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HTGR FUELS AND CORE DEVELOPMENT PROGRAM

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
FEBRUARY 28, 1978

Prepared under
Contract EY-76-C-03-0167
Project Agreement No. 65
for the San Francisco Operations Office
Department of Energy

DATE PUBLISHED: MARCH 1978

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ABSTRACT

This publication continues the quarterly report series on HTGR Fuels and Core Development. The tasks previously covered (fuel development, graphite development, and fission product/coolant chemistry) are now part of the HTGR Generic Technology Program, which also includes reactor core engineering reported here. The primary purpose of the HTGR Generic Technology Program is to develop, in a time frame consistent with application to specific HTGR plants, the HTGR technology base and design definition of nuclear components and systems which are common to HTGR reactor types.

The work documented here includes the design, analysis, and testing of the reactor core and its components comprising the fuel elements, hexagonal reflector elements, plenum elements, neutron sources, control rods, and reserve shutdown material. Also included are studies of reactions between core materials and coolant impurities, basic fission product transport mechanisms, core graphite development and testing, the development and testing of recyclable fuel systems, and physics and fuel management studies. Materials studies include irradiation capsule tests of both fuel and graphite. Experimental procedures and results are discussed and, where appropriate, the data are presented in tables, graphs, and photographs. More detailed descriptions of experimental work are presented in topical reports.

INTRODUCTION

This report covers the work performed by the General Atomic Company under four program areas of the High-Temperature Gas-Cooled Reactor (HTGR) Generic Technology Program sponsored by the U.S. Department of Energy under Contract EY-76-C-03-0167, Project Agreement No. 65. This Project Agreement calls for development of the HTGR technology base and design definition of nuclear components and systems which are common to HTGR reactor types.

Characteristics of large HTGR designs include:

1. A single-phase gas coolant allowing generation of high-temperature, high-pressure steam with consequent high-efficiency energy conversion and low thermal discharge.
2. A prestressed concrete reactor vessel (PCRv) offering advantages in field construction, primary system integrity, and stressed member inspectability.
3. Graphite core material assuring high-temperature structural strength, large temperature safety margins, and good neutron economy.
4. Thorium fuel cycle leading to U-233 fuel which allows good utilization of nuclear resources and minimum demands on separative work.

These basic features are incorporated into the 330-MW(e) prototype Fort St. Vrain reactor which is currently undergoing prestartup testing.

Work under the program areas of HTGR reactor core engineering, fuel development, graphite development, and fission product/coolant chemistry are included in this report.

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1. REACTOR CORE ENGINEERING
189a NO. 00794; PROGRAM AREA 9

FUEL METHODS

Advanced FIPER Development

Introduction

A year ago, sporadic numerical problems, such as non-convergence or roundoff errors, were being encountered with most of the fission product diffusion codes in use at General Atomic. The first 6 months of the past year were spent overhauling the numerics of the COPAR (Ref. 1-1), TRAFIC (Ref. 1-2), and FIPERQ (Ref. 1-3) programs. Due to these problems, and also as a result of experience by users during the Code Validation Program, it was generally agreed that some new diffusion codes should be developed. The motivation for the new development was:

1. Different physical processes, such as multiple species interaction and multiple path transport, needed to be modeled.
2. More reliable numerical methods were needed. In particular, methods that were purposely designed to solve nonlinear problems efficiently were needed.
3. It was not feasible to implement the above as modifications to existing codes, which were already badly cluttered.

The first of the new diffusion codes to be developed is temporarily called "Advanced FIPER." This will be a multiple species, multiple path,

nonlinear, finite difference code. It has been designed to replace FIPERQ. The organization of the Advanced FIPER code is being planned so that it will be compatible with other codes that employ analytical solutions, such as TRAFIC and COPAR. Thus, following the initial finite difference development, it is planned to incorporate into this framework some analytical solution modules and thereby derive advanced versions of COPAR and TRAFIC.

Work on the task in the last 5 months^{*} has been spent largely in planning and organizing the Advanced FIPER code. This has included defining the problem, some numerical studies, and some of the detailed derivational work. As a result of this work the concept is now firmly established. The following sections describe that concept.

Statement of Problem

The objective of the Advanced FIPER code is to calculate the space and time-dependent transport of fission products in a one-dimensional, inhomogeneous medium. The geometrical arrangement will be a sequence of NB material regions, or "branches," separated by either gap or solid interfaces, as shown below.

^{*} Since this is the first report prepared under the new HTGR Generic Technology Program, the reporting period covered is around 5 months, beginning with the first quarter in FY-78 (October 1, 1977).

The radial (r) direction is the one-dimensional spatial coordinate of the problem. Most variables will vary continuously as a function of r and are assumed to be independent of the axial coordinate z. However, certain variables will also be permitted to vary slowly in the z direction, and this z-dependence will be accounted for in an approximate fashion by an "axial stacking" or quasi two-dimensional procedure. The coordinate system for each material region of the problem will be either slab, cylindrical, or spherical geometry. The z coordinate will be considered only for slabs and cylinders.

The problem is formulated mathematically as a nonlinear system of coupled diffusion/convection equations. The model will consider the nuclear coupling of NN interacting nuclides. In addition, the transport will be along NP coupled paths. The number of paths and their physical nature will be optional and controlled by the user. The following three possibilities serve as examples:

1. Single solid path (Fickian diffusion) with solid/gas boundary conditions.
2. Two-path model of L. Zumwalt (Ref. 1-4) (grains and grain boundaries) with solid/gas boundary conditions.
3. Three-path model proposed by W. Hudritsch (Ref. 1-5) (grains, grain boundaries, and pores). The model would consider convection through pores, internal kinetic vapor pressure relationships, and gas phase continuity at boundaries.

The transport of metallic fission products through graphite is governed by uncertain processes, but since at least two parallel paths are apparently involved, a model with this generality will be useful both as a research tool (with various models) and as a design tool (with a reference model).

The central governing equations for this problem are a system of N coupled partial differential equations in N unknowns, where N is the product of NN , the number of nuclides, and NP , the number of paths. A typical equation is

$$\frac{\partial C_{ik}}{\partial t} = S_{ik} + \sum_{j=1}^{NN} A_{ijk} C_{jk} + K_{ik_0} + \sum_{\ell=1}^{NP} K_{ik\ell} C_{i\ell} - \vec{\nabla} \cdot \vec{J}_{ik} \quad , \quad (1-1)$$

where C_{ik} = partial concentration of nuclide i in path k
(atoms/cm³),

S_{ik} = direct source rate of C_{ik} ,

A_{ijk} = an element of the depletion matrix, which couples the nuclides,

$K_{ik\ell}$ = an element of the transfer matrix, which couples the paths,

K_{ik_0} = an extra term due to linearizing the path coupling equations,

$\vec{J}_{ik}$ = mass flux (atoms/cm²-g), and

$\vec{\nabla}$ = vector divergence operator.

In addition various boundary and interface conditions must be satisfied, which will be considered later.

The mass flux $\vec{J}$ in Eq. 1-1 is caused by both diffusion and convection processes. It will be assumed that

$$\vec{J}_{ik} = -D_{ik} \left\{ \vec{\nabla} C_{ik} + \left[\frac{E_{ik}}{RT^2} (\vec{\nabla} T) \right] C_{ik} \right\} + \vec{v}_{ik} C_{ik} \quad , \quad (1-2)$$

where D_{ik} = diffusion coefficient of species i on path k (cm^2/s),
 $\vec{\nabla}C_{ik}$ = concentration gradient (atoms/cm^4),
 E_{ik} = "heat of transport" for thermal diffusion ($\text{cal}/\text{g mol}$),
 R = ideal gas constant ($\approx 1.986 \text{ cal}/\text{g mol K}$),
 T = temperature (K),
 $\vec{\nabla}T$ = temperature gradient (K/cm), and
 $\vec{V}$ = velocity (cm/s).

Equations 1-1 and 1-2 can be combined, and, after a suitable manipulation of terms, they can ultimately be reduced to the following vector system of coupled partial differential equations:

$$\left\{ \dot{C} \right\} = \left\{ S^* \right\} + \left[D^* \right] \left\{ \frac{\partial^2 C}{\partial r^2} \right\} + \left[V^* \right] \left\{ \frac{\partial C}{\partial r} \right\} + \left[A^* \right] \left\{ C \right\} \quad . \quad (1-3)$$

Here the length of the vector C is N , the total number of unknowns at any given space point. It can be shown that the $N \times N$ matrices D^* and V^* are diagonal matrices, whereas the matrix A^* is a full matrix that couples all paths and nuclides. To this point nothing has been mentioned about the functional dependence of the coefficients in Eq. 1-3. In general, S^* , D^* , V^* , and A^* will all be functions of spatial position, r , and time, t . It is also possible for the coefficients to be functions of C , making the system nonlinear.

The interface gaps usually contain axial coolant flows, and in general there may be mass-transfer from or into the coolant at both boundaries of any material region. Closed gaps, i.e., a solid-solid interface, will be special cases of this general situation. The interface conditions will be expressed in a general nonlinear form,

$$g(C^-, J^-, J^+, C^+) = 0 \quad , \quad (1-4)$$

where - and + indicate conditions on either side of a gap. Equation 1-4 can consider a number of simultaneous phenomena such as adsorption, mass transfer through boundary layers, axial convection, kinetic resistance of the fluid in the gap, etc. By analogy, the conditions at an external boundary can be expressed as

$$\begin{aligned} b^-(C^-, J^-) &= 0, \\ \text{or} & \\ b^+(C^+, J^+) &= 0. \end{aligned} \tag{1-5}$$

The solution method presented in the following section was specifically designed to take all these nonlinearities into account, including the boundary and interface conditions and those in the interior of material regions.

Solution Method

The solution method planned for the Advanced FIPER code was originally developed by Wilson et al. (Ref. 1-6) for a finite element application. The method is extended here to a finite difference application. The objective of the method is to solve a nonlinear problem as efficiently as possible. In addition, a substructuring technique is introduced which facilitates the organization of the program storage and logic. A summary of the method is given below.

Spatial Discretization - Vector Notation. It will be found that the formalism of applying Newton's method to solve a nonlinear system of equations is easier to develop if a vector notation is immediately adopted. The first step in this process is to discretize the problem spatially. Using finite differences to approximate the spatial derivatives, the partial differential equation (1-3) can be written separately for each space point (m) of a material region. The result is

$$\left\{ \dot{C}_m \right\} = \left\{ d_m (C_{m-1}, C_m, C_{m+1}) \right\}, \tag{1-6}$$

where d_m is the finite difference approximation for the continuous equation,

$$\left\{ d_m(C) \right\} \equiv \left\{ S_m^* \right\} + \left[D_m^* \right] \left\{ \frac{\partial^2 C}{\partial r_m^2} \right\} + \left[V_m^* \right] \left\{ \frac{\partial C}{\partial r_m} \right\} + \left[A_m^* \right] \left\{ C_m \right\} \quad . \quad (1-7)$$

Note that Eqs. 1-6 and 1-7 each contain $N = NN \times NP$ unknowns.

Equation 1-6 is now repeated sequentially for each of the NS space points in a typical branch, ib . Condensing the notation and eliminating brackets, the equations for a branch can be written as

$$\dot{C}_{ib} = d_{ib} \left(C_o, C_{ib}, C_{NS+1} \right) \quad , \quad (1-8)$$

where C_{ib} is the vector of unknowns associated with the set of all space points in a branch. The extra unknowns, C_o and C_{NS+1} , appear in Eq. 1-8 as a consequence of the finite difference approximations. These can be eliminated in favor of mass fluxes, which are more natural for expressing the boundary conditions. The result is

$$\dot{C}_{ib} = d_{ib} \left(J_{ib}^-, C_{ib}, J_{ib}^+ \right) \quad . \quad (1-9)$$

Equation 1-9 is now repeated sequentially for each branch in turn, and the branch equations are alternated with interface conditions (1-4) and boundary conditions (1-5) wherever appropriate. The final, complete system of equations takes the form

$$\begin{Bmatrix} 0 \\ \dot{C}_1 \\ 0 \\ \dot{C}_2 \\ \vdots \\ \dot{C}_{NB} \\ 0 \end{Bmatrix} = \begin{Bmatrix} b_1(C) \\ d_1(C) \\ g_{12}(C) \\ d_2(C) \\ \vdots \\ d_{NB}(C) \\ b_{NB}(C) \end{Bmatrix} . \quad (1-10)$$

This can again be condensed and written in the form

$$\left\{ \dot{C} \right\} = \left\{ f(C) \right\} . \quad (1-11)$$

The following important points should be noted about Eq. 1-11:

1. The vector of unknown C includes not only all concentrations but also the mass fluxes at each branch boundary.
2. The vector $\dot{C}$ contains zero entries because the interface and boundary conditions are steady-state equations.
3. There are now the same number of unknowns as equations.
4. The vector function $f(C)$ can be evaluated one space point at a time using the exact nonlinear relationships in Eq. 1-7. Finite difference approximations are used for the local derivatives, but it is not necessary to construct an implicit spatial coupling matrix to evaluate f .

In this section we have spatially discretized the equations and reduced a system of partial differential equations (1-3) to a system of nonlinear ordinary differential equations (1-11). The boundary and interface conditions have been included. The object is now to solve this system by time integration.

Time Discretization and Integration. Beginning with known initial conditions, the object is to integrate Eq. 1-11 and obtain the solution C as a function of time. To do this, the time history is discretized as a sequence of finite steps, and the integration is performed incrementally. This section considers the integration in a typical time step. The object now is to evaluate

$$\left\{ C \right\}_{n+1} = \left\{ C \right\}_n + \int_{t_n}^{t_{n+1}} \left\{ f(C) \right\} dt \quad . \quad (1-12)$$

The method employed will be an implicit method that is a generalization of the Crank Nicholson method. An "equilibrium iteration" or "residual force correction" procedure will be used to correct for nonlinearities and ensure that the state equations (1-11) are satisfied identically at the end of each step.

The algorithm is derived by assuming a one-parameter interpolation function for $\dot{C}(r,t)$, i.e., it is assumed that for $t_n \leq t \leq t_{n+1}$

$$\left\{ \dot{C}(t) \right\} = [1 - p(t)] \left\{ \dot{C}(t_n) \right\} + p(t) \left\{ \dot{C}(t_{n+1}) \right\} \quad , \quad (1-13)$$

with

$$\begin{aligned} p(t_n) &= 0 \quad , \\ p(t_{n+1}) &= 1 \quad . \end{aligned}$$

For any given $p(t)$, the integration of Eq. 1-13 can be performed exactly, with the result

$$\left\{C\right\}_{n+1} = \left\{C\right\}_n + (1 - \theta) \Delta t \left\{\dot{C}\right\}_n + \theta \Delta t \left\{\dot{C}\right\}_{n+1} \quad , \quad (1-14)$$

where θ is an integration parameter ($0 \leq \theta \leq 1$). The integration is implicit because $\dot{C}_{n+1}$ is unknown, and it is nonlinear because, through Eq. 1-11, $\dot{C}$ depends nonlinearly on C .

The calculation now proceeds in two phases. The first phase is an extrapolation or "predictor," used to obtain a first guess for the increment of C and $\dot{C}$ in a time step. The next phase is the equilibrium iteration or "corrector," used to correct for nonlinearity.

For the predictor phase, $\dot{C}$ is expanded in a Taylor series about the known converged value at the end of the previous step. Thus, it is assumed that

$$\left\{\dot{C}\right\}_{n+1}^0 = \left\{\dot{C}\right\}_n^\infty + \left[\frac{\partial f}{\partial c}\right]_n^\infty \left(\left\{C\right\}_{n+1}^0 - \left\{C\right\}_n^\infty \right) \quad . \quad (1-15)$$

Here the superscript is an iteration counter, with 0 being the first guess and ∞ denoting convergence. The matrix,

$$[T] \equiv \left[\frac{\partial f}{\partial c}\right] \quad (1-16)$$

is the Jacobian or tangent matrix of the rate equations (Eq. 1-11). Substituting Eq. 1-15 into Eq. 1-14 and collecting terms, we can solve for the unknown, C_{n+1} . The final result is

$$\left\{C\right\}_{n+1}^0 = \left\{C\right\}_n^\infty + \left[I - \theta \Delta t T_n^\infty \right]^{-1} \left\{\dot{C}\right\}_n^\infty \Delta t \quad . \quad (1-17)$$

Substituting this result back into Eq. 1-14, the first guess at $\dot{C}_{n+1}$ is

$$\left\{ \dot{C} \right\}_{n+1}^0 = \frac{1}{\theta \Delta t} \left(\left\{ C \right\}_{n+1}^0 - \left\{ C \right\}_n^\infty \right) - \left(\frac{1 - \theta}{\theta} \right) \left\{ \dot{C} \right\}_n^\infty . \quad (1-18)$$

For the corrector phase, $\dot{C}$ is expanded in a Taylor series about the most recent iterate. Employing an analogous procedure to the above, it can be shown that the corrections are given by

$$\left\{ C \right\}_{n+1}^{i+1} = \left\{ C \right\}_{n+1}^i + \left[I - \theta \Delta t T_{n+1}^i \right]^{-1} \left(\left\{ f \left(C_{n+1}^i \right) \right\} - \left\{ \dot{C} \right\}_{n+1}^i \right) \theta \Delta t . \quad (1-19)$$

Furthermore, with the aid of Eq. 1-18, the corrections in $\dot{C}$ are given by

$$\left\{ \dot{C} \right\}_{n+1}^{i+1} = \left\{ \dot{C} \right\}_{n+1}^i + \frac{1}{\theta \Delta t} \left(\left\{ C \right\}_{n+1}^{i+1} - \left\{ C \right\}_{n+1}^i \right) . \quad (1-20)$$

Note that the right-hand side of Eq. 1-19 contains the term

$$\left\{ R \right\}^i \equiv \left\{ f \left(C_{n+1}^i \right) \right\} - \left\{ \dot{C} \right\}_{n+1}^i . \quad (1-21)$$

This is the residual (or error) of failing to satisfy Eq. 1-11 at iteration i . As the iteration converges, the error vector R goes to zero. This key fact is the essence of the method, and from it one can draw several conclusions:

1. Equation 1-19 is exactly equivalent to Newton's method. It was derived by the same procedure.

2. The accuracy of convergence of Eq. 1-19 is controlled only by the accuracy with which the residual R is evaluated.
3. The method will still converge if the Crank Nicholson tangent matrix,

$$\left[I - \theta \Delta T_{n+1}^1 \right] ,$$

is inaccurate. Thus, one can replace this matrix by the constant matrix,

$$\left[I - \theta \Delta t T_n^\infty \right] ,$$

that was used in Eq. 1-17. The use of a constant matrix (modified Newton's method) may result in more iterations, but it has the possibility of considerably reducing calculational effort.

4. The residual R can form the basis of an alternative convergence criterion. The difference of successive C iterates may not be sufficient.

Many other nonlinear solution algorithms are available, but most of them depend on accurate evaluation of the matrix. With the present method, the only error is truncation error in the time integration, and the nonlinearities can be converged exactly with a minimum of effort.

Substructuring. A further improvement in efficiency can be achieved by a technique known as "substructuring." The procedure is outlined below.

1. For each branch in turn, the interior unknowns are eliminated algebraically, and a reduced branch equation involving only boundary unknowns is obtained.

2. The global system of equations (analogous to Eq. 1-10) now contains only boundary unknowns and the system can be solved.
3. Given the boundary unknowns, then for each branch in turn one can back substitute in the original equations for the interior unknowns.

This method has a distinct advantage when the interior is linear and the boundary conditions are nonlinear (the usual case at this time). Then the reduction needs to be done only once, and all iteration can be done with the reduced system. Back substitution is not required until the boundary conditions have converged.

Even for fully linear or fully nonlinear problems, where there is no saving of calculational effort, this method has distinct advantages in terms of logical organization and computer storage.

Linearization. The tangent matrix T defined by Eq. 1-16 can be calculated in a number of ways. The way selected for the Advanced FIPER is as follows:

1. The nonlinear differential equation (1-3) is first linearized. The technique for doing this is to expand the equation in a Taylor series, assuming that

$$\left\{ \frac{\partial^2 C}{\partial r^2} \right\}, \quad \left\{ \frac{\partial C}{\partial r} \right\}, \quad \text{and} \quad \{C\}$$

are independent variables. When this is done and like terms are collected, one obtains the linearized equation,

$$\{\dot{C}\} = \{S^{**}\} + [D^{**}] \left\{ \frac{\partial^2 C}{\partial r^2} \right\} + [V^{**}] \left\{ \frac{\partial C}{\partial r} \right\} + [A^{**}] \{C\} \quad . \quad (1-22)$$

2. The linearized equation is spatially discretized by a standard finite difference procedure. For a typical branch ib one obtains

$$\left\{ \dot{C} \right\}_{ib} \approx \left\{ F \right\}_{ib} + \left[\frac{\partial d}{\partial C} \right]_{ib} \left\{ C \right\}_{ib} \quad . \quad (1-23)$$

The matrix $(\partial d / \partial C)$ in Eq. 1-23 is one block of the tangent matrix T . Note that it is not a square matrix.

3. All of the branches, interspersed with interface and boundary conditions, are similarly linearized, and the corresponding tangent blocks are assembled into the global matrix.

Conclusions

The preceding sections have given a statement of the problem and a summary of the solution method to be employed in the Advanced FIPER code. An important feature of this code is that the problem formulation and the solution method are intended to be quite general and capable of easy adaptation to a number of physical models. The concept of the model is now firm, but a number of details remain to be worked out. Actual programming is scheduled to begin in April 1978. The task is scheduled for completion by the end of September 1978.

Hyperfine Mesh Power Distributions

The calculation of the detailed temperature and stress distribution in the fuel blocks of an HTGR requires that a detailed calculation be made to determine the neutron flux and power distribution. Since it is impractical to calculate such a detailed power distribution throughout the core, approximate methods are needed.

In the past, a superposition technique had been employed to develop these approximations; however, it proved to be unwieldy to use and difficult to apply with confidence. To remedy this situation, a new technique has been developed and implemented for use in HTGR design.

The essence of the technique is the use of fluxes from a coarse mesh calculation of the entire reactor as boundary fluxes for the solution of a very detailed model of the fuel block. This approach ensures that the approximation is as consistent as possible with the results of the full core calculation. Additionally it avoids the numerical difficulties associated with the use of negative current-to-flux ratios when logarithmic boundary conditions, rather than fluxes, are used to close the set of diffusion equations.

The initial testing of the method has been completed and production use is expected to begin shortly.

CORE DESIGN STUDIES

Summary

Preliminary design studies for the Large HTGR (LHTGR) core have concentrated on definition of its basic parameters and an initial estimate of expected behavior with the non-proliferation fuel cycle. This cycle, termed the MEU/Th cycle, consists of uranium enriched to 20% in U-235 as the fissile material plus thorium as the fertile material.

Several core layouts in the 800 to 900 MW(e) range were studied and a layout was selected which provided the required plant output [880 MW(e)] at a core power density similar to that for the original LHTGR, i.e., 7.1 W/cm^3 . Minor modifications were made to the boundary regions of this layout, which resulted in a reduction of one foot in the PCRV diameter and height without increasing core power density.

Plutonium inventories for an MEU/Th cycle and fuel particle burnups were estimated for design purposes. Preliminary estimates of core temperature coefficients were also made. A preliminary evaluation of core shutdown margins was also made to ensure that shutdown criteria could be met with this layout.

Initial estimates of expected fission product inventories with MEU/Th fueled core were also made to provide preliminary data for plant scoping studies particularly in the area of shielding requirements for fuel handling and shipping and for primary loop plateout studies.

An investigation is under way of the applicability of a nonlinear stress-strain model to the prediction of stresses in graphite fuel elements. The goal is to provide a method for predicting peak stresses in graphite fuel elements which avoids the excessive conservatism of the present linear model and thus provides a more economic core design with no reduction in safety margins.

Stress distribution, rock-up strength, and dynamic strength tests were performed on four- and five-dowel systems.

LHTGR Conceptual Core Layout

The proposed core layout for the conceptual LHTGR design is shown in Fig. 1-1. The number of fuel columns, control rod drives, etc., are also summarized in Fig. 1-1. This layout is optimum in that it provides the required core thermal power at one acceptable power density, and the core-reflector boundary has been adjusted to minimize PCRV diameter and height. The core power level for this layout is 2240 MW(t).

The control rod drive (CRD) assemblies for use in the reflector are equipped only with a control rod pair and drive and a reserve shutdown system unit. They do not include a power rod or flow control valve. The

Fig. 1-1. Core layout for conceptual LHTGR

need for a reserve shutdown system unit with each of these six reflector CRD assemblies is a consequence of the current criteria in GASSAR-6 (Ref. 1-7).

Figure 1-1 also shows the location of the six reflector control rod pairs. Note that these CRDs are uniformly distributed around the core with one exception: the CRD which would have been located in region 65 is located in region 64. Region 65 would have been a slightly more preferable location since the CRD would then have been closer to the unrodded two-column region, i.e., region 64. On the other hand, having a CRD in region 65 would have resulted in considerable nuclear steam supply piping complications (Ref. 1-8) and would not have significantly improved the calculated shutdown margin. Since this occurs at only one location, the impact, if any, on plant availability should be minimal.

Core arrangement drawings and component tabulations have been prepared for the reference 2240-MW(t) core layout.

Plutonium Inventories in MEU-Fueled LHTGR Core

Plutonium inventories to be expected in a LHTGR using the MEU/Th fuel cycle were estimated to provide data for accident analyses and for calculations on core fission product inventories. The estimated masses of the plutonium isotopes in a LHTGR [3360 MW(t)] core fueled with 20% enriched uranium are given in Tables 1-1 through 1-3. The tables summarize the masses of the plutonium isotopes in each type of fuel particle, i.e., fertile particle (ThO_2), fissile (U-235) particle, and the recycle (U-233) particle, and in each fuel segment. The data are for the end of reload interval 10 and are thus near equilibrium end-of-cycle values. While the optimum MEU/Th fuel cycle had not been established at the time these calculations were performed, the results should be indicative of what to expect for an MEU/Th-fueled core. The cycle assumed here had C/Th ratios of 306 and 400 in the initial core and reload segments, respectively, with recycle of bred U-233 commencing at reload 2.

TABLE 1-1
PLUTONIUM CONTENT IN FRESH FISSILE
(U-235) PARTICLES

MEU/Th FUELED LHTGR, RELOAD 10

	Nuclide Weights (g)			
	Segment B 1-Year Old	Segment A 2-Years Old	Segment D 3-Years Old	Segment C 4-Years Old
Pu-236	2.88×10^{-8}	2.65×10^{-7}	7.46×10^{-7}	1.43×10^{-6}
Pu-238	1.55×10^2	9.92×10^2	2.46×10^3	4.51×10^3
Pu-239	2.86×10^4	3.18×10^4	3.11×10^4	3.10×10^4
Pu-240	9.29×10^3	1.40×10^4	1.46×10^4	1.47×10^4
Pu-241	5.17×10^3	1.28×10^4	1.55×10^4	1.63×10^4
Pu-242	7.08×10^2	4.28×10^3	8.91×10^3	1.33×10^4
Pu-243	1.77×10^{-1}	1.07×10^0	2.24×10^0	3.34×10^0
Pu-244	3.79×10^{-3}	5.42×10^{-2}	1.97×10^{-1}	4.43×10^{-1}
Pu-245	1.87×10^{-8}	2.68×10^{-7}	9.75×10^{-7}	2.20×10^{-6}
Pu-246	1.81×10^{-10}	2.96×10^{-9}	1.12×10^{-8}	2.59×10^{-8}

TABLE 1-2
PLUTONIUM CONTENT IN FERTILE (TH-232) PARTICLES

MEU/Th FUELED LHTGR, RELOAD 10

	Nuclide Weights (g)			
	Segment B 1-Year Old	Segment A 2-Years Old	Segment D 3-Years Old	Segment C 4-Years Old
Pu-236	7.25×10^{-13}	3.16×10^{-11}	3.31×10^{-10}	1.54×10^{-09}
Pu-238	1.11×10^{-2}	2.55×10^{-1}	1.30×10^0	5.44×10^0
Pu-239	4.56×10^{-4}	6.23×10^{-3}	7.71×10^{-2}	3.66×10^{-1}
Pu-240	8.59×10^{-4}	1.04×10^{-3}	1.76×10^{-2}	9.83×10^{-2}
Pu-241	4.76×10^{-4}	5.02×10^{-4}	8.70×10^{-3}	5.73×10^{-2}
Pu-242	3.26×10^{-2}	8.43×10^{-2}	7.07×10^{-2}	7.78×10^{-2}
Pu-243	7.89×10^{-6}	2.05×10^{-5}	1.74×10^{-5}	2.01×10^{-5}
Pu-244	3.80×10^{-6}	5.62×10^{-6}	9.76×10^{-6}	1.32×10^{-5}
Pu-245	-	-	-	-
Pu-246	-	-	-	-

TABLE 1-3
PLUTONIUM CONTENT IN RECYCLE FISSIONABLE
(U-233) PARTICLES
MEU/Th FUELED LHTGR, RELOAD 10

	Nuclide Weights (g)			
	Segment B 1-Year Old	Segment A 2-Years Old	Segment D 3-Years Old	Segment C 4-Years Old
Pu-236	1.19×10^{-9}	6.55×10^{-9}	1.69×10^{-8}	3.27×10^{-8}
Pu-238	5.29×10^0	2.25×10^1	5.41×10^1	1.02×10^2
Pu-239	2.59×10^{-1}	1.54×10^0	4.21×10^0	8.49×10^0
Pu-240	4.33×10^{-2}	4.01×10^{-1}	1.32×10^0	2.95×10^0
Pu-241	1.39×10^{-2}	2.15×10^{-1}	8.96×10^{-1}	2.27×10^0
Pu-242	1.86×10^{-3}	2.67×10^{-2}	2.01×10^{-1}	7.55×10^{-1}
Pu-243	1.65×10^{-6}	8.23×10^{-6}	5.14×10^{-5}	1.90×10^{-4}
Pu-244	9.15×10^{-11}	6.62×10^{-7}	1.16×10^{-6}	1.14×10^{-5}
Pu-245	0	2.13×10^{-12}	6.98×10^{-12}	5.73×10^{-11}
Pu-246	0	3.28×10^{-14}	6.18×10^{-14}	6.42×10^{-13}

While there is, of course, significantly more plutonium in an MEU core, it is noted that the Pu-238 inventory in an MEU core is practically the same as in an HEU core. This is because Pu-238 is formed primarily by successive neutron captures starting from U-235, and the U-235 loadings for the MEU and HEU cores are of similar magnitude.

Fraction of Fissions per Nuclide in MEU/Th-Fueled LHTGR

The expected burnup characteristics of the fuel particles in the MEU/Th cycle for the LHTGR must be estimated so that their basic parameters, such as kernel diameter and coating thicknesses, can be evaluated. These parameters in turn form the basis for the detailed core design studies which are used for licensing approvals.

Figures 1-2 and 1-3 present a preliminary estimate of the relative number of fissions occurring in the primary fissile nuclides in the MEU/Th-fueled LHTGR. Figures 1-2a and 1-2b present the data for the initial core and equilibrium core, respectively, for the case of no recycle of bred U-233 or plutonium. Figure 1-3 presents the corresponding data for the equilibrium core assuming recycle. While the MEU/Th fuel cycle has not yet been completely optimized, these data represent the current reference cycles with and without recycle.

LHTGR Temperature Coefficients

LHTGR temperature coefficients have been estimated for an HEU core by modifying existing data from an earlier 2000-MW(t) design. Temperature coefficients for the MEU/Th fuel will be determined later when detailed physics studies are complete. The data were obtained from the TEMCO (Ref. 1-9) computer code which computes temperature coefficients using a one-dimensional core model. All calculations were based on equilibrium core end-of-cycle conditions where the temperature coefficients are the least negative to assure conservative results. A comparison

Fig. 1-2. Preliminary estimate of fractional fissions in primary fissile nuclides in MEU/Th-fueled LHTGR, C/Th = 350/850, no recycle, 3360 MW(t): (a) initial core

Fig. 1-2. Preliminary estimate of fractional fissions in primary fissile nuclides in MEU/Th-fueled LHTGR, C/Th = 350/850. no recycle, 3360 MW(t): (b) equilibrium cycle

Fig. 1-3. Preliminary estimate of fractional fissions in primary fissile nuclides in MEU/Th-fueled LHTGR, C/Th = 350/850, recycle at reload 2, 3360 MW(t), equilibrium cycle

of temperature coefficients shows that the LHTGR fuel temperature coefficients are slightly more negative than those for the 2000-MW(t) plant and the moderator temperature coefficients are slightly more positive. Overall, the sum of the fuel and moderator temperature coefficients was found to be more negative in the LHTGR than in the 2000-MW(t) plant.

The most significant factor affecting the fuel temperature coefficient is the heavier thorium loading found in the LHTGR. It was therefore decided to split the fuel temperature coefficient with respect to k_{∞} into two parts, one part the temperature coefficient due to thorium alone and the other part the temperature coefficient due to all other reactor materials.

$$TC_{\text{total}}^{2000} = TC_{\text{other}}^{2000} + TC_{\text{Th}}^{2000} \quad , \quad (1-24)$$

where $TC = \text{temperature coefficient} = \frac{1}{K} \frac{dK}{dT}$.

The assumption was then made that the thorium contribution to the fuel temperature coefficient was proportional to its atom density in the reactor. Therefore, by multiplying the thorium temperature coefficient in Eq. 1-24 by the ratio of the LHTGR and 2000 MW(t) thorium atom densities and rearranging terms, the LHTGR fuel temperature coefficient with respect to k_{∞} becomes

$$TC_{\text{total}}^{\text{LHTGR}} = TC_{\text{Th}}^{2000} - TC_{\text{Th}}^{2000} \left(1 - \left(\frac{N_{\text{Th}}^{\text{LHTGR}}}{N_{\text{Th}}^{2000}} \right) \right) \quad . \quad (1-25)$$

To correct the above fuel temperature coefficient for the fact that the LHTGR is of finite size, the axial nonleakage (ANL) and radial nonleakage (RNL) probability temperature coefficients for the 2000-MW(t) plant were added to Eq. 1-25 under the assumption that since the 2000-MW(t) plant and LHTGR are similar in size, their respective leakages will be roughly the same.

$$TC_{\text{fuel}}^{\text{LHTGR}} = \frac{1}{k_{\text{eff}}} \frac{dk_{\text{eff}}}{dT} = TC_{\text{total}}^{\text{LHTGR}} + \frac{1}{P_{\text{ANL}}} \frac{dP_{\text{ANL}}}{dT} + \frac{1}{P_{\text{RNL}}} \frac{dP_{\text{RNL}}}{dT} \quad (1-26)$$

The moderator temperature coefficients were then determined by converting 2000-MW(t) isothermal (fuel and moderator at same temperature) temperature coefficients to LHTGR conditions in a manner similar to that described above and then subtracting the LHTGR fuel temperature coefficients.

$$TC_{\text{mod}}^{\text{LHTGR}} = TC_{\text{iso}}^{\text{LHTGR}} - TC_{\text{fuel}}^{\text{LHTGR}} \quad (1-27)$$

Figures 1-4 and 1-5 illustrate the variations in the fuel and moderator temperature coefficients, respectively, with temperature for the LHTGR. By integrating these temperature coefficient curves, the fuel and moderator temperature defects can then be computed (Figs. 1-6 and 1-7).

LHTGR Shutdown Margins

As part of the evaluation of the LHTGR core layout, it was determined that combining the 24 single fuel columns on the core-reflector boundary into 12 two-column regions as shown in Fig. 1-1 would reduce the outer core diameter and hence the PCRV cavity diameter. A net reduction of 0.305 m (1 ft) in both PCRV diameter and height was achieved with this change. However, this change in core layout can affect core shutdown margins when one and two rod pairs are stuck out of the core. As can be seen from Fig. 1-1, the unrodded core area under these circumstances is maximum when the stuck rods are in boundary regions adjacent to the two-column regions. In order to determine the effect of this change in layout, core shutdown margins were estimated based on earlier HEU studies.

Several effects must be considered in estimating the time during a cycle when minimum shutdown occurs. One such effect involves the creation

Fig. 1-4. Fuel temperature coefficient

Fig. 1-5. Moderator temperature coefficient

Fig. 1-6. Fuel temperature defect

Fig. 1-7. Moderator temperature defect

of Pa-233 during reactor operation. Following shutdown, Pa-233 decays radioactively into U-233, resulting in an increase in core reactivity. Other effects influencing the time of minimum shutdown include the depletion of the lumped burnable poison and the decrease in fuel concentration. When all the various effects are considered, it is generally found that minimum shutdown occurs near the middle of cycle.

An examination of k_{eff} versus time indicates that the maximum excess reactivity for the LHTGR occurs at 200 days into the cycle for the initial core and 150 days into the cycle for the equilibrium cores. Reload 6 was chosen as the basis for the equilibrium core studies as this segment has a somewhat higher fissile inventory than the other reload segments. The results indicate that for the equilibrium core it is possible to maintain indefinite shutdown, but only marginally, with the one maximum worth control rod pair stuck out, but not if all uncertainties are included. With two maximum worth control rods stuck out, it is only possible to remain shutdown for less than 2 days. In order to improve this situation, six additional control rod pairs were modeled by GAUGE in the reflector region of the reactor adjacent to the high worth patches in the core. The results for this case indicate that indefinite shutdown can be maintained with the one maximum worth rod stuck out and with uncertainties included. With two maximum worth control rods stuck out, the shutdown time is increased to approximately 1 week. Shutdown margins for the initial core are somewhat worse due to the higher fissile inventory resulting from the long cycle length.

The cross sections used in this study were generated by the INTERP computer code using HEU cross-section data. The self-shielding factors for the various temperatures were also computed using HEU cross-section data following the procedure outlined in Ref. 1-10. Cross sections and self-shielding factors were generated at three temperatures (1005, 377, and 300 K) corresponding to the operating, refueling, and cold shutdown temperatures, respectively.

Results for the initial core at 200 days and for reload 6 at 150 days are presented in Tables 1-4 and 1-5, respectively. For conservatism, all runs were made assuming no power rods were in the core. Table 1-6 presents k_{eff} versus time for one and two maximum worth control rods stuck out with and without the six additional reflector control rods. Again, no credit was taken for the power rods. The maximum worth rod was determined to be in region 69, with the second highest worth rod in region 70. These rods were the ones assumed to be stuck out. The effect of the reflector control rods was found to be very localized. Table 1-7 illustrates the change in k_{eff} rod and the change in k_{eff} resulting from the effects of all the reflector control rods combined.

Estimates of Fission Product Inventories in the MEU/Th LHTGR Core

Interim fission product source terms, including gas-borne activity and plateout, have been generated for the 2240-MW(t) LHTGR. These data were generated with the RADC code in a manner analogous to previous design calculations, with several important exceptions.

1. Since a full-core physics and thermal survey of this core was not available at the time of the analysis, the data were derived, unless otherwise noted, via simple scaling of previous 3427-MW(t) LHTGR numbers; i.e., the fraction $0.67 [(2240)(1.02)^* / 3427]$ was applied to all numbers in the previous work.
2. MEU fuel was assumed to be the fissile material. As such, a preliminary curve of fraction of fissions in fissile (U-235, Pu-239, and Pu-241) and fertile (U-233) material applicable to a 4-year, equilibrium, recycle, MEU core was used (Fig. 1-3). This results in a significant reduction in the fraction of fissions in U-233 for MEU fuel (0.23) versus a similar HEU core (0.60).

* This factor represents a safety margin included in source term calculations.

TABLE 1-4
 Δk FOR LHTGR INITIAL CORE, 200 DAYS,
 NO POWER RODS

Bank worth (1000K)	0.181
Xe worth (300K)	0.031
Pa worth (300K)	0.031

TABLE 1-5
 Δk FOR LHTGR RELOAD 6, 150 DAYS,
 NO POWER RODS

Band worth (1000K)	0.108
Xe worth (300K)	0.029
Pa worth (300K)	0.029

TABLE 1-6
 k_{eff} FOR LHTGR RELOAD 6, 150 DAYS,
 NO POWER RODS, 300K

Time (days)	1 Stuck Rod 0 Refl. Rods	2 Stuck Rods 0 Refl. Rods	1 Stuck Rod 6 Refl. Rods	2 Stuck Rods 6 Refl. Rods
0	0.927	0.957	0.921	0.943
2	0.959	0.991	0.953	0.976
14	0.967	0.999	0.961	0.984
28	0.973	1.006	0.967	0.990
140	0.987	1.020	0.980	1.004

TABLE 1-7
 EFFECT OF REFLECTOR RODS ON k_{eff} , LHTGR, RELOAD 6,
 150 DAYS, NO POWER RODS, 300K

1 Stuck Rod 0 Refl. Rods	1 Stuck Rod 1 Nearby Refl. Rod	1 Stuck Rod 6 Refl. Rods
0.927	0.921	0.921

3. The fission yields of important nuclides born in fissile material were amended to include birth in Pu-239 and Pu-241. The nuclide fission yields for Kr-88, Rb-88, Kr-90, Sr-90, and Ag-110m were selected for revision because (a) these nuclides were shown to have an important impact on plant maintenance and/or safety criteria and (b) the fissile fission yield was significantly different (greater than $\pm 25\%$) for MEU versus HEU fuel.
4. Ag-110m formation and release were reassessed in view of the much larger yield of Ag-109 (the precursor of γ -emitting Ag-110m) in MEU (5.6×10^{-3}) versus HEU (3.81×10^{-4}) fuel. Using provisional estimates, the Ag-110m release from an MEU core was estimated to be a factor of ~ 7.5 times larger than a comparably sized HEU core.

Fission product release from the core can be summarized by examining the activity of a few key nuclides (level A) (Table 1-8). The differing values for the two cores reflect the four factors outlined above. These results will be updated when complete core survey analyses are completed at a later date.

Nonlinear Stress-Strain Law and Strain Gradient Theory

Stress analyses of Peach Bottom test elements under diametral compression loading have been compared with experiments. It was found that the computed peak stress at failure was substantially larger than the uniaxial tensile strength, and the measured deflection was about twice as large as the calculated one. This result casts serious doubt on the present failure criteria and shows that the linear method of analysis for graphite components is highly conservative. As a result, a program has been initiated to provide a more sophisticated model, based on nonlinear theory, which would yield closer agreement with experimental measurements.

TABLE 1-8
NUCLIDE ACTIVITIES

	2240-MW(t) MEU LHTGR	3360-MW(t) HEU LHTGR
Circulating Activity (Ci/MW) ^(a)		
Kr-85m	0.45	0.54
Kr-88	0.83	1.23
I-131	2.5×10^{-5}	2.7×10^{-5}
Xe-133	0.30	0.28
40-Year Plateout Activity (Ci/MW) ^(a)		
Sr-90	0.37	0.35
Ag-110m	0.77	0.10
I-131	3.53	3.88
Cs-137	5.82	5.84

(a) Includes 102% of full-power allowance.

Diametral compression of ring specimens was suggested. Because of the simple geometry configuration, results can be analyzed easily and hence used as a guideline for the theoretical derivation of a material model. Experiments have been performed by the Graphite Development Branch.

A nonlinear material model has been constructed based on a yield criterion and a flow rule. The prediction of stress with use of this model appears to give a 20% reduction in calculated peak stresses as compared to those predicted by the linear model (present method). The phenomenon that computed peak stress at failure using the linear elastic model is substantially larger than the uniaxial tensile strength can therefore be partially explained by the nonlinear model. Improvements to the nonlinear model, as well as implementation of the model in a computer program for analyzing the diametral compression of ring specimens, are in progress.

A study has been initiated to determine whether the strain gradient theory of linear elasticity can be applied to reactor graphites. This theory was previously suggested to treat brittle materials but does not appear to have been applied to graphite modeling. When the strain gradient theory is used to calculate nonuniform stress distributions and local stress peaks, lower stress concentration factors are predicted than those predicted by the linear elastic model currently in use. This feature makes the theory particularly interesting with regard to graphite since it should help to account for the observation that graphite under nonuniform stress conditions does not fail until peak stresses (as calculated by the linear elastic model) well beyond the ultimate tensile strength are reached.

The principal conclusions at this stage of the program are as follows:

1. In-plane stress predictions have been verified by ring-cutting experiments.
2. Diametral compression and pressure burst tests on disks cut from teledial elements have successfully demonstrated the effect of

residual stresses on strength and have further verified the in-plane stress predictions.

3. The overprediction in the diameter and groove distance changes at high fluences indicates that the current method for design calculation is probably conservative and that a modified material model is required for establishing a more realistic method.
4. The measured deflections of irradiated and unirradiated specimens under diametral compression are considerably larger than those calculated with a linear elastic model.
5. Failure stresses under primary load exceed the uniaxial tensile strength increasingly with the severity of stress gradient for both irradiated and unirradiated specimens.
6. Axial stress predictions have not been verified by experiment; the strip cutting technique is suspect as the cause of the discrepancies.

A more detailed discussion of the results is given in Refs. 1-11 and 1-12.

Fuel Element Design Studies

Seismic design calculations and static strength tests of dowel/socket systems have been conducted and are summarized in Ref. 1-13. This work has shown that a four-dowel system will probably be adequate for HTGRs located at low seismic zones, while a five-dowel system will be required for HTGRs located at high seismic zones. The dowel/socket system of the HTGR fuel and reflector elements is designed to:

1. Align in a column the control rod and coolant channels in adjacent fuel and reflector elements.

2. Maintain the alignment to allow control rod insertion and sufficient coolant flow to remove the reactor core decay heat during and after a safe shutdown earthquake (SSE) incident.
3. Structurally withstand an operating base earthquake (OBE) incident.

Experiments were performed with four- and five-dowel systems to determine the following:

1. Percentage of load shared by the dowels on a fuel element or the stress distribution in a fuel element under loading (stress distribution test).
2. Strength reduction formula of a fuel element when the element is tilted at an angle (rock-up strength test).
3. Fatigue strength formula of a fuel element under dynamic loading (dynamic strength test).

The test fixture for the stress distribution test is shown in Fig. 1-8. One-half of the test fixture was bolted to two fixed I-beams; the other half of the test fixture was connected to an actuator which supplied the loads. A load cell was connected between the test fixture and the actuator to measure the applied loading. Each half of the test fixture held a 203.2 mm (8 in.) section of a fuel element. The load was transmitted between the sections via the dowels and sockets. The test fixture was designed to guide the fuel elements during testing so that the dowels and sockets could be loaded at predetermined angles.

Two linear variable differential transformers (LVDTs) were utilized in each test to monitor the displacements and/or permanent set of the fuel elements during the test. The locations of the LVDTs are shown in Fig. 1-8. Strain gage locations are shown in Fig. 1-9.

Fig. 1-8. Dowel test setup

Fig. 1-9. Strain gage locations for four- or five-dowel elements (note that the four-dowel fuel element does not have gages 13 and 14)

Figure 1-10 shows the modification to the test fixture for the rock-up strength test. Shims were inserted at the back of the stationary fuel element to simulate the rock-up angle.

The dynamic strength test was performed with the same test fixture but the fixture was installed in an impact machine as shown in Fig. 1-11. The impact load was simulated by dropping the machine crosshead onto the load cell. A force measurement system was installed at the bottom of the stationary fuel element to record the reaction force from each impact. This system consisted of two aluminum sandwich plates and five Kistler-type 57,827 N (13,000 lb) load cells.

The results of the stress distribution test are shown in Table 1-9. This table gives rough estimates of the load distribution in the dowels. The distribution shown is due to machining tolerances in the specimens and experimental setup which cause the specimens to be unevenly loaded.

The results of the rock-up strength tests are shown in Table 1-10. The four- and five-dowel systems exhibited strength reductions of 39% and 29%, respectively, as compared to the results given in Ref. 1-13 with a rock-up angle of 1 degree. This is due to the fact that the loading on the bottom two dowels changed from pure shear to shear and bending. The five-dowel sample experienced a lesser strength reduction due to the presence of the center dowel which carried a portion of the load.

The results of the dynamic strength test are shown in Table 1-11. Two loads were recorded during each test, the applied impact load and the reaction load of the stationary 203.2 mm (8 in.) test block. The load experienced by the dowel/socket system was somewhere between these two loads. Conservatively, the lesser of the two loads is used. The dynamic strengths of the four- and five-dowel systems were 35% and 17% higher, respectively, than their static strengths.

Fig. 1-10. Rock-up angle test fixture, section view (ref. Fig. 1-8)

Fig. 1-11. Impact test setup

TABLE 1-9
RESULTS OF STRESS DISTRIBUTION TEST^(a)

No. of Dowels	Upper Dowels, Strain Gages 1-6	Lower Dowels, Strain Gages 7-12	Center Dowels, Strain Gages 13, 14
4	38%	62%	--
5	22%	32%	46%

(a) Loads up to 450 kg (1000 lb). See Figs. 1-8 and 1-9 for test setup and strain gage locations.

TABLE 1-10
RESULTS OF ROCK-UP STRENGTH TEST^(a)

No. of Dowels in Fuel Element	Formula ^(b)
4	$F = 7568 e^{-0.175098\theta}$
4	$S = 6824 e^{-0.145153\theta}$
5	$F = 12777 e^{-0.135072\theta}$
5	$S = 106521 e^{-0.193441\theta}$

(a) See Figs. 1-8 and 1-10 for test setup.

(b) F = rock-up strength, lb,
S = rock-up stiffness, lb.in.,
 θ = rock-up angle, degrees.

TABLE 1-11
RESULTS OF DYNAMIC STRENGTH TEST^(a)

No. of Dowels	Formula ^(b)
4	$F = 16512 e^{-0.0131149N}$
5	$F = 19159 e^{-0.007732N}$

(a) See Fig. 1-11.

(b) F is the fatigue strength in pounds at various impact force levels and N is the number of times each impact force is applied prior to failure of a specimen.

FUEL CYCLE MANAGEMENT

Summary

The fuel management evaluations of alternate MEU/Th cycles have been expanded building on the preliminary conclusions reached in the LEU Fuels Feasibility Study. The primary new effort has been related to a detailed reevaluation of the Fort St. Vrain (FSV) MEU/Th cycle design options based on the use of a two-particle system utilizing a dense UCO fissile particle. The results of this survey, which are described in detail below, show that it is difficult to achieve a 6-yr cycle having thorium loadings representative of the LHTGR cycle within the limitation of 20% feed uranium enrichment.

Additional studies were performed to compare in-situ with in-process denaturing of bred U-233. These newer results, which are not repeated herein, confirmed the preliminary earlier results given in the feasibility study report (Ref. 1-14). The basic conclusion reached is that in-situ denaturing strategies result in fuel utilization and fuel cycle costs which are only a few percent larger than those for in-process denaturing if MEU-235 of 20% ϵ is used as the in-situ denaturing agent. It was found that natural uranium or previously exposed discharge uranium could not be effectively used for this purpose.

An additional equilibrium cycle survey study was performed to determine the potential resource savings that would result if HEU-233 were recycled in MEU/Th cycle designs. This study shows that an additional savings of about 18% in the U_3O_8 requirement could be achieved at equilibrium if the gamma activity of U-233 is assumed a sufficient deterrent against diversion.

Fuel cycle costs and resource requirements have been calculated for the MEU/Th cycles considered to date and comparisons made between the performance of the MEU/Th cycles and the HEU/Th and LWR designs.

Another major area of work has been the preparation of fuel cycles and fuel cycle performance data for the alternate LHTGR designs to be included in the Non-Proliferation Alternative Systems Assessment Program (NASAP) studies.

Fort St. Vrain (FSV) Fuel Cycle Studies

The fuel cycle survey previously reported for the MEU/Th cycle in the FSV reactor (Ref. 1-14, pp. 13-25) was focused on the use of a mixed U,Th fissile particle and led to the recommendation of a 3-yr cycle with semi-annual refueling. This choice was based on excess reactivity requirements at end of cycle. Because of the high U-238 resonance absorption with the mixed fissile particle design, it was difficult to achieve longer cycle lengths.

Subsequent core performance calculations indicated unsatisfactory amoeba migration of the mixed oxide fissile kernel. In addition, there was considerable interest in determining whether it was possible to achieve longer cycle lengths, in particular, the 6-yr cycle with annual refueling currently employed with the high-enriched uranium cycle in FSV.

A second survey has been completed based on the use of an all-uranium fissile particle with a dense UCO kernel. This design provides better performance with respect to amoeba migration and better lumping (shielding) of the U-238 absorption resonances for a given amount of uranium.

The particle designs used are given in Table 1-12. A rather high maximum burnup of 27% FIMA was chosen for the fissile particle design in anticipation of the higher burnup of the 6-yr cycle.

The range of fuel rod diameters considered was 1.0 to 1.4 cm. The reference FSV rod diameter in the present cycle is 1.27 cm. While it

TABLE 1-12
FUEL PARTICLE DESIGNS FOR FSV SURVEY

		G/CM ³
Fissile particle		
Kernel (UCO)	360 μm diameter	10.5
Buffer	105 μm thick	1.05
IPyC	35 μm thick	1.90
SiC	35 μm thick	3.20
OPyC	45 μm thick	1.80
Total particle	800 μm diameter	
Fertile particle		
Kernel (ThO ₂)	450 μm diameter	9.9
Buffer	60 μm thick	1.05
IPyC	35 μm thick	1.90
SiC	35 μm thick	3.20
OPyC	45 μm thick	1.80
Total particle	800 μm diameter	

is assumed that this diameter cannot be changed for the initial MEU/Th segments that would be loaded into FSV because of licensing impact on the schedule, it is assumed that the equilibrium cycle could use a different rod diameter if that proved to give an economic advantage. The lower limit of 1.0 cm was assumed to be the smallest size practical to fabricate with the injection process. The parametric survey thus considered diameters of 1.0, 1.2, 1.27, and 1.4 cm.

The thorium loadings selected for the survey were lower than those previously used since it was anticipated that not much thorium could be included in the 6-yr cycles. Because of the desire to include the case of no thorium at all, the loadings were expressed in terms of Th/C ratio rather than the usual C/Th ratio. The values used were as follows:

$\frac{\text{Th}}{\text{C}}$	$\frac{\text{C}}{\text{Th}}$
0	∞
0.0005	2000
0.0010	1000
0.0015	666.7

Previous dimensional calculations for FSV have indicated a peak-to-average FIMA ratio of 1.2. The fissile particle design based on 27% maximum FIMA is therefore assumed to be good for 22.5% average FIMA.

Unless otherwise noted, all cases assume a particle packing fraction of 0.5 (fissile and fertile) to allow some margin for zoning, and a capacity factor of 80% (292 EFPD/yr).

The maximum allowable age-peaking factor was assumed to be 1.35 to 1.4. The end-of-cycle reactivity requirement was assumed to be $k_{\text{eff}} = 1.015$. This reflects the lack of burnable poison in the calculations and the omission of several fission products and higher actinides usually carried in burnup calculations.

Figure 1-12 gives the end-of-cycle reactivity for 6-yr throwaway cycles as a function of Th/C ratio and fuel rod diameter. The required value of $k = 1.015$ is also indicated. The general behavior of the curves is similar to that previously seen in the LHTGR surveys (Ref. 1-14, pp. 26-48) except that most of the cases considered in the present survey are on the undermoderated side of the reactivity peak, i.e., too heavily loaded, with consequent degradation of the thermal spectrum (low η) and excessive uranium requirements.

For the current reference rod diameter of 1.27 cm, a critical case is seen to be achieved at a Th/C ratio of 0.000275 or $C/Th = 3636$. Similarly, at the minimum rod diameter considered (1.0 cm) the critical point is at $Th/C = 0.000625$ or $C/Th = 1600$. Both of these cases represent significantly lower thorium loadings than are typically found for 4-yr cycles in the LHTGR ($C/Th = 400$ to 800).

Figure 1-13 shows the annual uranium requirements for all cases with the locus of critical cases ($k = 1.015$) also shown. The left-hand scale indicates kilograms of uranium metal loaded per year and the right-hand scale indicates U_3O_8 requirements in MT/GW(e)-yr (for 0.25 tails). A comparison value of 154 MT/GW(e)-yr for the present 6-yr HEU throwaway cycle in FSV is indicated. It is seen that the case with the reference rod diameter has requirements of 342 MT/GW(e)-yr, while the 1.0-cm-diameter case requires 188 MT/GW(e)-yr. The average FIMA for each case is also indicated in the figure. The 1.0-cm-diameter case is seen to be essentially at the limit of the particle design assumed here, while all other cases have some margin. Since the large-diameter cases are undermoderated, some reduction of the uranium requirements is possible without significant reactivity loss by lowering the particle packing fraction below the 0.5 assumed here. This will also raise the FIMA values.

Several ways of increasing the reactivity of the cases having more thorium (the $Th/C = 0.0015$ line) have been investigated. The most obvious

Fig. 1-12. End-of-cycle reactivity for 6-yr throwaway cycles in FSV, 27% maximum FIMA particle, volume fraction = 0.5, $\epsilon = 0.20$

is reducing the cycle length to 4 yr. The criticality results for these cycles are shown in Fig. 1-14 and the uranium requirements are given in Fig. 1-13.

It is seen that with the reduced cycle length it is possible to have critical cases in the $C/Th = 600$ to 700 range more typical of the LHTGR design. From Fig. 1-13, the annual uranium requirements for a minimum rod diameter case are 214 MT/GW(e)-yr , somewhat higher than the equivalent 6-yr case. If a credit for buyback of spent U-235 and bred U-233 were assumed in place of pure throwaway, however, the 6-yr case would have 156 MT/GW(e)-yr and the 4-yr case $131.3 \text{ MT/GW(e)-yr}$. The current 6-yr HEU design has a net requirement of about 80 MT/GW(e)-yr with buyback of spent U-235 and U-233.

It can also be noted that the 4-yr case at minimum rod diameter has FIMA and age peaking factor values of 18% and 1.2 compared to 23% and 1.4 for the comparable 6-yr case. The 6-yr case is close to the limits for these parameters.

Another possibility studied for varying the reactivity is the adjustment of the particle volume fraction in the rod. If the volume fraction is reduced below the 0.5 value used in all cases discussed above, while keeping the same rod diameter and C/Th ratio, the loadings of U-235 and U-238 are reduced and the resonance integral of U-238 increases. This leads to reactivity reductions, but in cases with very heavy loadings, there is also a softening of the thermal spectrum and improvement in the value of η which provides a compensating positive reactivity effect.

These effects are illustrated in Figs. 1-15 and 1-16 for the 6- and 4-yr cycles, respectively, for the $Th/C = 0.0015$ ratio composition. The values of η are given by each point. For the 1.0 cm rod diameter cases, the fuel composition is already overmoderated and there is no gain from the thermal spectrum effect by reducing the volume fraction. The more heavily

Fig. 1-14. End-of-cycle reactivity for 4-yr throwaway cycles in FSV, 27% maximum FIMA particle, volume fraction = 0.5, $\epsilon = 0.20$

Fig. 1-15. k_{eff} versus particle volume fraction, FSV 6-yr cycle, $Th/C = 0.0015$

Fig. 1-16. k_{eff} versus particle volume fraction, FSV 4-yr cycle, $Th/C = 0.0015$

loaded cases at larger diameters show slight gains with an optimum volume fraction between 0.4 and 0.5. These gains are not enough to make the 6-yr cases critical, however, and there is no promise of larger gains at even higher rod diameters. Adjustments of volume fraction are thus mainly useful for reducing uranium requirements at constant reactivity.

The potential reactivity increase that might be realized by redesigning the particle for the lower FIMA actually achieved in the more heavily loaded cases was also explored. Three cases from Fig. 1-12 were repeated with a particle design corresponding to 15.4% average FIMA (18.5% maximum FIMA). In these examples the fissile particle total diameter is unchanged, but the kernel is enlarged at the expense of the buffer. The volume fraction of fissile particles in the rod is unchanged so the uranium loading is increased by about 27%. The thorium loading is unchanged. The results are given in Table 1-13. It is seen that there is essentially no change in reactivity. This results from a balance of competing effects. Both U-235 and U-238 loadings were increased by the same factor but since the U-238 resonance cross sections dropped, the net change in absorption was in favor of more U-235 absorptions. However the increased loadings further deteriorated the undermoderated spectrum, producing a reduction in η . There is thus no advantage to a better particle design in a heavily undermoderated case. Substantial gains in reactivity have been noticed previously when the particle design was changed for cases on the overmoderated side of the curve. It is thus possible that changing the particle design in conjunction with reducing the particle volume fraction would yield a gain. It would not be expected to be a large gain, however, since the two effects work in opposite directions depending on the moderation ratio, as indicated below.

	<u>Overmoderated</u>	<u>Undermoderated</u>
Reduce particle volume fraction	k drops fast	k rises slowly, may stay the same
Increase U loading in particle by better design	k rises fast	k drops slowly, may stay the same

TABLE 1-13
EFFECT OF REDESIGNING FISSILE PARTICLE IN FSV SURVEY

	Case 1 Th/C=0.0015, $D_F=1.27$		Case 2 Th/C=0.0015, $D_F=1.2$		Case 3 Th/C=0.0010, $D_F=1.2$	
	27(a)	18.5	27	18.5	27	18.5
RI ²³⁸	87.5	78.1	92.2	82.6	88.8	79.3
k_{BOC}	1.0138	1.0107	1.0253	1.0219	1.0462	1.0415
k_{MOC}	0.9889	0.9883	0.9975	0.9975	1.0167	1.0158
k_{EOC}	0.9697	0.9695	0.9780	0.9783	0.9955	0.9951
η_{MOC}	1.866	1.835	1.889	1.858	1.873	1.842

(a) FIMA design.

Finally, it has been calculated that the three 6-yr cases for C/Th = 0.0015 and diameters of 1.0, 1.2, and 1.27 could all be made critical by raising the enrichment from 20% to about 25%.

Since the calculations described here were completed, a more accurate treatment of the neutron leakage was found to be necessary. Including such a treatment increases the reactivity of all cases, in particular the more heavily loaded ones. In addition an error was found in the fission product input data in the burnup calculations, which reduces reactivity. The net effect of these corrections should be an increase in reactivity, so that the results given here are somewhat conservative. The basic conclusion remains, however, that it is not easy to achieve thorium loadings in FSV comparable to those specified for the LHTGR within the limitations of a 6-yr cycle and an enrichment of 20%.

MEU/Th Cycle with Nondenatured U-233 Recycle

A reference MEU/Th cycle for nondenatured U-233 recycle in the LHTGR was studied based on the assumption that the U-233 gamma activity may be deemed a sufficient deterrent against diversion. The cycle selected has the following cycle parameters:

C/Th ratio	400
Fuel rod diameter	1.4 cm
Lifetime	4 yr
Capacity factor	0.8

The savings in U_3O_8 requirements achieved by not denaturing the recycle U-233 with U-238 are of the order of 12%. Additional savings of 6% are achieved by using continuous rather than once-through recycle.

The recycle blocks contain recycle U-233 fissile particles (non-denatured) and the same fertile particle as the makeup blocks. In previous

studies with denatured U-233 recycle, it was always assumed that the recycle blocks have the same C/Th ratio, fuel rod diameter, and particle packing fraction as the makeup blocks. This was reasonable since the recycle fissile particle design was essentially the same as the makeup fissile particle. In the present case, this assumption may not be appropriate since the recycle fissile particle has a different design with about three times as much fissile uranium per particle. In addition, since there is no U-238 in the recycle blocks, it is not necessary to have a design with maximum lumping of the fissile particles. The easiest way to handle the recycle blocks in the present parameteric survey is to assume the same rod diameter and C/Th ratio (to keep the thorium resonance cross sections the same in both block types) and let the fissile particle volume fraction float according to how much U-233 is produced and recycled in each case.

The following procedure is therefore used. For each C/Th ratio and rod diameter, the GAFFEE zero-dimensional burnup code is given the feed atom densities of carbon, silicon, and thorium corresponding to a full segment of makeup blocks. It is told to recycle the U-233, U-234, U-235, and U-236 produced from the thorium and to search for the amount of 20% enriched uranium feed material required to make the system critical at end-of-life. This amount is called N_{feed} . The amount of uranium that corresponds to a full segment of makeup blocks, N_{full} , can be calculated for the given rod diameter and C/Th ratio. There are 1322 blocks in an LHTGR reload segment. The numbers of makeup and recycle blocks is then

$$n_{\text{makeup}} = 1322 \frac{N_{\text{feed}}}{N_{\text{full}}}$$

$$n_{\text{recycle}} = 1322 - n_{\text{makeup}}$$

Knowing the number of recycle blocks and the total atom density of the four uranium nuclides being recycled, one can calculate the volume fraction

of the recycle fissile particles for the assumed rod diameter and check that, in combination with the fertile particles, it leads to a realistic total particle packing fraction.

Table 1-14 summarizes the GAFFEE results. It is seen that the split between makeup and recycle blocks depends strongly on the rod diameter assumed and that the total volume fraction of particles in the rods of the recycle blocks is significantly less than the 0.5 value assumed for the makeup blocks. One could therefore add extra fuel to these blocks if this were useful to modify their power generation behavior relative to makeup blocks.

It is noted that the uranium requirements drop sharply with increasing thorium loadings but are not too sensitive to the rod diameter. Thus, the diameter can be adjusted to suit the requirements of matching the block types. It is also noted that the maximum age peaking factors and FIMA values are within acceptable limits for all cases.

The cycle to be studied is shown in Fig. 1-17. The core is made up of makeup blocks and recycle blocks. The makeup blocks contain denatured uranium fissile particles (20% enriched) and fertile particles. With the particle designs being given (see Table 1-15) and the particle packing fraction in the rod being set at 0.5, the initial composition of these blocks is completely fixed when the usual variable parameters of C/Th ratio and rod diameter are selected. What is not known in advance is how many makeup blocks are required.

All of the cases shown in Table 1-14 have once-through recycle of the bred U-233. This is the way recycle has been handled to date for denatured cases, since the large amount of U-238 accompanying the U-233 makes the enrichment very low after one recycle. The present case of undenatured recycle does not have this limitation and thus can use continuous recycle of the U-233, which will further reduce the uranium makeup requirements. Data for two C/Th ratios are given in Table 1-16.

TABLE 1-14
SURVEY RESULTS, ONCE-THROUGH RECYCLE, B² LEAKAGE MODEL

Fuel Rod Diameter (cm)	N _{feed}	N _{full}	No. of Blocks Makeup/Recycle	Part. Vol. Frac. Recycle Block	Max. Age Peaking	Avg. % FIMA	Makeup (kg/yr)
C/Th=850							
1.0	7.561X10 ⁻⁵	6.290X10 ⁻⁵	No recycle				
1.2	7.673X10 ⁻⁵	10.43X10 ⁻⁵	973/349	0.1860	1.434	22.5	3557
1.4	7.858X10 ⁻⁵	15.32X10 ⁻⁵	678/644	0.1077	1.451	22.0	3643
C/Th=700							
1.0	7.318X10 ⁻⁵	5.566X10 ⁻⁵	No recycle				
1.2	7.388X10 ⁻⁵	9.812X10 ⁻⁵	955/327	0.2305	1.383	22.2	3425
1.4	7.568X10 ⁻⁵	14.72X10 ⁻⁵	678/644	0.1304	1.405	21.7	3508
C/Th=550							
1.0	7.170X10 ⁻⁵	4.677X10 ⁻⁵	No recycle				
1.2	7.124X10 ⁻⁵	8.855X10 ⁻⁵	1064/258	0.3237	1.311	21.6	3303
1.4	7.223X10 ⁻⁵	13.79X10 ⁻⁵	692/630	0.1661	1.333	21.2	3348
C/Th=400							
1.0	No solution						
1.2	7.058X10 ⁻⁵	7.185X10 ⁻⁵	1299/23	Infeasible			
1.4	7.011X10 ⁻⁵	12.17X10 ⁻⁵	762/560	0.2238	1.235	20.2	3250
C/Th=300							
1.2	No solution						
1.4	No solution						
1.6	7.538X10 ⁻⁵	16.01X10 ⁻⁵	623/699	0.2157	1.156	17.6	3495

Fig. 1-17. Non-denatured U-233 recycle

TABLE 1-15
PARTICLE DESIGNS FOR NON-DENATURED RECYCLE SURVEY

		G/CM ³
Makeup fissile		
Kernel (UCO)	350 μ m diameter	10.6
Buffer	105 μ m thick	1.05
IPyC	35 μ m thick	1.9
SiC	35 μ m thick	3.2
OPyC	45 μ m thick	1.8
Total particle	790 μ m diameter	
t/D	0.63	
Recycle fissile		
Kernel (WAR UCO)	360 μ m diameter	3.3
Buffer	65 μ m thick	1.05
IPyC	35 μ m thick	1.9
SiC	35 μ m thick	3.2
OPyC	40 μ m thick	1.8
Total particle	710 μ m diameter	
t/D	0.486	
Makeup and recycle fertile		
Kernel (ThO ₂)	500 μ m diameter	9.9
Buffer	85 μ m thick	1.05
PyC	75 μ m thick	1.90
Total particle	820 μ m diameter	
t/D	0.32	

TABLE 1-16
SURVEY RESULTS, CONTINUOUS RECYCLE, B² LEAKAGE MODEL

Fuel Rod Diameter (cm)	N _{feed}	N _{full}	No. of Blocks Makeup/Recycle	Part. Vol. Frac. Recycle Block	Max. Age Peaking	Avg % FIMA	Makeup (kg/yr)
C/Th=850							
1.0	$\sim 7.4 \times 10^{-5}$	6.290×10^{-5}	No recycle				
1.2	7.551×10^{-5}	10.43×10^{-5}	957/365	0.2218	1.424	22.3	3500
1.4	7.715×10^{-5}	15.32×10^{-5}	666/656	0.1230	1.443	21.9	3577
C/Th=400							
1.0	8.930×10^{-5}	2.967×10^{-5}	No recycle				
1.2	6.579×10^{-5}	7.185×10^{-5}	1210/112	Infeasible			3050
1.4	6.547×10^{-5}	12.17×10^{-5}	711/611	0.2687	1.23	19.9	3035

As noted in the previous discussion of the Fort St. Vrain survey, the results are very sensitive to the treatment of neutron leakage. In particular, the model used to date, which involves bucklings only for the fast energy groups (no thermal inleakage), was found to significantly overestimate uranium requirements. This conservative model has also been used for the results given in Tables 1-15 and 1-16 to facilitate comparison with the previous denatured recycle cases, and because there are not yet any appropriate dimensional calculations on which to base a better leakage model. Table 1-17 gives some results based on using the leakage per source neutron model from an HEU cycle. The gains in reduced uranium requirements are substantial but cannot be regarded as certain until dimensional calculations are done for the actual MEU compositions.

The uranium requirements for all cases are summarized in Fig. 1-18, which shows annual uranium requirements (in kilograms of metal) as a function of C/Th ratio. The minimum values are seen to occur around C/Th ratios of 300 to 400. For several reasons, it is prudent not to go to the maximum thorium loading indicated in the figure in selecting a reference case. The leakage correction is not certain, which means the optimum may be closer to 400 than 300. In addition, extra thorium may be required during the approach to equilibrium to control reactivity and age peaking, and also at equilibrium to match power generation characteristics of recycle and makeup blocks. Thus, a C/Th ratio of 400 was selected for the reference case.

The problem of matching power generation between makeup and recycle blocks was studied at length for the HEU cycle with a large number of two-dimensional calculations (Ref. 1-15). It is not known if the criteria derived from that work will prove applicable to the MEU cycle, but it has been assumed from those results that a rough criterion should be that the ratios of equivalent fertile and fissile atom densities in the two block types be close to equal, i.e.,

TABLE 1-17
SURVEY RESULTS, CONTINUOUS RECYCLE, L/S LEAKAGE MODEL

Fuel Rod Diameter (cm)	N_{feed}	N_{full}	No. of Blocks Makeup/Recycle	Part. Vol. Frac. Recycle Block	Max. Age Peaking	Avg % FIMA	Makeup (kg/yr)
C/Th=400							
1.0	6.183×10^{-5}	2.967×10^{-5}	No recycle				
1.2	6.022×10^{-5}	7.185×10^{-5}	1108/214	Infeasible			
1.4	6.046×10^{-5}	12.169×10^{-5}	657/665	0.2532	1.277	20.8	2803
C/Th=300							
1.2	5.869×10^{-5}	5.146×10^{-5}	No recycle				
1.4	5.595×10^{-5}	10.19×10^{-5}	726/596	0.3532	1.188	19.6	2594
1.6	5.699×10^{-5}	16.01×10^{-5}	471/851	0.2311	1.209	19.3	2642
C/Th=250							
1.2	7.499×10^{-5}	3.518×10^{-5}	No recycle				
1.4	5.591×10^{-5}	8.606×10^{-5}	859/463	0.4765	1.132	18.2	2592
1.6	5.581×10^{-5}	14.48×10^{-5}	510/812	0.3376	1.145	18.0	2587

Fig. 1-18. Uranium requirements for non-denatured recycle

$$\frac{N_{\text{makeup}}^{\text{fissile equiv.}}}{N_{\text{recycle}}^{\text{fissile equiv.}}} = \frac{N_{\text{makeup}}^{\text{fertile equiv.}}}{N_{\text{recycle}}^{\text{fertile equiv.}}}$$

This will hopefully lead to similar k_{∞} values for the two block types.

By "equivalent," it is meant that the U-233 atom density is multiplied by a parity ratio of 1.3 and the U-238 density is multiplied by the ratio of total absorptions in U-238 to those in Th-232 before combining atom densities. The data on ratios is summarized in Table 1-18. It is seen that at the selected reference C/Th ratio of 400, a rod diameter of 1.4 cm gives nearly equal loading ratios. Pending dimensional calculations, this diameter was therefore chosen for the reference case.

Having selected a reference case based on the equilibrium cycle, the approach to equilibrium was studied with the GARGOYLE code. It was found that the relatively heavy thorium loading could not be accommodated in the early reload segments before the equilibrium amount of U-233 for recycle had been built up. As a consequence, the advantage in U_3O_8 requirements of the nondenatured recycle case over the denatured case is not as large when evaluated over 30-yr operation as it is in the equilibrium cycle (i.e., 7% instead of 18%). Improvements can undoubtedly be made by optimizing on the 30-yr basis rather than on the equilibrium cycle and varying both the C/Th ratio and the rod diameter for each reload. However, dimensional studies of the power sharing between makeup and recycle blocks should probably be done to confirm the validity of the assumptions made in the GAFFEE survey before going further.

Fuel Cycle Costs - Resource Requirements

Fuel Cycle Costs

Fuel cycle cost comparisons and resource requirement comparisons have been carried out to determine the relative competitiveness of the preliminary MEU/Th cycles under consideration. These evaluations have also

TABLE 1-18
LOADING RATIOS IN MAKEUP AND RECYCLE BLOCKS

D _F	Makeup Blocks					Recycle Blocks			N _{makeup} /N _{recycle}	
	N ²⁵	N ²⁸	N Th	Abs. 238 Abs. 232	Equiv. N _{fertile}	N ²³	N ²⁵	Equiv. N _{fissile}	Fertile	Fissile
Once-Through Recycle: B ² Leakage Model										
C/Th = 850										
1.0	1.285X10 ⁻⁵	5.032X10 ⁻⁵	-							
1.2	2.086X10 ⁻⁵	8.349X10 ⁻⁵	7.193X10 ⁻⁵	1.36	18.499X10 ⁻⁵	7.445X10 ⁻⁶	0.311X10 ⁻⁶	9.989X10 ⁻⁶	2.57	2.09
1.4	3.064X10 ⁻⁵	12.256X10 ⁻⁵	6.993X10 ⁻⁵	1.23	22.019X10 ⁻⁵	3.977X10 ⁻⁶	0.170X10 ⁻⁶	5.340X10 ⁻⁶	3.15	5.74
C/Th = 700										
1.0	1.113X10 ⁻⁵	4.452X10 ⁻⁵	-							
1.2	1.962X10 ⁻⁵	7.850X10 ⁻⁵	8.731X10 ⁻⁵	1.43	19.940X10 ⁻⁵	9.598X10 ⁻⁶	0.378X10 ⁻⁶	12.855X10 ⁻⁶	2.28	1.53
1.4	2.944X10 ⁻⁵	11.776X10 ⁻⁵	8.482X10 ⁻⁵	1.27	23.473X10 ⁻⁵	4.774X10 ⁻⁶	0.196X10 ⁻⁶	6.407X10 ⁻⁶	2.77	4.60
C/Th = 550										
1.0	0.935X10 ⁻⁵	3.742X10 ⁻⁵	-							
1.2	1.771X10 ⁻⁵	7.084X10 ⁻⁵	11.111X10 ⁻⁵	1.56	22.127X10 ⁻⁵	15.44X10 ⁻⁶	0.551X10 ⁻⁶	20.623X10 ⁻⁶	1.99	0.86
1.4	2.758X10 ⁻⁵	11.032X10 ⁻⁵	10.793X10 ⁻⁵	1.38	25.984X10 ⁻⁵	6.207X10 ⁻⁶	0.232X10 ⁻⁶	8.301X10 ⁻⁶	2.41	3.32
C/Th = 400										
1.0	0.594X10 ⁻⁵	2.374X10 ⁻⁵	-							
1.2	1.437X10 ⁻⁵	5.748X10 ⁻⁵	-							
1.4	2.434X10 ⁻⁵	9.735X10 ⁻⁵	14.84X10 ⁻⁵	1.54	29.793X10 ⁻⁵	9.542X10 ⁻⁶	0.300X10 ⁻⁶	12.705X10 ⁻⁶	2.01	1.92
Continuous Recycle: L/S Leakage Model										
C/Th = 400										
1.0	0.594X10 ⁻⁵	2.374X10 ⁻⁵	15.635X10 ⁻⁵	2.09	20.597X10 ⁻⁵					
1.2	1.437X10 ⁻⁵	5.748X10 ⁻⁵	15.268X10 ⁻⁵	1.70	25.040X10 ⁻⁵	26.16X10 ⁻⁶	3.60X10 ⁻⁶	37.608X10 ⁻⁶	1.64	0.38
1.4	2.434X10 ⁻⁵	9.735X10 ⁻⁵	14.835X10 ⁻⁵	1.46	29.048X10 ⁻⁵	8.203X10 ⁻⁶	1.13X10 ⁻⁶	11.794X10 ⁻⁶	1.96	2.06
C/Th = 300										
1.2	1.029X10 ⁻⁵	4.117X10 ⁻⁵	20.341X10 ⁻⁵	2.05	28.780X10 ⁻⁵					
1.4	2.038X10 ⁻⁵	8.150X10 ⁻⁵	19.764X10 ⁻⁵	1.69	33.538X10 ⁻⁵	12.688X10 ⁻⁶	1.82X10 ⁻⁶	18.314X10 ⁻⁶	1.70	1.11
1.6	3.201X10 ⁻⁵	12.805X10 ⁻⁵	19.097X10 ⁻⁵	1.45	37.664X10 ⁻⁵	8.502X10 ⁻⁶	1.22X10 ⁻⁶	12.273X10 ⁻⁶	1.97	2.61
C/Th = 250										
1.2	0.704X10 ⁻⁵	2.814X10 ⁻⁵	24.394X10 ⁻⁵	2.47	31.345X10 ⁻⁵					
1.4	1.721X10 ⁻⁵	6.885X10 ⁻⁵	23.701X10 ⁻⁵	1.90	36.783X10 ⁻⁵	20.61X10 ⁻⁶	3.07X10 ⁻⁶	9.863X10 ⁻⁶	1.55	0.58
1.6	2.895X10 ⁻⁵	11.582X10 ⁻⁵	22.902X10 ⁻⁵	1.63	41.780X10 ⁻⁵	11.48X10 ⁻⁶	1.70X10 ⁻⁶	16.627X10 ⁻⁶	1.82	1.74

included comparisons with the HEU-233 LHTGR cycle and with the LWR cycle. Table 1-19 summarizes the unit handling cost and resource cost assumptions used in the cost comparisons which are summarized in Table 1-19.

The following conclusions can be made from the cost results summarized in Table 1-20.

1. Under throwaway conditions, the MEU/Th design is about 6% lower in fuel cycle costs than the LEU or the PWR cycle.
2. Under recycle U conditions, the MEU/Th advantage is about 12% over the LWR and 14% over the LEU design.
3. Under recycle U conditions, the HEU/Th design has about 13% lower costs than the MEU/Th design and about 23% lower costs than the LWR.
4. With only U-233 recovery and recycle, the discharged fissile particle containing most of the bred plutonium could be sent directly to waste. This cycle option results in costs about 8% lower than the LWR with all U recovery and recycle.

Resource Utilization

The initial core, annual reload, and 30-yr U_3O_8 and enrichment resource requirements for several MEU/Th, HEU/Th, and LWR cycles have been compared and are summarized in Table 1-21. These results show that MEU/Th cycles, under throwaway conditions, require about 19% more enrichment and 22% less U_3O_8 than the LWR under throwaway conditions.

Under U recycle assumptions, the MEU/Th and LWR enrichment requirements are about equal, but the MEU/Th U_3O_8 requirements are 26% lower than the LWR.

TABLE 1-19
ECONOMIC, RESOURCE, AND HANDLING COST ASSUMPTIONS

Fuel Cycle Cost Optimization Basis

1989 startup date, 80% capacity factor
 Fuel cycle costs levelized over 15 years
 Fuel cycle costs discounted to startup year
 U_3O_8 price: \$38/lb in 1976
 Inflationary escalation rate: 6%/yr
 Scarcity escalation rate on U_3O_8 : 0% to 1995, 2.5% thereafter
 Enrichment price: \$103/SWU in 1976
 Discount rate: 10.2%/yr
 Working capital rate: 15.6%/yr
 Tail assay: 0.20%

LHTGR Unit Costs

LWR Unit Costs

Spent fuel shipping cost: \$1715/fuel element in 1977 \$	\$21/kg
Reprocessing cost: \$4035/fuel element in 1977 \$	\$280/kg
Waste disposal: \$2235/fuel element in 1977 \$	\$95/kg
Refabrication: \$7480/fuel element in 1977 \$	\$260/kg
Fresh fuel fabrication: \$3000/fuel element in 1977 \$	\$133/kg
	initial core
	\$73/kg
	reloads

TABLE 1-20
FUEL CYCLE COST SUMMARY^(a)

[15-yr Levelized Costs, M/kW-hr(e)]

HEU/Th HTGR		
	Throwaway	18.3
	Recycle U	14.6
LWR		
	Recycle U, Pu	16.8
<u>Non-Proliferation Cycles</u>		
MEU/Th HTGR		
	Throwaway	19.2
	Recycle U-233; throwaway fissile particle	17.3
	Recycle all U	16.7
LEU HTGR		
	Throwaway	20.5
	Recycle U	19.4
LWR		
	Throwaway	20.5
	Recycle U	18.9
	MEU/Th	20.5

^(a) Based on 0.2% tails; U_3O_8 2.5%/yr scarcity after 1995.

TABLE 1-21
 U_3O_8 AND ENRICHMENT REQUIREMENTS
 (0.2% Tails, 80% Capacity Factor)

Cycle	Initial Core		Annual Reload		30-Year Total	
	MT U_3O_8 /GW(e)	10^3 SWU/GW(e)	MT U_3O_8 /GW(e)	10^3 SWU/GW(e)	MT U_3O_8 /GW(e)	10^3 SWU/GW(e)
HTGR HEU/Th - recycle U	460	510	55	60	2180	2410
HTGR HEU/Th - throwaway	270	290	133	146	4070	4530
LWR - U and Pu recycle	390	220	113	85	3960	2870
LWR - U recycle	390	220	149	119	4790	3680
LWR - throwaway	390	220	186	123	5790	3790
HTGR MEU/Th - recycle U-233	340	340	122	122	3900	3900
HTGR MEU/Th - recycle all U	340	340	110	115	3540	3670
HTGR MEU/Th - throwaway	340	340	144	144	4520	4520
HTGR MEU/Th - HEU-233	340	340	105	107	3200	3360
HTGR LEU - recycle U	270	210	135	132	4210	4080
HTGR LEU - throwaway	270	210	148	136	4590	4200

NASAP Cycle Inputs

Yearly mass flow rates for twelve alternate LHTGR cycles were prepared as part of the LHTGR data package prepared for DOE for use in the upcoming NASAP evaluations. Performance specifications, design specifications, fuel management information, and fuel cycle costs were supplied for each alternate design.

The preliminary cycle optimization studies performed to date have been aimed at defining MEU/Th cycle characteristics which minimize the U_3O_8 requirement within certain performance and/or cost limitations while maintaining the primary goal that any MEU/Th cycle design must be capable of being converted to the HEU/Th cycle at some later date. Consistent with these objectives and/or limitations, the following guidelines have been used in arriving at the preliminary MEU/Th cycle designs picked for the NASAP cases:

1. Same outer graphite block dimensions as HEU/Th reference.
2. Lower or equal core pressure drop as in the previous reference, i.e., the same or larger coolant flow frontal area.
3. The same or smaller fuel pin diameter, but not smaller than 1.0 cm diameter due to potential fabrication difficulties in making small pins.
4. Particle coating designs having coating thicknesses which, with existing technology, are deemed to be required for the predicted maximum fuel particle burnup (FIMA).
5. Reload age peaking factor (APF) of ≤ 1.40 as in previous reference design.
6. Peak nvt or fast neutron fluence of $\leq 8.0 \times 10^{25}$ for $E \geq 29$ fJ.

The cycles for which data were supplied are summarized in Table 1-22.

TABLE 1-22
FUEL CYCLE DESCRIPTION

1. MEU/Th (20% ϵ), throwaway cycle. C/Th = 350/850,^(a) PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.17 cm. MEU fissile particle and thorium fertile particle.
2. MEU/Th, recycle U-233, denature in-process. C/Th = 350/850, PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.17 cm. MEU fissile particle and thorium fertile particle. Discharged HEU-233 denatured to MEU-233 with U-238 and recycled. Discharged MEU-235 particle not recycled in mass flows and fissile value not credited in costs.
3. MEU/Th, recycle U-233, denature in-situ. C/Th = 350/850, PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.17 cm. MEU fissile particle and MEU/Th fertile particle. Recycle ~12% ϵ MEU-233 particle. Residual MEU fissile particle not recycled in mass flows and fissile value not credited in costs.
4. MEU/Th, recycle all U, denature in-situ. Same mass flows as used in (3) except discharged MEU fissile particle uranium is credited in costs.
5. HEU/Th (old reference). C/Th = 180/180, PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.58 cm. Initial cycle = 1.5 yr. HEU-235 makeup fissile and thorium fertile particle. HEU-235 recycled once and then discharged.
6. HEU (spiked)/Th. Same conditions and same mass flows as (5).
7. Pu/Th converter. C/Th = 650, PD = 7.1 W/cm³, 4.9-yr cycle batch loaded for 1344 EFPDs per reload, 3360 MW(t), D_{rod} = 1.58 cm. Discharged HEU-233 not recycled but credited and used in another reactor as HEU-233 (or denatured to MEU-233 in-process).
8. HEU-233/Th(U-233 makeup from breeder). C/Th = 150/150, PD = 5.0 W/cm³, 6-yr cycle, 3360 MW(t), D_{rod} = 1.58 cm. Single Th/HEU-233 feed particle and single Th/HEU recycle particle.
9. MEU-233/Th (denatured U-233 makeup from breeder). C/Th = 350/850, PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.17 cm. Run as no recycle case with all discharged MEU credited in costs.
10. MEU/Th-HEU-233 recycle. C/Th = 350/400, PD = 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.17 initial core, 1.4 cm equilibrium reload. MEU-235 fissile particle, Th fertile particle, and HEU-233 recycle particle. Mass flows based on self-generated equilibrium cycle quantity of HEU-233 available starting at second reload. All discharged MEU-235 and HEU-233 not recycled in mass flows but fissile value credited at discharge.

TABLE 1-22 (cont)

11. 40% ϵ MEU/Th, recycle all U, denature in-situ. C/Th = 215/215, 7.1 W/cm³, 4-yr cycle, 3360 MW(t), D_{rod} = 1.4 cm. Single MEU/Th with U/Th = 0.22 feed particle and Th fertile particle. All MEU combined and recycled once in MEU recycle particle. Discharged recycle MEU particle credited in cost.
 12. MEU/Th, recycle all U, denature in-process. Same mass flows as used in (2) except discharged MEU fissile particle uranium value is credited in costs.
-

(a) C/Th ratio in initial core/equilibrium reload.

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4. HTGR FISSION PRODUCT AND COOLANT CHEMISTRY*
189a NO. 00549; PROGRAM AREA 12

TASK 100: FISSION PRODUCT BEHAVIOR

Subtask 110: Fission Product Transport

Burnup Dependence of R/B for Failed Particles with ThO₂ Kernels

Introduction and Summary. In calculating the release of fission product gases from HTGR fuel elements, it is necessary to know the effect of burnup on R/B for the release of short-lived isotopes (half-lives between 8 s and 5.3 d) from particles with failed coatings. Myers et al. (Ref. 4-1) demonstrated that for failed particles with dense UC₂ kernels, R/B is independent of burnup over the range 0% to 63% FIMA. On the other hand, it has been demonstrated that for failed ThO₂ particles, R/B is dependent on burnup (Refs. 4-2, 4-3), and an upper limit estimate of this dependence has been formulated (Ref. 4-2). The present report provides (1) a mean curve for the burnup dependence of R/B for failed particles with ThO₂ kernels and (2) an estimate of the associated uncertainty.

Burnup Dependence Data. Fission gas release (R/B) data are available for failed particles containing ThO₂, (8Th,U)O₂, and (2Th,U)O₂ kernels. Data exist for laser-failed particles and for particles failed in-pile. The data for laser-failed particles are considered the most reliable. The data for particles failed in-pile are considered less reliable because of uncertainty of the failed fraction.

The increase in fission gas release with increase in burnup for failed particles is dependent on structural changes in the kernel material. These

* Now a program area under the HTGR Generic Technology Program.

changes undoubtedly depend on the composition of the kernel; therefore, the data for particles with ThO_2 and $(8\text{Th},\text{U})\text{O}_2$ kernels are considered to be the most appropriate for the present purpose of determining the burnup dependence of R/B. The data for $(2\text{Th},\text{U})\text{O}_2$ are considered to be less appropriate because the relatively high uranium content may cause a different morphology under irradiation conditions.

The available data on laser-failed particles with $(8\text{Th},\text{U})\text{O}_2$ kernels are given in Table 4-1 for Kr-85m release. The data for non-zero burnups given in Table 4-1 are, with the exception of the GF-2-II particles, for particles irradiated at 1250°C . This temperature is appreciably higher than the average fuel temperature for an HTGR system. The effect of the irradiation temperature on the R/B values measured at 1100°C is indicated by a comparison of the R/B values of Table 4-1 for GF-1-II and GF-2-II particles. This comparison indicates that the release is greater for the lower irradiation temperature. Since the burnup for the GF-2-II datum is lower than that for the GF-1-II datum, the increase in R/B at 1100°C with decrease in the irradiation temperature would be predicted to be even greater if data for the same burnup were compared. The increase in fractional release at a given temperature with decrease in irradiation temperature has also been found for cesium release (Ref. 4-8). Thus, use of the data in Table 4-1 with 1250°C irradiation temperatures to obtain the mean curve of R/B versus burnup could lead to underestimating the gaseous release. However, allowance for this possibility at present has to be included only in the uncertainty estimates since there are insufficient data to quantitatively account for it.

The data of Table 4-1 apply to Kr-85m at about 1100°C . Data exist at ~0% and 7% FIMA for other nuclides and at other temperatures (Ref. 4-4). These data are plotted in Fig. 4-1; as shown in the figure, the ratio of R/B at 7% FIMA to that at ~0% FIMA is a smooth function of temperature for each nuclide.* It can be seen from Fig. 4-1 that assuming the ratio to be

*The data of Fig. 4-1 were read from smooth curves through the R/B data for the measurement temperatures; hence, the temperatures of Fig. 4-1 do not correspond in general to the measurement temperatures.

TABLE 4-1
FISSION GAS RELEASE (R/B) VALUES FROM LASER-FAILED (8Th,U)₂ PARTICLES FOR Kr-85m at 1100°C

Burnup (% FIMA)	R/B Kr-85m	Particle Source	Particle Identification	Particle Diameter (μm)	Irrad. Temp. (°C)	Lab	Symbol (in Fig. 4-2)	Ref.
~0	1.2×10^{-3} (a)	NA ^(b)	6155.01.030 ^(c)	500	NA	CEA	○	4-4
7	1.6×10^{-2} (a)	SSL-1;A8 ^(c)	6155.01.030 ^(c)	500	1250	CEA	○	4-4
9.18	2.0×10^{-2} (d)	GF-1-III fuel rod b	6155.01.030	500	1250	CEA	○	4-5
11.1 ^(c)	1.6×10^{-2} (d)	GF-1-II fuel rod a	6155.00.030	NA	1250	CEA	○	4-5
7.9 ^(c)	2.2×10^{-2} (d)	GF-2-II fuel rod a	6155.00.030	NA	1050	CEA	○	4-5
9.18	1.2×10^{-2}	NA	6155.01.030 5A	500	1250	GA	△	4-6
9.18	1.3×10^{-2}	NA	6155.01.030 5B	500	1250	GA	△	4-6
3.5	7.0×10^{-3} to 8.0×10^{-3}	GF-3-II	6542.02.036	480	NA	CEA	○	4-5

(a) Determined by interpolation between R/B values measured at 1025° and 1150°C.

(b) Not presently available.

(c) Data provided by private communication from CEA (Ref. 4-7).

(d) Determined by extrapolation using R/B values measured at 925° and 1025°C.

Fig. 4-1. Ratio of R/B at 7% FIMA to ~0% FIMA as a function of temperature

the same for all nuclides at temperatures up to 1100°C (represented by the dashed line) gives conservatively high R/B values or produces negligible error. Above 1100°C, the ratio declines for nuclides with half-lives greater than ~ 1 h whereas it rises for the shorter-lived nuclides and apparently, as indicated by the Xe-138 data, ultimately declines.

There is a source of uncertainty associated with the data in Fig. 4-1. For non-zero burnups, measurements at 925° and 1025°C were made at a fission rate density of 1.5×10^{19} fissions/m³.s, whereas measurements at 600°, 1150°, and 1350°C were made at a fission rate density of 1.1×10^{18} fissions/m³.s. There appears to be a distinct change in mechanism between these two fission rate densities (Ref. 4-4). The fission rate density relevant to HTGR operating conditions is $\sim 2 \times 10^{19}$ fission/m³.s. Thus, strictly, only the data at 925° and 1025°C may be relevant to HTGR operating conditions; the data above 1025°C may be governed by a mechanism that is not dominant under HTGR operating conditions; and the treatment of the data above 1100°C is questionable. We choose simply to maintain a constant value for the ratio for all nuclides at temperatures above 1100°C equal to the value for Kr-85m at 1100°C. This should give conservatively high R/B values for nuclides with half-lives greater than ~ 1 h, and the error associated with R/B values for nuclides with half-lives less than ~ 1 h should be less than a factor of two. In any case, there is the mitigating factor that on a time-average basis only about 2% of the HTGR fuel is at temperatures above 1100°C.

Fission gas release data are given in Table 4-2 for failed BISO ThO₂ particles in the P13N and P13P capsules. The entry for P13N is uncertain in R/B as a result of uncertainty (a factor of 4) in failure fraction. In using the data from P13P, the average of R/B values was taken for the first and second triplets of data.

The Dependence of R/B on Burnup Derived from the Data on ThO₂ and (8Th,U)O₂ Particles. The data from Tables 4-1 and 4-2 are shown in Fig. 4-2 where R/B for Kr-85m at 1100°C is plotted as a function of burnup. The

TABLE 4-2
Kr-85m R/B VALUES FOR FAILED BISO ThO₂ PARTICLES IN CAPSULES P13N and P13P

Burnup (% FIMA)	R/B Kr-85m	Particle Source	Particle Position	Particle Diameter (μm)	Average (Peak) Irrad. Temp. (°C)	Lab	Symbol (in Fig. 4-2)	Ref.
0.8	9.0×10^{-3}	P13P	5A	500	1232 (1479)	GA	◊	4-9, 4-10
0.9	2.0×10^{-3}	P13P	4D	500	1003 (1217)	GA		4-9, 4-10
1.0	1.0×10^{-3}	P13P	4B	400	1068 (1285)	GA		4-9, 4-10
1.9	2.0×10^{-3}	P13P	2D	500	1397 (1682)	GA	□	4-9, 4-10
1.9	2.0×10^{-3}	P13P	2B	500	1370 (1652)	GA		4-9, 4-10
1.8	2.0×10^{-3}	P13P	2A	500	1321 (1581)	GA		4-9, 4-10
1.7-3.0	3.8×10^{-4} to 6.0×10^{-3}	P13N	1E	400	1718 (1902)	GA	⬆	4-11

Fig. 4-2. Dependence of R/B for Kr-85m at 1100°C on burnup (% FIMA)
(see Tables 4-1 and 4-2 for definition of symbols)

error bars associated with the data are the estimated standard deviations; the latter are given in Table 4-3 along with the R/B, burnup data, and weighting factors. The weighting factors, which are proportional to the inverses of the variances, are used in obtaining a least-squares fit to the data. In view of the data scatter, the simplest functional relation was chosen to represent the burnup dependence of R/B. The least-squares fit, shown in Fig. 4-2, is given by

$$(R/B)_{\text{Kr-85m, 1100}^\circ\text{C}} = 0.000666 + 0.00414 F^{0.6} \quad , \quad (4-1)$$

where F is % FIMA. The coefficients in this equation were determined using the FUNFIT computer program (Ref. 4-12). The exponent of F (0.6) is the value which makes the uncertainty a minimum. This is shown in Fig. 4-3 where the standard deviation, computed with FUNFIT, is plotted versus the exponent of F.

The uncertainty analysis of the data in Table 4-3 leads to the following:

$$(R/B)_{95\%} = (R/B) e^{\pm 1.645S} \quad , \quad (4-2)$$

where $(R/B)_{95\%}$ is the steady-state fractional release at the 95% confidence limits, (R/B) is the steady-state fractional release determined according to Eq. 4-1, and S is given by

$$S = \sqrt{0.188 - 0.1063 F^{0.6} + 0.0181 F^{1.2}} \quad . \quad (4-3)$$

The + or - sign (Eq. 4-2) is chosen to calculate the upper or lower 95% confidence limit, respectively. The confidence limits are shown in Fig. 4-4.

Refinement in Description of R/B Dependence on Burnup. At very low burnups, fission gas release from dense (98% theoretical density) ThO_2 kernels is thought to occur by direct recoil into the surrounding buffer

TABLE 4-3
R/B, BURNUP, ESTIMATED STANDARD DEVIATIONS, AND WEIGHTING FACTORS

F (% FIMA)	(R/B) _{Kr-85m, 1100°C}	Estimated Std. Dev. (%)	Weighting ^(a) Factor	Symbols ^(b)
~0	1.2×10^{-3}	20 ^(c)	0.25	○
0.9	4.0×10^{-3}	40 ^(d)	0.0625	◇
1.9	2.0×10^{-3}	40 ^(d)	0.0625	□
2.35	1.5×10^{-3}	300	0.00111	⬆
3.5	7.5×10^{-3}	20 ^(c)	0.25	○
7.0	1.6×10^{-2}	20 ^(c)	0.25	○
7.9	2.2×10^{-2}	20 ^(c)	0.25	○
9.18	2.0×10^{-2}	20 ^(c)	0.25	○
9.18	1.2×10^{-2}	20 ^(c)	0.25	△
9.18	1.3×10^{-2}	20 ^(c)	0.25	△
11.1	1.6×10^{-2}	20 ^(c)	0.25	○

(a) These weighting factors apply to the dependent variable, R/B. The weighting factor for the independent variable, % F, is 1.0 for all values. This corresponds to an estimated standard deviation of 10%.

(b) As shown in Fig. 4-2.

(c) The value of 20% was estimated by calculating the standard deviation for the CEA data at 7, 7.9, 9.18, and 11.1% FIMA; this value is twice the error given in Ref. 4-4. It is also applied to the GA data.

(d) The value of 40% was estimated on the basis of the data at 0.9 and 1.9% FIMA taking into account that the data at these burnups are averages over the measured values.

Fig. 4-3. Dependence of the standard deviation on the exponent of F

Fig. 4-4. R/B for Kr-85m at 1100°C versus burnup (% FIMA) with 95% confidence limits (data from Table 4-3)

coating. This process is dominant over diffusional release from the kernel for temperatures below $\sim 1300^{\circ}\text{C}$. The recoiled fission gases are subsequently released from the buffer by diffusion.

At very low burnups, the effect of increasing burnup is to retard fission product release from both fuel and graphite. This behavior is observed for fission gases in ThO_2 and UO_2 fuel (Refs. 4-13 through 4-17) and for cesium in graphite (Ref. 4-18). The retardation can be ascribed to trapping of the fission products at sites of radiation-induced damage. One can reasonably also expect similar behavior in the buffer for the fission gases recoiled from the kernel.

This behavior is factored into the present description of the burnup dependence of R/B to provide (1) a reasonable description as burnup approaches zero and (2) a smooth connection with the Fuel Design Data Manual treatment of fission gas release (Ref. 4-19).

With the increasing burnup, the release of fission gases from the ThO_2 kernel increases (Refs. 4-1 and 4-3) due to an increase in internal surface area and connected porosity. As a consequence, the increased release from within the kernel becomes more important than the release of fission gases which have recoiled into the buffer surrounding the kernel. This results in a minimum in the relationship between R/B and burnup. The minimum will be represented by the datum of Table 4-1 which is assigned a nominal burnup of 0% FIMA (in Table 4-1) but which actually corresponds to a burnup of 0.015% FIMA (based on the data in Ref. 4-5).

The value of R/B at zero burnup will be taken to be 0.005. The true value is not known. However, given the form of the R/B versus burnup curve, which as described above has a minimum at very low burnup, the effect of choosing another value is practically negligible.

Analytical Expression for Dependence of R/B on Burnup. By combining the above data, a general analytical expression for the dependence of R/B on burnup is given by the following:

$$(R/B)_{T,F} = (0.133 + 0.828 F^{0.6} + 0.867 e^{-205F}) (R/B)_T, \quad (4-4)$$

where F is % FIMA and $(R/B)_T$ is the R/B value calculated at temperature T and 0% FIMA. This expression applies to the nuclides of Kr and is assumed to apply to Xe, I, and Te nuclides. It applies to burnups less than 11% FIMA. It does not apply to bromine because the release of bromine from oxide (UO_2 and ThO_2) kernels is apparently greater than krypton (Refs. 4-17 and 4-20).

Equation 4-4 is plotted, along with the data, in Fig. 4-5 for Kr-85m at 1100°C. Included in Fig. 4-5 are curves representing 95% confidence limits calculated using Eqs. 4-2 and 4-3.

Review of Available Data for Silver Transport Relative to Medium-Enriched Uranium (MEU) Fuel

Introduction and Summary. Silver isotopes are more important in low-enrichment fuel than in high-enrichment fuel because of the relatively large yield of these isotopes from plutonium fissions. The most important silver isotope is 253-d Ag-110m, which is produced by neutron activation of Ag-109. The gamma ray dose rate of Ag-110m is about five times that of Cs-137. The fission yields of Ag-109 are 0.044% for U-233, 0.030% for U-235, and 1.40% for Pu-239. Estimated Ag-110m activities at end of life (4 years) are 15.2 mCi/g U for medium (20%) enriched (MEU) fuel and 4.7 mCi/g U for high (93.15%) enriched (HEU) fuel (Ref. 4-21). Therefore, even though the fission yield of Ag-109 for Pu-239 is about 47 times that for U-235, the activity of Ag-110m in 4-year MEU fuel is only about 3.2 times that in 4-year HEU fuel.

Available information relative to silver transport in MEU fuel was reviewed. The results indicate:

1. The fractional release of silver from coated fuel particles is at least a factor of ten greater than that of Cs.

Fig. 4-5. R/B for Kr-85m at 1100°C versus burnup (% FIMA) with 95% confidence limits (data from Tables 4-1 and 4-2)

2. Silver release from TRISO particles is much lower than that from BISO particles.
3. Diffusion of silver in the kernel is the governing mechanism for silver release from BISO particles. For TRISO particles, diffusion in the SiC coating is governing.
4. Release of silver from dense carbide fuel is higher than from dense UO_2 and $(\text{Th},\text{U})\text{O}_2$. (There are no data on silver release from oxycarbide fuel.)
5. Silver apparently migrates along grain boundaries in SiC.
6. The migration rate of silver in SiC significantly increases at fuel operating temperatures above 1200°C in-pile.
7. Below about 1200°C , the release of silver from intact TRISO particles during fuel life is negligible.

The basis for these conclusions is given below, following a description of sources of information on silver release.

Sources of Silver Transport Data. Nabielek et al. (Ref. 4-22) in a recent paper presented considerable data on the release of silver from fuel particles and very adequately described the status of information on silver release. Most of the above conclusions were derived from their paper.

These investigators derived diffusion coefficient data for silver in UO_2 and $(\text{Th},\text{U})\text{O}_2$ kernels and for silver in SiC. These data were obtained from (1) analysis of silver release during irradiation experiments in the Dragon reactor, (2) annealing of BISO and TRISO particles at 1500°C , and (3) literature sources.

Earlier, Brown and Faircloth (Ref. 4-23) presented diffusion coefficient data for silver in UO_2 kernels, derived from the Ag-110m distribution between the kernel and inner pyrocarbon and buffer layers for irradiated intact TRISO particles.

Offermann (Ref. 4-24) used a helium backscattering technique to measure diffusion coefficients for silver in pyrocarbon at temperatures between 450° and 800°C.

York and Hick (Ref. 4-25) performed gamma-spectrometric studies of fuel elements withdrawn from the Dragon reactor. They observed that Ag-110m is always released to a greater extent than Cs-137 with the difference being at least one order of magnitude and sometimes up to four orders. They concluded that SiC retains silver up to about 1250°C.

Adams et al. (Ref. 4-26) at General Atomic measured the in-pile release of Ag-110m from coated particles irradiated in capsules P13L and P22. In this work calculated activities of Ag-110m in the particles were compared with measured activities. The calculated activities were obtained utilizing the FISS/PROD computer code, and the measured activities were obtained by radiochemical analysis. The work of Adams et al. is described in some detail below.

The FISS/PROD Ag-110m computations used (1) 0.03% U-235 and 0.044% U-233 fission yields for the Ag-109 production, (2) uranium loadings based on observed Ru-106 activities, (3) a cross section of 82 barns for the burnup of Ag-110m during the irradiation, (4) nickel-cobalt wire flux monitor data for the thermal and fast fluxes, and (5) a Ag-109 capture cross section of 5.78 barns for the reactor spectrum. The calculated Ag-110m activities are probably on the low side because the epithermal neutron contribution was not considered. Sample sizes were one or two particles.

The radiochemical analysis involved (1) dissolution of the particle sample, (2) addition of silver carrier, (3) double precipitation as chloride, and (4) gamma-counting of the precipitate.

The data obtained by Adams et al. are given in Table 4-4. It can be seen that the calculated and observed Ag-110m activities agree within a factor of 1.3 for the TRISO coated particles. In each of the three cases, the calculated dpm values are lower than the observed values, perhaps because, as mentioned above, the epithermal neutron contribution was not considered. The agreement between the calculated and observed Ag-110m activities for the TRISO particles suggests that the TRISO coatings were effective in retaining silver nuclides. However, due to the uncertainty of the data and the fact that two large numbers are being compared, a release of less than about 10% would not be discernible.

As shown in Table 4-4, silver releases from the BISO coated particles were relatively high (0.65 and 0.72), and the data indicate little difference in the release behavior of BISO UO_2 and BISO UC_2 particles.

Silver Release from Carbide Kernels. The Dragon data (Refs. 4-22 and 4-25) indicate higher silver release from carbide kernels than from oxide kernels. This behavior may be due to a higher solubility of silver in the oxide or to lower escape tendency (thermodynamic activity) for silver in the oxide. The Dragon data further indicate that the release of silver from carbide kernels is several orders of magnitude higher than the release of cesium.

The data of Adams et al. given in Table 4-4 indicate little difference between silver release from the carbide and oxide kernels irradiated to high burnup. Perhaps the relative silver release behavior for the carbide and oxide kernels changes with burnup.

Silver Release from Oxide Kernels. Nabielek et al. (Ref. 4-22) gave the following expression for the reduced diffusion coefficient of silver in UO_2 and $(Th,U)O_2$ kernels:

TABLE 4-4
IN-PILE FRACTIONAL RELEASE OF Ag-110m FROM COATED FUEL PARTICLES

Capsule No.	Particle ^(a) Identification No.	Coating Type	Kernel Type	Approx. Irrad. Temp. (°C)	Burnup (% FIMA)	Ag-110m Calculated (dpm)	Ag-110m Found (dpm)	Ag-110m Fractional Release
P22 ^(b)	4155-151E	TRISO-II	(Th,U)C ₂	920	20	7.0 x 10 ⁵	8.8 x 10 ⁵	<0.1 ^(c)
P13L ^(b)	4413-35E-C5T6	BISO-LTI	UO ₂	900	68	5.1 x 10 ⁵	1.8 x 10 ⁵	0.65
P13L	4403-113E-C5T2	BISO-LTI	UC ₂	900	68	3.9 x 10 ⁵	1.1 x 10 ⁵	0.72
P13L	4413-5E-C1T3	TRISO-LTI	UC ₂	1375	72	7.3 x 10 ⁵	7.5 x 10 ⁵	<0.1
P13L	4403-143E-C2T3	TRISO-LTI	UC ₂	1250	73	8.7 x 10 ⁵	9.5 x 10 ⁵	<0.1

(a) One or two particles were used in each of the five measurements. Descriptions of the particles used are given in Ref. 4-27 for P22 and Ref. 4-28 for P13L. It should be noted that in capsules P22 and P13L, particles from the same batch were irradiated at several different positions and temperatures.

(b) Irradiation times (full power days) were 266 for P22 and 273 for P13L.

(c) A fractional release of less than about 10% was not discernible.

$$D' = 0.0550 \exp \left(\frac{-213000}{RT} \right) (s^{-1}) \quad , \quad (4-5)$$

where D' is the reduced diffusion coefficient, 213000 is in units of Joules per mol, and R is 8.314. This expression is based on reduced diffusion coefficient data derived from measured silver release for fuel bodies irradiated in the Dragon reactor. The mean burnup for the particles in the fuel bodies was about 12% FIMA. Nabielek et al. state that the above expression represents an upper envelope to the data.

Brown and Faircloth (Ref. 4-23) obtained information on the release of silver from 800 μm , low-enriched, 80% dense UO_2 kernels. They determined the Ag-110m distribution between the kernel and the inner pyrocarbon and buffer layers for a number of intact TRISO particles irradiated under various conditions of time (up to 750 days), temperature (1000° to 1400°C), and burnup [6% to 12% FIMA (Ref. 4-29)]. The outcome of this work leads to the following expression:

$$D' = 3.62 \times 10^{-3} \exp \left(\frac{-361880}{RT} \right) (s^{-1}) \quad , \quad (4-6)$$

where the units are the same as for Eq. 4-5. This expression is apparently not significantly different from Eq. 4-5 in the temperature range from 1000° to 1400°C.

On the basis of the data of Nabielek et al. (Ref. 4-22), the fractional release calculated for the conditions of the measurements of Adams et al. (Ref. 4-26) for BISO UO_2 particles in capsule P13L (see Table 4-4) is about 0.05. This value can be obtained either by using Eq. 4-5 and the curve of fractional release versus $D't$ presented by Nabielek et al. (Ref. 4-22) or from Eq. 4-5 and the relation for fractional release, f , given by

$$f = \frac{4}{\sqrt{\pi}} (D't)^{1/2} \quad , \quad (4-7)$$

which provides a good estimate of in-pile fractional release up to a release of about 0.6. In the calculation of fractional release, the irradiation temperature was 900°C and the irradiation time was 2.36×10^7 s (273 d).

The fractional release value of 0.05 calculated above from the data of Nabielek et al. is about 13 times smaller than the value 0.65 (see Table 4-4) found by Adams et al. The data of Nabielek et al. are based on particles with a burnup of around 12% FIMA, whereas the data of Adams are for BISO UO_2 particles with a burnup of 68% FIMA. On the basis of these results, it is suggested that the reduced diffusion coefficient for silver release from UO_2 is strongly dependent on burnup.

Using the data of Nabielek et al. and the data of Adams et al. for UO_2 particles, the reduced diffusion coefficient, including a burnup dependence, can be represented by

$$D' = 2.9 \times 10^{-3} (1 + 0.0104 F^3) \exp \left(\frac{-213000}{RT} \right) (s^{-1}) \quad , \quad (4-8)$$

where F is % FIMA. This expression has been derived by (1) using the data of Table 4-5, (2) assuming the burnup dependence at 900°C is the same as at other temperatures, and (3) choosing a conservative value of $D' = 10^{-12} s^{-1}$ at zero burnup and 900°C.

The burnup dependence of Eq. 4-8, F^3 , is similar to that which can be derived from data on cesium release from ThO_2 kernels (Ref. 4-30).

Silver Migration in BISO Coatings. Analyses of in-pile irradiation experiments (Refs. 4-22 and 4-25) lead to the conclusion that at HTGR fuel temperatures there is practically no holdup of silver in dense pyrocarbon.

This same conclusion can be derived by extrapolation of Offermann's results (Ref. 4-24) to HTGR fuel operating temperatures. Diffusion

TABLE 4-5
DATA USED IN DERIVING THE BURNUP DEPENDENCE FOR THE
REDUCED DIFFUSION COEFFICIENT OF SILVER IN UO_2

D' (1/s)	T (°C)	Average % FIMA	Fractional Release
1.8×10^{-11} (a)	900	12	$0.05^{(b)}$
3.5×10^{-9} (c)	900	68	0.65

(a) Calculated using Eq. 4-5.

(b) Calculated as described in text.

(c) Calculated using Eq. 4-7.

coefficients for silver in pyrocarbon (both HTI and LTI) determined by Offermann between 450° and 800°C (Ref. 4-24) are described by the expression

$$D = 10^{-8} \exp \left(\frac{-164000}{RT} \right) \text{ (m}^2\text{/s)} \quad , \quad (4-9)$$

where 164000 is in units of Joules per mol and R is 8.314. From this expression, one calculates $D = 1.9 \times 10^{-15} \text{ m}^2\text{/s}$ at the typical HTGR fuel operating temperature of 1000°C. From this value and using the relation $t = x^2/6D$, one estimates a delay time of only 2.5 days for silver to migrate through a 50- μm -thick pyrocarbon coating, indicating little holdup of silver in pyrocarbon.

Silver Diffusion in SiC. Nabielek et al. (Ref. 4-22) analyzed (1) data on silver release from TRISO particles during irradiation experiments performed in the Dragon reactor and (2) results of out-of-pile anneal tests conducted by Brown and Faircloth (Ref. 4-23).

In the anneal tests, irradiated TRISO particles were annealed at 1500°C for periods up to 210 days. Nabielek et al. report that no release of cesium or strontium above the contamination level was observed, but silver started to be released after 10 days. The shape of the resulting silver release curve is not consistent with diffusion of silver in SiC, but shows the SiC layer to become gradually transparent to silver. As stated by Nabielek et al., this behavior has the appearance of a changeover from "good" to "bad" SiC. Nabielek et al. further report that the deterioration of SiC (i.e., changeover from "good" to "bad" SiC) appears to occur in-pile at fuel operating temperatures above 1200°C and, to a lesser extent, during out-of-pile annealing.

Nabielek et al. derived effective diffusion coefficient data for silver in SiC from the in-pile and out-of-pile silver release results.

They plotted the data against temperature and obtained an upper limit curve given by the expression

$$D_{\text{SiC}} = 6.74 \times 10^{-9} \exp \left(\frac{-213000}{RT} \right) (\text{m}^2/\text{s}) \quad , \quad (4-10)$$

where the activation energy 213000 J/mol is the same as for silver in UO_2 and R is 8.314. This expression is applicable to "bad" SiC and to "good" SiC after deterioration under irradiation.

The silver release data derived from the P13L and P22 capsule irradiation experiments show the effectiveness of the TRISO coating in retaining silver (see Table 4-4). The three TRISO coated carbide samples showed silver releases of less than 10%, whereas the BISO coated particles showed high releases. (Releases for the TRISO particles may have been much less than 10%, but the analysis method used could not discern releases of less than 10%.) The TRISO particles in capsule P13L showed especially good performance considering that the particles were irradiated to high burnup (70%) at the high temperatures of 1250° and 1375°C.

Based on the above information on silver diffusion in SiC, it is concluded that (1) TRISO coatings with "good" SiC effectively retain silver at temperatures less than 1200°C, (2) "good" SiC converts to "bad" SiC during irradiation at temperatures above about 1200°C, and (3) Eq. 4-10 gives the diffusion coefficient for silver in "bad" SiC.

Plutonium Transport in HTGR Systems

Introduction and Summary. This section is an interim report on (1) an evaluation of the importance of plutonium in HTGR systems from the standpoint of reactor safety and (2) a review of plutonium transport information. The evaluation and review were performed because of interest in MEU or LEU fuel for HTGR systems.

In summary, the evaluation of the importance of plutonium in HTGR systems involved (1) establishing plutonium nuclide activities in MEU and HEU HTGR fuel, (2) determining the predominant plutonium nuclides from the safety standpoint, and (3) estimating limitations for the release of plutonium nuclides into the primary circuit. The evaluation led to the following conclusions. Plutonium is potentially very important as a radiological hazard in HTGR systems. Plutonium-238 (87.4 y) is the dominant plutonium nuclide for both MEU and HEU fuel systems. Tolerable levels of plutonium release into the primary circuit are more than 10 times lower than the allowable fraction of uranium contamination (10^{-4}) in fuel rods; therefore, holdup of plutonium in fuel rods or fuel element graphite is required.

The review of plutonium transport information leads to the following conclusions, qualified by the meagerness of the information. Plutonium release from TRISO particles will be insignificant (i.e., less than contamination levels) under reactor conditions. Plutonium release from BISO particles and failed particles could be significant (i.e., above contamination levels). Plutonium release is higher from carbide and oxycarbide kernels than from oxide kernels. Plutonium released from fuel and that resulting from fuel contamination will hold up in the graphite if it escapes from the fuel rod.

Importance of Plutonium in HTGR Systems from Safety Standpoint. The importance of plutonium in HTGR systems from the standpoint of reactor safety is being evaluated (Ref. 4-31), and an interim report of the results follows.

In terms of potential radiological hazard, Pu-238 (87.4 y) is the dominant plutonium nuclide for both MEU and HEU fuel systems. Plutonium-238 has a high bone inhalation effectivity (rem/Ci, about 500 times that of Sr-90) and has a relatively high fuel activity. Plutonium-238 is produced mainly from U-235; therefore, the activity of this nuclide in 4-y HEU fuel is about the same as in 4-y MEU fuel.

The estimated tolerable fractional release (release rate/birth rate) of Pu-238 from the core into the primary circuit of a large [3400-MW(t)] HTGR with gas reheat is around 9×10^{-6} . (It should be noted that these release limits for Pu-238 are based on the assumption that the bone dose is exclusively from Pu-238.) For comparison, tolerable Sr-90 fractional releases are calculated to be 1.3×10^{-4} for the large HTGR.

The present fuel specification allows 10^{-4} fraction uranium contamination (i.e., 10^{-4} fraction of uranium in fuel rods outside of intact coated particles, which is equivalent to an R/B of 3×10^{-5} for Kr-85m at 1100°C). Therefore, attenuation of plutonium migration by fuel rod matrix material and fuel block graphite is required.

For the large HTGR with gas reheat, levels of Pu-238 and Sr-90 in the primary circuit are limited by the reheater header accident, which leads to the highest calculated releases of these two nuclides to the environment.

Of the other plutonium nuclides, only Pu-241 (14.3 y) appears to contribute appreciably, relative to Pu-238, in accident situations. For MEU fuel systems, estimated dose contributions of Pu-241 are less than 50% of the Pu-238 doses. For HEU fuel systems, estimated dose contributions of Pu-241 are less than 1% of the Pu-238 doses.

Review of Plutonium Transport Information. This section is an interim report on the results of a review of available data on plutonium transport in graphite, coating, and kernel materials.

Orth (Ref. 4-32) measured diffusion losses of lanthanide and actinide elements from graphite at temperatures from 1600° to 2600°C. He found the diffusion rate of plutonium in graphite to be much higher than that of uranium.

Table 4-6 compares vapor pressures of carbides and oxides of plutonium and uranium at 1227°C. It can be seen that PuC_2 has a relatively high vapor pressure, suggesting that plutonium release from carbide fuel kernels will be relatively high. Solid solution formation in the fuel kernels will reduce the plutonium vapor pressures substantially. Langer (Ref. 4-37) earlier discussed the relative vapor pressures of actinides over their carbides.

Potter (Ref. 4-38) determined vapor pressure data for plutonium oxycarbide ($\text{PuC}_{0.5}\text{O}_{0.5}$) using the Langmuir free evaporation method. The resulting vapor pressure data are unreasonably high. His vapor pressure equation yields a value of 3×10^{-7} atm at 1227°C, which is much higher than the values given in Table 4-6. He attributed the unreasonably high vapor pressures to a too low value for the evaporation surface area.

Baldwin et al. (Ref. 4-39) studied the diffusion of plutonium in high-temperature isotropic (HTI) pyrocarbon over the temperature range 1200° to 1800°C. Their data indicate that the diffusion coefficient for plutonium in isotropic pyrocarbon at 1000°C is about two orders of magnitude higher than that for uranium and several orders of magnitude higher than that for thorium. They reported the equation

$$D = 4.47 \times 10^{-8} \exp \left(\frac{-230000}{RT} \right) (\text{m}^2/\text{s}) \quad , \quad (4-11)$$

where D is the diffusion coefficient for plutonium in HTI pyrocarbon, 230000 is in units of Joules per mol, and R is 8.314.

Buzzelli (Ref. 4-40) measured in-pile releases of plutonium and certain fission product metals from loose intact BISO coated $(\text{Th},\text{U})\text{C}_2$ particles irradiated in capsules P17 and P18 and loose intact TRISO coated $(\text{Th},\text{U})\text{C}_2$ particles irradiated in capsule P22. A brief description of his work and an analysis of his results are given below.

TABLE 4-6
VAPOR PRESSURES AT 1227°C (1500 K)

System	Total Pressure (atm)	Principal Vapor Species	Ref.
$\text{PuC}_2 + \text{C}$	6.8×10^{-10}	Pu	4-33
$\text{UC}_2 + \text{C}$	6.7×10^{-15}	U	4-34
PuO_2	1.0×10^{-12}	PuO, PuO_2	4-35
UO_2	1.1×10^{-12}	UO_2	4-36

Description and Analysis of Buzzelli's Work on Plutonium Transport.

Buzzelli measured the in-pile fractional release of Pu, U, Th, Sr-90, Cs-137, and Ce-144 for BISO coated (Th,U)C₂ particles (lot 3663-39E) irradiated in capsules (irradiation experiments) P17 (position C5T1,TC-W13) and P18 (position C3T1,TC-W7) and TRISO coated (Th,U)C₂ particles (lot 4155-151E) irradiated in capsule P22 (position C3T5,TC-W9). The BISO coated particles are described in Ref. 4-41 and the TRISO coated particles in Ref. 4-42. The particles contained fully enriched (93%) uranium. Irradiation times (full power days) were 159 for P17, 264 for P18, and 266 for P22.

Calculated average irradiation temperatures for the particle samples are 1322°C for P17, 1533°C for P18, and 1368°C for P22. These temperatures were derived from temperature analyses of capsules P17, P18, and P22 (Ref. 4-43). In the analyses, indicated temperatures were corrected for thermocouple decalibration. For P18, the calculated average temperatures for the three thermocouples in cell 3 are 1437°C for W7, 1531°C for W8, and 1536°C for W9. Examination of the P18 calculated temperature data (Ref. 4-43) indicates that the temperature value (1437°C) for thermocouple W7 was anomalously low; therefore, the average irradiation temperature for the particle sample in P18 was taken to be 1533°C, which is the average of the calculated temperatures for thermocouples W8 and W9.

During the capsule irradiations, each of the three particle samples was contained in a graphite thimble inserted into the graphite body of the capsule. Thimble wall thicknesses were 0.86 mm (0.034 in.) for capsule P17 and 1.07 mm (0.042 in.) for capsule P18.

To obtain the release data, Buzzelli measured nuclide inventories in the particles, thimbles,* and graphite bodies. Results for Pu, U, Th, and Sr-90 are given in Table 4-7. These results indicate:

1. The fractional release of plutonium from the TRISO particles was low (1.0×10^{-5}) and can be attributed to contamination. Thus,

* It is not known for sure if the graphite thermocouple wells were analyzed along with the thimbles.

TABLE 4-7
RELEASE FRACTIONS AND DISTRIBUTION FOR INTACT BISO AND TRISO
PARTICLES IN P17, P18, AND P22(a)

Species	Fractions Found in Thimble and Crucible				
	BISO (Th,U)C ₂ Lot 3663-39E P17, Pos. C5T1		BISO (Th,U)C ₂ Lot 3663-39E P18, Pos. C3T1		TRISO (Th,U)C ₂ Lot 4155-151E P22, Pos. C3T5
	Thimble	Graphite Body	Thimble	Graphite Body	Thimble
Pu	1.3×10^{-3}	1.4×10^{-5}	2.1×10^{-2}	1.1×10^{-5}	1.0×10^{-5}
U	7.9×10^{-5}	8.9×10^{-5}	2.1×10^{-4}	1.6×10^{-4}	5.6×10^{-5}
Th	1.2×10^{-4}	1.3×10^{-4}	1.4×10^{-3}	2.1×10^{-5}	2.6×10^{-5}
Sr-90	0.09	0.01	0.35	0.41	Low

(a) From Ref. 4-40.

(b) This value is unreasonably high compared to the other values for Th and U and is probably in error.

the SiC coating was effective in retaining plutonium at the relatively high irradiation temperature of 1368°C.

2. Fractional releases of plutonium from the BISO particles were relatively high (0.0013 for P17 and 0.021 for P18).
3. Essentially all the released plutonium stopped in the thin-walled graphite thimble that contained the particles, indicating that the diffusion rate of plutonium in graphite is low.
4. Uranium and thorium fractions outside the fuel particles were low ($<10^{-3}$) and can be attributed to fuel contamination.
5. Fractional releases of strontium from the BISO particles were relatively high (10% for P17 and 76% for P18), and a large fraction of the strontium was found in the graphite crucible (outside the graphite thimble).
6. Plutonium releases were very much lower than strontium releases.

An estimate of the diffusion coefficient of Pu in (Th,U)C₂ kernels has been made using the data of Buzzelli. Data used in the analysis are given in Table 4-8. Temperatures given in the table were derived from a temperature analysis of capsules P17 and P18 (see above). In analyzing the data of Table 4-8, it was assumed that the coatings of the particles were not a barrier to release of plutonium. This implies that essentially all the plutonium released from the (Th,U)C₂ kernels was found in the thimbles.

From the data of Table 4-8, the reduced diffusion coefficient D' is calculated according to the relation

$$f = \frac{4}{\pi^{1/2}} \sqrt{D' t} \quad , \quad (4-12)$$

TABLE 4-8
 DATA ON PLUTONIUM RELEASE FROM BISO (Th,U)C₂
 PARTICLES (LOT 3663-39E) IRRADIATED IN CAPSULES P17 AND P18

Capsule	Position	T (°C)	Irradiation Time (d)	Burnup (% FIMA)	Release Fraction
P17	C5T1	1322	159	14	0.0013
P18	C3T1	1533	264	18	0.021

where f is the fractional release and t is the irradiation time. This relation provides a good estimate of the in-pile fractional release of metals up to a release of about 0.6.

The calculated values of D' are $2.41 \times 10^{-14} \text{ s}^{-1}$ at 1322°C and $3.80 \times 10^{-12} \text{ s}^{-1}$ at 1533°C . Combining these leads to the expression

$$D' = 1.4 \times 10^5 \exp \left(\frac{-573000}{RT} \right) (\text{s}^{-1}) , \quad (4-13)$$

where D' is the reduced diffusion coefficient, the activation energy (573000) is in units of Joules per mol, and R is 8.314. This activation energy is reasonable for the diffusion of a heavy metal in kernel material.

For a particle of $200 \mu\text{m}$ diameter ($= 2a$), the diffusion coefficient (D) is estimated from the relation $D = D'a^2$ to be

$$D = 1.4 \times 10^{-3} \exp \left(\frac{-573000}{RT} \right) (\text{m}^2/\text{s}) , \quad (4-14)$$

which gives the diffusion coefficient for plutonium in a typical $(\text{Th,U})\text{C}_2$ kernel.

The diffusion coefficient for plutonium migration in $(\text{Th,U})\text{C}_2$ is now compared with the diffusion coefficient for migration in pyrocarbon by forming the ratio of the diffusion coefficients. Using the diffusion coefficient for plutonium migration in pyrocarbon given by Eq. 4-11, one obtains

$$\frac{D_{\text{pyro}}}{D_{\text{kernel}}} = 3.2 \times 10^{-5} \exp \left(\frac{343000}{RT} \right) . \quad (4-15)$$

Thus, for temperatures below about 1600°C , the diffusion coefficient for plutonium in pyrocarbon exceeds that in $(\text{Th,U})\text{C}_2$ by a factor greater than

10^5 . Consequently, for release of plutonium from the BISO particle, the kernel controls the release in accord with the assumption on which the analysis was based. Therefore Eq. 4-13 should be a valid estimate of the diffusion coefficient of plutonium in $(\text{Th,U})\text{C}_2$.

Equation 4-13 applies to $(\text{Th,U})\text{C}_2$ kernels which have experienced burnups of 14% to 18% FIMA. The diffusion coefficient may increase with burnup, based on the diffusion behavior of other metals in fuel kernels.

Subtask 120: Fission Product Methods Validation

Assessment of Equivalency of TRAFIC and FIPERQ Using COPAR Option

Summary. The equivalency of the TRAFIC and FIPERQ fission metal release codes has been reassessed using the COPAR option. Release from a single ten-row FSV-type fuel element has been modeled over 1557 days of irradiation. The results confirm the findings of the earlier TRAFIC-FIPER comparison showing that in each case the codes give similar results for absolute release, fractional release, partition coefficients, and loading in graphite. The discrepancy in the source terms noted previously is still present when using the COPAR option. Overall, this study again confirms the equivalency of the two codes.

Discussion. A recent comparison of TRAFIC and FIPERQ showed that the two codes give essentially equivalent answers when modeling a given reactor operating history (Ref. 4-44). The simplifying assumptions of total retention of fission metal inventory in intact particles and total release from failed particles were used. With the recent introduction of COPAR (Ref. 4-45) to both codes, several of the test cases have been rerun to ensure that FIPER and TRAFIC still provide equivalent results.

For this analysis, the release of cesium and strontium from the fuel particles was modeled using the following assumptions:

Intact UC ₂ TRISO fissile fuel	No release
Failed UC ₂ TRISO fissile fuel	Total release upon failure
Intact and failed ThO ₂ BISO fissile fuel	Diffusive release of cesium (Ref. 4-8) and strontium (Ref. 4-46)

Table 4-9 presents a summary of the major input assumptions used in the analysis. (Reference 4-44 should be reviewed if additional details are required.) Table 4-10 presents a summary of results for the present study, as well as the findings for analogous cases in the previous comparison.

The results of all three cases can be summarized as follows:

1. The overall releases of both cesium and strontium are in very close agreement at all time points.
2. The fractional releases show greater variation than the total releases since FIPER and TRAFIC generate source terms that differ by ~9%, FIPER yielding the larger source. This is in agreement with the findings of the previous comparison.
3. The partition coefficients are in close agreement.
4. The metal loadings in graphite show acceptable agreement in view of the larger uncertainty associated with these numbers.

The cesium releases in the present study are less than those predicted earlier. This is not surprising since modeling the retention of cesium in failed oxide BISO particles more than compensates for diffusive release from intact BISO particles also modeled by COPAR. Strontium release is seen to be slightly greater in the current analysis, indicating that diffusive release from intact BISO particles provides an important source for release.

TABLE 4-9
SUMMARY OF MAJOR INPUT ASSUMPTIONS USED IN TRAFIC-FIPERQ COMPARISON

Case No.	Length of Irradiation (days)	Fissile Failure Fraction	Fertile Failure Fraction	Temperature	Power (W/cm ³)	Comments
1 ^(a)	1557	1%	Step increase from 0 to 1% at 779 days	Constant ^(b)	30	Base case - model release from intact and failed fertile particles
2 ^(c)	1557	Variable	Variable	Variable	Variable	Model cesium release with varying power, temperature, and failure fraction history
3 ^(d)	1557	Variable	Variable	Variable	Variable	Model strontium release with varying power, temperature, and failure fraction history

(a) Same as case 2 of Ref. 4-44.

(b) Fuel centerline = 1071°C, fuel rod outer edge = 1004°C, graphite inner edge = 949°C, and graphite outer edge = 927°C.

(c) Same as case 5 of Ref. 4-44.

(d) Same as case 6 of Ref. 4-44.

TABLE 4-10
COMPARISON OF FIPERQ AND TRAFIC OUTPUT FOR HYPOTHETICAL IN-PILE OPERATING CONDITIONS

Case No.	FIPERQ					TRAFIC				
	Source (Ci)	Accumulated Release (Ci)	Fractional Release	ϕ_{Final}	Graphite Loading (Ci)	Source (Ci)	Accumulated Release (Ci)	Fractional Release	ϕ_{Final}	Graphite Loading (Ci)
1. (Cesium)										
A. Present study	9,827	48.7	0.0050	0.79	5.4	9,020	48.3	0.0054	0.74	4.1
B. Ref. 4-44	9,869	68.5	0.0069	0.81	11.1	9,020	66.0	0.0073	0.76	9.3
2. (Cesium)										
A. Present study	10,827	60.6	0.0056	0.82	6.1	9,940	59.6	0.0060	0.78	4.7
B. Ref. 4-44	10,872	77.6	0.0071	0.82	7.8	9,940	73.4	0.0074	0.79	6.5
3. (Strontium)										
A. Present study	11,294	9.06×10^{-3}	8.0×10^{-7}	7.5	14.0	10,300	9.85×10^{-3}	9.6×10^{-7}	7.7	11.7
B. Ref. 4-44	11,342	4.87×10^{-3}	4.3×10^{-7}	6.5	8.1	10,300	5.13×10^{-3}	5.0×10^{-7}	6.9	6.5

TASK 200: COOLANT CHEMISTRY

Subtask 210: Coolant Impurities/Core Materials Interaction

A new series of experiments to determine the effect of fuel hydrolysis on the steady-state fission gas release (R/B) of irradiated UC_2 and $(Th,U)C_2$ fuel particles has been started. This work will complement similar work performed previously on unirradiated fuel particles (Ref. 4-47). The specimens will be laser-drilled TRISO coated UC_2 and $(U,Th)C_2$ having burnups of 8, 20, and 50% FIMA. The particles will be incorporated into fuel rods prepared from standard matrix material, shim, and inert fuel particles. The test rods will then be loaded into H-451 graphite crucibles having a wall thickness of 5 mm, which simulates the web thickness in the HTGR fuel element. The test rods will undergo an initial R/B test in the TRIGA in-core King furnace facility and will then be exposed to conditions simulating (1) full-power reactor operation with continuous moisture ingress (i.e., 10 Pa water vapor in He at 900°C) and (2) shutdown conditions following a major steam ingress event (i.e., 2000 Pa water vapor in He at 250°C). The R/B of the test rods will be measured at intervals of approximately 100 to 500 h during the exposure. The total exposure time will be approximately 2000 h.

TASK 300: REACTOR FISSION PRODUCT AND COOLANT CHEMISTRY SURVEILLANCE

Fission gas activities and impurity levels in the primary coolant of the Fort St. Vrain reactor are being measured at power steps during the rise-to-power program.

Subtask 310: Fort St. Vrain Fission Product Surveillance

Fission gas activities and R/B data were obtained at 50.9% reactor power as shown in Table 4-11. The calculated average fuel temperature attained at this power level was 658°C (1217°F).

TABLE 4-11
MEASURED STEADY-STATE ACTIVITIES AND R/B VALUES AT 50.9% POWER
DURING FSV RISE TO POWER

Measurement ^(a)	Kr-85m	Kr-87	Kr-88	Kr-89	Xe-135	Xe-137	Xe-138
Activity ($\mu\text{Ci}/\text{cm}^3 \times 10^4 \text{ STP}$)	6.75	15.5	14.3	5.92	17.7	5.33	16.6
R/B $\times 10^5$	0.54	0.43	0.34	0.11	0.43	0.07	0.18

(a) Data points corrected for 2.5-min delay time between primary coolant and sampling point.

The measured R/B values are low (10^{-6} range) indicating continued satisfactory retention of fission gases by the fuel and thus satisfactory performance of the fuel.

Subtask 320: Fort St. Vrain Coolant Impurity Surveillance

Summary

This subtask involves measuring the primary coolant impurities during reactor operation and analysis of the data using the GOP computer code developed and run by G. L. Tingey and W. C. Morgan of Battelle Northwest Laboratory (BNWL).

The coolant composition of the Fort St. Vrain HTGR has been predicted by the GOP model as a function of power level, H_2O inleakage rate, and operating temperature. For full-power operation, the carbon removal rate is a much stronger function of inleakage rate than of temperature because essentially all the water is removed by the core and only small quantities by the purification system at all temperatures. H_2/H_2O ratios generally increase with increasing temperature and decreasing leak rate.

Introduction

The coolant composition of gas-cooled reactors must be carefully controlled to prevent thermal and radiation-induced reaction with the reactor components. In this program, the measured gas compositions in the Fort St. Vrain HTGR are being compared with calculated compositions using the GOP coolant chemistry code. In the version of the code used for the present study, both radiolytic and thermal reactions occurring in the core of the reactor are considered. No reactions occurring outside of the core are included. The possible consequence of this omission will be considered later. Reactions which will be evaluated include the thermal reaction of the coolant with the graphite support structure and the catalyzed carbon deposition reactions which may occur on the heat exchanger surface ($H_2 + CO$
 $H_2O + C$; $2CO \rightarrow CO_2 + C$).

Results

Coolant gas compositions which were calculated in a parametric study of the effect of water inleakage rates and changes in core temperature at 100% of full reactor power are shown in Table 4-12. Another series of calculations at 38% and 53% of full power were also run to examine the effect of water inleakage rates. These results are shown in Table 4-13 along with the actual reactor measurements. In both of these studies the reactor core was modelled as a single fuel zone, with simplified profiles for the temperature and radiation flux. Moreover, the 38% and 53% power models were extrapolated from a model based on calculated conditions at 50% power.

Table 4-14 lists the results of a third parametric study of the effects of water inleakage rate and power level. In these calculations, the core is modelled as being composed of five zones with much more realistic profiles of the temperature and radiation flux.

Even at the highest leak rate, the gas compositions calculated using the single zone model (see Table 4-12) are in good agreement with the results, shown in Table 4-14, obtained using the five-zone model.

TABLE 4-12
CALCULATED GAS COMPOSITIONS FOR THE FORT ST. VRAIN HTGR AT FULL POWER (SINGLE ZONE MODEL)

Max. Graphite Temp (°C)	Gas Outlet Temp. (°C)	Leak Rate [lb/h (kg/h)]	Gas Composition (ppm)					CO/CO ₂	H ₂ /H ₂ O
			H ₂ O	CO	CO ₂	H ₂	CH ₄		
831	784	0.004 (1.8 x 10 ⁻³)	0.03	0.5	0.1	0.5	0.1	5	17
		0.04 (1.8 x 10 ⁻²)	0.6	5.1	1.1	4.6	1.3	5	8
		0.4 (1.8 x 10 ⁻¹)	12.0	46.0	11.0	44.0	11.0	4	4
881	828	0.004 (1.8 x 10 ⁻³)	0.02	0.6	0.07	0.5	0.1	9	25
		0.04 (1.8 x 10 ⁻²)	0.4	6.1	0.7	4.9	1.3	8	12
		0.4 (1.8 x 10 ⁻¹)	7.4	57.0	7.5	48.0	12.0	8	6
931	872	0.004 (1.8 x 10 ⁻³)	0.01	0.7	0.04	0.5	0.1	18	50
		0.04 (1.8 x 10 ⁻²)	0.2	7.0	0.4	5.1	1.3	18	25
		0.4 (1.8 x 10 ⁻¹)	4.4	68.0	3.7	51.0	12.0	18	12
981	916	0.004 (1.8 x 10 ⁻³)	0.007	0.8	0.01	0.5	0.1	80	71
		0.04 (1.8 x 10 ⁻²)	0.1	7.5	0.1	5.2	1.3	75	52
		0.4 (1.8 x 10 ⁻¹)	2.5	75.0	1.4	52.0	12.0	54	21
1081	1004	0.004 (1.8 x 10 ⁻³)	0.003	0.8	0.002	0.5	0.1	400	167
		0.04 (1.8 x 10 ⁻²)	0.05	7.8	0.02	5.3	1.3	390	106
		0.4	0.9	78.0	0.2	54.0	12.0	390	60

TABLE 4-13
COMPARISON OF CALCULATED AND MEASURED GAS COMPOSITIONS FOR THE FORT ST. VRAIN HTGR (SINGLE ZONE MODEL)

Reactor Power (%) Data Type (Calc. or Meas.)	Leak Rate [lb/h (kg/h)]	Gas Composition (ppm)					CO/CO ₂	H ₂ /H ₂ O
		H ₂ O	CO	CO ₂	H ₂	CH ₄		
38								
Calculated	0.004 (1.8 x 10 ⁻³)	0.3	0.5	0.08	0.5	0.05	6.3	1.7
	0.02 (0.9 x 10 ⁻²)	2.0	1.7	0.5	2.3	0.2	3.4	1.2
	0.04 (1.8 x 10 ⁻²)	4.5	2.8	1.1	4.3	0.4	2.5	1.0
	0.4 (1.8 x 10 ⁻¹)	56.0	8.9	15.0	34.0	2.4	0.6	0.6
Measured		<0.6	2.0	0.5	2.0	0.2	4.0	~3.0
53								
Calculated	0.004 (1.8 x 10 ⁻³)	0.2	0.5	0.1	0.6	0.07	5.0	3.0
	0.02 (0.9 x 10 ⁻²)	1.3	2.2	0.5	2.5	0.3	4.4	1.9
	0.04 (1.8 x 10 ⁻²)	3.1	3.9	1.0	4.8	0.6	3.9	1.5
	0.4 (1.8 x 10 ⁻¹)	44.0	19.0	14.0	38.0	4.1	1.4	0.9
Measured		0.2	3.8	1.0	3.0	0.4	3.8	15.0

TABLE 4-14
CALCULATED GAS COMPOSITIONS FOR THE FORT ST. VRAIN HTGR (FIVE ZONE MODEL)

Max. Graphite Temp. (°C)	Gas Outlet Temp. (°C)	Leak Rate [lb/h (kg/h)]	Gas Composition (ppm)					CO/CO ₂	H ₂ /H ₂ O	Percent of Full Power
			H ₂ O	CO	CO ₂	H ₂	CH ₄			
740	706	0.004 (1.8 x 10 ⁻³)	0.1	0.7	0.05	0.7	0.05	13.0	5.3	28.6 ^(b)
		0.04 (1.8 x 10 ⁻²)	2.4	5.8	0.5	6.1	0.4	11.0	2.6	
		0.4 (1.8 x 10 ⁻¹)	42.0	38.0	6.2	46.0	2.3	6.2	1.1	
806	767	0.004 (1.8 x 10 ⁻³)	0.05	0.7	0.08	0.7	0.09	8.5	13.0	50 ^(c)
		0.04 (1.8 x 10 ⁻²)	1.0	6.4	0.8	6.3	0.8	8.4	6.4	
		0.4 (1.8 x 10 ⁻¹)	19.0	56.0	7.4	58.0	6.5	7.5	3.1	
885	828	0.004 (1.8 x 10 ⁻³)	0.02	0.6	0.07	0.5	0.1	9.1	22.0	100 ^(d)
		0.04 (1.8 x 10 ⁻²)	0.4	6.2	0.7	5.0	1.3	9.0	13.0	
		0.4 (1.8 x 10 ⁻¹)	8.0	57.0	6.9	49.0	11.0	8.3	6.1	

(a) 1.9×10^{-3} kg/h (0.042 lb/h) is the "design inleakage rate" for FSV.

(b) Based on GA calculation (problem No. 7): FSV reactor at 29% flow, 301°C (574°F) inlet, 4.14 MPa (600 psi); BNW six-zone model, including 12.5% bypass flow.

(c) Based on GA calculation (problem No. 9): FSV reactor at 47% flow, 331°C (628°F) inlet, 4.27 MPa (620 psi); BNW five-zone model, including 12.5% bypass flow.

(d) Based on GA calculation (problem No. 16): FSV reactor at 100% flow, 419°C (786°F) inlet, 4.83 MPa (700 psi); BNW five-zone model, including 12.5% bypass flow.

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9. HTGR FUEL DEVELOPMENT AND ENGINEERING*
189a NO. 00551; PROGRAM AREA 10

TASK 100: FUEL PRODUCT SPECIFICATION

No significant progress.

TASK 200: ACCELERATED IRRADIATION TESTING

Subtask 210: Fresh Fuel Qualification

Summary

Preparation of the fuel for capsule HRB-14 is progressing well; 70% of the fuel rod types have been fabricated.

Preparation of the fuel for capsule HRB-15b has also been started. Seventy-five percent of the kernel types and 55% of the coating types to be applied to these kernels have been completed; 20% of the density separations have been completed.

The postirradiation examination (PIE) of capsules HT-31 and HT-33 has been completed and data analysis is now under way. Calculated in-pile kernel migration coefficients (KMCs) for TRISO ThO₂ particles from HT-31 were found to be in excellent agreement (within a factor of 1.5) with out-of-pile KMC values for BISO ThO₂ particles. This result helps to confirm out-of-pile postirradiation thermal gradient heating experiments.

Capsule HRB-14

Capsule HRB-14 is a joint GA-ORNL irradiation experiment. In the GA half of the experiment, it is planned to test Fort St. Vrain (FSV) test

*Now a program under the HTGR Generic Technology Program.

element medium-enriched uranium (MEU) fuel rods and to perform a parametric test of OPyC coating-matrix interactions on the ThO_2 TRISO fuel particles. The capsule will irradiate a variety of GA TRISO coated fissile and fertile fuel to a peak temperature of 1250°C over a wide range of fluences and burnups. The performance of TRISO coated $(\text{Th,U})\text{O}_2$, UCO, and UO_2 particles containing MEU and TRISO ThO_2 particles will be characterized in cure-in-place fuel rods manufactured at GA as part of the effort to qualify a proliferation-resistant fuel for HTGR use at an early date. A more detailed description of the capsule was given in Ref. 9-1.

Preparation of the fuel for capsule HRB-14 is progressing well. All TRISO coated fuel particle batches including eight ThO_2 particle batches, two $(\text{Th,U})\text{O}_2$ and two UC_xO_y batches, and one UO_2 medium-enriched particle batch have been coated. The last batches are presently being analyzed by Quality Control. Fourteen of the twenty fuel rod types have been fabricated.

Capsule HRB-15B

Capsule HRB-15B will be a joint experiment, one in a series, conducted by GA and ORNL as part of a continuing effort to identify and characterize critical HTGR fuel performance parameters. Approximately one-third of the test volume will contain ORNL fuel specimens, with the remaining space allotted to GA. A variety of fuel particle types will be tested including UO_2 , UC_2 , UCO, $(\text{Th,U})\text{O}_2$, and ThO_2 ; each type will be both BISO and TRISO coated. All fissile particle types will contain medium (20%) enriched uranium in response to the current effort to qualify a proliferation-resistant fuel for the HTGR. No fuel rods will be irradiated in this capsule, this being the first HRB experiment to test loose fuel particles exclusively. The coated particles will be contained in circular graphite trays in individual holes machined in the trays. The experiment will be conducted at ORNL in the removable beryllium (RB) section of the High Flux Isotope Reactor (HFIR).

The objectives for capsule HRB-15B are as follows:

1. Characterize the performance of candidate MEU fuel particle types subjected to a peak fluence and burnup equivalent to that of a 4-year irradiation in an HTGR (GA).
2. Provide representative irradiated fuel particles for out-of-pile heating tests (GA).
3. Investigate the behavior of BISO coated fertile particles irradiated to reference HTGR burnup and fast neutron fluences but at relatively low temperatures (ORNL).
4. Identify an optimum carbon/oxygen ratio for the candidate UCO fissile particle (GA).
5. Assess the performance of the outer low-temperature isotropic (OLTI) coating and SiC layer on relatively large-diameter TRISO coated particles (ORNL).
6. Verify by in-pile tests TRISO coating performance models and pressure vessel models for MEU fuel particles (GA).
7. Perform initial test of UO_2 particles with ZrC-doped buffers (GA).

The capsule will consist of 184 graphite trays, or planchets, containing the loose fuel particles in individual blind holes machined in the trays and holding a maximum of 116 particles each. The trays will be nominally 2.0 mm thick with inner and outer diameters of ~ 11.2 and 23.6 mm, respectively. Sixty to 116 particles of one type only will be held in each tray, depending on the heavy metal loading per particle in a particular batch and the amount of heavy metal required as dictated by the neutron-flux profile of the RB facility. To further flatten the axial

heat generation rate and temperature profile over the length of the capsule, the trays will be stacked in an alternating fissile-fertile sequence.

The experiment will be doubly contained within two stainless steel tubes. Provision has also been made to include approximately 1000 inert particles within the graphite spines.

The temperature profile along the fuel stack length will be monitored by eight Chromel/Alumel thermocouples. Provisions will be made in the capsule design for a sweep-gas system. This system will be used to control operating temperatures, as well as to provide sweep-gas samples to determine fission gas R/B data. Since HRB-15B is a single-cell capsule, R/B measurements will not provide quantitative data for any one fuel variety, but will serve as an indicator of overall fuel performance.

Tentative plans call for 10 cycles of irradiation in the HFIR beginning in mid-1978. The expected fast fluence will range from 4 to $8 \times 10^{25} \text{ n/m}^2$ ($E > 29 \text{ fJ}$)_{HTGR} with peak burnups (% FIMA) for the various particle types as follows: ThO_2 - 8.1; $1.1(\text{Th,U})\text{O}_2$ - 21.1; and UO_2 , UCO , and UC_2 - 34.0. The projected time-average operating temperature of all fuel specimens will be 1000°C .

There will be eight different fuel kernel types in HRB-15B; six of these [UCO with $\text{O/U} \sim 0.5$, UCO with $\text{O/U} \sim 1.0$, $(\text{Th,U})\text{O}_2$, UO_2 , UO_2 with ZrC , and ThO_2] have been fabricated and two (UC_2 and UCO with $\text{O/U} \sim 1.6$) remain to be made. Since most kernel types have more than one type of coating, there are 16 separate particle batches of which nine remain to be coated. Three of approximately 15 density separations on the particle batches have been completed.

Capsules HT-31 and HT-33

Capsules HT-31 and HT-33 are part of the continuing HT-capsule series, which is a cooperative effort between GA and ORNL. The uninstrumented capsules were the first to test TRISO and BISO coated ThO_2 particles fabricated in the pilot plant 240-mm-diameter coater. The OPyC coating properties of these particles were varied. The samples were irradiated at design temperatures of 1200° and 1500°C to fluences and burnups beyond peak LHTGR conditions. A detailed description of the capsules is given in Ref. 9-2.

The PIE of the fuel from capsules HT-31 and HT-33 is in progress and the majority of the results will be reported as soon as the thermal analysis has been completed by ORNL.

One important result is that kernel migration of TRISO ThO_2 particles was observed for the first time in a GA irradiation experiment. The migration was seen during the metallographic examination of the high-temperature samples from both HT-31 and HT-33; an example is presented in Fig. 9-1. Kernel migration coefficients (KMC) were calculated and the results are shown in Table 9-1. The distance the kernels traveled was measured on the polished sections of two specimens from capsule HT-31. This is a crude measurement because the particles are randomly sectioned and there is no way to determine the sectioned plane of each particle relative to the true thermal gradient direction. The thermal gradient and the amoeba equivalent temperature were nevertheless estimated from the calculated graphite annulus and peak particle surface temperatures.

The temperatures and thermal gradients of the unbonded particles have large uncertainties and the in-pile data are not corrected for the incubation period. In spite of all the assumptions used, the calculated in-pile KMCs of TRISO ThO_2 particles are in agreement with the out-of-pile KMC values for BISO ThO_2 particles (Ref. 9-3) and help to confirm out-of-pile postirradiation thermal gradient heating experiments.

L7626-165

(a)

L7626-164

(b)

L7626-163

(c)

Fig. 9-1. Example of in-pile kernel migration of TRISO ThO_2 particles in capsule HT-31: (a, b) bright field, (c) polarized light field

TABLE 9-1
CALCULATED KERNEL MIGRATION COEFFICIENTS FOR TRISO ThO₂ PARTICLES
IRRADIATED IN CAPSULE HT-31

	Sample 33	Sample 38
Fast fluence ^(a) $\left[10^{25} \text{ n/m}^2 (\text{E} > 29 \text{ fJ})_{\text{HTGR}}\right]$	7.9	7.4
Burnup ^(a) (% FIMA)	8.6	8.2
Irradiation time (s)	7.73×10^6	7.73×10^6
Amoeba equivalent temperature ^(b) (°C)	1470	1435
Thermal gradient ^(a) (K/m)	5.75×10^4	5.99×10^4
Kernel migration distance ^(c) (μm)		
Individual Measurement	37	24
↓	38	25
	39	26
	50	41
	61	42
	71	45
	78	59
	--	62
	--	73
	--	82
Average kernel migration distance (μm)	53	48
Kernel migration coefficient (m ² -K/s)		
Measured in-pile ^(d)	3.6×10^{-10}	3.0×10^{-10}
Predicted out-of-pile ^(e)	5.2×10^{-10}	3.4×10^{-10}

(a) Calculated value.

(b) The constant temperature that would result in the migration distance predicted using the variation in the temperature with time.

(c) Measured on particles in metallographic polished section.

(d) Equation used: $\text{KMC} = \frac{\Delta x}{\Delta t} T^2 \left(\frac{\Delta T}{\Delta X} \right)^{-1}$

(e) Ref. 9-3.

TASK 300: INTEGRAL FUEL SYSTEM TESTING

Manufacture and Quality Control inspection of Fort St. Vrain fuel test elements FTE-1 through FTE-8, as well as final evaluation of the Peach Bottom fuel test elements, are continuing.

TASK 400: OUT-OF-PILE PARTICLE TESTING AND EVALUATION

Subtask 430: Out-of-Pile Isothermal Heating

Summary and Conclusions

A series of out-of-pile tests was conducted to demonstrate the performance of irradiated TRISO ThC_2 and TRISO ThO_2 fertile particles under conditions similar to those postulated for a loss of forced circulation (LOFC) core heatup event in an HTGR. The ThC_2 fuel, which was selected from FSV core 1 production material, was irradiated in accelerated irradiation capsule F-30. The properties of the oxide samples are similar to those proposed for HTGR TRISO ThO_2 fertile fuel particles. The pressure vessel design proposed for HTGR TRISO ThO_2 fertile fuel will, however, be more conservative than the oxide samples tested in this program. The oxide samples were irradiated in accelerated irradiation capsules P13S and HB-1.

A total of 101 TRISO ThC_2 and 476 TRISO oxide particles was tested by heating from $\sim 1100^\circ$ to $\sim 2500^\circ\text{C}$ in time periods ranging from 9.0 to 70.0 h. The test conditions are similar to conditions predicted for the hypothetical LOFC core heatup event that must be evaluated as part of HTGR licensing efforts. Fuel performance during testing was monitored using the variation in Kr-85 and Cs-137 release fractions determined as a function of temperature during each individual test. Seven individual tests were completed. The primary purposes of the tests were to (1) compare TRISO ThC_2 and TRISO ThO_2 performance and (2) compare the gaseous

fission product release from each fuel type with predictions made using FSV FSAR fuel failure and fission product release models. The conclusions drawn are summarized below.

1. Krypton-85 release fractions measured from both TRISO ThC_2 and TRISO oxide samples are very low relative to predictions made using FSV fuel failure and particle fission product release models, thereby demonstrating that the FSV models are conservative.
2. Krypton and cesium release from TRISO ThO_2 fertile fuel will be less than or equal to that from the present FSV ThC_2 fertile fuel.
3. Conversion of ThO_2 in fuel with failed coatings to ThC_2 would not cause additional fuel failure during an LOFC core heatup event.

Introduction

Fuel particle performance under conditions postulated for an LOFC core heatup event in an HTGR is being evaluated in an ongoing experimental program (Refs. 9-1, 9-4). Efforts during this past quarter have concentrated on TRISO ThO_2 and TRISO ThC_2 fertile fuel behavior. TRISO ThO_2 is the backup fertile fuel for an HTGR; it is also being considered as a replacement for the current FSV TRISO ThC_2 fertile fuel.

Irradiated particles from three TRISO oxide samples and one TRISO carbide sample were heated from $\sim 1100^\circ$ to $\sim 2500^\circ\text{C}$ in 9.0 to 70.0 h to simulate conditions predicted during an LOFC core heatup event. Fission product release measurements made during each test will be discussed and compared with predictions made using FSV fuel failure and particle fission product release models (Ref. 9-5).

Test Samples

Kernel and coating dimensions of the test samples are given in Table 9-2; irradiation exposures are given in Table 9-3. The TRISO ThC₂ sample was selected from core 1 FSV fertile B production material and irradiated in capsule F-30, which was the proof test for FSV carbide fuel (Refs. 9-6, 9-7). The TRISO oxide fuels were prepared in prototype production equipment for irradiation in accelerated capsule tests P13S and HB-1 (Refs. 9-8, 9-9). The TRISO (Th/U)O₂ samples were included in these tests to simulate high burnup fertile fuel.* The inner PyC, SiC, and outer PyC coating thicknesses of the oxide samples are similar to those specified for HTGR TRISO ThO₂ fertile fuel. The nominal kernel diameter to be specified for TRISO ThO₂ will be 450 μm; the nominal buffer thickness will be 65 μm. The average kernel diameters of the oxide samples are 491 to 512 μm and the average buffer thicknesses are 64, 87, or 90 μm.

The relative conservatism of the oxide designs can best be evaluated by comparing stress distributions calculated using TRISO particle stress codes (Ref. 9-10). Past comparisons between calculated stress distributions and observed performance have been made at 1250°C. The stress distributions for these test samples were therefore developed assuming that the fuel was irradiated to the conditions given in Table 9-3 and then heated to 1250°C, which is equivalent to irradiation at 1250°C. The stress distributions are characterized in Table 9-4 by giving the probability that calculated SiC stresses are more positive than -19 MPa (-2800 psi) since recent studies (see Task 600) have shown that the probability for FSV carbide fuel pressure vessel failure equals the probability that calculated SiC stresses are greater than -19 MPa (-2800 psi). Even though the burnup of the ThO₂ kernels (4.1% FIMA) is less than that of the (Th/U)O₂ kernels (8% FIMA), the ThO₂ particles have higher coating stresses than the (Th/U)O₂ fuel because the ThO₂ particles have thinner

*The peak fertile kernel burnup in FSV is 7% FIMA.

TABLE 9-2
KERNEL DIAMETER AND COATING THICKNESS OF PARTICLES USED FOR CORE HEATUP SIMULATION TESTING

Data Retrieval Number	Kernel		Coating Thickness (μm)			
	Type	Diameter (μm)	Buffer	Inner PyC	SiC	Outer PyC
6252-00-025	ThO_2	512	64	30	31	42
6155-01-010	$(\text{Th/U})\text{O}_2^{(a)}$	491	87	30	28	41
6155-01-020	$(\text{Th/U})\text{O}_2^{(a)}$	496	90	32	28	39
CT6B-932	$\text{ThC}_2^{(b)}$	432	56	35	23	47

(a) Th/U ratio = 8/1.

(b) FSV fertile B fuel.

TABLE 9-3
IRRADIATION CONDITIONS FOR TEST SAMPLES USED IN CORE HEATUP SIMULATION TESTS

Particle Type	Data Retrieval Number	Irradiation Data			
		Location	Temperature (°C)	Fast Neutron Exposure (10^{25} n/m ²) ($E \geq 29$ fJ) _{HTGR}	Kernel Burnup (% FIMA)
TRISO ThO ₂	6252-00-025	P13S;C3T11,T12	860-925	11.5	4.1
TRISO (Th/U)O ₂ ^(a)	6155-01-010	HB-1;T051	<700	5.0	8.0
TRISO (Th/U)O ₂ ^(a)	6155-01-020	HB-1;T051	<700	5.0	8.0
TRISO ThC ₂ ^(b)	CT6B-932	F-30;2-1	1160	9.1	4.7

(a) Th/U ratio = 8/1.

(b) FSV fertile B fuel.

TABLE 9-4
CALCULATED SiC STRESS DISTRIBUTIONS^(a) FOR SAMPLES USED IN CORE HEATUP SIMULATION TESTS

Particle Type	Data Retrieval Number	Average Kernel Diameter (μm)	Average Buffer Thickness (μm)	Irradiation Exposure		Probability That Calculated SiC Stresses Are More Positive Than -19 MPa (-2800 psi)
				Kernel Burnup (% FIMA)	Fast Neutron Exposure (10 ²⁵ n/m ²) (E > 29 fJ) _{HTGR}	
TRISO ThO ₂	6252-00-025	512	64	4.1	11.5	0.040
TRISO (Th/U)O ₂	6155-01-010	491	87	8.0	5.0	0.022
TRISO (Th/U)O ₂	6155-01-020	499	90	8.0	5.0	0.013
TRISO (Th/U)C ₂	CT6B-932	432	56	4.7	9.1	0.006

^(a) Calculations done at 1250°C.

buffers than the (Th/U)O₂ particles. The ThO₂ samples will therefore have a higher probability of pressure vessel failure in the early stages of testing than the (Th/U)O₂ samples. The probability that SiC stresses will exceed -19 MPa (-2800 psi) after irradiation at 1250°C to 7% FIMA and $8 \times 10^{25} \text{ n/m}^2$ ($E \geq 29 \text{ fJ}$)_{HTGR} is 0.006 for FSV fertile B carbide fuel and 0.003 for the TRISO ThO₂ fertile fuel (Ref. 9-9). The oxide samples chosen for this study have a higher probability of exceeding -19 MPa (-2800 psi) and therefore will provide a conservative estimate of the behavior of TRISO ThO₂ fuel during an LOFC core heatup event.

Test Conditions and Procedures

Samples were equilibrated at 950° to 1190°C for 16 to 20 h prior to each LOFC core heatup simulation test (CHST). After equilibration, samples were heated to temperatures as high as 2525°C in periods of 9.0 to 70.0 h. The change in temperature with time was approximately linear. Conditions achieved and the number of particles used in each individual test are summarized in Table 9-5. Fuel performance was evaluated from Kr-85 and Cs-137 release data collected during each test. Procedures used to monitor fission product release during each test and for post-test evaluations are summarized in Ref. 9-1.

With one exception, all tests were conducted using fuel particles shown to be intact by postirradiation fission gas release measurements, gamma ray spectrometry, or X-radiographic examinations. The exception was test 78IHR-B-3-1 which contained 60 particles from (Th/U)O₂ sample 6155-01-020. Six of the 60 particles had coatings that were pierced by laser drilling prior to heating. All CHSTs are conducted in He; consequently, the oxide kernels in the failed fuel would convert to carbide at some temperature by reacting with carbon in the particle coatings. The test containing failed fuel was conducted to evaluate the rate of fission product release from the failed particles in a reducing atmosphere and to determine whether cascading (additional failure) would result from conversion of the oxide kernels to carbide.

TABLE 9-5
CORE HEATUP SIMULATION TEST CONDITIONS

Particle Type	Data Retrieval Number	Test Number	Range Of Test Temperatures (°C)	Time To Reach Peak Temperature (h)	Number Of Particles Tested
TRISO (Th/U)C ₂	CT6B-932	78IHR-B-1-1	1120 - 2475	26.5	101
TRISO ThO ₂	6252-00-025	78IHR-A-1-1	1190 - 2505	9.0	100
	6252-00-025	78IHR-B-2-1	1100 - 2525	28.3	100
	6252-00-025	78IHR-C-2-1	1110 - 2410	70.0	100
TRISO (Th/U)O ₂ ^(a)	6155-01-010	77IHR-A-6-1	950 - 2350	10.2	55
	6155-01-020 ^(b)	78IHR-B-3-1	1100 - 2500	27.0	60
	6155-01-010	78IHR-C-1-1	1100 - 2430	67.1	61

(a) Th/U ratio = 8/1.

(b) Six of 60 particles were laser failed prior to testing.

Experimental Results

Temperature Uncertainties. Cesium-137 and Kr-85 release fractions are determined by collecting fission products released from the test samples on traps (mullite is used to collect Cs-137 and liquid N₂ cold traps are used to collect Kr-85) and gamma counting the traps. The data are reported as fission product release fraction versus the temperature measured at the time any fission product trap is sampled. The test apparatus is described in detail in Refs. 9-1 and 9-4. Helium flows over each test sample at a flow rate of 0.83 cc/s. Any fission products released during testing are swept up in the flowing He and transported to the mullite or liquid N₂ cold traps. The time required to transport the fission products to their respective traps was analysed to estimate the uncertainty in temperature associated with the Cs and Kr release data.

The test samples and Cs traps are contained in 11.68 mm (0.46 in.) i.d. Ta tubes. The average distance between a sample and Cs trap is 254 mm (10 in.). Since there are no restrictions to flow between the samples and Cs traps, one can assume slug flow to estimate the time needed to transport the Cs to a trap. At a flow rate of 0.83 cc/s, the Cs should reach the mullite traps 0.009 h after it is released from a test sample. Given a temperature ramp from 1100° to 2500°C, this amounts to uncertainties of +0,-1.6°C for an 8-h test; +0,-0.4°C for a 30-h test; and +0,-0.2°C for an 80-h test, which is negligible.

Samples from a single irradiation sample are heated in three individual Ta tubes during each CHST. The exit gas from all three tubes is combined on leaving the furnace. The gas (He + Kr) then flows at 2.5 cc/s through a series of traps designed to remove H₃ and Rn, through two electrometers which are used to continuously monitor Kr release, and into the liquid N₂ cold trap. The total volume of the system is 1625 cc. Two samples from test 78THR-B-3-1 (see Table 9-5) were used to determine the response time for the Kr-85 detection system. The first was taken when

the He flow was initiated just prior to the start of the heating ramp.* The second was taken after completion of the test. One Ta tube had been sealed off 25.8 h into the 27.0-h test because a gas leak was suspected. It was shown later that the tube did not leak. The tube was purged at 2.50 cc/s after all Kr-85 had been removed from the other two tubes in 78IHR-B-3-1. Since no additional Kr-85 was released during the purge in each of these cases, the results can be used to determine the system response to Kr-85. The response was analysed by evaluating the fraction of total Kr-85 in each sample collected as a function of time. The results are shown in Fig. 9-2. Approximately 50% of the Kr-85 released during a single event was detected 0.5 h after the event; 90% was detected 0.8 h after the event. Using a linear response curve through the two points indicated above, it was shown that x% Kr-85 release was indicated by the system about 0.5 h after x% of the Kr-85 was released from the fuel samples, assuming a 100-particle sample experienced one failure per hour or one to ten failures each 0.1 h. This suggests that a temperature uncertainty of +0,-78°C is associated with Kr-85 data collected during a 9-h test in which temperatures are increased linearly with time from 1100° to 2500°C. The uncertainty in a 27- or 70-h test would be +0,-26°C or +0,-10°C, respectively.

The time required to collect 90% of the Kr released during a single event (0.8 h) is not consistent with the volume of the system and the He flow rate. Assuming slug flow, the sample should be collected in 0.18 h. If ideal mixing occurred in the whole volume of the system, the slowest collection of the Kr-85 would occur. For a volume of 1625 cc, 90% of the gas would pass through the system in 0.42 h (Ref. 9-11). The Kr-85 flow through the system must be retarded by one of the H₃ or Rn traps. The Rn is removed in an activated charcoal trap that is held in a dry ice - alcohol bath. Recent experience (Ref. 9-11) has shown that this type of trap temporarily holds up Kr. The impact of this trap on the Kr-85 response time will be evaluated in future tests. If significant, the method used to trap Rn will be changed.

*10% of the particles were laser failed before testing; therefore, some Kr-85 was released during the 16- to 20-h hold that occurs prior to starting the heating ramp.

Fig. 9-2. Response time for Kr-85 measurements made in the CHST facility

X-Radiographic and Kr-Cs Release Results. Contact X-radiographs of each test sample were prepared before and after heating. The radiographs are used to obtain a visual indication of particle integrity and to measure kernel and particle diameters before and after heating. Kernel and particle dimensions measured before and after heating are given in Tables 9-6 and 9-7. Preheating and postheating dimensions will be compared at a 95% confidence level using the students t-test and are discussed below in conjunction with Kr and Cs release data.

Krypton-85 and Cs-137 release fractions measured during the 26.5-h test of FSV fertile B fuel (TRISO ThC₂) are shown as a function of temperature in Fig. 9-3. As discussed earlier, the particles in each test are separated into three samples. Cesium release fractions from each sample are monitored separately; consequently, there is some scatter in the data. Krypton release data are averaged over all particles in each test; therefore, the Kr-85 release fraction increases smoothly with time and temperature. Krypton and Cs release fractions ranged from 10^{-3} to 10^{-2} at temperatures less than 2000° to 2050°C. At temperatures exceeding 2050°C, fission product release fractions increased rapidly with temperature and the Kr-85 release fraction at any given temperature was less than or equal to the Cs-137 release fraction.

Radiographs of typical particles from TRISO ThC₂ sample CT6B-932 are shown before and after heating in Fig. 9-4. One hundred and one particles were heated. Heavy metal dispersion occurred to the extent indicated in Fig. 9-4b in 88 of the particles. The diameter of the dispersed metal is shown in Table 9-7 as the postheating total particle diameter of this sample. Since no change in particle diameter occurred during heating, it must be concluded that the outer PyC layer remained "intact" and that the kernel dispersion extended into the outer PyC. It is not clear whether actinide metal and/or fission product - SiC reactions or pressure vessel failure of the SiC (the total particle diameters imply intact outer PyC layers) lead to dispersion through the SiC and into the OPyC

TABLE 9-6
KERNEL DIAMETERS MEASURED BEFORE AND AFTER CORE HEATUP SIMULATION TESTING

Particle Type	Data Retrieval Number	Length Of CHST (h)	Temp ^(a) (°C)	Number Of Particles Examined	Kernel Diameter (μm)					
					Before Heating			After Heating		
					Avg	Std Error	Range	Avg	Std Error	Range
TRISO ThC ₂	CT6B-932	26.5	2475	0	(b)			(b)		
TRISO ThO ₂	6252-00-025	70.0	2160 ^(c)	32	524	1.88	500-544	541	1.96	513-561
TRISO (Th/U)O ₂	6155-01-010	10.2	2350	55	514	2.28	472-545	540	2.79	495-591
	6155-01-010	67.1	2330 ^(d)	20	515	3.69	483-557	540	3.94	502-579
	6155-01-010	67.1	2430	41	510	2.26	484-537	528 ^(e)	3.18	492-564
		67.1	2430	9				531 ^(f)	5.04	500-551
		67.1	2430	19				527 ^(g)	4.09	492-564

(a) Maximum temperature experienced by the sample examined.

(b) Kernels dispersed throughout the particle during heating.

(c) Test was interrupted at 2160°C; this sample was removed.

(d) Test was interrupted at 2330°C; this sample was removed.

(e) Obtained from 28 particles: 9 had visible cracks in the SiC layer; no visible cracks detected in the other 19 particles.

(f) Obtained from the 9 particles [see (e)] with visible cracks in the SiC layer.

(g) Obtained from the 19 particles [see (e)] with no visible cracks in the SiC layer.

TABLE 9-7
PARTICLE DIAMETERS MEASURED BEFORE AND AFTER CORE HEATUP SIMULATION TESTING

Particle Type	Data Retrieval Number	Length Of CHST (h)	Temp ^(a) (°C)	Number Of Particles Examined	Total Particle Diameter (µm)					
					Before Heating			After Heating		
					Avg	Std Error	Range	Avg	Std Error	Range
TRISO ThC ₂ ^(b)	CT6B-932	26.5	2475	17	759	9.21	699-825	754	13.78	666-816
TRISO ThO ₂	6252-00-025	70.0	2160 ^(c)	33	833	4.74	756-875	838	4.62	763-886
TRISO (Th/U)O ₂	6155-01-010	10.2	2350	55	877	4.41	803-945	901	3.66	849-960
	6155-01-010	67.1	2330 ^(d)	20	877	6.85	812-927	893	6.19	831-946
	6155-01-010	67.1	2430	41	873	4.34	823-938	950 ^(e)	20.17	823-1393
		67.1	2430	9				1100 ^(f)	42.09	964-1393
		67.1	2430	19				892 ^(g)	6.29	842-932
		67.1	2430	5				899 ^(h)	20.16	823-935

(a) Maximum temperature experienced by the sample examined.

(b) Kernels dispersed throughout the particle during heating.

(c) Test was interrupted at 2160°C; this sample was removed.

(d) Test was interrupted at 2330°C; this sample was removed.

(e) Obtained from 33 particles: 9 had visible cracks in the SiC layer; no cracks were visible in 19 particles; kernels dispersed throughout the coatings in 5 particles.

(f) Obtained from the 9 particles [see (e)] with visible cracks in the SiC layer.

(g) Obtained from the 19 particles [see (e)] with no visible cracks in the SiC layer.

(h) Obtained from the 5 particles [see (e)] showing kernel dispersion throughout the coatings.

Fig. 9-3. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 26.5-h ramp test of TRISO ThC₂ fuel irradiated to 4.7% FIMA

(a)

(b)

Fig. 9-4. Preheating (a) and postheating (b) contact X-radiographs of TRISO ThC₂ particles heated from 1120° to 2475°C in 26.5 h

layer. The fission product release data do indicate 100% Cs and Kr loss from these particles. However, the coatings did act as effective barriers to Cs and Kr release until temperatures exceeded 2000°C. Coating fragments consistent with 13 "pressure vessel failures" were also observed. These failures occurred at temperatures exceeding 2000°C.

The Kr-85 and Cs-137 release fractions measured from the TRISO ThO₂ sample (4.1% FIMA) during 9.0-, 28.3-, and 70.0-h ramp tests are shown in Figs. 9-5, 9-6, and 9-7, respectively. The initial Cs inventory of each sample was determined by gamma counting prior to heating; consequently, the uncertainty in Cs release fractions is determined by counting statistics. Although not evaluated for each specific sample, the uncertainty is small (<10%). The initial Kr-85 inventory used to estimate Kr-85 release fractions was calculated from measured Ru-106 and Cs-137 inventories. Gaseous release fractions as high as 1.23 were detected from one TRISO ThO₂ sample, implying that the predicted Kr-85 inventory was only 81% of the actual inventory. The range in Kr-85 release fractions relative to those shown in Figs. 9-5 through 9-7 is therefore from 1 to 0.81 times the reported values. The Kr-85 release fractions shown in Figs. 9-5 through 9-7 are somewhat higher than Cs-137 release fractions at temperatures up to 2000° to 2100°C. After accounting for temperature uncertainties associated with the Kr-85 data, Kr-85 release fractions are less than or equal to Cs release fractions at temperatures exceeding 2100°C.

The variation in Kr-85 release fractions with temperature are shown for the three TRISO ThO₂ ramp tests (9.0, 28.3, 70.0 h) in Fig. 9-8; a similar comparison for Cs is shown in Fig. 9-9. At temperatures greater than 2100°C, the Kr-85 or Cs-137 release fraction at any given temperature increased with the length of the test. At temperatures less than 2100°C, Kr release fractions determined in the 28.3- and 70.0-h tests are similar and slightly higher than those obtained in the 9.0-h test. Similar conclusions are drawn from the Cs-137 data. The Kr-85 data suggest 3% to 4%

Fig. 9-5. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 9.0-h ramp test of TRISO ThO₂ fuel irradiated to 4.1% FIMA

Fig. 9-6. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 28.3-h ramp test of TRISO ThO₂ fuel irradiated to 4.1 % FIMA

Fig. 9-7. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 70.0-h ramp test of TRISO ThO₂ fuel irradiated to 4.1 % FIMA

Fig. 9-8. Effect of the length of a CHST on Kr-85 release from TRISO ThO₂ as a function of temperature

Fig. 9-9. Effect of the length of a CHST on Cs-137 release from TRISO ThO₂ as a function of temperature

particle failure at temperatures less than 2100°C. X-radiographic observations, which are discussed below, showed that this is associated with pressure vessel failure. No pressure vessel failure was suggested at temperatures less than 2000°C from results obtained during heating of the TRISO ThC₂ and TRISO (Th/U)O₂ samples, which is consistent with assessments of the relative conservatism of the test sample pressure vessel designs.

Radiographs of TRISO ThO₂ particles (4.1% FIMA) heated from 1110° to 2410°C in 70 h are shown in Fig. 9-10. Figures 9-10a and c are typical particles before and after heating, respectively. Typical particles from a group of 33 that were removed after reaching 2160°C are shown in Fig. 9-10b. Heating to 2160°C did not affect the total particle diameter; however, the average kernel diameter increased by 3.2%.

One of the 33 particles radiographed after heating at 2160°C had experienced pressure vessel failure. This failure fraction is consistent with the fission product release fraction observed during testing of these ThO₂ particles at temperatures less than 2100°C. The failed particle retained its buffer and inner PyC layer; the kernel remained dense and spherical and was 525 μm in diameter.

Figure 9-10c is typical of the postheating appearance of TRISO ThO₂ particles experiencing peak temperatures in the range 2410° to 2525°C after 9.0-, 28.3-, or 70.0-h ramp tests. Heavy metal dispersion into the outer PyC layer was apparent in all particles. Heavy metal diffusion through the outer PyC layer and pressure vessel failure were apparent in some particles. The particles showing dispersion tended to agglomerate; consequently, no dimensional measurements were made. Complete Kr-85 and Cs-137 release was observed in each of these tests. No ThO₂ kernel dispersion has been observed in BISO (Ref. 9-1) or TRISO coated ThO₂ coatings considered to be intact after testing. This coupled with the dispersion observed in TRISO ThC₂ fuel suggests that the ThO₂ dispersion may be

(a)

(b)

(c)

Fig. 9-10. Preheating (a) and postheating (c) contact X-radiographs of TRISO ThO_2 particles heated from 1110° to 2410°C in 70.0 h. Particles in (b) were removed from the test after reaching 2160°C .

associated with loss of CO from the individual particles, conversion of the oxide kernels to ThC_2 , and then dispersion of the converted ThC_2 kernel material.

Krypton and Cs release fractions measured during 10.2-, 27.0-, and 67.1-h tests of TRISO (Th/U) O_2 particles (8.0% FIMA) are shown in Figs. 9-11, 9-12, and 9-13, respectively. Krypton-85 and Cs-137 release fractions are similar in the 10.2- and 67.1-h tests up to 2100° to 2200°C. At temperatures exceeding 2200°C, the Cs-137 release fraction exceeded the Kr-85 release fraction at any given temperature.

Six of the 60 particles heated in the 27-h test were laser failed prior to heating. The Kr-85 and Cs-137 release fractions approached 0.14 and 0.11, respectively, at 1900° to 2000°C (Fig. 9-12). Since both the ThO_2 and (Th/U) O_2 samples were irradiated in the General Electric Test Reactor at Vallecitos, California, it is reasonable to assume that the uncertainty in Kr-85 release fractions is the same for each sample (range from 1.0 to 0.81 times the reported value). Given this uncertainty, the Cs and Kr release fractions shown in Fig. 9-12 at 2000° to 2200°C can be considered equal. At temperatures exceeding 2300°C, the Cs release fraction at some temperature T is greater than the Kr release fraction.

The Kr release fractions are shown as a function of temperature in Fig. 9-14 for the three mixed oxide tests; Cs release data are shown in Fig. 9-15. Both figures clearly show the impact of failed fuel on Kr-85 and Cs-137 release fractions. Although no significant Kr-85 release was observed in the 10.2-h test, the temperature at which the Kr release fraction begins to increase appears to be similar for the 10.2- and 67.1-h tests (Fig. 9-14). The variation in Cs-137 release fraction with temperature is also independent of the length of the ramp test based on the 10.2- and 67.1-h results (Fig. 9-15). This suggests that the variation in Kr and Cs release fractions with temperature in the 27.0-h test would have been similar to the results obtained in the 10.2- and 67.1-h tests

Fig. 9-11. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 10.2-h ramp test of TRISO (Th,U)O₂ fuel irradiated to 8.0% FIMA

Fig. 9-12. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 27.0-h ramp test of TRISO (Th,U)O₂ fuel irradiated to 8.0% FIMA

Fig. 9-13. Kr-85 and Cs-137 release fractions measured as a function of temperature during a 67.1-h ramp test of TRISO (Th,U)O₂ fuel irradiated to 8.0% FIMA

Fig. 9-14. Effect of an initial failed fuel fraction and the length of a CHST on Kr-85 release from TRISO oxide fertile fuel as a function of temperature

Fig. 9-15. Effect of an initial failed fuel fraction and the length of a CHST on Cs-137 release from TRISO oxide fertile fuel as a function of temperature

had no failed fuel been included in the 27.0-h test. Figures 9-14 and 9-15 show that the fission product release fractions observed in the three tests are similar once the release fractions observed in the 10.2- and 67.1-h tests approach the release fraction caused by the failed fuel that was included in the 27.0-h test. This implies that conversion of the $(\text{Th/U})\text{O}_2$ kernels to ThC_2 in the initial failed fuel fraction did not lead to cascading (additional fuel failure) during the CHST.

Radiographs of TRISO $(\text{Th/U})\text{O}_2$ particles heated from 950° to 2350°C in 10.2 h or 1100° to 2430°C in 67.1 h are shown in Figs. 9-16 and 9-17, respectively. Although approximately 50% Cs-137 release was detected in the 10.2-h test, no Kr-85 release was observed (see Fig. 9-11). Fifty-four of the 55 test particles appeared intact during radiographic examinations; the SiC layer on one particle contained a large number of fine cracks (Fig. 9-16c). The particle with the failed SiC layer did not release Kr-85 since no release was detected during this test. The average kernel diameter of the test particles increased 4.8% during heating; the average total particle diameter increased 2.7%.

The 67.1-h test of TRISO $(\text{Th/U})\text{O}_2$ was interrupted at 2330°C . Approximately 10% of the Cs-137 and less than 1% of the Kr-85 inventories had been released at this point. Radiographs of these particles are included with the pretest and posttest radiographs in Fig. 9-17. No indication of coating failure was seen in X-radiographs of the particles removed at 2330°C . The average kernel diameter of these particles increased by 6.8%; no change in total particle diameter had occurred.

Three morphologies were seen in the 41 particles heated to 2430°C in this 67.1-h test (Fig. 9-17c). No apparent change occurred in 19 particles, 9 showed considerable cracking or decomposition of the SiC layer, and 5 appeared white indicating heavy metal dispersion. Coating fragments consistent with eight "pressure vessel failures" were also observed. There is no significant difference in the postheating average

(a)

(b)

(c)

Fig. 9-16. Preheating (a) and postheating (b, c) contact X-radiographs of TRISO (Th/U)O₂ particles heated from 950° to 2350°C in 10.2 h

(a)

(b)

(c)

Fig. 9-17. Preheating (a) and postheating (c) contact X-radiographs of TRISO (Th/U) O_2 particles heated from 1100° to 2430°C in 67.1 h. Particles in (b) were removed from the test after reaching 2330°C.

kernel diameter of particles with and without visible cracks in the SiC layers. The average diameter of these kernels increased 3.5% during heating. The average total diameter of the particles showing no visual indication of failure increased 2.2%. The average total diameter of the "white" particles is 3.0% larger than the initial diameter; however, the diameter increase cannot be supported statistically. The most significant change in total particle diameter occurred in the particles with cracked SiC layers. The range of particle diameters was 964 to 1393 μm after heating. The average diameter (1100 μm) was 26% larger than the average particle diameter of the test sample prior to heating. The largest particle is shown in Fig. 9-17c. The outer PyC is visible as a thin ring 100 to 150 μm beyond the cracked or decomposing SiC layer. Since the kernels in these particles are similar in appearance and dimensions to those in particles that appear intact, one must assume that the CO pressure within the particles is adequate to prevent conversion of the kernels to ThC_2 . The large amounts of PyC thermal creep required for the diameter changes implies high internal gas pressures, suggesting that the kernels retained the majority of their fission gasses.

Additional Metallic Fission Product Release Results. Samples were gamma counted prior to heating to determine the metallic fission product content. Particles were gamma counted in their crucibles and separate from their crucibles after heating; the empty H-451 graphite crucibles were also gamma counted after heating. This provides data on the total release of metallic fission products other than Cs from the fuel and the fraction of released metallic fission products retained by H-451 graphite. Data obtained from TRISO ThC_2 , TRISO ThO_2 , and TRISO $(\text{Th/U})\text{O}_2$ tests are summarized in Tables 9-8, 9-9, and 9-10, respectively. The total Cs release fractions are consistent with those determined during testing by gamma counting the mullite Cs traps, implying that the traps are efficient Cs collectors.

The variation in Eu-154, Ce-144, and Ru-106 particle release fractions with the peak temperature experienced during a CHST is shown

TABLE 9-8
METALLIC FISSION PRODUCT RELEASE FRACTIONS FROM TRISO ThC₂ PARTICLES
IRRADIATED TO 4.7% FIMA AND THE FRACTION RETAINED BY H-451 GRAPHITE
SAMPLE HOLDERS DETERMINED BY GAMMA COUNTING BEFORE AND AFTER CORE
HEATUP SIMULATION TESTING

Radionuclide	CHST = 26.5 h Peak Temp = 2475°C	
	Fraction Released from Particles	Fraction Retained by H-451 Graphite
Zr-95	ND ^(a)	
Ru-106	0.08	(b)
Sb-125	1.00	0
Cs-134	0.96	0
Cs-137	0.96	<0.01
Ce-144	0.92	0.87
Eu-154	1.00	0.00

(a) Not detected in original test sample.

(b) Could not be estimated; particle release fractions too low.

TABLE 9-9
METALLIC FISSION PRODUCT RELEASE FRACTIONS FROM TRISO ThO₂ PARTICLES IRRADIATED TO 4.1% FIMA AND
THE FRACTION RETAINED BY H-451 GRAPHITE SAMPLE HOLDERS DETERMINED BY GAMMA COUNTING BEFORE AND
AFTER CORE HEATUP SIMULATION TESTING

Radionuclide	CHST = 9.0 h Peak Temp = 2505°C	CHST = 28.3 h Peak Temp = 2525°C		CHST = 70.0 h Peak Temp = 2160°C ^(a)		CHST = 70.0 h Peak Temp = 2410°C
	Fraction Released from Particles and H-451 Graphite ^(b)	Fraction Released from Particles	Fraction Retained by H-451 Graphite	Fraction Released from Particles	Fraction Retained by H-451 Graphite	Fraction Released from Particles and H-451 Graphite ^(b)
Zr-95	ND ^(c)	ND ^(c)		ND ^(c)		ND ^(c)
Ru-106	0.05	0.43	0.84	0	(d)	0.03
Sb-125	1.00	1.00	0.00	0.23	1.00	1.00
Cs-134	1.00	1.00	0.00	0.21	0.01	1.00
Cs-137	1.00	1.00	0.00	0.19	0.02	1.00
Ce-144	0.09	0.85	0.88	0.06	(d)	0
Eu-154	1.00	1.00	0.00	0.03	(d)	1.00

(a) Particles were removed from the 70-h test after reaching 2160°C.

(b) Particles and crucibles could not be separated easily.

(c) Not detected in the initial test sample.

(d) Release from particles too small to evaluate.

TABLE 9-10
METALLIC FISSION PRODUCT RELEASE FRACTIONS FROM TRISO (Th/U)O₂ PARTICLES IRRADIATED TO 8.0% FIMA AND
THE FRACTION RETAINED BY H-451 GRAPHITE SAMPLE HOLDERS DETERMINED BY GAMMA COUNTING BEFORE AND
AFTER CORE HEATUP SIMULATION TESTING

Radionuclide	CHST = 10.2 h Peak Temp = 2350°C		CHST = 27.0 h Peak Temp = 2500°C		CHST = 67.1 h Peak Temp = 2330°C ^(a)		CHST = 67.1 h Peak Temp = 2430°C	
	Fraction Released from Particles	Fraction Retained by H-451 Graphite	Fraction Released from Particles	Fraction Retained by H-451 Graphite	Fraction Released from Particles	Fraction Retained by H-451 Graphite	Fraction Released from Particles	Fraction Retained by H-451 Graphite
Zr-95	ND ^(b)		ND ^(b)		ND ^(b)		ND ^(b)	
Ru-106	0.04	(c)	0.26	1.00	0.02	(c)	0.12	(c)
Sb-125	0.04	(c)	0.96	0.00	0.00	(c)	0.23	0.00
Cs-134	0.38	<0.01	0.97	0.00	0.15	<0.01	0.71	<0.01
Cs-137	0.50	<0.01	0.97	<0.01	0.15	<0.01	0.71	<0.01
Ce-144	0.07	(c)	0.72	0.81	0.02	(c)	0.42	(c)
Eu-154	0.49	0.39	1.00	0.00	0.32	0.15	0.81	<0.02

(a) Particles removed from the 67.1-h test after reaching 2330°C.

(b) Not detected in the initial sample.

(c) Release from particles too small to evaluate.

for TRISO oxide samples in Fig. 9-18. The length of the individual tests is not indicated in the figure. Data from BISO ThO₂ tests (Ref. 9-1) are also shown. Release of each species (Eu, Ce, and Ru) appears to be independent of coating type implying (1) that the release occurred after the SiC layer on the TRISO samples was rendered ineffective as a barrier against metallic fission product release, and (2) that release of these species is controlled by release from the oxide kernels. This suggests that Eu is retained by oxide kernels at temperatures up to 2250°C and that Ce and Ru are retained at temperatures up to ~2450°C.

Lanthanide fission product - SiC reactions are known to cause SiC failure in irradiated TRISO UC₂ fuel (Refs. 9-12, 9-13). Cerium and Eu release precedes Cs release when this phenomenon causes SiC failure in TRISO UC₂. Cesium release occurred before Ce release in these oxide samples, implying that Ce-SiC reactions were not responsible for the SiC degradation in the TRISO oxide samples. Cesium and Eu release fractions from the TRISO oxide samples were similar. It is possible therefore that Eu-SiC reactions were responsible for the SiC degradation, leading to the conclusion that lanthanide fission product - SiC reactions may limit the performance of both TRISO UC₂ and TRISO ThO₂ under conditions predicted for an LOFC core heatup event.

Data in Table 9-3 and Ref. 9-1 show that very little of the Eu released from fuel particles is retained by the H-451 graphite crucibles at temperatures in excess of 2190°C. No data are available from these tests at temperatures less than 2190°C. The H-451 graphite retains in excess of 80% of the Ce and Ru released from test particles at temperatures as high as 2475° to 2525°C.

Discussion

Release of Kr and Cs From Oxide Fuel With Failed Coatings. A series of tests designed to evaluate Kr, Xe, and Cs release from irradiated ThO₂

Fig. 9-18. Variation in Ru-106, Ce-144, and Eu-154 release fractions from BISO and TRISO ThO_2 fuel with peak temperatures experienced during core heatup simulation testing

kernels was recently conducted using simulated core heatup conditions (Refs. 9-11, 9-14). Semi-empirical models are being developed for Kr, Xe, and Cs release from kernels in failed coatings. The models are a function of irradiation temperature, kernel burnup, and time and temperature during a core heatup temperature excursion. The major difference between the test conditions and those described in this report was the composition of the He gas used to transport fission products to their respective traps. The He atmosphere used in the kernel tests contained a small quantity of CO to inhibit conversion of the oxide kernels to carbide. The tests described in this report did not utilize CO to inhibit oxide conversion after coating failure.

When completed, the models for fission product release from ThO_2 kernels heated in an oxidizing atmosphere will be used to evaluate results obtained in this series to determine if conversion from ThO_2 to ThC_2 would have an impact on fission product release during an LOFC core heatup event. Qualitative comparisons show the differences in fission product release observed under the two test conditions to be small.

TRISO ThO_2 Versus TRISO ThC_2 . Krypton-85 and Cs-137 release fractions are shown as a function of temperature in Figs. 9-19 and 9-20, respectively, for the TRISO ThC_2 sample and all TRISO oxide samples without an initial laser-failed fuel fraction. Krypton-85 release from the TRISO ThO_2 samples is slightly higher than from the carbide sample at temperatures less than 2100°C. This is associated with the small TRISO ThO_2 pressure failure fraction (~ 0.03) caused by a nonconservative pressure vessel design. The pressure vessel design of TRISO ThO_2 being considered as HTGR fertile fuel is more conservative than the ThO_2 and $(\text{Th/U})\text{O}_2$ samples used in these tests. One would therefore expect the gaseous fission product release from TRISO ThO_2 to be less than or equal to that from TRISO ThC_2 during a core heatup. Cesium release fractions from TRISO ThO_2 will be less than or equal to those expected from TRISO ThC_2 .

Fig. 9-19. Comparison of Kr-85 release fractions versus temperature obtained during core heatup simulation testing of TRISO ThC₂ and TRISO oxide fertile fuels

Fig. 9-20. Comparison of Cs-137 release fractions versus temperature obtained during core heatup simulation testing of TRISO ThC_2 and TRISO oxide fertile fuels

Comparison of FSV Model Predictions and Experimental Observations.

One purpose of this test program was to provide the data required to compare measured gaseous fission product release fractions from TRISO ThC₂ and TRISO ThO₂ with those predicted for an LOFC core heatup event. Predictions were made using fuel failure and particle fission product release models developed for FSV (Ref. 9-5) to evaluate some of the conservatisms inherent in the design of an operating HTGR. The FSV release models assume fission product holdup within coated particles after coating failure. Fission product release from the failed fuel is a function of time and temperature. There are effectively three stages to the fuel failure models. The fuel failure fraction at the onset of an LOFC event is assumed to be $\frac{0.10}{6}(t)$, where t equals the fuel age in years; the fuel failure fraction increases slowly, based on time at temperature, with temperatures up to 1725°C; and at temperatures greater than 1725°C, the failure fraction is assumed equal to 1.0. The fuel used in this experimental program was irradiated to fast neutron exposures and/or kernel burnups greater than or equal to peak values expected in 6-yr-old FSV fuel. FSV model predictions were made for 2-yr, 4-yr, and 6-yr-old fuel assuming a linear rise in temperature from 1100° to 2500°C in 8, 30, or 80 h (Ref. 9-15).

Predicted Kr-85 release fractions are compared with data collected during a 26.5-h ramp test of TRISO ThC₂ (FSV fertile B) fuel in Fig. 9-21. Krypton-85 predictions made assuming 2-yr and 6-yr-old fuel are shown in Fig. 9-21 to illustrate the effect of the assumed initial failed fuel fraction on Kr release estimates. The FSV models used to estimate Kr release are extremely conservative relative to the actual performance of TRISO ThC₂. Experimental Kr release fractions are equivalent to predicted fractions at temperatures 400°C in excess of predicted temperatures. This is equivalent to a 7.5-h delay in gaseous fission product release relative to predictions made for a 26.5-h temperature ramp.

Fig. 6-21. Comparison of Kr-85 release fractions determined during a CHST conducted on TRISO ThC₂ (FSV fertile B) particles with predictions made using FSV fuel failure and particle fission product release models

Predicted and observed Kr-85 release fractions are compared in Fig. 9-22 for the TRISO (Th/U)O₂ sample with an initial failed fuel fraction of 0.10. The predictions assume an initial failed fuel fraction of 0.10. The predicted Kr release fractions are again extremely conservative. Experimental and predicted Kr release fractions are equivalent at temperatures 200° to 600°C higher than predicted.

Measured and predicted Kr-85 release fractions are compared for the remaining oxide tests in Figs. 9-23 through 9-25. The predictions shown in each case assumed 2-yr-old fuels even though irradiation exposures of the test samples were equivalent to 6-yr-old fuel. The 2-yr predictions were used for comparative purposes because the initial failed fuel fraction is less than assumed for 6-yr fuel and these test samples had an initial failed fuel fraction of 0. The Kr predictions are, in each case, extremely conservative relative to experimental observations.

Based on results of tests conducted on 476 irradiated TRISO oxide fertile and 101 irradiated TRISO ThC₂ fertile particles, it can be stated conclusively that the models used to describe failure and gaseous fission product release from fuel particles during an LOFC core heatup in FSV are conservative.

TASK 600: FUEL DESIGN AND PERFORMANCE MODELS

Summary and Conclusions

A coating design was developed for TRISO ThO₂, which is the backup LHTGR fertile fuel. The design was based on calculated SiC coating pressure vessel failure stresses developed from FSV TRISO (Th/U)C₂ fissile and TRISO ThC₂ fertile fuel irradiation data. The TRISO ThO₂ fuel was designed so that pressure vessel failure fractions would be less than expected from FSV carbide fuel after irradiation to equivalent

Fig. 9-22. Comparison of Kr-85 release fractions determined during a CHST conducted on TRISO (Th,U)O₂ particles with an initial fuel failure fraction of 0.1 with predictions made using FSV fuel failure and particle fission product release models

Fig. 9-23. Comparison of Kr-85 release fractions determined during 9.0- and 10.2-h CHSTs conducted on TRISO oxide fertile fuel with predictions made using FSV fuel failure and particle fission product release models

Fig. 9-24. Comparison of Kr-85 release fractions determined during a 28.3-h CHST of TRISO ThO₂ fertile fuel with predictions made using FSV fuel failure and particle fission product release models

Fig. 9-25. Comparison of Kr-85 release fractions determined during 17.1- and 70-h CHSTs of TRISO oxide fertile fuel with predictions made using FSV failure and particle fission product release models

conditions. The nominal dimensions for one oxide fuel that satisfies the design criteria are: kernel diameter = 450 μm and coating thicknesses = buffer 65 μm , inner PyC 35 μm , SiC 35 μm , outer PyC 45 μm . Studies are continuing to develop a design with similar pressure vessel characteristics that has a smaller total volume.

Introduction

The reference LHTGR fertile fuel is BISO coated ThO_2 ; the backup fertile fuel is TRISO coated ThO_2 . The TRISO ThO_2 particles are designed to minimize the pressure vessel failure fraction expected during service in a LHTGR. The expected failure fraction is determined by comparing calculated SiC layer stress distributions with the apparent failure stress (AFS) for the SiC layer. The AFS is determined empirically from accelerated irradiation capsule results by comparing stress distributions calculated for individual irradiation samples with the pressure vessel failure fraction observed during irradiation. It is assumed in these studies that the probability for pressure vessel failure is 0 if a calculated stress is less than the AFS or 1 if a calculated stress is equal to or greater than the AFS.

A series of irradiation tests is under way to demonstrate the performance capabilities of TRISO ThO_2 . The initial design for TRISO ThO_2 was based on AFS values determined from irradiation experiments conducted on FSV TRISO $(\text{Th/U})\text{C}_2$ fissile and TRISO ThC_2 fertile fuel. The oxide fuel was designed so that the pressure vessel failure of TRISO ThO_2 would be less than or equal to that of FSV TRISO carbide fuel after irradiation to equivalent conditions. Studies leading to AFS values for FSV carbide fuels and the initial TRISO ThO_2 particle design are summarized.

Coated Particle Stress Models

The models used to calculate coating stresses that develop during the irradiation of TRISO coated fuel particles were developed in 1969 (Refs. 9-10, 9-16). The TRISO particle is treated as a composite pressure

vessel in which internal pressure increases during irradiation as fission gasses accumulate within the buffer layer void volume. The void volume is estimated from the as-deposited buffer layer density assuming that the buffer densifies to 2.0 Mg/m^3 during irradiation. The models assume that the SiC is dimensionally stable during irradiation and that the inner and outer PyC layers are subject to irradiation-induced densification, creep, and anisotropic dimensional changes (Ref. 9-17).

The SiC layer stress distribution is calculated for any irradiation sample using the TRISO particle stress code in conjunction with a Monte Carlo routine that randomly varies kernel and coating dimensions. The average and standard deviation of each dimension are used as input to the Monte Carlo routine. SiC stresses are calculated for 1000 particles to develop the stress distribution for any single irradiation sample.

With the exception of dimensions, input parameters used to predict the irradiation characteristics of the buffer, inner PyC, SiC, and outer PyC coatings are independent of kernel type. Kernel characteristics are also similar with the exception of gaseous species formed during irradiation. Equal quantities of fission gases (primarily Kr and Xe) are formed per fission in ThO_2 and ThC_2 kernels; it is assumed that all Kr and Xe formed are released into the buffer void volume. Thorium dioxide also releases O (as CO) into the buffer. The quantity released is a function of temperature and the valence state of oxide-forming fission products. The empirical expression used to estimate the moles of O released from ThO_2 per mole of fissions (O/f) is (Ref. 9-18):

$$\text{O/f} = 74.6F e^{-7400/T}, \quad (9-1)$$

where F = burnup (% FIMA),

T = temperature (K).

Apparent Failure Stress; FSV Carbide Fuels

The initial design for TRISO ThO_2 was based upon the AFS determined for FSV $(\text{Th/U})\text{C}_2$ fissile and ThC_2 fertile fuel since the kernel burnups and particle sizes for the FSV fuels are similar to those expected for TRISO ThO_2 . Irradiation results obtained from capsule F-30 (Ref. 9-6), which was the proof test for FSV carbide fuel, were used. Fissile and fertile fuel particles were irradiated in F-30 as unbonded particles and in fuel rods. The AFS for FSV carbide fuel was determined from F-30 unbonded particle performance data. Kernel and coating properties of the irradiation samples are summarized in Table 9-11.

An example of the method used to determine the AFS for a single irradiation sample is given in Fig. 9-26, which shows the calculated SiC layer stress distribution after irradiation of fertile fuel test sample CT6A-2835. The ordinate is the probability that a calculated SiC stress exceeds the value shown on the abscissa. The total particle failure fraction for this sample was 0.003. The stress exceeded by three particles in 1000 is -10 MPa (-1500 psi); therefore, the AFS for this sample is -10 MPa (-1500 psi).

The irradiation conditions and failure fractions are given for each F-30 unbonded particle sample in Table 9-12. Total particle failure data were estimated from postirradiation fission gas release (R/B,* Kr-85m) measurements made at 1100°C assuming an R/B per failed particle of 0.025; outer PyC failure fractions were based on visual examinations (Ref. 9-6). The AFS values are also given. The AFS values were determined assuming (1) that total particle failure resulted from simultaneous failure of the SiC and outer PyC layers, or (2) that SiC failure occurred after outer PyC failure. The average and standard deviations in the average AFS determined by the first method are -37 and 33 MPa (-5300 and 4800 psi), respectively. The average and standard deviations in the average AFS determined by the second method are 11 and 23 MPa (1600 and 3400 psi),

*R/B = release to birth ratio.

TABLE 9-11
 KERNEL AND COATING PROPERTIES OF TRISO (Th/U)C₂ AND TRISO ThC₂ SAMPLES IRRADIATED IN CAPSULE F-30

Particle Batch Data Retrieval Number	Kernel				Coatings													Total Coated Particle	
					Buffer			IPyC			SiC			OPyC					
	Type	Diameter (μm)		Density (Mg/m ³)	Thickness (μm)		Density (Mg/m ³)	Thickness (μm)		Density (Mg/m ³)	Thickness (μm)		Coating Temp (°C)	Thickness (μm)		Density (Mg/m ³)	Coating Temp (°C)	Mean Diameter (Calculated) (μm)	Density (Mg/m ³)
		Mean	σ		Mean	σ		Mean	σ		Mean	σ		Mean	σ				
CU6B-2422	(Th,U)C ₂	252	18.42	9.24	53	5.84	1.18	26	5.47	1.89	26	2.22	1525	43	4.89	1.80	1430	548	2.70
CU6B-2427	(Th,U)C ₂	252	16.30	9.18	59	8.82	1.11	25	3.81	1.85	24	2.24	1525	42	4.61	1.78	1430	552	2.65
CU6A-6326	(Th,U)C ₂	180	22.35	9.19	51	10.21	1.16	25	4.75	1.89	27	2.77	1525	38	5.18	1.83	1430	462	2.41
CU6A-6328	(Th,U)C ₂	180	23.13	9.19	54	13.51	1.10	23	3.02	1.89	28	2.46	1525	39	4.50	1.84	1430	468	2.41
CT6A-2835	ThC ₂	366	31.67	8.94	44	14.03	1.17	39	7.91	1.94	26	2.97	1575	43	6.62	1.76	1500	670	3.13
CT6A-2399	ThC ₂	374	35.11	8.89	50	14.45	1.19	29	3.69	1.89	27	2.65	1575	41	6.23	1.80	1550	668	3.27
CT6B-914	ThC ₂	443	31.05	8.90	46	16.96	1.14	35	7.71	1.91	26	2.60	1575	47	10.12	1.79	1550	765	3.29
CT6B-932	ThC ₂	437	24.42	8.90	49	14.71	1.14	35	7.71	1.91	26	2.67	1575	48	8.44	1.80	1550	753	3.38

Fig. 9-26. Calculated SiC stress distribution for TRISO ThC₂ batch CT6A-2835 after irradiation to 10.3×10^{25} n/m² ($E > 29$ fJ)_{GETR} and 4.7% FIMA at 1139°C

TABLE 9-12
APPARENT SiC FAILURE STRESS (AFS) ESTIMATES FOR FSV REACTOR TRISO (Th/U)C₂ FISSILE AND
TRISO ThC₂ FERTILE FUEL

Data Retrieval Number ^(a)	Irradiation Conditions			Failure Fraction		AFS (10 ⁴ psi) ^(d)	
	Fast Neutron Exposure (10 ²⁵ n/m ²) (E ≥ 29 fJ) _{GETR}	Kernel Burnup (% FIMA)	Temp (°C)	Total Particle ^(b)	Outer PyC ^(c)	SiC + Outer PyC Failure ^(e)	SiC Failure After Outer PyC Failure ^(f)
CU6B-2422	10.3	20.1	1238	0.091	0.004	-1.42	(g)
CU6B-2427	10.2	20.1	1251	0.004	0.010	-1.19	0.76
CU6A-6326	10.3	18.3	1231	0	0.006	>-0.88	(g)
CU6A-6326	7.3	15.3	1173	0	0	>-1.61	(g)
CU6A-6328	10.2	18.2	1243	0.001	0.002	0.25	-0.16
CU6A-6328	7.2	15.0	1250	<0.001	0.001	>-1.30	-0.03
CT6B-914	10.3	4.7	1170	0.006	0.010	3.42	-0.05
CT6B-914	7.3	2.4	1064	0.002	0.010	-1.82	-1.15
CT6B-932	10.2	4.7	1160	<0.001	0.025	>0.84	1.72
CT6B-932	7.2	2.3	1070	0.011	0.008	-3.01	(g)
CT6A-2399	10.2	4.7	1140	0	0.022	>0.50	(g)
CT6A-2835	10.3	4.7	1139	0.003	0.023	-0.15	0

(a) CU are fissile samples; CT are fertile samples.

(b) Based on postirradiation fission gas release measurements.

(c) Based on visual examination.

(d) 10⁴ psi value x 6.9 = MPa value.

(e) Assumes simultaneous SiC and outer PyC failure.

(f) Assumes SiC failure after outer PyC failure.

(g) Calculations not done if total particle failure fraction = 0 or if total particle failure fraction greater than or equal to outer PyC failure fraction.

respectively. It is not possible to determine the mode of pressure vessel failure from the F-30 data. Since the difference in the average AFS values is not significant at a 95% confidence level, AFS data based on both failure assumptions were combined to yield an average AFS of -19 MPa (-2800 psi) for FSV carbide fissile and fertile fuel. The standard deviation of the average AFS is 23 MPa (3300 psi).

The AFS value implies that the SiC layer is failing in compression, which is physically unreasonable. The major input parameters that affect the calculated stresses are irradiation-induced PyC dimensional change data and creep coefficients and the assumed value for the buffer layer density after irradiation. Small changes in these parameters could increase the AFS above 0 MPa. These changes would not, however, change the relative relationship between the various samples or predictions based on the calculated AFS as long as consistent assumptions are made for all studies. The standard input data will therefore be used, and AFS values less than 0 will be accepted.

Mixtures of fissile and fertile particles are combined into fuel rods for irradiation in a HTGR using a carbonaceous matrix. The AFS, determined from unbonded particle samples, is used in conjunction with calculated SiC stress distributions to estimate fuel failure fractions and fission product release from fuel rods. The accuracy of predictions made using the unbonded particle AFS values was tested by predicting fuel failure fractions and Kr-85m R/B values for F-30 fuel rods. The criteria employed when selecting fuel rods for this study were:

1. Postirradiation fission gas release values were measured at 1100°C.
2. The postirradiation fission gas release values measured on all samples from a single irradiation cell agreed with end-of-life in-pile values.

3. Metallographic examinations were performed so that outer PyC failure fractions were known.

Two fuel rods from capsule F-30 (3A-14 and 4B-15) met the three stated criteria. The fissile and fertile fuel, irradiation conditions, outer PyC failure fractions, and postirradiation fission gas release values for these rods are given in Table 9-13. Fissile and fertile fuel failure fractions were estimated for each rod assuming AFS values at 5% [-67 MPa (-9700 psi)], 50% [-19 MPa (-2800 psi)], and 95% [28 MPa (4100 psi)] confidence levels. Failure fractions were estimated separately for fuel with intact and failed outer PyC layers and then normalized to account for the fraction of particles having failed outer PyC layers. No pressure vessel failure was predicted for the fissile fuel in either fuel rod. Predicted fertile particle failure fractions are shown for each rod in Table 9-14 assuming AFS values at 5%, 50%, and 95% confidence levels.

The R/B ratio for Kr-85m was estimated from the failure fractions for each rod assuming an R/B per failed particle of 5×10^{-3} (Ref. 9-19) at 1100°C. The R/B for Kr-85m at 1100°C was calculated using

$$R/B_{\text{Kr-85m at 1100°C}} = \left(\frac{\alpha_{\text{fis}}^f \alpha_{\text{fis}}^Y Y_{\text{fis}} + \alpha_{\text{fer}}^f \alpha_{\text{fer}}^Y Y_{233}}{\alpha_{235}^Y Y_{235} + \alpha_{233}^Y Y_{233}} \right) 5 \times 10^{-3}, \quad (9-2)$$

where

- α_{fis} = fraction of fissions in fissile fuel,
- α_{fer} = fraction of fissions in fertile fuel,
- α_{233} = fraction of fissions produced by U-233,
- α_{235} = fraction of fissions produced by U-235,
- Y_{fis} = Kr-85m yield per fission in fissile fuel,

TABLE 9-13
PARAMETERS NEEDED TO ESTIMATE FUEL FAILURE FRACTIONS AND Kr-85m R/B VALUES FOR F-30
FUEL RODS 3A-14 AND 4B-15

	3A-14	4B-15
Fissile fuel		
Batch number	CU6A-6326	CU6A-6328
Kernel burnup (% FIMA)	19.1	17.7
Outer PyC failure fraction	0	0
Fertile fuel		
Batch number	CT6A-2835	CT6A-2399
Kernel burnup (% FIMA)	5.7	4.3
Outer PyC failure fraction	0.12	0.26
Irradiation parameters		
Temperature (°C)	1050	1079
Fast neutron exposure (10^{25} n/m ²) ($E \geq 29$ fJ) _{GETR}	10.6	9.4
Fraction Th in fertile fuel	0.89	0.87
Fraction fissions in U-233	0.93	0.92
Postirradiation fission gas release rate (R/B Kr-85m) at 1100°C	1.2×10^{-4}	2.2×10^{-5}

TABLE 9-14
 PREDICTED PRESSURE VESSEL FAILURE FRACTIONS^(a) AND Kr-85m R/B VALUES FOR F-30
 FUEL RODS 3A-14 AND 4B-15

	3A-14	4B-15
Fertile fuel failure fraction		
AFS = -67 MPa (-9700 psi) ^(b)	0.124	0.230
AFS = -19 MPa (-2800 psi) ^(c)	0.066	0.055
AFS = 28 MPa (4100 psi) ^(d)	0.029	0.010
Kr-85m R/B at 1100°C		
AFS = -67 MPa (-9700 psi) ^(b)	5.3×10^{-4}	9.5×10^{-4}
AFS = -19 MPa (-2800 psi) ^(c)	2.8×10^{-4}	2.3×10^{-4}
AFS = 28 MPa (4100 psi) ^(d)	1.2×10^{-4}	4.1×10^{-5}
Kr-85m R/B measured at 1100°C during postirradiation examination	1.2×10^{-4}	2.2×10^{-5}

- (a) No fissile fuel pressure vessel failure predicted for 3A-14 or 4B-15.
 (b) AFS at the 5% confidence level.
 (c) AFS at the 50% confidence level.
 (d) AFS at the 95% confidence level.

$$\begin{aligned}
Y_{233} &= \text{Kr-85m yield per U-233 fission,} \\
Y_{235} &= \text{Kr-85m yield per U-235 fission,} \\
f_{fis} &= \text{fissile fuel failure fraction,} \\
f_{fer} &= \text{fertile fuel failure fraction.}
\end{aligned}$$

Since f_{fis} was 0 for the fuel rods in question, Eq. 9-2 reduces to

$$R/B_{\text{Kr-85m at } 1100^{\circ}\text{C}} = \left(\frac{f_{Th} \alpha_{233} f_{fer} (0.022)}{\alpha_{235} (0.013) + \alpha_{233} (0.022)} \right) 5 \times 10^{-3}, \quad (9-3)$$

where f_{Th} = fraction of Th in fertile fuel,

$$(f_{Th}) (\alpha_{233}) = \alpha_{fer},$$

$$0.022 = Y_{233} \text{ (Ref. 9-20),}$$

$$0.013 = Y_{235} \text{ (Ref. 9-20).}$$

Krypton-85m R/B values consistent with failure predictions made assuming 5%, 50%, or 95% confidence AFS values are also shown in Table 9-14. The predicted Kr-85m R/B values most consistent with observed values are, in both cases, developed from the 95% confidence limit for AFS [28 MPa (4100 psi)]. This implies that the best estimate for AFS [-19 MPa (-2800 psi)] determined from the F-30 unbonded particle samples is conservative and that pressure vessel failure fractions will be less than those predicted using an AFS of -19 MPa (-2800 psi).

There are two easily identifiable sources for conservatism in the unbonded particle data. The first is that the best estimate for AFS was

based on some samples in which failure fractions less than 0.001 were observed. In these cases, the AFS was said to be greater than the maximum stress calculated for 1000 particles (see Table 9-12). These values were used when calculating the average values for AFS. The second source is associated with the total particle failure fraction which was determined from postirradiation fission gas release measurements. Mixed (Th/U)C₂ and ThC₂ kernels are known to hydrolyze if exposed to air. This can increase the R/B per failed particle significantly (Ref. 9-19). Since the total particle failure fractions shown in Table 9-12 were calculated assuming the R/B per failed particle of nonhydrolyzed fuel, the failure fractions may be overestimated. Failure fractions determined for these samples from fission gas release data are in fact higher than those visually determined (Ref. 9-6).

TRISO ThO₂ Design

TRISO ThO₂ particles were designed, using FSV carbide AFS values, so that oxide pressure vessel failure fractions would be less than or equal to those expected for FSV carbide fuels after irradiation to equivalent conditions. This was done by choosing ThO₂ kernel and coating properties so that the probability for exceeding the AFS at a 5% confidence level [-70 MPa (-9700 psi)], a 50% confidence level [-19 MPa (-2800 psi)] and a 95% confidence level [28 MPa (4100 psi)] would be less than the same probabilities for FSV TRISO ThC₂ fertile B fuel. The stress distributions were compared assuming irradiation to a kernel burnup of 7% FIMA and a fast neutron exposure of $8 \times 10^{25} \text{ n/m}^2$ ($E \geq 29 \text{ fJ}$)_{HTGR} at 1250°C.

The TRISO ThC₂ properties used for this comparison were those determined for FSV segment 8 (reload 1) fertile B fuel production (Ref. 9-21). Kernel and coating properties are given in Table 9-15. The initial ThO₂ design was developed assuming a 450 μm average kernel diameter with a standard deviation of 5%. The inner PyC, SiC, and outer PyC properties

TABLE 9-15
TRISO ThC₂ AND TRISO ThO₂ PROPERTIES ASSUMED TO DEVELOP THE TRISO ThO₂
PRESSURE VESSEL DESIGN

Particle Type	TRISO ThO ₂	TRISO ThC ₂
Kernel		
Diameter (μm)	450	446
Standard deviation (μm)	22.5	36.1
Density (Mg/m ³)	9.8	8.81
Buffer		
Thickness (μm)	(a)	53
Standard deviation (μm)	(b)	11.54
Density (Mg/m ³)	1.05	1.06
Inner PyC		
Thickness (μm)	35	27
Standard deviation (μm)	5.7	5.0
Density (Mg/m ³)	1.95	1.95
SiC		
Thickness (μm)	35	25
Standard deviation (μm)	5.0	3.6
Density (Mg/m ³)	3.20	3.20
Outer PyC		
Thickness (μm)	45	46
Standard deviation (μm)	5.7	8.4
Density (Mg/m ³)	1.80	1.80

(a) Average buffer thicknesses ($t_{B \text{ avg}}$) were varied.

(b) Estimated using $[(0.15 t_{B \text{ avg}})^2 + 25]^{1/2}$ to account for distribution.

of thicknesses in a single production run and the distribution of averages allowed by a fuel specification.

are the same as those anticipated from future HTGR TRISO UC_2 production (Ref. 9-21). The average buffer thickness was varied to determine the value leading to a particle with a pressure vessel design at least as conservative as the TRISO ThC_2 FSV fertile B fuel. The TRISO oxide properties are shown in Table 9-15.

Results of the design study are summarized in Table 9-16. The nominal dimensions of a batch of TRISO ThO_2 fuel satisfying the design criteria are: kernel diameter 450 μm , buffer thickness 65 μm , inner PyC thickness 35 μm , SiC thickness 35 μm , and outer PyC thickness 45 μm . The SiC stress distributions calculated for the TRISO ThC_2 fertile B and initial TRISO ThO_2 fuels are shown in Fig. 9-27. The design shown for ThO_2 should result in a particle that has better performance than the ThC_2 particle. Based on analyses of the irradiation performance of F-30 fuel rods, the pressure vessel failure fraction of the oxide and carbide fuels should be less than 0.003 after irradiation to 7% FIMA and $8 \times 10^{25} \text{ n/m}^2$ ($E \geq 29 \text{ fJ}$)_{HTGR} at 1250°C. Studies are continuing to define an equivalent ThO_2 particle design with a goal of reducing the volume occupied by the particle.

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TABLE 9-16
TRISO ThC₂ - TRISO ThO₂ PRESSURE VESSEL DESIGN STUDY

Particle Type	Kernel Diameter/ Buffer Thickness/ Total Particle Diameter (μm)	Probability That Calculated SiC Stresses Exceed		
		-67 MPa (-9700 psi)	-19 MPa (-2800 psi)	28 MPa (4100 psi)
TRISO ThC ₂ (FSV fertile B)	446/53/748	0.018	0.006	0.003
TRISO ThO ₂	450/60/800	0.026	0.005	0.003
	450/65/810	0.015	0.003	0.001
	450/70/820	0.009	0.001	<0.001
	450/80/840	0.001	<0.001	<0.001

Fig. 9-27. Comparison of SiC stress distributions calculated for TRISO ThC₂ FSV fertile B fuel and the initial TRISO ThO₂ fertile fuel considered for an HTGR

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11. GRAPHITE DEVELOPMENT^{*}
189a NO. 00552; PROGRAM AREA 11

Graphites that will satisfy the requirements for LHTGR components are being developed and evaluated. Characterization and irradiation studies to establish reference and backup grades for fuel elements, core support components, and side reflectors are in progress. Support technology is being developed to provide data for design and safety analyses. Development of control materials is included in the long-range program but no work is being funded during FY-78.

Preliminary characterization work aimed at selecting support graphites for in-depth studies revealed characteristics of the candidate materials that required further study before they could be committed as reference grades. Decisions on whether to develop backup grades for support structures are pending.

The irradiation work was suspended during FY-77 due to the temporary closing of the Oak Ridge Reactor (ORR). Assembly of capsule OG-5 was completed during FY-77 and the capsule is being stored at GA. It is not clear at this time when funds will be available to complete the irradiation.

Support technology studies include generic studies of the mechanical behavior of graphites, verification of stress calculation methods, and the effects of oxidation on the strength and integrity of the core and core support blocks. Current work is concentrated on the behavior of PGX graphite under oxidizing conditions and studies of fracture mechanics aimed at determining fracture mechanisms.

^{*} Now a program area under the HTGR Generic Technology Program.

Task numbers have been changed to correspond with those currently used in 189a No. SU00552.

TASK 100: CHARACTERIZATION OF CANDIDATE GRAPHITES FOR PROPERTIES
AND PURITY (FORMERLY TASK 300)

Replaceable Fuel and Reflector Elements

Characterization of production grade H-451 graphite for flexural strength is reported. Three graphite logs (7194-49, -52, and -87) from Great Lakes Carbon Company (GLCC) production lot 478 produced as a portion of the order for reload segments of the Fort St. Vrain reactor were evaluated.

The mean flexural strength ranged from 20.7 to 28.4 MPa (3000 to 4120 psi) in the axial direction and from 18.1 to 26.1 MPa (2630 to 3790 psi) in the radial direction. The minimum mean flexural strength of the radial specimens occurred at the midlength center, while the axial specimens exhibited minimal flexural strength at either the end center or midlength center locations. Test data for the three logs are presented in Tables 11-1 through 11-3.*

Support Post and Blocks and Permanent Side Reflectors

Tensile test data obtained by GA on 2020 graphite were lower than the values published by the manufacturer and did not compare favorably with flexural tests performed at GA. Therefore, a round-robin test was initiated among GA, Stackpole Carbon Company (SC), and Southern Research Institute (SoRI) to clarify the suspect data. The results of the round-robin tests are reported. Comparative tensile tests were conducted by GA and SoRI and comparative flexural tests by GA and SC. All specimens were taken from the same sample population.

* Tables appear at the end of the text.

Companion samples were cored from a standard production log of grade 2020 graphite (slab 3, GA log No. 6484-110, Figs. 11-1 and 11-2).^{*} Randomly selected sample cores from the same region were distributed to the three testing laboratories to be machined into the test specimen geometries given in Table 11-4. The GA tensile specimens consisted of a graphite cylinder glued to aluminum end pieces as contrasted to the reduced section (dogbone) tensile specimen routinely tested by SoRI (Fig. 11-3). The GA flexural test specimen was also cylindrical as opposed to a rectangular cross section used by SC. The SC flexural tests utilized a three-point load application while GA used a four-point technique.

The mean flexural strength reported by SC was 29.4 MPa (4262 psi), which fell between the modulus of rupture (MOR) [32.5 MPa (4710 psi)] and the flexural strength [26.6 MPa (3860 psi)] measured at GA. The flexural strengths reported by GA were calculated from MOR data by incorporating the nonlinearity (plasticity) of the stress-strain curve in calculating the stress field in the specimens, whereas SC calculated flexural strength following a linear elastic analysis. Consequently, the flexural strength is always less than the MOR. The standard deviations of the SC flexural strength and the GA MOR data were 2.3 MPa (330 psi).

Tensile strength values reported by SoRI were about twice the GA values (Table 11-5). Because of the discrepancy, the tensile test method used by GA employing glued cylindrical specimens was re-examined for 2020 graphite. Additional tests indicated the multiaxial stress field imposed on the graphite cylinder at the glued joint led to premature failure of the test specimen at the joint. Tensile tests using specimens from the same population as the original tests were performed using ASTM standard C749-73, which utilizes a dogbone specimen geometry. The values for these tests averaged 22.2 MPa (3220 psi) compared with the SoRI average of 22.5 MPa (3260 psi).

The following conclusions were drawn from the round-robin tests:

^{*} Figures appear at the end of Section 11.

1. The behavior of 2020 graphite under multiaxial loading may be load sensitive and the mechanics of fracture different than those of coarse-grained reactor graphite such as H-451 or PGX.
2. The ultimate tensile strength of the 2020 log used for the round-robin tests is 20.7 MPa (3000 psi) or greater.
3. There was good agreement between the SC and GA flexural strength data.
4. There was excellent agreement between the SoRI and GA tensile strength data when dogbone-type specimens were used.

A reappraisal of the tensile strengths reported previously on coarse-grained graphite such as H-451 and PGX where the "glued cylindrical specimen" technique was used is under way. Additional tests will be made using dogbone specimens to reappraise the tensile strength of the logs measured previously.

The preliminary findings from the round-robin tests indicate the strength of grade 2020 may be higher than previous results indicated; thus, development of a high-strength grade H-440 as a support post material will not be pursued vigorously at this time. Grade H-440 will continue to be monitored through contacts with GLCC until it is confirmed that 2020 is suitable for support post design. The above decision on grade H-440 completes milestone 1 under Task 100: Characterization of Support and Side Reflector Graphites, dated February 24, 1978.

TASK 200: FABRICATION AND OPERATION OF IRRADIATION CAPSULES (FORMERLY TASK 100)

Capsule Fabrication and Irradiation

Capsule OG-5 was constructed in FY-77 and is ready for insertion in the Oak Ridge Reactor when funds become available. In the meantime the capsule is being stored at GA.

TASK 300: GRAPHITE SPECIMEN PREPARATION AND PROPERTY MEASUREMENTS
FOR CAPSULE IRRADIATIONS (FORMERLY TASK 200)

No work funded during FY-78.

TASKS 400 THROUGH 700: DEVELOPMENT OF CONTROL MATERIALS
(FORMERLY TASK 900)

No work funded in FY-78.

TASK 800: OXIDATION STUDIES (FORMERLY TASK 1000)

Efforts are being concentrated on the characterization of PGX graphite, which is a candidate for support floor blocks. The work includes studies of oxidation reaction rates and the effect of oxidation on mechanical properties and verification of oxidation characteristics under HTGR conditions.

Reaction Rate Studies

The rate of steam oxidation of PGX graphite was determined. The effect of parameters such as water concentration, hydrogen concentration, burnoff, temperature, and specimen geometry or surface-to-volume ratio on the reaction rate is under investigation. In addition, density profiles were determined on oxidized PGX specimens and compared to profiles predicted from calculations.

Specimen Preparations

All specimens were taken from PGX logs 6484-138 and 6484-112 in accordance with a standard sampling plan shown in Figs. 11-4 through 11-6 and Tables 11-6 and 11-7. The oxidation rate specimens were either thin disks (32-mm diameter by 1.6 mm thick or right solid cylinders (ranging in diameter from 6.4 to 32 mm by 76 mm long). The PGX logs were standard

commercial-grade product, both having high ash and metallic impurity content. Log 112 was previously reported to average 1500 ppm Fe and log 138 200 ppm Fe (Refs. 11-1, 11-2) when specimens taken at locations throughout the logs were measured by emission spectroscopy. More recent measurements on localized specimens taken specifically for oxidation work on logs 112 and 138 showed Fe contents in the range 2900 to 3200 ppm and 1250 to 1280 ppm, respectively, when measured by an atomic absorption method.

Experimental Procedure and Apparatus

Reaction rate studies were carried out in automatic recording micro-balances described elsewhere (Ref. 11-3). In reaction rate tests thin disks were used to minimize the limitations of in-pore diffusion. A gas flow rate of 1.0 liter/min, corresponding to a velocity of 35 mm/s at 1273 K (1000°C), was used in all tests.

Experimental Results

The effect of the H_2/H_2O ratio and percent burnoff on the reaction rate is shown in Fig. 11-7. The upper curves where $H_2/H_2O = 6.7$ and 10 show reaction rates increasing with percent burnoff, whereas the lower curve where $H_2/H_2O = 0.3$ shows a decrease in rate versus burnoff. It has been previously reported (Ref. 11-4) that under the test conditions given in Fig 11-7 for $H_2/H_2O = 0.3$ and at equilibrium, iron impurities exist as an oxide. Assuming iron is the primary catalyst, the data in Fig. 11-7 suggest that during the exposure period iron was slowly oxidized, thereby diminishing its catalytic activity. This behavior is consistent with work at Brookhaven National Laboratory (Ref. 11-5) which has shown that iron in the reduced state is much more effective as a catalyst than iron in the oxide form.

The effect of water vapor concentration on reaction rate at two temperatures is shown in Fig. 11-8. The reaction rate is considered to be

linear with respect to PH_2O . (The linear line is the dashed line in Fig. 11-8.)

The effect of sample geometry on reaction rate is shown in Fig. 11-9. At higher temperatures the reaction rate per geometric surface area is essentially constant with the surface-to-volume ratio (S/V). At temperatures above 1093 K (820°C) essentially all moisture that diffuses into the specimen reacts. At lower temperatures, however, a definite dependence of surface reaction rate on S/V is observed. This phenomenon is illustrated in Fig. 11-10, which is a plot of the apparent activation energy versus S/V . The data from the thin disks, $S/V \approx 1.38 \text{ mm}^{-1}$, produced an apparent activation energy of 240 kJ/mol, which is consistent with that expected if the reaction was predominantly chemical in nature. Data from the larger specimens showed apparent activation energies about 1/2 of those for the thin specimens, which is consistent with the reaction being controlled by a combination of in-pore diffusion and chemical reaction (Refs. 11-6, 11-7).

The effect of temperature on reaction rate is shown in Fig. 11-11. At low temperatures the apparent activation energy (obtained from the slope) is about 240 kJ/mol, consistent with a chemical reaction. At temperatures above about 1000 K (727°C) the apparent activation energy is approximately 6 kJ/mol, which is consistent with gas phase diffusion control (Refs. 11-6, 11-7). Thus, the high-temperature reactions are interpreted as being controlled by diffusion across the gas boundary layer adjacent to the specimen surfaces. The solid line in Fig. 11-11 is 1000 times the calculated reaction rate of H-451 graphite (Ref. 11-8) and represents an approximate upper bound to the PGX data. When additional data are available on PGX specimens with high S/V (i.e., thin disks), a best line fit based on 95% confidence statistics may be used.

The results of CO_2 oxidation of PGX graphite are shown in Fig. 11-12. The predicted line for H_2O oxidation based on 1000 times the H-451 reaction rate is shown for comparison. The oxidation of PGX by CO_2 is

0.1 or less than the predicted rate by H_2O . Reaction rate data taken from the literature for G-5 graphite (Ref. 11-6) and multiplied by 1000 and for KC graphite (Ref. 11-8) (multiplied by 500) are superimposed on the PGX experimental points. The reaction rate constants for the KC graphite - CO_2 reaction are presently incorporated into the GOP (Graphite Oxidation Program) computer code.

Oxidation Profiles

Density profiles of oxidized cylindrical samples (25.4 mm diameter x 50.8 mm long) were measured using a technique reported previously (Ref. 11-9), except in the measurements reported here a precision centerless grinder replaced the lathe.

Samples were taken from two different logs and oxidized at either 1053 or 1173 K (780° or 900°C) in flowing He containing 400 Pa H_2O and 4000 Pa H_2 . The density profiles are shown as percent burnoff versus depth in Fig. 11-13. These profiles were analyzed using the following relation:

$$R_T = S/V F_1 L R_S \quad , \quad (11-1)$$

where R_T = overall observed reaction rate of the sample, s^{-1} ,
 S/V = geometric surface-to-volume ratio, 0.24 mm^{-1} ,

$$F_1 = \text{efficiency factor, } \frac{I_1(r/L)}{I_0(r_o/L)} \quad ,$$

I_1, I_0 = modified Bessel functions of the first kind (first and zero order, respectively),

r_o = radius of the sample,

r = radial depth of interest,

R_S = chemical reaction rate in absence of mass transport, s^{-1} ,

L = diffusion length, or $L = \sqrt{D_{eff}/K_v}$,

K_v = volumetric reaction rate constant given by

$$K_v = R_S \rho / 12P_{H_2O},$$

D_{eff} = effective diffusion coefficient of water vapor in graphite
 given by $D_{eff} = m D_{1,2}$,

$$D_{1,2} = \text{gas phase diffusion of H}_2\text{O in He calculated by} \\ 1.06 \times 10^{-4} T^{1.58},$$

m = a geometry parameter related to porosity/tortuosity of the graphite.

In Eq. 11-1 the overall average reaction rate (R_T) is obtained by dividing the total fractional weight loss by the total time in seconds. The quantity L is experimentally determined from the individual profiles using the equation

$$\frac{C}{C_0} = e^{-x/L}, \quad (11-2)$$

where C/C_0 = fraction of PH_2O at depth x . It is assumed that burnoff is directly related to water concentration or $C/C_0 = B/B_0$, which is the burnoff at depth x divided by the burnoff at the surface; therefore, L is the distance x where $B/B_0 = e^{-1}$. Using Eq. 11-2, and measured L values for each test, the solid lines in Fig. 11-13 were calculated.

Table 11-8 lists the results of the profile tests to date. The last column in Table 11-8 lists the calculated values of $\bar{m}$, the average diffusion geometry parameter across the entire oxidized zone or profile. In Fig. 11-14, $\bar{m}$ is plotted versus percent burnoff for all tests. The parameter $\bar{m}$ varies with burnoff or as the porosity increases with oxidation.

Another important result of these tests is that the chemical reaction rates at the surface of the specimens, R_S values, are substantially lower than R_T values obtained from thin disks (see Fig. 11-15). It has been assumed the reaction rates of thin specimens (high S/V) are representative of the chemical reaction rate, R_S . The difference between the two rates may be due to the difference in the exposed number of active sites or catalytic sites per unit volume for thin versus thick specimens. Another possible explanation is that the transport of oxidation products CO_2 , CO , and H_2 away from the active sites is more rapid in thin specimens than in thick specimens. These hypotheses will be tested in future experiments.

Effect of Oxidation on Mechanical Properties

Cores were taken from PGX log 6484-138 in accordance with Figs. 11-4 and 11-16 and Table 11-9. Two-hundred axial specimens and 120 radial specimens were machined from the cores. Specimens 6.4 mm in diameter by 31 mm long were oxidized at two different temperatures. After the weight loss was measured on each specimen, the specimens were trimmed to 23 mm in length to eliminate the oxidized ends. The specimens were reweighed and remeasured and tested to failure in tension.

The specimens were oxidized at 1073 K (800°C) and at 1273 K (1000°C) in flowing helium with 5% H₂ and 3% H₂O added. At 1273 K (1000°C), oxidation of the graphite was concentrated at the surface of the midlength center specimens. The surface appeared to have "wormholes" that only penetrated a few millimeters below the surface. Consequently, the percentage weight loss due to oxidation burnoff calculated from trimmed specimens was lower than the burnoff calculated on the untrimmed oxidized specimens. Rate data are reported for the untrimmed specimens, but mechanical properties were measured on the trimmed specimens.

The ultimate tensile strength (UTS), elastic modulus (E), and strain at failure (ϵ_f) are reported for each specimen in Tables 11-10 and 11-11. The UTS versus burnoff data are plotted in Figs. 11-17 and 11-18. In specimens oxidized at 1273 K (1000°C), the rate of strength loss with burnoff was lower at burnoff values between 10% and 25% than at values between 0 and 10%. This is reflected in the hand-fit curves of Fig. 11-19.

Strength (UTS) and modulus (E) data over the burnoff range 0 to 10% were fit to a first approximation by linear regression analysis. The resultant linear relationships are presented in Figs 11-20a and b. The slopes of the linearly fit curves from Figs. 11-20a and b are tabulated in Table 11-12 along with the mean oxidation rate for each group of specimens.

The results from Table 11-12 show a significant difference in the oxidation rates of the two sets of specimens (MLC and MLE). The MLE specimens oxidized slower than the MLC specimens at both temperatures. The slower oxidation rates of the MLE specimens were accompanied by a higher rate of strength loss; thus, the MLE specimens had lower strength values in comparison with the MLC specimens at equal total burnoff percents. Examination of the data on the MLC and MLE specimens after oxidation at 1073 K (800°C), which should produce flatter oxidation profiles (more in-depth oxidation) than those oxidized at 1273 K (1000°C), showed the slowest oxidation rates and also the largest difference in mechanical properties between the MLC and MLE specimens.

Explanations for the large differences in oxidation rates and mechanical property changes of the MLC and MLE specimens will be sought. Variations in bulk density and impurity content will be examined.

Effect of Nonuniform Oxidation on Mechanical Properties

The plan for determining the effects of nonuniform oxidation profile on the mechanical properties of graphite has been revised to include PGX graphite. Following preliminary tests to establish suitable oxidation conditions, rectangular beams with a steep oxidation profile will be prepared and tested in four-point bending and in tension. Uniformly oxidized tensile specimens will be tested to provide baseline data needed for the analysis of stresses in the nonuniform specimens.

TASK 900: FRACTURE MECHANICS (FORMERLY TASK 400)

A test plan for a long-term, comprehensive program on fracture mechanics measurements on graphites has been drafted. Measurements will be made on grades H-451, PGX, and 2020 graphites. The test plan covers the following four phases of the work:

1. Preliminary testing to select a specimen geometry that gives results consistent with the theories of linear elastic fracture mechanics and is suitable for crack-growth tests in a controlled atmosphere chamber.
2. Determination of K_{IC} as a function of orientation, location in the parent log, temperature, and atmosphere.
3. Measurement of the crack growth rate under cyclic fatigue, da/dN , as a function of stress intensity range at ambient temperature and at 1023 K (750°C).
4. Measurement of subcritical crack growth rate under static fatigue conditions, da/dT , as a function of stress intensity factor at elevated temperatures in moist helium.

TASK 1000: FATIGUE BEHAVIOR (FORMERLY TASK 500)

A topical report covering work completed on this subtask in FY-77 was issued (Ref. 11-10). No further work is funded in FY-78.

**TASK 1100: STRUCTURAL INTEGRITY OF GRAPHITE BLOCKS
(FORMERLY TASK 800)**

Analyses of results from the examination of PB-FTE-2 are in progress. Funding for this task will end on March 31, 1978. Final results will be summarized in the next quarterly report.

TASK 1200: RDT GRAPHITE STANDARDS (FORMERLY TASK 600)

No work funded in FY-78.

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TABLE 11-1
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-49		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AC	2A	AX	EC	22.9	20.1
	4A	AX	EC	27.2	23.2
	4B	AX	EC	31.8	26.1
	14A	AX	EC	29.7	24.8
	14B	AX	EC	23.9	20.8
	20A	AX	EC	23.2	20.3
	20B	AX	EC	19.7	17.7
	26A	AX	EC	26.9	23.0
	26B	AX	EC	27.0	23.0
	28A	AX	EC	25.6	22.1
1HC	32A	AX	EC	23.6	20.6
	36A	AX	EC	23.1	20.3
	42A	AX	EC	23.2	20.3
	42B	AX	EC	25.5	22.0
	52A	AX	EC	19.4	17.4
	52B	AX	EC	20.4	18.2
	54A	AX	EC	27.9	23.6
	54B	AX	EC	21.7	19.2
	58A	AX	EC	11.6	10.9
MEAN				23.9 MPA (3468. PSI)	20.7 MPA (3005. PSI)
STD. DEV.				4.5 MPA (647. PSI)	3.3 MPA (483. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-49 SPEC. LENGTH 0. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AC	1 RAD	EC		28.7	23.6
1AC	5 RAD	EC		24.2	20.7
	7 RAD	EC		22.3	19.4
	11 RAD	EC		23.9	20.5
	13 RAD	EC		21.4	18.8
	23 RAD	EC		23.7	20.4
	25 RAD	EC		24.9	21.2
	29 RAD	EC		23.7	20.4
	31 RAD	EC		23.4	20.2
	35 RAD	EC		23.2	20.0
14C	41 RAD	EC		29.6	24.2
	45 RAD	EC		28.9	23.8
	47 RAD	EC		29.2	24.0
	51 RAD	EC		22.8	19.7
	53 RAD	EC		25.4	21.5
	63 RAD	EC		29.6	24.2
	65 RAD	EC		29.2	23.9
	69 RAD	EC		24.4	20.9
	71 RAD	EC		25.4	21.6
	75 RAD	EC		20.4	18.0
MEAN				25.2 MPA (3658. PSI)	21.4 MPA (3098. PSI)
STD. DEV.				2.9 MPA (425. PSI)	1.9 MPA (280. PSI)

TABLE 11-1 (CONT.)
 FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.				SPEC. DIA.	6.4 MM
LOG NO.				SPEC. LENGTH	0. MM
LOG DENSITY				MG/M**3	
SPECIMEN NUMBER	ORIENT- ATION	LUCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AE	64A	AX	EE	28.7	24.5
1AE	64B	AX	EE	27.3	23.5
	66A	AX	FE	29.8	25.3
	66H	AX	EE	31.1	26.1
	70A	AX	EE	27.8	23.9
	70H	AX	FE	28.3	24.3
	76A	AX	EE	27.1	23.4
	76B	AX	EE	29.3	24.9
	78A	AX	FE	28.1	24.1
	78B	AX	EE	32.3	26.8
1AE	84A	AX	EE	27.9	24.0
	84B	AX	EE	34.1	27.9
	86A	AX	EE	28.9	24.7
	86B	AX	EE	31.0	26.0
	90A	AX	EE	25.7	22.4
	90B	AX	EE	23.6	20.8
	96A	AX	EE	20.9	18.8
	96B	AX	EE	26.4	22.9
	98A	AX	EE	25.3	22.1
	98B	AX	EE	27.9	23.9
MEAN				28.1 MPA (4072. PSI)	24.0 MPA (3485. PSI)
STD. DEV.				3.0 MPA (428. PSI)	2.0 MPA (297. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-40		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AE	81 RAD	EE		25.3	21.6
1AE	85 RAD	EE		29.6	24.3
	87 RAD	EE		29.3	24.1
	91 RAD	EE		26.5	22.3
	93 RAD	EE		28.8	23.8
	103 RAD	EE		28.3	23.5
	105 RAD	EE		28.4	23.6
	109 RAD	EE		30.2	24.6
	111 RAD	EE		24.7	21.2
	115 RAD	EE		29.1	24.0
1RE	121 RAD	EE		32.9	26.1
	125 RAD	EE		32.8	26.0
	127 RAD	EE		32.7	26.0
	131 RAD	EE		30.2	24.6
	133 RAD	EE		25.5	21.7
	143 RAD	EE		28.0	23.3
	145 RAD	EE		29.7	24.4
	149 RAD	EE		25.2	21.5
	151 RAD	EE		28.1	23.4
	155 RAD	EE		27.5	23.0
MEAN				28.6 MPA (4155. PSI)	23.7 MPA (3432. PSI)
STD. DEV.				2.5 MPA (356. PSI)	1.5 MPA (215. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-49 SPEC. LENGTH 0. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	102A	AX	MLC	24.4	21.3
	106A	AX	MLC	24.7	21.5
	106H	AX	MLC	19.5	17.6
	112A	AX	MLC	23.8	20.9
	112H	AX	MLC	26.2	22.6
	120A	AX	MLC	21.8	19.4
	120H	AX	MLC	25.1	21.8
	124A	AX	MLC	28.0	23.9
	124H	AX	MLC	21.7	19.3
	128A	AX	MLC	26.9	23.1
3HC	132A	AX	MLC	22.6	20.0
	136A	AX	MLC	25.2	21.9
	136H	AX	MLC	26.7	22.9
	142A	AX	MLC	29.2	24.6
	142H	AX	MLC	23.6	20.8
	152A	AX	MLC	23.0	20.3
	152H	AX	MLC	32.6	26.7
	154A	AX	MLC	25.8	22.3
	154H	AX	MLC	29.9	25.1
	158A	AX	MLC	26.5	22.8
MEAN				25.4 MPA (3678. PSI)	22.0 MPA (3184. PSI)
STD. DEV.				3.1 MPA (447. PSI)	2.2 MPA (314. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-49	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	161 RAD	MLC		26.7	22.3
	165 RAD	MLC		19.0	16.9
	167 RAD	MLC		23.3	20.0
	171 RAD	MLC		21.9	19.1
	173 RAD	MLC		21.2	18.5
	183 RAD	MLC		17.7	15.9
	185 RAD	MLC		22.2	19.2
	189 RAD	MLC		22.8	19.7
	191 RAD	MLC		22.5	19.5
	195 RAD	MLC		21.4	18.7
3FC	201 RAD	MLC		20.6	18.1
	205 RAD	MLC		20.0	17.6
	207 RAD	MLC		20.5	18.0
	211 RAD	MLC		25.6	21.5
	213 RAD	MLC		21.9	19.0
	223 RAD	MLC		25.4	21.4
	225 RAD	MLC		19.2	17.0
	229 RAD	MLC		27.2	22.6
	231 RAD	MLC		22.2	19.2
	235 RAD	MLC		21.9	19.1
MEAN				22.2 MPA (3216. PSI)	19.2 MPA (2781. PSI)
STD. DEV.				2.5 MPA (367. PSI)	1.8 MPA (256. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-40		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LUCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	164A	AX	MLE	33.6	28.1
	164B	AX	MLE	34.8	28.8
	166A	AX	MLE	31.1	26.4
	166B	AX	MLE	32.4	27.3
	170A	AX	MLE	33.9	28.3
	170B	AX	MLE	29.2	25.1
	176A	AX	MLE	32.9	27.6
	176B	AX	MLE	32.0	27.1
	178A	AX	MLE	30.3	25.9
	178B	AX	MLE	27.2	23.8
3AE	184A	AX	MLE	35.7	29.4
	184B	AX	MLE	30.4	25.9
	186A	AX	MLE	31.5	26.7
	186B	AX	MLE	35.0	29.0
	190A	AX	MLE	30.1	25.8
	190B	AX	MLE	37.5	30.4
	192A	AX	MLE	35.2	29.1
	192B	AX	MLE	33.8	28.2
	198A	AX	MLE	34.1	28.4
	198B	AX	MLE	32.3	27.2
MEAN				32.7 MPA (4737. PSI)	27.4 MPA (3977. PSI)
STD. DEV.				2.5 MPA (362. PSI)	1.6 MPA (235. PSI)

TABLE 11-1 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-49		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	241 RAD	MLE		22.8	19.9
	245 RAD	MLE		30.9	25.2
	247 RAD	MLE		28.1	23.5
	251 RAD	MLE		26.9	22.7
	253 RAD	MLE		26.8	22.6
	263 RAD	MLE		22.3	19.5
	267 RAD	MLE		25.3	21.6
	269 RAD	MLE		21.5	18.9
	273 RAD	MLE		24.4	21.0
	275 RAD	MLE		23.3	20.2
3BE	281 RAD	MLE		28.8	23.9
	285 RAD	MLE		28.0	23.4
	287 RAD	MLE		29.3	24.3
	291 RAD	MLE		25.6	21.8
	293 RAD	MLE		30.2	24.8
	303 RAD	MLE		23.4	20.4
	305 RAD	MLE		28.7	23.9
	309 RAD	MLE		27.1	22.8
	311 RAD	MLE		23.9	20.7
	315 RAD	MLE		28.4	23.7
MEAN				26.3 MPA (3813. PSI)	22.2 MPA (3226. PSI)
STD. DEV.				2.8 MPA (407. PSI)	1.9 MPA (270. PSI)

TABLE 11-2
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-52	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION		LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)

1AC	2A	AX	FC		25.9	22.4
	4A	AX	EC		27.5	23.6
	4B	AX	EC		28.0	23.9
	14A	AX	EC		16.0	14.8
	14B	AX	EC		25.4	22.1
	20A	AX	EC		20.1	18.1
	20B	AX	EC		25.4	22.1
	26A	AX	EC		24.7	21.6
	26B	AX	EC		28.2	24.1
	28A	AX	EC		30.4	25.5
1BC	32A	AX	EC		26.4	22.8
	36A	AX	EC		28.8	24.4
	36B	AX	EC		19.1	17.3
	42A	AX	EC		24.8	21.6
	42B	AX	EC		29.1	24.6
	52A	AX	EC		29.4	24.8
	52B	AX	EC		27.1	23.3
	54A	AX	EC		23.2	20.4
	54B	AX	EC		23.5	20.7
58A	AX	EC		29.6	24.9	

MEAN					25.6 MPA (3718. PSI)	22.2 MPA (3213. PSI)
STD. DEV.					3.8 MPA (548. PSI)	2.8 MPA (404. PSI)

TABLE 11-2 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-52	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)

1AC	1 RAD	EC		30.5	24.9
	5 RAD	EC		30.1	24.7
	7 RAD	EC		28.4	23.7
	11 RAD	FC		25.0	21.4
	13 RAD	EC		23.0	20.0
	23 RAD	EC		26.9	22.7
	25 RAD	FC		27.5	23.1
	29 RAD	EC		26.2	22.3
	31 RAD	EC		21.6	19.0
	35 RAD	EC		21.5	18.9
1HC	41 RAD	EC		30.3	24.8
	45 RAD	EC		30.2	24.8
	47 RAD	EC		30.1	24.7
	51 RAD	EC		19.9	17.7
	53 RAD	EC		22.9	19.9
	63 RAD	EC		25.1	21.5
	65 RAD	EC		27.3	23.0
	69 RAD	EC		22.4	19.6
	71 RAD	EC		27.8	23.3
	75 RAD	EC		30.2	24.8

MEAN				26.3 MPA (3821. PSI)	22.2 MPA (3225. PSI)
STD. DEV.				3.5 MPA (506. PSI)	2.3 MPA (340. PSI)

TABLE 11-2 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	5.4 MM
LOG NO.	7194-52	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
14E	64A	AX	EE	31.2	26.9
	64B	AX	EE	25.7	22.6
	66A	AX	EE	28.3	24.8
	66B	AX	EE	34.5	29.1
	70A	AX	EE	30.2	26.2
	70B	AX	EE	33.3	28.3
	76A	AX	EE	32.3	27.6
	76B	AX	EE	29.8	25.9
	78A	AX	EE	34.3	28.9
	78B	AX	EE	33.5	28.4
18E	84A	AX	EE	33.7	28.6
	84B	AX	EE	36.0	30.1
	86A	AX	EE	33.0	28.1
	86B	AX	EE	31.2	26.9
	90A	AX	EE	31.4	27.0
	90B	AX	EE	30.1	26.1
	96A	AX	EE	27.1	23.9
	96B	AX	EE	32.6	27.8
	98A	AX	EE	31.6	27.2
	98B	AX	EE	34.2	28.9
MEAN				31.7 MPA (4596. PSI)	27.2 MPA (3942. PSI)
STD. DEV.				2.6 MPA (380. PSI)	1.8 MPA (266. PSI)

TABLE 11-2 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-52	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)

1AE	81	RAD	EE	32.0	26.4
	85	RAD	EE	28.5	24.2
	87	RAD	EE	32.1	26.5
	91	RAD	EE	29.5	24.8
	93	RAD	EE	28.9	24.4
	103	RAD	EE	26.5	22.8
	105	RAD	EE	30.8	25.6
	109	RAD	EE	30.4	25.4
	111	RAD	EE	31.2	25.9
	115	RAD	EE	29.8	25.0
1BE	121	RAD	EE	34.7	28.0
	125	RAD	EE	32.6	26.8
	127	RAD	EE	23.1	20.4
	131	RAD	EE	28.9	24.4
	133	RAD	EE	26.0	22.5
	143	RAD	EE	28.8	24.4
	145	RAD	EE	32.2	26.5
	149	RAD	EE	32.1	26.5
	151	RAD	EE	30.1	25.2
	155	RAD	EE	30.8	25.7

MEAN				29.9 MPA (4343. PSI)	25.1 MPA (3636. PSI)
STD. DEV.				2.6 MPA (381. PSI)	1.7 MPA (250. PSI)

TABLE 11-2 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-52		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LUCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	102A	AX	MLC	26.3	23.1
	106A	AX	MLC	28.7	24.8
	106B	AX	MLC	31.9	26.9
	112A	AX	MLC	21.7	19.5
	112B	AX	MLC	24.7	21.8
	120A	AX	MLC	27.1	23.7
	120B	AX	MLC	28.0	24.3
	124A	AX	MLC	29.7	25.5
	124B	AX	MLC	30.0	25.7
	128A	AX	MLC	28.1	24.4
3BC	132A	AX	MLC	27.6	24.1
	136A	AX	MLC	32.4	27.3
	136B	AX	MLC	27.2	23.7
	142A	AX	MLC	28.4	24.6
	142B	AX	MLC	31.1	26.4
	152A	AX	MLC	28.2	24.5
	152B	AX	MLC	31.0	26.4
	154A	AX	MLC	27.8	24.1
	154B	AX	MLC	26.2	23.0
	158A	AX	MLC	29.6	25.4
MEAN				28.3 MPA (4102. PSI)	24.5 MPA (3548. PSI)
STD. DEV.				2.5 MPA (365. PSI)	1.8 MPA (262. PSI)

TABLE 11-2 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-52	SPEC. LENGTH	0. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	161 RAD	MLC		20.1	17.8
	165 RAD	MLC		23.3	20.1
	167 RAD	MLC		22.5	19.6
	171 RAD	MLC		21.5	18.8
	173 RAD	MLC		22.5	19.5
	183 RAD	MLC		17.4	15.6
	185 RAD	MLC		11.9	11.2
	189 RAD	MLC		21.2	18.6
	191 RAD	MLC		9.0	8.5
	195 RAD	MLC		20.1	17.7
3BC	201 RAD	MLC		16.8	15.2
	205 RAD	MLC		21.9	19.1
	207 RAD	MLC		20.7	18.2
	211 RAD	MLC		20.2	17.8
	213 RAD	MLC		20.1	17.8
	223 RAD	MLC		22.8	19.8
	225 RAD	MLC		22.5	19.6
	229 RAD	MLC		24.5	20.9
	231 RAD	MLC		26.8	22.4
	235 RAD	MLC		28.4	23.4
MEAN				20.7 MPA (3003. PSI)	18.1 MPA (2621. PSI)
STD. DEV.				4.4 MPA (645. PSI)	3.4 MPA (498. PSI)

TABLE 11-2 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-52 SPEC. LENGTH 0. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LUCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	164A	AX	MLE	37.3	30.8
	164B	AX	MLE	29.5	25.7
	166A	AX	MLE	31.7	27.2
	166B	AX	MLE	30.4	26.3
	170A	AX	MLE	31.9	27.3
	170B	AX	MLE	30.2	26.1
	176A	AX	MLE	32.2	27.6
	176B	AX	MLE	24.4	21.8
	178A	AX	MLE	32.6	27.8
	178B	AX	MLE	31.0	26.7
3HE	184A	AX	MLE	34.8	29.2
	184B	AX	MLE	35.1	29.4
	186A	AX	MLE	32.4	27.7
	186B	AX	MLE	34.2	28.8
	190A	AX	MLE	37.0	30.7
	190B	AX	MLE	33.6	28.5
	192A	AX	MLE	33.6	28.5
	192B	AX	MLE	34.0	28.7
	198A	AX	MLE	32.6	27.8
	198B	AX	MLE	32.5	27.8
MEAN				32.6 MPA (4721. PSI)	27.7 MPA (4021. PSI)
STD. DEV.				2.8 MPA (408. PSI)	1.9 MPA (282. PSI)

TABLE 11-2 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.		6.4 MM	
LOG NO.		7194-52		SPEC. LENGTH	
LOG DENSITY		MG/M**3		0. MM	
SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	241	RAD	MLE	29.0	24.5
	245	RAD	MLE	26.5	22.8
	247	RAD	MLE	25.1	21.8
	251	RAD	MLE	27.2	23.3
	253	RAD	MLE	28.2	24.0
	263	RAD	MLE	29.9	25.1
	267	RAD	MLE	28.1	23.9
	269	RAD	MLE	28.7	24.3
	273	RAD	MLE	29.9	25.1
	275	RAD	MLE	29.4	24.8
3HE	281	RAD	MLE	27.6	23.6
	285	RAD	MLE	30.3	25.3
	287	RAD	MLE	29.3	24.7
	291	RAD	MLE	31.1	25.9
	293	RAD	MLE	29.9	25.1
	303	RAD	MLE	29.4	24.8
	305	RAD	MLE	29.4	24.8
	309	RAD	MLE	31.0	25.8
	311	RAD	MLE	25.5	22.1
	315	RAD	MLE	25.8	22.3
MEAN				28.6 MPA (4142. PSI)	24.2 MPA (3509. PSI)
STD. DEV.				1.8 MPA (258. PSI)	1.2 MPA (172. PSI)

TABLE 11-3
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-A7 SPEC. LENGTH 51. MM
LOG DENSITY 14G/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AC	2A	AX	EC	27.4	23.6
	4A	AX	EC	32.5	27.0
	4B	AX	EC	33.2	27.4
	14A	AX	EC	31.9	26.6
	14B	AX	EC	28.0	24.0
	20A	AX	EC	28.1	24.1
	20B	AX	EC	26.4	22.9
	26A	AX	EC	26.6	23.1
	26B	AX	EC	25.2	22.0
	28A	AX	EC	27.9	24.0
1BC	32A	AX	EC	27.0	23.4
	36A	AX	EC	27.7	23.9
	36B	AX	EC	18.0	16.4
	42A	AX	EC	32.0	26.7
	42B	AX	EC	28.3	24.3
	52A	AX	EC	29.3	24.9
	52B	AX	EC	28.4	24.3
	54A	AX	EC	30.2	25.5
	54B	AX	EC	32.4	26.9
	58A	AX	EC	27.1	23.4
MEAN				28.4 MPA (4117. PSI)	24.2 MPA (3514. PSI)
STD. DEV.				3.4 MPA (492. PSI)	2.4 MPA (349. PSI)

TABLE 11-3 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-87 SPEC. LENGTH 51. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AC	1	PAD	EC	31.5	25.3
	5	PAD	EC	27.6	23.0
	7	PAD	EC	28.8	23.8
	11	PAD	EC	26.1	22.0
	13	PAD	EC	23.0	20.0
	23	PAD	EC	29.9	24.4
	25	PAD	EC	37.0	27.8
	29	PAD	EC	32.8	26.0
	31	PAD	EC	31.5	25.3
	35	PAD	EC	30.1	24.5
	41	PAD	EC	30.8	24.9
	45	PAD	EC	31.9	25.5
	47	PAD	EC	30.6	24.8
	51	PAD	EC	27.6	23.0
1BC	53	PAD	EC	27.0	22.6
	63	PAD	EC	31.8	25.5
	65	PAD	EC	31.1	25.1
	69	PAD	EC	30.4	24.7
	71	PAD	EC	27.4	22.9
	75	PAD	EC	25.5	21.6
MEAN				29.6 MPA (4296. PSI)	24.1 MPA (3501. PSI)
STD. DEV.				3.1 MPA (448. PSI)	1.8 MPA (260. PSI)

TABLE 11-3 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-87 SPEC. LENGTH 51. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AE	64A	AX	EE	35.2	29.1
	64B	AX	EE	32.0	27.1
	66A	AX	EE	33.2	27.8
	66B	AX	EE	31.5	26.7
	70A	AX	EE	35.6	29.3
	70B	AX	EE	32.9	27.6
	76A	AX	EE	33.9	28.2
	76B	AX	EE	33.4	27.9
	78A	AX	EE	33.7	28.1
	78B	AX	EE	29.7	25.5
1BE	84A	AX	EE	35.1	29.0
	84B	AX	EE	33.9	28.2
	86A	AX	EE	26.9	23.5
	86B	AX	EE	33.7	28.1
	90A	AX	EE	30.1	25.8
	90B	AX	EE	31.5	26.7
	96A	AX	EE	34.8	28.9
	96B	AX	EE	34.4	28.6
	98A	AX	EE	29.0	25.0
	98B	AX	EE	29.2	25.1
MEAN				32.5 MPA (4711. PSI)	27.3 MPA (3961. PSI)
STD. DEV.				2.4 MPA (351. PSI)	1.6 MPA (232. PSI)

TABLE 11-3 (CONT.)
FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO. SPEC. DIA. 6.4 MM
LOG NO. 7194-87 SPEC. LENGTH 51. MM
LOG DENSITY MG/M**3

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
1AE	81 RAD	EE		33.5	27.0
	85 RAD	EE		31.4	25.8
	87 RAD	EE		29.5	24.6
	91 RAD	EE		29.7	24.8
	93 RAD	EE		32.3	26.4
	103 RAD	EE		29.0	24.3
	105 RAD	EE		30.2	25.1
	109 RAD	EE		34.1	27.4
	111 RAD	EE		34.8	27.7
	115 RAD	EE		32.6	26.5
1BE	121 RAD	EE		29.7	24.8
	125 RAD	EE		31.7	26.0
	127 RAD	EE		34.0	27.3
	131 RAD	EE		30.6	25.4
	133 RAD	EE		33.6	27.1
	143 RAD	EE		35.7	28.2
	145 RAD	EE		31.6	26.0
	149 RAD	EE		30.7	25.4
	151 RAD	EE		32.9	26.7
	155 RAD	EE		30.1	25.1
MEAN				31.9 MPA (4626. PSI)	26.1 MPA (3782. PSI)
STD. DEV.				2.0 MPA (284. PSI)	1.1 MPA (165. PSI)

TABLE 11-3 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-87	SPEC. LENGTH	51. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LCCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	102A	AX	MLC	29.8	25.3
	106A	AX	MLC	31.1	26.1
	106B	AX	MLC	27.7	23.9
	112A	AX	MLC	27.8	23.9
	112B	AX	MLC	31.8	26.6
	120A	AX	MLC	27.0	23.4
	120B	AX	MLC	27.4	23.7
	124A	AX	MLC	30.9	26.0
	124B	AX	MLC	32.9	27.3
	128A	AX	MLC	32.4	27.0
3BC	132A	AX	MLC	23.7	21.0
	136A	AX	MLC	22.6	20.2
	136B	AX	MLC	28.4	24.4
	142A	AX	MLC	25.7	22.5
	142B	AX	MLC	27.2	23.5
	152A	AX	MLC	27.8	23.9
	152B	AX	MLC	29.8	25.3
	154A	AX	MLC	26.6	23.1
	154B	AX	MLC	23.1	20.5
	158A	AX	MLC	25.8	22.5
MEAN				28.0 MPA (4058. PSI)	24.0 MPA (3480. PSI)
STD. DEV.				3.0 MPA (431. PSI)	2.1 MPA (299. PSI)

TABLE 11-3 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-87	SPEC. LENGTH	51. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AC	161 RAD	MLC		23.4	19.9
	165 RAD	MLC		21.9	18.9
	167 RAD	MLC		20.5	17.9
	171 RAD	MLC		18.7	16.6
	173 RAD	MLC		20.8	18.1
	183 RAD	MLC		25.5	21.3
	185 RAD	MLC		23.8	20.2
	189 RAD	MLC		26.3	21.8
	191 RAD	MLC		23.1	19.7
	195 RAD	MLC		22.3	19.2
3BC	201 RAD	MLC		17.4	15.6
	205 RAD	MLC		20.7	18.1
	207 RAD	MLC		18.6	16.5
	211 RAD	MLC		22.9	19.6
	213 RAD	MLC		23.8	20.2
	223 RAD	MLC		17.5	15.6
	225 RAD	MLC		24.3	20.5
	229 RAD	MLC		25.3	21.2
	231 RAD	MLC		24.0	20.3
	235 RAD	MLC		19.9	17.5
MEAN				22.0 MPA (3195. PSI)	18.9 MPA (2745. PSI)
STD. DEV.				2.7 MPA (387. PSI)	1.9 MPA (272. PSI)

TABLE 11-3 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-87	SPEC. LENGTH	51. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	164A	AX	MLE	31.4	26.7
	164B	AX	MLE	35.1	29.1
	166A	AX	MLE	33.5	28.1
	166B	AX	MLE	35.1	29.1
	170A	AX	MLE	37.9	30.7
	170B	AX	MLE	34.8	28.9
	176A	AX	MLE	32.7	27.6
	176B	AX	MLE	32.4	27.3
	178A	AX	MLE	37.8	30.7
	178B	AX	MLE	35.4	29.3
3BE	184A	AX	MLE	30.8	26.4
	184B	AX	MLE	34.7	28.9
	186A	AX	MLE	30.6	26.2
	186B	AX	MLE	35.6	29.4
	190A	AX	MLE	33.4	28.0
	190B	AX	MLE	34.7	28.9
	192A	AX	MLE	32.8	27.6
	192B	AX	MLE	36.0	29.7
	198A	AX	MLE	34.8	28.9
	198B	AX	MLE	31.2	26.6
MEAN				34.0 MPA (4938. PSI)	28.4 MPA (4121. PSI)
STD. DEV.				2.1 MPA (309. PSI)	1.3 MPA (195. PSI)

TABLE 11-3 (CONT.)

FLEXURAL PROPERTIES OF H-451 GRAPHITE

LOT NO.		SPEC. DIA.	6.4 MM
LOG NO.	7194-87	SPEC. LENGTH	51. MM
LOG DENSITY	MG/M**3		

SPECIMEN NUMBER	ORIENT- ATION	LOCA- TION	DENSITY (MG/M**3)	MODULUS OF RUPTURE (MPA) (UNCORRECTED)	FLEXURAL STRENGTH (MPA) (CORRECTED)
3AE	241 RAD	MLE		28.7	24.0
	245 RAD	MLE		30.5	25.1
	247 RAD	MLE		30.8	25.2
	251 RAD	MLE		29.6	24.5
	253 RAD	MLE		30.1	24.8
	263 RAD	MLE		28.4	23.8
	267 RAD	MLE		30.1	24.6
	269 RAD	MLE		26.1	22.3
	273 RAD	MLE		25.4	21.8
	275 RAD	MLE		30.1	24.8
3EE	281 RAD	MLE		26.1	22.2
	285 RAD	MLE		25.7	22.0
	287 RAD	MLE		28.4	23.7
	291 RAD	MLE		31.0	25.4
	293 RAD	MLE		26.2	22.3
	303 RAD	MLE		30.6	25.1
	305 RAD	MLE		31.5	25.6
	309 RAD	MLE		28.8	24.0
	311 RAD	MLE		30.2	24.9
	315 RAD	MLE		28.4	23.8
MEAN				28.8 MPA (4183. PSI)	24.0 MPA (3482. PSI)
STD. DEV.				2.0 MPA (285. PSI)	1.2 MPA (181. PSI)

TABLE 11-4
2020 GRAPHITE ROUND-ROBIN TEST SPECIMENS

Laboratory	Test	Specimen Geometry	Sample Numbers
General Atomic	Flexural	Cylinder 6.4 mm diam x 51. mm long	2,4,6,8,10,12,14,16,18, 20,22,24,26,28,30,32,34, 36,38,40,42,44,46,48
Stackpole Carbon	Flexural	Bar 4.7 mm x 9.5 mm x 24.8 mm long	52,56,58,62,72,74,78,84, 88,90,94,102,104,106,108, 110,112,114,116,118,120, 122,124,126,128,130,132, 140,142,144,146,148
Southern Research Institute	Tensile	Dogbone 5.6 mm diam x 25.4 mm gage length	200,202,204,206,208,210, 212,214,216,218,220,222
General Atomic	Tensile	Cylinder 12.8 mm diam x 70 mm long	50,54,60,64,66,70,76,80, 82,86,92,96
	Tensile	ASTM 6.4 mm diam x dogbone 44.5 mm gage length	228,230,234,236,238,242, 246,248,252,254,258,260, 264,266,270,272

TABLE 11-5
DATA ON 2020 GRAPHITE ROUND-ROBIN TESTS

Laboratory	Test	Mean Strength		Standard Deviation		Number of Specimens Tested
		(MPa)	(psi)	(MPa)	(psi)	
Southern Research Institute	Tensile: Dogbone	22.5	3260	1.1	157	12
General Atomic	Tensile: Cylinder Cylinder	11.6	1678	0.8	122	12
		14.0	2025	1.8	263	12
	Tensile: Dogbone Extensometer	22.2	3220	0.7	96	8
		20.8	3010	1.2	173	8
General Atomic	Flexure: MOR Corrected flexural strength	32.5	4710	2.3	335	12
		26.6	3860	1.4	198	
Stackpole Carbon	Flexure	29.4	4262	2.3	335	264

TABLE 11-6
KINETIC RATE CONSTANT SAMPLING PLAN FOR SUPPORT FLOOR GRAPHITE
PGX, LOGS 6484-112 AND -138, SLAB 6

GA Log No.	Specimen Size	Specimen Location	Orientation	Frequency	Specimen Numbers
6484-138	32 mm diam x 1.6 mm long	6AX	Radial	5	1A-E, 3A-E, 5A-E, 7A-E
		6BX	Radial	5	9A-E, 11A-E, 13A-E, 15A-E
		6AE	Radial	5	17A-E, 19A-E, 21A-E, 23A-E
		6BE	Radial	5	25A-E, 27A-E, 29A-E, 31A-E
6484-112	32 mm diam x 1.6 mm long	6AX	Axial	20	04, 06, 08, 016, 018, 020, 028, 026
		6BX	Axial	20	0124, 0126, 0128, 0136, 0138, 0140, 0146, 0148
		6AE	Axial	20	072, 074, 076, 078, 0102, 0104, 0106, 0108
		6BE	Axial	20	0192, 0194, 0196, 0198, 0226, 0228, 0230, 0232

TABLE 11-7
MASS TRANSPORT SAMPLING PLAN FOR SUPPORT FLOOR GRAPHITE
PGX, LOGS 6484-112 AND -138, SLAB 6

GA Log No.	Specimen Size	Specimen Location	Orientation	Frequency	Specimen Numbers
6484-138	32 mm diam x 70 mm long	6AX	Axial	4	2A,2B,4A,4B
		6BX	Axial	4	6A,6B,8A,8B
		6AE	Axial	4	26A,26B,28A,28B
		6BE	Axial	4	30A,30B,32A,32B
	25.4 mm diam x 70 mm long	6AX	Axial	2	12A,12B
		6BX	Axial	2	10A,10B
		6AE	Axial	2	34A,34B
		6BE	Axial	2	36A,36B
	19 mm diam x 70 mm long	6AX	Axial	2	16A,16B
		6BX	Axial	2	14A,14B
		6AE	Axial	2	38A,38B
		6BE	Axial	2	40A,40B
	12.7 mm diam x 70 mm long	6AX	Axial	2	20A,20B
		6BX	Axial	2	18A,18B
		6AE	Axial	2	42A,42B
		6BE	Axial	2	44A,44B
	6.4 mm diam x 70 mm long	6AX	Axial	2	24A,24B
		6BX	Axial	2	22A,22B
		6AE	Axial	2	46A,46B
		6BE	Axial	2	48A,48B
6484-112	32 mm diam x 70 mm long	6AX	Axial	4	02A,02B,024A,024B
		6BX	Axial	4	0122A,0122B,0144A,0144B
		6AE	Axial	4	062A,062B,068A,068B
		6BE	Axial	4	0182A,0182B,0188A,0188B

TABLE 11-7 (Continued)

GA Log No.	Specimen Size	Specimen Location	Orientation	Frequency	Specimen Numbers
6484-112	25.4 mm diam x 70 mm long	6AX	Axial	2	014A,014B
		6BX	Axial	2	0134A,0134B
		6AE	Axial	2	064A,064B
		6BE	Axial	2	0184A,0184B
	19 mm diam x 70 mm long	6AX	Axial	2	032A,032B
		6BX	Axial	2	0152A,0152B
		6AE	Axial	2	066A,066B
		6BE	Axial	2	0186A,0186B
	12.7 mm diam x 70 mm long	6AX	Axial	2	038A,038B
		6BX	Axial	2	0158A,0158B
		6AE	Axial	2	070A,070B
		6BE	Axial	2	0190A,0190B
	6.4 mm diam x 70 mm long	6AX	Axial	4	178A,178B,180A,180B
		6BX	Axial	4	410A,410B,412A,412B
		6AE	Axial	4	182A,182B,186A,186B
		6BE	Axial	4	430A,430B,432A,432B
	25.4 mm diam x 76 mm long	6AY	Axial	20	02A,02B,04A,04B,06A,010A, 010B,012A,014A,014B,018A, 018B
	25.4 mm diam x 76 mm long	6AY	Axial	20	022A,022B,024A,024B,026A, 030A,030B,032A,034A,034B, 038A,038B

TABLE 11-8
OXIDATION PROFILES OF PGX GRAPHITE CYLINDRICAL SPECIMENS ^(a)

Specimen No.	GA Log No.	Orig. Density/ Final Density (Mg/m ³)	Fe Content (ppm)	Burnoff (%)	Time (h)	R _T (s ⁻¹ Pa ⁻¹)	R _S (s ⁻¹ Pa ⁻¹)	L (mm)	$\bar{m}$
Oxidation Temperature = 1173 K (900°C)									
6A	6484-112	1.785/1.654	High ^(b)	7.34	105	9.7x10 ⁻¹⁰	6.7x10 ⁻⁹	0.6	4.5x10 ⁻³
4B	-112	1.781/1.708	High ^(b)	4.1	142	2x10 ⁻¹⁰	1.7x10 ⁻⁹	0.5	7.8x10 ⁻⁴
12A	-112	1.788/1.613	High ^(b)	9.79	142	4.8x10 ⁻¹⁰	2x10 ⁻⁹	1.0	3.8x10 ⁻³
30B	-138	1.741/1.569	Low ^(c)	9.88	64.5	1.1x10 ⁻⁹	3.6x10 ⁻⁹	1.25	1x10 ⁻²
24A	-138	1.740/1.547	Low ^(c)	11.1	114	6.7x10 ⁻¹⁰	1.9x10 ⁻⁹	1.5	7.8x10 ⁻³
Oxidation Temperature = 1053 K (780°C)									
14B	6484-112	1.785/1.645	High ^(b)	7.84	886	6.1x10 ⁻¹¹	1.7x10 ⁻¹⁰	1.6	8.5x10 ⁻⁴
4A	-112	1.783/1.685	High ^(b)	5.5	886	4.4x10 ⁻¹¹	1.5x10 ⁻¹⁰	1.2	4.4x10 ⁻⁴
30A	-138	1.744/1.420	Low ^(c)	18.2	886	1.4x10 ⁻¹⁰	1.6x10 ⁻¹⁰	12.0	4.7x10 ⁻²
24B	-138	1.741/1.585	Low ^(c)	8.96	550	1.1x10 ⁻¹⁰	1.3x10 ⁻¹⁰	4.6	3x10 ⁻³

(a) Specimen dimensions = 25.4 mm diam x 50.8 mm long.

(b) Specimens from log 112 "high" Fe = 2926 to 3203 ppm.

(c) Specimens from log 138 "low" Fe = 1245 to 1283 ppm.

TABLE 11-9
EFFECTS OF OXIDATION ON MECHANICAL PROPERTIES OF PGX GRAPHITE CORE SUPPORT FLOOR BLOCKS

Specimen Type		Specimen Location		Orientation	Sampling Frequency	Specimen Numbers
Temp. [K(°C)]	Burnoff (%)	Slab	Section			
Control		6	AC-AX	Axial	20	2A-D, 12A-D, 30A-D, 46A-D, 70A-D
1073 (800)	1	6	AC-AX	Axial	20	14A-D, 18A-D, 38A-D, 44A-D, 56A-D
1073 (800)	2	6	AC-AX	Axial	20	4A-D, 10A-D, 34A-D, 42A-D, 52A-D
1073 (800)	4	6	AC-AX	Axial	20	8A-D, 16A-D, 40A-D, 48A-D, 66A-D
1273 (1000)	1	6	AC-AX	Axial	20	22A-D, 28A-D, 36A-D, 50A-D, 60A-D
1273 (1000)	2	6	AC-AX	Axial	20	20A-D, 26A-D, 64A-D, 54A-D, 58A-D
1273 (1000)	4	6	AC-AX	Axial	20	6A-D, 24A-D, 32A-D, 62A-D, 68A-D
Control		6	BC-BX	Radial	20	1A-D, 11A-D, 15A-D, 19A-D, 29A-D
1073 (800)	4	6	BC-BX	Radial	20	7A-D, 9A-D, 17A-D, 21A-D, 25A-D
1273 (1000)	4	6	BC-BX	Radial	20	3A-D, 5A-D, 13A-D, 23A-D, 27A-D
Control		6	AE-AY	Axial	20	72A-D, 82A-D, 86A-D, 90A-D, 100A-D
1073 (800)	4	6	AE-AY	Axial	20	78A-D, 80A-D, 88A-D, 92A-D, 96A-D
1273 (1000)	4	6	AE-AY	Axial	20	74A-D, 76A-D, 84A-D, 94A-D, 98A-D
Control		6	BE-BY	Radial	20	31A-D, 41A-D, 45A-D, 49A-D, 59A-D
1073 (800)	4	6	BE-BY	Radial	20	37A-D, 39A-D, 47A-D, 51A-D, 55A-D
1273 (1000)	4	6	BE-BY	Radial	20	33A-D, 35A-D, 43A-D, 53A-D, 57A-D

TABLE 11-10
OXIDATION VERSUS STRENGTH OF PGX GRAPHITE
[Log 6484-138, Slab 6, Oxidation Temperature = 1073 K (800°C)]

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC						
2A			0	9.6		
2B			0	10.0		
2C			0	9.3		
2D			0	9.8		
4A	1.742	1.664	4.5	7.2	4.2	0.210
4B	1.744	1.662	4.7	8.1	4.0	0.274
4C	1.741	1.686	3.16	8.7	3.9	0.295
4D	1.745	1.665	4.6	8.0		
8A	1.726	1.600	7.3	6.9		
8B	1.727	1.597	7.5	7.4	4.0	0.251
8C	1.732	1.616	6.7	7.6	4.4	0.203
8D	1.730	1.616	6.6	8.0	4.5	0.250
10A	1.750	1.682	3.9	8.1	5.5	0.165
10B	1.748	1.670	4.5	9.1	4.7	0.250
10C	1.749	1.669	4.6	8.0	4.0	0.238
10D	1.754	1.684	4.0	8.4	5.4	0.200
12A			0	9.1	5.7	0.224
12B			0	9.1	5.5	0.219
12C			0	9.3	5.2	0.248
12D			0	9.7	5.4	0.250
14A	1.742	1.721	1.2	10.7	4.8	0.320
14B	1.726	1.696	1.7	8.1	4.7	0.203
14C	1.733	1.695	2.2	9.4	4.8	0.249
14D	1.729	1.698	1.8	7.7	5.1	0.145
16A	1.733	1.610	7.1	6.7		
16B	1.727	1.614	6.5	7.6	4.2	0.200
16C	1.728	1.615	6.5	7.6	3.9	0.260
16D	1.726	1.602	7.2	7.6	4.1	0.277

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC (Continued)						
18A	1.727	1.689	2.2	8.0	4.8	0.224
18B	1.736	1.697	2.3	9.1	4.5	0.305
18C	1.725	1.703	1.3	8.6	5.3	0.222
18D	1.726	1.706	1.2	8.2	4.5	0.242
30A			0	10.3		
30B			0	9.4		
30C			0	9.0	5.1	0.225
30D			0	9.1	5.5	0.213
34A	1.742	1.665	4.4	8.0	4.5	0.241
34B	1.759	1.694	3.7	8.1	4.5	0.221
34C	1.748	1.668	4.6	8.3	4.4	0.245
34D	1.739	1.673	3.8	9.1	4.1	0.290
38A	1.725	1.699	1.5	8.9	4.5	0.290
38B	1.730	1.693	2.1	7.9	4.6	0.219
38C	1.726	1.695	1.8	8.7	4.5	0.249
38D	1.722	1.710	0.7	8.3	4.5	0.259
40A	1.724	1.607	6.8	8.1	4.3	0.214
40B	1.735	1.623	6.5	7.3	4.5	0.219
40C	1.728	1.602	7.3	7.0	3.7	0.239
40D	1.727	1.612	6.7	8.4	4.5	0.249
42A	1.739	1.658	4.7	7.4	4.2	0.231
42B	1.753	1.675	4.5	8.4	3.8	0.310
42C	1.742	1.666	4.4	7.3	3.2	
42D	1.743	1.680	3.6	8.0	5.2	0.215
44A	1.725	1.709	0.9	9.8	5.5	0.282
44B	1.728	1.701	1.6	9.1	4.6	0.259
44C	1.729	1.695	2.0	9.6	4.8	0.264
44D	1.727	1.708	1.1	8.7	3.5	
46A			0	10.0		
46B			0	9.4		

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC (Continued)						
46C			0	10.1	5.2	0.272
46D			0	10.7	4.9	0.309
48A	1.737	1.635	5.9	6.7		
48B	1.730	1.593	7.9	6.6	4.3	0.198
48C						
48D	1.730	1.601	7.5	8.2	3.7	0.348
52A	1.742	1.685	3.3	7.7	4.8	0.200
52B	1.745	1.666	4.5	8.0	4.2	0.278
52C	1.739	1.683	3.2	8.7	5.0	0.235
52D	1.741	1.667	4.3	8.0	2.5	0.424
56A	1.734	1.697	2.1	8.2	5.1	0.209
56B	1.725	1.705	1.2	9.0	3.9	0.291
56C	1.721	1.709	0.7	8.9	4.9	0.248
56D	1.731	1.713	1.0	8.7	4.2	0.271
66A	1.729	1.601	7.4	7.3	5.3	0.172
66B	1.737	1.627	6.3	7.7	4.4	0.208
66C	1.738	1.616	7.0	7.4	4.2	0.234
66D	1.731	1.600	7.6	7.2	3.7	0.270
70A			0	8.4	4.8	0.224
70B			0	7.0	3.3	
70C			0	9.4		
70D			0	9.3		

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLE						
72A			0	10.5		
72B			0	11.8		
72C			0	10.7	6.4	0.202
72D			0	9.3	7.4	0.139
78A	1.770	1.667	5.8	6.9		
78B	1.763	1.675	5.0	7.4		
78C	1.772	1.687	4.8	8.4		
78D	1.771	1.682	5.0	8.1	4.1	0.260
80A	1.770	1.668	5.8	7.3		
80B	1.771	1.667	5.9	7.4	4.0	0.250
80C	1.772	1.681	5.1	8.0		
80D	1.774	1.682	5.2	7.7		
82A			0	8.8		
82B			0	9.3		
82C			0	11.6	7.2	0.218
82D			0	11.1	6.6	0.251
86A			0			
86B			0	10.2	6.1	0.225
86C			0	9.4		
86D			0	7.0		
88A	1.775	1.692	4.7	8.0	4.2	0.222
88B	1.771	1.669	5.8	6.9		
88C	1.754	1.671	4.7	10.2		
88D	1.773	1.671	5.8	5.6		
90A			0	10.7		
90B			0	10.1		
90C			0	11.1	5.3	0.265
90D			0	10.9	6.1	0.236
92A	1.771	1.686	4.8	7.2	3.8	0.240

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLE (Continued)						
92B	1.769	1.693	4.3	7.7	4.5	0.208
92C	1.766	1.678	5.0	6.0		
92D	1.767	1.673	5.3	7.0	3.9	0.225
96A	1.757	1.676	4.6	8.0	4.5	0.228
96B	1.751	1.696	3.1	7.6	4.1	0.250
96C	1.754	1.706	2.7	8.6		
96D	1.764	1.687	4.4	7.7	4.6	0.210
100A			0	10.2		
100B			0	9.8	6.0	0.212
100C			0	11.8		
100D			0	11.6	5.4	0.318

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLC						
1A			0	9.5	5.8	0.205
1B			0	9.0	5.0	0.221
1C			0	10.1	6.4	0.201
1D			0	9.4	7.1	0.174
7A	1.736	1.655	4.7	7.7	6.0	0.159
7B	1.748	1.649	5.7	8.3	6.1	0.177
7C	1.748	1.664	4.8	8.4	4.6	0.226
7D	1.747	1.658	5.1	8.3	5.5	0.208
9A	1.750	1.655	5.4	8.0	6.4	0.140
9B	1.750	1.662	5.0	7.2	5.3	0.188
9C	1.745	1.648	5.6	9.4	6.4	0.194
9D	1.755	1.660	5.4	10.0	5.7	0.218
11A			0	10.2	6.4	0.210
11B			0	9.4		
11C			0	10.5	6.4	0.215
11D			0	10.5	7.3	0.199
15A			0	11.5		
15B			0	10.2	7.3	0.180
15C			0	11.4	6.1	0.260
15D			0	12.2	6.7	0.250
17A	1.752	1.663	5.1	6.7		
17B	1.744	1.650	5.4	9.1	6.4	0.190
17C	1.749	1.657	5.3	9.7	5.4	0.218
17D	1.753	1.661	5.3	8.3	5.6	0.180
19A			0	11.1	7.7	0.191
19B			0	9.4	8.1	0.145
19C			0	10.1	6.6	0.190
19D			0	10.5		
21A	1.749	1.653	5.5	9.0	6.3	0.168
21B	1.750	1.659	5.2	10.1	6.3	0.229

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLC (Continued)						
21C	1.754	1.664	5.1	10.0	5.3	0.250
21D	1.751	1.659	5.3	6.7		
25A	1.750	1.660	5.1	9.4	6.3	0.198
25B	1.754	1.660	5.4	9.5	5.7	0.230
25C	1.749	1.658	5.2	8.8	5.4	0.204
25D	1.753	1.650	5.9	8.8	5.8	0.195
29A			0	10.9	6.1	0.260
29B			0	10.2	6.7	0.189
29C			0	10.4		
29D			0	9.3	6.6	0.175

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLE						
31A			0	9.7	6.9	0.182
31B			0	11.9	6.6	0.250
31C			0	11.6	7.8	0.217
31D			0	11.7	6.3	0.175
37A	1.764	1.665	5.6	7.9		
37B	1.761	1.680	4.6	8.0	5.0	0.200
37C	1.761	1.675	4.9	7.2	4.9	0.180
37D	1.759	1.679	4.6	7.4	4.0	0.228
39A	1.773	1.691	4.6	7.6	4.7	0.210
39B	1.769	1.669	5.7	8.0	4.4	0.239
39C	1.764	1.679	4.8	7.6	5.0	0.189
39D	1.758	1.685	4.2	8.1	4.7	0.220
41A			0	12.1	6.9	0.247
41B			0	11.0	5.7	0.246
41C			0	10.9	8.0	0.187
41D			0	11.2		
45A			0	12.4	7.3	0.237
45B			0	12.9	7.3	0.248
45C			0	12.1		
45D			0	10.7	6.6	0.208
47A	1.772	1.675	5.5	8.6	4.8	0.238
47B	1.770	1.667	5.8	8.1	5.5	0.173
47C	1.769	1.686	4.7	8.0	5.3	0.177
47D	1.764	1.682	4.7	7.3	4.3	0.206
49A			0	11.4	5.7	0.240
49B			0	12.1	7.3	0.214
49C			0	12.5	7.8	0.224
49D			0	10.5	7.2	0.193

TABLE 11-10 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLE (Continued)						
51A	1.770	1.684	4.9	7.6	4.9	0.192
51B	1.768	1.679	5.0	8.3	5.0	0.211
51C	1.762	1.679	4.7	8.7	5.1	0.212
51D	1.763	1.671	5.2	7.0		
55A	1.770	1.684	4.9	8.0	4.8	0.226
55B	1.768	1.671	5.5	7.9	5.1	0.191
55C	1.763	1.662	5.7	7.2	5.4	0.160
55D	1.755	1.670	4.8	7.3	4.4	0.221
59A			0	12.2		
59B			0	11.2	6.4	0.236
59C			0	10.7		
59D			0	11.7	8.5	0.182

TABLE 11-11
OXIDATION VERSUS STRENGTH OF PGX GRAPHITE
[Log 6484-138, Slab 6, Oxidation Temperature = 1273 K (1000°C)]

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC						
6A	1.725	1.653	4.2	8.4	4.3	0.269
6B	1.727	1.605	7.1	6.5		
6C	1.728	1.645	4.8	8.4	4.5	0.246
6D	1.727	1.671	3.2	8.4	4.6	0.248
20A	1.726	1.637	5.2	7.9	3.3	0.280
20B	1.740	1.646	5.4	7.2	5.4	0.162
20C	1.728	1.570	9.1	6.9		
20D	1.721	1.631	5.2	7.0	4.9	0.190
22A	1.725		~1.6	9.1		
22B	1.729	1.706	1.3	10.0	4.3	0.321
22C	1.723	1.704	1.1	10.0	4.6	0.259
22D	1.721	1.697	1.4	9.4	5.0	0.251
24A	1.723	1.652	4.1	8.1	4.6	0.249
24B	1.727	1.290	25.3	5.2		
24C	1.727					
24D	1.724	1.667	3.3	8.4	4.8	0.237
26A	1.723	1.668	3.2	8.8	4.4	0.270
26B	1.727	1.643	4.9	8.3	4.4	0.255
26C	1.737	1.654	4.8	7.2	5.2	0.190
26D	1.731	1.646	4.9	8.6	4.0	0.280
28A	1.728	1.711	1.0	9.4	4.5	0.301
28B	1.736	1.721	0.9	8.4	4.4	0.257
28C	1.727	1.710	1.0	7.6	5.3	0.168
28D	1.726	1.713	0.8	8.6	4.5	0.249
32A	1.729	1.479	14.5	6.2		
32B	1.729	1.216	29.7	4.8		
32C	1.736	1.654	4.7	8.4	4.1	0.258
32D	1.722	1.659	3.7	9.3	4.2	0.319

TABLE 11-11 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC (Continued)						
36A	1.731	1.708	1.3	8.0	6.5	0.160
36B	1.730	1.698	1.8	9.3	5.0	0.248
36C	1.725		~1.6	8.0		
36D	1.729	1.704	1.4	9.3	4.7	0.321
50A	1.722	1.702	1.2	9.7	5.8	0.241
50B	1.733	1.722	0.6	10.0	5.3	0.258
50C	1.722	1.709	0.8	9.1	4.9	0.241
50D	1.726	1.710	0.9	8.3	4.2	0.257
54A	1.721	1.693	1.6	8.6	6.0	0.189
54B	1.720	1.682	2.2	9.4	4.3	0.300
54C	1.729	1.498	13.4	5.4		
54D	1.724		~1.5	9.3		
58A	1.729	1.622	6.2	7.5	5.2	0.180
58B	1.732	1.696	2.1	8.6	4.6	0.249
58C	1.755	1.727	1.6	9.0	4.7	0.243
58D	1.738	1.407	19.0	5.6		
60A	1.729	1.670	3.4	8.6	5.2	0.220
60B	1.728	1.706	1.3	8.7	5.7	0.250
60C	1.753	1.737	0.9	9.7	4.6	0.276
60D	1.731	1.685	2.7	8.6	4.2	0.289
62A	1.734	1.647	5.0	5.9		
62B	1.725	1.662	3.6	8.1	4.6	0.231
62C	1.726	1.576	8.7	7.2	4.5	0.200
62D	1.727	1.133	34.4	4.0		
64A	1.726	1.691	2.0	9.4	4.5	0.294
64B	1.726	1.570	9.0	8.1	3.7	0.300
64C	1.729	1.490	13.8	5.8		
64D	1.726	1.686	2.3	9.7	4.7	0.281
68A	1.730	1.476	14.7	6.5		
68B	1.734	1.650	4.8	8.6	4.5	0.249

TABLE 11-11 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Axial MLC (Continued)						
68C	1.726	1.529	11.4	6.5		
69D	1.724	1.643	4.7	8.7	3.9	
Axial MLE						
74A	1.754	1.634	6.8	7.6	3.9	0.265
74B	1.755	1.484	15.4	4.4		
74C	1.758	1.682	4.3	9.1		
74D	1.761	1.703	3.3	9.1		
76A	1.756	1.649	6.1	7.9	4.0	0.249
76B	1.762	1.665	5.5	8.1	4.8	0.250
76C	1.758	1.495	15.0	3.8		
76D	1.759	1.689	4.0	9.5		
84A	1.750		~4.2	7.9		
84B	1.758	1.687	4.0	9.0		
84C	1.757	1.663	5.4	9.3	4.8	0.292
84D	1.749	1.675	4.2	8.8		
94A	1.746	1.639	6.1	7.9	4.9	0.210
94B	1.750		~4.6	7.7		
94C	1.751	1.668	4.7	9.3		
94D	1.750	1.655	5.4	8.0	5.2	0.203
98A	1.744	1.673	4.1	7.9		
98B	1.758	1.410	19.8	3.4		
98C	1.749		~4.2	8.3		
98D	1.746	1.676	4.0	9.1	5.3	0.240
Radial MLC						
3A	1.733	1.672	3.5	8.1	5.5	0.187
3B	1.729	1.664	3.8	8.7		
3C	1.715	1.666	2.9	8.1	6.2	0.161
3D	1.736	1.679	3.3	8.8	5.3	0.238

TABLE 11-11 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLC (Continued)						
5A	1.734	1.631	5.9	9.3	5.4	0.220
5B	1.730	1.487	14.0	5.1		
5C	1.735	1.669	3.8	8.6		
5D	1.731	1.539	11.1	7.6	5.0	0.192
13A	1.733	1.522	12.2	7.9	5.8	0.173
13B	1.733	1.606	7.3	8.7		
13C	1.739	1.600	8.0	7.7	3.7	0.237
13D	1.734	1.607	7.3	8.6	5.3	0.206
23A	1.733	1.732	0.1	10.9	6.4	0.227
23B	1.738	1.660	4.5	7.7	5.9	0.156
23C	1.730		~5.0	10.0		
23D	1.728	1.313	24.0	5.9		
27A	1.731	1.457	15.8	6.9		
27B	1.731	1.634	5.6	8.3		
27C	1.732	1.640	5.3	8.4	4.8	0.231
27D	1.728	1.583	8.4	7.7	5.1	0.190
Radial MLE						
33A	1.755		~5.7	10.1		
33B	1.750	1.601	8.5	7.4	5.4	0.162
33C	1.743	1.690	3.0	10.0	4.3	0.291
33D	1.749	1.689	3.4	7.2		
35A	1.752	1.678	4.2	9.7	5.1	0.199
35B	1.753	1.647	6.0	8.7	5.6	0.196
35C	1.752	1.492	14.8	5.8		
35D	1.740	1.688	3.0	7.7	5.2	0.178
43A	1.749	1.613	7.8	8.3		
43B	1.747	1.677	4.0	9.4		
43C	1.744	1.681	3.6	9.1	6.6	0.193
43D	1.739	1.448	16.7	4.6		
53A	1.754	1.679	4.3	8.3		

TABLE 11-11 (Continued)

Specimen	ρ_o (Mg/m ³)	ρ_{test} (Mg/m ³)	Burnoff (%)	UTS (MPa)	Modulus (GPa)	ϵ_f (%)
Radial MLE (Continued)						
53B	1.759	1.686	4.2	8.6	5.6	0.212
53C	1.746	1.645	5.8	9.4	6.5	0.190
53D	1.742	1.698	2.5	9.0		
57A	1.756	1.462	16.8	5.8		
57B	1.745	1.706	2.2	9.3	6.0	0.218
57C	1.763	1.644	6.8	9.7	6.0	0.218
57D	1.742	1.706	2.1	9.7		

TABLE 11-12
RATE OF STRENGTH REDUCTION AND OXIDATION RATES OF PGX GRAPHITE SPECIMENS
OXIDIZED AT 1075 AND 1273 K (800° AND 1000°C)

	MLC				MLE			
	Axial		Radial		Axial		Radial	
	1073 K (800°C)	1273 K (1000°C)	1073 K (800°C)	1273 K (1000°C)	1073 K (800°C)	1273 K (1000°C)	1073 K (800°C)	1273K (1000°C)
UTS (slope) ^(a)	-0.28	-0.28	-0.30	-0.30	-0.55	-0.38	-0.73	-0.46
Oxidation rate $\times 10^6 \text{ (s}^{-1}\text{)}$	2.2	11.8	1.6	13.2	0.013	3.6	0.0077	4.3
Elastic modulus (slope) ^(a)	-0.11	-0.11	-0.15	-0.17	-0.43	-0.29	-0.41	-0.22

^(a)Slope of linear fit of UTS and elastic modulus versus burnoff data (see Figs. 11-19 and 11-20).

Fig. 11-1. Sampling diagram for round-robin strength tests of 2020 graphite, log 6484-110, slab 3

Fig. 11-2. Coring diagram for round-robin strength tests of 2020 graphite, log 6484-110, slab 3

(a) SoRI test specimen

DIMENSIONS IN MM

(b) GA test specimen

Fig. 11-3. Tensile test specimen geometry

Fig. 11-4. Slabbing diagram for oxidation rate and strength versus oxidation of support floor block graphite PGX, logs 6484-112 and -138, slab 6

Fig. 11-5. Oxidation rate and mass transport sampling diagram for PGX graphite, slab 6: (a) section AX, 270° to 0°

Fig. 11-5. Oxidation rate and mass transport sampling diagram for PGX graphite, slab 6: (b) section BX, 90° to 180°

Fig. 11-5. Oxidation rate and mass transport sampling diagram for PGX graphite, slab 6: (c) section AE, 270° to 0°

Fig. 11-5. Oxidation rate and mass transport sampling diagram for PGX graphite, slab 6: (d) section BE, 90° to 180°

SECTION AY

Fig. 11-6. Sampling plan for oxidation profile mass transport of PGX graphite, logs 6484-112 and 138, slab 6, section AY, 270° to 0°

Fig. 11-7. Effect of P_{H_2} and burnoff on PGX graphite oxidation

Fig. 11-8. Effect of $P_{\text{H}_2\text{O}}$ on PGX graphite oxidation

Fig. 11-9. Oxidation of PGX graphite versus S/V

Fig. 11-10. E_a versus S/V for PGX graphite

Fig. 11-11. Oxidation of PGX graphite versus 1/T

Fig. 11-12. Oxidation of PGX graphite by CO_2

Fig. 11-13. Oxidation profile of PGX graphite: (a) specimen 24A

Fig. 11-13. Oxidation profile of PGX graphite: (b) specimen 30B

Fig. 11-13. Oxidation profile of PGX graphite: (c) specimen 12A

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Fig. 11-13. Oxidation profile of PGX graphite: (d) specimen 4B

Fig. 11-13. Oxidation profile of PGX graphite: (e) specimen 6A

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Fig. 11-13. Oxidation profile of PGX graphite: (f) specimen 30A

Fig. 11-13. Oxidation profile of PGX graphite: (g) specimen 4A

Fig. 11-13. Oxidation profile of PGX graphite: (h) specimen 24B

Fig. 11-13. Oxidation profile of PGX graphite: (i) specimen 14B

Fig. 11-14. Relation of $\bar{m}$ to burnoff

Fig. 11-15. Comparison of reaction rate (R_T) of thin disk with calculated reaction rate (R_S) from thick cylinder profile samples

Fig. 11-16. Oxidation versus strength sampling diagram for PGX graphite, slab 6: (a) section AC-AX, 0° to 90°

Fig. 11-16. Oxidation versus strength sampling diagram for PGX graphite, slab 6: (b) section BC-BX, 180° to 270°

Fig. 11-16. Oxidation versus strength sampling diagram for PGX graphite, slab 6: (c) section AE-AY, 0° to 90°

Fig. 11-16. Oxidation versus strength sampling diagram for PGX graphite, slab 6: (d) section BE-BY, 180° to 270°

Fig. 11-17. Ultimate tensile strength versus burnoff, PGX graphite, 1073 K (800°C) oxidation temperature: (a) axial MLC

Fig. 11-17. Ultimate tensile strength versus burnoff, PGX graphite, 1073 K (800°C) oxidation temperature: (b) axial MLE

Fig. 11-17. Ultimate tensile strength versus burnoff, PGX graphite, 1073 K (800°C) oxidation temperature: (c) radial MLC

Fig. 11-17. Ultimate tensile strength versus burnoff, PGX graphite, 1073 K (800°C) oxidation temperature: (d) radial MLE

Fig. 11-18. Ultimate tensile strength versus burnoff, PGX graphite, 1273 K (1000°C) oxidation temperature: (a) axial MLC

Fig. 11-18. Ultimate tensile strength versus burnoff, PGX graphite, 1273 K (1000°C) oxidation temperature: (b) axial MLE

Fig. 11-18. Ultimate tensile strength versus burnoff, PGX graphite, 1273 K (1000°C) oxidation temperature: (c) radial MLC

Fig. 11-18. Ultimate tensile strength versus burnoff, PGX graphite, 1273 K (1000°C) oxidation temperature: (d) radial MLE

Fig. 11-19. Ultimate tensile strength of oxidized PGX graphite, 1273 K (1000°C) oxidation temperature, GA log 6484-138

Fig. 11-20. Linear relationship of ultimate tensile strength and modulus to burnoff: (a) axial and radial MLC

Fig. 11-20. Linear relationship of ultimate tensile strength and modulus to burnoff: (b) axial and radial MLE