This is a practical tolerance because the conical surfaces are ground accurately on an angle grinder to achieve the surface finish. All parts were then inspected for surface finish, angle, and

conical concentricity dimensions. The results of the inspection are:

| Test Part No.  | Angle       | Finish<br>( $\mu$ in.AA) | Concentricity            |       | Ovality                  |       |
|----------------|-------------|--------------------------|--------------------------|-------|--------------------------|-------|
|                |             |                          | (mm)                     | (in.) | (mm)                     | (in.) |
| Grid L-1-2A    | 30° 2' 52"  | 7                        | <0.0013 ( $\pm$ 0.00005) |       | <0.00254 ( $\pm$ 0.0001) |       |
| Element L-1-2A | 29° 58' 20" | 8                        | <0.0013 ( $\pm$ 0.00005) |       | <0.00254 ( $\pm$ 0.0001) |       |
| Grid L-1-2B    | 30° 3' 45"  | 6                        | <0.0013 ( $\pm$ 0.00005) |       | <0.00254 ( $\pm$ 0.0001) |       |
| Element L-1-2B | 29° 55' 37" | 8                        | <0.0013 ( $\pm$ 0.00005) |       | <0.00254 ( $\pm$ 0.0001) |       |

It is seen that the reported concentricity and ovality are better than the required tolerance by at least a factor of 2. The measurements represent the limit of measurement and the as-built tolerances probably are lower than those reported. It is noted that this is the result of a single finish grinding operation without extensive rework. The tool and part setup require the most time, but in quantity grinding the time per part is greatly reduced.

The angular tolerances on the parts drawing for test parts L-1-2A are 30° 2'  $\pm$ 1/2' and 29° 58'  $\pm$ 1/2' for the grid-plate and element parts, respectively, for a test nominal mismatch of 4'  $\pm$ 1'. The actual mismatch is 4' 32" for the as-built parts for test L-1-2A. The angular tolerances of the parts drawing for test parts L-1-2B are 30° 4'  $\pm$ 1/2' and 29° 56'  $\pm$ 1/2' for the grid and element parts, respectively, for a nominal mismatch of 8'  $\pm$ 1'. The actual mismatch is 8' 0.8" for test parts L-1-2B.

The first test series was then initiated with parts L-1-2A. However, the room-temperature leakage rates for both the element seal and vent-connection seals were below the range of the flow meters. The second set of parts were then set up for measurements and the results were the same, i.e., the leakage rates were too low for measurement. It was decided to interrupt the tests to procure helium flow meters to cover the low-flow range from 0.08 ft<sup>3</sup>/hr (0.629 cm<sup>3</sup>/sec) to 1 ft<sup>3</sup>/hr (7.87 cm<sup>3</sup>/sec) for the vent connection seal and 0.3 ft<sup>3</sup>/hr (2.36 cm<sup>3</sup>/sec) to 2 ft<sup>3</sup>/hr (15.7 cm<sup>3</sup>/sec) for the element seal.

After receiving the new flow meters, the tests were initiated with test parts L-1-2A. The test procedure was changed to include complete

testing at room temperature and to increase the temperature by  $\sim 100^\circ\text{C}$  steps to  $300^\circ\text{C}$ . This change was necessary to evaluate the effect of temperature changes on the clamp load mechanism and to check the accuracy of correlation of data from low temperature to high temperature. The data are currently being reduced. The parts from test L-1-2A will be examined, photographed, and then remachined to the next angle tolerance mismatch of  $12'$  between the grid and element seal cone angles. The room-temperature data and correlation are discussed below.

5.1.2.1. Evaluation of Data. The helium leakage data taken at low pressure and room temperature can be converted to helium leakage at GCFR operating conditions of  $570^\circ\text{F}$  ( $300^\circ\text{C}$ ). This is shown to be simply a viscosity correction by the following analysis.

The expression for pressure drop across the seal<sup>(1)</sup> is given by

$$\Delta P = \frac{1}{2} \rho \bar{v} \frac{4fL}{D_e} , \quad (5.1)$$

where  $\Delta P$  = pressure change,

$\rho$  = density,

$\bar{v}$  = average velocity,

$f$  = friction coefficient,

$L$  = channel length,

$D_e$  = hydraulic diameter.

Then, using the gas law for pressure relation, we have

$$P = \rho \frac{RT}{M} \quad (5.2)$$

where  $P$  = pressure,

$R$  = gas constant,

$T$  = absolute temperature,

$M$  = molecular weight.

For friction in gas at laminar flow,

$$f = \frac{C}{Re} \quad (5.3)$$

where C is a constant and Re, the Reynolds number, is given by

$$Re = \frac{4 \dot{m}}{\mu P_f} \quad (5.4)$$

where  $\dot{m}$  = mass flow rate,

$\mu$  = gas viscosity,

$P_f$  = wetted perimeter of gap.

The gas velocity is

$$V = \frac{\dot{m}}{A} \quad , \quad (5.5)$$

where A is the cross-sectional flow area.

Substituting Eqs. (5.2), (5.3), (5.4), and (5.5) into Eq. (5.1), the following is obtained

$$\Delta P = \frac{RT}{2 M_p} \left( \frac{\dot{m}}{A} \right)^2 \frac{4L}{(4A/P_f)} \frac{C}{(4 \dot{m}/\mu P_f)} \quad (5.6)$$

or

$$\Delta P = \left( \frac{C}{8} \frac{RT}{P} \right) \left( \frac{\mu}{M} \right) \frac{LP_f^2}{A^3} \dot{m} \quad . \quad (5.7)$$

Equation (5.7) relates the pressure drop,  $\Delta P$ , and mass flow to the following characteristic groups:

$$\left( \frac{C}{8} \frac{RT}{P} \right) \quad \text{test environment conditions,}$$

$$\frac{\mu}{M} \quad \text{gas properties,}$$

$$\frac{LP_f^2}{A^3} \quad \text{seal geometry.}$$

Changing these to a volume flow rate relation by using Eq.(5.2), we have

$$Q = \dot{m} = \dot{m} \left( \frac{RT}{MP} \right)$$

or

$$\dot{m} = \left( \frac{MP}{RT} \right) Q .$$

Equation (5.7) then becomes

$$\Delta P = \frac{C}{8} \mu \left( \frac{LP_f^2}{A^3} \right) Q$$

and thus

$$Q = \frac{\Delta P}{\frac{C}{8} \mu \left( \frac{LP_f^2}{A^3} \right)} .$$

The volume leakage rate,  $Q$ , is only a function of gas viscosity and seal geometry for a given  $\Delta P$  across the seal. Since the geometry of the test seal parts and the element seal are the same, then the volume leakage rate is only a function of the viscosity. Thus, room-temperature data can be correlated to reactor conditions by the following:

$$Q_{\text{design}} = \frac{\mu_{\text{test}}}{\mu_{\text{design}}} Q_{\text{test}}$$

and the mass flow is then

$$\dot{m}_{\text{design}} = Q_{\text{design}} \rho_{\text{design}} .$$

5.1.2.2. Test Results. The first tests conducted during this quarter were at room temperature. The viscosity corrections to design temperature of 300°C are

$$\mu_{\text{design}} = \mu_{300\text{C}} = 0.0744 \text{ lb/ft-hr (0.111 kg/h-m)} ,$$

$$\mu_{\text{test}} = \mu_{\text{RT}} = 0.052 \text{ lb/ft-hr (0.0774 kg/h-m)}$$

or

$$Q_{300C} = Q_{\text{test}} \times \frac{0.052}{0.0744} = 0.70 Q_{\text{test}} .$$

For the L-1-2A set of parts, the element seal leakage is always less than 0.3 STP ft<sup>3</sup>/hr (2.36 cm<sup>3</sup>/sec), which is the lowest range of accuracy on the flow meter. On this particular flow system in the test, there are five flow meters ranging by factors of 10 from 0.3 to 1000 STP ft<sup>3</sup>/hr (2.36 to 7,870 cm<sup>3</sup>/sec). The GCFR reference design allowable seal bypass leakage is 0.33% of the total reactor helium flow. On a volumetric basis for a 300-MW(e) core with 265 elements and a total reactor coolant flow of 1,540 lb/sec (699 kg/sec), this is approximately 150 STP ft<sup>3</sup>/hr (1180 cm<sup>3</sup>/sec) per element on the average. Thus, an exact measurement of flows below 0.3 STP ft<sup>3</sup>/hr (2.36 cm<sup>3</sup>/sec) does not appear to be warranted for an engineering test.

The vent-connection leakage for this set of parts is shown in Fig. 5.2 as a function of the pressure difference,  $\Delta P_{\text{vc}}$ , across the vent connection, the pressure difference,  $\Delta P_{\text{c}}$ , across the core element seal, and the clamping force between the conical sealing surface. The data are room temperature data that have been correlated to design temperature by the relation derived above. It is seen that the leakage rate is nearly a linear function of the  $\Delta P_{\text{vc}}$  and is independent of  $\Delta P_{\text{c}}$ . This would be expected since the guard ring function is to isolate the vent connection from the high-pressure side of the core. The vent-connection leakage is also independent of clamping pressure, which correlates with low element-seal leakage. That is, the angle mismatch ( $\sim 4$  min) favors the element seal and fixes the leakage gap of the vent connection seal.

The criterion for vent-connection leakage is specified to be one-tenth of the suction-hole flow. The design basis used for the suction-hole flow is 1.25 lb/hr (0.158 g/sec) for each vent connection, or 4 STP ft<sup>3</sup>/hr (31.5 cm<sup>3</sup>/sec) at a  $\Delta P_{\text{vc}}$  of 2 psi across the vent connection.

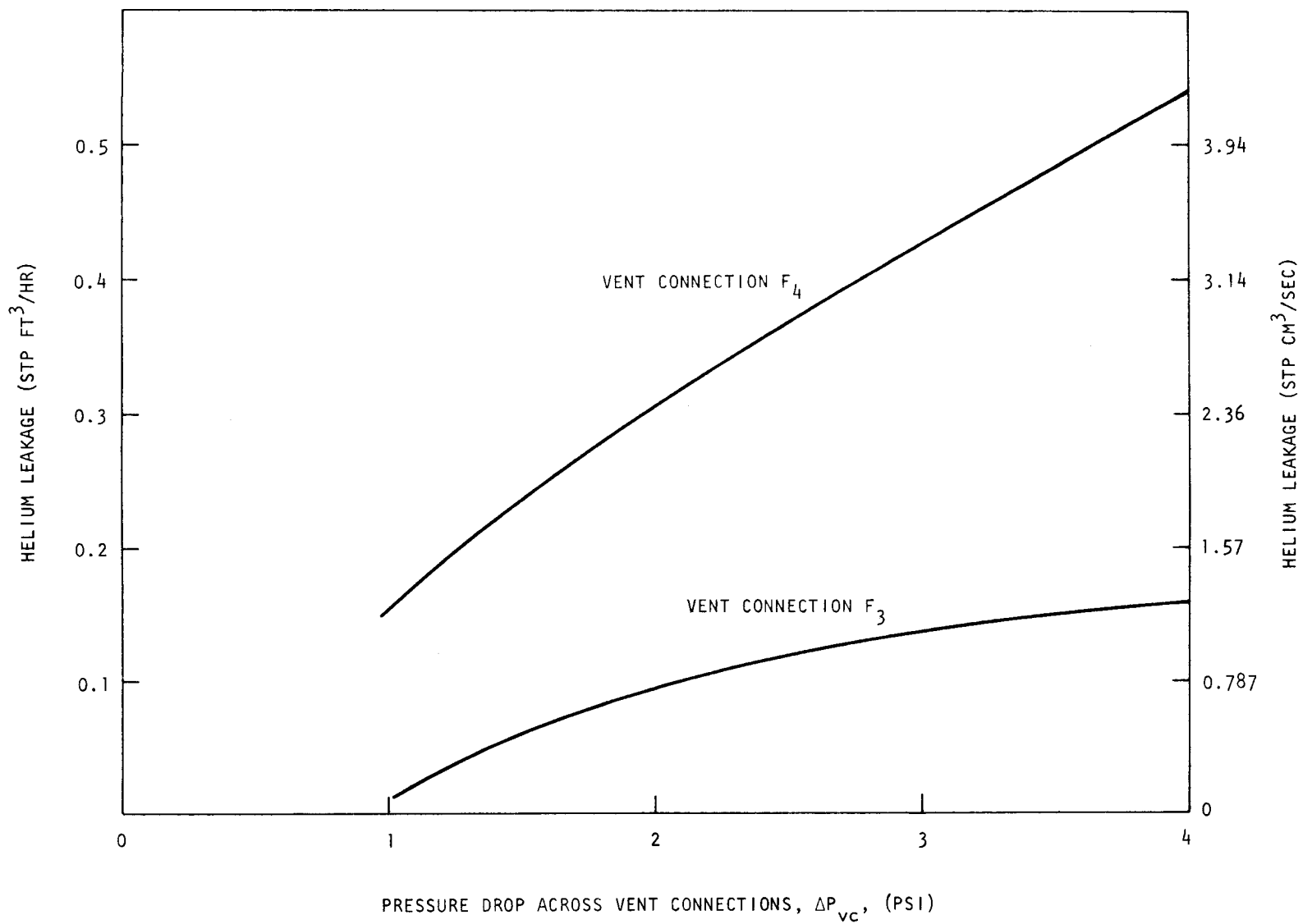


Fig. 5.2 Leakage rate at room temperature corrected to 300°C

Using this criterion, the design-basis leakage for the vent connection is 0.125 lb/hr (0.0158 g/sec) or 0.4 STP ft<sup>3</sup>/hr (3.15 cm<sup>3</sup>/sec). For this set of parts in this configuration, the actual leakage rate is within the design basis.

The test at temperature for this configuration will be continued. Then the element part will be rotated 180° to determine the leakage after rotation.

## 5.2. PES COMPUTER CODE DEVELOPMENT AND ANALYSES

Two computer codes are to be developed under this subtask: (1) a transient flow network code, which has not yet been identified, and (2) a radiation-detection and gamma-spectrum-monitoring instrumentation design and performance code called COUNT.

### 5.2.1. Transient Network Code

The results of a comprehensive literature survey of one-dimensional transient flow codes was completed. Further work on the development of this code has been discontinued for the remainder of this fiscal year.

### 5.2.2. COUNT Code

No effort was expended on the development of this code during this period. The activity on this development has been reduced to correlation of the code with the data extracted from irradiation capsule GB-10 (see Section 3.1).

## 5.3. FUEL-ELEMENT MANIFOLD FABRICATION DEVELOPMENT

The evaluation of alternative fabrication processes for the element manifold during this quarterly period consisted of the following:

1. Completion of quality control evaluation of a one-third segment of a manifold structure fabricated by electrodischarge machining (EDM).
2. Evaluation of forming fission-product passages by electrochemical machining (ECM) and by conventional drilling.
3. Evaluation of small segments of a variation of the manifold design with welded connections from the manifold to the fuel

rods and a welded cover plate over the passages in the manifold.

4. Procurement of three one-third segments of the manifold structure to be produced by casting.

A one-third segment of the manifold structure fabricated by first machining the hexagonal-shaped blank and then forming the webs by EDM from solution-annealed Type 316 stainless-steel rolled plate has been evaluated. As previously reported, measurements of the as-fabricated segment showed the dimensions to be within or acceptably close to the tolerances specified.<sup>(2)</sup>

A low-temperature (350°C for 4 hr) stress-relief treatment was performed on the one-third manifold segment to determine if residual stresses thus relieved would cause dimensional changes to occur. Dimensional measurements after the stress relief showed no movement of any part greater than 0.002 in. (0.0508 mm). The dimensions compared were (1) straightness of the webs, (2) parallelism of the webs to the base (edge of manifold structure), (3) web thickness, and (4) web spacing. The maximum change of dimension of 0.002 in. (0.0508 mm) occurred in the parallelism of the web to the base and is within the desired tolerances.

Two holes 8 in. (203 mm) deep and  $0.050 \pm 0.002$  in. ( $1.27 \pm 0.0508$  mm) in diameter (formed by drilling 4 in. deep from opposite ends of the test sample) were formed by the electrochemical machining (ECM) method. The runout for one of the holes was within the tolerance of 0.002 in./in. (0.0508 mm/mm). The runout in the second hole exceeded tolerance and the coincidence of the holes drilled from opposite ends was not good. Neutron radiographs taken of the test samples are shown in Fig. 5.3. The vendor believes that the runout in holes produced by the ECM method is primarily caused by nonmetallic inclusions in the metal (as illustrated in Fig. 5.4) and that the method is a viable means for producing the deep holes, provided inclusion-free metal is used. The drilling rate (excluding setup time) for the ECM method was stated to be 0.05 in./min (1.27 mm/min) or 3.0 in./hr (76.2 mm/hr).

One-step conventional drilling by itself is not considered a viable method for deep-hole drilling. However, it was found that a sufficiently deep hole (4 in. (102 mm) deep, 0.073 in. (1.85 mm) in diameter using

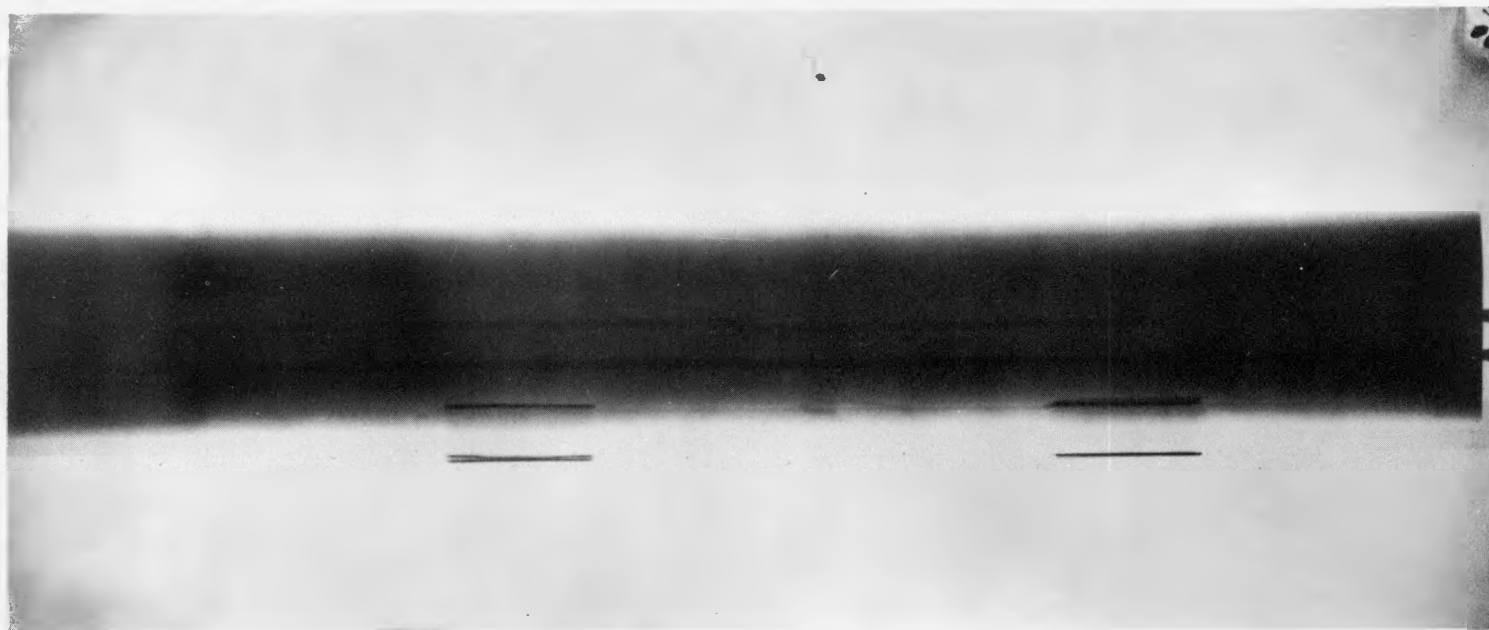


Fig. 5.3 Neutron radiograph of holes drilled by the ECM method from the right and left ends of a stainless steel block to meet at the center

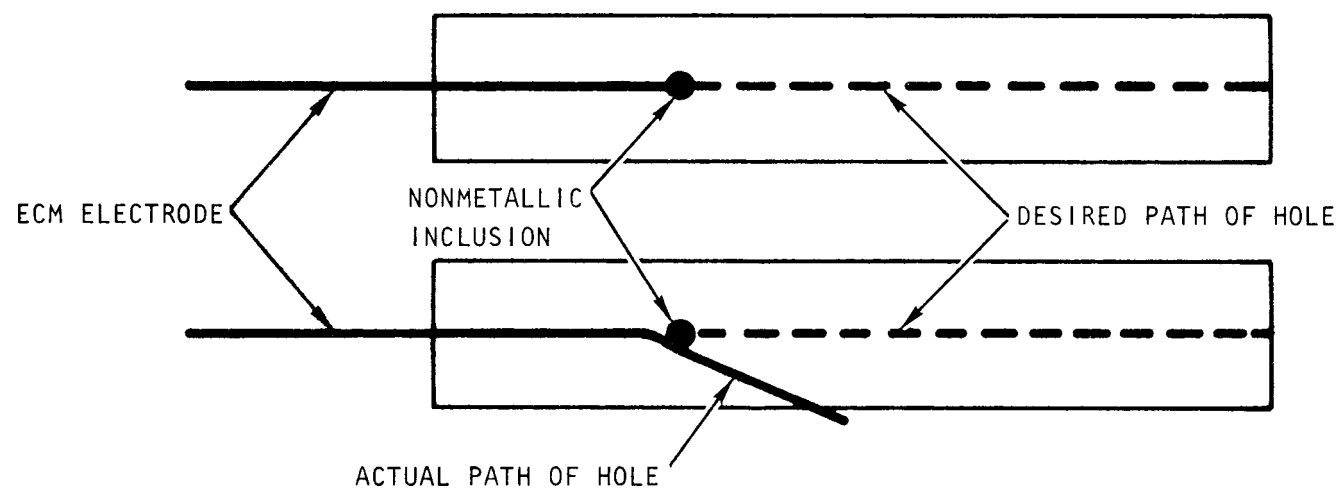


Fig. 5.4 Schematic showing how inclusions in the metal block can cause deflection of the electrode from the intended path, or misalignment

a No. 49 drill) could be drilled using three different types of conventional high speed drills in the proper sequence (jobbers length, taper length, and aircraft length). The measured runout for the hole was a maximum of 0.005 in. (0.127 mm). The drilling rate was found to be 1.5 in./hr (38.1 mm/hr), excluding setup time. An additional 8 in. (203 mm) long hole (4 in. (102 mm) deep starting from each end of the part and meeting at the center of its length) was drilled to check for coincidence of the holes. The sample has not yet been evaluated. Six additional 8 in. (203 mm) long holes are currently being drilled to evaluate the reliability of the conventional drilling method.

A sample with a fitted end cap for sealing the ends of passages by electron-beam welding (EBW) was prepared for trial purposes. The seal weld produced by the EBW method was found to be leak tight.

Samples were fabricated for EBW trials on rod-to-manifold joints and cover-plate-to-manifold seals in the variation of the manifold design employing a cover plate over the fission-gas passages. The trial welds were performed in a conventional vacuum chamber type of EBW equipment. All of the welds in these samples were found to be unsatisfactory. Some contained incomplete welds and some contained cracks. The cracks were probably caused by weld metal shrinkage in a "cosmetic" welding pass in which the beam was rotated in a circle over the initial weld to make the welded surface smoother.

Three one-third segments of a full-sized manifold structure that are to be precision cast have been ordered. The three segments were ordered to allow some evaluation of the reproducibility in quality and dimensions in manifold structures produced by casting. Delivery of the segments is scheduled for mid-february 1975.

#### 5.4. PLATEOUT AND PLUGGING

The goals of the plateout and plugging subtask were discussed previously.<sup>(2)</sup> The work to date has been to review pertinent literature regarding the experimental studies on fission-product migration, both in-pile and out-of-pile, and to draw some conclusions regarding the observed

results and their possible impact on the operation of the PES. The following is a brief recapitulation of the literature relevant to fission-product plateout and plugging and a description of the proposed experimental plan.

#### 5.4.1. Cesium Migration

The transport of cesium within a fuel rod is one of the most important considerations in connection with the formation of a flow-restricting plug. This transport is due to the high volatility of elemental cesium (b.p. 625°C) and the relatively high volatility of several of its compounds. The "peaking" of cesium deposition at the ends of the fuel column, i.e., at the fuel-blanket interfaces, is a well-documented phenomenon.<sup>(2-5)</sup> This peaking results from the thermal gradient in these interfacial regions. In the fuel region, cesium and its more volatile compounds would be expected to have moderate mobilities. In the blanket region, however, the surface temperatures are considerably lower, with near isothermal conditions expected within a pellet length from the fuel-blanket interface, at which point the temperature will be less than 325°C. At the reduced temperature, cesium or its compounds can condense in the porous blanket structure or in the blanket-cladding gap. Furthermore, if the blanket material is sufficiently hyperstoichiometric or if the oxygen potential in the fuel-blanket interfacial region has become large enough owing to thermally caused oxygen redistribution, then it is also possible for cesium to form uranates with the blanket. The cesium uranates, having a much lower density than the uranium dioxide blanket material, can produce local swelling that can, in turn, lead to possible restriction of the fuel-cladding gap or, in an extreme case, to the rupture of the cladding. This type of mechanism was likely to have caused the failure of a fuel rod in the NUMEC rod C-8 test,<sup>(6,7)</sup> a point that will be discussed further. In any case, flow restrictions at the fuel-blanket interface (but not complete blockages) have been reported in GCFR F-1 fuel rod G-7 and in the fuel rod in capsule GB-9. Capsule GB-10 is currently exhibiting a flow restriction during operation at or near full power (but not at zero power). It would be speculation at this time to try to assess the mechanism for such restrictions, but certainly one suspicion is that cesium deposition at or near the fuel-blanket interface is likely to be a contributing factor.

5.4.1.1. Effect of Fuel Stoichiometry. The distribution of cesium within the fuel rod will depend strongly on the oxygen-to-metal (O/M) ratio of the fuel. The results of fuel irradiations to 11 at-% burnup, described by Neimark, et al.,<sup>(3)</sup> showed that for a stoichiometric fuel (initial O/M = 1.99 to 2.00), cesium was uniformly distributed along the length of the fuel region at moderate concentrations with some at the ends of the fuel column. Furthermore, there was essentially no tail in the distribution of cesium into the blanket region. For a fuel with low initial O/M ratio (1.94 to 1.97), cesium was also distributed uniformly along the length of the fuel region; however, the cesium peak at the end of the fuel column was quite large and a substantial amount of cesium had migrated into the upper blanket, where it was suspected that reaction of cesium with the blanket material had taken place, predominantly in the first blanket pellet.

Out-of-pile tests have also shown that cesium thermomigration through mixed-oxide fuel is affected by the O/M ratio of the fuel.<sup>(9,10)</sup> With a fuel O/M ratio of 1.94, cesium migrated in an 1100° to 550°C thermal gradient essentially completely to the cold end. At an O/M ratio of 1.96, there appeared to be a bimodal transport process in which some of the cesium migrated rapidly to the cold end, whereas another portion of the cesium migrated much more slowly. It is believed that the cesium initially carried sufficient oxygen with it (as  $\text{Cs}_2\text{O}$ ) to raise the local fuel O/M ratio to greater than 1.985. This O/M value has been found to have a high enough oxygen potential to cause reaction of the fuel with cesium. Consequently, the slow mode of cesium migration could be due to a compound formation-dissociation-reformation type of mechanism, because there is no evidence that cesium-fuel compounds have any measurable volatility. With stoichiometric fuel (O/M = 2.00), cesium did not migrate significantly from the hot end of the fuel region (it is also possible that the migration rate is unobservably slow in the time scale of the experiments).

5.4.1.2. Migrating Species. There is still considerable speculation as to the various compounds of cesium that are important in both its migration and its compound constituents (molybdenum, oxygen, iodine, and tellurium).

The compound  $\text{Cs}_2\text{MoO}_4$  is quite another story. It has been hypothesized that with sufficiently high oxygen potentials and at sufficiently low temperatures, cesium molybdate is formed in the columnar grain and equiaxed fuel regions.<sup>(6,7,11,12)</sup> The reaction is said to take place between cesium vapor, oxygen, and molybdenum found in noble metal alloy inclusions. It is not clear whether  $\text{Cs}_2\text{MoO}_4$  is an important species in the axial transport of cesium, molybdenum, or oxygen; nevertheless, it has been postulated that  $\text{Cs}_2\text{MoO}_4$  plays a role in intergranular attack at the fuel-cladding interface.<sup>(12)</sup> It has been reported that cesium molybdate is less stable than the cesium-fuel compounds, such as  $\text{Cs}_{1.3}(\text{U,Pu})\text{O}_3$ ,<sup>(10)</sup> but this point is obscured by the fact that the molybdate has a high volatility and can vaporize prior to decomposition. The importance of  $\text{Cs}_2\text{MoO}_4$  in any plugging mechanism still remains to be investigated.

There are a number of candidate oxygenated cesium species, such as  $\text{Cs}_2\text{O}$ ,  $\text{Cs}_2\text{O}_2$ , and  $\text{CsOH}$ . The viability of any of these species depends on the oxygen potential in the environment in which they are to exist. An unlikely species is  $\text{Cs}_2\text{O}$  except at rather high oxygen potentials.<sup>(13)</sup> Not too much is known about the existence of  $\text{Cs}_2\text{O}_2$  or of its importance in the mechanism for cesium and oxygen transport. One of the more important cesium-oxygen compounds is  $\text{CsOH}$ .<sup>(13)</sup> The transport of cesium in out-of-pile thermal-gradient experiments has been aided by the presence of some hydrogen in the gaseous atmosphere employed.<sup>(14)</sup> It is believed that some of the hydrogen can participate in the reaction to produce  $\text{CsOH}$ . The influence of the oxygenated cesium compounds on cladding attack will be discussed in Section 5.4.2.

Cesium was shown to associate strongly with tellurium in out-of-pile thermomigration studies.<sup>(9)</sup> In addition, the combination of the two elements appears to associate quite strongly with the fuel, as indicated by the bulk of cesium and tellurium remaining at the hot end of the thermal-gradient diffusion tube. It is not clear at present what effect, if any, the association of cesium with tellurium will have on potential plugging mechanisms. Consequently, this point will have to be investigated further. The influence exerted by cesium in combination with tellurium on cladding attack will be discussed in Section 5.4.3.

5.4.1.3. Cesium Interaction with Blanket. It appears to be quite possible that the stoichiometry of the blanket uranium dioxide may be quite important in limiting the extent of cesium reaction to produce low-density uranates that could lead to swelling or eventual cladding rupture. As mentioned before, the formation of  $\text{Cs}_2\text{UO}_4$  by reaction of cesium with the blanket material was postulated as the mechanism for the cladding rupture of a fuel rod in NUMEC test rod C-8.<sup>(6,7)</sup> This particular fuel rod had a mixed-oxide fuel with an initial O/M ratio of 1.96. This O/M value has been shown to permit facile cesium migration. However, it is not clear what the blanket O/M ratio was for this fuel rod, and this could be an important point because other fuel rods having low initial O/M ratios in the mixed oxide have not exhibited failure. Out-of-pile experimentation has shown that there are a number of cesium-uranium compounds that can be formed,<sup>(14,15)</sup> so volume change calculations based only on the formation of  $\text{Cs}_2\text{UO}_4$ <sup>(7)</sup> may be inadequate. A further study of the interaction of cesium (and its compounds that are believed to be important in axial transport) with uranium dioxide is one of utmost importance. In experiments of this type, it will be necessary to evaluate the role of oxide stoichiometry as well as the oxygen potential of the gaseous environment in the fuel rod.

#### 5.4.2. Oxygen Migration

The redistribution of oxygen in the fuel is an important factor in determining the mobility of species that can serve as sources of potential plugging problems and in affecting the conditions for cladding attack. Experimental evaluation has indicated that the radial oxygen potential gradient is established during initial fuel restructuring and that it varies only slightly with burnup.<sup>(16)</sup> Less has been studied about axial oxygen transport; nevertheless, out-of-pile studies with candidate oxygen getters have shown that axial oxygen transport is slow and that the placement of the getter as close to the fuel region as possible was required in order to have the proper effect.<sup>(17)</sup>

5.4.2.1. Effect of Fuel Stoichiometry. As the O/M ratio of the fuel increases, the quantity of oxygen available for migration also increases. However, experimental results show that the slope of the radial

oxygen gradient from the central void to the fuel-cladding interface decreases as the O/M ratio of the fuel increases from hypostoichiometric to stoichiometric values.<sup>(16)</sup> For  $UO_{2+x}$  blanket material, excess oxygen will migrate toward the hotter region,<sup>(18)</sup> so there exists the potential for an oxygen buildup at the fuel-blanket interface from both sides of that interface.

5.4.2.2. Techniques for Measuring Oxygen Redistribution. One of the methods devised for measuring the local oxygen potential in irradiated fuels involves the determination of the Mo/MoO<sub>2</sub> ratio at discrete locations in the columnar grain and equiaxed regions of the fuel by electron microprobe techniques.<sup>(16)</sup> Elemental molybdenum is found in noble metal inclusions in the hotter regions of the fuel, whereas the molybdenum found in the oxide matrix surrounding these inclusions is believed to be present as MoO<sub>2</sub>. If this redox couple is in equilibrium with its environment, then it can certainly serve as an indicator for determining localized O/M ratios or oxygen potentials. An electromotive force (EMF) probe employing a zirconia electrolyte and Ni/NiO reference electrode has also been employed for determining oxygen potentials at discrete locations along the oxide matrix.<sup>(14)</sup> This method seems relatively straightforward and should have wide applicability.

5.4.2.3. Transport Mechanism. Some controversy exists at present in the literature regarding the predominant mechanism of oxygen transport in the fuel region. One school of thought focuses attention on the ionic migration of oxygen in the solid oxide matrix.<sup>(19)</sup> The other opinion is that solid-state migration is important only above a certain critical temperature, whereas below that temperature gas-phase transport of oxygenated species becomes important.<sup>(14)</sup> Regardless of the primary mechanism of radial oxygen transport from the hottest portions of the fuel region, it does not appear that solid-state diffusion can be important in the kind of axial oxygen transport that would be of concern to possible plugging mechanisms. In addition, the evidence has shown that the transport of molybdenum from the hotter regions of the fuel depends on a concomitant radial transport of cesium and oxygen.

#### 5.4.3. Cladding Attack

There are still many unknowns regarding the understanding of the influence and importance of the many parameters that affect cladding attack. Postirradiation electron microprobe examinations of the regions in the fuel-cladding gap in close proximity to the region of cladding attack have shown the presence of cesium, molybdenum, and tellurium.<sup>(2,12,13,20)</sup> Consequently, these elements (or their compounds) are highly suspect in their behavior toward cladding attack. One generality that has appeared is that no cladding attack is observed at temperatures less than 500°C. The subject of cladding attack is of some direct importance to the area of plate-out and plugging in that the groundwork experimentation has shown that the site of cladding attack is often the site of an accumulation of fission products, which could conceivably indicate the incipient formation of a flow restriction or plug.

5.4.3.1. Effect of Oxygen Potential. It appears that with fuels of initial O/M ratios of less than 1.97, no cladding attack is observed even at a burnup of 11 at-% and a cladding temperature of 560°C.<sup>(3)</sup> Out-of-pile experiments have also shown that the local oxygen potential will dictate whether fission-product-induced cladding attack can take place. In one series of experiments, it was demonstrated that in order to cause attack by cesium alone or by  $\text{MoO}_3$ , an oxygen potential more positive than -84kCal/mole is required.<sup>(20)</sup> This potential is applied by using a series of solid metal/metal-oxide oxygen buffers. In another series of experiments, it was observed that a mixture of  $\text{Cs}_2\text{O}$  and  $\text{CsOH}$  exhibited rather vigorous attack of the cladding.<sup>(21)</sup> It was also found that a cesium/tellurium ratio of two or less could produce intergranular attack if the oxygen potential was more positive than the threshold value for oxidation of stainless steel.<sup>(20)</sup> The presence of carbon can also enhance the course of fission-product-induced intergranular attack if the oxygen potential is just slightly greater than the oxidation threshold.<sup>(20)</sup> In this way, localized, semistable carbides can be formed and dissociated, which would allow intergranular penetration to occur. When the oxidation potential becomes sufficiently high, the carbon becomes associated strongly with oxygen and is no longer able to form the intermediary carbides. It has

been suggested that the data that have been amassed to date indicate the need for a liquid electrolyte to participate in the intergranular attack mechanism.<sup>(12,21)</sup> In connection with this hypothesis, it is believed that liquid phases of cesium-oxygen and cesium-molybdenum-oxygen compounds exist above 500°C. But, of course, the formation of these compounds depends on the prevailing oxygen potential.

#### 5.4.4. Experimental Plan

The development of the plateout and plugging experimental plan will be a natural consequence of the literature review that has been performed. Discussions have already taken place with ANL and with General Electric at Vallecitos, the two major groups that have been investigating fission-product-fuel-cladding interactions. Since there are areas of obvious mutual interest between the experimental aspects of the LMFBR developmental program and the GCFR program regarding the potential for flow restriction in the fuel rod, it appears to be advantageous to initiate some work at those laboratories where related aspects are being investigated. As a result, GA will be submitting proposals to both of these groups for add-on experimentation in the area of out-of-pile fission product-blanket interaction with special emphasis on evaluating the conditions that lead to constriction. This will permit experimental work at GA in the field of fission-product plateout in the grid-plate holes and monitor lines. This research will aim at evaluating conditions for volatile fission-product deposition and liftoff as a function of physical form (gas or aerosol) and grid-plate and monitor-line geometry or morphology.

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## VI. CORE ASSEMBLIES STRUCTURAL-THERMAL-FLOW TESTS (189a No. 13759)

A series of out-of-pile simulation tests are to be performed to demonstrate the ability of the GCFR fuel-, control-, and blanket-assembly designs to meet design goals and to verify predictions of analytical models that describe design operation and accident behavior. The test emphasis will be to obtain thermal-structural data for steady-state, transient, and margin conditions using electrically heated rod bundles in a dynamic helium loop.

The program plan for the GCFR Core Flow Test Loop<sup>(1)</sup> contains the requirements for and the test program to be conducted in the Core Flow Test Loop (CFTL) that will be constructed and operated by Holifield National Laboratory (HNL). Work during this quarterly period has included planning of analysis and predictions, analyses in selected areas, review and evaluation of HNL's conceptual system design description, and a definition of the testing loop for the prototype assemblies.

### 6.1. TEST ANALYSIS AND PREDICTION PLAN

A prime objective of the test program is to verify the predictions of analytical models of the fuel, control, and blanket assemblies. An initial step has been taken to identify the analyses that will be verified by comparing test results with predictions. These analytical methods and computer codes will be applied to the various sizes and configurations of the test assemblies. However, the test simulation will be approximate rather than exact, so additional analyses will be planned to account for the modeling deviations, such as a reduced number of rods in an assembly (i.e., larger ratio of duct surface to rod surface), and the differences in thermal properties between an operating fuel rod and an electrically heated rod. Also, predictive analysis of the loop performance is required to ensure that the loop will provide the desired test conditions. The test analysis and prediction plan deals mainly with the first two types

of analysis: (1) application of GCFR analysis to predict test assembly performance and (2) prediction of the effects that are introduced by the approximate nature of the tests. Although the prediction of loop performance is an HNL activity and will be planned and reported by HNL, sufficient activity is planned in this area to review and confirm the HNL loop design.

The analyses to be performed are listed below.

Fuel- and Control-assemblies Operating Parameter Prediction

1. Average or macroscopic steady-state operating conditions over a range of flows and powers;  $\Delta P$  versus flow prediction.
2. Analysis of bundle heat loss to surroundings and evaluation of how this perturbs internal bundle conditions.
3. Analysis of flow and power conditions when natural convection affects flow distribution.
4. Analysis of reduced roughening effects as a function of Reynolds number.
5. Analysis of flow-transition (turbulent to laminar) effects.
6. Analysis of axial power distribution options; cosine versus uniform distribution.
7. Analysis of transverse power distribution and effect on flow and temperature distribution. Temperature prediction for comparison with measurements.
8. Analysis of heat loss through control-rod guide tube at steady-state for a range of flows and powers.
9. Analysis of bundle performance with hot-spot allowance to predict steady-state temperature for comparison with measurements.
10. Analysis of bundle transient thermal performance for a range of power and flow.
11. Analysis of bundle transient thermal performance for a range of power, flow, and depressurization.

12. Analysis of bundle margin performance for steady-state and transient conditions.
13. Analysis of rod bowing and axial movement to provide rod stresses and strains at steady-state.
14. Analysis of spacer-grid distortion at steady-state.
15. Analysis of spacer-grid holder distortion at steady-state.
16. Analysis of duct distortion at steady-state.
17. Analysis and interaction of coolant geometry and thermal-mechanical interaction at steady-state.
18. Analysis of relative and absolute movement of rods, spacers, element duct, and guide tube during transients.
19. Analysis of bundle stress and strains during transients.
20. Analysis of wear during simulation of normal transients.
21. Analysis of coolant geometry distortion during transients and feedback effects on thermal performance.
22. Analysis to determine potential for vibration of rods, spacers, and element duct and frequency and amplitude.
23. Evaluation of the effect of extreme operating conditions.
24. Evaluation of failure mode or margin to failure.
25. Evaluation of local blockage and input for specification of local-blockage test.
26. Evaluation of molten cladding relocation.
27. Evaluation of control-rod dynamics.
28. Analysis of flow-control orifice operation.
29. Analysis of outlet temperature sensor performance.

A similar set of analyses will be planned for the blanket assembly. The analysis of the blanket assembly differs from the fuel and control assembly because it will be full size and will contain baffles to promote cross flow.

### Analyses in Support of Bundle Design and Specification of Loop Operating Parameters

1. Analysis of power control and its perturbation of the simulation, such as the effect of oscillating power on axial movement.
2. Analysis to determine best simulation that accounts for difference in transient thermal behavior of heater rods compared to fuel rods.
3. Evaluation of test compromises to determine effect of changing bundle size, length, spacer design, spacer-holder design, and element duct design.
4. Evaluation of gas inlet options and the resulting effect on flow through the simulated core region.

### Loop Operating Parameter Prediction

1. Steady-state operating conditions over a range of flows and powers.
2. Operating power transients over a range of flows and powers (macroscopic treatment of loop components).
3. Depressurization transients over a range of flows and powers (macroscopic treatment of loop components).

## 6.2. TEST ANALYSIS AND TEST CONDITIONS

Specific work has been initiated to identify the required input data and to provide HNL sample operating conditions in preparation for a conceptual test specification.

### 6.2.1. GCFR 100% Power Operating Conditions

The heater power control for the loop must provide for the GCFR operating power level, the GCFR power tilt across an assembly, and the power reduction associated with reactor trip to after-heat levels. Using detailed power data from the physics code GAUGE<sup>(2)</sup>, the individual fuel-rod powers are calculated by a linear interpolation with the RODPWR code. Typical rod powers and operating parameters for the 300-MW(e) GCFR are presented in Figs. 6.1, 6.2, and 6.3 for the fuel assembly, control assembly, and blanket assembly, respectively.

Each number in the hexagonal display represents a fuel rod and is the total axial power for that fuel rod. Maximum powers are based on a sinusoidal axial power distribution with a maximum-to-average value of 1.21.

#### 6.2.2. GCFR Transients from 100% Power

The initial scoping transient requirements were based on typical and limiting transients to provide a design basis for the CFTL. This information is being supplemented by specific transients, with the data given numerically for ease and accurate use in the computer code that will simulate the loop performance. The data for a power, flow, and pressure transient that is predicted for a normal trip as the result of an upset condition are listed in Table 6.1. The data listed in Table 6.2 are for power, flow, and pressure transients that are predicted for a depressurization accident of a failure of a large seal-weld closure that results in rapid depressurization. This is a faulted condition.

#### 6.2.3. Thermal Evaluation of CFTL Tests

The COBRA code<sup>(3)</sup> is being developed to predict the detailed flow-thermal performance of the GCFR assemblies. COBRA models of the CFTL fuel assemblies have been established. The geometric input data for 19-, 37-, and 61-rod assemblies are given in Table 6.3 and the model configurations are given in Figs. 6.4, 6.5, and 6.6.

Initial attempts to run COBRA for the 19-rod assembly have been unsuccessful and code debugging is continuing. An older version of COBRA-G (HETHRA) with only steady-state capability is being used temporarily to predict thermal-flow information. This code predicts axial and radial information on coolant temperatures, cladding temperatures, pressures, local flow rates, and cross flows.

#### 6.2.4. CFTL Dynamics

A scoping dynamic model of the CFTL is required to provide input information for the preparation of test specifications, to evaluate the HNL loop design, and to develop background knowledge for evaluating the detailed dynamic model that ORNL is developing. The model will predict helium flow, temperature, and pressure as a function of time at the connecting

ASSEMBLY POWER = 6280. KW

MAX ROD POWER = 27.64 KW                      MIN ROD POWER = 18.09 KW

MAX ROD LINEAR RATING = 10.24 KW/FT

ROD TO ROD VARIATION - MAXIMUM = .5913 KW                      MINIMUM = .0203 KW

HELIUM FLOW THROUGH ASSEMBLY = 5.3024 KG/SEC                      FLOW PER ROD = 19.6 G/SEC

FLOW AREA = 17.54 IN\*IN                      H. T. AREA/FT = 21.870 FT                      HYDRAULIC DIAMETER = .267 IN

HELIUM MASS VELOCITY = 345469. LB/(HR\*FT\*FT)

AVG REYNOLDS NO. = 89366.

HELIUM INLET TEMPERATURE = 322.0 C                      AVERAGE OUTLET TEMPERATURE = 550.0 C

MAX OUTLET TEMPERATURE = 592.9 C                      MIN OUTLET TEMPERATURE = 499.3 C (NO MIXING)

Fig. 6.1a Input and output data associated with individual rods for fuel assembly 6.06 at initial operation

23.59 23.07 22.55 22.02 21.50 20.98 20.46 19.94 19.41 18.89  
 24.04 23.57 23.05 22.53 22.00 21.48 20.96 20.44 19.92 19.39 18.87  
 24.49 24.02 23.55 23.03 22.51 21.98 21.46 20.94 20.42 19.90 19.38 18.71  
 24.94 24.47 24.00 23.53 23.01 22.49 21.96 21.44 20.92 20.40 19.81 19.21 18.62  
 25.39 24.92 24.45 23.93 23.51 22.99 22.47 21.94 21.42 20.90 20.31 19.72 19.13 18.53  
 25.84 25.37 24.90 24.43 23.96 23.49 22.97 22.45 21.92 21.40 20.81 20.22 19.63 19.04 18.44  
 26.29 25.82 25.35 24.88 24.41 23.94 23.47 22.95 22.43 21.90 21.31 20.72 20.13 19.54 18.95 18.36  
 26.74 26.27 25.80 25.33 24.86 24.39 23.92 23.45 22.93 22.40 21.81 21.22 20.63 20.04 19.45 18.86 18.27  
 27.19 26.72 26.25 25.78 25.31 24.84 24.37 23.90 23.43 22.91 22.32 21.72 21.13 20.54 19.95 19.36 18.77 18.18  
 27.64 27.17 26.70 26.23 25.76 25.29 24.82 24.35 23.88 23.41 22.82 22.23 21.63 21.04 20.45 19.86 19.27 18.68 18.09  
 27.61 27.14 26.67 26.20 25.73 25.26 24.79 24.32 23.85 23.35 22.76 22.17 21.57 20.98 20.39 19.80 19.21 18.62  
 27.58 27.11 26.64 26.17 25.70 25.23 24.76 24.29 23.79 23.29 22.70 22.11 21.51 20.92 20.33 19.74 19.15  
 27.54 27.07 26.60 26.13 25.66 25.19 24.72 24.23 23.73 23.23 22.64 22.04 21.45 20.86 20.27 19.68  
 27.51 27.04 26.57 26.10 25.63 25.16 24.66 24.16 23.67 23.17 22.58 21.98 21.39 20.80 20.21  
 27.48 27.01 26.54 26.07 25.60 25.10 24.60 24.10 23.61 23.11 22.52 21.92 21.33 20.74  
 27.45 26.98 26.51 26.04 25.54 25.04 24.54 24.04 23.55 23.05 22.45 21.86 21.27  
 27.42 26.95 26.48 25.98 25.48 24.98 24.48 23.98 23.48 22.99 22.39 21.80  
 27.39 26.92 26.42 25.92 25.42 24.92 24.42 23.92 23.42 22.93 22.33  
 27.36 26.86 26.36 25.86 25.36 24.86 24.36 23.86 23.36 22.37

Fig. 6.1b Individual rod power (kW) for fuel assembly 6.06 at initial operation; total power = 6280 kW

ASSEMBLY POWER = 5924. KW

MAX ROD POWER = 26.62 KW                      MIN ROD POWER = 23.87 KW

MAX ROD LINEAR RATING = 9.86 KW/FT

ROD TO ROD VARIATION - MAXIMUM = .1740 KW                      MINIMUM = .0041 KW

HELIUM FLOW THROUGH ASSEMBLY = 5.0017 KG/SEC                      FLOW PER ROD = 21.6 G/SEC

FLOW AREA = 15.68 IN\*IN                      H. T. AREA/FT = 19.790 FT                      HYDRAULIC DIAMETER = .264 IN

HELIUM MASS VELOCITY = 364635. LB/(HR\*FT\*FT)

AVG REYNOLDS NO. = 93781.

HELIUM INLET TEMPERATURE = 322.0 C

AVERAGE OUTLET TEMPERATURE = 550.0 C

MAX OUTLET TEMPERATURE = 559.7 C

MIN OUTLET TEMPERATURE = 535.1 C (NO MIXING)

Fig. 6.2a Input and output data associated with individual rods for control assembly 4.04 at initial operation

|                                                                                           |                                     |
|-------------------------------------------------------------------------------------------|-------------------------------------|
| 25.38 25.22 25.05 24.90 24.75 24.59 24.43 24.27 24.12 23.96                               |                                     |
| 25.52 25.38 25.23 25.07 24.91 24.75 24.60 24.44 24.28 24.12 23.95                         |                                     |
| 25.65 25.52 25.39 25.23 25.08 24.92 24.76 24.60 24.44 24.29 24.11 23.94                   |                                     |
| 25.79 25.66 25.53 25.40 25.24 25.08 24.92 24.77 24.61 24.45 24.28 24.10 23.93             |                                     |
| 25.93 25.80 25.67 25.54 25.40 25.25 25.09 24.93 24.77 24.61 24.44 24.27 24.09 23.92       |                                     |
| 26.07 25.94 25.80 25.67 25.54 25.41 25.25 25.09 24.94 24.78 24.60 24.43 24.26 24.08 23.91 |                                     |
| 26.20 25.07 25.94 25.81 25.68 25.55                                                       | 24.77 24.59 24.42 24.25 24.07 23.90 |
| 26.34 26.21 26.08 25.95 25.82 25.69                                                       | 24.76 24.59 24.41 24.24 24.06 23.89 |
| 26.48 26.35 26.22 26.09 25.95 25.82                                                       | 24.75 24.58 24.40 24.23 24.05 23.88 |
| 26.62 26.49 26.35 26.22 26.09 25.96                                                       | 24.74 24.57 24.39 24.22 24.04 23.87 |
| 26.61 26.48 25.35 26.22 26.09 25.96                                                       | 24.90 24.72 24.55 24.37 24.20 24.03 |
| 26.61 26.48 26.35 26.22 26.09 25.95                                                       | 25.05 24.88 24.70 24.53 24.35 24.18 |
| 26.60 26.47 26.34 26.21 26.08 25.95                                                       | 25.21 25.03 24.86 24.68 24.51 24.34 |
| 26.60 26.47 26.34 26.21 26.08 25.94 25.80 25.65 25.51 25.36 25.19 25.01 24.94 24.67 24.49 |                                     |
| 26.60 26.47 26.33 26.20 26.07 25.93 25.79 25.63 25.49 25.34 25.17 24.99 24.82 24.65       |                                     |
| 26.59 26.46 26.33 26.20 26.05 25.91 25.76 25.62 25.47 25.32 25.15 24.98 24.80             |                                     |
| 26.59 26.46 26.33 26.13 26.03 25.89 25.74 25.60 25.45 25.31 25.13 24.98                   |                                     |
| 26.53 26.45 26.31 26.16 26.02 25.87 25.72 25.58 25.43 25.29 25.11                         |                                     |
| 26.53 26.43 26.29 26.14 26.00 25.85 25.71 25.56 25.41 25.27                               |                                     |

Fig. 6.2b Individual rod power (kW) for control assembly 4.04 at initial operation; total power = 5925 kW

ASSEMBLY POWER = 399. KW

MAX ROD POWER = 6.36 KW                      MIN ROD POWER = 1.20 KW

MAX ROD LINEAR RATING = 2.18 KW/FT

ROD TO ROD VARIATION - MAXIMUM = .7373 KW                      MINIMUM = .0501 KW

HELIUM FLOW THROUGH ASSEMBLY = .3369 KG/SEC                      FLOW PER ROD = 2.7 G/SEC

FLOW AREA = 10.60 IN\*IN                      H. T. AREA/FT = 18.617 FT                      HYDRAULIC DIAMETER = .190 IN

HELIUM MASS VELOCITY = 36314. LB/(HR\*FT\*FT)

AVG REYNOLDS NO. = 6714.

HELIUM INLET TEMPERATURE = 322.0 C

AVERAGE OUTLET TEMPERATURE = 550.0 C

MAX OUTLET TEMPERATURE = 780.1 C

MIN OUTLET TEMPERATURE = 408.1 C (NO MIXING)

Fig. 6.3a Input and output data associated with individual rods for blanket assembly 7.07 at initial operation

|      |      |      |      |      |      |      |      |      |      |      |      |      |      |      |      |
|------|------|------|------|------|------|------|------|------|------|------|------|------|------|------|------|
|      |      |      |      | 3.69 | 3.32 | 2.94 | 2.57 | 2.19 | 1.82 | 1.45 |      |      |      |      |      |
|      |      |      |      | 4.14 | 3.52 | 3.15 | 2.77 | 2.40 | 2.03 | 1.65 | 1.40 |      |      |      |      |
|      |      |      |      | 4.58 | 3.97 | 3.35 | 2.98 | 2.60 | 2.23 | 1.86 | 1.61 | 1.36 |      |      |      |
|      |      |      |      | 5.03 | 4.41 | 3.80 | 3.18 | 2.81 | 2.44 | 2.06 | 1.81 | 1.57 | 1.32 |      |      |
|      |      |      |      | 5.47 | 4.86 | 4.24 | 3.63 | 3.01 | 2.64 | 2.27 | 2.02 | 1.77 | 1.53 | 1.28 |      |
|      |      |      |      | 5.92 | 5.30 | 4.69 | 4.07 | 3.46 | 2.84 | 2.47 | 2.22 | 1.98 | 1.73 | 1.48 | 1.24 |
| 6.36 | 5.75 | 5.13 | 4.52 | 3.90 | 3.29 | 2.68 | 2.43 | 2.18 | 1.94 | 1.69 | 1.44 | 1.20 |      |      |      |
| 6.15 | 5.54 | 4.93 | 4.31 | 3.70 | 3.08 | 2.59 | 2.34 | 2.09 | 1.85 | 1.60 | 1.35 |      |      |      |      |
|      |      |      |      | 5.95 | 5.33 | 4.72 | 4.10 | 3.49 | 2.99 | 2.50 | 2.25 | 2.00 | 1.76 | 1.51 |      |
|      |      |      |      | 5.74 | 5.13 | 4.51 | 3.90 | 3.40 | 2.90 | 2.41 | 2.16 | 1.91 | 1.67 |      |      |
|      |      |      |      | 5.53 | 4.92 | 4.30 | 3.81 | 3.31 | 2.81 | 2.32 | 2.07 | 1.82 |      |      |      |
|      |      |      |      | 5.33 | 4.71 | 4.22 | 3.72 | 3.22 | 2.72 | 2.23 | 1.98 |      |      |      |      |
|      |      |      |      | 5.12 | 4.62 | 4.13 | 3.63 | 3.13 | 2.64 | 2.14 |      |      |      |      |      |

Fig. 6.3b Individual rod power (kW) for blanket assembly 7.07 at initial operation; total power = 399 kW

Table 6.1  
TRANSIENT DATA FOR GCFR NORMAL TRIP

| Time<br>(sec) | Power<br>(MW) | Flow<br>(lb/sec) | Pressure<br>(psi) | Time<br>(sec) | Power<br>(MW) | Flow<br>(lb/sec) | Pressure<br>(psi) |
|---------------|---------------|------------------|-------------------|---------------|---------------|------------------|-------------------|
| 0.00          | 826.50        | 1524.00          | 1279.00           | 5.40          | 81.18         | 606.90           | 1237.20           |
| 0.20          | 825.20        | 1524.00          | 1279.00           | 5.60          | 80.41         | 552.60           | 1236.60           |
| 0.40          | 640.20        | 1518.00          | 1278.30           | 5.80          | 79.67         | 498.30           | 1236.00           |
| 0.60          | 294.20        | 1512.00          | 1277.40           | 6.00          | 78.95         | 444.00           | 1235.60           |
| 0.80          | 126.00        | 1524.00          | 1278.40           | 6.20          | 78.26         | 389.70           | 1235.20           |
| 1.00          | 117.70        | 1518.00          | 1277.00           | 6.40          | 77.59         | 335.40           | 1234.90           |
| 1.20          | 113.50        | 1512.00          | 1275.40           | 6.60          | 76.95         | 332.80           | 1234.60           |
| 1.40          | 110.00        | 1506.00          | 1273.50           | 6.80          | 76.32         | 330.10           | 1234.30           |
| 1.60          | 107.10        | 1500.00          | 1271.40           | 7.00          | 75.71         | 327.50           | 1234.00           |
| 1.80          | 104.50        | 1494.00          | 1269.10           | 8.00          | 72.96         | 314.40           | 1232.60           |
| 2.00          | 102.20        | 1474.00          | 1266.70           | 9.00          | 70.65         | 301.20           | 1231.30           |
| 2.20          | 100.10        | 1453.00          | 1264.20           | 10.00         | 68.67         | 288.00           | 1230.10           |
| 2.40          | 96.28         | 1433.00          | 1261.60           | 11.00         | 65.24         | 274.90           | 1238.90           |
| 2.60          | 96.57         | 1376.00          | 1259.10           | 12.00         | 63.34         | 261.70           | 1227.70           |
| 2.80          | 95.00         | 1319.00          | 1256.60           | 13.00         | 61.65         | 250.70           | 1226.60           |
| 3.00          | 93.56         | 1261.00          | 1254.20           | 14.00         | 60.13         | 240.30           | 1225.60           |
| 3.20          | 92.21         | 1204.00          | 1251.80           | 15.00         | 58.76         | 229.90           | 1224.60           |
| 3.40          | 90.93         | 1154.00          | 1249.60           | 16.00         | 57.50         | 219.40           | 1223.60           |
| 3.60          | 89.73         | 1096.00          | 1247.40           | 17.00         | 56.35         | 209.00           | 1222.70           |
| 3.80          | 88.60         | 1041.00          | 1245.30           | 18.00         | 55.29         | 198.60           | 1221.90           |
| 4.00          | 87.52         | 987.10           | 1244.00           | 19.00         | 54.30         | 188.10           | 1221.20           |
| 4.20          | 86.49         | 932.80           | 1242.70           | 20.00         | 53.38         | 177.70           | 1220.50           |
| 4.40          | 85.50         | 878.50           | 1241.60           | 30.00         | 46.54         | 102.40           | 1217.10           |
| 4.60          | 84.56         | 824.50           | 1240.50           | 40.00         | 42.14         | 88.96            | 1217.10           |
| 4.80          | 83.67         | 769.90           | 1239.60           | 50.00         | 39.00         | 75.52            | 1218.70           |
| 5.00          | 82.80         | 715.50           | 1238.70           | 60.00         | 36.62         | 62.08            | 1221.10           |
| 5.20          | 81.97         | 661.20           | 1237.90           |               |               |                  |                   |

Table 6.2  
TRANSIENT DATA FOR GCFR DBA DEPRESSURIZATION AND NORMAL  
TRIP WITH 25 IN.<sup>2</sup> LEAK AREA IN INLET PLENUM

| Time<br>(sec) | Power<br>(MW) | Flow<br>(lb/sec) | Pressure<br>(psi) |
|---------------|---------------|------------------|-------------------|
| 0.00          | 826.50        | 1524.00          | 1308.00           |
| 1.00          | 826.50        | 1466.00          | 1275.00           |
| 2.00          | 826.50        | 1455.00          | 1248.00           |
| 3.00          | 826.50        | 1441.00          | 1225.00           |
| 4.00          | 826.50        | 1427.00          | 1203.00           |
| 4.50          | 300.00        | 1381.00          | 1190.00           |
| 5.00          | 117.70        | 1336.00          | 1178.00           |
| 6.00          | 102.20        | 935.00           | 1132.00           |
| 7.00          | 93.56         | 575.00           | 1095.00           |
| 8.00          | 87.52         | 277.00           | 1068.00           |
| 9.00          | 82.80         | 201.00           | 1044.00           |
| 10.00         | 78.95         | 186.00           | 1018.00           |
| 15.00         | 65.24         | 179.00           | 919.00            |
| 20.00         | 57.50         | 185.00           | 839.00            |
| 30.00         | 47.00         | 160.00           | 704.00            |
| 45.00         | 41.00         | 108.00           | 543.00            |
| 60.00         | 36.00         | 71.00            | 425.00            |
| 75.00         | 34.00         | 55.00            | 331.00            |
| 90.00         | 32.00         | 51.00            | 262.00            |
| 120.00        | 30.00         | 50.00            | 171.00            |
| 150.00        | 29.00         | 43.00            | 108.00            |
| 180.00        | 28.00         | 35.00            | 68.00             |
| 240.00        | 25.00         | 30.00            | 31.00             |
| 270.00        | 25.00         | 27.00            | 29.00             |

Table 6.3  
FUEL BUNDLE DATA  
(All Dimensions Are at 70°F)

|                                                 | 19 Rods<br>(Fig. 6.4) | 37 Rods<br>(Fig. 6.5) | 61 Rods<br>(Fig. 6.6) |
|-------------------------------------------------|-----------------------|-----------------------|-----------------------|
| Rod OD, surface roughened, in.                  | 0.294                 | 0.294                 | 0.294                 |
| Rod OD, root, in.                               | 0.282                 | 0.282                 | 0.282                 |
| Rod pitch, in.                                  | 0.386                 | 0.386                 | 0.386                 |
| Minimum rod-to-wall clearance, <sup>a</sup> in. | 0.050                 | 0.050                 | 0.050                 |
| Internal duct flat-to-flat, in.                 | 1.791                 | 2.460                 | 3.128                 |
| Internal duct corner radius, in.                | 0.22                  | 0.22                  | 0.22                  |
| Subchannel type 1 data                          |                       |                       |                       |
| Number                                          | 24                    | 54                    | 96                    |
| Gap, <sup>b</sup> in.                           | 0.104                 | 0.104                 | 0.104                 |
| Wetted perimeter, <sup>a</sup> in.              | 0.462                 | 0.462                 | 0.462                 |
| Heated perimeter, <sup>b</sup> in.              | 0.443                 | 0.443                 | 0.443                 |
| Flow area, <sup>a</sup> in. <sup>2</sup>        | 0.0306                | 0.0306                | 0.0306                |
| Centroidal distance, <sup>b</sup> in.           | 0.111                 | 0.111                 | 0.111                 |
| Subchannel type 2 data                          |                       |                       |                       |
| Number                                          | 12                    | 18                    | 24                    |
| Gap a, <sup>b</sup> in.                         | 0.104                 | 0.104                 | 0.104                 |
| Gap b, <sup>b</sup> in.                         | 0.056                 | 0.056                 | 0.056                 |
| Wetted perimeter, <sup>a</sup> in.              | 0.848                 | 0.848                 | 0.848                 |
| Heated perimeter, <sup>b</sup> in.              | 0.443                 | 0.443                 | 0.443                 |
| Flow area, <sup>a</sup> in. <sup>2</sup>        | 0.0537                | 0.0537                | 0.0537                |
| Centroidal distance a, <sup>b</sup> in.         | 0.193                 | 0.193                 | 0.193                 |
| Centroidal distance b, <sup>b</sup> in.         | 0.143                 | 0.143                 | 0.143                 |
| Subchannel type 3 data                          |                       |                       |                       |
| Number                                          | 6                     | 6                     | 6                     |
| Gap a, <sup>b</sup> in.                         | 0.056                 | 0.056                 | 0.056                 |
| Wetted perimeter, <sup>a</sup> in.              | 0.416                 | 0.416                 | 0.416                 |
| Heated perimeter, <sup>b</sup> in.              | 0.148                 | 0.148                 | 0.148                 |
| Flow area, <sup>a</sup> in. <sup>2</sup>        | 0.0184                | 0.0184                | 0.0184                |
| Centroidal distance a, <sup>b</sup> in.         | 0.090                 | 0.090                 | 0.090                 |

Table 6.3 (continued)

|                                                | 19 Rods<br>(Fig. 6.4) | 37 Rods<br>(Fig. 6.5) | 61 Rods<br>(Fig. 6.6) |
|------------------------------------------------|-----------------------|-----------------------|-----------------------|
| Total wetted perimeter, <sup>a</sup> in.       | 23.76                 | 42.71                 | 67.2                  |
| Total heated perimeter, <sup>b</sup> in.       | 16.833                | 32.78                 | 54.04                 |
| Total flow area, <sup>a</sup> in. <sup>2</sup> | 1.489                 | 2.729                 | 4.337                 |
| Total rod length, in.                          | 88.00                 | 88.00                 | 88.00                 |
| Inlet unheated length, in.                     | 30.85                 | 30.85                 | 30.85                 |
| Heated core length, in.                        | 39.2                  | 39.2                  | 39.2                  |
| Outlet unheated length, in.                    | 17.9                  | 17.9                  | 17.9                  |
| Number of spacers                              | 9                     | 9                     | 9                     |
| Location of spacer 1, in.                      | 0.0                   | 0.0                   | 0.0                   |
| Location of spacer 2, in.                      | 10.0                  | 10.0                  | 10.0                  |
| Location of spacer 3, in.                      | 20.0                  | 20.0                  | 20.0                  |
| Location of spacer 4, in.                      | 30.0                  | 30.0                  | 30.0                  |
| Location of spacer 5, in.                      | 40.0                  | 40.0                  | 40.0                  |
| Location of spacer 6, in.                      | 50.0                  | 50.0                  | 50.0                  |
| Location of spacer 7, in.                      | 60.0                  | 60.0                  | 60.0                  |
| Location of spacer 8, in.                      | 70.0                  | 70.0                  | 70.0                  |
| Location of spacer 9, in.                      | 80.0                  | 80.0                  | 80.0                  |

<sup>a</sup>Based on rod diameter including roughening.

<sup>b</sup>Based on rod root diameter.

node points. The driving functions will be a set of valve positions, circulator speed, heater power, and cooling air flow. The driving functions will be programmed to create the reference GCFR transient condition at the test section inlet.

The dynamic simulation of the CFTL will be done using the System Simulation Language Code (SYSL) for the UNIVAC 1108 computer at GA. The general method of simulation is to formulate equations describing the major loop components. In general, these equations form a system of coupled first-order nonlinear partial differential equations. The equations are then discretized (by finite differences) in the space domain, resulting in coupled first-order nonlinear ordinary differential equations. The SYSL code is used to perform the integration in time from the given initial

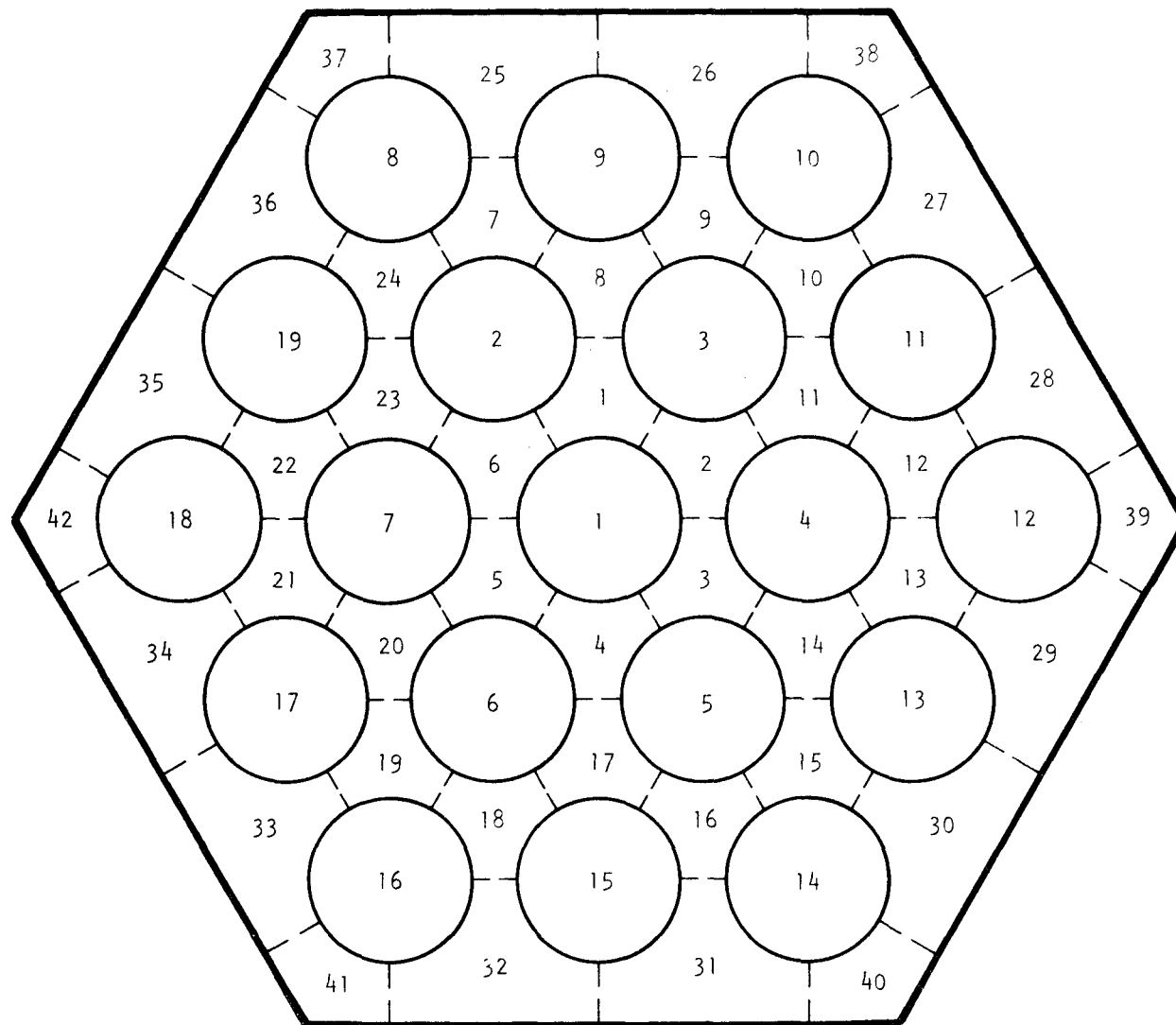


Fig. 6.4 19-rod bundle COBRA model

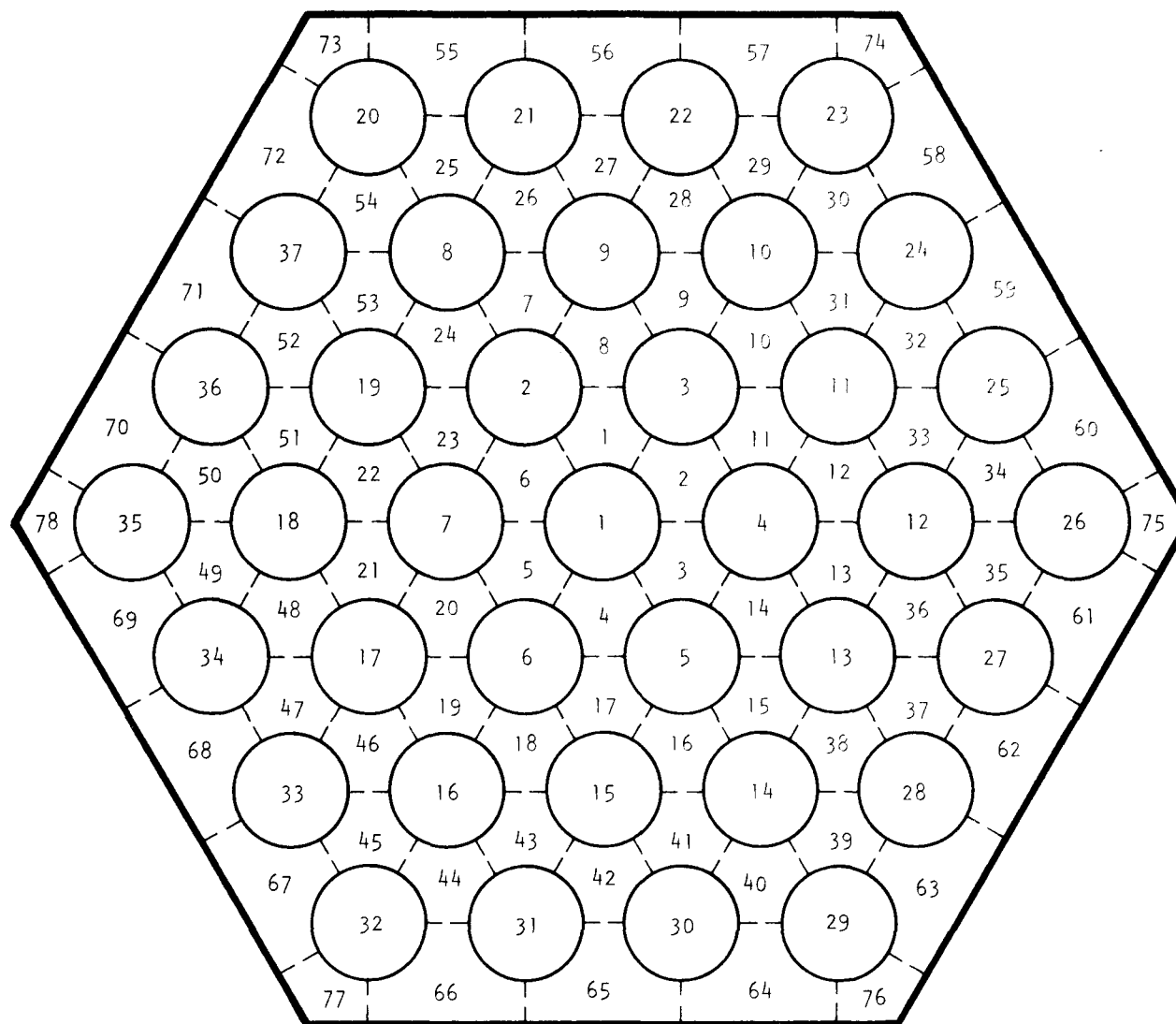


Fig. 6.5 37-rod bundle COBRA model

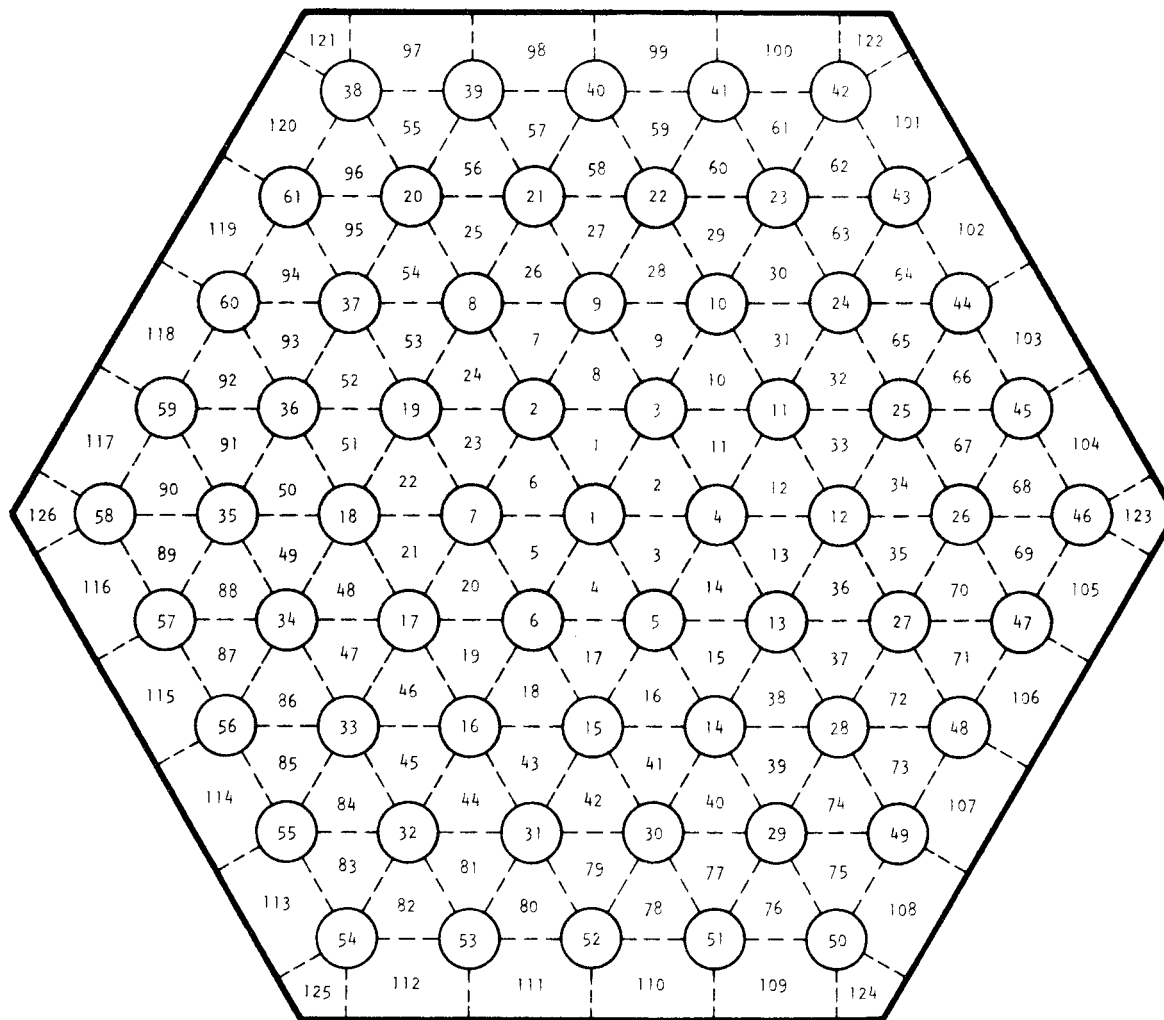


Fig. 6.6 61-rod bundle COBRA model

conditions, namely, the steady-state conditions existing immediately preceding the operational transient.

During the course of the simulation, key parameters, such as test-section conditions and circulator gas inlet temperature, are monitored and controls actuated to keep them within prescribed limits. This monitoring is also used to determine whether the flow is laminar or turbulent and in case of the simulation of the depressurization accident, whether flow reversal occurs. This latter point is particularly important because reversed flow requires a somewhat different computational method. This will be accounted for in the depressurization simulations.

### 6.3. REVIEW OF CFTL DESIGN BY HNL

Near-term activities at HNL are to complete the conceptual design, to prepare a conceptual system design description, and to perform development necessary to resolve anticipated CFTL design problems in the area of helium coolant flow control, heater design, flow and power control, particularly for transient operation, and heater protection devices.

During this quarterly period, the job specification for helium circulators was received for information and comment. The review focused on, "Does the specification cover the requirements of the Core Flow Test Loop Program Plan?" and the conclusion was that the specifications are adequate. The required test operating range is specified in the CFTL Program Plan and these conditions are to be met by a combination of circulator design and test loop design. To judge the adequacy of the circulator specification to meet the required transient performance, it is necessary to generate information on loop dynamics.

A draft of the Conceptual Design Report for the CFTL was also received. The outline specification appears to be satisfactory on a component by component basis. However it was noted that the circulator head requirements are 22,300 ft in the job specification and 29,100 ft in the conceptual design. The test assembly pressure loss requirement has remained the same,  $\Delta P/P = 0.034$  for a pressure range of 1300 to 1500 psia or a 44.0 psi loss at 1305 psia, the GCFR reactor inlet pressure. The job specification stipulates that the balance of loop pressure drop at full flow be no greater than 26.1 psi.

#### 6.4. GCFR PROTOTYPE ASSEMBLIES TEST LOOP

Planning of the test loop for the GCFR prototype assemblies is being initiated to provide for dynamic testing of full-size GCFR assemblies.

Prototype fuel, control, and blanket assemblies will be fabricated according to the preliminary assembly designs and these assemblies will be tested sufficiently to manifest any design deficiencies. Only this loop of the loops planned for GCFR development will have the capability to test full-size assemblies at maximum flow conditions. The results of the testing will be used to aid in the evaluation of the preliminary designs prior to completion of final design. In addition, the loop will be used to qualify the initial production assemblies for the first GCFR core.

The facility must be capable of testing full-size GCFR prototype fuel, control, and blanket assemblies at full pressure and full flow. The fluid will be helium with a controlled range of impurities to simulate GCFR conditions. The nominal test inlet temperature will be based on the GCFR design inlet temperature, with the capability to operate up to the GCFR coolant outlet temperature. The prototype assemblies will be heated or cooled by the helium flow, and at steady state, the test assembly will be at the same temperature as the coolant. The ability to operate over a range of flows is required to simulate steady-state part-load operation. Thermal cycling of each assembly will be accomplished by rapidly varying the coolant inlet temperature. The rate of coolant temperature reduction will be 10°C to 50°C per second to simulate GCFR power reduction and scram. A significant portion of the testing will be endurance testing that may require several thousand hours of testing in either the base-load mode or load-following mode.

Typical operating conditions to simulate full power of the 300-MW(e) GCFR demonstration plant are:

|                                      |                                    |
|--------------------------------------|------------------------------------|
| Fluid . . . . .                      | Helium                             |
| Maximum loop pressure . . . . .      | $9.135 \times 10^6$ Pa (1325 psia) |
| Maximum loop pressure drop . . . . . | $0.55 \times 10^6$ Pa (80 psi)     |

Test section inlet temperature range . . . 320°C - 550°C  
 Maximum test section pressure drop . . .  $0.29 \times 10^6$  Pa (42 psi)  
 Transient temperature range . . . . . 100°C  
 Maximum loop heater power . . . . . 2 MW

|                                 | <u>Fuel</u> | <u>Control</u> | <u>Blanket</u> |
|---------------------------------|-------------|----------------|----------------|
| Maximum flow through assemblies | 6.0 kg/sec  | 5.2 kg/sec     | 1.6 kg/sec     |

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## VII. GAS REACTOR IN-PILE SAFETY TEST LOOP PROGRAM (189a No. 13760)

### 7.1. INTRODUCTION

The Gas Reactor In-pile Safety Test (GRIST) program is being evaluated as a potential follow-on to analytical and experimental programs conducted under the LMFBR and GCFR programs. Special support to GRIST would be provided by in-pile endurance testing of 12-rod GCFR test assemblies in the BR-2 in Mol, Belgium, and by the out-of-pile CFTL tests (see Section VI).

The GRIST program for the GCFR would investigate experimentally the behavior of GCFR fuel assemblies in reactor in a pressurized helium environment simulating GCFR operating conditions. Specifically, the testing would focus on transient conditions of core power and coolant flow and pressure and would investigate the effects of upset, emergency, and faulted conditions on the performance of fuel assemblies for gas-cooled breeder reactors. The test program would provide information that should lead to the reduction of design margins, which is projected to be of importance for improving the performance of commercial GCFR plants. In addition, consideration has been given to utilization of the in-pile loop to investigate fuel behavior in the unlikely event of cladding or fuel melting. Such experiments would assist in obtaining phenomenological understanding of the behavior of molten cladding and fuel and thus would yield information in the area of safety research.

On the GRIST program, GA has the responsibility of (1) defining the requirements for loop design and test procedures, (2) planning the test program, and (3) designing and fabricating the test bundles. Loop design and fabrication is the responsibility of Aerojet Nuclear Company (ANC).

During this quarterly period, that portion of the scoping study to be prepared by GA was completed. In addition, several meetings have been held with personnel from ANC and RRD to review program requirements, test

procedures, test-bundle handling, examination requirements, and potential revisions to the preliminary program plan.

## 7.2. SCOPING STUDY FOR THE GCFR IN-PILE LOOP PROGRAM

The scoping study for the in-pile loop program was performed by Aerojet Nuclear Company and General Atomic<sup>(1,2)</sup> and was submitted to RRD for review and comments.

The GRIST program information that was established during the course of the scoping study was primarily used to develop an overall program schedule and cost estimates and to perform program-cost sensitivity studies involving the test capacity of the loop and the extent of the test program. In summary, it was recommended that conceptual loop design work be continued on the basis of a flow and thermal capacity sized for 37-rod bundles and the in-pile tube should be sized for 61-rod bundles. Regarding the test program, it was found that although the scoping study included the definition of a test series to determine the number of test bundles needed, the work to define the test program should be continued with emphasis on defining more clearly the application of this program to the overall development of the GCFR and safety evaluations.

## 7.3. PRELIMINARY TEST PROGRAM AND TEST PROCEDURES

The basic transients that are being considered to explore design and safety margins are (1) coolant depressurization, (2) overcooling, and (3) flow reduction transients. In addition, a fourth type of transient, i.e., overpower transients, is being given consideration. Even though overpower transients yield similar power-to-flow mismatch conditions as undercooling transients, there is a significant difference between both transients in the area of fuel and cladding mechanical interactions that may warrant experimental verification.

In the area of dynamic failure tests, three major parameters have been identified. These parameters are (1) coolant pressure, (2) rate of approach to melting, and (3) time-dependent effects.

With regard to coolant pressure, two cases may warrant experimental investigation: (1) constant pressure at the normal operating pressure

level and (2) reduced pressure during a depressurization accident simulation. The rate of approach to melting has a significant influence on the temperature distribution through the test bundle, including the blanket regions, and thus may have an important effect on the potential of molten materials to refreeze within the rod bundle. Experimental investigation of time-dependent effects may be used to verify the analytically determined time sequences of initiation and progression of cladding and fuel melting. In addition, mechanical flow obstructions within a test bundle are proposed to be used to verify the absence of the potential for failure propagation during local flow-reduction incidents.

The test program proposed is described in Ref. 2. The program is based on the repeated use of the test bundles, i.e., each test bundle will be exposed to increasingly severe transients for margin exploration and subsequently could be used for dynamic failure tests. Following the margin tests, the bundle conditions will be explored in-pile to the maximum extent possible. This will include a return to full-power operation over an extended period to check whether any damages that may have occurred to the bundle may lead to failure during subsequent full-power operation. Bundle performance during transient and steady-state operation will continuously be checked, primarily by monitoring the cladding temperatures and fission-gas release through the fuel vent system. The information on bundle conditions obtained through in-pile performance tests will be further refined through interim hot-cell examinations, which will focus on rod and spacer distortion and cladding temperature. Following these interim examinations, the test bundles could be reinserted into the test loop to undergo gross dynamic fuel-rod failure tests.

#### 7.4. TRADE-OFF STUDY FOR INTERIM EXAMINATIONS

As stated above, interim examinations are proposed to gain more information on the physical conditions of the test bundle prior to beginning new test phases. The information that can be obtained from in-pile performance tests and from the interim examinations is considered sufficient for interpretation of the effect of transient operating conditions.

There are three factors that have to be considered and weighed against each other to determine the relative value of interim examinations:

1. Using the test bundles for more than one test phase will result in a relatively small number of test bundles needed for a given test program.
2. Interim examinations may require facilities and equipment.
3. Interim examinations will involve considerable handling and transporting of the test bundles, which may result in potential damage to the bundles.

These are discussed in further detail below.

1. Number of Test Bundles

The costs of the test bundles have been calculated for the two test programs proposed in the scoping study by assuming interim examinations of test bundles used for more than one test and destructive examinations of test bundles used only once. The results are shown in Table 7.1. For the smaller test program, which has been recommended, the gross savings due to the multi-use of test bundles has been calculated to be \$5.6 million. These relatively large savings yield a strong incentive to look further into the potential advantages and disadvantages of interim examinations.

2. Facility and Equipment Requirements

Current plans project that interim examinations of GRIST test bundles will be conducted at the new HFEF-North facilities. A test train handling machine and a heavy duty truck, which have been developed and built for other similar programs, are available for the GRIST program and may require only minor modifications.

3. Damage Risk During Handling and Transportation

Each handling and transportation operation has a certain potential for damage to the bundle. This risk is present in both post-test and pre-test activities. It is difficult at this time to assign a dollar value to this risk. However, if a test bundle

is lost because of a damage, the total loss may amount to about \$1 million, including the cost for test reactor operation.

The equipment required to handle the bundles with maximum care will have to be available independent of interim examinations because there will always be post-test handling operations that have to meet the same requirements.

An additional way of reducing the overall risk involved with handling operations would be to eliminate long distances between the test reactor and the examination facility. Because of the large number of expensive equipment available at HFEF-North, post-test examinations should be conducted at this facility. However, interim examinations could be conducted at a new interim examination facility, to be built for GRIST at the ETR facilities. The cost of such a facility has been estimated by ANC to be \$2.6 million.

Table 7.1  
TRADE-OFF STUDY FOR INTERIM EXAMINATIONS OF TEST BUNDLES

| Test Program Procedure                                             | Test-bundle Costs <sup>a</sup><br>(\$ Millions) |                             |
|--------------------------------------------------------------------|-------------------------------------------------|-----------------------------|
|                                                                    | 30 Test Phases <sup>b</sup>                     | 20 Test Phases <sup>c</sup> |
| Multi-use of test bundles with interim examinations                | 4.7                                             | 3.0                         |
| One-time use of test bundles (no interim examinations)             | 12.9                                            | 8.6                         |
| Gross savings with multi-use test bundles and interim examinations | 8.2                                             | 5.6                         |

<sup>a</sup>Test bundle costs include 30% contingency.

<sup>b</sup>Equivalent to the larger test program involving 11 test bundles.

<sup>c</sup>Equivalent to the smaller test program involving 7 test bundles, which has been proposed as an alternative to the larger test program with 11 test bundles.

From these cost estimates, it can be concluded that even with the maximum precautions, i.e., providing an interim examination facility near the test reactor, conducting interim examinations will still yield a benefit of about \$3 million. However, the benefits of an interim examination facility itself result from cost comparisons with potential bundle losses during extended handling and transportation operations for the case that no interim examination facility is made available at the test reactor area. Using the cost estimate given above, it is found that if the complete loss of about three test bundles has to be considered, an interim examination facility becomes relatively attractive. This loss figure at the break-even point appears rather high and indicates that there is an incentive to have all test bundles shipped to HFEF-North for interim examinations.

To summarize, it is found that there is a high incentive to conduct interim examinations of the test bundle rather than to provide separate test bundles for the individual test phases. Furthermore, it is found that the potential for damage to the test bundles during handling and transportation operations appears to be sufficiently low and does not justify construction of a new interim examination facility at the test reactor area.

#### 7.5. TEST-BUNDLE HANDLING AND EXAMINATION

An evaluation of test-bundle handling and examination requirements has been initiated. Preliminary findings, which were forwarded to and discussed with ANC personnel, are to be used for conceptual designs, scheduling, and cost estimates.

Basically, the test bundles have to be shipped from the manufacturer to the test reactor (ETR) and after testing, to the hot-cell facilities (HFEF). The unirradiated bundles can be kept in a protected and not shielded area at ETR until they are inserted in the test reactor for testing. Following completion of a test phase, the test bundles will be removed from the loop and will be shipped to the hot cell facility for interim examinations. If HFEF-North is selected for these examinations, a heavy cask and truck that provide sufficient shielding, shock protection, and a sufficiently clean and dry atmosphere will be required.

Special consideration has to be given to the different atmospheres existing in the loop (helium) and in the hot cell (argon). For the GRIST program, previously proven techniques will be adopted for the change of atmospheres. At the hot cell, the test bundle will be taken through a material lock and will be exposed to the argon atmosphere. Upon removal from the hot cell and reinsertion into the test train transfer cask, the bundle will be exposed again to the helium atmosphere.

The objective of interim examinations is to determine the bundle condition after a period of in-pile testing. To maintain the bundle conditions for further in-pile tests, it is not proposed to remove and replace individual rods. Removal of the flow duct is desirable but is not required. Examination parameters that are being considered are: (1) external appearance, (2) bundle dimensions, (3) internal bundle and rod condition (to the extent possible), and (4) burnup. For these examinations, neutron radiography is highly desired but not required.

For post-test examinations, a large spectrum of destructive examinations is being considered in addition to initial nondestructive tests. These include metallographic, ceramographic, and microprobe analyses, and Kr<sup>85</sup> determination of cladding operating temperatures. If fuel melting and dispersion has been experienced, the region of melting and refreezing as well as the appearance of relocated fuel will be examined.

The evaluation of requirements in the areas of test-bundle handling and examination is being continued.

#### 7.6. TASK FORCE ON GCFR FUEL SAFETY PROGRAM

A task force has been organized at GA to provide a rational basis for the definition of the GCFR fuel safety program. This task force will assist in defining more clearly the application of the GRIST program to the overall GCFR development program and safety evaluations. Specifically, the task force is expected to provide information that may have an effect on further considerations regarding the definition of a test program and the need for destructive examinations subsequent to some or all of the margin exploration tests. Furthermore, the task force findings and conclusions may bear on the recommendation regarding the need for preirradiated

rods within some of the test bundles to yield more realistic simulations of end-of-life rod and bundle conditions.

Three objectives have been defined for the task force: (1) to define phenomenologically the damage limit and safety limit for GCFR fuel; (2) to prepare event tree sequence diagrams leading from generalized initiating events to final conditions representative of stable, long-term situations; (3) to identify the phenomenological areas in which additional information is required in order to define what the stable, long-term situation will be; and (4) to identify the analytical and experimental work that will yield the information required, to conceptualize the experimental programs and the facilities required, and to explore some of the tradeoffs if the experimental programs are not conducted.

The current status is that a set of definitions have been proposed for the core element operational and damage limits and their criteria. The limits have been defined for normal, upset, emergency, and faulted conditions. Regarding the second objective of the task force, the transients to be evaluated have been defined and the event tree sequence diagrams are being prepared.

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## VIII. REACTOR ENGINEERING (189a No. 13823)

Under this task, analytical methods and models applicable to the assessment of thermal, hydraulic, and structural performance of the GCFR core and core support will be evaluated and developed, the development required for reactor control-rod, shutdown-rod, and core component locking mechanisms will be conducted, and methods and materials behavior models and information will be evaluated to assess the capability of the PCRVR internal structures to serve as postaccident fuel containment.

### 8.1. CORE THERMAL-HYDRAULICS

The thermal-hydraulic performance of the core assembly and the core monitoring-instrumentation requirements and methods are being evaluated and accurate computer models for these analyses are to be defined.

The overall core and blanket performance interfaces were defined in the previous quarterly report.<sup>(1)</sup> In addition, the approach to the more limited problem of determining the orifice management program was described. The work on orifice management was continued during this quarterly period.

The restraints, goals, and variables affecting the allocation of coolant to the core and blanket elements have been identified. The restraints are:

1. Adequate coolant flow must be provided such that the peak mid-wall cladding temperature in the limiting rod of each element does not exceed the design limit. The current design limit is a mid-wall cladding temperature of 700°C.
2. The coolant flow requirements (pressure drop and total flow) must be within the design limits of the primary coolant circuit.
3. A single orifice specification at the beginning of a burnup cycle must incorporate the above requirements and include an allowance

for uncertainties and deviations from the anticipated operating history for individual elements.

Within these restraints, the goal of the orifice, or coolant, management program is to maximize the performance of the reactor system. Specifically, this means maximizing the mixed-mean coolant outlet temperature over any given burnup cycle. Alternatively, this goal may be viewed as the equivalent goal of minimizing the total coolant flow rate or maximizing the useful expenditure of core pressure drop. A secondary goal will be to keep to a minimum the number of mechanical operations required to implement an orifice strategy and thus minimize the associated reactor downtime.

Variables that are available to satisfy the above restraints and goals include (1) specification of reload fuel enrichments, (2) element relocation, (3) element orientation, and (4) orifice-plate hole size.

Two parallel computer programs applicable to the coolant orificing evaluation have been under development during this quarterly period. For the production program, which will be the primary output of this development effort, conceptualization and formalization of a computer model capable of calculating helium-coolant flow rates in both the fuel and blanket elements has been accomplished. The input required from physics analyses for this code is the axial power distribution in the peak rod of each element. Characteristics of the computer code include variable dimensioning, incorporation of the peak rod cooling requirements and midwall cladding temperature limits, and use of pertinent state-of-the-art material behavior and heat-transfer correlations. The removal of conceptual errors and the verification of the model, using sample problems for which known solutions exist, has been initiated. Additional methods development that will be incorporated into the above model and will support the formulation of an overall coolant management scheme has begun.

A second, simplified, computer code has been developed to aid in formulation of the basic orificing strategy. It interfaces with currently available two-dimensional (hexagonal) burnup analyses for the GCFR demonstration plant core and blanket configurations. A simplified analysis is employed to account for the overall axial power profile, flux depressions in the

vicinity of inserted control rods, radial flux gradients across fuel assemblies, fissile buildup in blanket regions, and overall uncertainties in the calculated power distribution. Hand calculations have been performed to verify the operation of this code. Calculations of sample cores have revealed the following burnup characteristics that influence the approach to developing an optimum orificing strategy:

1. During a burnup cycle, increases in the fraction of power generated in the blanket regions lead to a characteristic reduction in the end-of-cycle minimum coolant flow requirements for each of the core elements. This result has led to the consideration of evaluating circulator control in the overall coolant management program.
2. The beginning-of-cycle to end-of-cycle change in the power generation was shown, as expected, to be significant for those elements close to the outer edge of the core. This characteristic may allow a position-dependent cladding limit to be incorporated (in the future) into a refined computer code for coolant distribution evaluations.
3. Radial power gradients across the core assemblies were found to be essentially constant over the burnup cycle. The importance of properly accounting for these radial gradients in specifying the fissile enrichments for reload fuel was demonstrated. These gradients will also result in significant variations in the helium-coolant outlet temperature from different core elements.
4. The probability of employing only a few orifice sizes for flow control in the core elements appears likely.

## 8.2. CONTROL-ROD, SHUTDOWN-ROD, AND ELEMENT-LOCKING MECHANISMS

Activities under this subtask include (1) developing element-locking mechanisms and the control-rod and shutdown-rod mechanisms and (2) demonstrating the performance and reliability of these components by testing under anticipated operating conditions. The current work, however, is limited to an evaluation of alternative systems for reactor shutdown.

Previous work under this subtask identified a number of alternative shutdown system concepts.

During this quarterly period, a topical report entitled "Evaluation of Alternative Shutdown System Concepts for the Gas-Cooled Fast Breeder Reactor" was written. In this evaluation, fifteen shutdown system concepts were examined for potential application to the GCFR. Of these, systems employing  $B_4C$  balls dropped by gravity into defined core positions and retrieved mechanically or pneumatically appeared most attractive. Further evaluations of these ball systems will consider such aspects as potential reactivity worth, testability of each system, provisions for position indication, their ability to operate in a distorted channel, additional integral mechanical features such as energy-absorbing devices, and component behavior under the anticipated life cycle, including potential bridging and agglomeration of the balls.

A final comparison with the GCFR reference design shutdown-rod system will be made on the basis of diversity, but also with consideration of the development aspects of a particular comprehensive design.

### 8.3. CORE SUPPORT AND STRUCTURE

The availability of adequate analytical methods for evaluating the static and dynamic structural response of the core support structure is being determined under this subtask.

Previous work in this area has included a review of the available computer programs and models for seismic analysis of the core support structures and the static analysis of the grid plate. The current work has extended the effort in each of these areas.

#### 8.3.1. Seismic Analyses

8.3.1.1. Current Status. The original seismic analysis of the core and core support structure correctly accounted for the mass and translational stiffness of the structural assembly but it neglected the rotational inertia effects of the grid plate and the cantilevered core elements. In order to evaluate the significance of the rotational inertia, a simplified analysis was performed wherein the structure was approximated by a representative diametral beam section with several cantilevered

attachments to simulate core elements. This simplified analysis has shown a significant reduction of the equivalent beam frequency. Consequently, this result indicated the need for a more elaborate technique to treat the system of grid plate and core elements. The present mathematical model that is being employed in the seismic analysis program SAP-IV<sup>(2)</sup> treats the 265 fuel and blanket elements as a single beam, in which all of the masses and stiffnesses of these elements are lumped into a single unit. Consequently, the computed stresses may represent an average value of the true physical condition of the individual fuel and blanket elements.

8.3.1.2. Vibration Theory of the Grid Plate. Because of the possible importance of the rotational effect of the grid plate and core on the seismic analysis of the core and support structure, the lowest frequencies of the grid plate were reviewed.

First, a brief numerical computation was carried out to study the mass and rotary inertia effects on a beam, which is cut out from a perforated plate. Equivalent loading conditions were used on the beam.<sup>(3)</sup> This computation has shown that the rotary inertia effect is as important as the effects of the mass of the fuel and blanket elements on the vibration of this beam. The rotary inertia of the fuel elements tends to reduce the lowest frequency of the equivalent beam by a factor of two. Therefore, this problem had to be further investigated by applying plate theory.

The dynamic theory of the grid plate was developed by generalizing the thick-plate theory of Reissner-Mindlin<sup>(4,5)</sup> with the orthotropic constants and by modifying the formulations of the rotary inertia expressions to include the rotary inertia effects of the core elements. In this general case, the differential equations of the problem can be solved by numerical techniques. However, by applying the effective elastic constants of the perforated plate in the customary fashion, the solutions were determined analytically in terms of Bessel functions.

Since the grid plate has an outer solid ring around its periphery, the effect of the ring on the vibration of the plate must also be taken into account. This effect was determined by applying the proper boundary conditions at the interface of the plate and the ring for the deflections,

slopes, bending moments, and shear forces. Their mathematical expressions were satisfied by equating them at the interface. Thus, a completely general solution was obtained for the grid plate. No restrictions were needed to be imposed either on the perforated plate or on the outer boundary of the ring.

Typical core data of the GCFR were used for the numerical computation for the model shown in Fig. 8.1. The calculation has shown that the rotary inertia of the core elements can reduce the lowest natural frequency of the perforated plate significantly.

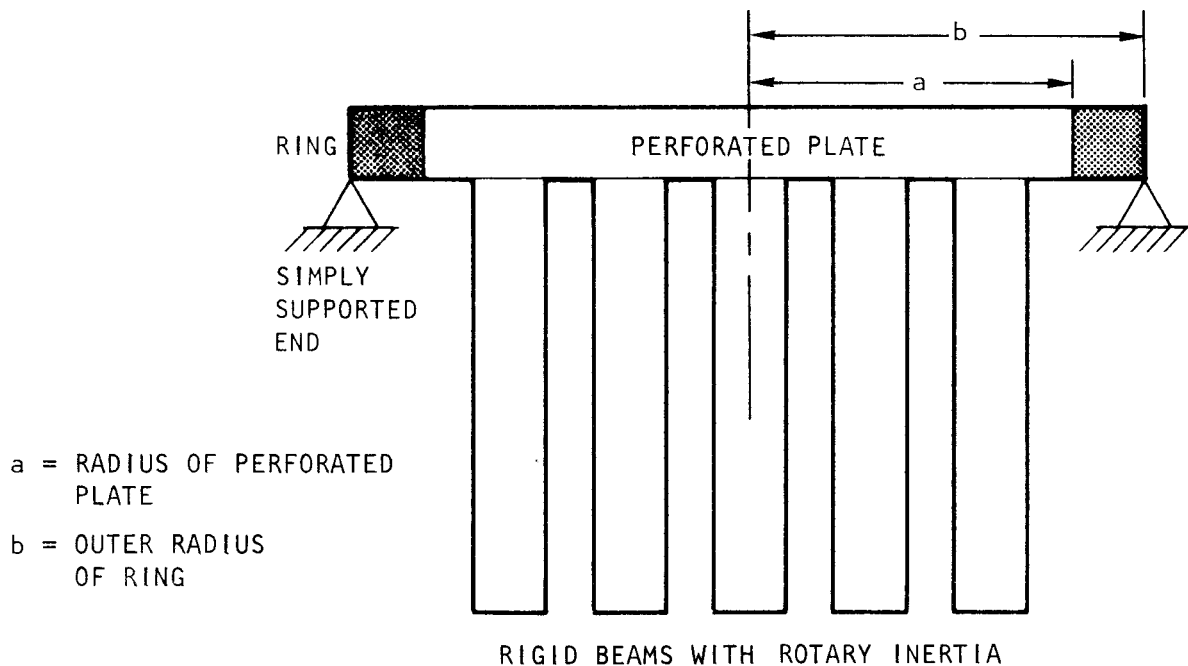


Fig. 8.1 Perforated plate with solid ring

Because the frequencies of the grid plate and those of the fuel elements may be in the same range, both must be included in the seismic analysis. The inclusion of the "elastic" grid plate in the present spring-mass seismic model would probably increase the amplitudes of the fuel elements significantly, which would result in unnecessary overdesign of the core structure. Therefore, a simple spring-mass model is inadequate.

Instead, a more elaborate finite-element seismic model based on plate and shell elements will be required.

The technique of modal synthesis will then be applied to determine the effects of dynamic coupling and to study the seismically induced motion of core elements in a realistic manner.<sup>(6)</sup> In this mathematical model, the fuel and blanket elements will be represented on an individual basis according to their different stiffness properties and damping factors. Using this concept, the displacement of the individual fuel or blanket elements can be studied and the relative amplitudes and the possible impacts of each can be determined. It is anticipated that this more elaborate method of analysis, even with the inclusion of the dynamic properties of the grid plate, will show smaller amplitudes, stresses, and reactivity than the present spring-mass seismic model. One of the reasons for this prediction is that the new analysis will not make use of as many conservative assumptions as the previous analysis.

An attempt will be made to utilize the SAP-IV program to its fullest extent. If this effort does not prove to be satisfactory, then new sub-routines will be developed and included in the SAP-IV code.

A section in which the above-mentioned analyses are discussed has been prepared for inclusion in a topical report on the seismic response of the core and core support structure.

#### 8.3.2. Core-support Structural Analysis

The review and evaluation of stress analysis methods for perforated plates in the existing literature have continued. The study will provide a sound choice of the stress analysis method for the GCFR grid-plate design based on the current state of art.

The problem of determining stresses and displacements in perforated-plate structures has been extensively investigated since Gardner introduced the equivalent solid-plate concept.<sup>(7-9)</sup> The fundamental assumption in this work was that the deflections in the perforated plate and the equivalent solid plate, under the same loading conditions, could be matched by reducing the flexural rigidity of the solid plate. Later, Horvay<sup>(10-12)</sup> and Malkin<sup>(13)</sup> independently proposed the use of fictitious elastic con-

stants, called effective elastic constants, for the equivalent solid plate. As a result, the perforated-plate material is treated as a solid material with the elastic constants replaced by the effective elastic constants. These constants are determined from the requirement that gross deformations in the perforated plate and the equivalent solid plate be the same under the same loading conditions. Subsequently, theoretical and experimental development of the equivalent solid-plate concept led O'Donnell and Lauger<sup>(14)</sup> to formulate a more advanced approach to the perforated-plate design, which has formed the basis of the well-known design code in the boiler and pressure vessel industry.<sup>(15)</sup>

The analysis methods by Horvay and Malkin were based on the approximation of the perforated plate to the honeycomb structure in which a strength-of-materials analysis can be applied. As new applications for perforated plates are found, e.g., nuclear reactor grid plates, more accurate analyses are needed to ensure the reliability and integrity of the structural performance. Moreover, the availability of high-speed computers facilitates the numerical solution of complicated mathematical boundary-value problems. Consequently, in recent years, study of the structural behavior of perforated plates has been concentrated on a numerical approach based on the elasticity theory. For instance, Slot and O'Donnell<sup>(16)</sup> formulated the effective elastic constants for thick and thin perforated plates in the generalized plane strain and plane stress, respectively. They provided the effective elastic constants of thin and thick plates with triangular and square penetration patterns for a broad range of Poisson's ratios and ligament efficiencies (defined as the ratio of the minimum web width between two adjacent holes and the distance between these holes).

A parallel effort has also been made to review and evaluate the computer codes that can be used for the core-support structural analysis. Two in-house computer programs, STAC and TEPC, were chosen to serve this purpose. The STAC code, which is a combination of a thermal analysis code and a shell analysis code, will be used for the thermal stress analysis. The TEPC code will cover the analysis of elastic, plastic, and irradiation-induced swelling deformations.

#### 8.4. POSTACCIDENT FUEL CONTAINMENT

The objectives of this subtask are (1) to develop an accurate description of the thermal and structural processes accompanying the events in a postulated core meltdown arising from a very low probability series of failures and (2) to assist in defining experimental studies to verify thermal processes of importance in Item (1).

In the previous quarterly report,<sup>(1)</sup> several areas contributing to uncertainties in the postaccident fuel containment (PAFC) analysis were discussed and the results of a preliminary analytical study, which emphasized heat removal via the cavity liner cooling system (downward heat removal), were presented.

During the current quarterly period, work has continued on the uncertainty evaluations for models of the thermal processes that would accompany core meltdown. The preliminary analytical study has included a comprehensive analysis of upward decay-heat removal (i.e., radiation, conduction, and convection to the structures and gas above the molten fuel) that was found to be important for larger size GCFRs. In addition, a PAFC development plan has recently been initiated. Planning documents are currently being reviewed and an outline of the development plan is being written.

##### 8.4.1. Uncertainty Evaluations

8.4.1.1. Decay Heat. The RIBD code, developed at HEDL for the LMFBR program to compute isotope-by-isotope decay heats, has been received and made operational on the GA computer for the purpose of computing time-dependent decay heat rates in the GCFR. The computation of decay heat rates under different core meltdown conditions will be under way in the near future.

8.4.1.2. Recriticality. Criticality calculations made at GA have shown that the molten fuel in a layer of uniform thickness is inherently subcritical. However, in the initial meltdown period, the dropped core masses may form a pile in the center portion of the preshield before they spread out radially. The progression of events during a postulated core meltdown and the resulting spatial distribution of the molten fuel masses will be studied further.

8.4.1.3. Natural Convection. Recent publications have indicated that an intensive study has been made of the problem of thermally induced natural convection in layers of molten material.<sup>(17,18)</sup> However, an improved natural convection correlation in the higher Rayleigh number range, characteristic of the PAFC problem, is still not available.

8.4.1.4. Chemical Reactions. Chemical reactions between the core materials and the lower shield materials were discussed in the last quarterly report. It may also be necessary to consider some other chemical reactions that could accompany a core meltdown if additional equipment failures are postulated to occur concurrently.

#### 8.4.2. Preliminary Analytical Study

A detailed study of the upward decay-heat removal using the NUTAP computer code has been planned. A cylindrical two-dimensional transient model has been established for numerical calculations. Heat transfer by radiation, conduction, and convection in the region above the molten fuel is considered.

#### 8.4.3. PAFC Development Program Plan

Based on uncertainty evaluations, the following tasks have been proposed to be carried out in the PAFC Development Program Plan:

1. Preliminary Analytical Study - Parametric studies using existing computer codes, simplified models, and variable materials property values.
2. Computer Code Development - Establishment of a multidimensional heat-transfer code capable of solving PAFC problems with more realistic models.
3. Initial Conditions - Selection of a range of values for decay-heat rate and distribution, fission-product inventory, core-debris spatial distribution, and assurance of subcriticality as a basis for analytical and experimental evaluations.
4. Physical Properties Evaluations - Extensive literature survey for dependable property values and experimental investigations for the most uncertain properties.

5. Molten-pool Heat Transfer - Experimental study to find the best correlation for thermally induced natural convection and to observe the real mechanism of crust forming and cracking.
6. Physical Movements - Experimental and/or analytical study to evaluate the influence of geometric changes from ideal geometry, such as possible flotation of graphite blocks in the lower axial shield during the process of melting progression, for example.
7. Chemical Reactions Accompanying Core Meltdown Conditions - Experimental study to investigate chemical reactions between the various material constituents during the process of melting progression.

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## IX. PRESSURE-BOUNDARY INTEGRITY (189a No. 13824)

The long-range objective of this task is to verify the adequacy of the design of the prestressed concrete reactor vessel (PCRv) pressure-boundary system. The work will include preparation of plans for initiating critical development tests for the pressure barrier. The design basis for the PCRv will be critically reviewed with the objective of ensuring pressure-boundary integrity over the life of the plant for the GCFR operating conditions. This work will include an evaluation of the basis for radiation exposure criteria and key safety-related aspects, such as the effect of a liner crack.

### 9.1. DESIGN BASIS FOR THE INTEGRITY OF THE PCRv PRESSURE BOUNDARY

The pressure boundary being considered is that which contains the reactor helium coolant. The boundary includes not only the PCRv with its associated liners and closures but also the pressure-containing areas of the steam generators and helium circulators and the core auxiliary cooling system. Those sections of the pressure boundary and closures that use concrete as a structural material are designed to the ASME Boiler and Pressure Vessel Code, Section III, Division 2. The other parts of the pressure boundary are designed to Division 1 of the ASME Code. These pressure-containing areas will be described and evaluated as part of the task.

Prior work on the evaluation of the PCRv pressure-boundary integrity has included the design and analysis of PCRv designs for gas-cooled reactors in Europe and the U.S. that have demonstrated the integrity of this type of pressure vessel. More recently, HTGR design and development has added to this background of technology. The GCFR design involves higher primary coolant gas pressures than current HTGR designs, and this has led to investigating and evaluating the effect of the higher pressure on the pressure boundary integrity, including the PCRv closures.

Although the GCFR design has a higher helium pressure than previously constructed HTGRs, the design criteria governing reactor vessels, including concrete and steel stresses, tendon and wire-winding loadings, and design margins, are the same as for HTGRs. Thus, the stresses resulting from the higher pressure can be limited as part of the design.

A topical report on the evaluation of pressure-boundary integrity is being prepared. Drafts of the following sections of the topical report have been completed: the description of the pressure boundary, a discussion of the performance requirements, and an evaluation of the PCRV liners and anchors and the flow restrictors. The prestressing system, the helium circulator, and the structural analysis and behavior of GCFR PCRV will be treated in other sections of the report. In the structural analysis, applicable design codes, methods of analysis, and modeling testing used for verifying analytical procedures will be discussed.

## X. REACTOR SAFETY (189a No. 13825)

The purpose of this task is to study the reactor safety aspects of the GCFR by employing logical probabilistic methods to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications that would help reduce risks. This task also includes liaison between GA and the RRD-funded GCFR Safety Task at Argonne National Laboratory.

### 10.1. ACCIDENT INITIATION AND PROGRESSION ANALYSIS (AIPA)

Applying the AIPA analysis techniques developed in FY-74, work is being directed toward the probabilistic analysis of potential accident sequences leading to low-probability, high-consequence sequences of events, also under study at ANL on GCFR Safety Aspects of Fuel and Core.

Prior work in this area has been directed toward the evaluation of loss-of-reactor-coolant-flow accidents. A probabilistic model of the GCFR shutdown cooling system was developed and computerized utilizing the GO code technique.<sup>(1)</sup> The effects of various accident initiators are being analyzed using this model to establish an initial set of the more dominant (i.e., more probable) accident sequences involving loss of coolant flow.

During this quarterly period, efforts were directed toward determining what factors may most significantly affect the ultimate public consequence of a core fission-product release resulting from the low-probability accidents under study. The aim of these scoping studies is to provide initial information to more optimally structure the selection of higher-risk accidents for the GCFR. The light-water reactor (LWR) risk assessment<sup>(2)</sup> has shown that containment mitigating factors, such as the success or failure of operation of cleanup and heat-removal systems and the mode and time of containment failure, cause a large spread in the spectrum of public consequences that would result from core melt events and thus may significantly affect the risk imposed by any particular accident sequence in an

LWR. Initial studies have shown that the same may be true of the accidents under study for the GCFR, and therefore understanding of these factors is essential to the process of a public risk assessment.

Program technical review meetings were held at GA during this period with members of the Reactor Safety Division, RRD. Topics discussed were the status of present work, the direction of future work, and the potential impact of the WASH-1400 results on program assumptions.

#### 10.2. SAFETY RESEARCH AND DEVELOPMENT LIAISON

Liaison with ANL was continued during the period, although it has been limited in large part to maintaining cognizance of ANL progress under the RRD-funded task on GCFR Safety Aspects of Fuel and Core.

A list of questions regarding the ground rules used to evaluate large GCFR plant characteristics was received from ANL early in the reporting period. Some of the requested information was contained in information submitted to ANL late in the previous quarterly period. The remaining aspects were orally discussed to a satisfactory extent with the cognizant ANL principal investigator, and it was mutually agreed that a written response from GA will not be required.

#### REFERENCES

1. "GCR Accident Initiation and Progression Analysis - Progress Report for the Period February 1 through June 30, 1974," USAEC, Report GA-A13094, General Atomic Company, October 8, 1974.
2. "Reactor Safety Study - An Assessment of Accident Risks in the U.S. Commercial Nuclear Power Plants," USAEC, Report WASH-1400, August 1974 (Draft).

## XI. REFUELING SYSTEM DEVELOPMENT (189a No. 13831)

### 11.1. REFUELING SYSTEM DEVELOPMENT PLAN

A comprehensive development program plan that will cover all the essential aspects for the development of the GCFR refueling system and its components is to be prepared under this subtask. The plan will consider system and task descriptions as well as functional evaluations and feasibility studies of candidate designs prepared during the conceptual and preliminary design phases. This development plan will supplement the overall GCFR demonstration plant development program plan.<sup>(1)</sup>

An initial result of the step-by-step refueling sequence, which was started in the last quarterly period, was an attempt to reduce the complexity and quantity of equipment items involved in the reference design. For example, the reference sequence indicated many intermediate steps were required during preparation for refueling alone. This is mainly due to the numerous casks and containers housing the various machine components that must be transported between the reactor service building and the PCRV bottom-head penetrations in the reactor containment building. A reduction in the quantity of equipment will reduce the number of operations and thereby simplify the task and significantly reduce the refueling downtime. This sequence preparation effort will continue to enable development of conceptual design schemes for further study.

Effort was started during the quarter on detailed task definitions and logic diagrams for the sequence of development of the overall fuel handling system and its components. Those started during the quarter pertain to the fuel transfer machine and the fuel lifting machine. These diagrams will be utilized in the preparation of detailed schedules, which will be part of the refueling development plan and used as inputs to the overall PERT-type schedule for monitoring progress.

## 11.2. COOLING AND SHIELDING SUBSYSTEMS EVALUATION

The objective of this evaluation is to determine the maximum cooling and shielding requirements for the GCFR refueling system. This objective is being achieved through analysis and feasibility studies and will include alternative methods to meet the requirements and help determine the selection of a reference system.

The work on cooling systems was initiated during the previous quarter for the fuel transfer and fuel lifting machines. Three schemes were identified for further evaluation and analysis; the first to be investigated will employ a small helium circulator or fan.

These three initial schemes plus three additional ones were studied and evaluated in more detail.

### 1. Helium Fans

This system would utilize helium within the lower reactor plenum and could function in one of two possible configurations. In the first configuration, the fan would "push" helium through the element. In the second, the fan would "pull" helium through the fuel element.

### 2. Ejectors

An ejector system would utilize an external high-pressure helium source driving an ejector, which in one method would "push" or in a second method "pull" helium from the PCRV lower plenum through the fuel element.

The first method would involve supplying helium at some determined pressure to the high-pressure inlet of the ejector. Helium circulating within the plenum would be induced into the low-pressure ejector inlet. The total helium mixture would leave the ejector and be "pushed" through the fuel element.

The second method parallels the first in principle, but in this case the ejector would function as a suction pump. The low-pressure inlet of the ejector would be coupled directly to the fuel element and helium from the PCRV lower plenum would be "pulled" through the fuel element.

3. Direct Cooling with External Helium Source

A third system would utilize an external high-pressure helium source connected to a nozzle located at the top or bottom of an element and "push" helium through the fuel element.

4. Direct Cooling with Helium from Fuel Transfer Cask

This system is only applicable when the fuel element is being lowered or lifted through the refueling penetration in the PCR. The system would utilize an external high-pressure helium source connected to the bottom of the fuel transfer cask. The helium flow would travel through the cask, up into the penetration, around and through the fuel element, and then into the lower reactor plenum.

5. Direct Cooling with Loop Helium

This system is only applicable when the fuel element is being moved laterally within the PCR lower plenum.

After a fuel element is removed from the grid-plate and lowered into the lower plenum, a "funnel" attachment is placed at the top end of the element. As the fuel element is moved laterally to the refueling penetration, the "funnel" captures helium leaving the grid-plate attached elements and directs this flow through the fuel element being removed.

All of these cooling systems were studied to determine the technical feasibility of each system. Since helium fans tend to be very large for the flow needed and motor reliability could be questionable, this cooling system is being abandoned at the present time. In comparison, the other cooling systems offer distinct advantages in mechanical simplicity. An ejector system would be preferred over direct cooling since the high-pressure helium mass flow would be smaller for an ejector and this would reduce the diameter of the high-pressure supply lines. Storage and handling of the helium supply lines within the PCR lower plenum is a problem common to both cooling systems, but smaller diameter lines would reduce the complexity of this problem.

In summary, the cooling systems that are currently under consideration are: ejectors, direct cooling originating from the fuel transfer cask, and direct cooling with loop helium.

Pending results from more comprehensive fuel-element cooling analysis relative to GCFR refueling operations, these cooling systems will be studied further and refinements will be made to assure reliable fuel-element cooling during in-vessel fuel handling.

#### REFERENCE

1. "Gas Cooled Fast Breeder Reactor Demonstration Plant Development Plan," Vols. I, II, and III, USAEC, Report Gulf-GA-A10788, Gulf General Atomic, September 20, 1972.

## XII. COMPONENT DEVELOPMENT ENGINEERING (189a No. 13840)

Currently, this task includes (1) completion of the development program plans for the main-loop components of the GCFR, (2) evaluation of existing HTGR standards against GCFR design criteria to determine additional standards requirements, and (3) analysis of critical components. Work to date has been on the first task, which is nearing completion.

### 12.1. DEVELOPMENT PROGRAM PLANS

This subtask includes revision and completion of the development program plans for selected main-loop components of the GCFR—namely, the steam generator, the main circulator, and the PCR.V.

#### 12.1.1. Steam-generator Development Program Plan

During this reporting period, the draft of the development plan, which includes a detailed schedule, was reviewed prior to final approval and publication. The development tasks, which include boiling stability, helium flow distribution, flow-induced vibration, and tube-wear protection were expanded and incorporated into the overall schedule for design and fabrication. A section on material evaluation has been added, which includes consideration of candidate materials corrosion and mechanical properties as well as their permeability to tritium.

#### 12.1.2. Circulator Development Program Plan

The revised circulator development program plan has been issued.<sup>(2)</sup> The general content of the original GCFR development plan<sup>(1)</sup> relating to the circulator was completely reviewed and the majority of the tasks included in the original development plan were retained, but with changes in scope. In addition, certain other tasks have been added, including a materials evaluation program and critical acoustic and vibration test programs. The revised program plan includes comprehensive bearing and seal testing to verify experimentally the operating performance of the circulator

water-bearing system, the water-bearing seals, the helium-water buffer seal, the steam-water seal, and the drain cavity system. This test will utilize a complete rotor, except for turbine and compressor blading, that will be installed in a housing complete with shaft seals. The shaft power drive and test system will be designed so that it will be possible to reproduce normal and reverse thrust on the circulator thrust bearings to simulate the normal and accident loading conditions on the bearings. In addition to the bearing and seal assembly, the facility will include a full-pressure service system and its control. This system will duplicate those component parts that provide the bearing water, helium, and other services so as to reproduce as closely as possible all conditions that could be applied to the circulator, the bearings, and the seal systems. The test rig will provide data prior to commitment to fabrication of the prototype complete circulator unit.

The development program includes a major prototype circulator qualification test program that is essentially the same as proposed in the original development plan, including an extensive series of operating tests, performance checks, and acoustic and vibration tests. The test will also include demonstrating circulator operation during a simulated primary loop depressurization accident. Test facility requirements for this phase of the program are under study and several alternative conceptual approaches for providing a steam-generating service and control system for the test have been identified.

One alternative, a full-scale, full-load test facility integrated with a large electric power plant, offers an attractive solution since there are a number of newer large-capacity plants that are capable of providing the high steam flow and pressure required for the full-power testing of the GCFR circulator. However, the availability of steam power from an existing electric power plant remains to be determined and may well limit the extent of the test program and its schedule. Consequently, utility acceptance of this type of experimental facility is uncertain at this time and further evaluation is required.

The leading alternative is a part-load test facility similar to that

to be used for the large HTGR circulator test program. The HTGR test facility will utilize an electric-driven steam compressor for supplying high-pressure steam to the circulator turbine. The steam compressor will replace the steam boiler. The pressure and temperature conditions of the steam are to be obtained by conversion of compressor power loss to heat as the steam is circulated in a closed loop between the steam compressor, circulator turbine, and steam cooler. Steam, helium, and power requirements are sufficiently different that the HTGR test facility cannot be used, as presently planned, for GCFR testing. Accordingly, a similar but new facility would be required.

Also included in the report is a detailed schedule outlining the development program interfaces with the circulator design, manufacture, testing, and installation in the demonstration plant.

#### 12.1.3. PCRVR Development Program Plan

The development plan for the prestressed concrete reactor vessel (PCRVR) was distributed for final internal approval prior to its being issued. The development program plan has been strengthened by providing for additional testing for PCRVR closures and the thermal barrier. Prior to the testing of the large scale model closures for the reactor and steam generator cavities (1/4 scale), the plan now includes preliminary tests on smaller scale (~1/15 scale) closure models. The thermal barrier development program now includes a test to confirm the thermal performance of the thermal barrier in the hot gas inlet duct from the reactor cavity to the steam generator. The development program schedule has been revised to incorporate the change of scope and displays the interfaces between the development task and the overall design and construction schedule for GCFR demonstration plant.

A meeting was held in December 1974 between HNL and GA to discuss the HNL GCFR PCRVR Closure Development Program, at which the status of the program was reviewed in the context of the reduced funding and revised scope and schedule. Decisions were reached on the program scope to include the development of a test model and test procedure for the closures for the GCFR steam generator and reactor cavities. The objectives of the closure studies are to demonstrate the failure mode of the closure and to use

the resulting data to evaluate the associated analytical studies. GA continued the design effort on the steam generator closure and forwarded to ORNL additional details on the hold-down system and the location of reactions.

At a meeting at HNL in January 1975 in connection with the PCRV closure development program, GA reviewed the results of the HNL stress analysis of the proposal model for the steam generator closure. The results indicated that the stress levels developed in the closure under normal operating conditions using the primary hold-down system were acceptable. The effect on the stress levels of using the backup hold-down system, assuming failure of the primary hold-down system, is yet to be determined. The design of this backup system is currently in work.

A schedule for the interchange of design and test information was reviewed and agreed upon. It included the completion of the design of the steam-generator closure model for testing later this year.

There was general agreement on the design analysis and testing procedures presented by HNL and tentative agreement was reached in which the scope of certain sections of the ERDA-sponsored GA work on pressure boundary integrity would be revised to include work complementary to the HNL closure development program.

#### REFERENCES

1. "Gas-Cooled Fast Breeder Reactor Demonstration Plant Development Program Plan," Vol. II "Problem Identification and Work Programs," USAEC, Report Gulf-GA-A17088, Gulf General Atomic, September 20, 1972.
2. Mouritzen, G., "Development Plan for the GCFR Main Helium Circulators," USAEC, Report GA-A13207, General Atomic Company, December 6, 1974 (Controlled Planning Document with Limited Distribution).

### XIII. SHIELDING REQUIREMENTS (189a No. 13841)

The purpose of the shielding task is to verify the adequacy of the methods and data (physics and engineering) for the design of GCFR shields and to evaluate the effectiveness of various shield configurations. In addition, this task will coordinate and provide liaison with the analytical and experimental GCFR shielding activities at HNL.

In the last quarterly period, the results of preliminary one-dimensional physics analyses indicated that a substantial reduction in the radial shield thickness could be achieved by appropriate optimization of shield material and configuration. Various structural analyses that were being performed for the reference shielding configurations were also discussed.

During this quarterly period, the sensitivity of the analyses to the boron content in borated regions of the radial shield were made. Calculations were also made to determine the sensitivity of thermal spectrum calculations to the determination of multigroup cross sections. Gamma-ray heating calculations continued, with emphasis on determining the effect of gamma production in the radial blanket on the heating rates in the radial shield. Finally, engineering analyses continued to examine alternative shield design configurations that may offer advantages of reduced costs and enhanced structural integrity.

#### 13.1. PHYSICS ANALYSIS

##### 13.1.1. Sensitivity Analysis of the 300-MW(e) GCFR Radial Shield with Respect to the Weight-Percent of Boron

Previous preliminary radial flux calculations of a proposed boronated graphite shield for the 300-MW(e) GCFR demonstrated that it is possible to reduce the reference design thickness of the radial shield about ~30 cm (12 in.) at the core midplane.<sup>(1)</sup> A preliminary sensitivity study of the thermal flux at the PCRV liner demonstrated that an optimum rearrangement of the shield materials can reduce the thermal neutron flux at the liner

by about a factor of three, to about  $2.2 \times 10^8$  neutrons/cm<sup>2</sup>-sec, which is about a factor of four below the present permissible level of  $1 \times 10^9$  neutrons/cm<sup>2</sup>-sec.

During this quarterly period, the effect of boron concentration on the radial neutron flux profile in the shield was evaluated; the material configuration of the radial shield is given in Table 13.1. To evaluate the effectiveness of boron in the B<sub>4</sub>C + C layer for different boron concentrations, the neutron flux in the radial shield was calculated using the 1DFX code (S<sub>6</sub> angular quadratures and 10 energy groups). All variables except the weight-percent of boron in the B<sub>4</sub>C + C layer were kept constant. The ratio of neutron flux at the liner to the neutron flux incident to the radial shield ( $\phi_L/\phi_0$ ) was then calculated. This ratio is shown in Fig. 13.1 as a function of boron concentration. It can be seen that beyond a concentration of 10%, the attenuation does not change significantly.

Table 13.1  
MATERIAL CONFIGURATION FOR THE 300-MW(e) GCFR  
WITH A BORONATED RADIAL SHIELD

| Material             | Dimension <sup>a</sup><br>(cm) | Shield<br>Thickness<br>(cm) |
|----------------------|--------------------------------|-----------------------------|
| Core                 | 0 - 100.46                     | 100.46                      |
| Blanket              | 100.46 - 149.78                | 49.32                       |
| Helium               | 149.78 - 174.0                 | 24.22                       |
| Carbon (graphite)    | 174.0 - 189.4                  | 15.4                        |
| 304 stainless steel  | 189.4 - 202.6                  | 13.2                        |
| Helium               | 202.6 - 208.6                  | 6.0                         |
| Carbon (graphite)    | 208.6 - 226.4                  | 17.8                        |
| B <sub>4</sub> C + C | 226.4 - 244.6                  | 18.2                        |
| 304 stainless steel  | 244.6 - 249.6                  | 5.0                         |
| Helium               | 249.6 - 284.9                  | 35.3                        |
| Iron                 | 284.9 - 286.8                  | 1.9                         |
| 4-B concrete         | 286.8                          |                             |

<sup>a</sup>From core centerline

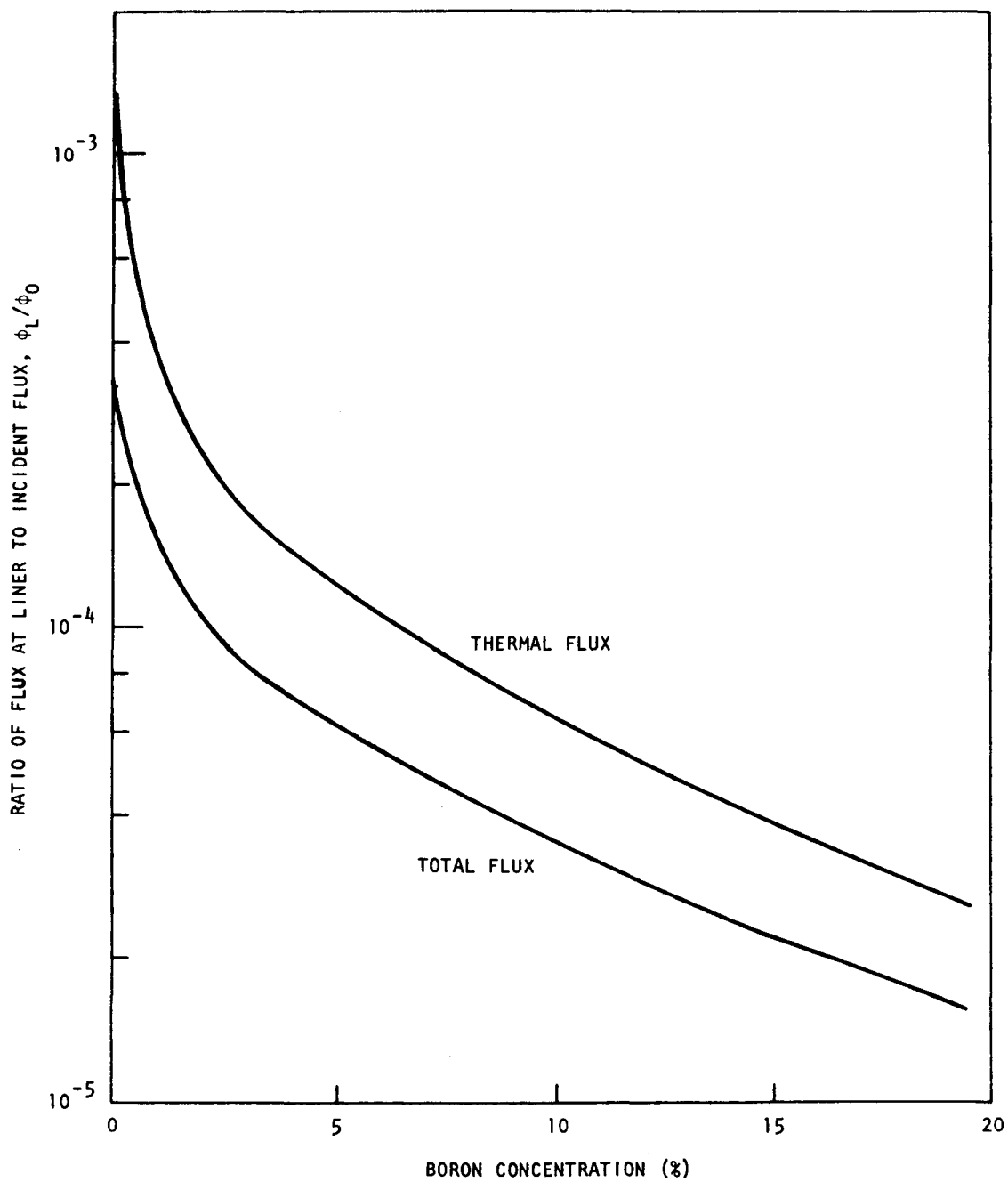


Fig. 13.1 Effect of boron concentration on the attenuation properties of the 300-MW(e) GCFR radial shield

Since the cost of the boronated shield will be proportional to the boron concentration, further studies will be made to determine the minimum concentration required to reduce the thermal flux to acceptable levels.

#### 13.1.2. Spectrum and Cross-section Sensitivity Analysis of the GCFR Radial Shield

The usual procedure for generating multigroup cross sections is to calculate a representative spectrum in many groups (usually 100 or more groups) and then collapse the pointwise cross-section data over this weighting spectrum. This approximate method is valid for use in transport calculations in which there is no strong spatial dependence in the spectrum. In the radial shield of the GCFR, however, the spatial variation of the neutron spectrum is large, ranging from fast near the blanket to highly thermal near the PCRV. Spatially dependent spectral effects could be treated by using many groups in the transport calculations. However, this requires long computer running times and large data storage. The thermal spectrum is particularly important because the calculation of  $(n, \gamma)$  capture rates in stainless steel is very sensitive to the thermal cross section. Therefore, a sensitivity study was made to determine the effect of differences in thermal spectra on the thermal cross sections.

Previous calculations employed a weighting spectrum corresponding to a homogeneous mixture (50%/50%) of graphite and steel with one thermal group extending to 2.38 eV. To study the effect of the thermal spectrum to graphite-steel mixture compositions, a series of GGC-5 thermal spectrum calculations<sup>(2)</sup> were made for six graphite/steel mixtures. The results of these calculations are shown in Fig. 13.2, where the spectra are normalized at the highest energy group. The corresponding spectrum-weighted thermal capture cross sections for iron and stainless steel are tabulated in Table 13.2. As expected, the higher graphite concentrations yield softer thermal spectra with correspondingly higher capture cross sections.

Since the absorption cross section of iron is quite sensitive to the spectrum, a study of the spatial dependence of the spectrum was made to determine the appropriate spectrum (graphite/iron mixture) to use in the multigroup calculations. The spatial variation of the thermal flux

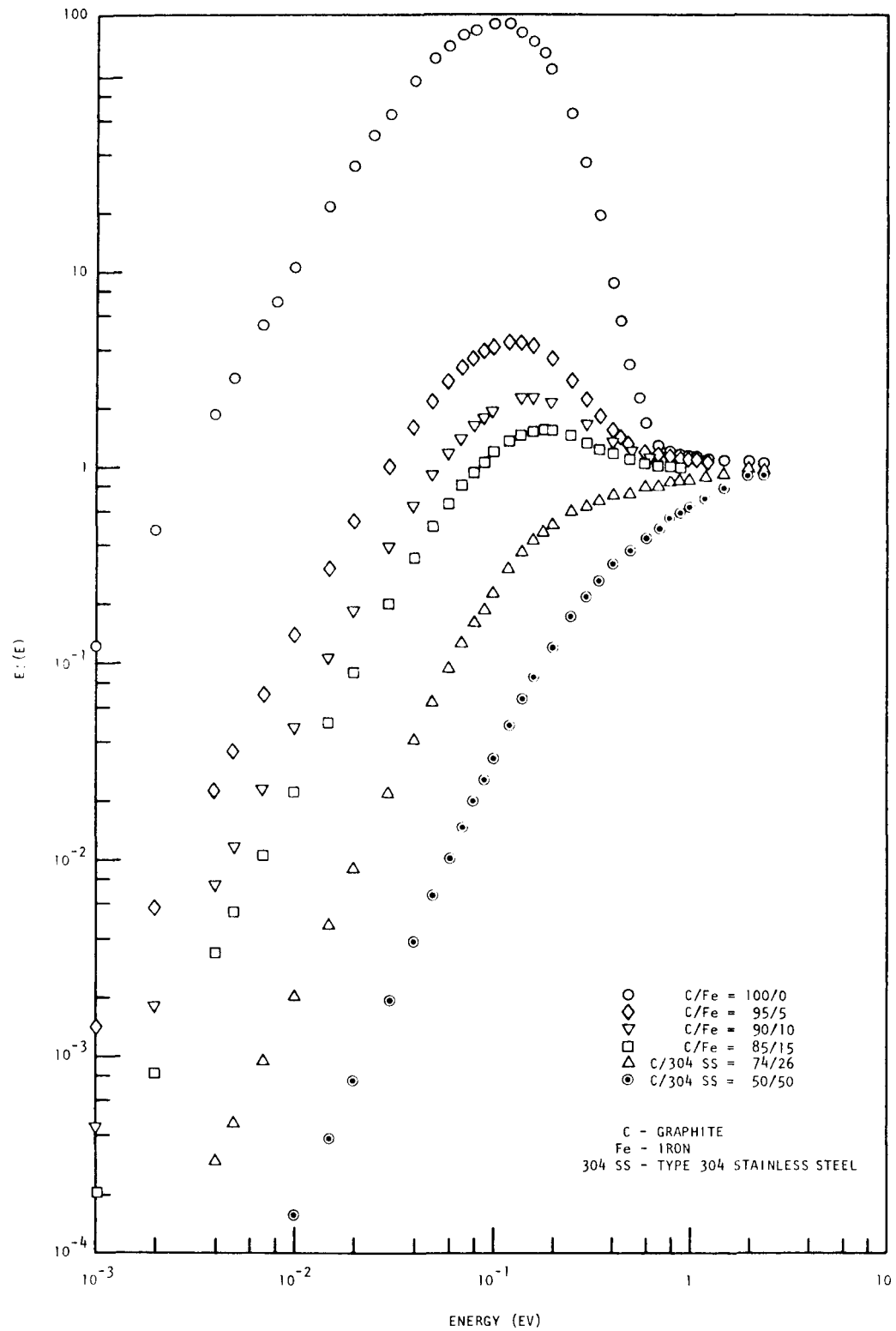


Fig. 13.2 Variation of spectrum for different graphite-stainless steel compositions

Table 13.2

THERMAL ABSORPTION CROSS SECTIONS FOR IRON OR STAINLESS STEEL FOR  
DIFFERENT GRAPHITE-IRON (OR STAINLESS STEEL) SPECTRA

| % Number Density<br>of Graphite<br>in Composition | % Number Density<br>of Iron or 304 SS<br>in Composition | Thermal Absorption<br>Cross Section<br>(E < 2.38 eV)<br>(barn) |
|---------------------------------------------------|---------------------------------------------------------|----------------------------------------------------------------|
| 100                                               | 0                                                       | 1.610                                                          |
| 95                                                | 5, iron                                                 | 1.249                                                          |
| 90                                                | 10, iron                                                | 1.038                                                          |
| 85                                                | 15, iron                                                | 0.913                                                          |
| 76                                                | 24, 304 SS                                              | 0.820                                                          |
| 50                                                | 50, 304 SS                                              | 0.544                                                          |

in the GCFR radial shield conforming to the configuration in Table 13.3 was calculated with 18 groups (9 fast and 9 thermal) using the 1DFX code. Eight upscattering groups in the nine thermal groups were allowed. The calculations were performed for slab geometry with  $S_{12}$  angular quadrature.

The spatial dependence of the thermal neutron flux through the radial shield can be characterized by the spatial dependence of

$$I(x) = \frac{\int_0^{0.414 \text{ eV}} \phi(E,r) dE}{\int_0^{2.38 \text{ eV}} \phi(E,r) dE} .$$

This function represents the spatial dependence of thermal neutrons below 0.414 eV (the cadmium cutoff) relative to the total flux (thermal plus epithermal). The variation of  $I$  through the radial shield is shown in Fig. 13.3, which indicates the dramatic changes in the thermal spectrum as one proceeds from the graphite through the stainless-steel regions. Also shown in Fig. 13.3 are the values of  $I$  corresponding to the six graphite/iron mixtures used in this study. From the analysis, it is clear that the weighting spectrum for the graphite region can be approximated by a calculated spectrum corresponding to an 85% graphite/15% steel mixture.

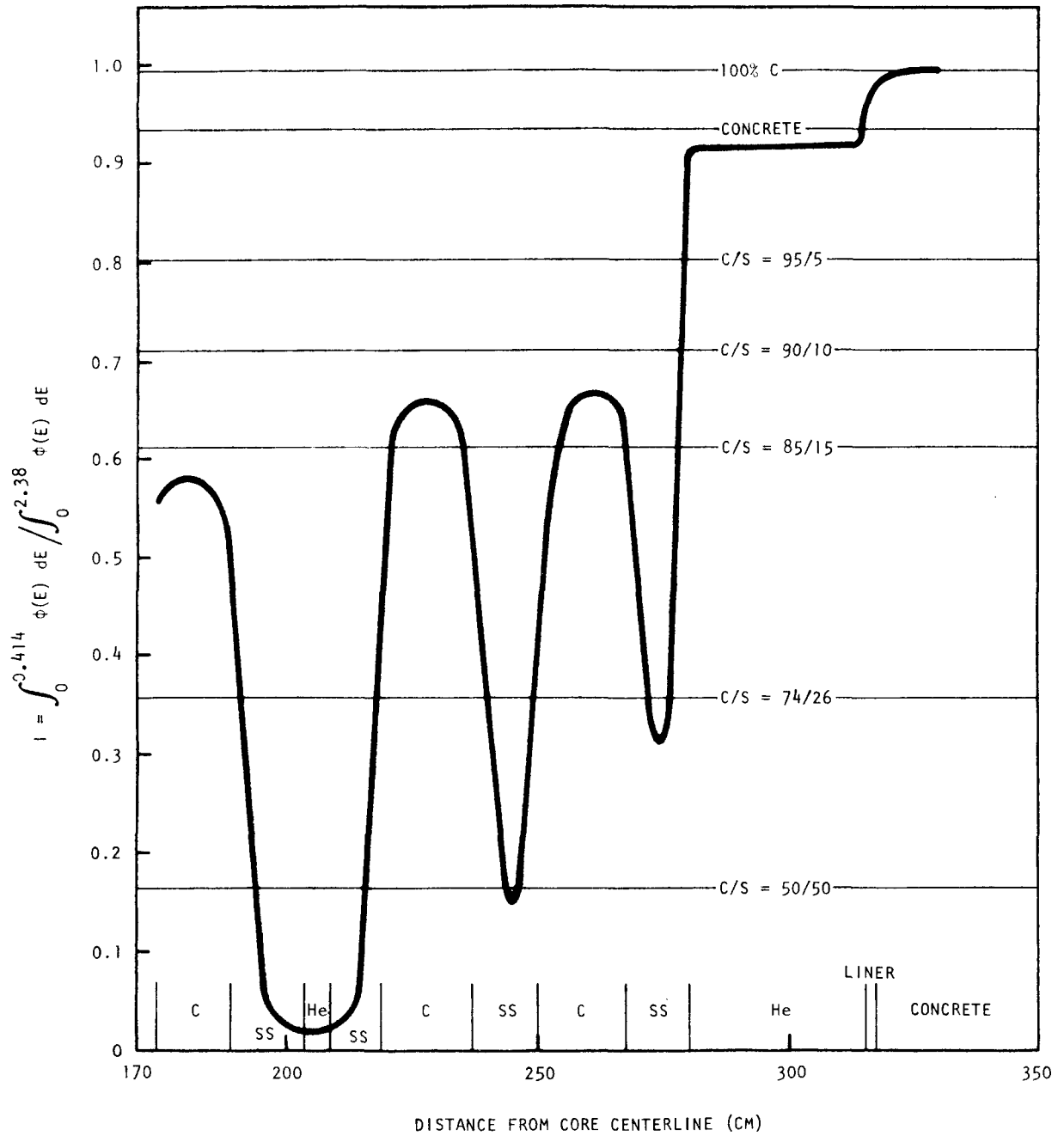


Fig. 13.3 Ratio of thermal flux with  $E < 0.414$  eV to total thermal flux with  $E < 2.38$  eV at different positions in the GCFR radial shield

Table 13.3  
1-D MODEL OF 300-MW(e) GCFR WITH INHOMOGENEOUS SHIELD  
(No boronated region)

| Zone | Radii<br>(cm)              | Material            |
|------|----------------------------|---------------------|
| 1    | 0 - 100.46                 | Core (averaged)     |
| 2    | 100.46 - 149.18            | Blanket (averaged)  |
| 3    | 149.18 - 174.0             | Helium gap          |
| 4    | 174.0 - 189.4              | Graphite            |
| 5    | 189.4 - 202.575            | 304 stainless steel |
| 6    | 202.575 - 208.575          | Helium gap          |
| 7    | 208.575 - 218.735          | 304 stainless steel |
| 8    | 218.735 - 236.9            | Graphite            |
| 9    | 236.9 - 249.6              | 304 stainless steel |
| 10   | 249.6 - 267.4              | Graphite            |
| 11   | 267.4 - 280.2              | 304 stainless steel |
| 12   | 280.2 - 315.5              | Helium gap          |
| 13   | 315.5 - 317.4              | Iron                |
| 14   | 317.4 - 347.4 <sup>a</sup> | Concrete            |

<sup>a</sup>Does not equal outer radius of PCRV.  
Outer radius of the PCRV is 1280.16 cm.

For the inner steel regions, the weighting spectrum can be represented by a calculation based on a mixture of about 50% graphite/50% steel. However, for regions near the PCRV, the spectrum is considerably softer. A spectrum corresponding to the inner shield regions will therefore underestimate the thermal neutron capture rate, and consequently the gamma-ray production rates will also be underestimated. It can also be concluded from Fig. 13.3 that the liner iron cross sections should be weighted by a spectrum representative of the PCRV. The effect of these differences on gamma-ray heating in the concrete are being studied. Nevertheless, it is clear from the studies to date that thermal neutron transport ( $E < 2.38$  eV) should be treated separately with a sufficiently large number of groups in order to adequately predict gamma-ray heating effects near the PCRV.

### 13.1.3. Gamma-ray Heating Studies

Studies of gamma-ray heating in the radial shield of the GCFR continued; in particular, the effect of gamma-ray production in the radial blanket on heating rates in the shield and PCRV were investigated.

As no gamma-ray production data are yet available for thorium, the present studies were based on a uranium blanket. Using the methods described in the last quarterly report,<sup>(1)</sup> a neutron-coupled gamma-ray cross-section set was generated for the uranium blanket region. The material composition for the blanket is given in Table 13.4. Since gamma-ray production cross-section data were not available for chromium and nickel, gamma production data for iron were used.

Table 13.4  
COMPOSITION OF URANIUM RADIAL BLANKET

| Nuclide          | Nuclide Density<br>(No. atoms/barn) | Nuclide Density Used<br>(No. atoms/barn) |
|------------------|-------------------------------------|------------------------------------------|
| U <sup>238</sup> | $9.420 \times 10^{-3}$              | $9.420 \times 10^{-3}$                   |
| Helium           | $3.748 \times 10^{-4}$              | $3.748 \times 10^{-4}$                   |
| Oxygen           | $1.898 \times 10^{-2}$              | $1.898 \times 10^{-2}$                   |
| Chromium         | $2.130 \times 10^{-3}$              |                                          |
| Iron             | $8.450 \times 10^{-3}$              |                                          |
| Nickel           | $1.670 \times 10^{-3}$              |                                          |
| Molybdenum       | $2.450 \times 10^{-3}$              |                                          |
| Equivalent Fe    | $2.450 \times 10^{-3}$              | $1.25 \times 10^{-2}$                    |

Because of the present unavailability of gamma-ray transport measurements in U<sup>238</sup>, no direct check of the U<sup>238</sup> neutron-coupled gamma-ray cross sections could be carried out; therefore, sources and contributions of gamma-ray production in U<sup>238</sup> were closely monitored and compared with other cross-section tables.

Using the U<sup>238</sup>-coupled cross-section set in conjunction with the other radial shield cross-section sets, the following slab geometry transport calculations were carried out: (1) Using the neutron flux incident

on the blanket, the total gamma-ray heating profile in the radial shield due to gamma rays produced in the blanket and other radial shield regions was calculated. (2) Using the neutron flux incident on the blanket, while turning off gamma-ray production in other parts of the shield, gamma-ray heating effects due to blanket-produced gamma rays only were calculated. The results of these calculations are shown in Fig. 13.4, which indicate the following: (1) the contribution of gamma rays produced in the  $U^{238}$  blanket to heating effects in the PCRV region is less than 2.5% of the total gamma-ray heating effect there, and (2) the contribution of gamma rays produced in the  $U^{238}$  blanket is substantial in the first carbon-steel region and should be included for heating calculations in the inner shield regions.

### 13.2. SHIELD ENGINEERING

In the last quarterly report, various structural analyses that were being performed for the reference shielding configuration were discussed.<sup>(1)</sup> During the present reporting period, similar analyses were pursued for an alternative design configuration that may offer advantages of reduced costs and enhanced structural integrity.

As an alternative design configuration to the segmented outer radial shield design, an outer radial shield of the shell type has been proposed. In this design, the outer radial shield consists of graphite and boronated graphite blocks enclosed in the annular space between two concentric cylindrical steel shells.

An effort is now under way to determine the analytical methods and the existing computer programs required to analyze this structure. This effort may be divided into the following areas:

1. Determination of thermal stresses and thermal expansions in the structure. The methods of analyses are well known in these areas and therefore methods development effort is probably not required.
2. Review or development of a method for the analysis of flow-induced vibration of the outer radial shield. Very few papers

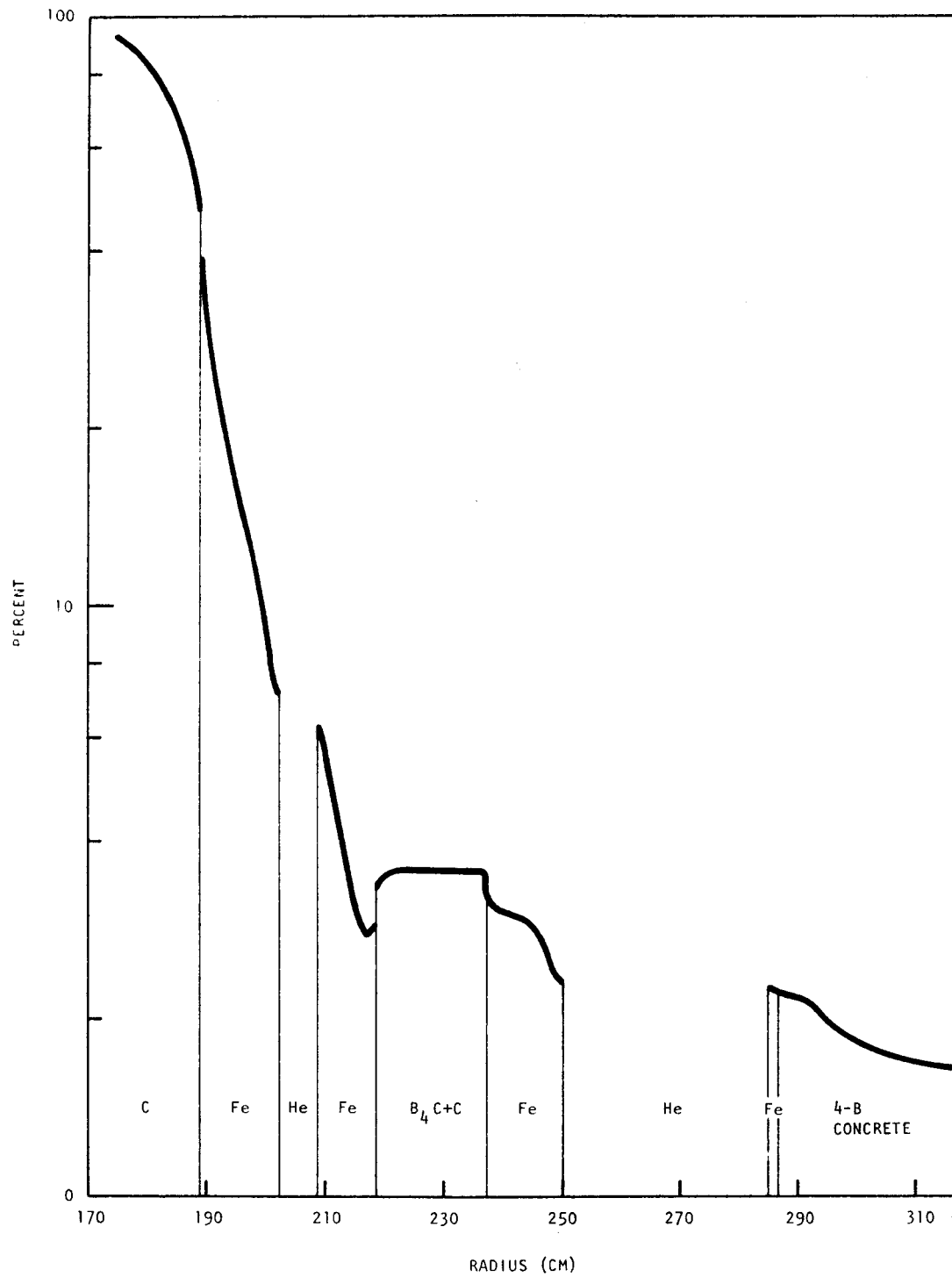


Fig. 13.4 The effect of gamma-ray production in the  $U^{238}$  radial blanket on gamma-ray heating in the radial shield

have been published with regard to the method of analysis and most of the test information is related to pressurized-water reactors.

3. Development of a realistic seismic model based on existing computer codes or newly developed codes. For a preliminary analysis, the SAP-IV code may be adequate. However, a detailed analysis may necessitate the modification of SAP-IV because of the anticipated computer storage problems.
4. Development of computer codes to determine the dynamic pressure of the coolant on the outer radial shield during a design-basis depressurization accident, and any deformation and stresses of the shell structure under the dynamic loading conditions.
5. Development of the analytical methods and related computer programs to investigate the stability of the outer radial shield during a design-basis depressurization accident. The effects of uniform and nonuniform dynamic pressure distributions along the shield in the circumferential direction will be included in this study.

#### REFERENCES

1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1974 through October 31, 1974," USAEC, Report GA-A13238, General Atomic Company, December 12, 1974.
2. Mathews, D. R., et al., "GGC-5, A Computer Program for Calculating Neutron Spectra and Group Constants," Gulf General Atomic, Report GA-8871, September 27, 1971.

#### XIV. FUEL-ROD ENGINEERING (189a No. 13861)

The steady-state and transient performance of the fuel-, blanket-, and control-element rods is being evaluated under this task to determine the performance characteristics, operating limits, and design criteria. The analytical tools (such as the LIFE-III code) are being adapted, updated and/or developed, and applied to the analysis of the rods and the evaluation of GCFR irradiation experiments. Continuous surveillance of the LMFBR fuels and materials development programs and technology is maintained to maximize the use of developing technology and materials properties. Support is provided in the planning and designing of irradiation experiments.

##### 14.1. FUEL-, BLANKET-, AND CONTROL-ROD ANALYSIS

The objectives of this analytical task are to adapt and assess the ability of analytical behavioral models and computer codes (such as LIFE-III) to analyze the GCFR rods for the fuel, blanket, and control elements and to evaluate irradiation experiments. The definition of design criteria, operating margins, failure criteria, and mechanisms under steady-state, transient, and load-following conditions will follow from these analyses.

##### 14.1.1. Fuel- and Blanket-rod Analysis

14.1.1.1. Improvement and Checkout of LIFE-III Code. Considerable effort has been expended toward securing approvals to establish a data link with the Berkeley CDC-7600 computer. This will facilitate the use of the Master Version of the LIFE-III code, which has been put on permanent file at Berkeley and will permit GA to participate more actively in the calibration and checkout of the code. An off-site terminal that is directly linked with the Berkeley computer will be utilized.

Participation in the LIFE Working Group periodic conference calls has continued. The completion of updates to the code is scheduled for

mid-february 1975, at which time the calibration will begin. FORTRAN improvements to the subroutines MAIN, BEGIN, READ 1, and RITE 2 were proposed by General Electric. These changes were incorporated in the GA version of the LIFE-III code and have been successfully checked out. The results obtained were identical to those obtained with the unmodified version of the code, except for some improvement in the code running time. These results were concurred by ANL and Westinghouse Advanced Reactor Division (WARD) during the scheduled conference call of the Working Group. These proposed changes were approved for incorporation into the Master Version of the code on file at ANL and Berkeley.

The draft documentation for the code (User's Manual and Nuclear Systems Materials Handbook, which will contain documented materials properties and the behavioral models) is scheduled to be issued early in February for review. The proposed documentation of the fuel thermal expansion correlation for LIFE-III was received for review. Our comments on the subject were transmitted to the issuing organization (GE).

Following the successful calibration of the LIFE-III code, a post-calibration checkout will be performed by the Working Group member organizations (GE, GA, WARD, AI, HEDL, and ANL) according to an agreed upon checkout plan, which will utilize the observed experimental results from a number of selected irradiated fuel rods. Rods G-1 and G-3 from the GA F-1 irradiation experiment in EBR-II are included in this group of checkout rods. The available pre- and postirradiation data for these rods have been scrutinized and collected for transmittal to HEDL, who has the responsibility of organizing the plan. The proposed checkout plan has been received from HEDL and is now being reviewed.

General Atomic is participating in the "round-robin" analysis of the rods irradiated by General Electric in EBR-II (GE F-20 experiment). The analytical effort will be limited to the nine selected rods that appear to provide the most effective support to GCFR fuel design. The analysis was started during the last quarterly period<sup>(1)</sup> using the LIFE-III code. Preliminary results were then made available for two of the rods. As the LIFE code is now undergoing updating, calibration, and checkout, it was

decided (in accordance with the LIFE Code Working Group) to await completion of the code checkout prior to continuing any analysis of these rods.

14.1.1.2. Fuel-rod Ribbed Cladding Thermal Stress Analysis. The computer code TAC-2D was utilized to study the temperature gradients caused by the variation of the heat-transfer coefficient along the length of the ribbed surface of the fuel-rod cladding so that the stress distribution could subsequently be derived.

The heat-transfer coefficient multiplier for the ribbed surface varies between 1 and 5 as a function of axial position, with an average of about 2.5. The heat-transfer coefficient for a smooth rod is  $0.68 \text{ W/cm}^2\text{-}^\circ\text{C}$ . The helium temperature rise in the reactor is about  $303^\circ\text{C}$  in 101.6 cm, which corresponds to about  $0.4^\circ\text{C}$  in one pitch length. The temperature of helium can therefore be assumed constant at  $427^\circ\text{C}$  at the center of the core for a 410 W/cm linear power. The conductance for the gap between the fuel and the cladding was taken as  $0.568 \text{ W/cm}^2\text{-}^\circ\text{C}$ .

A three-rib model was set up initially to eliminate the end effects on the middle rib. From the results of this calculation, the points along the Z axis where the temperature gradient in the Z direction was zero were picked out and used as adiabatic boundaries for the subsequent three-rib model. This procedure was carried out two times to obtain the boundaries, which were truly adiabatic, for the final model.

From the final three-rib model, the temperatures were chosen to apply as boundary conditions for the one-rib models. The isotherms in the final one-rib model are shown in Fig. 14.1. The isotherms in smooth cladding (without ribs) and a heat-transfer coefficient of  $1.70 \text{ W/cm}^2\text{-}^\circ\text{C}$  ( $2.5 \times 0.68 \text{ W/cm}^2\text{-}^\circ\text{C}$ ) are shown in Fig. 14.2.

The outside surface temperature of the roughened cladding is shown as a function of axial length in Fig. 14.3. The surface temperature of the cladding without ribs is  $533^\circ\text{C}$ .

The temperature variation of the ribbed cladding at the inside surface is shown in Fig. 14.4. Without ribs, this temperature is  $576^\circ\text{C}$ .

The temperature gradient for smooth cladding is in the radial direction and is equal to  $886.5^\circ\text{C/cm}$ . The maximum gradient with ribs at point

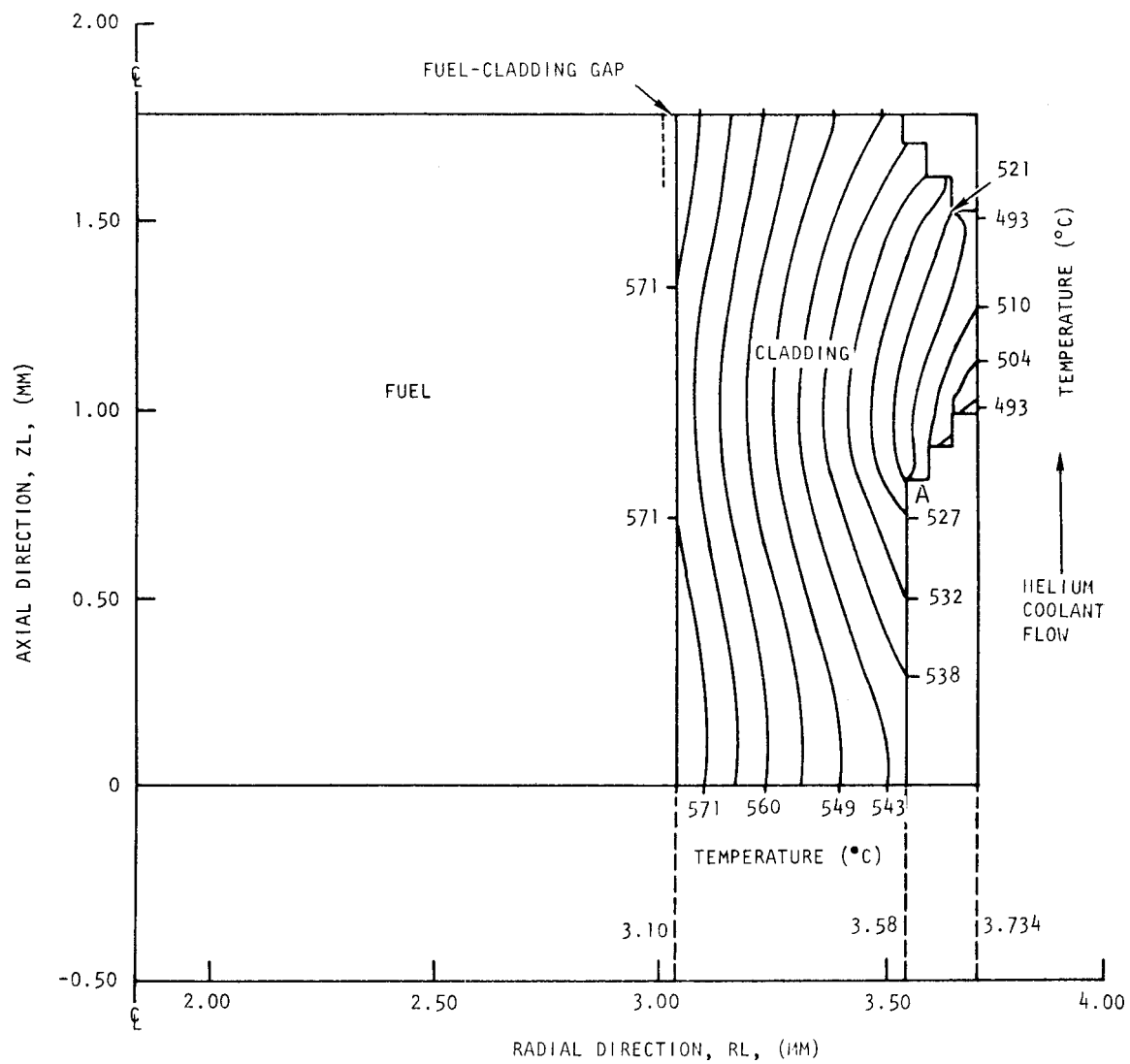


Fig. 14.1 Isotherms in one-rib model for 300-MW(e) fuel rod clad with Type 316 stainless steel

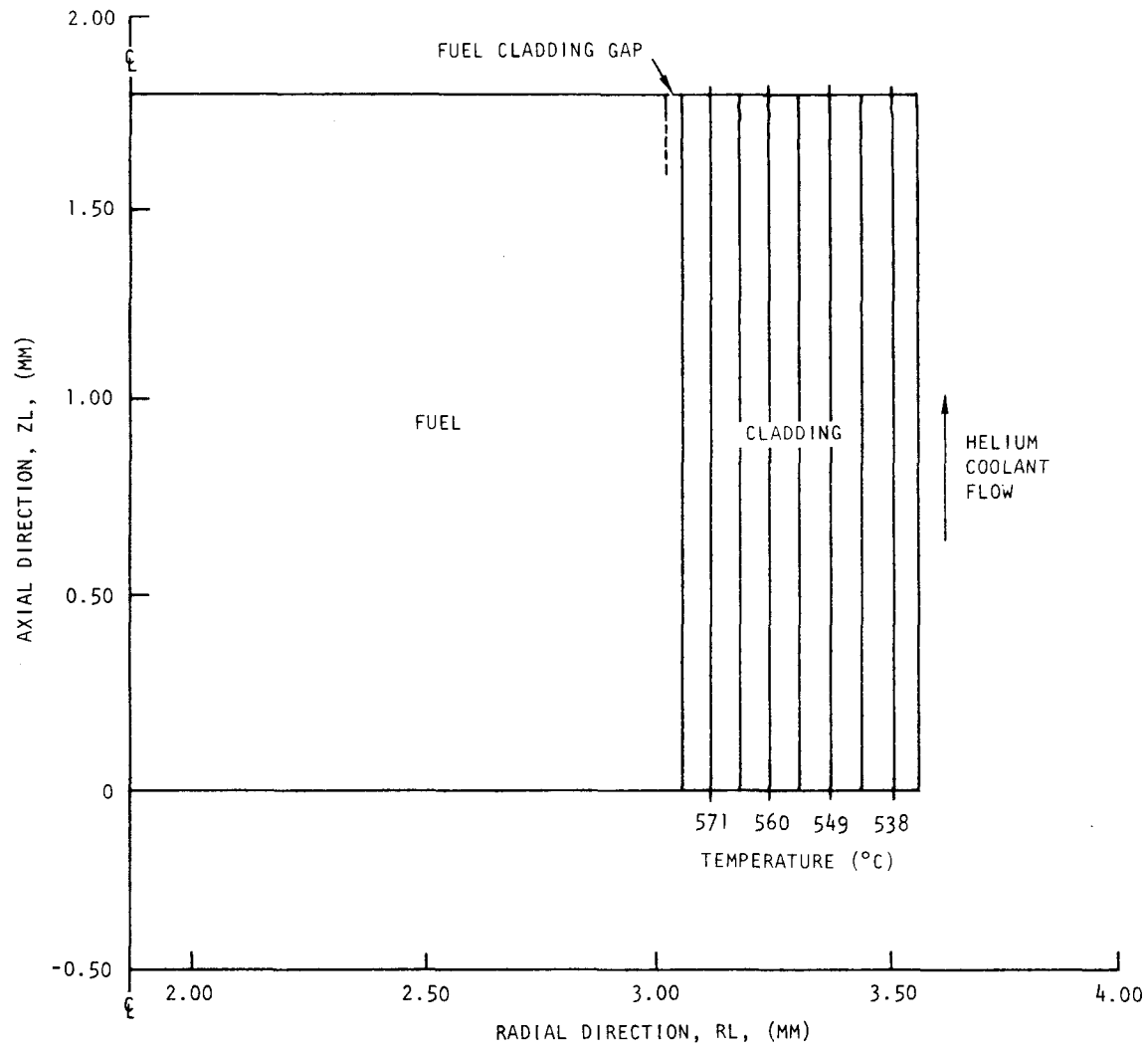


Fig. 14.2 Isotherms in smooth cladding for 300-MW(e) fuel rod clad with Type 316 stainless steel

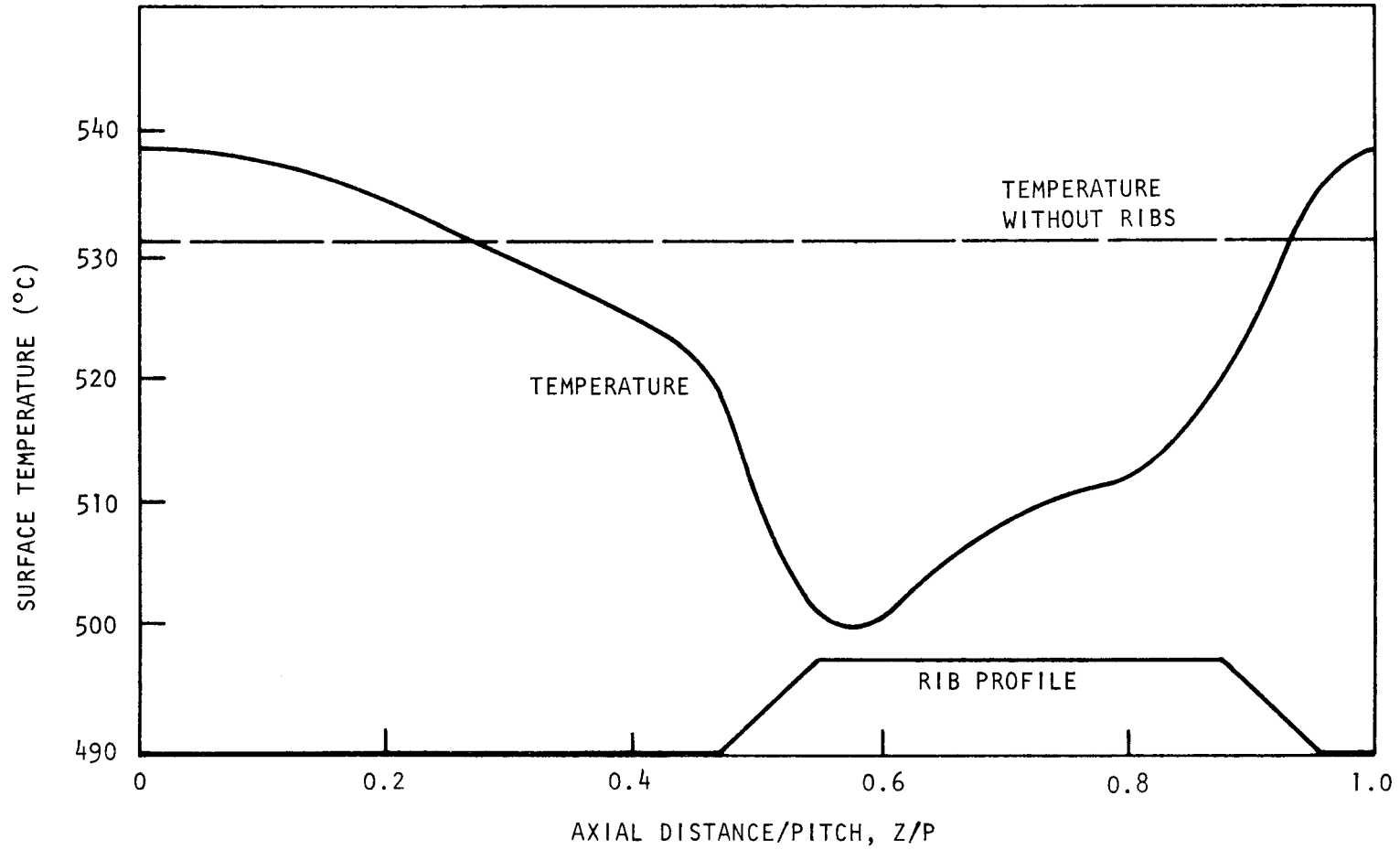


Fig. 14.3 Outside surface temperature of roughened fuel-rod cladding

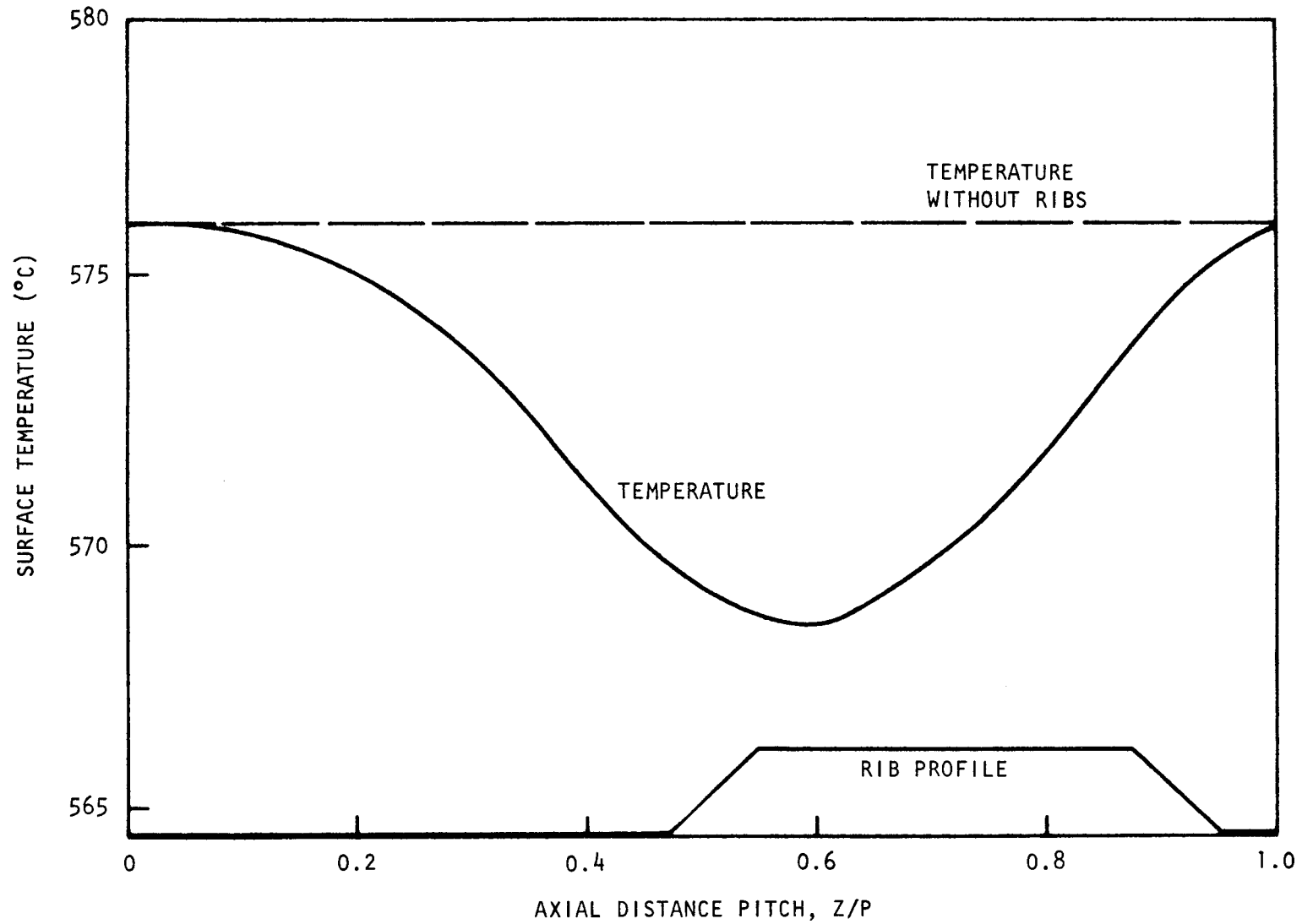


Fig. 14.4 Inside surface temperature of roughened fuel-rod cladding

A in Fig. 14.1 is in a direction  $7.74^\circ$  to the horizontal and is equal to  $1796^\circ\text{C}/\text{cm}$ .

Based on the adiabatic boundary temperatures obtained above, a study of the initial thermal stresses and their subsequent relaxation due to thermal- and irradiation-induced creep has been started. The distortion of the cladding in the rib area due to creep and thermal and swelling effects will also be investigated.

The computer code TEPC-2D is being used initially. A finite-element model of one pitch length containing 1108 triangular elements and 606 nodes was generated (see Fig. 14.5). It provides a fine mesh in the root diameter-rib-base junction within the rib itself and a coarser mesh elsewhere. A constant heat flux simulates the fuel-cladding gap, whereas adiabatic boundaries are enforced on the other three surfaces of the model. Nodes along the left-hand section are constrained from motion in the axial direction.

Because the code is capable of a heat-transfer analysis, and since the TEPC and TAC models are so different, the temperature profiles will be regenerated by TEPC and then the stress and distortion analyses performed.

#### 14.1.2. Analysis of Irradiation Tests

14.1.2.1. Rod G-1 Molten Boundary and Structural Zones. Two of the experimental rods (rods G-1 and G-3) from the GA F-1 irradiation experiment in EBR-II were included in the group of irradiated experimental rods that will be utilized in the postcalibration checkout of the LIFE-III Master Version (see Section 14.1.1.1). Postirradiation ceramography indicated that some central fuel melting had occurred in rod G-1. Since the radii of melting and the radii of fuel restructuring zones (columnar and equiaxed grain growth) are a desirable part of the data package needed for the postcalibration checkout of the code, an analysis of the postirradiation structure in rod G-1 was performed (from photographs obtained from ANL) in order to determine the location at which measurements of columnar and equiaxed grain growth and melt regions should be made.

Hand calculations, using the known heat-transfer equations and various combinations of conservatively low gap conductance and fuel thermal

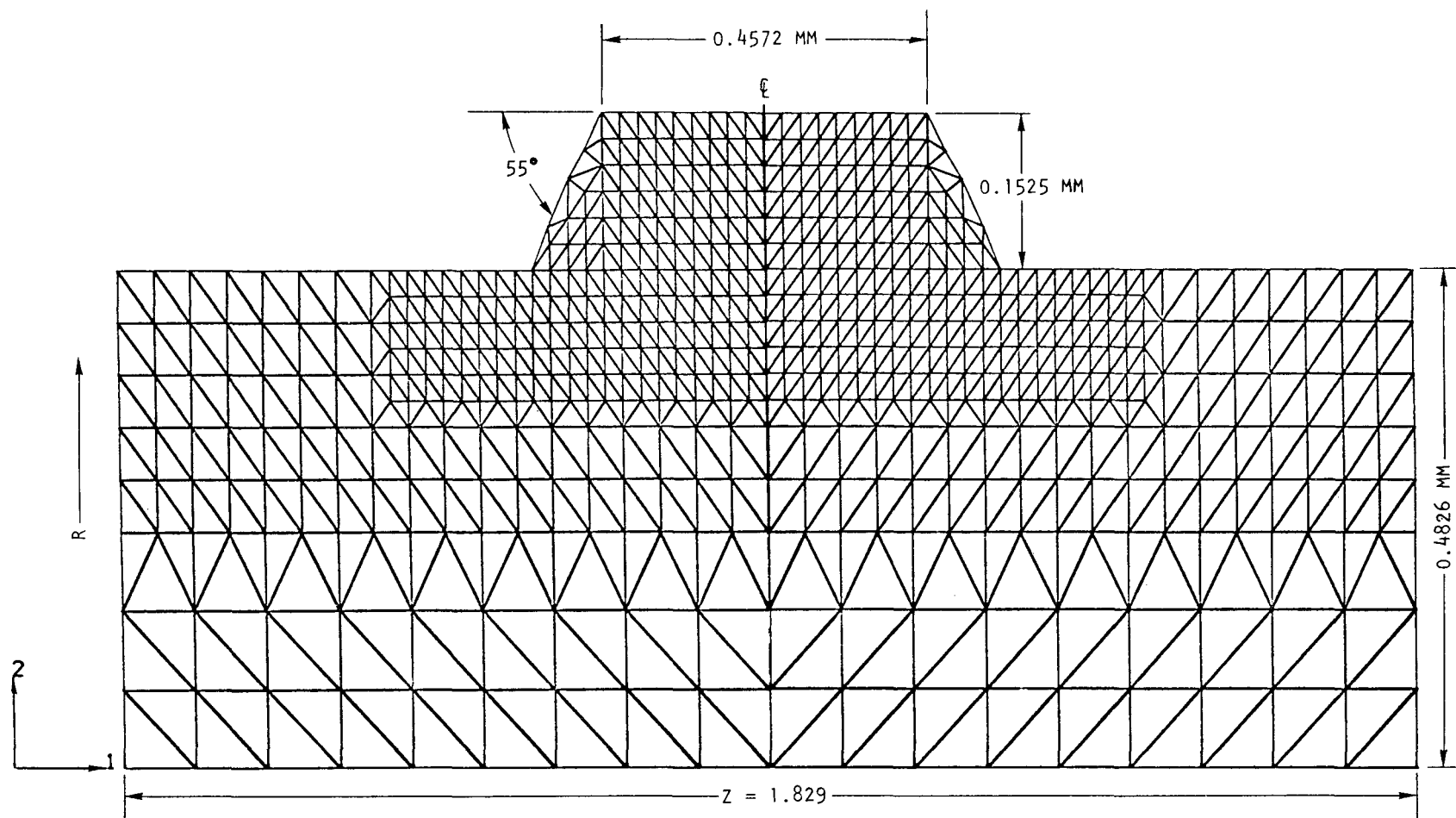


Fig. 14.5 Finite-element model of one pitch length of rib

conductivity values, were also performed to obtain estimates of maximum anticipated fuel centerline temperature and maximum extent of melting.

The interpretation of the microstructure associated with the thermal histories of ceramic nuclear fuels has a major influence on the interpretation of experimental results and on the evaluation of fuel-performance-prediction computer codes because the evaluation is made on the basis of how closely the codes can predict the observed experimental results.

#### 1. Grain Growth and Restructuring

Microstructural features and grain growth from postirradiation ceramography are utilized to determine certain temperature boundaries, including the molten boundary. Good agreement exists among investigators about the temperatures associated with the boundaries of grain growth (equiaxed grain growth and columnar grain growth).

Identification of the molten boundary has often been difficult and controversial. This discussion is thus primarily concerned with the molten boundary in view of the fact that identification of this boundary is essential to the analysis of the data from rod G-1 and the comparison to the LIFE code predictions.

Fission spikes, temperature, and thermal gradients encountered in porous nuclear fuel pellets result in density, grain structure, and dimensional changes that significantly affect the temperature calculations. Aside from possible density and dimensional changes (recently shown to be a function of initial material characteristics, fuel microstructure resulting from the fabrication procedure, and fission rate<sup>(2)</sup>), good agreement exists among the investigators on the grain growth and associated temperature boundaries.<sup>(3-10)</sup> Ceramic nuclear fuels that operated with a centerline temperature below the melting point will display one or all of the following structural zones.

Columnar Grain Growth Region. The process of columnar grain growth requires a high-temperature environment and a thermal gradient. Experimentation at the AECL, Chalk River<sup>(3,5)</sup> and confirmed by others,<sup>(4)</sup> proved conclusively that columnar grains form by a mechanism of void migration up a thermal gradient by a process of evaporation and deposition. Based

on experimental data,<sup>(4-7,9)</sup> the lower temperature limit for this process is 1760°C. The process of void migration up the thermal gradient is continuous; the sources of voids are the equiaxed grain region and the circumferential cracks in the fuel pellet<sup>(7)</sup> (void migration leads to central void formation). The amount of porosity that travels up the thermal gradient is also a strong function of pore size distribution, larger pores being more stable.<sup>(2)</sup> The outer limit of columnar grain growth is then associated with a temperature of 1760°C.

Equiaxed and Unrestructured Grain Regions. Data presented in Refs. 3 and 4 indicate an outer temperature boundary for the equiaxed grain growth region of 1480°C.

Structural and grain growth changes in the region of temperature below 1480°C (aside from possible porosity changes) are minimal, and this region is called the unrestructured region.

Fuel Molten Boundary. A typical structure found in fuel that has undergone central melting is shown in Fig. 14.6. The once disputed radii of melting are shown.

Robertson, et al.,<sup>(11)</sup> and DeHalas and Horn<sup>(12)</sup> originally hypothesized that melting occurred to the "outer edge of the porous zone within the columnar grain region." An experiment was later performed by Horn and Christensen<sup>(13)</sup> which utilized tungsten wire markers positioned in a spiral array within the fuel which was then irradiated, and partial central melting occurred in a vertical axial position. The radial limit to which the tungsten wires were still visible represented the radius of melting since the wires within the molten region would have sunk to the bottom. The experiment was reported to confirm the above hypothesis.

A later experiment performed by Pember and compiled by Horn<sup>(14)</sup> utilized tungsten spheres instead of wires. The position of the spheres indicated a radius of melting that corresponded to the outer limit of the high-density band (moving radially outward). This is in agreement with Lyons results<sup>(15)</sup> of two tests in which tungsten wires (0.76 mm diam by 0.76 mm long) and a tungsten slug (2.54 mm diam by 2.54 mm long) were used. In both tests, the markers came to rest on a plane corresponding to the outer limit of the high density band.

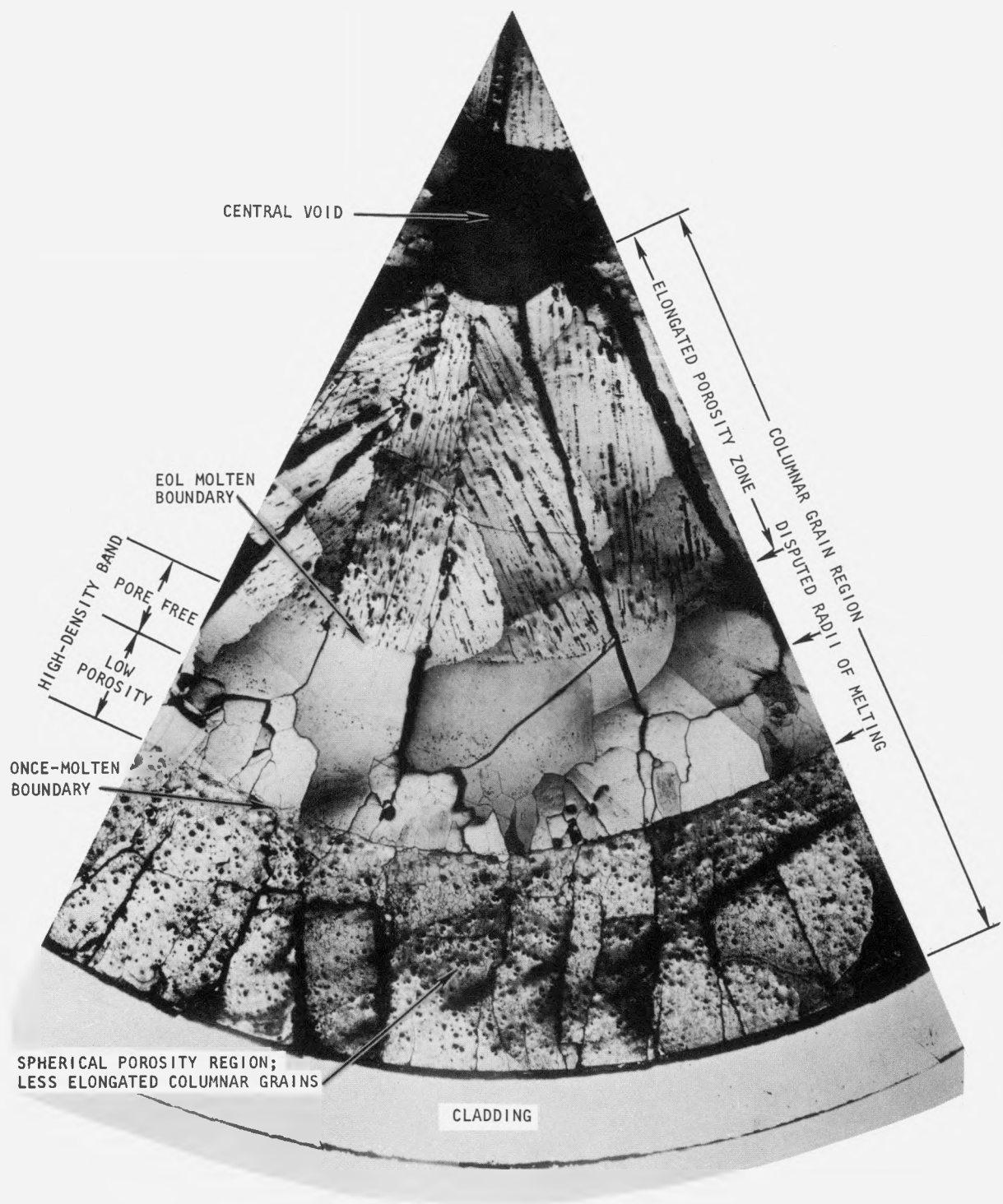


Fig. 14.6 Typical structure of fuel after operation with central melting (linear heat rating,  $950 \pm 200$  W/cm; initial density, 95% TD; exposure, 680 MWd/Te)

More evidence, which supports the "outside edge of the high-density relatively pore-free band," is provided by an experiment performed by Christensen.<sup>(16,17)</sup> Experimental fuel pins were operated with the fuel axis horizontal and at power levels such that central fuel melting occurred. Large central voids were observed in the fuel specimens during postirradiation metallography. These voids were all displaced upward relative to the geometric and thermal centers of the fuel. Since this was a void that existed during irradiation, it should have behaved "...much like the bubble in a spirit level with the upper surface of the void coinciding with the solid-liquid interface." In general, this upper surface was found to coincide with the melt radius postulated by Lyons.<sup>(15)</sup> Christensen states that the experiment was a clear demonstration of the Lyons, et al., melt boundary, contrary to his own previous conclusions.<sup>(13)</sup>

From the evidence presented above, the following is concluded (regarding Fig. 14.6):

1. The radially inward (from pellet surface) termination of a relatively large and spherical porosity zone is the original "once molten" boundary.
2. The termination of large elongated porosity (moving radially outward from the center) is the end-of-life (EOL) molten boundary.
3. The high-density band between these two zones was once molten and lost most of its porosity in melting. The thermal conductivity of the molten fuel increased due to the decrease in the porosity and the result was a gradual radially inward movement of the molten boundary, leaving behind a high-density relatively pore-free zone. Continued operation after solidification of this zone caused some fine porosity to start moving up the thermal gradient by vapor transport process (low-porosity high-density band).
4. The large, radially elongated porosity and the trails of fine porosity within the central EOL molten zone are primarily shrinkage voids trapped during the sudden solidification and porosity, which was in transit up the thermal gradient.

5. The relatively large spherical pores in the outer zone of the less elongated columnar grains are the result of pore coalescence rather than migration up the less steep thermal gradients in the outer band; hence, the less elongated grains and the fine sub-grain structure.

## 2. Postirradiation Structure of Rod G-1

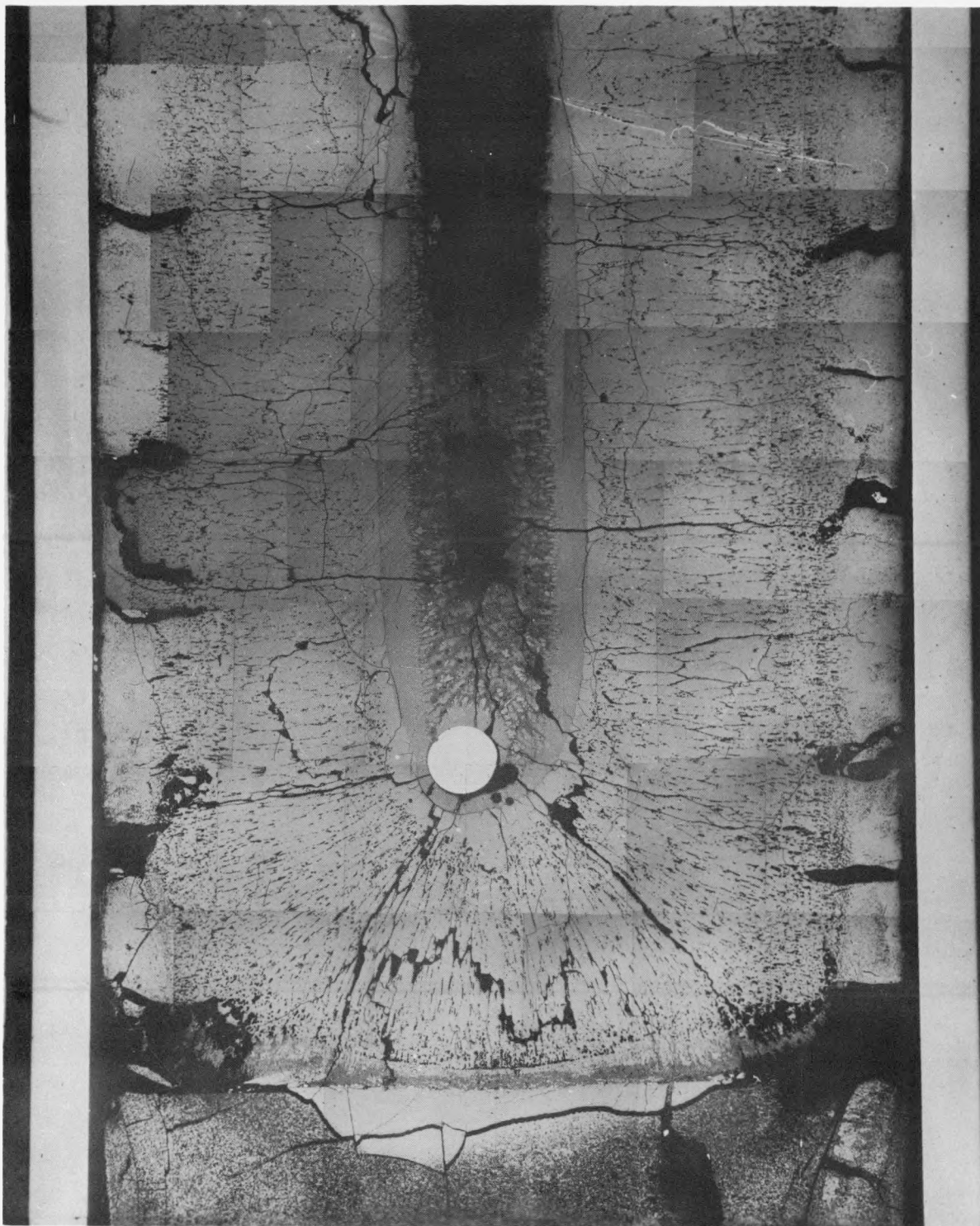
Various sections of irradiated rod G-1 are shown in Figs. 14.7, 14.8, and 14.9. The longitudinal section from the bottom of the rod (Fig. 14.7) shows a molten zone that had occurred after considerable fuel vaporization and deposition at the cooler end had occurred and thus formed a plug in the central void at the end. This zone extends to a maximum radius of 0.838 mm from the center (0.102 mm beyond the original void radius) and extends upward less than 13 mm. No indication of molten fuel is seen in the upper end of the fuel column (Figs. 14.8 and 14.9). The existence of a relatively pore-free zone past the molten zone indicates that earlier in life the molten zone may have extended to a radius of as much as 0.965 mm (0.229 mm beyond the original void radius). An extended columnar grain growth region is present in both the lower and upper ends of the fuel, which is followed by a high-porosity band, an equiaxed grain region, and a relatively unstructured region.

These outer limits of grain growth (moving radially outward) are indicative of temperatures well below melting (1760°C for columnar grains, 1480°C for equiaxed grains). It is concluded that melting is visible in the lower (~13 mm) end of the fuel column only and the "once molten" fuel may have extended to a maximum diameter of 1.930 mm (0.229 mm radially beyond the original central void).

The measured diameters of grain growth are 4.572 mm and 5.486 mm, respectively, for the columnar and equiaxed regions (Fig. 14.8).

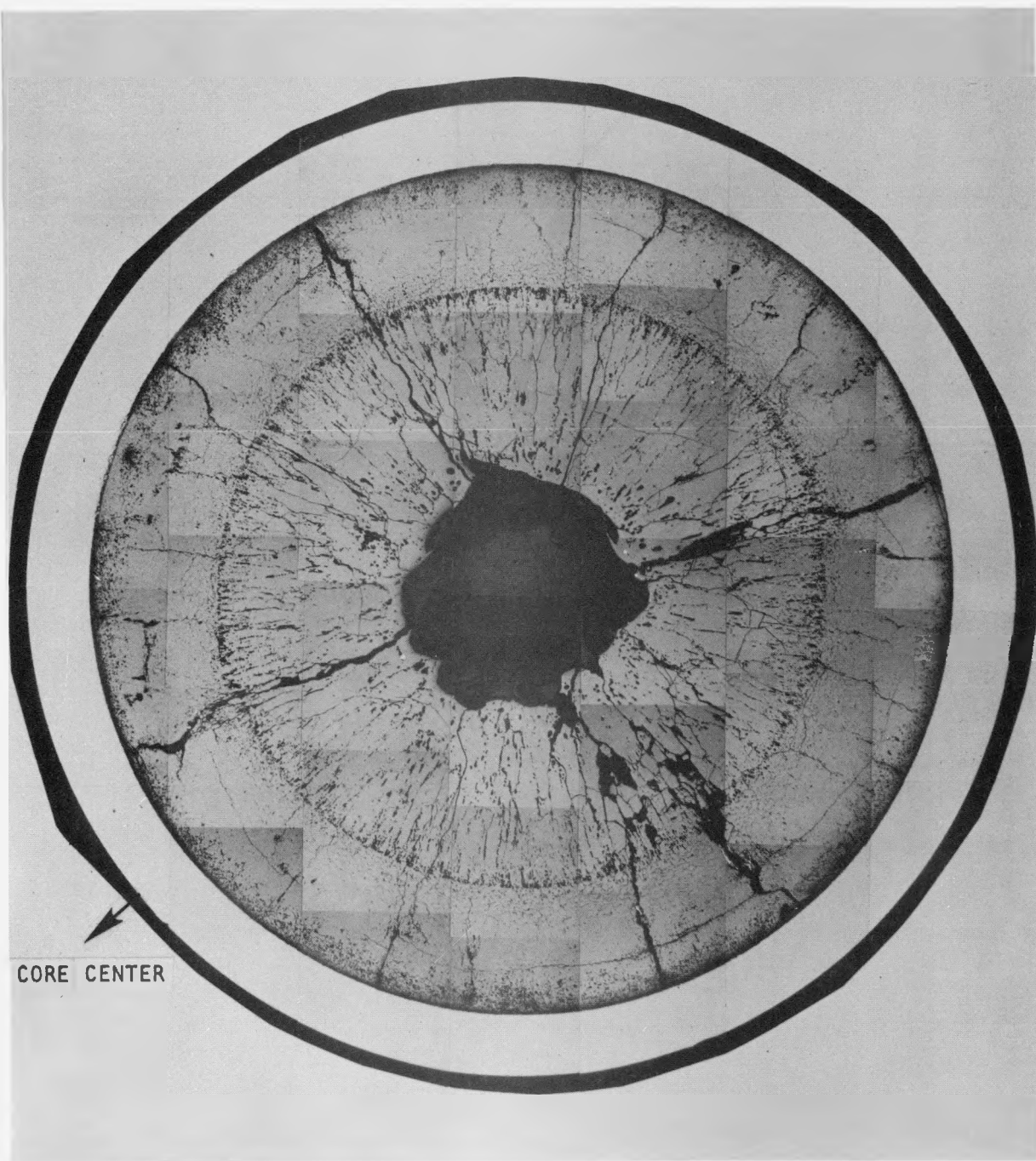
## 3. Fuel-temperature Calculations

Hand calculations using the known heat-transfer equations were performed to obtain estimates of maximum anticipated fuel centerline temperature. The calculations were based on the following assumptions:



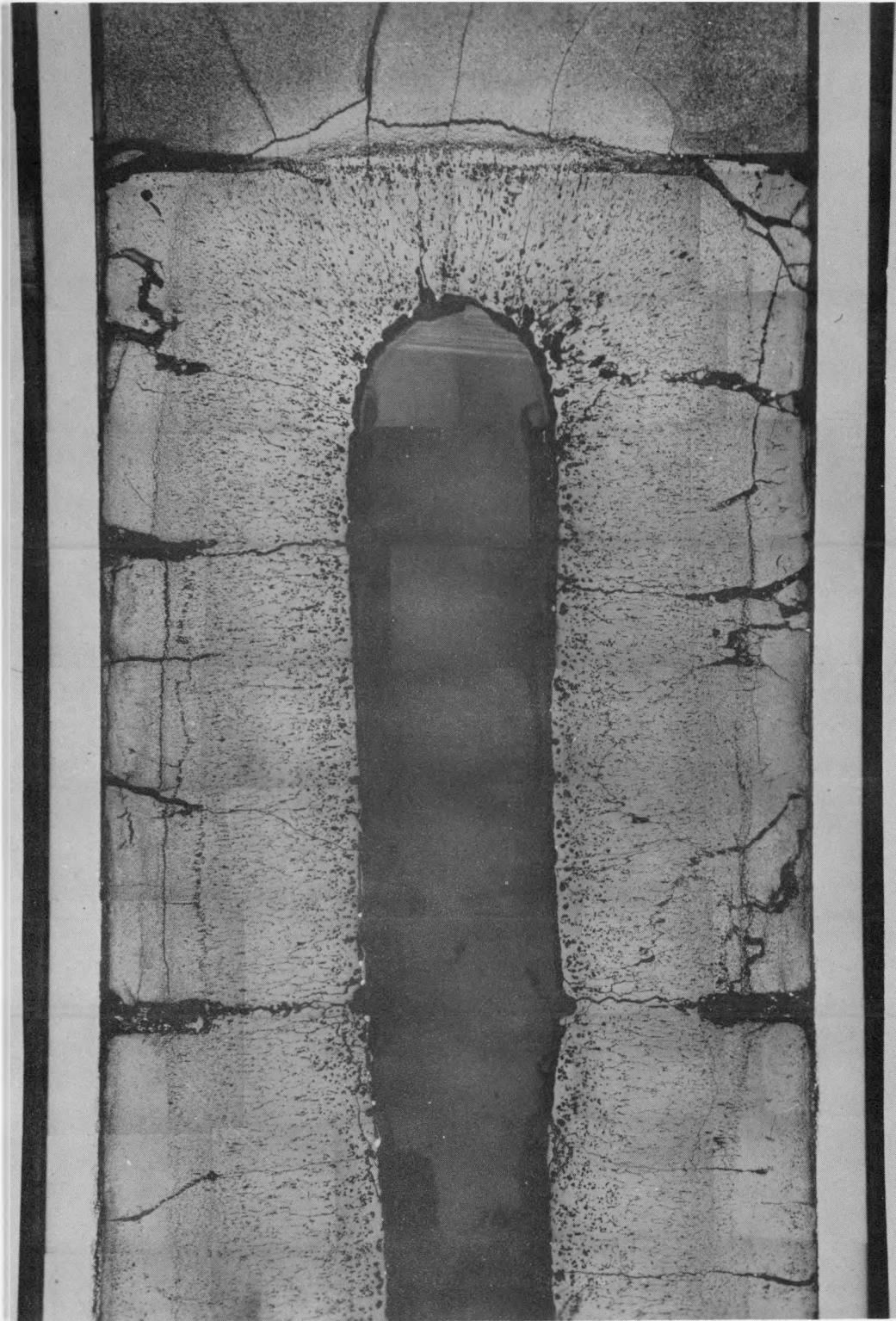
MSD-168787

Fig. 14.7 Photograph showing evidence of central melting near bottom of fuel rod G-1



MSD-168567

Fig. 14.8 Photograph showing grain growth regions in fuel rod G-1 3.3 in. above core bottom



MSD-168783

Fig. 14.9 Photograph showing central void closure by vapor transport at top of fuel column in fuel rod G-1

- a. The linear power levels were corrected by a factor of 0.91, as suggested by ANL (EBR-II was found to have operated at 91% of full power).
- b. A conservatively low assumed fuel-cladding gap conductance value of  $0.51 \text{ W/cm}^2\text{-}^\circ\text{C}$  (a value of 0.71 would be more applicable even without considering fuel outward ratcheting).
- c. A conservatively low assumed gap conductance value of  $0.284 \text{ W/cm}^2\text{-}^\circ\text{C}$ , assuming that the fuel densified to 96.5% TD with a consequent decrease of fuel diameter (increase in fuel-cladding gap) and no outward fuel ratcheting.
- d. The fuel thermal conductivity relation, which is used in the LIFE-III code.
- e. A cladding outside surface temperature of  $800^\circ\text{C}$ .
- f. The fuel melting temperature decreased with burnup ( $\sim 18^\circ\text{C}$  per 10 MWd/kg metal burnup).

The maximum rod linear powers occurred during EBR-II runs 47 and 51; therefore, the calculations were performed for those two runs. The parameters used are given in Table 14.1; the basic equations utilized were:

$$Q/A = \frac{qE_r}{\pi d_o}$$

$$\Delta T_c = \frac{(Q/A)t_c}{K_c} = ^\circ\text{C}$$

$$\Delta T_g = \frac{Q/A}{h_g} = ^\circ\text{C}$$

$$\int_{T_s}^{T_d} K_f(dT) = \frac{q}{4\pi} \left[ 1 - \frac{2b^2 \ln(a/b)}{a^2 - b^2} \right] E_r = \text{W/cm} \quad .$$

$$\int_{T_s}^{T_b} K_f(dT) = \left( \frac{q}{4\pi} \right) \left( \frac{1}{a^2 - b^2} \right) [(a^2 - d^2) - 2b \ln(a/b)] E_r = \text{W/cm} \quad .$$

where  $Q/A$  = surface heat flux ( $W/cm^2$ ),  
 $q$  = linear rod power ( $W/cm$ ),  
 $E_r$  = fraction of power generated in fuel,  
 $t_c$  = cladding thickness (cm),  
 $K_c$  = cladding thermal conductivity ( $W/cm-^{\circ}C$ ),  
 $K_f$  = fuel thermal conductivity ( $W/cm-^{\circ}C$ ),  
 $h_g$  = fuel-cladding gap conductance ( $W/cm^2-^{\circ}C$ ),  
 $d_o$  = rod outside diameter (cm),  
 $a$  = pellet radius (cm),  
 $b$  = pellet central void radius (cm),  
 $d$  = any radius at which integral is to be evaluated (cm),  
 $\Delta T_c$  = temperature drop across cladding ( $^{\circ}C$ ),  
 $\Delta T_g$  = temperature drop across fuel-cladding gap ( $^{\circ}C$ ),

Table 14.1  
 ROD G-1 PARAMETERS USED IN CALCULATIONS

|                                                                          |             |               |               |
|--------------------------------------------------------------------------|-------------|---------------|---------------|
| Rod ID, mm . . . . .                                                     | 6.680       |               |               |
| Rod OD, mm . . . . .                                                     | 7.620       |               |               |
| Cladding thickness, mm . . . . .                                         | 0.467       |               |               |
| Cladding thermal conductivity at 820°C, $W/cm^2-^{\circ}C$ . . . . .     | 0.256       |               |               |
| Fuel pellet central void radius, mm . . . . .                            | 0.737       |               |               |
| Fuel outer radius, mm . . . . .                                          | 3.2957      |               |               |
| Cladding outer surface temperature, $^{\circ}C$ . . . . .                | 800         |               |               |
| Fuel as-fabricated density, % TD . . . . .                               | 91          |               |               |
| Assumed fuel-cladding gap conductance values, $W/cm^2-^{\circ}C$ . . . . | 0.284, 0.51 |               |               |
|                                                                          |             | <u>Run 47</u> | <u>Run 51</u> |
| Linear power, <sup>a</sup> $W/cm$ . . . . .                              | 480         | 493           |               |
| Effective full-power hours of operation . . . . .                        | 194         | 1770          |               |
| Fuel melting temperature, $^{\circ}C$ . . . . .                          | 2790        | 2770          |               |
| Burnup, MWd/kg metal . . . . .                                           | 1.7         | ~15           |               |
| Measured columnar grain growth radius, mm . . . . .                      | 2.350       |               |               |
| Thickness of columnar grain growth region, mm . . . . .                  | 1.727       |               |               |

<sup>a</sup>The maximum operating linear power levels occurred during runs 47 and 51.

The results of the calculations are shown in Table 14.2. Even when most adverse conditions are assumed (i.e., the fuel-cladding gap conductance has dropped to  $0.284 \text{ W/cm}^2\text{-}^\circ\text{C}$  as a result of fuel densification, there is no fuel radial ratcheting, and the integral of thermal conductivity for fuel of 91% TD is used even though fuel has densified), the estimated maximum fuel temperature never exceeded  $3000^\circ\text{C}$  if the fuel was operating at the reported linear power levels of 480 and 493 W/cm during runs 47 and 51, respectively, and if an excursion of power or temperature (not known to us) did not occur during the irradiation. The molten zone (as seen in the photomicrographs) has the appearance of solidified material from a melt after it has collected toward the bottom of the central void either from skin melting of the upper region or a quick melt of a thin layer due to temperature or power excursion.

Calculations and metallographic observations both indicated that either melting occurred to a maximum diameter of 1.676 mm (0.102 mm beyond original void radius) or skin melting occurred in the peak power region and the molten fuel relocated at the bottom of central void.

14.1.2.2. Support of Proposed Irradiation Tests. Irradiation testing of GCFR prototypical design rods (F-5 series) in EBR-II is being planned. This test is prototypical except that

1. The rod is not vented, but a large-enough plenum will be provided so as to maintain a low internal pressure;
2. The coolant is not helium (EBR-II coolant);
3. The rods are not full length; and
4. The fluence is lowered by a factor of  $\sim 1.5$  and thus the cladding damage and swelling would not be representative of GCFR conditions.

One of the requirements is to perform a step change in power to simulate the rotation of fuel assemblies to minimize the total assembly distortion. The other requirement is that the fuel-rod internal pressure at end of life should approximate as closely as possible that of the actual

Table 14.2  
RESULTS OF CALCULATIONS FOR ROD G-1

| Linear Power (W/cm) | Total Hours of Operation | Assumed Gap Conductance (W/cm <sup>2</sup> -C) | Assumed Fuel Conductivity (% TD) <sup>a</sup> | Calculated Max. Fuel Temperature (°C) | Calculated Temperature Above Melting (°C) | Estimated Melt Radius <sup>b</sup> (mm) | Comments                                                                                 |
|---------------------|--------------------------|------------------------------------------------|-----------------------------------------------|---------------------------------------|-------------------------------------------|-----------------------------------------|------------------------------------------------------------------------------------------|
| EBR-II Run 47       |                          |                                                |                                               |                                       |                                           |                                         |                                                                                          |
| 480                 | 194                      | 0.51                                           | 91%                                           | 2770                                  | ---                                       | -----                                   | Most adverse conditions.<br><br>Calculated from measured columnar grain-growth diameter. |
|                     |                          | 0.284                                          | 96.5%                                         | 2835                                  | 45                                        | <0.076                                  |                                                                                          |
|                     |                          | 0.284                                          | 91%                                           | 2995                                  | 205                                       | <0.280                                  |                                                                                          |
|                     |                          | -----                                          | 96.5%                                         | 2806                                  | 16                                        | <0.025                                  |                                                                                          |
|                     |                          | -----                                          | 91%                                           | 2983                                  | 193                                       | <0.254                                  |                                                                                          |
| EBR-II Run 51       |                          |                                                |                                               |                                       |                                           |                                         |                                                                                          |
| 493                 | 1770                     | 0.51                                           | 91%                                           | 2820                                  | 50                                        | <0.102                                  | Most adverse conditions.<br><br>Calculated from measured columnar grain-growth diameter. |
|                     |                          | 0.284                                          | 96.5%                                         | 2875                                  | 105                                       | <0.153                                  |                                                                                          |
|                     |                          | 0.284                                          | 91%                                           | 3000                                  | 230                                       | <0.305                                  |                                                                                          |
|                     |                          | -----                                          | 96.5%                                         | 2830                                  | 50                                        | <0.102                                  |                                                                                          |
|                     |                          | -----                                          | 91%                                           | 3000                                  | 230                                       | <0.305                                  |                                                                                          |

<sup>a</sup>The LIFE-III conductivity is used for the appropriate fuel density.

<sup>b</sup>Beyond original central void radius.

GCFR environment (i.e., to limit the resulting strain from internal gas pressure to less than 0.02%, which is a negligible value). The coordination of these requirements for this test necessitated that the following be determined:

1. The time at which fuel-cladding contact would occur at a power level of 410 W/cm (12.5 kW/ft).
2. The time and power level necessary for restructuring of the fuel prior to a step increase to a higher power level.
3. The total end-of-life internal gas pressure buildup that would result in a permanent cladding strain not to exceed 0.02% (this low value was chosen so that it will be negligible in comparison to cladding strains that may arise from other causes).

Using the allowable cladding-strain limit, the end-of-life internal gas pressure was calculated. For the requirement for step change in power, a number of possible experimental approaches were considered. These are discussed in Section 14.5. To aid in the evaluation of these possible approaches, the calculations discussed below were performed. The following parameters were used in the calculations:

|                                                |                        |
|------------------------------------------------|------------------------|
| Pellet diameter, mm . . . . .                  | 6.096                  |
| Central hole diameter, mm . . . . .            | 1.27                   |
| Pellet density, % TD . . . . .                 | 91                     |
| Cladding OD, mm . . . . .                      | 7.163                  |
| Cladding ID, mm . . . . .                      | 6.198                  |
| Cladding material . . . . .                    | 20% CW 316 SS          |
| Linear power rating, W/cm . . . . .            | 410                    |
| Cladding mid-wall temperature, °C . . . . .    | 700                    |
| EOL burnup, MWd/kg metal . . . . .             | 150                    |
| Time of irradiation, hr . . . . .              | 23,000                 |
| Fluence rate, n/cm <sup>2</sup> -sec . . . . . | $2.2 \times 10^{15}$   |
| Total fluence, n/cm <sup>2</sup> . . . . .     | $1.822 \times 10^{23}$ |

The calculation of the EOL internal gas pressure that would result in a cladding strain of 0.02% (0.0002 mm) was performed by integrating, with respect to time, the in-pile creep rate expression for 20% cold-worked

Type 316 stainless steel and assuming the pressure buildup to be linear over the time span of 23,000 hr. The total EOL internal gas pressure at power that would produce the above strain is  $1.4 \times 10^6$  Pa, which includes a  $4.8 \times 10^5$  Pa allowance for the external coolant pressure on the outside of the rod.

The time at which fuel-cladding contact occurs was determined by applying the LIFE-III code and adding a fuel diametral ratchet.

The pellet diameter increase due to first-cycle diametral ratchet was calculated using an expression derived from a fitting of experimental data gained in irradiations of light-water reactor fuels. The first-cycle fuel diametral ratchet was calculated to be 0.0109 mm. The time at which the diametral gap is 0.0102 mm (excluding fuel ratchet) was then determined, by a LIFE-III run, to be 2,800 hr.

#### 14.2. DESIGN CRITERIA

A set of structural design criteria, consisting of limits on cladding stress, strains, and displacements, is being assembled under this subtask for fuel rods and blanket rods. Failure modes for all possible loading conditions will be identified.

Work continued on the review and evaluation of the General Electric report, "Preliminary Draft of RDT Standard Structural Design Criteria for Core Components." A meeting of the Nuclear Systems Materials Handbook Committee at Richland, Washington, was attended to review current data on irradiation creep, swelling, thermal creep, irradiation affected tensile properties, and stress rupture. Material property correlations and application methods were collected and are being reviewed for possible incorporation in the GCFR design criteria.

Available information on the alloy PE-16 was assembled and evaluated by comparison with cold-worked 316 stainless steel. The higher ductility loss and lower swelling characteristics of PE-16 are being considered as possible criteria items.

#### 14.3. FUEL-ROD OPERATING LIMITS

Plant system parameter limits on steady-state and transient operation

are being formulated under this subtask. Both postulated accidents and continued-operation transients will be identified as well as their effect on the fuel cladding structural stability. From the known material limits and the design criteria available, analysis methods will be applied to provide equivalent limits on fuel-rod power levels.

Work continued on identifying transients and postulated accident conditions by reviewing LMFBR documents. The type of transient conditions considered, the analysis methods used, the observed and predicted fuel-rod performance, and materials characteristics under reactor conditions are being reviewed.

#### 14.4. SURVEILLANCE OF OTHER PROGRAMS FOR FUEL-, BLANKET-, AND CONTROL-ROD TECHNOLOGY

The purpose of this subtask is to support the rod analysis, development, and design tasks by providing through surveillance the information and data that are available from the LMFBR program that can be used in the GCFR design and then to determine what the additional requirements for information are to provide guidance for planning of supplemental tests where the GCFR fuel, blanket, and control materials differ from those of the LMFBR or where the environment is different.

GCFR personnel are participating in the AEC-sponsored Program on Fuel-Cladding Chemical Interaction. As a part of this program, an effort is being made to develop methods for the uniform measurement and reporting of depth of cladding attack. The problem has been studied at GA and recommendations are being formulated.

#### 14.5. GCFR FUEL-ROD TEST PROGRAM REQUIREMENTS

The purpose of this subtask is to maintain a continual surveillance of GCFR irradiation requirements and available data from all irradiations programs and to ensure that the GCFR irradiation schedule will provide the supplementary irradiation test data required. Also, justifications for tests, test schedules, and requests for approvals-in-principle will be prepared.

In this quarterly period, a conference between GA, ANL, and RRD was held at AEC headquarters to review the GCFR experimental irradiation

program and to define the priority of near-term experiments. The GCFR design criteria for the fuel rod and fuel assembly were reviewed by GA, and a tentative schedule for GCFR fuel-, blanket-, and control-rod irradiation programs was presented. Five near-term candidate irradiation tests had been conceived and were presented along with the priorities and justifications. These, in order of priorities, are:

1. Irradiation of GCFR prototypical-design fuel rods,
2. Fast-flux vented rod experiment (FGB-11),
3. Blanket-rod irradiations,
4. Power-to-melt tests, and
5. High-pressure fuel rod tests.

It was agreed that the first priority for FY-76 should be the GCFR prototypical-design irradiation test. ANL will review the effects of high beginning-of-life gas pressure on sintering and fission-gas release mechanisms. GA will reassess the need for the fast-flux vented rod experiment in the light of similar data already gained from thermal-flux irradiations. Proposed power-to-melt test requirements will also be reassessed by GA. Future priorities will be established based on these reviews.

The objectives of the GCFR prototypical-design irradiation test (F-5) have been defined as follows:

1. Investigate the dominant mode of failure and/or demonstrate high reliability to reference burnup under simulated reference GCFR operating conditions.
2. For likely high-probability modes of failure, incorporate design alternatives to achieve equivalent or better performance.
3. Obtain reliability data.

The test is expected to consist of one 37-rod subassembly or two 19-rod subassemblies. It will be divided into a primary and a secondary grouping. The primary grouping will consist of rods designed using the best current information and will be subjected to a planned step change in power. This group is expected to consist of about 20 rods. The secondary grouping would introduce changes in areas of greatest uncertainty and is expected

to consist of three subgroups of about 6 rods each. The candidates for these subgroups are:

1. No step change in power.
2. Variation in fuel-blanket interface design.
3. Low-enrichment pellets in part of the fuel.
4. Annealed cladding to simulate swelling.

The transport of the volatile fission products to the fuel-blanket interface and the associated cladding strain due to the interaction of cesium with the blanket pellets is considered to be a very important potential failure mechanism. For this reason, the fuel-blanket interface design variation has the highest priority. Next in priority is no step change in power. The third subgroup will be selected from the remaining two candidates.

The primary grouping, which will undergo the step change in power operation, is required to simulate the rotation of the fuel assemblies in the GCFR. Rotation of the fuel assemblies results in step changes in power for the fuel rods because of the power tilt existing in the fuel assemblies. It is anticipated that rotation will occur during refueling, which is on an annual basis. The maximum number of rotations in the life-time of a fuel assembly in the GCFR is two. Both low-to-high and high-to-low step changes in power will occur, depending on the position of the rod in the fuel assembly. Only one low-to-high-power step change associated with the fuel assembly rotation will occur during the life of the fuel assembly. This change from low-to-high power is considered to be the most likely to have any significant influence on the performance of the assembly.

The maximum design burnup for a GCFR fuel rod is 100 MWd/kg, but this burnup will be attained only by the lead rods in assemblies with small power tilts. Lead rods in assemblies with the maximum power tilts will operate up to a maximum linear power rating (including hot-spot factors) of 410 W/cm and will accumulate burnups of ~33 MWd/kg during an annual cycle. Some of the other rods in the same assemblies will operate at 60% of the maximum rating and accumulate burnups of ~20 MWd/kg during the annual cycle. Rotation of these maximum-power-tilt assemblies then subjects the

rods operating at 60% power to the maximum low-to-high step change in power operation. From these considerations, the following step changes in power sequences are anticipated in different GCFR fuel assemblies:

1. Full power operation to a maximum burnup of  $\sim 33$  MWd/kg, followed by operation at 60% of full power for an additional burnup of  $\sim 20$  MWd/kg, and then full-power operation to a total burnup of 86 MWd/kg. These assemblies do not accumulate a maximum burnup of 100 MWd/kg because they are situated in the core where a flux gradient exists and they operate at a lower average power.
2. Operation at 60% of full power to a burnup of  $\sim 20$  MWd/kg, followed by full-power operation to an additional burnup of 33 MW/kg, and then operation at 60% of full power to a total burnup of  $\sim 73$  MWd/kg.
3. Operation at 60% of full power to a burnup of  $\sim 40$  MWd/kg (2 cycles) and then full-power operation to a total burnup of  $\sim 73$  MWd/kg. This sequence of operation (not the reference design) is being considered since the total permanent distortion at the end of the first cycle is expected to be minimal, eliminating the need of rotation at the end of the first cycle.

One of these three sequences of operation will be simulated in the irradiation test F-5. By following the first sequence, the low-to-high step change in power takes place at  $\sim 53$  MWd/kg. At that time, most of the cladding ductility is lost, fission-product attack on the cladding would have probably occurred, and the fuel and cladding would have passed the point of hard contact, reducing the possibility of a severe fuel-cladding mechanical interaction.

The second and third operational sequences require the fuel rods to be operated at low power (240 W/cm) up to the low-to-high step change in power operation at  $\sim 20$  or 40 MWd/kg. In the GCFR, the low-powered fuel rods will operate at lower fuel and cladding temperatures. This will probably lead to greater cladding and fuel swelling. Fission-product cladding attack is expected to be minimal, and the cladding would have essentially lost most of its ductility.

The total irradiation time required for simulation of the first operational sequence can be minimized by limiting the low-power operation step to  $\sim 2$  MWd/kg (500 hr). The 500-hr operation is considered sufficient for restructuring and relocation of the fuel at lower power (240 W/cm). It is not possible to simulate the second and third operational sequences and effect similar reductions in the total irradiation time. From these considerations, it was decided to simulate the first operational sequence in the irradiation test with the short ( $\sim 500$  hr) low-power irradiation prior to making the step change to the previous high power.

The following specific design parameters and operating requirements for the F-5 irradiation test have been formulated, based on the conceptual design of the 300-MW(e) GCFR demonstration plant, and have been forwarded to ANL:

1. The fuel, axial blanket, and cladding parameters will be essentially the same as the principal design data for the GCFR fuel rod, but are subject to review.
2. The rods will be unencapsulated.
3. There will be no contact with the outer surface of the cladding in the active region.
4. The rods will have fission-product traps containing activated charcoal.
5. The experimental position will be as close to the center of the core as possible to achieve the highest possible fluence and to aid in the step change in power operation.
6. The maximum linear power rating will be 410 W/cm.
7. The maximum cladding midwall temperature will be  $680^{\circ}\text{C} \pm 20^{\circ}\text{C}$ . (Uncertainties in the maximum midwall cladding temperatures will be subject to review after the experiment is designed.)
8. The maximum burnup will be 150 MWd/kg (16.5 at-%).
9. The end-of-life pressure in the plenum will be  $1.4 \times 10^6$  Pa. This is based on a creep strain value due to fission-gas pressure not to exceed 10% of the creep strain limit, which is 0.2%.

To achieve this end-of-life pressure limit, large plena or possibly venting (by puncturing) and resealing during interim examination can be considered.

10. The number of interim examinations will be two.
11. The step-change-in-power sequence will be full-power operation to 50 MWd/kg, operation at 60% full power for  $\sim 2.0$  MWd/kg, and then full-power operation to maximum burnup.
12. The number of spare rods is yet to be determined.
13. No attempt should be made to simulate GCFR axial power profile in the primary grouping. However, simulation can be considered for one of the secondary groupings.
14. Bottom plena should be considered.

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