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HTGR SAFETY RESEARCH PROGRAM

QUARTERLY PROGRESS REPORT

for

JANUARY - MARCH 1975

I. INTRODUCTION

The Los Alamos Scientific Laboratory (LASL) is carrying out a broad program of research in High-Temperature Gas-Cooled Reactor (HTGR) safety technology under the direction of the Reactor Safety Research Division of the United States Nuclear Regulatory Commission. The present LASL program includes the following task areas:

- Fission Product Technology
- Primary Coolant Impurities
- Structural Evaluation
- Safety Instrumentation and Control Systems
- Accident Delineation
- Phenomena Modeling and Systems Analysis

This program was started in March 1974 and is a continuing effort. Previously, progress has been reported in a series of informal monthly progress reports. This present report is the second in a series of formal quarterly progress reports.

II. SUMMARY

Calculations of the 2-h Exclusion Area Boundary (EAB) thyroid dose produced by the release of iodine during a Maximum Hypothetical Fission Product Release (MHFPR) accident from a 2000-MWt HTGR were made with varying assumptions regarding fuel particle failure and iodine release rates from failed fuel. For the cases considered, the calculated thyroid dose ranged from 0.83 rem to 2677 rem. The detailed results are

described in Section III and show that the failed fuel iodine release constants are the most important parameter in the calculations.

The maximum whole-body dose to an individual at the Low Population Zone (LPZ) boundary was calculated for an accident that releases the entire core inventory of tritium (^3H) from a 3000-MWt HTGR. The calculated dose, 0.17 rem, indicates that ^3H is not an important nuclide to consider in dose calculations for major HTGR accidents.

The probabilistic techniques described in the Rasmussen report were applied to a calculation of ^{131}I thyroid dose from a MHFPR accident, assuming the accident had occurred. Results are reported graphically in Section III.

Release of ^{131}I was calculated for the MHFPR accident with the additional condition of failure to scram. Results indicate that the release of a given fraction of the ^{131}I occurs two or three hours earlier if there is failure to scram. Since the release is nearly total in either case, there is no significant change due to the failure to scram.

A calculation was made of the possible magnitude of the release of fission products into the coolant due to transverse gasborne transport through the fuel block graphite. The calculation shows that this mechanism may provide fission product release in the first year of fuel life several orders of

magnitude larger than the slower mechanisms of diffusion and adsorption-desorption.

The OXIDE-3 code received from General Atomic is now running at LASL on the CDC 7600. Fairly good agreement in test problem results has been obtained.

The CIMPRES code is now agreeing fairly well with analytical solutions for two simple heat transfer problems.

Experiments were conducted to examine the feasibility of using electrical resistance measurements to following the progress of gas-metal reactions. In experiments using wires of pure nickel in hot flowing air a sensitivity of 1% was obtained. The technique appears to be useful and will be further applied.

The complex equilibria codes used to compute coolant (thermodynamic) equilibrium compositions have been modified to calculate reaction potentials and to accommodate cesium adsorption on graphite. Preliminary calculations with the latter show an expected drop in gaseous cesium concentration. Continuing use of the codes has generated oxygen partial pressure values for various assumed initial coolant compositions.

Construction of the transpiration experiment apparatus has started.

Presentations were made by two equipment vendors to acquaint LASL personnel with equipment and operational problems for test systems for multiaxial testing of concrete and multiaxial dynamic testing of HTGR core models.

Development of analytic models for simplified HTGR core models has continued. SIMEAR, a computer program which generates simulated earthquake frequencies and intensities, has been obtained and modified for our purposes.

In preparation for physical testing of specific physical core models, a parameter study has been carried out to obtain a better understanding of the importance of various parameters to the response of core blocks to harmonic excitation. This

general approach will be extended by use of the additional analytical models which are now available.

A preliminary assessment of the possibility of cross-flow induced vibration of the support posts of a 3000-MWt HTGR has been carried out. It was concluded that such excitation is possible, particularly at full flow.

The prototype helium afterglow moisture monitor has been calibrated for a single set of flow conditions over a moisture content range of 0.22 to 1400 Vpm. A more compact and hardened version of the monitor for testing on-line at Ft. St. Vrain is being constructed. Use of the monitor for detecting other coolant impurities is being examined.

A preliminary examination of several alternate temperature measuring techniques has begun. Of particular interest are techniques for direct measurement of fuel temperature so that fuel failure and fission product behavior can be correlated with fuel temperature in experiments and operating reactors.

Use of event and fault tree logic and diagrams in an iterative fashion to carry out accident delineation has been chosen as the best methodology for our studies. Examples of this approach are given in Section VII.

Power distribution for the Fulton initial core have been calculated using both one- and two-dimensional models. Results are generally consistent with information given in the PSAR.

Parametric studies of the effects of ^{233}U on the isothermal temperature coefficient of the FGS reactor have been made by varying ^{233}U fractions in fixed $\text{C}/(^{235}\text{U} + ^{233}\text{U})$ cores. The presence of ^{233}U results in a less negative temperature coefficient with the magnitude of the effect increasing with ^{233}U content and temperature.

Several sets of multigroup cross-sections for HTGR safety analysis have been generated and additional sets are being

generated. LASL cross-section characteristics are critically compared with General Atomic (GA) generated cross sections in Section VIII.

Modeling and model checkout is proceeding for various system components to be included in the CHAP code. Modeling of the steam reheater, steam generator, and primary coolant circulator is discussed in Section VIII, along with the numerical integration technique developed for CHAP.

III. TASK 1, FISSION PRODUCT TECHNOLOGY

A. Exclusion Area Boundary Thyroid Dose - a Parametric Study (J. E. Foley)

1. Introduction

Calculations were made of the 2-h Exclusion Area Boundary (EAB) thyroid dose produced by the release of iodine during a Maximum Hypothetical Fission Product Release (MHFPR) Accident from a 2000-MWt HTGR. The purpose of this study was to identify the parameters of importance for the 2-h EAB dose calculations. Various initial conditions were considered: they ranged from the release of 25% of the core inventory of iodine at the onset of the accident to the General Atomic Company (GAC) values of fuel failure and iodine release rates. The following parameters are common to all of the calculations:

- The reactor power at the onset of the accident is 2000 MWt.
- The containment building leak rate¹ is 0.25%/day.
- The containment cleanup system recirculation rate¹ is 1 vol/h.
- There is a 1-h delay in the startup of the containment cleanup system after the onset of the accident.¹
- The chemical composition of the iodine in the containment building¹ is

96% inorganic
4% organic.

- The filter efficiencies of the containment cleanup system¹ are
 - 90% for inorganic
 - 30% for organic.
- The Exclusion Area Boundary distance is 350 m from the reactor and the atmospheric dilution factor¹ χ/Q is

$$\chi/Q = 1.27 \times 10^{-3} \text{ s/m}^3.$$
- The breathing rate¹ is $3.47 \times 10^{-4} \text{ m}^3/\text{s}$.
- Twenty-five percent of the core inventory of iodine is released during the MHFPR accident (Ref. 1, p. C-1-1).
- The dose conversion factors for inhalation of iodine² are

Isotope	Thyroid Dose (rem/mCi inhaled)
¹³¹ I	1486
¹³² I	52.88
¹³³ I	395.1
¹³⁴ I	25.38
¹³⁵ I	123.1

- The equilibrium inventories of the iodine isotopes were calculated using the fission yields given by Zysin, Lbov, and Sel'chenkov:³

Isotope	Inventory (Ci)
¹³¹ I	5.20×10^7
¹³² I	7.88×10^7
¹³³ I	11.56×10^7
¹³⁴ I	13.08×10^7
¹³⁵ I	10.22×10^7

Twenty-five percent of the inventory of each isotope is assumed to be released from the core during the MHFPR accident.

2. Calculations

Six iodine release cases were considered; each will be discussed separately.

a. Case 1: Release of 25% of the Inventory of Each Iodine Isotope at the Onset of the Accident

All five iodine isotopes were considered and buildup of ^{132}I and ^{134}I from precursor decay was included in the analysis. The amount of each isotope released from the containment building during the first 2 h after the onset of the accident and the resulting thyroid doses at the EAB are given below:

Isotope	Ci Released	Thyroid Dose (rem)	Fraction of Dose
^{131}I	2254.6	1476.8	0.55
^{132}I	3473.8	80.9	0.03
^{133}I	4879.3	849.0	0.32
^{134}I	4470.5	50.1	0.02
^{135}I	4063.2	220.6	0.08

Total Dose = 2677.4 rem

It is important to note that most of the thyroid dose (~87%) is from ^{131}I and ^{133}I ; these isotopes are long lived (compared to the 2-h time interval being considered) and are not produced from precursor decay. Because of this, subsequent calculations of the other cases will be done for ^{131}I only, and the total thyroid dose will be estimated by multiplying the dose due to ^{131}I by the factor $1/0.55 = 1.82$. This value is similar to that of 1.88 used by Sesonke⁴ to account for the dose contribution of the other iodine and tellurium isotopes. The error in the total dose produced by estimating the dose from ^{131}I release only will be small (less than a few percent).

b. Case 2: Fuel Fails at Beginning of Fuel Failure Curve and Failed Fuel Release Constants are Large.

The fuel was assumed to fail at the beginning of the failure curves for

TRISO particles.⁵ The temperatures of failure for the various ages of fuel are:

Fuel Age	Failure Temperature (K)
1 yr	1548
2 yr	1423
3 yr	1373
4 yr	1303

These failure temperatures were obtained from Fig. 5-1 of the SORS report (Ref. 5). The release constants were set to a large value, 1000 h^{-1} . The time-dependent release of iodine from the core was calculated (for this case and for all subsequent cases) with the AYER model and the release from the containment building was calculated using the model described in the previous quarterly report.⁶

The amount of ^{131}I released during the first 2 h after the onset of the accident is calculated to be 661.9 Ci; the estimated thyroid dose at the EAB is 786.0 rem.

c. Case 3: Fuel Fails at Beginning of Fuel Failure Curve and Failed Fuel Release Constants Equal 1 h^{-1}

Case 3 is identical to Case 2 except that the release constants equal 1 h^{-1} rather than 1000 h^{-1} ; i.e., the release rates are much lower in this case than they are in Case 2.

The amount of ^{131}I released during the first 2 h after the onset of the accident is calculated to be 292.2 Ci; the estimated thyroid dose at the EAB is 348.1 rem.

d. Case 4: Linear Fuel Failure Model and Failed Fuel Release Constants = 1 h^{-1}

Case 4 is similar to Case 3 except that the fuel does not fail at the beginning of the fuel failure curve, but it fails linearly within the failure region. The parameters for the linear fuel failure model were obtained directly from Fig. 5-1 of the SORS report (Ref. 5):

Fuel Age	Temperature of Fuel Failure (K)	
	Begin	Complete
1 yr	1548	1823
2 yr	1423	1723
3 yr	1373	1698
4 yr	1303	1673

The amount of ^{131}I released during the first 2 h after the onset of the accident is calculated to be 102.2 Ci; the estimated thyroid dose at the EAB is 120.5 rem.

e. Case 5: Linear Fuel Failure Model, GAC Failed Fuel Release Constants, and Initial Failed Fuel Fractions of 2.5%, 5%, 7.5%, and 10%

Case 5 differs from Case 4 in that the failed fuel release constants are those given in the SORS report (Ref. 5, Fig. 5-3) and the fraction of fuel failed at the onset of the accident is 2.5%, 5%, 7.5%, and 10% for 1-, 2-, 3-, and 4-yr-old fuel, respectively (Ref. 5, p. 6.3). This case appears to be identical to that in the Summit Plant PSAR of the EAB dose calculations for the MHFPR accident.

The amount of ^{131}I released during the first 2 h after the onset of the accident is calculated to be 0.80 Ci; the estimated thyroid dose at the EAB is 0.95 rem. The value given in the Summit Plant PSAR is ~ 2.5 rem (Ref. 1, Fig. C.6-2).

f. Case 6: Linear Fuel Failure Model, GAC Failed Fuel Release Constants, and Initial Failed Fuel Fraction Equal 0

Case 6 is identical to Case 5 except that there is no failed fuel at the onset of the accident.

The amount of ^{131}I released during the first 2 h after the onset of the accident is calculated to be 0.70 Ci; the estimated thyroid dose at the EAB is 0.83 rem.

3. Results

Table I summarizes the results of the calculations of the thyroid dose at the

EAB for the various cases considered. It is seen that the failed fuel release constants are the most important parameters in the dose calculations.

B. Tritium Dose From An HTGR Accident (J. E. Foley)

1. Introduction

The maximum whole-body exposure to an individual located at the Low Population Zone (LPZ) boundary, 2.4 km from the reactor, is calculated for an accident that releases the entire core inventory of tritium (^3H). It is concluded from this analysis that ^3H is not an important nuclide to consider in dose calculations from an HTGR accident.

2. Calculations

The core inventory of ^3H for a 3000-MWt reactor is estimated to be $\sim 30\,000$ Ci.⁷ It is assumed that the entire inventory is released at a uniform rate during a time interval of 8 h; i.e., the release rate (Q) is ~ 1 Ci/s. Pasquill Type F inversion conditions are assumed to persist for the entire 8-h release interval. χ/Q for the time interval is 1.7×10^{-4} (s/m³), obtained from Regulatory Guide 1.3 (Ref. 8) for center-line plume concentration at 2.4 km from the reactor with ground level release. The atmospheric concentration (χ) of ^3H at the LPZ boundary is then calculated to be

$$\begin{aligned}\chi &= 1.7 \times 10^{-4} Q \\ &= 1.7 \times 10^{-4} \text{ Ci/m}^3.\end{aligned}$$

The amount of ^3H inhaled by a "standard man" at the LPZ boundary is equal to the product of the inhalation rate (B), given in Regulatory Guide 1.3 as 3.47×10^{-4} m³/s, the atmospheric concentration (χ) of the nuclide, and the length of the exposure; i.e., the amount of ^3H inhaled during the 8-h (2.88×10^4 -s) interval is

$$\begin{aligned}I &= B \cdot \chi \cdot t \\ &= (3.47 \times 10^{-4}) \times (1.7 \times 10^{-4}) \times (2.88 \times 10^4) \text{ Ci} \\ &= 1.7 \text{ mCi}.\end{aligned}$$

TABLE I
EAB THYROID DOSE

<u>Case</u>	<u>I Released (Ci)</u>	<u>Thyroid Dose (rem)</u>
1. Release of 25% of iodine at onset of the accident	2254.6 (I-131) 3473.8 (I-132) 4879.3 (I-133) 4470.5 (I-134) 4063.2 (I-135)	2677.4
2. a. Fuel Fails at beginning of fuel failure curve	661.9 (I-131)	786.0*
b. Failed fuel release constants = 1000 h ⁻¹		
3. a. Fuel fails at beginning of fuel failure curve	292.2 (I-131)	348.1*
b. Release constants = 1 h ⁻¹		
4. a. Linear fuel failure	102.2 (I-131)	120.5*
b. Release constants = 1 h ⁻¹		
5. a. Linear fuel failure		
b. GAC release constants	0.80 (I-131)	0.95*
c. Initial failed fuel fractions of 2.5%, 5%, 7.5%, and 10%		
6. a. Linear fuel failure		
b. GAC release constants	0.70 (I-131)	0.83*
c. Initial failed fraction = 0		

*Total thyroid dose estimated from ¹³¹I release.

This 1.7 mCi of ³H inhaled can be converted to a whole-body dose equivalent using the conversion factor of ~ 0.1 rem/mCi of ³H inhaled⁹ for acute exposures.

Thus, the whole-body dose equivalent to an individual located on the LPZ boundary from the release of the entire core inventory of ³H during inversion weather conditions is

$$D = (1.7 \text{ mCi}) \times (0.1 \text{ rem/mCi}) \\ = 0.17 \text{ rem.}$$

3. Conclusions

The maximum whole-body dose equivalent received by an individual located on the LPZ boundary (2.4 km from the reactor)

during the release of the entire core inventory of ³H from a 3000 MWt HTGR reactor under inversion weather conditions is 0.17 rem. This dose is approximately equal to the dose that would be received by the individual in one year from natural background radiation. Since the entire core inventory of ³H would not be released to the environment during an accident for a reactor with an intact containment building, the actual dose received would be significantly less than the 0.17 rem calculated here. It is thus concluded that ³H is not an important isotope to consider in dose calculations for acute exposure to the population from an HTGR accident.

C. Consequence Analysis of the Fulton
MHFPR Accident (G. A. Baca)

In an effort to understand the techniques and methodology used in the Rasmussen report,¹⁰ the same type of analysis was performed utilizing the available data from the several sources - the Rasmussen report, the Fulton PSAR,¹¹ and Regulatory Guide 1.3.⁸ The accident chosen for analysis was the Design Basis Depressurization Accident (DBDA) with core heatup, i.e., the MHFPR Accident. The ^{131}I source terms used for iodine release from the prestressed concrete reactor vessel (PCRV) to the containment building are those reported by General Atomic (Ref. 5). The accident is assumed to take place over a 720-h period. Radioactive decay of the ^{131}I before release to the atmosphere is considered; however, precursor buildup of ^{131}I is neglected because of the relative short half-life of ^{131}I precursors.

Leakage of ^{131}I from the containment building is assumed to be 0.1% per day for the first 24 h after the onset of the accident and 0.05% per day for the remaining 696 h. This time-dependent leakage is the source term for the generalized Gaussian diffusion model.⁸ In this analysis the plume centerline concentration calculated with the above model was assumed to be the uniform concentration over the width of the plume at the distance of interest. The plume is released at ground level, is assumed to spread uniformly in a $22\text{-}1/2^\circ$ sector (to coincide with the population distributions), and is damped by the appropriate building wake factor.

Twenty-four cases of atmospheric stability were considered.¹⁰ The vertical standard deviations of the plume, σ_z , were obtained from standard plots¹² of σ_z vs distance from release. A limit of 1500 m was assumed on the vertical dispersion. All atmospheric conditions - stability, wind direction, and wind speed - were

assumed to remain constant for the entire period of the accident. Deposition (fall-out), rainout, radioactive decay, and precursor decay were not considered during atmospheric transport of the released ^{131}I .

The population distributions considered are those reported in the Rasmussen report. At distances close to the release the concentration of ^{131}I varies significantly with distance; therefore, in order to provide a more accurate calculation of thyroid man-rem, a linear interpolation between the points given in each category was performed to give a finer mesh with respect to distance and population. The thyroid dose in man-rem was calculated using these population distributions, the calculated ^{131}I concentrations, the appropriate breathing rate, and thyroid dose per curie as given in Ref. 10.

The total probability that a particular sector of the population surrounding the "average reactor site" is affected and to what extent it is affected is the product of the probability of occurrence of a particular accident (P_{accident}), the probability that a particular set of atmospheric weather conditions prevail ($P_{\text{wind}} \times P_{\text{stability}}$), and the probability that a particular population density will be exposed ($P_{\text{population}}$). The probability of occurrence of the MHFPR accident was taken as 1.0. In other words, the MHFPR accident had occurred.

Figures 1 and 2 are plots of the integral probability density functions. These plots are of interest because they show the probability of exceeding a dose of a given magnitude. Figure 1 was prepared to show the consequences of an MHFPR accident assuming the ventilation system operated properly. (The proper operation of the ventilation system is: Start up 2 h after the onset of the accident and continue operation with filter efficiencies of 99% for particulate iodine, 90% for elemental iodine, and 70% for organic iodine for the duration of the accident.) Figure 2

Fig. 1. Probability of the population receiving a thyroid dose from ^{131}I greater than or equal to that indicated on the abscissa from the MHFPR. The containment cleanup system operates properly.

presents the consequences of the MHFPR accident assuming the ventilation system fails completely and remains nonfunctional for the entire accident period.

From these figures it is noted that the dose with failure of the containment cleanup system varies from 100 to 150 times the dose when the system operated properly. It is also of significance to note that there are probabilities, given the accident, which correspond to the occurrence of no consequences. This is illustrated in Figs. 1 and 2 by the curve leveling off at a probability of approximately 0.9. In other words, there is a 10% chance, given the accident, that no one will receive a dose from the ^{131}I released.

It is of utmost importance in reviewing the data presented in Figs. 1 and 2 to understand that they were prepared assuming that the MHFPR accident had occurred and in no way should the probability axis be construed to include the probability of the MHFPR accident occurring. Accident probabilities have as yet not been defined

Fig. 2. Probability of the population receiving a thyroid dose from ^{131}I greater than or equal to that indicated on the abscissa from the MHFPR. The containment cleanup system fails to operate.

for the HTGR, but they might reasonably be expected to be on the same order of magnitude as those for the PWR and BWR, 10^{-5} , in which case the probability scale would be multiplied by this constant factor. The range would then be reduced from $10^0 - 10^{-4}$ to $10^{-5} - 10^{-9}$.

D. Release of ^{131}I in the MHFPR Accident with Failure to Scram (G. E. Cort)

1. Introduction

The release of ^{131}I was calculated for a series of simultaneous events which appear to be very improbable: 1) failure of the primary coolant pressure boundary, 2) failure of the main and auxiliary circulators, 3) failure of the control rod drive mechanisms (control rods do not move). Exactly how unlikely these simultaneous failures are would have to be determined by an event-tree and fault-tree analysis.

2. Method

The AYER model was modified to include calculation of negative reactivity insertion due to temperature increase and

the resulting changes in fission and decay power. One average group of delayed neutrons was considered, with the limitation that the negative period could not be less than 80 s because of the neutrons belonging to the group with precursors of longest life. The helium has a slight reactivity worth; so for the first time step, the reactivity change was set at $-5 \times 10^{-5} \frac{\Delta k}{k}$ due to complete depressurization and helium removal. Two cases with different isothermal temperature coefficients of reactivity were used; one as a function of temperature, and the other assumed a constant $-10^{-5} \frac{\Delta k}{k}$. At the end of the equilibrium cycle (EOEC), the temperature coefficient may be in this range for temperatures near 1900 K (almost twice normal operating temperature) due to buildup of ^{233}U .¹³ Similar calculations were later done for the temperature coefficient using a six-group point kinetics model (Ref. 6, pp. 46-49). Figure 3 shows

Fig. 3. Prompt fission power in loss of coolant accident with failure to scram.

the prompt fission power vs time. The six-group point kinetics model shows a slightly faster power decay than the one-group model using the same temperature coefficient. The core thermal power (prompt and delayed fissions plus fission product decay) is shown in Fig. 4. The temperatures calculated by AYER throughout the core and the resulting fission product release will, therefore, be on the conservative (high) side. Figure 5 shows the maximum and average core temperatures. For the assumed $\alpha_T = -10^{-5}$, the average temperature reaches the fuel failure level (~ 1573 K) about 8 ks (2.2 h) sooner than when the reactor scrams.

The ^{131}I release fractions in Fig. 6 demonstrate that the release is about 2 to 3 h faster depending on α_T . Since the total release is nearly complete in either case, there is no significant change due to the failure to scram. For isotopes with short half-lives, or where temperatures affect other mechanisms (diffusion and adsorption), the total release could be altered significantly.

Fig. 4. Core thermal power.

Fig. 5. Core temperatures.

Fig. 6. ^{131}I release fraction.

E. Transverse Flow of Fission Products Through Graphite (B. L. Holian)

A possible mechanism for the release of fission products from the fuel sticks into the circulating helium coolant of an HTGR is that of gasborne transport through the porous fuel-block graphite separating fuel and coolant holes. (See Fig. 7 for fuel element geometry.) For every coolant hole there are two fuel holes arranged in a hexagonal pattern about the coolant hole (each of the six surrounding fuel regions being shared by three coolant holes). This convergent geometry can be simplified to a cylindrical coolant hole surrounded by an inner graphite annulus (as shown in Fig. 7), representing the graphite fuel block, and an outer charcoal annulus, representing the fuel sticks (composed of pyrocarbon- and silicon

Fig. 7. Hexagonal arrangement of fuel holes surrounding coolant hole (2:1); typical HTGR Core geometry, horizontal section. Dimensions given in cm. (Only central coolant hole and neighboring fuel holes shown.)

carbide-coated microsphere fuel particles embedded in a charcoal matrix). Since the graphite block is porous, the helium coolant gas can permeate the graphite annulus and flow along the length of the gap between the fuel sticks and the graphite block for the length of the fuel element. (See Fig. 8 for vertical section of fuel element.) This gaseous flow through the graphite block, driven by the pressure drop along the coolant channel, has the potential of picking up fission products that have escaped the fuel sticks and begun to diffuse through the helium in the gap,

Fig. 8. Vertical section (A-A of Fig. 7) through one fuel element (graphite block). Dimensions given in cm, not drawn to scale.

$$h = \frac{1}{2} (d_1 - d_0)$$

$$\epsilon = \frac{1}{2} (d_2 - d_1)$$

$$d = \frac{1}{2} (d_1 + d_2) = d_1 + \epsilon = d_2 - \epsilon$$

and sweeping them out through the graphite, thereby to a large extent bypassing the slower mechanisms of diffusion and adsorption-desorption in graphite.

This problem has been discussed previously,¹⁴ but due to the choice of diverging, rather than converging geometry as outlined above, and due to minor numerical errors in the presentation, the development will be briefly outlined here once again.

The radial (transverse) flow velocity $u_r(z)$ (cylindrical coordinates, $r = (x^2 + y^2)^{1/2}$, θ , z ; see Figs. 7 and 8) of coolant gas through the graphite annulus can be obtained from Darcy's Law for flow through a porous medium:

$$u_r(z) = \frac{K}{\mu h} [P_0(z) - P_1(z)], \quad (1)$$

where K is the permeability (cm^2) of graphite to helium, μ is the viscosity of helium, h is the thickness of the graphite

annulus, P_0 is the helium pressure in the coolant channel, and P_1 is the helium pressure in the gap. If $\Delta P_0 \equiv P_0(0) - P_0(L)$, the pressure drop along the coolant channel in a fuel element of length L , is small compared to P_0 , then

$$P_0(z) = P_0(0) - \frac{\Delta P_0}{L} z, \quad (2)$$

so that

$$-\frac{dP_0(z)}{dz} = \frac{\Delta P_0}{L}. \quad (3)$$

The axial flow velocity along the gap $u_z(r, z)$ can be obtained by solving the cylindrical equation for Poiseuille flow within the gap,

$$-\frac{dP_1(z)}{dz} + \mu \left(\frac{\partial^2 u_z}{\partial r^2} + \frac{1}{r} \frac{\partial u_z}{\partial r} \right) = 0, \quad (4)$$

subject to the boundary conditions

$$u_z \left(\frac{d_1}{2}, z \right) = u_z \left(\frac{d_2}{2}, z \right) = 0, \quad (5)$$

i.e., no flow at the inner and outer cylindrical boundaries of the gap. After some tedious algebra, the following result is obtained:

$$-\frac{dP_1(z)}{dz} = \frac{32 \mu u_z(z)}{d_2^2 + d_1^2 - \frac{d_2^2 - d_1^2}{\ln(d_2/d_1)}} + \frac{12 \mu}{\epsilon^2} u_z(z) \quad (6)$$

as $\epsilon \rightarrow 0$ (in obtaining Eq. 6, the average over the radial profile of the axial velocity has been performed).

Mass balance in a differential cylindrical volume element of the gap (shown in Fig. 9) determines the differential equation for axial flow: the mass flow radially outward through dA_z is $-\rho u_r(z) dA_z$ (ρ is the mass density of the coolant gas), outward axially through A_z at z is

Fig. 9. Differential cylindrical volume element of gap.

$-\rho u_z(z) A_e$, and outward axially through A_e at $z + dz$ is $\rho u_z(z + dz) A_e$. Since the total mass flow out of the volume element must be zero ($dA_z = \pi d_1 dz$ and $A_e = \pi \epsilon d$, d is the average gap radius, see Fig. 8),

$$\rho \pi \epsilon d \cdot du_z(z) = \rho u_r(z) \pi d_1 \cdot dz, \quad (7)$$

or, using Eq. 1,

$$\begin{aligned} \frac{du_z(z)}{dz} &= \frac{d_1}{\epsilon d} u_r(z) \\ &= \frac{K d_1}{\mu h \epsilon d} [P_0(z) - P_1(z)]. \end{aligned} \quad (8)$$

Differentiating by z and using Eq.(3) and the asymptotic value of Eq.(6) for small gap width ϵ relative to d , the following is obtained:

$$\begin{aligned} \frac{d^2 u_z(z)}{dz^2} &= \frac{K d_1}{\mu h \epsilon d} \left[\frac{dP_0(z)}{dz} - \frac{dP_1(z)}{dz} \right] \\ &= \left(\frac{12 K d_1}{h \epsilon^3 d} \right) u_z(z) - \left(\frac{\Delta P_0 K d_1}{\mu L h \epsilon d} \right) \\ &= A^2 u_z(z) - B^2, \end{aligned} \quad (9)$$

where $A^2 = 12 K d_1 / h \epsilon^3 d$ and $B^2 = \Delta P_0 K d_1 / \mu L h \epsilon d$.

Subject to the boundary condition

$$u_z(0) = \frac{du_z}{dz} \left(\frac{L}{2} \right) = 0, \quad (10)$$

the solution to Eq.(9) is easily shown to be

$$u_z(z) = \frac{B^2}{A^2} \left[1 - \frac{\cosh A \left(\frac{L}{2} - z \right)}{\cosh \frac{AL}{2}} \right]. \quad (11)$$

As long as $\Delta P_0 \ll P_0$, the axial flow will be very close to symmetrical about $z = \frac{L}{2}$, as dictated by Eq. 10, although the above solution (Eq. 11) also satisfies the more general boundary condition $u_z(0) = u_z(L) = 0$. The mass of coolant gas sweeping through the gap and out into the coolant per unit time is equal to the mass leaving the region $\frac{L}{2} \leq z \leq L$ (or entering the region $0 \leq z \leq \frac{L}{2}$, by symmetry) per unit time and is obtained by integrating (see Eq. 7)

$$\begin{aligned} \dot{m} &= \rho u_r(z) dA_z \\ &= \rho u_r(z) \pi d_1 \cdot dz \\ &= \rho \pi \epsilon d \cdot du_z(z), \end{aligned} \quad (12)$$

from $z = 0$ [$u_z(0) = 0$] to $z = \frac{L}{2}$:

$$\begin{aligned} \dot{m} &= \rho \pi \epsilon d \cdot u_z \left(\frac{L}{2} \right) \\ &= \frac{\rho \pi \epsilon^3 d \Delta P_0}{12 \mu L} \left(1 - \frac{1}{\cosh \gamma} \right), \end{aligned} \quad (13)$$

where

$$\gamma = \frac{AL}{2} = \left(\frac{3KL^2 d_1}{h \epsilon^3 d} \right)^{1/2}, \quad (14)$$

a dimensionless permeability parameter.

As $\gamma \rightarrow \infty$,

$$\dot{m} \rightarrow \frac{\rho \pi \epsilon^3 d \Delta P_0}{12 \mu L}, \quad (15)$$

which is independent of the permeability K . As $\gamma \rightarrow 0$, $1 - (\cosh \gamma)^{-1} \rightarrow \frac{1}{2} \gamma^2$ and

$$\dot{m} \rightarrow \frac{\rho \pi K L d_1 \Delta P_0}{8 \mu h}, \quad (16)$$

which is independent of the width of the gap. The radial flow velocity can be gotten from Eqs. (8) and (11):

$$u_r(z) = \frac{B^2 \epsilon d}{A D_1} \cdot \frac{\sinh A \left(\frac{L}{2} - z \right)}{\cosh \frac{A L}{2}}; \quad (17)$$

therefore, the maximum ingress is at $z = 0$ and for $\gamma \rightarrow \infty$,

$$u_r(0) \rightarrow \frac{\Delta P_0}{\mu L} \left(\frac{K \epsilon^3 d}{12 h d_1} \right)^{1/2}, \quad (18)$$

while for $\gamma \rightarrow 0$,

$$u_r(0) \rightarrow \frac{K \Delta P_0}{2 \mu h}. \quad (19)$$

The mass flow rate of fission product vapor from the fuel gap through the graphite to the coolant hole will then be determined by the relative mass densities of the fission product vapor and the helium coolant:

$$\begin{aligned} \dot{m}_f &= \frac{\rho_f}{\rho} \dot{m} \\ &= \frac{M_f P_f}{4 P} \dot{m}, \end{aligned} \quad (20)$$

where ρ_f is the fission product vapor density, P_f the fission product vapor pressure determined by the Freundlich adsorption isotherm for the fuel stick charcoal, and M_f the isotope mass (e.g., for Cs^{137} , $M_f = 137$; $M_{\text{He}} = 4$). For reactor-grade graphite¹⁵ $K \approx 10^{-10} \text{ cm}^2$; the helium coolant in a reference 300-MWt HTGR core is at a pressure of 49 atm and a temperature of 1014 K, with $\rho = 2.36 \times 10^{-3} \text{ g/cm}^3$, $\mu = 4.55 \times 10^{-4} \text{ g/cm-s}$, and $\frac{\Delta P_0}{L} = 1072 \text{ g/cm}^2\text{-s}^2$. Therefore, with 3300 coolant holes laterally across the core and eight fuel blocks in height, the total-core mass flow rate calculated from Eq. 13 (for one coolant hole in one block)

is $\dot{m}_{\text{core}} = 46 \text{ g/s}$. If Cs^{137} is considered, its vapor pressure in the gap may be estimated¹⁶ to be approximately 10^{-9} atm for one-tenth monolayer coverage in the fuel stick charcoal at a temperature of about 1163 K. Consequently, the mass flow rate of Cs^{137} through the graphite into the coolant via this transverse (radial) flow mechanism would be $\dot{m}_f = 3.2 \times 10^{-8} \text{ g/s}$ or about $5 \times 10^{21} \text{ atoms/yr}$, compared to $3 \times 10^{16} \text{ atoms}$ in the first year via the slower diffusion and adsorption-desorption mechanism (the latter requires at least four years to build up to such a level of release to the coolant). As a check on the validity of the method, the maximum axial flow velocity past the fuel stick is calculated from the first of Eqs. (13) to be about 10 cm/s, while the radial flow velocity is, according to Eq. (17), about 0.01 cm/s, both of which are tiny compared to the coolant flow speed of 5500 cm/s. Also, as expected, the pressure drop across the graphite annulus is at most 0.05% (see Eq. 1).

As reported in the open literature, experiments show remarkably good agreement with the theory.¹⁴ A subsequent correction¹⁷ to the original paper discards the predictions of the mechanism as it affects the Ft. St. Vrain reactor, without sufficient justification, especially in view of the above calculations. It appears, therefore, that independent experiments are needed either to verify or disqualify, once and for all, the proposed transverse flow mechanism as a significant mode of fission product release in an HTGR, especially for the metallic fission products.

IV. TASK 2, PRIMARY COOLANT IMPURITIES

Efforts within this task area are directed to study of the interactions between impurities in the helium primary coolant gas and the various materials in the primary coolant circuit. The most

likely impurities are steam and the products of the steam-graphite reaction: hydrogen and carbon monoxide. Materials of interest are the core and related graphite structures, and the metallic components such as steam generators, helium circulators, orifice valves, and control rod systems. Interactions between gaseous impurities and fission products with subsequent formation of volatile species are also of interest.

A. Code Development (A. Bowman, R. Feber)

The OXIDE-3 code received from General Atomic Company is now running at the Los Alamos computing facility on the CDC 7600. Results of the run of the test problem presented in code documentation¹⁸ are in fairly good agreement with the GAC results.

We are now making a detailed study and cleanup of the OXIDE-3 code eliminating unused statements and correcting errors as they appear. This effort should yield a cleaner, more efficient code, as well as developing a thorough understanding of the actual computations involved.

The computer model GOP-TWO (obtained earlier from the Battelle Pacific Northwest Laboratories), which calculates the rate of core graphite oxidation by thermal and radiolytic reactions due to impurities in the helium coolant of an HTGR, has been reviewed. Except for one apparent error (dealing with conversion of units) in the portion of the code calculating the rates of the radiolytic reactions, the program appears to be consistent with the assumed model.

Several computer runs were made with and without the radiolytic reactions using input data comparable with but not identical to those used elsewhere to calculate steady-state gas compositions for the Fort St. Vrain reactor.¹⁹ The effect of adding the radiolytic reactions as modeled is to increase the CO₂ concentrations significantly without changing the H₂O, CO, and

H₂ concentrations greatly. When the apparent error was corrected, however, the calculated steady state concentrations of CO₂ decreased by about 40%.

The CIMPRES(Coolant IMPurity REactions) code is now operational for two dimensional heat flow. The code gives fairly good agreement with the analytical solutions for two simple heat transfer problems.

B. Reactions of Impurities with Graphite and Metallic Components (L. C. Michels, W. A. Stark)

Feasibility experiments were conducted utilizing electrical resistance measurements to follow the progress of gas-metal reactions. The technique appears useful for investigations of surface oxidation of wire specimens; the specimen resistance increases as the metal oxide is formed, due to a decrease in the cross-sectional area of the base metal. The resistance of an oxide film formed on the surface of a metal or alloy is generally much greater (several orders of magnitude) than the resistance of the base metal, resulting in a measurement that would reflect only the resistance of the base metal. The situation can be more complex since changes other than surface oxidation, such as carburization or decarburization might occur to change the resistivity of the base metal. Whether or not such changes would be measurable within the accuracy of the experiment, or whether or not the effects of such changes occurring simultaneously could be separated will be the subject of continuing experimental studies.

Initial experiments have been used to examine the feasibility of using electrical resistance measurements to follow surface oxidation kinetics in the case of the oxidation of pure Ni (99% Ni) in flowing air at 900°C.

Resistance was determined by potentiometric measurements of the voltage drop across the specimen and comparison with the voltage drop across a known resistance (standard resistance). Both the

specimen and the standard resistance were connected in series with a current source. The specimen resistance was then calculated using the expression $R = R_s \left(\frac{E}{E_s} \right)$, in which R and R_s are the specimen and standard resistances, respectively, and E and E_s are the voltage drops across the specimen and standard resistances, respectively. Two specimens were run simultaneously at the same temperature. One specimen was allowed to oxidize in flowing air, while the other was maintained in flowing helium to provide a means of separating any changes due to purely thermal effects. The millivoltage drops across the standard resistance and each of the two specimens, as well as the millivoltage output from a thermocouple were measured and recorded at 30-s intervals on a Honeywell multipoint potentiometer. Both specimens were maintained under flowing helium during furnace heatup. Flowing air was then substituted for flowing helium over one specimen. The flow rates of both the helium and the air were maintained at approximately $2.36 \times 10^{-5} \frac{\text{m}^3}{\text{s}}$ ($3 \frac{\text{ft}^3}{\text{h}}$) at atmospheric pressure.

In the first of two experiments, specimens of 99% Ni wire of nominal 0.051-mm (0.020-in.) diam were used. In the second experiment, specimens of 99.5% Ni rod of nominal 1.59-mm (1/16-in.) diam were used. In the case of the rod specimens the measurements were not sufficiently sensitive to provide meaningful results, since sensitivity decreases as specimen surface to volume ratio decreases. The wire specimen was first exposed to flowing air at 700°C. No resistance changes were observed in about 7.2 ks (2 h). The specimen was returned to flowing helium and the temperature increased to 900°C. The specimen was again exposed to flowing air, and a resistance increase was observed within 3.6 ks (1 h). The resistance continued to increase for the duration of the experiment. The specimen exposed only to helium showed no resistance changes apart

from a minor decrease in the first 10.8 ks (3 h) followed by a return to the original value. It is assumed that any oxidation that might have occurred at 700°C is negligible compared to that which occurred at 900°C. The results of this experiment are given in Table II. An error analysis indicated the sensitivity of the resistance measurements to be $\pm 1\%$.

The electrical resistance R of a cylindrical specimen of length ℓ , radius r , and resistivity ρ is given by $R = \frac{\rho \ell}{\pi r^2}$. If it is assumed that the resistance change is due only to the reduction of the cross-sectional area of the specimen, $R = \frac{A}{(r_0 - T)^2}$ in which $A = \frac{\rho \ell}{\pi}$, r_0 = the initial specimen radius, and T = the thickness of the metal layer lost due to oxidation. If it is also assumed that T is a parabolic function of time,

$$T = kt^{1/2}, \quad (21)$$

then

$$R = \frac{A}{(r_0 - kt^{1/2})^2}. \quad (22)$$

Rewriting Eq. (22) in logarithmic form and using the approximation $\ln \left(1 - \frac{kt^{1/2}}{r_0} \right) \approx \frac{-kt^{1/2}}{r_0}$ yields

$$\ln R = \ln A - 2 \ln r_0 + \left(\frac{2k}{r_0} \right) t^{1/2}. \quad (23)$$

The slope of a plot of $\ln R$ versus $t^{1/2}$ will be $\frac{2k}{r_0}$ and the intercept will be $\ln A - 2 \ln r_0$. A linear least squares analysis produced values for the constants A and k of:

$$A = 7.589 \times 10^{-3} \Omega \cdot \text{mm}^2$$

and

$$k = 3.775 \times 10^{-5} \text{mm} \cdot \text{s}^{-1/2}.$$

Resistance values calculated using Eq. (22) are included in Table II to show the fit of Eq. (22) to the measured values of R .

TABLE II
DATA FOR INITIAL NICKEL OXIDATION EXPERIMENT

<u>Time (ks)</u>	<u>Resistance (ohms)</u> <u>(Measured)</u>	<u>Resistance (ohms)</u> <u>(Calculated from Equation 2)</u>
0.6	0.1224	0.1220
3.6	0.1235	0.1233
7.2	0.1239	0.1243
10.8	0.1249	0.1250
14.4	0.1254	0.1256
18.0	0.1261	0.1261
21.6	0.1268	0.1266
25.2	0.1268	0.1271
28.8	0.1277	0.1275
32.4	0.1279	0.1279
39.6	0.1286	0.1287
46.8	0.1294	0.1294
54.0	0.1301	0.1300

It is interesting to note that A could alternatively be calculated using the expression $A = \frac{\rho l}{\pi}$, the initial specimen dimensions, and either a literature value for ρ which yields $A = 7.883 \times 10^{-3} \Omega \cdot \text{mm}^2$ or the initial resistance measurement which yields $A = 7.668 \times 10^{-3} \Omega \cdot \text{mm}^2$.

The thickness of the lost Ni layer on the specimen exposed to flowing air was determined metallographically to be 7.24 μm for a total exposure time of 76 ks (21.12 h). Equation (21) yields a value of 10.35 μm , approximately 43% higher than the measured value. It is probable that the value determined metallographically is in error, since no special precautions were taken to insure specimen edge retention. Therefore an additional specimen of the Ni wire was exposed to flowing air at 900°C for 20 h, and the weight gain was determined. The thickness of the lost Ni layer determined in this way was found to be 8.07 μm . Equation 21 yields a value of 10.08 μm , approximately 25% higher than the measured value.

For comparison of these results with independent results, a value of 14.5 $\frac{\text{gm}}{\text{m}^2}$ (Ref. 20) for an exposure of 28.8 ks (8 h) was taken. This specific weight gain value corresponds to a lost Ni layer thickness on specimens of the size used in this experiment of 6.11 μm . A value of 6.37 μm calculated using Eq. (21) for the same time indicates excellent agreement.

The technique thus appears to have merit for our investigations, and will be further applied.

C. Corrosion and Mass Transport Phenomena
(R. Feber, J. Lunsford, and W. A. Stark)

The adsorption of elemental fission-product species onto graphite has been taken into account in the computer codes previously used to calculate equilibrium concentrations of various gaseous species due to reactions of the graphite with impurities in the primary coolant and in the presence of fission products. In particular, the adsorption of cesium has been expressed by a Freundlich isotherm and equilibrium concentrations calculated in

the C-H₂-O-Cs system at one temperature and several initial cesium pressures. The effect of the addition of the isotherm is, as intuitively expected, to lower the amount of volatile Cs. The effect on the relative stability of the various cesium species has not yet been determined. Currently the model for adsorption is being elaborated to account for deviations of the observed adsorption of cesium from a Freundlich isotherm at relatively high cesium partial pressures.

The capability to calculate the potentials for reaction as a function of temperature and pressure has been added to the interactive equilibrium code QUIL. As an example, the reaction potential for a reaction in the graphite-water system at a given temperature and pressure may be defined as the logarithm of the ratio of the equilibrium value of (CO₂/CO) when no graphite is present to the equilibrium value in the presence of graphite. This feature will be of value in applying the equilibrium calculations to reactions not assumed to be at equilibrium (particularly those involving the deposition or removal of a solid) by calculating the extent of the departure of the reactions from equilibrium.

The carbon-steam system equilibria have been examined over a range of pressures and temperatures of interest to HTGR Safety efforts. Using the free-energy minimization codes, the equilibrium oxygen pressure (or equivalently, the ratio $R_H = P_{H_2}/P_{H_2O}$) has been calculated for various initial amounts of water in the presence of excess carbon at constant temperature. These results are presented in Table III for three different temperatures. As discussed in earlier reports, these are computations of thermodynamic equilibrium compositions, involving many components. Thus, the effects of minor species on the equilibria are taken into account, something that is not clearly

done, for example, in the analysis of Everett, Kinsey, and Romberg.²¹

Since these values are obtained assuming equilibrium, there is no modifying effect due to kinetics, and the results represent maximum extents of reaction. In particular the oxygen pressures presented here are the minimum to be expected in the reactor environment; if the extent of reaction were inhibited due to kinetics effects the oxygen potential would be higher.

We have also calculated equilibrium oxygen partial pressures for water ingress in the presence of additional hydrogen or carbon monoxide. Such conditions might be representative of the situation during steady steam ingress; the results are presented in part in Tables IV and V.

Additional CO increases the oxygen potential by as much as four orders of magnitude, while additional H₂ depresses the oxygen pressures. At 600 K the effect of added H₂ is nearly comparable to CO, albeit in the opposite direction. However, at 1100 K, the effect of H₂ in depressing the oxygen pressure is quite small. A few runs with both added H₂ and CO suggest that the effect of the CO is overriding; that is, the oxygen pressure is increased over the equilibrium oxygen pressure obtained from a steam only ingress of the same magnitude. The calculations in general suggest a potentially more oxidizing environment than anticipated, and a continuing effort will approach this problem.

A start has been made on the construction of the transpiration experimental apparatus.

TABLE III

EQUILIBRIUM OXYGEN PRESSURES RESULTING FROM STEAM INGRESS

Temperature (K)	Initial Water Concentration (ppmv)	$R_H = \frac{P_{H_2}}{P_{H_2O}}$	P_{O_2} (atm)	$RT \ln P_{O_2}$ (kJ/mol) ²
600	10	2.779	6.95×10^{-39}	-438.3
600	100	0.982	5.56×10^{-38}	-427.9
600	500	0.470	2.43×10^{-37}	-420.5
600	1000	0.339	4.68×10^{-37}	-417.3
800	10	92.98	3.04×10^{-31}	-467.4
800	100	14.34	1.28×10^{-29}	-442.5
800	500	5.23	9.60×10^{-29}	-429.1
800	1000	3.57	2.06×10^{-28}	-424.0
1100	10	22089	3.49×10^{-27}	-557.1
1100	100	2211	3.49×10^{-25}	-515.0
1100	500	445	8.61×10^{-24}	-485.7
1100	1000	224	3.40×10^{-23}	-473.1

TABLE IV

EFFECT OF ADDITIONAL H_2 ON THE EQUILIBRIUM OXYGEN PRESSURE
IN THE CARBON-STEAM SYSTEM

Temperature (K)	Initial H_2O Conc., (ppmv)	Initial Ratio $(P_{H_2}/P_{H_2O})_0$	Equilibrium Log P_{O_2} (P_{O_2} in atm)	Δ Log P_{O_2} (from steam ingress)
600	10	0.1	-38.17	-0.01
		1	-38.28	-0.12
		10	-38.97	-0.82
		100	-40.14	-1.98
	100	0.1	-37.28	-0.02
		1	-37.45	-0.20
		10	-38.20	-0.94
		100	-39.24	-1.98
	1000	0.1	-36.36	-0.03
		1	-36.56	-0.22
		10	-37.28	-0.95
		100	-38.30	-1.97
1100	10	0.1	-26.46	~ 0
		1	-26.46	~ 0
		10	-26.46	~ 0
		100	-26.46	~ 0
	100	0.1	-24.46	~ 0
		1	-24.46	~ 0
		10	-24.46	-0.01
		100	-24.50	-0.05
	1000	0.1	-22.47	~ 0
		1	-22.47	~ 0
		10	-22.51	-0.04
		100	-22.79	-0.32

TABLE V

EFFECT OF ADDITIONAL CO ON THE EQUILIBRIUM OXYGEN PRESSURE
IN THE CARBON-STEAM SYSTEM

Temperature (K)	Initial H ₂ O Conc. (ppmv)	Initial Ratio (P_{CO}/P_{H_2O}) ₀	Equilibrium Log P _{O₂} (P in atm)	Δ Log P _{O₂} (from steam ingress)
600	10	0.1	-38.11	+0.05
		1	-37.80	+0.35
		10	-37.00	+1.16
		100	-36.01	+2.15
	100	0.1	-37.20	+0.06
		1	-36.85	+0.40
		10	-36.00	+1.25
		100	-35.02	+2.24
	1000	0.1	-36.27	+0.06
		1	-35.89	+0.44
		10	-35.01	+1.32
		100	-34.05	+2.28
1100	10	0.1	-26.38	+0.08
		1	-25.86	+0.60
		10	-24.38	+2.08
		100	-22.46	+4.00
	100	0.1	-24.38	+0.08
		1	-23.86	+0.60
		10	-22.38	+2.07
		100	-20.53	+3.93
	1000	0.1	-22.39	+0.08
		1	-21.88	+0.59
		10	-20.46	+2.01
		100	-18.91	+3.56

V. TASK 4, STRUCTURAL INVESTIGATIONS

A. Review and Evaluation of PCRV Design and Failure Criteria (E.G. Endebrock and J. B. Payne)

Reference 22 summarizes our efforts in this area. Other than a continuing review of literature as necessary to maintain a state-of-the-art knowledge of new developments, no further efforts are planned in this area.

B. Triaxial Testing Preparations (E. G. Endebrock and J. B. Payne)

Two test equipment vendors held seminars to acquaint LASL personnel with the state-of-the-art of test systems. These seminars were directed towards test systems for multiaxial testing of concrete and for multiaxial dynamic testing of HTGR cores and core supports. Both test systems are essentially composed of actuators, hydraulic pressure supplies, and control systems. It was determined that only the hydraulic pressure supply could be used in both test systems. The vendors also gave information on operational problems inherent in dynamic testing, capabilities of available control systems, equipment limitations, and costs of comparable systems currently operational.

In addition, a seminar was given by personnel of Swanson Associates. They described the usage and capabilities of their general structural analysis computer program. This computer program appears to be the most advanced structural analysis program currently in existence. It is a proprietary computer program.

C. Analytical Model Development for Core Seismic Response (J. G. Bennett, J. L. Merson, R. C. Dove, and K. H. Duerre)

The development of the analytical models that was reported last quarter has continued. Up to this point, all of the analytical core models have been driven with single frequency harmonic motion. In preparation for the use of more

realistic driving functions, a computer program which generates simulated earthquakes has been obtained and modified for our purposes. This program, SIMEAR, was developed by M. Watabe at the University of California, Berkeley, and obtained through the National Information Services, Earthquake Engineering.

SIMEAR allows the user to specify the shape of the earthquake's frequency spectrum, and its intensity function in the time domain. Also provided by the user are maximum allowable values of acceleration, velocity, and displacement. The program generates an "expected spectrum". Components of this spectrum are then added together with random phase angles to produce a time history. The resulting acceleration, velocity, and displacement time histories are then scaled to the limiting maximum value supplied by the user.

The program also has the capability of calculating the response spectra to the generated "simulated earthquakes" for systems having various amounts of damping.

SIMEAR has been modified to include frequencies up to 40 Hz since the 20-Hz limit imposed in the original version made it impossible to generate spectra which extended to the 33-Hz limit which must be considered in seismic analysis in accordance with the Regulatory Guide.

Film plotting capabilities have also been added to facilitate visual display of the generated earthquakes, and their comparison with the Regulatory Response Spectra. One such plot is given as an example in Fig. 10. The lower piece-wise linear curve is a one "g" ground motion limiting curve. The upper piece-wise linear curve is the Regulatory Response Spectra for one "g" ground motion and 0.5% damping. Also shown are typical response spectra for a system with 0.5%

Fig. 10. Typical response spectra of simulated earthquakes compared with regulatory spectra.

damping, and for a system with 25% damping, which closely approximates ground motion.

A model for the controlled evaluation of inelastic energy loss during impact has been developed. Figure 11 illustrates a typical loading and unloading path during the impact sequence. The area by the loading path represents energy lost during impact. The advantage of this model is that

Fig. 11. Hysteretic internal energy loss model under study.

the energy lost can be specified as a percentage of the energy stored during the loading sequence. In practice this is done by specifying the percentage energy loss which defines the unloading modulus D through the relationship.

$$D = K_B - \frac{\% \text{ Energy Loss}}{100} K_B$$

This model has been used to study the hysteretic effects on the maximum block acceleration as described later in this report.

The one-dimensional model shown in Fig. 12 has been extended to include the effect of the springs on the boundary as shown in Fig. 13. This model will be used parametrically to determine the effect of various types of springs and spring constants.

Fig. 12. One-dimensional model.

Fig. 13. Spring-mounted model.

The two-dimensional model shown in Fig. 14 has been used to illustrate the effect of biaxial (horizontal and vertical) base excitation. These results have been presented in the form of a computer-generated movie. The effect of the friction mechanism in locking the stacks together during upward vertical excitation is quite apparent visually in this presentation. Locating pins and pin clearances are currently being implemented into this model and a limited parameter study is being planned to extend the results from the one-dimensional model.

Finite element studies of the fuel blocks have also been initiated in order to determine the geometric effects of the holes on the block stiffness. These studies are currently limited to modeling the graphite as a linear elastic material and using the plane strain elements. Among the tools developed in conjunction with these studies is a special purpose meshing program which will generate a planar mesh for one fourth of a fuel block.

Fig. 14. Two-dimensional model.

Other finite element studies are being planned for the various core components including a three-dimensional representation of the locating pins for the fuel blocks.

D. Physical Model Test Program for Core Seismic Response (R. C. Dove, J. L. Merson, and J. G. Bennett)

During the present quarter several manufacturers of electrohydraulic test equipment have been contacted as a first step in planning a suitable facility for core model testing.

In preparation for the detailed design of specific physical models a parametric study has been undertaken. The one-dimensional analytical model described in the last quarterly report (see Fig. 12) has been used to investigate the effect which the various parameters have on core block acceleration. The intention was to systematically evaluate the effects that variations in parameters such as block modulus, clearance between blocks, etc. have on the block accelerations developed during core block impact.

The one-dimensional model is characterized by the following functional equation:

$$\ddot{X} = \phi(X_B, t, d, c, W, \mu_s, \mu_k, E, D, g) \quad (24)$$

in which

- $\ddot{X}$ = acceleration of a block
- X_B = amplitude of sinusoidal motion applied to the base
- t = period of input sinusoidal motion or any significant time
- d = characteristic block dimension
- c = clearance between blocks
- W = weight of block
- μ_s = static coefficient of friction
- μ_k = kinetic coefficient of friction
- E = modulus of block, loading
- D = modulus of block, unloading
- g = gravitational constant

This functional equation can be rewritten using dimensionless groups as follows:

$$\ddot{X} = \Psi\left(\frac{X_B}{d}, \frac{c}{d}, \frac{Ed^2}{W}, \mu_k, \frac{\mu_s}{\mu_k}, \frac{E}{D}, \frac{X_B}{gt^2}\right). \quad (25)$$

Designating each dimensionless group as a "Pi" term we can write:

$$\pi_1 = \Psi(\pi_2, \pi_3, \pi_4, \pi_5, \pi_6, \pi_7, \pi_8). \quad (26)$$

Obviously, many other groupings of terms are possible but this set was chosen to make it as easy as possible to vary one group while holding the remaining groups constant.

In making the parameter study all of the terms on the right-hand side of Eq. (24) were assigned values and the "base" value of each dimensionless group (π term) on the right-hand side of Eq. (25) was then computed. (See Table VI.) Computer runs were then made in which the value of one π term was varied from its base value while all of the others were held at their base values.

The first investigation was to determine the effect of variation of static friction (breakaway friction), μ_s . The data shown on Fig. 15 indicate that variation in μ_s has little effect on the block accelerations. As a result of this finding, static friction (μ_s) has been eliminated as a separate independent governing parameter. Figure 16 shows the importance of the clearance gap (c) on block acceleration. It is clear that block acceleration is strongly dependent on clearance. The effect of varying the coefficient of kinetic friction (μ_k) is shown in Fig. 17. The importance of the frictional effect in limiting acceleration is clear.

The effect of energy loss due to inelastic impact was studied by varying π_7 , (E/D). The results, shown in Fig. 18

TABLE VI
BASE VALUES USED IN PARAMETER STUDY

Independent Variable	"Pi" Term (π_n = Base Value)
$X_B = 1.277$ cm	$\pi_2 = 0.03333$
$t_p = 0.2$ s	$\pi_3 = 0.01$
$d = 0.381$ m	$\pi_4 = 2.051 \times 10^6$
$c = 0.381$ cm	$\pi_5 = 0.16$
$W = 975.95$ N	$\pi_6 = 1.25$
$\mu_s = 0.20$	$\pi_7 = 1$
$\mu_k = 0.16$	$\pi_8 = 3.235 \times 10^{-2}$
$E = 13.79 \times 10^6$ N/m ²	
$D = E$	
$g = 9.81456$ m/s ²

Fig. 15. Effect of π_6 .

Fig. 16. Effect of π_3 .

Fig. 17. Effect of π_5 .

indicate that the effect of this internal energy loss on block accelerations is relatively small.

From the dimensional analysis, Eq. (25), we see that the effect of block stiffness (E) and block weight (W) can be investigated simultaneously by varying π_4 , $\frac{Ed^2}{W}$. The results of this investigation are shown in Fig. 19.

The quantity $\pi_8(X_B/gt^2)$, was varied by changing the period of the input motion (t); however, π_8 is proportional to the ratio of base acceleration ($\ddot{X}_B$) to the acceleration of gravity (g) which is treated as a constant. Also we observe that the block accelerations must equal the base acceleration when the coefficient of static friction

Fig. 19. Effect of π_4 .

Fig. 18. Effect of π_7 .

(μ_s) is equal to, or greater than, the base acceleration. Therefore, data from these runs were plotted as shown in Fig. 20. The quantity $\pi_2(X_B/d)$, was then varied while $\pi_8, \frac{X_B}{gt^2}$, was held constant by simultaneously varying t. As a result of this study, X_B varying independently was found to have a relatively smaller effect on block acceleration than the effect which varying X_B has on $\ddot{X}_B$. Data from the runs

Fig. 20. Effect of π_2 .

in which $\pi_2 \frac{X_B}{d}$ was varied are therefore also plotted on Fig. 20 and Eq.(25) is re-written with X_B replacing both X_B and t . Thus:

$$\ddot{X}_g = \psi \left(\frac{\ddot{X}_B}{g}, \frac{c}{d}, \frac{Ed^2}{W}, \mu_k, \frac{E}{D} \right) \quad (27)$$

or

$$\pi_1 = \psi'(\pi_2', \pi_3, \pi_4, \pi_5, \pi_7).$$

It should be emphasized that, as the scatter in the data plotted on Fig. 20 shows, this reduced group of π terms is only an approximation; that is, the response of this model does indeed depend upon the amplitude/frequency content of the acceleration input. However, characterization of the excitation in terms of a single variable (base acceleration) may offer a significant advantage when dealing with mixed frequency inputs.

From the data plots the following four equations can be written. From Fig. 16:

$$\ddot{X} = 600 \left(\frac{c}{d} \times 10^2 \right)^{0.511} \quad (28)$$

Fig. 17:

$$\ddot{X} = 655 - 333 \mu_k \quad (29)$$

Fig. 19:

$$\ddot{X} = 425 \left(\frac{Ed^2}{W} \times 10^{-6} \right)^{0.500} \quad (30)$$

Fig. 18:

$$\ddot{X} = 600 \xi^{-0.026} \quad (31)$$

and from Fig. 20, using the upper bounding curve:

$$\ddot{X} = 670 (\ddot{X}_B - \mu_s)^{0.451} \quad (32)$$

Speculating that the function ψ' in Eq. 27 is a simple product of the functional equations for each π term we can write

$$\begin{aligned} \frac{\ddot{X}}{600} &= \frac{670}{600} (\ddot{X}_B - \mu_s)^{0.451} \times \frac{600}{600} \left(\frac{c}{d} \times 10^2 \right)^{0.511} \\ &\times \frac{425}{600} \left(\frac{Ed^2}{W} \times 10^{-6} \right)^{0.500} \\ &\times \frac{655-333 \mu_k}{600} \times \frac{600}{600} \xi^{-0.026} \quad (33) \end{aligned}$$

where 600 is the value computed for the maximum block acceleration ($\ddot{X}$) when the base values (see Table VI) are substituted for the independent variable.

To illustrate the possible use of this equation, the parameter values listed in Table VII were used as data for the one-dimensional, four-block, analytical model (Fig. 12) and maximum block accelerations were computed. Values obtained from the program runs are compared to values computed using Eq.(33) in Table VII.

Where these three test cases fall in relation to the NRC earthquake frequency spectra is shown in Fig. 21. It is interesting to note that since, with single frequency excitation, no block impact occurs at acceleration levels (in "g" units) less than the value of the static coefficient (μ_s), the actual frequency/amplitude region of interest is considerably limited. For these three test points it is clear that, on either an absolute or percentage basis, maximum block acceleration predicted by Eq. (33) is indeed conservative. The principal reason for this conservatism is the fact that the upper limit of the effect of base acceleration on block acceleration (see Fig. 20) was used in the prediction. This was done to insure that the predicted result will always be conservative for any test condition (frequency and acceleration) that falls within the area of the spectra that is of interest (i.e., the area bounded by points A, B, C, D on Fig. 21.).

TABLE VII
PREDICTIONS FOR $\ddot{X}$, BASED ON EQUATION (33)
Value Assigned

Parameter	Test 1a	Test 1b	Test 1c
$\ddot{X}_B$, g	0.5	0.5	0.5
f^* , Hz	10	3	1
X_B^* , cm	0.1242	1.3813	12.420
μ_k , -	0.100	0.100	0.100
μ_s , -	0.125	0.125	0.125
E , N/m ²	10.09×10^9	10.09×10^9	10.09×10^9
W , N	975.9	975.9	975.9
C , cm	0.1905	0.1905	0.1905
d , cm	38.1	38.1	38.1
ξ , %	0	0	0
D^* , N/m ²	D=E	D=E	D=E
Max $\ddot{X}$ From Eq. (33)		271 g	
Max $\ddot{X}$ From 1D Program	189 g	208 g	207 g

*"f, X_B , and D" are not needed for Eq. (33) but are used in the computer program.

Fig. 21. Test points with respect to regulatory response spectra.

The purpose of this parameter study has been to gain a better physical understanding of the four-block, one-dimensional model in particular and of physical models of the HTGR core in general. An understanding of the relative importance of the various parameters on core block response to harmonic excitation has been obtained. This general approach will be used and extended by means of the additional analytical models that are now available.

E. Preliminary Study of Cross-Flow Induced Vibration of Large HTGR Power Plant Core Support Posts

(R. J. Bartholomew)

1. Introduction

The structural arrangement of the HTGR nuclear core support elements, together with the large helium gas coolant flow rates may combine to create a potential problem involving flow-induced vibration of the support posts due to cross-flow periodic vortex shedding. The classical problem of flow-induced vibration has been investigated in depth by both analysis and experiment. A very complete review of the general theory was presented in 1964 by Marris.²³ Application of analysis and experimental investigative results for heat exchangers was made by Putnam²⁴ in 1964. More recent analytical and experimental investigations^{25, 26} have

contributed significantly to a better understanding of the coupling effects of elastic structures with the flow; however, the complex mechanisms associated with closely spaced elastic structures are still not too well understood.

The scope of the present investigation is limited to a preliminary assessment of the possibility of cross-flow vibrations of the support posts of the General Atomics large HTGR power plant designated as the 1160 MWe (3000 MWt) configuration.^{27,28} A preliminary engineering assessment of the structural and coolant flow configuration of the plant has resulted in the conclusion that, whereas cross-flow vibration excitation would be possible, parallel-flow mechanisms as discussed in Ref. 29 are highly unlikely. This conclusion was drawn from two considerations: (a) the compact multi-annular configuration of the coolant passages in the graphite core blocks is not conducive to core block vibration, (b) helium gas is much less dense than water and would not contain enough parallel flow excitation energy to be considered significant. The present investigation deals with the determination of the likelihood of cross-flow excitations of core support posts due to vortex shedding for three modes of operation of the plant involving coolant flow: 1) normal operation at full load, 2) core auxiliary coolant only with depressurized PCRV, and 3) core auxiliary coolant only with pressurized PCRV. For normal operation at less than full load, the coolant flow rates would be less. A cursory view of the resulting induced vibration at full load indicated that with reduced coolant flow, the region of vibration sensitivity would be moved outward towards plane 6, (Fig. 22) thus decreasing the area of the sensitive annular region. Thus, the full load condition has the greatest flow-induced vibration sensitivity.

Fig. 22. Plan view of core, coolant flow net for normal operation and core support post arrangement.

2. Method of Analysis

a. Basic Assumptions

(1) The complex three-dimensional flow problem between the exit plane of the core and the coolant discharge openings is not rigorously solved from fluid dynamic considerations. It is assumed that the flow is radial and outward to quasi-stagnation points of certain regions between discharge openings that are assumed to be adequately described by eddy flow (Figs. 22 and 23). The flow then contracts smoothly to the areas of the discharge openings. There is assumed free communication from the core exit plane to the discharge openings of core elements even in the region of the assumed eddy flows. This assumption is necessary to preserve flow continuity. The eddy boundaries (or quasi-stagnation lines) are assumed to be described by circular arcs tangent to each side of adjacent exit openings in the direction of the flow. No attempt is made to estimate flow-induced vibrations in the eddy flow regions.

Fig. 23. Plan view of core, coolant flow net for auxiliary cooling operation and core support post arrangement.

Equipotential surfaces considered to represent flow areas are assumed to be described by circular cylinders at locations outward from the center hexagonal bundle of seven blocks to points of tangency with the eddy boundaries, after which the equipotential surfaces are assumed to be approximately normal to the main direction of one-dimensional flow out of the exit openings. To allow for the 90° flow direction change from a vertical to radial direction, the lengths of the cylindrical flow areas are reduced to 1.83 m from 2.13 m. These flow areas are assumed to undergo a smooth transition between the rectangular cylindrical sections near the transition regions of radial flow to the circular cross-sections of the exit openings. These transition flow areas are estimated graphically.

(2) The perfect gas law is assumed to be the appropriate equation of state for the helium coolant at all temperatures and pressures even during flow processes.

(3) The effective flow coefficient of discharge (C_D) in all regions is assumed to be 0.7. This is somewhat arbitrary, but conservative in that higher flow velocity is predicted, which gives vortex shedding frequencies closer to the fundamental lateral vibration frequency of any post. According to Ref. 30, p. 123, a value of 0.82 for C_D has been found for a similar sharp-edged flow configuration.

(4) No pressure effects are included in the calculations of kinematic viscosity of the helium gas, but temperature effects are included.

(5) The Strouhal number is assumed invariant with Reynolds number (Re) at a value of 0.21 (Ref. 24 and 26). In the range of $5 \times 10^5 \leq Re \leq 6 \times 10^6$ the vortex shedding frequency is highly speculative.²⁶

(6) Because the support posts are closely clustered in groups of three, some indeterminance of the vortex shedding frequency is inherent. Hence, the calculations are made for two cases: (a) single posts and (b) three posts acting as a single unit. This should bound the results between extremes (see Fig. 22).

(7) Each post is considered as a pinned-pinned uniform beam of 0.1524-m diam and 2.13 m in length for the purpose of calculating the fundamental lateral vibration frequency. The effects of core weight in terms of an axial load of 56 047 N (12 600 lb.) as given in Ref. 31, and the low pre-stress of 3,073 kPa (446 psi) are such that ignoring the effect of axial load causes an error of less than 3% in the fundamental lateral vibration frequency. At this stage of the analysis such an error is ignorable.

b. Analytical Procedure

For the operating conditions and configuration data,^{27,28} the coolant density was calculated from the perfect gas law in the form:

$$\rho_H = \frac{P}{gRT} \quad (34)$$

where

$$\begin{aligned} \rho_H &= \text{Helium density (Kg/m}^3\text{)} \\ p &= \text{Gas pressure (N/m}^2\text{)} \\ R &= \frac{1546 \times .3048 \times 1.8}{4.0} = 212 \text{ (m/K)} \\ T &= \text{Gas absolute temperature (K)} \\ g &= \text{Acceleration due to gravity} \\ &\quad (9.807 \text{ m/s}^2) \end{aligned}$$

From a nomograph of transport properties of various gases³² the viscosity (μ) of helium at various operating conditions was determined. Pressure effects above one atmosphere were not included, but data for air indicates a relative insensitivity to pressure. The kinematic viscosity (ν) was then determined using the previously calculated density ρ . Reynolds number (Re) was then determined by the well-known relationship:

$$Re = \frac{UD}{\nu} \quad (35)$$

where

$$\begin{aligned} U &= \text{Flow velocity (m/s)} \\ D &= \text{Post individual diameter or outside dimension of isosceles triangle of cluster of three posts (m)} \\ \nu &= \text{Kinematic viscosity (m}^2\text{/s)} \end{aligned}$$

The plan views of the core and indicated locations of support posts are as shown in Figs. 22 and 23. Figure 22 also shows the basics of the flow pattern assumed for the normal operating condition utilizing six exit orifices. Figure 23 shows the basics of the flow pattern assumed for the core auxiliary cooling operating condition appropriate for both the unpressurized PCRV case and the pressurized PCRV case. Figure 24 shows an estimated flow area of plane 6 for the normal operating case. At the several flow "planes", the flow areas, post profile areas, numbers of hexagonal

Post Shading Area = 1.932 m^2 for each of six exit planes.

Exit Plane Area without post shading = 3.345 m^2 each of six exit planes

(Plane five was approximated as consisting of six rectangles 2.743 m by 1.829 m minus the post area of 63 posts each with dimensions of 0.1524 m by 1.829 m)

Fig. 24. For normal operation, the exit plane area at plane six reduced by support post shading.

bundles, and flow rates at each "plane" were determined based on geometry data and coolant flow data available.^{27,28} (This geometry data is relatively crude at this stage.)

Next, the mean velocity of flow at each "plane" was obtained by dividing the flow rate $Q(\text{m}^3/\text{s})$ by the net effective flow area (0.7 times the difference between actual area and post profile shading area). The Reynolds number was then calculated for each of the conditions:

- single post, diameter 0.1524 m , and
- cluster of three posts, outer dimension 0.508 m . The vortex shedding frequency was then determined for the above two conditions by the following equation:

$$f = \frac{0.21 U}{D} \quad (36)$$

f = Vortex shedding frequency (Hz)

U = Flow mean velocity (m/s).

The Strouhal number is taken to be numerically equal to 0.21 in Eq. (36). According to Ref. 26 this is appropriate for all Re in the range $500 < Re < 5 \times 10^5$. During normal operation there was only one condition for the single post calculation and three conditions for the three post cluster calculation where the Re were outside that range.

As discussed in the results, the vortex shedding frequencies are speculative for these conditions.

The fundamental structural natural frequency of a single post was determined from the well-known equation:³³

$$f_s = \frac{K^2}{2\pi} \sqrt{\frac{EI}{A \rho_s}} \quad (37)$$

where

f_s = Structural frequency (Hz)

K = 1.472 for pinned-pinned beam
($Kl = \pi$)

l = 2.134 (m)

ρ_s = Density of graphite (1575 Kg/m^3)

A = Cross-sectional area of graphite column (0.01824 m^2)

E = Young's modulus for graphite
($E = 1.724 \times 10^{10} \text{ N/m}^2$)

I = Cross-sectional area moment of inertia of graphite column
($2.648 \times 10^{-5} \text{ m}^4$)

The result of Eq. (37) gives a value of $f_s = 43.5 \text{ Hz}$.

The ratio of vortex shedding frequency (f) to structural frequency (f_s) was then determined. According to Ref. 25, undesirable vibration occurs at resonance ($\frac{f}{f_s} = 1.0$) with some persistent vibration occurring in the range $0.7 \leq \frac{f}{f_s} \leq 1.75$. The probable occurrence of such ranges is pointed out in the results.

3. Results and Conclusion

a. Configuration Data

Three configuration cases were studied to determine flow-induced cross-flow excited vibration of the core support posts. Table VIII gives the basic and derived data for the helium coolant for each of these conditions.

Table IX shows the support post configuration flow area "plane", and geometry data used for the Normal Operation Condition (full load). Table X shows the same information for the Core Auxiliary Cooling Operation Depressurized PCRV

TABLE VIII

COOLANT DATA FOR 1160 MWe (3000 MWt) HTGR

Configuration	No. of Outlets	Outlet Diameter (Each Outlet)	Coolant Absolute Outlet Pressure	Coolant Temperature	Coolant Density	Coolant Flow Rate	Coolant Kinematic Viscosity
	N	D_o (m)	p (kPa = 10^3 N/m^2)	T (K)	ρ (Kg/m ³)	Q (m ³ /s)	ν (m ² /s)
Normal Operation	6	1.816	4928	1014	2.338	605.5	1.866×10^{-5}
Auxiliary Cool- ing Operation							
a. Depressurized PCRV	3	1.689	157.9	469	0.1619	236	1.607×10^{-4}
b. Pressurized	3	1.689	4824	566.5	4.0958	12.83	7.209×10^{-6}

TABLE IX

COOLANT FLOW CONFIGURATION DATA - NORMAL OPERATION FULL LOAD

Flow "Plane" Number	Diameter or Length (m)	Area (m ²)	No. of Posts in stream	Post Area (m ²)	Net Flow Area (m ²)	No. of Hex Bundles	Flow Rate/Hex (m ³ /s)	Total Flow Rate (m ³ /s)
1	1.015	5.831	18	5.017	0.814	1	7.12	7.12
2	2.621	15.059	27	7.525	7.534	6	42.74	49.9
3	4.328	24.869	45	12.542	12.327	12	85.48	135.3
4	5.974	34.327	63	17.558	16.769	18	128.21	263.6
5	6 at 2.743 = 16.459 by 1.829	30.100	63	17.558	12.542	24	170.97	434.5
6	6 openings Fig. 22 & Fig. 24	20.07	See Fig. 24	11.592	8.478	24	170.97	605.5

TABLE X
COOLANT FLOW CONFIGURATION DATA - AUXILIARY COOLING OPERATION DEPRESSURIZED PCRV

Flow "Plane" Number	Diameter or Length (m)	Area (m ²)	No. of Posts in stream	Post Area (m ²)	Net Flow Area (m ²)	No. of Hex Bundles	Flow Rate/Hex (m ³ /s)	Total Flow Rate (m ³ /s)
1	1.015	5.831	18	5.017	0.814	1	2.78	2.78
2	2.621	15.059	27	7.525	7.534	6	16.66	19.4
3	3 at 3.658 = 10.973 x 1.829	20.07	36	10.03	10.04	12	33.32	52.8
4	3 at 2.743 = 8.23 x 1.829	15.05	36	10.03	5.02	18	49.98	102.7
5	3 at 2.286 3 at av. diam = 2.057	9.97	21	5.85	4.12	24	66.63	169.4
6	3 at 1.981 3 at av. diam = 1.905	8.55	12	3.34	5.21	24	66.63	236.0
7	3 at 1.829 diam	7.88	8	2.23	5.65	0	0.0	236.0

and Table XI for the Auxiliary Cooling Operation Pressurized PCRV. The "number of hex bundles" column specifies the number of each bundle of seven core elements passed through between "planes" in progressing from "plane to plane".

b. Derived Flow-Induced Vibration Data

Using a discharge coefficient (C_D) of 0.7, the flow velocity (U) was calculated by dividing the total volumetric flow rate at each numbered "plane" by the net flow area at that plane times the discharge coefficient. From this velocity, and the values of kinematic viscosity (ν) taken from Table VIII, the Reynolds number (Re) was calculated for both the single post configuration and the cluster of three posts acting as a single unit. Equation (35) was used in this calculation.

The vortex shedding frequencies (f) were then calculated for both single and triple post configurations using Eq. (36). Then, the ratio of (f/f_s) was calculated using the fundamental single post structural bending frequency (f_s) as determined by Eq. (37). The value of (f_s) was found earlier to be 43.5 Hz. Table XII contains these final results for the Normal Operation Configuration, Table XIII for the Auxiliary Cooling Operation - Depressurized PCRV Configuration, and Table XIV for the Auxiliary Cooling Operation - Pressurized PCRV Configuration.

c. Conclusion

From the above results it is concluded, if we assume that unacceptably high cross-flow-induced vibrations can occur for the range $0.7 \leq f/f_s \leq 1.75$ (Ref. 25), then there will exist a potential flow-induced vibration problem from "plane" 4 outward during the Normal Operation Condition with the single post assumption. If each set of three posts responds as a unit, the vibration problem will exist only for the outermost support posts "plane" 6. For the Auxiliary

Cooling Operation Depressurized PCRV Configuration, and for single post excitation, induced vibrations are predicted in the region bounded by "plane" 4 and "plane" 5. For all other conditions and three-post assumptions there will be no expected cross-flow-induced vibrations.

4. Recommendations

In view of the preliminary analysis of support post cross-coolant flow-induced vibration sensitivity in the GA large HTGR power plant designated as the 1160 MWe (3000 MWt) configuration the following recommendations are made:^{27,28}

(1) A more refined theoretical flow analysis should be conducted in which not only the main three-dimensional flow net is determined, but also the eddy flow regions should be investigated to determine irrotational vortex generation.

(2) Methods of insuring that each three-post cluster behaves as a unit for vortex shedding and for increasing the fundamental structural frequency should be investigated. This would probably require that flow paths between the posts in a cluster be closed off and/or structural ties between posts be considered.

VI. TASK 6, SAFETY INSTRUMENTATION AND CONTROL SYSTEMS

A. Primary Coolant Moisture Monitors
(E. J. Dowdy, and G. W. Taylor)

The prototype fast flowing helium afterglow device has been calibrated for a single set of flow conditions over a moisture content range of 0.22- to 1400-Vpm. The calibration data are shown in Fig. 25. The flowrates of 2000 $\mu\text{mols/s}$ for the metastable bearing stream (discharge) and 175 $\mu\text{mol/s}$ for the impurity-laden stream (reagent) are typical - variations may be desirable to displace the dynamic range from that displayed in the figure. The apparent "saturation" above 2000 Vpm will be examined further. The full curve in the figure represents a least squares linear fit to an apparent power law

TABLE XI

COOLANT FLOW CONFIGURATION DATA - AUXILIARY COOLING OPERATION PRESSURIZED PCRV
 (SINGLE POST DIAMETER = 0.152 m; THREE-POST DIAMETER = 0.508 mf; $f_s = 43.5$ Hz)

Flow "Plane" Number	Diameter or Length (m)	Area (m ²)	No. of Posts in stream	Post Area (m ²)	Net Flow Area (m ²)	No. of Hex Bundles	Flow Rate/Hex (m ³ /s)	Total Flow Rate (m ³ /s)
1	1.015	5.831	18	5.017	0.814	1	0.15	0.15
2	2.621	15.059	27	7.525	7.534	6	0.91	1.06
3	3 at 3.658 = 10.973 x 1.892	20.07	36	10.03	10.04	12	1.81	2.87
4	3 at 2.743 = 8.23 x 1.829	15.05	36	10.03	5.02	18	2.72	5.59
5	3 at 2.286 3 at av. diam = 2.057	9.97	21	5.85	4.12	24	3.62	9.21
6	3 at 1.981 3 at av. diam = 1.905	8.55	12	3.34	5.21	24	3.62	12.83
7	3 at 1.829 diam	7.88	8	2.23	5.65	0	0.0	12.83

TABLE XII

COOLANT CROSS-FLOW-INDUCED VIBRATION PREDICTION - NORMAL OPERATION FULL-LOAD CONDITION
 (SINGLE POST DIAMETER = 0.1524 m; THREE-POST DIAMETER = 0.508 m; $f_s = 43.5$ Hz)

Flow "Plane" Number	Mean Flow Velocity (U) (m/s)	$Re = \frac{UD}{\nu}$		Vortex Shedding Frequency (f) $f = 0.21 U/D$ (Hz)		Ratio to Structural Frequency f/f_s	
		Single Post	Three Posts	Single Post	Three Posts	Single Post	Three Posts
1	12.49	1.01×10^5	3.36×10^5	17.2	5.2	0.395	0.120
2	9.45	7.64×10^4	2.55×10^5	13.0	3.9	0.299	0.090
3	15.68	1.27×10^5	4.22×10^5	21.6	6.5	0.497	0.149
4	22.45	1.81×10^5	$*6.05 \times 10^5$	30.9	9.3	0.710	0.214
5	49.49	4.0×10^5	$*1.33 \times 10^6$	68.2	20.5	1.57	0.471
6	102.3	$*8.27 \times 10^5$	$*2.76 \times 10^6$	141	42.3	3.24	0.972

* For these Re, f is highly speculative.

TABLE XIII

COOLANT CROSS-FLOW-INDUCED VIBRATION PREDICTION - AUXILIARY COOLING OPERATION DEPRESSURIZED PCRV

Flow "Plane" Number	Mean Flow Velocity (U) (m/s)	$Re = \frac{UD}{\nu}$		Vortex Shedding Frequency (f) $f = 0.21 U/D$ (Hz)		Ratio to Structural Frequency f/f_s	
		Single Post	Three Posts	Single Post	Three Posts	Single Post	Three Posts
1	4.86	4.61×10^3	1.54×10^4	6.7	2.01	0.154	0.046
2	3.69	3.49×10^3	1.16×10^4	5.08	1.52	0.117	0.035
3	7.51	7.12×10^3	2.37×10^4	10.35	3.10	0.238	0.071
4	29.25	2.77×10^4	9.24×10^4	40.3	12.09	0.926	0.278
5	59.18	5.61×10^4	1.87×10^5	81.55	24.47	1.87	0.563
6	64.79	6.14×10^4	2.05×10^5	89.28	26.29	2.05	0.604
7	59.48	5.64×10^4	1.88×10^5	82.0	24.59	1.89	0.565

TABLE XIV

COOLANT CROSS-FLOW-INDUCED VIBRATION PREDICTION - AUXILIARY COOLING OPERATION PRESSURIZED PCRV

Flow "Plane" Number	Mean Flow Velocity (U) (m/s)	$Re = \frac{UD}{\nu}$		Vortex Shedding Frequency (f) $f = 0.21 U/D$ (Hz)		Ratio to Structural Frequency f/f_s	
		Single Post	Three Posts	Single Post	Three Posts	Single Post	Three Posts
1	0.265	5.59×10^3	1.86×10^4	0.365	0.109	0.008	0.003
2	0.200	4.23×10^3	1.41×10^4	0.276	0.0828	0.006	0.002
3	0.408	8.63×10^3	2.88×10^4	0.563	0.169	0.013	0.004
4	1.59	3.36×10^4	1.12×10^5	2.19	0.658	0.050	0.015
5	3.22	6.80×10^4	2.27×10^5	4.44	1.33	0.102	0.031
6	3.52	7.45×10^4	2.48×10^5	4.86	1.46	0.112	0.033
7	3.23	6.84×10^4	2.28×10^5	4.46	1.34	0.102	0.031

Fig. 25. Calibration data.

behavior. The least squares fit equation is

$$M = 0.516I^{1.23} \quad (38)$$

where M is the moisture content, in Vpm, and I is the photomultiplier tube current, in nA.

A more compact and hardened version of the helium afterglow moisture is being constructed. This device will be used on line at the Ft. St. Vrain reactor.

B. Application of Monitor to Other Coolant Impurities (E. J. Dowdy and G. W. Taylor)

We have begun testing the fast flowing helium afterglow device for suitability as a monitor for other expected impurities. Only semi-quantitative results have been obtained thus far. Sensitivity to CO and CO₂ is predicted to be easily below the 10 Vpm range of the single test thus far. N₂ sensitivity is expected to be an order of magnitude lower than that for CO and CO₂. Sensitivity for CH₄ is somewhere in the neighborhood of the 20 Vpm range of the test. Competition among the various impurities for the available metastable helium atoms remains to be determined.

C. Temperature Sensors (V. Starkovich)

A preliminary examination of several alternate temperature measuring techniques has begun, with initial emphasis being placed on the suitability of melt wires and Kryptonates* for fuel temperature measurements. Alternate primary coolant temperature sensors, such as Johnson Noise thermometers and ultrasonic resonance and pulse methods will be considered in future progress reports.

Fuel temperature measurements provide valuable information needed to better estimate fuel failure thresholds and the migration of fission products into the primary coolant. Among the methods considered to date for fuel temperature measurements, melt wires and Kryptonates seem to offer the most promise. Temperature-sensitive paints have been found to be unsuitable for use in gas-cooled reactors due to flaking, powdering, indistinct indications, and indicated temperatures considerably at variance with the label. These poor results were probably due to the presence of the inert helium coolant or to a reaction with the graphite to which they were applied. In contrast, melt wires have been found³⁴ to be simple, easy to construct, and reliable. In addition, they do not displace a large volume of fuel, allowing ~ 200 temperature measurements per fuel element to be obtained at the expense of ~ 10⁻²% of the fuel. Because they are passive they do not interfere with the normal operation of the reactor.

The disadvantages of melt wires are that they only provide the maximum temperature experienced by the fuel and, because they are passive, their use is relatively time consuming. When melt wires are being used in a given fuel stack, it would be useful to record the core exit gas temperature for that region as well as the fission product level in the coolant for

* A registered trademark of Panametrics, Inc.

reference during data analysis. For an experiment in a test reactor, the fuel would be instrumented both actively and passively.

Kryptonates³⁵ have advantages and disadvantages similar to those of melt wires. At elevated temperatures these materials (generally carbides of Nb or Zr into which Kr⁸⁵ has been impregnated), release a fraction of their β -active Kr⁸⁵. At still higher temperatures a further release occurs. Thus, the maximum temperature experience by the impregnated material can be determined by removing the sample from the reactor and reheating it to the point of Kr⁸⁵ release. The method is applicable³⁶ for NbC, SiC, and composites consisting of TaC/NbC (50/50) for temperatures up to 1200°C. The technique is accurate to $\pm 5^\circ\text{C}$, with the samples occupying the same volume as do melt wire capsules (0.25-mm diam x 12 mm long). Additional work and coordination with the fission product transport workers would be needed to determine the feasibility of using the naturally produced Kr⁸⁵ in HTGR fuel as an indication of the maximum fuel temperature. For meaningful measurements, the transmutation of Kr⁸⁵ into Kr⁸⁶ would also have to be considered.

Thus, although melt wires and Kryptonates are less than ideal for fuel temperature measurements, both methods warrant further consideration for use on large power reactors. Useful fuel failure measurements could be performed on such reactors by intentionally varying the coolant flow in a given fuel region that has been passively instrumented with Kryptonates and/or melt wires. Prior to such tests, calculations would be performed to predict important processes such as the release of fission products and the amount of poisoning due to the presence of the temperature-sensing capsules.

VII. TASK 7, ACCIDENT DELINEATION

(M. G. Stevenson)

As part of the HTGR safety research effort, the accident delineation task identifies and provides descriptions of significant accident sequences. An initial step in this task was a review and summary cataloging⁶ of the vendor (General Atomic Company) accident analysis presented in the Fulton Generating Station (FGS) Preliminary Safety Analysis Report (PSAR).³⁷ We are currently reviewing two more recent vendor reports.^{38,39} The GASSAR document³⁹ is a Preliminary Safety Analysis Report for a standardized, six-loop nuclear steam supply system essentially identical to the FGS.

The next step in the accident delineation task is the selection of a procedure for identifying safety-significant fault conditions and describing (delineating) accident sequences resulting from or including these fault conditions. By far the majority of significant accidents will either have been identified by the vendor or can be identified by a review of the system characteristics. However, one of the objectives is to achieve completeness of the accident set, i.e., not to overlook a significant accident source or sequence. This then requires a relatively methodical procedure.

We have reviewed the (Rasmussen) Reactor Safety Study, WASH-1400 (draft),¹⁰ with regard to the use of its methodology. The Rasmussen study is comprehensive and thorough, and its use of accident event trees as a means of both studying and presenting accident sequences is helpful. The concurrent use of fault trees to evaluate major subsystem or component failure events which feed into the event trees also tends to optimize the use of the two tree methodologies. A different but also complementary use of the two tree techniques is as a means of iterating between the two kinds of logic. This helps in assuring completeness of the accident

set, and to illustrate this we show two simple examples for an FGS type HTGR. Suppose we choose a design-basis depressurization through a 645 cm^2 (100-in.²) opening as an accident of interest. Figure 26 shows a simple event sequence starting from this and assuming two subsequent branch events. The expected path for this accident lies along the top line, since the system is designed such that cooling is supplied by the main helium circulators driven by steam from the auxiliary boilers as in a normal shutdown case. If we arbitrarily assume failure to supply main loop cooling then the resulting lower branch allows two further possibilities. The expected path here would be for decay heat removal to be accomplished by the core auxiliary cooling system (CACS), resulting in a safe shutdown. If we postulate failure of the CACS, however, then the likely result is gradual heatup and eventual release of fission products from the core. With an assumed leakage path (the accident initiator in this case) then this could result in release of fission products to the containment. This condition is evaluated by GAC as the Maximum Hypothetical Fission Product Release (MHFPR) Accident,³⁹ although GAC does not ascribe this final condition (adiabatic heatup with the primary system open to the containment) to a mechanistic sequence as we have here. With proper operation of the containment cleanup

Fig. 26. Design basis depressurization accident event tree.

system the accident consequences are within regulatory guidelines. In delineating extended sequences, however, we would consider a branch point given by containment cleanup system success or failure.

To continue to follow the methodology used in the Rasmussen study,¹⁰ one would use fault tree methods to develop probabilities of failure of the major subsystems, i.e., the main-loop cooling system, the CACS, and the containment cleanup system. With a given probability of the accident initiator and assuming that subsequent failures were independent, one could calculate a probability of each of the consequences. One of the acknowledged difficulties of this approach is that failures may not be independent, i.e., that common mode failures may exist. Severe environmental disturbances could be a source of such failures in diverse subsystems. For example, an earthquake more severe than the safe shutdown earthquake, although of extremely low probability, could result in piping failures such that all cooling would be lost. Once identified, inclusion of probabilities allowing for this type of common mode failure would be straightforward. Other types of common mode failures are often more difficult to identify and address, such as a design deficiency leading to common failure of a set of identical but presumably independent components. Although there is no way to assure that all common mode failures have been identified (this is a subset of the "overlooking something" problem), the Rasmussen study attacked the problem directly. By making searches in different directions followed by redundant independent searches, considerable confidence was achieved that all significant common failure modes had been found.

Event tree diagramming is indeed straightforward and provides a clear view of the accident sequence. It is not so

useful for developing the initial logic of possible accident paths. To develop successfully an event tree sequence, one needs some prior knowledge of significant subsystem failures. Fault tree logic provides a complementary tool for the initial development of significant sequences. Figure 27 shows a diagrammed fault tree in which we have chosen the "top" event (the final undesired event) as that of loss of forced cooling. Beginning from this and working back, we determine the conditions necessary and/or sufficient to result in the top event. Since loss of both main loop and CACS cooling is necessary (loss of either is not sufficient), we have an "and" gate leading back to these two conditions. We can identify several sufficient conditions for loss of main-loop cooling, and an "or" gate leads back to four examples. Each of the first two, loss of all main circulators and loss of all feedwater flow, is sufficient in itself, while the second two, a DBDA and a loss of off-site power concurrent with a turbine trip, require a simultaneous failure of the auxiliary steam supply system to result in loss of main loop cooling. (This should be shown as an "and" gate but is simplified here). Figure 27 also identifies three

Fig. 27. Loss of forced cooling accident fault tree.

sufficient conditions for loss of CACS cooling. This system can be both automatically and manually actuated, but the possibility remains that it is not actuated. A second is that the system is actuated, but does not start. A third possibility is that the startup is delayed until system temperatures become high enough that the CACS fails on or shortly after startup. To develop the logic further would require identification of the conditions required for each of these failures.

We will use the combination of fault and event tree logic and diagrams in an iterative manner to continue the accident delineation task. Work will concentrate on identifying conditions and sequences which lead to loss of forced cooling, although other accidents will be included. An immediate goal is to define situations for which additional analysis methods are needed. As methods become available calculations of delineated sequences will be made. Although we recognize that a complete loss of forced cooling is of very low probability, it may still be a significant contributor to overall risk. The complete evaluation of a loss of forced cooling accident sequence will, as noted earlier, require delineation beyond initial core heatup to determine the different sequences and consequences resulting from further postulated system failures (the containment cleanup systems, for example).

VIII. TASK 9, PHENOMENA MODELING AND SYSTEMS ANALYSIS

A. HTGR Neutronic Analysis (J. C. Vigil)

1. Power Distributions for the FGS Initial Core

Radial and axial power distributions for the Fulton Generating Station (FGS) initial core at design conditions were calculated using one-dimensional models of the reactor. Results of the calculations are generally consistent with

information available in the FGS PSAR.⁴⁰ The calculations were performed with the DTF-IV transport theory code⁴¹ in the S_4 approximation. Cross sections for appropriate temperatures and compositions were generated with the INTERP interpolation code from the 9-group (5 fast, 4 thermal) MICROX library supplied by GAC.

The one-dimensional calculations were also used to collapse the cross sections from 9 groups to 4 groups (3 fast, 1 thermal), which were then used in a two-dimensional (R-Z) calculation with the TWOTRAN-II transport theory code.⁴² Axial and radial power distributions from the two-dimensional model agree well with results obtained from the one-dimensional models.

a. One-Dimensional Radial Power Distribution

The one-dimensional (cylindrical) model used to calculate the gross radial power profile is described in Table XV. Zone 1 includes the central refueling region, Zone 2 the ring of 6 refueling regions around Zone 1, and Zone 3 the next ring of 12 refueling regions. (A refueling region consists of a control fuel column and the six surrounding standard fuel columns). Zone 4 includes the remainder of the active core excluding a thin buffer region (Zone 5) which contains a lighter uranium loading and a heavier thorium loading than the rest of the core to reduce the power spike near the core-reflector interface. Material compositions of the radial zones are volume-weighted averages over the axial and azimuthal directions. Except for the buffer region, a uniform radial and azimuthal fuel loading is used in the initial core.

At the beginning of the initial cycle, control rods are present in the central refueling region (Ring 1) and in 6 of the 12 refueling regions in Ring 3. These control rods were homogenized into their respective radial zones

TABLE XV
ONE-DIMENSIONAL RADIAL MODEL (FGS)

Zone	Description	Outer Radius (m)
1	Core Ring 1	50.3
2	Core Ring 2	133.0
3	Core Ring 3	219.2
4	Core Ring 4	407.5
5	Core Ring 5	421.9
6	Radial Reflector	524.8

using self-shielding factors obtained from an auxiliary cell calculation. Burnable-poison pins containing B_4C in a graphite matrix are used to reduce the excess reactivity that must be compensated by control rods and also to help control the overall power distribution. As was the case for the control rods, self-shielding factors from an auxiliary cell calculation were used to homogenize the burnable-poison pins into the five radial core zones.

Microscopic cross sections for the core materials correspond to core-average compositions and temperatures (carbon-to-uranium atom ratio = 4920, thorium atom density in the fuel stick = 0.00142 atoms/b-cm, fuel temperature = 1160 K, and moderator temperature = 1010 K). For the radial reflector the microscopic cross sections correspond to a graphite temperature of 710 K.

Four different calculations were performed with the radial model as described in Table XVI. Comparison of the multiplication factors for the various cases yields the following worths for equilibrium Xe and Sm, seven control-rod pairs, and burnable poison:

<u>Component</u>	<u>Worth (ΔK)</u>
Equilibrium Xe and Sm	-0.039
7 Control-Rod Pairs	-0.037
Burnable Poison	-0.099

TABLE XVI
ONE-DIMENSIONAL RADIAL CALCULATIONS

Case	Description	k_{eff}
1	Reference calculation with burnable poison, 7 control-rod pairs, and equilibrium Xe and Sm included.	1.025
2	Same as 1 but without Xe and Sm.	1.064
3	Same as 1 but without control rods.	1.062
4	Same as 3 but without burnable poison.	1.161

Of the total worth for equilibrium Xe and Sm, 85% or $-0.033 \Delta k$ is due to Xe. This value is in good agreement with the equilibrium Xe worth of $-0.032 \Delta k$ given in the FGS PSAR. The PSAR indicates that the initial burnable-poison loading is worth $-0.097 \Delta k$ at 300 K, but its worth at operating temperature is not available. The worth of the 7 control-rod pair configuration is not given in the PSAR.

Normalized gross radial profiles, $P(r)/\bar{P}$ where $\bar{P}$ is the volume-weighted average, are shown in Fig. 28 for the various cases. Removal of Xe and Sm has little effect on the radial profile and therefore a single curve has been used to represent results for Cases 1 and 2. The control rods in Rings 1 and 3 have a large effect on the gross radial profile and result in a flatter radial power distribution. Radial zoning of the burnable-poison pins has a similar but much smaller effect.

Case 1 corresponds approximately to the hot critical configuration at the beginning of the initial cycle (BOIC). Except for fuel depletion effects, Case 4 is representative of the configuration at the end of the initial cycle (EOIC). Figure 28 shows that the gross radial peaking factor, $P_{max}/\bar{P}$, is 1.33 at BOIC and 1.71 at EOIC. The EOIC value may actually be smaller than 1.71 because the more rapid

Fig. 28. Normalized gross radial power distributions for the reference core (Case 1), without Xe and Sm (Case 2), without control rods in Rings 1 and 3 (Case 3), and without control rods or burnable poison (Case 4).

fuel depletion in the high-power regions will tend to flatten the radial power distribution. In addition, one would expect subsequent fuel cycles to have smaller gross radial peaking factors than the initial cycle because more detailed radial zoning is used in reload segments. The PSAR gives a maximum (over all cycles) gross radial peaking factor of 1.6. Our results for the initial cycle appear to be consistent with this value.

The overall radial peaking factor is required to calculate hot-channel

temperatures including the maximum fuel temperature. In addition to the gross peaking factor, the overall peaking factor includes local peaking effects. In the initial core the most important of these local effects is the power tilt within a refueling region. This intra-refueling-region power tilt is due primarily to the fact that the central control contains less fuel than each of the 6 surrounding standard fuel columns (there are 76 fuel sticks in a control element and 132 in a standard element). The power tilt within a refueling region is further aggravated if a control-rod pair is present in the control column.

A two-region (cylindrical) cell model of a refueling region was used to evaluate the intra-refueling-region power tilt. Calculations were performed both without and with the control-rod pair present in the control column. In the latter case the control-rod pair was homogenized into the control column taking into account self-shielding in the rods. These calculations yielded a local peak/average value of 1.16 with the control-rod pair present and 1.12 if the rod pair is not present.

Another local effect is the power distribution near a burnable-poison pin. This effect, which was evaluated with an appropriate cell model, results in a local peak/average value of 1.01. Thus we obtain a maximum local radial peaking factor of 1.17 (1.16×1.01). If we apply this factor to the gross peaking factor of 1.71 for the EOIC, we obtain a maximum overall radial peaking factor of 2.00 (1.71×1.17) for the initial cycle.

In subsequent fuel cycles another local effect that must be taken into account is the azimuthal variation in the fuel loading due to differences in the fuel age. At the end of the equilibrium cycle, for example, the four core segments will have ages ranging from one

year for the most recently loaded segment to four years for the oldest segment. This age-peaking effect increases the local peaking factor; however, the overall radial peaking factor is probably not larger than that for the initial cycle because the gross peaking factor is expected to be smaller as explained previously. The PSAR gives a maximum (over all cycles) overall radial peaking factor of 2.0. Our results for the initial cycle are consistent with this value.

b. One-Dimensional Axial Power Distribution

A one-dimensional (slab) axial model, in which material compositions were volume-averaged in the transverse (R- θ) plane, was used to compute the average axial power distribution. The model includes three axial fuel zones and top and bottom reflectors as shown in Table XVII.

As in the radial model, microscopic cross sections for the core materials correspond to core-average compositions and temperatures. However, microscopic cross sections for the top and bottom reflectors correspond to graphite temperatures of 610 K and 1030 K, respectively. Thus the top and bottom

TABLE VII
ONE-DIMENSIONAL AXIAL MODEL (FGS)

Zone	Description	Lower Boundary (cm)
1	Top Reflector	118.9
2	Top Four Core Layers	436.1
3	Next Three Core Layers	674.0
4	Bottom Core Layer	753.3
5	Bottom Reflector	872.2

reflector temperatures were assumed to be the same as the average inlet and outlet helium temperatures.

The initial core, like all subsequent reload segments, contains three axial fuel zones in Rings 1 through 4. Axial fuel zoning is not used in the thin buffer region (Ring 5) either in the initial core or in subsequent reload segments. However, axial zoning (two zones) of the burnable-poison loading is used in all five core rings. Axial zoning for equilibrium Xe and Sm was assumed to be the same as that for uranium. The burnable-poison pins and the 7 control-rod pairs were homogenized into the 3 axial core zones using effective (self-shielded) cross sections.

Calculations performed with the axial model are summarized in Table XVIII. The reactivity worth of equilibrium Xe and Sm is identical to that obtained from the radial model. This is reasonable because the radial distribution of Xe and Sm is constant except in the thin buffer region (Ring 5). The burnable-poison worth is also in good agreement with that obtained from the radial model. Thus homogenization of the burnable

poison, which is present in all the radial core regions, has little effect on its worth. This is not true for the 7 control-rod pairs which are worth considerably less in the axial model than in the radial model. Radial homogenization of the control rods, which are present only in Rings 1 and 3 of the radial model, is not a good approximation and therefore the axial model cannot be used to calculate absolute control-rod worths. However, the axial model should be adequate for computing relative worths as a function of axial position. The above results indicate that the worth of the 7 control-rod pairs inserted halfway into the core is one-third of the worth with the rods fully inserted.

Normalized gross axial power profiles, $P(Z)/\bar{P}$, are shown in Fig. 29 for Cases 3 and 5. The average axial profile with the control rods completely inserted is basically the same as that with the rods completely withdrawn. Equilibrium Xe and Sm and the initial burnable poison loading have only a small effect on the axial power shape. Thus the axial profile for Case 3 is representative of Cases 1 through 4. The

TABLE XVIII
ONE-DIMENSIONAL AXIAL CALCULATIONS

Case	Description	k_{eff}
1	Reference calculation with burnable poison, equilibrium Xe and Sm, and 7 control-rod pairs fully inserted	1.017
2	Same as 1 but without Xe and Sm	1.056
3	Same as 1 but without control rods	1.041
4	Same as 3 but without burnable poison	1.139
5	Same as 1 but with 7 control-rod pairs inserted only halfway into the core.	1.033

Fig. 29. Normalized gross axial power distribution from one-dimensional model.

axial profile for Case 5 shows the average effect of inserting the 7 control-rod pairs halfway into the core. In the actual core, however, axial power shapes will be relatively unchanged in the unrodded refueling regions and more severely tilted in the rodded regions as shown in Fig. 30.

Gross axial profiles obtained by the vendor from one-dimensional core-averaged axial calculations are shown in Fig. 4.3.2-6 of the FGS PSAR. Our results (Fig. 30) are qualitatively similar to those of the vendor but differ in some details. In particular our results for the half-inserted 7 rod-pair case more closely resemble the vendor's results for 13 rod pairs. Since details of the vendor calculations are not given in the PSAR, the reason for this discrepancy cannot be determined.

As can be seen from Fig. 29, the peak in the gross axial power distribution occurs at the bottom of core layer 4. Our results yield a gross axial peaking factor ($P_{\max}/\bar{P}$) of 1.48 with no control rods and 1.54 with 7 control-rod pairs inserted halfway into the core. These values are in good agreement with those from the PSAR. For a single refueling region with the associated rod-pair

Fig. 30. Normalized axial power distribution in a single refueling region with its associated control rod pair inserted halfway.

halfway inserted (Fig. 30), the peak in the axial distribution occurs at the top of core layer 7 with a peak/average value of 2.35. An axial profile for this case is not available in the PSAR.

Since the fuel pins do not extend over the entire length of the fuel element, there is a thin unfueled graphite region between each axial layer of fuel pins. Softening of the thermal neutron spectrum in these unfueled regions results in local power spikes at the top and bottom ends of the fuel pins. The axial power distributions shown in Figs. 29 and 30 do not include this local peaking effect. However, this effect was calculated with a one-dimensional (slab) cell model in which the fueled and unfueled portions of a fuel element layer were represented explicitly. Results of the cell calculation for a fuel element layer containing the average initial fuel loading yield a local axial peaking factor of 1.10 for the initial core. Because of the more rapid depletion at the ends of the fuel pins, these local power spikes should become less pronounced with fuel age.

c. Two-Dimensional Calculations

A two-dimensional (R-Z), 4-group calculation was performed for the unrodded FGS initial core at design conditions. The 4-group cross sections were obtained by collapsing the 9-group sets using flux spectra computed with the one-dimensional models. The R-Z model contains six radial zones and five axial zones with dimensions identical to those used in the one-dimensional models (Tables XV and XVII).

Average axial and radial power distributions from the two-dimensional model agree well with results obtained from one-dimensional models in which compositions were averaged in the transverse directions. The radial distributions from the one- and two-dimensional calculations are practically identical since they typically agree within

0.5% except near the radial reflector-core interface where they differ by 2%. As shown in Fig. 31, the axial distributions generally agree within 2% except near the bottom reflector-core interface where the difference is about 7%. Thus for the unrodded case the radial and axial Component of the power distribution are separable to a good approximation.

The R-Z model will be used in various studies including those listed below.

- 1) Perturbation calculations of prompt-neutron generation time, effective delayed-neutron fractions, material worth distributions, and temperature coefficients at design conditions (4-group regular and adjoint angular fluxes have been computed for this case).
- 2) Computation of shutdown margin and central rod-pair worth versus axial position. The interaction effect between the two rods in a pair will have to be computed from a separate two-dimensional (planar) calculation.
- 3) Computation of reactivity effects associated with particular rearrangements of core materials.

Fig. 31. Normalized axial power distribution from one- and two-dimensional calculations (no control rods).

An example is the reactivity change if a central refueling region is postulated to fall due to failure of its three support posts.

2. Effect of ^{233}U on the FGS Isothermal Temperature Coefficient

Our previous studies of the FGS temperature coefficient have focused on the beginning-of-life (BOL) core which does not contain ^{233}U . However, this nuclide will build up during the initial and subsequent fuel cycles due to conversion of ^{232}Th . In the nonrecycle case the ^{233}U concentration will reach a maximum at the end of the equilibrium cycle (EOEC) at which time 51% of the power is expected to be generated in ^{233}U .

The effect of ^{233}U on the isothermal temperature coefficient was studied by performing calculations for several cores characterized by a fixed $\text{C}/(^{235}\text{U} + ^{233}\text{U})$ atom ratio but containing several different ^{233}U fractions. Aside from the fact that various amounts of ^{235}U were replaced with ^{233}U , the core compositions are identical to the initial unpoisoned core. This approach was taken because information on core compositions at the EOEC is not available in the FGS literature.

A one-dimensional (cylindrical) heterogeneous cell model was used to compute k_{∞} as a function of core temperature for ^{233}U fractions of 0.0, 0.10, 0.25, 0.50, and 1.0. Calculations were performed with the DTF-IV code using 9-group GAC cross sections. Results of the calculations are summarized in Fig. 32.

It is clear from Fig. 32 that the reactivity worth of ^{233}U is greater than that for ^{235}U on a atom-for-atom basis. The reason for this is that both $\bar{\sigma}_a$ (average thermal absorption cross section) and $\bar{\eta}$ (average number of fission neutrons per thermal absorption) are greater for ^{233}U than for ^{235}U . As a consequence of differences in the temperature of $\bar{\sigma}_a$, the ratio $\bar{\sigma}_a(^{233})/\bar{\sigma}_a(^{235})$ varies

Fig. 32. Effect of ^{233}U on the temperature variation of k_{∞} .

from 1.10 at 300 K to 1.49 at 3000 K as shown in Fig. 33. The ratio $\bar{\eta}(233)/\bar{\eta}(235)$ is essentially independent of temperature and is equal to about 1.10. The results in Fig. 32 also show that k_{∞} decreases less rapidly with core temperature as the ^{233}U fraction is increased. This behavior is due to the fact that $\bar{\sigma}_a$ for ^{233}U decreases less rapidly with temperature than does $\bar{\sigma}_a$ for ^{235}U .

Isothermal temperature coefficients derived from the curves in Fig. 32 are shown in Fig. 34. As one would expect from the previous discussion, the presence of ^{233}U results in a less negative temperature coefficient and the magnitude of the effect increases with both the ^{233}U fraction and core temperature. We estimate that a ^{233}U isotopic fraction of 0.45 is required to produce 51% of the power in ^{233}U . Thus the results for the ^{233}U isotopic fraction of 0.50 can be considered to be representative of the EOEC for the nonrecycle case. For this case

Fig. 33. $^{233}\text{U}/^{235}\text{U}$ effective thermal absorption cross section ratio (50% ^{233}U case).

Fig. 34. Effect of ^{233}U on the isothermal temperature coefficient.

the magnitude of the temperature coefficient is reduced by 10% at 300 K and 33% at 1200 K compared to the values for the BOL core (no ^{233}U). At higher temperatures the reduction is even larger and beyond about 2750 K the temperature coefficient becomes positive.

The separate effects of burnable poison, control rods, and equilibrium Xe and Sm on the isothermal temperature coefficient for the FGS initial core were reported previously.⁶ Both the burnable poison and inserted control rods result in a more negative temperature coefficient. At the EOE, however, the negative contributions from these components will not be available because the burnable poison will have been depleted and all control rods will have been withdrawn to compensate for fuel burnup. On the other hand the positive contribution from equilibrium Xe and Sm, which is a maximum near the design temperature of the HTGR, will be present at the EOE and the temperature coefficient for the EOE might be less negative than that shown in Fig. 34 for the 50% ^{233}U case. Therefore we cannot rule out the possibility that the isothermal temperature coefficient at the EOE may be positive near the operating temperature of the FGS, even for the nonrecycle case.

B. Nuclear Data Processing for HTGR Safety Research (M. G. Stamatelatos, R. J. Labauve, and J. C. Vigil)

The current route for generating multigroup cross sections for HTGR neutronics calculations was described previously⁶ and is summarized, for convenience, in Fig. 35. Cross section sets for 300, 1 200, and 3 000 K have been generated using the data-flow system of Fig. 35. Cross sections for 500, 800, 1 700, and 2 300 K are currently being generated.

Some code modification was performed on MC² (Ref. 43) and on GLEN⁴⁴ which

Fig. 35. Data flow for HTGR cross section generation.

previously did not allow for energy variation of absorber scattering cross sections in the thermal region. Although valid in most instances, the constant-scattering cross-section approximation fails for absorbers like ^{135}Xe and ^{149}Sm , yielding inaccurate results. The current version of GLEN permits energy variation in the absorber scattering cross sections, whenever required, through a special input. Also, the current GLEN version is no longer limited to four absorbers when a thermal spectrum is input, but can take any number of absorber isotopes.

A critical evaluation of the resulting multigroup cross sections compared with other non-LASL sets (e.g., from the General Atomic Company) is currently in progress. Double heterogeneity effects on cross sections (especially particle self-shielding) have not yet been included. One level of heterogeneity (fuel stick-moderator) will be considered next to determine its effect on the ^{232}Th cross sections. A preliminary analysis yields the following conclusions on the initial LASL cross sections:

- 1) Resonance absorber cross sections appear to be low especially in the resolved resonance region. This may be due to the relatively small number of energy groups used in the "all-fine" option of

MC². "Ultra-fine" MC² runs with 2000 energy groups in the above-thermal neutron region will be performed for comparison.

- 2) With some exceptions, thermal cross sections appear to be in reasonable agreement with the GAC set.
- 3) The variation of the ²³²Th absorption cross section with temperature is less than anticipated in the resonance region. Again, this may be due to the small number of energy groups used. It is desirable to use a very fine energy grid resolution only in the resonance region. This, however, is not possible since MC² requires a uniform lethargy grid. As a consequence, MC² calculates with very fine energy grid resolution in the entire above-thermal region must be carried out, and these are time consuming. In this connection, the use of MINX,⁴⁵ using variable energy group size and other sophisticated techniques, seems desirable. MINX calculations for at least one temperature are planned for the immediate future. In addition, the possibility of comparison calculations by Argonne National Laboratory with MC² - Version II (not yet operational at LASL) is anticipated.

C. CHAP Code Development - Steam Reheater Modeling (J. S. Gilbert)

The dynamic model⁶ of the steam generator reheater is operational. When model checkout and sensitivity evaluation is complete, the reheater model will be incorporated into CHAP. TAF,⁴⁶ a steady-state, frequency response, and time response simulation program, is utilized in these studies of the model. The check-out of the model is made to determine the simplest form of

the model, to minimize computation time, and to identify and record the transient and frequency response limits of the model.

The schematic, Fig. 36, of the reheater illustrates the flow reversal of the steam inside the tubes relative to the exterior helium flow. The heated helium arrives at the reheater via circular radial ducts connecting with the reactor core outlet plenum. The helium at a temperature of 999 K (1340 F) enters at an elevation between the steam generator and the reheater. Then the helium is distributed over the reheater surface and flows down through the bundle. At the bottom of the reheater bundle, helium flow is again

Fig. 36. Flow schematic of reheater module.

turned and enters a center duct that extends to the top of the steam generator module.

Table XIX is a description of the reheater response studies performed using TAF. From the simultaneous set of non-linear first-order differential equations representing the reheater model, TAF computes steady-state, frequency response and time response for initial value problems.

The steady-state analysis was performed using operating conditions at full power (100% load) for the FGS 3000 - MW(t) HTGR.⁴⁷ In Fig. 37 selected parameters are plotted for steady-state operation. The fractional tube length refers to the 11.2-m active length. Models with 8, 6, 4, and 2 sections on the steam side and corresponding 4, 3, 2, and 1 sections on the helium side were considered. The centerline of the tube bundle is superimposed on the

plot indicating symmetric helium temperatures represented by the dashed portion of the helium curve. In comparing models, deviations from the solid lines by the symbols for 4 and 2 sections indicate the accuracy of the models. In Table XX the values plotted in Fig. 37 are tabulated for ease of comparison. The entrance and exit helium temperatures remain unchanged for all cases. The helium temperature for the 1-section model is 0.5 K higher than the linear interpolation between adjacent temperatures of the 2-section model. For the tube wall temperature at 75% of tube length the 2-section model temperature is 3 K less than the corresponding interpolated 4-section temperature. At the same location the 2-section model steam temperature is 5 K less than the interpolated value. In all cases the Newton-Raphson procedure used in TAF provided convergence to steady-state operating conditions after two iterations.

TABLE XIX
REHEATER EXAMPLE FOR 3000-MW(t) HTGR

	<u>Inlet</u>	<u>Exit</u>
HELIUM		
Temperature, K	999	933
STEAM		
Temperature, K	607	810
Pressure, kPa	4440	4040
<u>RESPONSE STUDIES</u>	<u>PARAMETERS</u>	<u>RANGE</u>
A. STEADY-STATE (Temperature)	1. Helium 2. Tube Wall 3. Steam	
B. TRANSIENT (Temperature)	1. Helium Inlet 2. Helium Exit 3. Steam Exit	0.0 to 100.0 s
C. FREQUENCY (Gain and Phase)	1. Exit steam internal energy/inlet helium temperature 2. Exit steam internal energy/inlet steam pressure	10^{-3} to 10^3 rad s ⁻¹

TABLE XX
CALCULATED STEADY-STATE TEMPERATURES

Tube Length Fraction, % (Number of Sections)	Steam Temperature, K				Tube Wall Temperature, K				Helium Temperature, K			
	8	6	4	2	8	6	4	2	4	3	2	1
0.00	607	607	607	607					933	933	933	933
6.25	623				738				941			
8.33		629				742				943		
12.50			639				751				949	
18.75	655				763				956			
25.00		671	(669.5) ^a	668		775	(775.5)	775		965	(965.5)	966
31.25	686				787				973			
37.50			700				800				982	
41.66		711				808				987		
43.75	716				812				990			
50.00									999	999	999	999
56.25	743				829							
58.33		747				830						
62.50			754				833					
68.75	767				838							
75.00		777	(774)	769		841	(840)	837				
81.25	787				845							
87.50			794				847					
91.66		800				849						
93.75	803				850							
100.00	810	810	810	809								

^a Temperature calculated by interpolation.

Fig. 37. Steady-state analysis of reheater models for 3000-MW(t) HTGR operating at full power.

We used TAF to predict the transient resulting from the inlet helium temperature profile pictured in Fig. 38. The inlet helium temperature profile is a result of a +1.0 dollar open loop step reactivity insertion during full power operation.⁶ The 2-section model is represented by the solid line. Only minor differences in temperature (< 1.0 K) occur when compared with the 8-section case for the exit helium up to 20 s, thereafter the results are identical. The steam exit temperature for the 2-section case is on the average 1.4 K less than the 8-section model with a maximum deviation of 2.6 K occurring at 100 s.

Numerical techniques for steady-state and time response calculations from differential equations are in common use, but not for frequency response. The method employed in TAF for frequency response utilizes a complex matrix.⁴⁶ The frequency response example for the linearized reheater model at nominal operating conditions is outlined in Table XIX and is subsequently presented in Figs. 39 and 40 for 2- and 8-section models. Guidelines for comparing frequency plots are suggested here. When changes in gain exceed 30 dB and result in a more negative value, consideration of gain is no longer necessary

Fig. 38. Time response study resulting from a +1.0 dollar open loop step reactivity insertion at full power for a 2-section reheater model.

Fig. 39. Frequency response of the exit internal energy of the steam with respect to a perturbation of the inlet helium temperature.

Fig. 40. Frequency response of the exit internal energy of the steam with respect to a perturbation of the inlet water pressure.

and interpretation of phase plots at corresponding frequencies may be disregarded. If the gain curve reverses its slope and takes on values less than 30 dB from the initial value of gain at low frequency, then both gain and phase again become significant. A phase lag difference in two plots of 20 degrees or less at high frequencies is acceptable.

All low-frequency comparisons of the gain and phase of models result in coincident plots because steady-state operation is the limiting case for low frequency operation. High-frequency response has as its limit the response to an impulse function. In Fig. 39 the frequency response of the exit internal energy of the steam with respect to a perturbation of the inlet helium temperature for the 8- and the 2-section cases at 3000 MW(t) is presented. Applying the above guidelines one concludes that the simpler model is an accurate representation of the reheater over the frequency range of 10^{-3} to 10^3 rad s^{-1} . Throughout the frequency range, the

response of the exit internal energy of the steam (Fig. 40) must be evaluated with respect to a perturbation of the inlet water pressure, e.g., rapid closing of an upstream valve. For frequencies less than 1.0 rad s^{-1} both models yield the same results. At frequencies higher than 10^2 rad s^{-1} the 2-section model will provide a somewhat different response.

The sensitivity evaluation of the reheater model for use in CHAP suggests a 2-section steam side and a 1-section helium side model is acceptable compared to the 8-section model as a standard. Additional evaluations will be performed using the methodology illustrated in the reheater examples.

D. CHAP Code Development - A Once-Through Steam Generator Model (P. A. Secker)

We have developed a model for the once-through steam generator described in Ref. 47. The large HTGR steam generator is the major component of the HTGR Nuclear Steam Supply system. The steam generator is part of the high-efficiency reheat steam cycle, and several are arranged in parallel loops within symmetrically located cavities in the Pre-stressed Concrete Reactor Vessel (PCRv). The number of steam generator loops depends on the size of the power plant, each steam generator being capable of transferring about 500 MW(t).

The steam generator (excluding the reheater) consists of helically wound tube bundles arranged within the steam generator cavity. Helium passes in counter-flow fashion over the tubes which contain the steam/water flow. The main steam bundle consists of economizer (subcooled to saturated liquid), evaporator (two-phase mixture), and superheater (saturated to superheated vapor) sections. The steam/water flow is upward through all sections of the main steam bundle to provide uphill boiling.

We have formulated conservation equations (mass, momentum, and energy) for the once-through steam generator model. Three lumped regions have been modeled corresponding to the economizer, evaporator, and superheater regions. The control volumes in each region have moving boundaries because the boiling and subcooled lengths along a tube vary during a transient or at different operating conditions.

All conservation relationships for the steam/water side of the steam generator and the tube wall energy relationships have been treated dynamically. The helium conservation relationships have been treated quasi-statically because of the small heat capacity of the helium compared with water and tube wall and because helium remains a single phase fluid.

The steam generator expressions are being coded, and will be studied with the TAF program. The model will be evaluated over a broad range of operating conditions in order to identify the limitations and dynamic characteristics. Special attention will be given to the flood-out condition, where the superheater and possibly even the evaporator regions disappear. In this situation the number of solution equations reduces as flood out progresses. After initial model checkout and evaluation we will incorporate the model into the CHAP code.

E. CHAP Code Development - Primary Coolant Circulator Model and Characteristics (P. A. Secker)

We have modeled the HTGR primary coolant circulator for inclusion in the CHAP code. A single differential equation represents the change in circulator speed due to torque differentials between the steam turbine drive and the helium compressor. The inertia equation is

$$\frac{dN}{dt} = \frac{2\pi}{I_{\text{circ}}} (M_{\text{ST}} - M_{\text{He}}) \quad (39)$$

where

N = the circulator speed

I_{circ} = the rotational inertia of the circulator

M_{ST} = the torque developed on the steam turbine side of the circulator

M_{He} = the torque produced by helium compression.

The two torque relations are given by

$$M = k \frac{W \Delta P}{\rho N} \quad (40)$$

where

k = a design constant

W = the mass flow rate

ΔP = the pressure differential across the circulator

ρ = the fluid density (steam or helium).

On the compressor side of the circulator the primary coolant circuit pressure losses are restored by the helium compressor. In the Fulton Generating Station (FGS) PSAR⁴⁸ a performance map is given for the HTGR representing specific head $\left(\frac{\Delta P}{\rho N^2}\right)$ as a function of specific capacity $\left(\frac{W}{\rho N}\right)$.

This is shown in Fig. 41 with the error bars indicating the standard deviation of the reduced data at each point. The overall standard deviation is $\pm 3.0\%$.

On Fig. 41 the dashed line represents a second-order polynomial fit to the reduced curve which is within 5.7% everywhere. The polynomial expression is

$$\begin{aligned} \frac{\Delta P}{\rho} &= 0.21039 N^2 + 1.1469 \\ &\left(\frac{W}{\rho}\right) N - 1.6708 \left(\frac{W}{\rho}\right)^2 \end{aligned} \quad (41)$$

where

$\frac{\Delta P}{\rho}$ has units of (m)

$\left(\frac{W}{\rho}\right)$ has units of (m³/s)

N has units of (rps).

Fig. 41. HTGR turbine circulator performance map.

Control of the turbine circulator depends on the main steam generator exit temperature, the circulator rotational speed, and the pressure ratio (steam inlet to steam outlet) across the circulator turbine. These conditions determine the valve positions in the turbine circulator main steam line and the turbine circulator bypass steam line.

This model is presently being coded and checked out with TAF and CHAP. It represents the final component in a complete primary coolant loop.

F. CHAP Code Development - The Numerical Integration Technique (P. A. Secker)

We have developed a semi-implicit predictor-corrector numerical integration technique which is appropriate for integration of the model equations used in the CHAP code and which is inherently stable for any time step size. The method is based on the integrating factor solution of first order ordinary differential equations (ODE's).⁴⁹ Local error is controlled during a transient solution by varying the solution integration step size.

Consider the ODE representing a feature of the model dynamics:

$$\frac{d}{dt} y_1(t) = f_1(y_1, y_2, \dots, y_1, \dots, y_N, t) \quad (42)$$

where N is the order of the dynamic model. This equation is put into integrating factor format by adding and subtracting $\alpha_1 y_1$ to the right-hand side.

$$\frac{d}{dt} y_1 = f_1 + \alpha_1 y_1 - \alpha_1 y_1 \quad (43)$$

where α_1 is arbitrary and is chosen in order to maximize the local time step. It can be demonstrated that the best choice for α_1 is

$$\alpha_1 = \frac{-1}{(t_j - t_{j-1})} \ln \left[\frac{y_1(t_j)}{y_1(t_{j-1})} \right], \quad (44)$$

i.e., the instantaneous period over the time interval from t_{j-1} to t_j .

The solution of Eq. (43) at time t_{j+1} is:

$$\begin{aligned} y_1(t_{j+1}) = & y_1(t_j) \exp(-\alpha_1 \Delta t) \\ & + \exp(-\alpha_1 \Delta t) \int_0^{\Delta t} [f_1(t_j + \tau) \\ & + \alpha_1 y_1(t_j + \tau)] \exp(\alpha_1 \tau) d\tau \end{aligned} \quad (45)$$

where

$$\Delta t = t_{j+1} - t_j.$$

For the numerical solution of $y_1(t_{j+1})$ we assume that the term in brackets within the integral of Eq. (45) is equal to the arithmetic average of the endpoint values at t_j and t_{j+1}

$$\begin{aligned} f_1(t_j + \tau) + \alpha_1 y_1(t_j + \tau) = & \frac{f_1^j + f_1^{j+1}}{2} \\ & + \alpha_1 \frac{(y_1^j + y_1^{j+1})}{2} \end{aligned} \quad (46)$$

where the superscript represents discrete time points in the solution. Performing the integration provides an implicit numerical predictor for y_i^{j+1} .

$$y_i^{j+1} = y_i^j + \left(\frac{f_i^j + f_i^{j+1}}{\alpha_i} \right) \left(\frac{1 - e^{-\alpha_i \Delta t}}{1 + e^{-\alpha_i \Delta t}} \right) \quad (47)$$

In general Eq. (47) is nonlinear and depends on known values of y_i^{j+1} . Equation 47 is linearized by assuming a first-order Taylor expansion of $f_i(t)$ about t_j .

$$f_i^{j+1} = f_i^j + \sum_{k=1}^N \left. \frac{\partial f_i}{\partial y_k} \right|_{t=t_j} (y_k^{j+1} - y_k^j) \quad (48)$$

Upon substituting Eq. (48) into (47) we are left with a N by N matrix solution of y_i^{j+1} .

Most of the equations in CHAP exhibit diagonal dominance in the linearized version, i.e., Eq. (48). In particular the energy, momentum, neutronics, and decay power relationships have diagonal dominance in the first-order Jacobian matrix. Because of this feature we have assumed that Eq. (48) can be closely approximated by

$$f_i^{j+1} = f_i^j + \left. \frac{\partial f_i}{\partial y_i} \right|_{t=t_j} (y_i^{j+1} - y_i^j) \quad (49)$$

where the $\frac{\partial f_i}{\partial y_i}$ represents the diagonal element of the Jacobian matrix (this expression can be analytically or numerically computed).

With the substitution of Eq. (49) into (47) the predictor for y_i^{j+1} is now explicit of the form

$$y_i^{j+1} = y_i^j + \frac{2 f_i^j \left(\frac{1 - e^{-\alpha_i \Delta t}}{1 + e^{-\alpha_i \Delta t}} \right)}{\alpha_i \left(\frac{1 - e^{-\alpha_i \Delta t}}{1 + e^{-\alpha_i \Delta t}} \right) - \left. \frac{\partial f_i}{\partial y_i} \right|_{t=t_j} \left(\frac{1 - e^{-\alpha_i \Delta t}}{1 + e^{-\alpha_i \Delta t}} \right)} \quad (50)$$

The predicted value of y_i^{j+1} (along with all other N-1 dependent variables is used to compute f_i^{j+1} . Equation (47) is then solved in straightforward fashion to obtain the corrected value of y_i^{j+1} . The relative error between the predicted and corrected values of y_i^{j+1} is compared to a specified error criterion and establishes a basis for altering the integration step size during the transient solution.

We have applied this technique very successfully to the core transient studies performed to date. Computer running times are acceptable for both fast and slow transients. For example, a 28-h real time transient of the core heatup during a loss-of-forced-cooling accident required 31 s of CDC 7600 machine time. We also used the method for studying step reactivity insertions up to one dollar with good success.

We hope to successfully apply this technique to the solution of the steam generator model equations. Variations of the approach may be required since diagonal dominance cannot be expected. However, a fully implicit version of the method is straightforward. Implicit methods have been applied to similar problems in the past with good results.⁵⁰

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