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Beginning-of-Life Neutronic Analysis of a 3 000-MW(t) HTGR

by

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MASTER

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BEGINNING-OF-LIFE NEUTRONIC ANALYSIS OF A 3 000-MW(t) HTGR

by

John C. Viril

ABSTRACT

This report describes the results of an independent study of safety-related neutronic characteristics for the beginning-of-life core of a 3 000-MW(t) High-Temperature Gas-Cooled Reactor. Emphasis was placed on the temperature-dependent reactivity effects of fuel, moderator, control poisons, and fission products. Other neutronic characteristics studied were gross and local power distributions, neutron kinetics parameters, control rod and other material worths and worth distributions, and the reactivity worth of a selected hypothetical perturbation in the core configuration. The study was performed for the most part using discrete-ordinates transport theory codes and neutron cross sections that were interpolated from a four-parameter nine-group library supplied by the HTGR vendor. A few comparison calculations were also performed using nine-group data generated with an independent cross-section processing code system. Results from the independent study generally agree well with results reported by the HTGR vendor.

1. INTRODUCTION

Under the auspices of the Nuclear Regulatory Commission's (NRC) Division of Reactor Safety Research (DRSR), the Los Alamos Scientific Laboratory (LASL) is involved in a broad program of research in High-Temperature Gas-Cooled Reactor (HTGR) technology whose overall objective is to provide an independent technical basis for assessment of HTGR safety. One of the subtasks in this program is the independent computation of safety-related neutronic characteristics of the HTGR and verification of the vendor's cross-section data base and processing techniques. This report describes the neutronic analyses performed for the 3 000-MW(t) Fulton Generating Station (FGS) beginning-of-life (BOL) core. Future studies of neutronic characteristics will focus on burned cores, in particular the end of the equilibrium fuel cycle.

The neutronic analyses involve primarily the active core and surrounding reflectors whose overall characteristics are summarized in Table I for the General Atomic Company (GAC) 3 000-MW(t) HTGR. The active core consists of 493 vertical hexagonal

TABLE I

HTGR OVERALL CHARACTERISTICS

Reactor thermal output [MW(t)]	3 000
Active core height (m)	6.34
Effective core diameter (m)	8.44
Reactor core power density [kW(t)/liter]	8.4
Average top reflector thickness (m)	1.19
Average bottom reflector thickness (m)	1.19
Average side reflector thickness (m)	1.03
Number of fuel elements	3 944
Number of control rod pairs	73

columns arranged to approximate a right circular cylinder. Each of these columns is composed of eight fuel elements (or blocks) together with top and bottom reflector elements. Surrounding the active core are 114 vertical hexagonal columns of replaceable graphite elements followed by permanent side reflector graphite columns shaped to make the transition to a circular outer boundary. The core is divided into 85 refueling regions each of which consists (except for the peripheral regions) of a

central fuel column and its six surrounding columns. Control rod and reserve shutdown channels are located in the central fuel column of 73 of the 85 refueling regions. The central column of a refueling region is called a control column and the associated fuel elements are called control fuel elements.

Both the standard and control fuel elements are hexagonal graphite blocks 0.357 m (14.17 in.) across the flats and 0.793 m (31.22 in.) high containing a matrix of fuel and coolant holes. The control fuel elements have in addition three large channels to accommodate a control rod pair and reserve shutdown material. The blind fuel holes (132 in standard elements and 76 in control elements) contain fuel pins or "sticks" and are capped with graphite plugs. The fuel pins, which are 1.56-cm-diam, contain fissile and fertile coated particles bonded together with a graphitic binder. Fissile particles consist of a uranium carbide kernel (200- μ m-diam) coated with pyrolytic carbon and silicon carbide and fertile particles consist of a thorium oxide kernel (500- μ m-diam) coated with pyrolytic carbon. In each of the six corners of the standard fuel elements (and in four out of six in the control elements) is a hole that may contain burnable poison in the form of a rod composed of boron carbide granules dispersed in a graphite matrix. For further descriptive details, the reader is referred to the FGS Preliminary Safety Analysis Report¹ (PSAR) and to the General Atomic Standard Safety Analysis Report² (GASSAR).

Calculational methods, data, and general procedures used in the LASL analyses of the FGS initial core are described in Sec. II. Temperature coefficient calculations are described in Sec. III including an analysis of the isothermal coefficient for the unpoisoned core, fuel and moderator components of the isothermal coefficient, and a study of the separate effects of burnable poison, control rods, xenon, samarium, and ²³³U on the isothermal coefficient. In addition, Sec. III includes a comparison of the beginning-of-cycle (BOC) isothermal coefficient obtained using GAC and LASL cross sections. One- and two-dimensional gross power distributions, and one-dimensional local distributions, are described in Sec. IV. Reactivity worths of control rods and other materials, and perturbation calculations of kinetic parameters and worths distributions, are discussed in Sec. V. The

LASL results are compared with GAC results in those cases where the latter are available in the literature.

II. CALCULATIONAL METHODS AND DATA

A. Neutronic Flux Codes

Except for one comparison calculation, all neutron flux calculations were performed with one- and two-dimensional discrete-ordinates transport theory computer programs using the S_4 approximation and transport-corrected P_0 scattering cross sections. In particular, we used the well-established LASL codes DTF³ and TWOTRAN.⁴

Transport theory is generally required for heterogeneous problems in which voids or highly absorbing materials (e.g., control rods) are represented explicitly. We have determined, however, that diffusion theory is adequate for those HTGR applications in which such heterogeneities are not present. For example, a two-dimensional (r-z) calculation of the unrodded homogenized core with the VENTURE diffusion theory code⁵ yielded a multiplication factor within 0.001 % of that obtained with the TWOTRAN code.

We have also determined that angular quadratures higher than S_4 are not generally required for HTGR analysis. Special applications such as determination of void streaming effects may require a higher order. Because anisotropic scattering effects in carbon are not large, and because neutron leakage from the HTGR core is relatively small, the use of transport-corrected P_0 scattering cross sections should be adequate.

Direct and adjoint angular fluxes from TWOTRAN were used in a LASL perturbation code to compute kinetics parameters and reactivity worth distributions in the unrodded core. This perturbation code is a multidimensional version of the DACI code.⁶

B. Neutron Cross Sections

With the exception of the comparison calculations discussed in Sec. III.G., all of the neutronic calculations were performed with cross sections derived from a nine-group, transport-corrected, P_0 (i.e., single-table) library supplied by GAC. The cross sections in this library were generated with the MICROX code⁷ for use in the physics design of the large HTGR and the library includes data for the 48 nuclides listed in Table II. In addition to nuclides that are present in fresh fuel, the list

TABLE II
ACTINIDES IN GAC CROSS-SECTION LIBRARY

I.D.	Nuclide Name	I.D.	Nuclide Name
1	Th-232	25	Sm-150
2	Pa-233	26	Sm-151
3	U-233	27	Sm-152
4	U-234	28	Eu-153
5	U-235	29	Eu-154
6	U-236	30	Eu-155
7	U-238	31	NSAG25 ^a
8	Np-237	32	B-10
9	Np-239	33	Si
10	Pu-238	34	C
11	Pu-239	35	B-nat
12	Pu-240	36	H
13	Pu-241	37	NSAG23 ^b
14	Mo-95	38	O
15	Tc-99	39	Pa-231
16	Rh-103	40	Pu-242
17	Xe-131	41	U-232
18	Xe-135	42	V
19	Cs-133	43	Ni
20	Nd-143	44	Cu
21	Nd-145	45	Th-230
22	Pm-147	46	Fe
23	Pm-148	47	Li-6
24	Sm-149	48	Zr

^aNon saturating fission-product aggregate for ²³⁵U.

^bNon saturating fission-product aggregate for ²³³U.

includes these fission products and actinides which GAC considers to be the most important with respect to reactivity effects. The effects of self-shielding in the fuel particles and fuel sticks (double heterogeneity effects) are taken into account in MICRON.

For each nuclide in the library, fast cross sections are tabulated for various values of fuel temperature (T_f) and fuel stick thorium atom density (N_{Th}). Thermal cross sections are similarly tabulated for various values of moderator temperature (T_m) and carbon-to-uranium (fissile) atom ratio (C/U_{fiss}). The values of these four parameters for which cross sections are given in the library are

listed in Table III. An interpolation code (INTERP) is used to generate cross sections in a format compatible with the LASL S_n codes for the desired values of T_f , N_{Th} , T_m , and C/U_{fiss} . The core-average values of these four parameters are $T_f = 1160$ K, $N_{Th} = 0.00142$ atoms/barn-cm, $T_m = 1010$ K, and $C/U_{fiss} = 4920$ for the FGS initial core at design conditions.

The nine-group energy structure and associated fission spectrum are given in Table IV. A Maxwellian distribution

$$\chi(E) = 2E^{1/2} e^{-E/T} / \pi^{1/2} T^{3/2},$$

with $T = 1.29$ MeV for thermal fission of ²³⁵U, was used to derive the nine-group fission spectrum. The first group in the nine-group structure contains most of the fission source; the second and third groups encompass, respectively, the unresolved and resolved resonance range for ²³²Th; group five is a narrow group to allow upscattering from the thermal to the epithermal range; and groups six through nine encompass the thermal range. The four energy groups in the thermal energy range ($E \leq 2.38$ eV) are characterized by nonzero graphite upscatter cross sections. Above 2.38 eV, which we refer to loosely as the fast energy range, the probability of upscattering events in the moderator is negligible.

To date the nine-group cross sections have been used only in one-dimensional calculations. Neutron flux spectra from these calculations were used to

TABLE III
VALUES OF PARAMETERS USED TO CHARACTERIZE
CROSS SECTIONS IN GAC LIBRARY

Fast Cross-Section Parameters		Thermal Cross-Section Parameters	
T_f (K)	N_{Th} (atoms/barn-cm)	T_m (K)	C/U
300	0.0025	300	2000
600	0.0020	600	3000
900	0.0015	850	4500
1050	0.0010	950	6000
1300	0.0005	1100	8500
1500		1300	
1800		1600	
2200		2000	
3000		3000	
3400		3400	

TABLE IV
ENERGY BOUNDARIES AND FISSION SPECTRUM
FOR NINE-GROUP STRUCTURE

Group	Lower Energy (eV)	Fission Fraction
1	1.83×10^5	0.96306
2	961	0.03694
3	17.6	0.0
4	3.93	0.0
5	2.38	0.0
6	0.414	0.0
7	0.10	0.0
8	0.04	0.0
9	0.0	0.0

collapse the nine-group cross sections to four groups according to the scheme 1, 2-3, 4, 5-9. The four-group cross sections were then used in the two-dimensional calculations.

Nine-group cross sections for homogeneous HTGR media have also been produced with an independent LASL cross-section processing code system. The data flow for this preliminary code system is shown in Fig. 1. Except for the graphite thermal scattering kernels, the system is based on evaluated cross-section data contained in the Evaluated Nuclear Data File (ENDF/B).⁸

In the fast energy range, the MC² code⁹ is used to generate fine-group (68 groups each having a lethargy width of 0.25) cross sections from the ENDF/B library using a 1/E weighting function. These fine-group cross sections are used in MC² to

Fig. 1. Data flow for preliminary LASL HTGR cross-section processing code system.

compute a fine-group flux spectrum for a specified homogeneous medium. Using the calculated spectrum, MC² then collapses the fine-group cross sections to the five fast groups of the nine-group structure.

In the thermal energy range, a separate pass is made through MC² to generate fine-group (68 groups, 0.125 lethargy interval) thermal cross sections from the ENDF/B library for all the absorber nuclides. A Maxwellian spectrum at the appropriate temperature, joined to a 1/E spectrum, is used as the weighting function to reduce the pointwise data in ENDF/B to the fine-group structure. Fine-group thermal scattering cross sections for the moderator are computed with two other codes from basic physics parameters such as the phonon frequency distribution, crystalline lattice parameters, and temperature. In particular the TOR code¹⁰ is used to compute the inelastic scattering matrix and the HEXSCAT code¹¹ is used to compute the elastic scattering component.

The fine-group thermal cross sections from MC², TOR, and HEXSCAT are used in the GLEN code¹² to compute a fine-group thermal spectrum (for a given homogeneous medium) which is then used to collapse the fine-group thermal cross sections to the four thermal groups of the nine-group structure. A small code (JMBLEAT) combines the fast and thermal broad-group cross sections to produce cross sections in the LASL S_n format.

ENDF/B input to MC² is not direct but requires preprocessing by other codes as shown in Fig. 2. The RIGEL code¹³ is used to convert an ENDF/B data tape in BCD format to binary format. From the binary ENDF/B tape, the ETOE code¹⁴ prepares a library tape for MC² which includes "W-Tables" supplied by the auxiliary code WL13. Because temperature is an input parameter to ETOE, a different MC² library tape must be prepared for each temperature. Not shown in

Fig. 2. Linked MC² code system.

Fig. 2 but also available is a code (MERMC2) for merging MC² library tapes. In order to facilitate the operation of the MC² code, especially since cross sections are needed for several temperatures, all the preprocessing and auxiliary codes related to MC² have been linked on the computer.

Work is in progress to incorporate treatment of the double-heterogeneity effect in the LASL cross-section processing system. Because of difficulties encountered in applying MC² to well-moderated systems such as the HTGR, and in order to take advantage of the most up-to-date computational techniques employed in the MIXX code,¹⁵ the following course of future action has been adopted:

1. Generate pointwise (PXSDF) cross sections from XSDF/R with the MIXX code over the entire energy range.
2. Apply particle heterogeneity correction to the XSDF capture cross sections in both the fast and thermal regions (the fertile particle is the important one in the fast region whereas the fissile particle is more important in the thermal region).
3. Collapse the corrected PXSDF cross sections to the desired fine-group structure in MIXX. Fine-group data from MIXX will be in the Bondarenko format.¹⁶
4. Calculate a fine-group neutron spectrum in the fast region with the IDX code¹⁷ (one-dimensional diffusion theory) and use this code to collapse to the desired fast broad-group structure. The fuel-pin/moderator heterogeneity effect will be taken into account at this stage by applying the proper correction for HTGR fuel pins in a hexagonal lattice.
5. As before, calculate a thermal fine-group spectrum with GLEN and collapse the fine-group cross sections to the desired thermal broad-group structure.
6. Investigate the importance of the fuel-pin/moderator heterogeneity effect on the broad-group thermal cross sections. This will require a one-dimensional thermalization code.

C. Core Compositions

Volume fractions of the various components in both standard and control fuel elements, and the overall volume fractions of these components in the core, are given in Table V. These volume fractions were derived from geometric information in the FGS PSAR. The core-average values reflect the fact that standard and control fuel elements comprise, respectively, 85.19% and 14.81% of the total fuel elements.

TABLE V
VOLUME FRACTIONS OF CORE COMPONENTS

Component	Standard Element	Control Element	Core Average
Fuel Pins	0.2057	0.1184	0.1928
Moderator	0.5502	0.5396	0.5472
Control Rod Assembly	-	0.1094	0.0162
Burnable Poison	0.0040	0.0026	0.0038
Coolant Channels	0.1149	0.1235	0.2014
Reserve Shutdown Hole	-	0.0628	0.0093
Other Voids	0.0252	0.0536	0.0292

The moderator, which refers to the structural graphite component of the fuel elements, occupies about 55% of the core volume. Note that the fuel pin and coolant channel average volume fractions are nearly equal (about 19% and 20%, respectively). The differences between standard and control elements reflect the fact that there are 132 fuel pins and 72 coolant holes in a standard element compared to 76 fuel pins and 43 coolant holes in a control element. When fully inserted, the 73 control-rod assemblies occupy about 1.6% of the core volume. The volume fraction given for the burnable poison is based on utilization of all poison pin locations and a nominal poison pin diameter of 1.016 cm (0.4 in.). Because all poison pin locations are not necessarily used, the actual volume fraction may be smaller than that given in Table V. The entry "other voids" includes the fuel-handling pickup hole, gaps between fuel pins and the moderator, gaps between poison pins and the moderator, and gaps between fuel element columns.

Volume fractions and material densities for the various fuel pin components are given in Table VI. The fissile and fertile particle volume fractions correspond to the average uranium and thorium loadings which are based on total loadings of 1 725 kg (²³⁵U) and 37 487 kg (²³²Th) as specified in the FGS PSAR. After all the BOL calculations had been completed, we determined from more recent information in the GASSAR that 1 725 kg should be the total U rather than the ²³⁵U loading. Since the uranium enrichment is 93.15%, this means that we have used a ²³⁵U loading that is 6.85% too high. This difference should have only a

TABLE VI
VOLUME FRACTIONS AND MATERIAL DENSITIES
OF FUEL PIN COMPONENTS

Component	Density (g/cm ³)	Volume Fraction in Fuel Pin
Fissile Particles		
UC ₂ Kernel	9.9	0.0030
Buffer Layer	1.15	0.0161
Inner Iso. Layer	1.95	0.0088
Silicon Carbide Layer	3.20	0.0112
Outer Iso. Layer	1.80	0.0202
Fertile Particles		
ThO ₂ Kernel	9.5	0.0656
Buffer Layer	1.15	0.0923
Outer Iso. Layer	1.90	0.1316
Binder Matrix	1.3	0.6512

secondary effect on temperature-dependent reactivity effects but must be taken into account when comparing absolute magnitudes of multiplication factors. We have found that for the FGS core at design temperature, fractional changes in uranium mass ($\Delta M/M$) are related to reactivity changes ($\Delta k/k$) by the expression

$$\frac{\Delta k}{k} = 0.21 \frac{\Delta M}{M}.$$

This expression has been used to adjust the LASL values of k when comparing with vendor results. Since $\Delta M/M = -0.0685$, the adjustment is given by $\Delta k/k = -0.014$ and the adjusted multiplication factor by 0.986 k .

Note from Table VI that the fissile (UC₂) and fertile (ThO₂) kernels occupy, respectively, only 0.3% and 6.56% of the average fuel pin volume. The remaining volume is either binder material (~ 65%) or particle coatings (5.6% for the fissile particles and 22.4% for the fertile particles). The binder material and the particle coatings, except for the silicon carbide coating on the fissile particle, are made of graphitic material so that about 93% of the fuel pin volume is occupied by carbon. Consequently, about 20% of all the moderator in the core is contained within the fuel pins.

Atom densities for the uranium isotopes were computed using the following isotopic composition for the initial uranium loadings:

Isotope	Atom Percent
²³⁴ U	0.73
²³⁵ U	93.15
²³⁶ U	0.28
²³⁸ U	5.84

The carbon atom densities in the structural core graphite and in the graphite reflectors were computed using a graphite density of 1.78 g/cm³. Natural boron was included in all graphite and carbon components at a level of two boron atoms per million carbon atoms to account for impurities.

III. TEMPERATURE COEFFICIENTS OF REACTIVITY

A. Isothermal Coefficient for Unpoisoned Core

Cross sections were generated with the INTERP code from the GAC nine-group library for isothermal core temperatures of 300, 500, 800, 1 200, 1 700, 2 300, and 3 000 K and a core composition corresponding to the initial fuel loading of the FGS. These cross sections were used to perform k -calculations at the various temperatures in order to determine the isothermal temperature coefficient for the initial, clean, unpoisoned core. Calculations were performed with one-dimensional (cylindrical) cell and full-reactor models.

Cell calculations were performed with the four-region model defined in Table VII. The cell is infinitely long with a reflective boundary condition at the center and a white (isotropic return) condition at the outer boundary. In this model the fuel pin and fuel/moderator gap are represented explicitly. These two components are followed by a moderator region and an outside void region which represents the coolant channels, control rod holes, and other voids. The dimensions of the moderator and void regions are such that core-average volume fractions as given in Table V are preserved (with the control rod and burnable poison volume fractions replaced by void).

A one-dimensional cylindrical model of the full reactor, described in Table VIII, was used to study the effect of temperature on neutron leakage from the core. The boundary conditions are reflective at the center and vacuum (no return) at the outer boundary. Because the initial fuel loading is not varied radially except for a thin buffer zone at the

TABLE VII
ONE-DIMENSIONAL FUEL PIN CELL MODEL

Region	Outer Radius (cm)	Mesh Intervals
Fuel Pin	0.7798	9
Fuel-Pin Hole	0.7925	1
Moderator	1.5342	7
Void	1.7759	3

TABLE VIII
ONE-DIMENSIONAL (CYLINDRICAL), TWO-REGION
FULL-REACTOR MODEL

Region	Outer Radius (m)	Mesh Intervals
Homogenized Cor.	4.219	70
Radial Reflector	5.248	15

core/reflector interface, and because control and fission product poisons are not being considered at this point, the full-reactor model includes only two regions: a homogenized core region and a reflector region. The full-reactor calculations were performed both without and with axial buckling. A buckling height of 6.344 m, which corresponds to the actual height of the active core region, was used in the latter calculations. The radial reflector temperature was in every case the same as the core temperature.

Average atom densities in the fuel pin, moderator, homogenized core, and reflector are given in Table IX for the unpoisoned initial core. As mentioned previously, the uranium atom densities are based on a total ^{235}U loading of 1 725 kg.

Multiplication factors obtained with the cell and full- (finite) reactor models at the various temperatures are plotted in Fig. 3. These results indicate that radial neutron leakage from the core is worth $-0.019 \Delta k$ at 300 K and $-0.022 \Delta k$ at 3 000 K. To the extent that axial leakage can be represented by a constant buckling height, axial leakage is worth $-0.020 \Delta k$ at 300 K and $-0.026 \Delta k$ at 3 000 K so that the overall leakage is worth $-0.039 \Delta k$ at 300 K and $-0.048 \Delta k$ at 3 000 K. Therefore core neutron leakage makes a small negative contribution to the isothermal temperature coefficient and this

TABLE IX
AVERAGE ATOM DENSITIES IN UNPOISONED BOL CORE
(atoms/barn-cm)^a

Nuclide	Fuel Pin	Moderator ($\rho = 1.78$)	Homogenized Core	Reflector ($\rho = 1.78$)
Th-232	1.422-03	-	2.742-04	-
U-234	5.063-07	-	9.762-08	-
U-235	6.461-05	-	1.246-05	-
U-236	1.942-07	-	3.744-08	-
U-238	4.050-06	-	7.810-07	-
O	2.844-03	-	5.484-04	-
Si	5.387-04	-	1.039-04	-
C	6.460-02	8.926-02	6.130-02	8.926-02
B	1.292-07	1.785-07	1.226-07	1.785-07

^a Atom densities are given using computer notation (e.g., 1.422-03 = 1.422×10^{-3})

contribution amounts to about 7% of the total temperature defect between 300 K and 3 000 K for the unpoisoned core. Inclusion of burnable poison and equilibrium xenon and samarium reduces the core neutron leakage and hence the reactivity effect from this source (see Secs. IV.C. and V.A.).

Fig. 3. Multiplication factors for cell and full-reactor models (unpoisoned core).

The multiplication factor reported in the FGS PSAR for the initial unpoisoned cold core is 1.220. We calculate a value of 1.238 using the one-dimensional finite reactor model with axial buckling. Adjustment of the LASL value to take into account the difference in uranium loading (see Sec. III.C.) yields $k = 1.221$ which is in excellent agreement with the vendor value.

Flux disadvantage factors, defined as the ratio of the average flux in the fuel pin ($\bar{\phi}_{\text{fuel}}$) to that in the cell ($\bar{\phi}_{\text{cell}}$), are given in Table X for the 300, 800, 1 700, and 3 000 K cases. The disadvantage factors are nearly unity for all energy groups and change very little with temperature. The small decrease in group 3 with increasing temperature results from Doppler broadening of the ^{232}Th resonances. Hardening of the thermal neutron spectrum with increasing temperature, and the corresponding decrease in the absorption cross sections in the thermal groups, causes the flux disadvantage factors in these groups to increase slightly. For the top thermal group (group 6), however, this effect is offset by Doppler broadening of a ^{235}U resonance.

Cell-averaged macroscopic cross sections were computed from the spatial flux distributions obtained with the fuel pin cell model. The cell-averaged cross sections were then used in the core region of the full-reactor model. Results from these calculations were almost identical to those obtained using straight volume weighting. This

means that the unpoisoned core is essentially homogeneous with respect to the nine-group cross sections and therefore the fuel heterogeneity effects must be taken into account at a finer level during the broad-group cross-section generation process.

Isothermal temperature coefficients, in the form $(1/k)(dk/dT)$, were calculated from the data in Fig. 3 by using quadratic fits to three adjacent points. The values so derived are shown in Fig. 4 for both the cell and full-reactor models. It is clear from Fig. 4 that neutron leakage from the core does not have a large effect on the isothermal temperature coefficient, particularly below 1 000 K. In other words, most of the isothermal temperature coefficient is due to changes in the infinite multiplication factor (k_{∞}). We have therefore used cell models to study the effects of various poisons and other components on the isothermal coefficient. Although neutron leakage is relatively unimportant with respect to the isothermal coefficient, it may be a significant contributor to the moderator coefficient which is itself small compared to the isothermal coefficient. This point is discussed further in Sec. III.B.

The results shown in Fig. 4 are in good agreement with GAC results for the BOL core using a one-dimensional full-reactor model. In particular, our results with axial buckling are within about 6% of the GAC results over the entire temperature range (300 to 3 000 K).

TABLE X
FUEL PIN DISADVANTAGE (SHIELDING) FACTORS
($\bar{\phi}_{\text{fuel}}/\bar{\phi}_{\text{cell}}$)

Group	300 K	800 K	1700 K	3000 K
1	1.028	1.028	1.028	1.028
2	1.000	1.000	1.000	1.000
3	0.994	0.993	0.992	0.991
4	0.997	0.997	0.997	0.997
5	0.999	0.999	0.999	0.999
6	0.997	0.997	0.996	0.996
7	0.991	0.991	0.992	0.992
8	0.985	0.986	0.987	0.987
9	0.974	0.975	0.976	0.976
Thermal ^a	0.988	0.990	0.992	0.994

^aFlux-averaged value for groups six through nine.

Fig. 4. Isothermal temperature coefficient for unpoisoned core.

TABLE XI
FOUR-FACTOR (η , ϵ , p , f) DECOMPOSITION OF k_{∞}
(unpoisoned core)

T(K)	η	ϵ	p	f
300	2.0545	1.0963	0.7450	0.7612
500	2.0478	1.0975	0.7324	0.7614
800	2.0373	1.0988	0.7189	0.7630
1200	2.0297	1.1001	0.7063	0.7649
1700	2.0301	1.1014	0.6948	0.7644
2300	2.0348	1.1027	0.6844	0.7619
3000	2.0397	1.1044	0.6749	0.7583

The infinite multiplication factors obtained from the fuel pin cell model are decomposed in Table XI into the conventional four factors

$$k_{\infty} = \eta \epsilon p f$$

where η = fission neutrons per thermal absorption in fuel,

ϵ = fast fission factor,

p = resonance escape probability,

f = thermal utilization factor,

and thermal refers to $E \leq 2.38$ eV (groups six through nine), fast to $E > 2.38$ eV (groups one through five), and fuel to ^{235}U . Temperature coefficients were calculated from the data in Table XI and the results are presented in Table XII. The temperature coefficient for k_{∞} is also included in Table XII for comparison purposes. It is clear that the resonance escape probability (p) is the dominating component of the isothermal coefficient. As shown in Table XIII, ^{232}Th is the major contributor to $(1/p)(dp/dT)$ and therefore the Doppler coefficient in thorium provides most of the temperature feedback in the unpoisoned core. About 60% of the neutrons absorbed in thorium are absorbed in the fast energy range.

We have also computed the contributions of ^{232}Th and ^{235}U to $(1/k_{\infty})(dk_{\infty}/dT)$. As shown in Table XIV, these two nuclides are the main contributors to the isothermal coefficient and they contributed almost equally up to about 1200 K. The fact that the contribution from ^{235}U is almost as large as that from ^{232}Th is surprising at first glance. However, note that $(1/\eta)(d\eta/dT)$, which is due entirely to ^{235}U by definition, makes a negative

TABLE XII
TEMPERATURE COEFFICIENTS OF η , ϵ , p , and f
FOR UNPOISONED CORE ($10^{-5}/\text{K}$)

T(K)	$\frac{1}{\eta} \frac{d\eta}{dT}$	$\frac{1}{\epsilon} \frac{d\epsilon}{dT}$	$\frac{1}{p} \frac{dp}{dT}$	$\frac{1}{f} \frac{df}{dT}$	$\frac{1}{k_{\infty}} \frac{dk_{\infty}}{dT}$
300	-1.61	+0.59	-9.35	-0.14	-10.51
500	-1.67	+0.48	-7.60	+0.35	-8.44
800	-1.38	+0.35	-5.46	+0.68	-5.81
1200	-0.51	+0.26	-3.91	+0.29	-3.86
1700	+0.20	+0.22	-2.94	-0.32	-2.83
2300	+0.37	+0.21	-2.29	-0.61	-2.32
3000	+0.31	+0.21	-1.72	-0.73	-1.94

TABLE XIII
CONTRIBUTIONS OF ^{235}U AND ^{232}Th TO $\frac{1}{p} \frac{dp}{dT}$ FOR
UNPOISONED CORE ($10^{-5}/\text{K}$)

T(K)	^{235}U	^{232}Th	Other*	Total
300	-1.51	-7.83	-0.01	-9.35
500	-1.22	-6.35	-0.03	-7.60
800	-0.86	-4.57	-0.03	-5.46
1200	-0.72	-3.18	-0.01	-3.91
1700	-0.71	-2.23	0.00	-2.94
2300	-0.67	-1.65	+0.03	-2.29
3000	-0.61	-1.18	+0.07	-1.72

* Computed from the total minus the contributions from ^{235}U and ^{232}Th .

TABLE XIV
CONTRIBUTIONS OF ^{235}U AND ^{232}Th TO NET ISOTHERMAL
COEFFICIENT FOR UNPOISONED CORE ($10^{-5}/\text{K}$)

T(K)	^{235}U	^{232}Th	Other*	Total
300	-4.78	-6.01	+0.28	-10.51
500	-4.14	-4.55	+0.25	-8.44
800	-3.03	-2.97	+0.19	-5.81
1200	-1.82	-2.12	+0.08	-3.86
1700	-1.06	-1.76	-0.01	-2.83
2300	-0.79	-1.49	-0.04	-2.32
3000	-0.67	-1.19	-0.08	-1.94

* Computed from the total minus the contributions from ^{235}U and ^{232}Th .

contribution which is comparable to the ^{235}U contribution to $(1/p)(dp/dT)$ up to about 1 200 K. Most of the remaining differences between the contributions of ^{235}U and ^{232}Th to $(1/p)(dp/dT)$ as compared to their contributions to $(1/k_{\infty})(dk_{\infty}/dT)$ are due to their contributions to $(1/f)(df/dT)$. The increase in resonance absorption in ^{232}Th with temperature reduces the number of available thermal neutrons. Since about 90% of the absorptions in ^{235}U occur in the thermal region, this results in a negative ^{235}U contribution to $(1/f)(df/dT)$. This contribution is offset to a large extent by positive contributions from ^{232}Th and other nonfissile nuclides so that the net value of $(1/f)(df/dT)$ is small (Table XII).

B. Fuel and Moderator Components of Temperature Coefficient

The GAC method¹⁸ of calculating the fuel or "prompt" temperature coefficient assumes that the fuel coefficient is due entirely to changes in the fast cross sections (Doppler effect). Thus in computing the fuel coefficient the same thermal cross sections (those corresponding to room temperature) are used for all the fuel temperatures specified. The moderator coefficient is assumed to be given by the difference between the net isothermal coefficient and the fuel coefficient, that is, it is assumed that the fuel and moderator coefficients are separable and that the moderator coefficient includes the temperature effect of the moderator contained within the fuel pins (fuel moderator) as well as that from the bulk moderator (structural graphite external to the fuel pins). Fuel and bulk moderators comprise, respectively, 20 and 80% of the total moderator in the FGS core.

During a reactor excursion, the temperature response of the fuel moderator will be prompt compared to that of the bulk moderator because of the relatively large thermal resistance presented by the gap between the fuel pins and the structural graphite. Furthermore, in the initial core the fuel moderator must be heated first in order to transfer heat from the fissile to the fertile fuel kernels. Thus the effect of fuel moderator heating will be more prompt in this case than the Doppler effect in the thorium. For these reasons we believe that the effect of the fuel moderator on the thermal neutron spectrum should be included in the fuel temperature coefficient rather than in the moderator temperature coefficient.

The fuel and moderator components of the net temperature coefficient were computed using both the GAC definitions of these components and the definitions which we believe to be more appropriate. These are referred to below as Case A and Case, B, respectively. In both cases, the coefficient for a particular component was obtained by calculating the multiplication factor with that component at various temperatures and the other component at a fixed temperature of 300 K. The fuel pin cell model of Table VII and compositions corresponding to the unpoisoned initial core were used in the calculations.

The effect of fuel temperature on k_{∞} is shown in Fig. 5 and temperature coefficients derived therefrom are plotted in Fig. 6. These results show that inclusion of the upscattering effect of the fuel moderator (Case B) makes the fuel coefficient more negative in the initial unpoisoned FGS core. The reason for this is that the moderator coefficient is negative in the initial core. Thus the GAC method (Case A) of calculating the fuel coefficient is conservative in this case. However, if the moderator coefficient becomes positive, then the GAC method will over-predict the magnitude of the fuel coefficient. According to GAC (see Fig. 4.3-3D of Ref. 2), the moderator coefficient becomes positive at the end of the equilibrium cycle and near the HTGR operating temperature.

Fig. 5. Effect of fuel temperature on k_{∞} (unpoisoned core).

Fig. 6. Fuel temperature coefficient (unpoisoned core).

The fuel coefficients for both Case A and Case B are very similar to the $(1/p)(dp/dT)$ component (Table XIII) of the isothermal coefficient. This indicates that the Doppler effect is the main contributor to the fuel coefficient even if the temperature effect of fuel moderator upscattering is included. Our results for the fuel coefficient are in reasonable agreement with GAC results for the BOL core. As expected, the agreement for Case A is better than that for Case B and is within 10% over the entire temperature range studied.

The contributions of ^{235}U and ^{232}Th to the fuel temperature coefficient are given in Table XV for both Case A and Case B. As was the case for the isothermal coefficient (Table XIV), these two nuclides are the principal contributors to the fuel coefficient. The contribution from ^{232}Th is essentially the same whether or not the temperature effect of the fuel moderator is included. On the other hand, inclusion of this effect increases the ^{235}U contribution by 8 to 35%. These observations can be understood from the following considerations. Over 50% of the ^{232}Th absorptions occur above the thermal energy range. Furthermore, changes in ^{232}Th absorptions with fuel temperature result mostly from Doppler broadening of the thorium resonances and are therefore relatively insensitive to changes in the thermal neutron spectrum. Thus one would expect the contributions from ^{232}Th to be quite similar for Cases A and B. As a matter of fact, the contribution of ^{232}Th to the fuel coefficient is comparable to its contribution to the net isothermal coefficient. In contrast to ^{232}Th , almost 90% of the ^{235}U absorptions occur in the thermal energy range so that the contribution of ^{235}U is very sensitive to the thermal neutron spectrum. Inclusion of the fuel moderator temperature effect in the fuel coefficient results in some

TABLE XV
CONTRIBUTIONS OF ^{235}U AND ^{232}Th TO FUEL TEMPERATURE COEFFICIENT
FOR UNPOISONED CORE ($10^{-5}/\text{K}$)

T(K)	^{235}U		^{232}Th		Other ^c		Total	
	Case A ^a	Case B ^b	Case A	Case B	Case A	Case B	Case A	Case B
300	-3.29	-3.56	-5.94	-5.93	+0.29	+0.31	-8.94	-9.18
500	-2.74	-3.07	-4.73	-4.71	+0.24	+0.23	-7.23	-7.55
800	-2.01	-2.40	-3.32	-3.30	+0.17	+0.15	-5.16	-5.55
1200	-1.47	-1.90	-2.31	-2.31	+0.11	+0.08	-3.67	-4.13
1700	-1.11	-1.50	-1.67	-1.71	+0.07	+0.03	-2.71	-3.18
2300	-0.87	-1.11	-1.28	-1.35	+0.05	+0.02	-2.10	-2.44
3000	-0.66	-0.83	-0.94	-1.02	+0.07	+0.01	-1.53	-1.84

^aFuel moderator upscattering temperature effect not included.

^bFuel moderator temperature effect included.

^cComputed from the total fuel coefficient minus the contributions from ^{235}U and ^{232}Th .

hardening of the thermal neutron spectrum with increasing fuel temperature. This explains why the contribution from ^{235}U is larger for Case B than for Case A.

The fact that the contribution from ^{235}U is almost as large as that from ^{232}Th is surprising if one assumes that it is due to Doppler broadening of the ^{235}U resonances. A detailed analysis of the ^{235}U contribution shows that this assumption is incorrect. The contribution from ^{235}U arises not from changes in ^{235}U absorptions in the fast energy range, but from changes in ^{235}U absorptions in the thermal energy range. However, these changes are not caused primarily by a shift in the thermal neutron spectrum, but by a general depletion of thermal neutrons due to increased resonance absorption in the thorium. Thus, although the thermal neutron spectrum is relatively unaffected by increasing fuel temperature (particularly for Case A), the number of thermal absorptions in ^{235}U decreases with increasing fuel temperature.

An analysis by the vendor of the U and Th contributions to the fuel coefficient for the initial FGS core is apparently not available in the published literature. However, such an analysis is available¹⁸ at 1 200 K for the Ft. St. Vrain (FSV) initial core and results for FGS should not be markedly different. For FSV, the vendor gives the following values at 1 200 K:

$$\begin{aligned} &-2.32 \times 10^{-5} \text{ for } ^{232}\text{Th} \\ &-1.66 \times 10^{-5} \text{ for } ^{235}\text{U}. \end{aligned}$$

These values should be compared with those for Case A in Table XV. This comparison shows almost perfect agreement for ^{232}Th but the vendor value for ^{235}U is 13% larger than that calculated by the LASL for the FGS. It should be pointed out, however, that the vendor values for FSV include the effect of burnable poison while the LASL values for FGS do not. Inclusion of burnable poison is not expected to affect the contribution from ^{232}Th but it should increase the contribution from ^{235}U slightly (see Sec. III.C.), thus improving the agreement in the values for ^{235}U .

The effect of moderator temperature on k_{∞} is shown in Fig. 7 for both Case A and Case B. Moderator temperature coefficients, derived from the

Fig. 7. Effect of moderator temperature on k_{∞} (unpoisoned core).

curves in Fig. 7, are shown in Fig. 8. Whether or not the fuel moderator temperature effect is included in the moderator coefficient affects the results by about 20% over most of the temperature range. This is reasonable considering the fact that the fuel moderator comprises 20% of the total moderator in the core.

The moderator coefficients in Fig. 8 are considerably smaller in magnitude than those reported by GAC (Fig. 4.3-3A of Ref. 2), particularly at

Fig. 8. Moderator temperature coefficient (unpoisoned core).

elevated temperatures. We believe that the discrepancy is probably due to the fact that the GAC moderator coefficient is computed from the difference between the isothermal and fuel coefficients computed from a one-dimensional full-reactor model. Thus the GAC moderator coefficient includes the effect of neutron leakage from the core. As we have shown in Fig. 4, leakage makes a negative contribution to the isothermal coefficient. Although the leakage contribution is small compared to the net isothermal coefficient, it is not small compared to the moderator coefficient. However, the leakage contribution can be attributed to the moderator coefficient only if this contribution is due primarily to changes in thermal neutron leakage. This question is being investigated further.

Contributions of ^{235}U and ^{232}Th to the moderator coefficient are shown in Table XVI. In this case the contributions are principally due to hardening of the thermal neutron spectrum and one would expect this effect to be larger for Case A. Note that ^{235}U is the main contributor up to about 1 200 K. Furthermore in this temperature range the contribution from ^{235}U is negative whereas that from ^{232}Th is positive. Beyond 1 200 K the signs change and ^{235}U is no longer the dominating component. Thus the contributions of these two nuclides to the moderator coefficient exhibit a more complex behavior

than is the case for the fuel coefficient. For ^{235}U , hardening of the thermal neutron spectrum with increasing moderator temperature initially causes both a decrease in the effective thermal absorption cross section ($\bar{\sigma}_a$) and an increase in $\bar{\alpha}$ (thermal capture-to-fission cross-section ratio). Both of these changes produce negative effects. Beyond about 1 700 K, $\bar{\alpha}$ decreases which offsets the decrease in $\bar{\sigma}_a$ to produce a small net positive effect. For ^{232}Th , the initial positive effect is due to the decrease in $\bar{\sigma}_a$. Beyond about 1 700 K this effect is offset by an increase in the epithermal flux which results in an increase in absorptions in the epithermal ^{232}Th resonances, thus producing a net negative effect.

A measure of the separability of the fuel and moderator components of the temperature coefficient can be obtained by comparing the sum of these two components with the net isothermal coefficient. This has been done in Table XVII where values for the isothermal coefficient are those obtained with the fuel pin cell model (see Sec. III.A.). Using the GAC definitions (Case A) of the fuel and moderator coefficients, these results show that the fuel and moderator components are indeed separable over the entire temperature range. These results were expected for Case A, but they serve the additional purpose of verifying the consistency among the LASL-calculated

TABLE XVI
CONTRIBUTIONS OF ^{235}U AND ^{232}Th TO MODERATOR TEMPERATURE COEFFICIENT
FOR UNPOISONED CORE ($10^{-5}/\text{K}$)

T(K)	^{235}U		^{232}Th		Other ^c		Total	
	Case A ^a	Case B ^b	Case A	Case B	Case A	Case B	Case A	Case B
300	-1.50	-1.26	+0.06	+0.04	-0.13	-0.12	-1.57	-1.34
500	-1.40	-1.20	+0.19	+0.17	+0.01	+0.01	-1.20	-1.02
800	-1.03	-0.93	+0.37	+0.35	+0.02	+0.02	-0.64	-0.56
1200	-0.37	-0.39	+0.20	+0.21	-0.03	-0.01	-0.20	-0.19
1700	+0.06	-0.01	-0.08	-0.03	-0.11	-0.09	-0.13	-0.13
2300	+0.11	+0.07	-0.22	-0.16	-0.12	-0.10	-0.23	-0.19
3000	+0.01	-0.01	-0.26	-0.21	-0.14	-0.11	-0.39	-0.33

^aFuel moderator upscattering temperature effect included.

^bFuel moderator temperature effect not included.

^cComputed from the total moderator coefficient minus the contributions from ^{235}U and ^{232}Th .

TABLE XVII
COMPARISON OF FUEL PLUS MODERATOR
COEFFICIENTS WITH NET ISOTHERMAL COEFFICIENT
($-10^{-5}/K$)

T(K)	<u>Fuel + Moderator</u>		<u>Net Isothermal</u>
	<u>Case A^a</u>	<u>Case B^b</u>	
300	10.51	10.52	10.51
500	8.43	8.57	8.44
800	5.80	6.11	5.81
1200	3.87	4.32	3.86
1700	2.84	3.31	2.83
2300	2.33	2.63	2.32
3000	1.92	2.17	1.94

^aFuel moderator temperature effect included in moderator coefficient.

^bFuel moderator temperature effect included in fuel coefficient.

values of the fuel, moderator, and net temperature coefficients. If the fuel moderator temperature effect is included in the fuel coefficient (Case B), which we believe should be the case, the two components are not separable if there is a large temperature differential between the fuel pins and the bulk moderator. For physically reasonable temperature differentials, however, the fuel and moderator components for Case B are also expected to be separable to a close approximation (note the Case B results at 300 K). Thus for Case B a more appropriate procedure for calculating the fuel and moderator components at a particular temperature is to start from the isothermal case at that temperature, and then make temperature perturbations of the desired component.

C. Effect of Burnable Poison on Isothermal Coefficient

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. In the initial core, poison pins are loaded in the standard fuel elements but not in the control elements. Although six holes are available in the standard fuel elements for loading poison pins, only three holes

are used in a portion of the elements and only one hole is used in the remainder.

The FGS PSAR gives a poison pin diameter of 1.016 cm (0.4 in.) and indicates that the pins contain about five weight percent natural boron. From other sources, there were indications that both the poison pin diameter and the boron loading might be varied with position in the core but specific information was not available at the time the calculations were performed. Because the aim of our study was not a precise determination of burnable poison worth but to determine the effect of this component on the temperature coefficient, we used a nominal pin diameter of 1.016 cm to specify the burnable poison cell model described in Table XVIII. The cell is infinitely long with a reflective boundary condition at the center and a white condition at the outer boundary. In this model the poison pin and associated gap are represented explicitly, followed by a homogenized core medium. The outside radius of the cell is such that the correct core-average volume fractions are obtained for the components represented. The poison pin density was assumed to be 1.6 g/cm^3 and the weight fraction of natural boron was adjusted to yield the same worth at room temperature as that ($-10\% \Delta k$) given in the PSAR. This procedure yielded a boron weight fraction of 1.9% or $3.04 \times 10^{-2} \text{ g/cm}^3$ of natural boron in the poison pin. Obviously the model cannot take into account the interaction effect between poison pins. We do not expect this effect to be large but, because of the way the pins are distributed, may not be negligible either.

Temperature-dependent multiplication factors and the resulting temperature coefficients are shown, respectively, in Figs. 9 and 10. For comparison purposes results obtained previously without burnable poison are also shown. As was anticipated, the effect of burnable poison is to make the isothermal

TABLE XVIII
ONE-DIMENSIONAL BURNABLE POISON CELL MODEL

<u>Region</u>	<u>Outer Radius (cm)</u>	<u>Mesh Intervals</u>
Poison Pin	0.5080	8
Poison Pin Hole	0.6337	2
Homogenized Core Medium (excluding poison pins)	15.13	30

Fig. 9. Effect of initial burnable poison loading on k_{∞} .

temperature coefficient more negative. The magnitude of the effect is 17% at room temperature (300 K) and 25% at 1 200 K. These results are in agreement with qualitative arguments made by the vendor.

The negative effect of burnable poison on the temperature coefficient arises from the fact that the ratio of neutrons absorbed in the poison pin to the total neutrons absorbed in the cell increases with core temperature as shown in Fig. 11. The reason for this is that thermal-neutron self-shielding

Fig. 10. Effect of initial burnable poison loading on isothermal temperature coefficient.

Fig. 11. Percentage of neutrons absorbed in poison pins.

in the poison pins is significant and decreases with core temperature as shown in Table XIX. In other words, the poison pin thermal shielding factor is significantly less than unity and increases with core temperature. On the other hand, thermal self-shielding in the fuel pins is small (fuel pin thermal shielding factor is almost unity) and changes little with core temperature (see Table X). Thus although unshielded microscopic thermal cross sections are affected in approximately the same manner

TABLE XIX
POISON PIN SHIELDING FACTORS
($\bar{\phi}_{\text{pin}}/\bar{\phi}_{\text{cell}}$)

Energy Group	300K	800K	1700K	3000K
1	0.9807	0.9819	0.9829	0.9836
2	0.9965	0.9967	0.9968	0.9969
3	0.9856	0.9861	0.9865	0.9869
4	0.9455	0.9456	0.9455	0.9454
5	0.9152	0.9149	0.9129	0.9081
6	0.8470	0.8383	0.8172	0.8221
7	0.6968	0.7043	0.7205	0.7335
8	0.5777	0.5924	0.6021	0.6104
9	0.4339	0.4462	0.4661	0.4790
thermal ^a	0.6840	0.7093	0.7458	0.7827

^aFlux-weighted value for groups six through nine.

in both the poison pin and the fuel by a shift in the thermal spectrum, this effect is offset to some extent in the poison pin by the change in the thermal self-shielding factor. This is illustrated in Fig. 12 which shows the effective (i.e., shielded) thermal absorption cross section for the boron in the poison pin and that for the ^{235}U in the fuel pin as a function of core temperature.

The contributions of ^{235}U and ^{232}Th to the isothermal temperature coefficient with the effect of burnable poison included are given in Table XX. As expected the contribution from ^{232}Th is not greatly different from the case in which burnable poison is not present (Table XIV), but the contribution from ^{235}U has increased significantly. The change in the contributions from other nuclides reflects the negative contribution from the boron in the poison pins. For the FSV initial core with burnable poison, the vendor has published¹⁸ the following values at 1 200 K:

$$\begin{aligned} &-2.11 \times 10^{-5} \text{ for } ^{232}\text{Th} \\ &-2.43 \times 10^{-5} \text{ for } ^{235}\text{U}. \end{aligned}$$

The LASL values for FGS at 1 200 K are within about 2% of the FSV values.

After the above calculations were performed, more specific information on the poison pin size

Fig. 12. Effective thermal absorption cross section for boron in poison pin and ^{235}U in fuel pin.

TABLE XX
CONTRIBUTIONS OF ^{235}U AND ^{232}Th TO ISOTHERMAL
TEMPERATURE COEFFICIENT WITH BURNABLE POISON
($\times 10^{-5}/\text{K}$)

T(K)	^{235}U	^{232}Th	Other ^a	Total
300	6.06	5.85	0.54	12.45
500	5.17	4.44	0.41	10.02
800	3.79	2.91	0.31	7.01
1200	2.47	2.06	0.36	4.89
1700	1.71	1.66	0.47	3.84
2300	1.41	1.37	0.51	3.29
3000	1.24	1.07	0.45	2.76

^aComputed from the total minus the contributions from ^{235}U and ^{232}Th .

and composition became available. This information (Table 4.3-9 of Ref. 2) indicates that two different poison pin sizes (1.270 and 1.016-cm-diam) are used in the initial loading where the larger pins are in the three-pin fuel elements. There are two different boron loadings in each pin type (for axial zoning) and therefore four different natural boron loadings ranging from 5.0×10^{-2} to $8.3 \times 10^{-3} \text{ g/cm}^3$. These loadings are considerably higher than that ($3.0 \times 10^{-2} \text{ g/cm}^3$) which we found necessary to yield a room temperature worth of 10% Δk using 1.016-cm-diam pins. The reason for this discrepancy is not known at this time but may be due in part to interaction effects among the poison pins. Another possibility is that the vendor loadings apply to reload segments rather than to the initial core.

D. Effect of Control Rods on Isothermal Coefficient

A one-dimensional (cylindrical) seven-region cell model, described in Table XXI, was used to study the effect of control rods on the isothermal temperature coefficient. This model represents explicitly the five regions in a single control rod including the spine, gap, inner sleeve, absorber compact, and outer sleeve. The control rod channel is also represented explicitly and this is followed by a homogenized core region containing the fuel and moderator.

Calculations were performed with various numbers of control rod pairs present in the core including 73 (all), 37, 19, and 13 rod pairs. The

TABLE XXI
ONE-DIMENSIONAL CONTROL ROD CELL MODEL

Region Description	Outer Radius (cm)	Mesh Intervals
Control Rod Spine	0.476	2
Gap (Void)	2.381	3
Control Rod Inner Sleeve	2.540	1
Control Rod Absorber Compact	4.286	30
Control Rod Outer Sleeve	4.445	1
Control Rod Channel	5.080	2
Homogenized Core Medium ^a (excluding control rods)	34.92, 49.05, 68.45, 82.76, 112.78	21

^aRadii correspond, respectively, to 73, 37, 19, 13, and 7 rod-pair cases.

associated control rod configurations are based on the rod withdrawal sequence given by the vendor and in all cases result in a uniform distribution of control rod pairs in the core. The amount of fuel material associated with each control rod (and therefore the outer radius of the cell) depends on the number of control rods present in the core. The cell is infinitely long with a reflective boundary condition at the center and a white (isotropic return) condition at the outer boundary.

The one-dimensional model cannot take into account the interaction between the two rods in a rod pair nor that between adjacent rod pairs. The minimum separation between rod pairs is about 96 cm whereas the separation between the two rods in a pair is only about 25 cm. Thus the interaction between rod pairs should be smaller than that between the two rods in a pair. The latter effect must be taken into account if the object is an accurate computation of the worth of a rod pair. This requires a two-dimensional model in which both rods in the pair are represented. However, the one-dimensional model should be adequate to calculate relative changes in rod worth with core temperature if the interaction effect does not change significantly with temperature.

Results of the k -calculations for the various control rod configurations and temperatures are plotted in Fig. 13. For comparison purposes, results

Fig. 13. Effect of control rods on k_{∞} .

obtained previously without control rods are also shown. Temperature coefficients derived from the curves in Fig. 13 are plotted in Fig. 14. As was the case with the burnable poison, the effect of control rods is to make the isothermal temperature coefficient more negative. The magnitude of the effect depends on both the core temperature and the number of control rods inserted.

Temperature coefficients for the 73 and 37 rod-pair cases are only of academic interest because the

Fig. 14. Effect of control rods on isothermal temperature coefficient.

reactor cannot be made critical with these configurations (burnable poison and neutron leakage, which are not included in the model, are together worth about $-0.15 \Delta k$). According to Table 4.3.2-7 of Ref. 1, the reactor can be made critical at room temperature (300 K) with 19 rod pairs inserted. For this cold critical configuration, our calculations indicate that the temperature coefficient is 19% more negative than the unrodded configuration. For the hot (1200 K) critical configuration, in which 13 rod pairs are expected to be present before Xenon equilibrium is established, the temperature coefficient is 20% more negative than the unrodded configuration. Only seven control rod pairs are expected to remain after equilibrium xenon and samarium are established at design power and temperature. The curves for this case should lie approximately halfway between those for the 13 rod-pair case and those for the unrodded case in Figs. 13 and 14.

The negative contribution of control rods to the temperature coefficient is due to the same mechanism as that from the burnable poison; that is, the effective thermal absorption cross section for the boron in the control rod decreases more slowly with increasing temperature than does that for the ^{235}U in the fuel pin. However, the control rod absorber is much more heavily self-shielded than was the case for the burnable poison. Control rod shielding factors, defined as the ratio of the average flux in the control rod absorber ($\bar{\phi}_{\text{cra}}$) to the average flux in the cell ($\bar{\phi}_{\text{cell}}$), are given in Table XXII as a function of energy group for the various cells at 300 K.

In the thermal energy range ($g = 6, \dots, 9$), the effective control rod absorption cross section

$$\bar{\sigma}_g = \left(\frac{\bar{\phi}_{\text{cra}}}{\bar{\phi}_{\text{cell}}} \right)_g \sigma_g \approx \text{constant}$$

where σ_g is the boron absorption cross section for group g . Therefore the flux (ϕ_g)-weighted thermal absorption cross section

$$\bar{\sigma}_{\text{thermal}} = \frac{\sum_{g=6}^9 \bar{\sigma}_g \phi_g}{\sum_{g=6}^9 \phi_g} \approx \text{constant}$$

independent of the thermal neutron spectrum and therefore independent of temperature. This behavior

TABLE XXII
CONTROL ROD SHIELDING FACTORS
($\bar{\phi}_{\text{cra}}/\bar{\phi}_{\text{cell}}$) AT 300 K

Energy Group	Number of Control Rod Pairs Inserted			
	73	37	19	13
1	8.071-01	7.522-01	6.992-01	6.715-01
2	6.895-01	6.444-01	5.999-01	5.770-01
3	2.338-01	2.142-01	1.975-01	1.898-01
4	7.089-02	6.448-02	5.924-02	5.693-02
5	4.205-02	3.818-02	3.505-02	3.369-02
6	2.158-02	1.942-02	1.776-02	1.705-02
7	8.695-03	7.728-03	7.015-03	6.722-03
8	5.067-03	4.471-03	4.041-03	3.867-03
9	2.761-03	2.429-03	2.191-03	2.095-03
thermal ^a	1.455-02	1.291-02	1.172-02	1.123-02

^a Flux-averaged value for groups six through nine.

is illustrated in Table XXIII for the 37 rod-pair case. The effective thermal absorption cross section for the ^{235}U in the fuel pins is also given in the table for comparison purposes. As a result of this behavior the fraction of neutrons absorbed in the control rod (and therefore the control rod worth) increases with core temperature.

The interaction between the two rods in a pair has been found by the vendor to reduce the worth of a single rod by 15% at room temperature.¹ That is, the worth of a rod pair was found to be 1.7 times

TABLE XXIII
EFFECTIVE THERMAL ABSORPTION CROSS SECTION
FOR NATURAL BORON IN CONTROL ROD ABSORBER
AND ^{235}U IN FUEL PIN

T(K)	Cross Section (barns)	
	Boron	^{235}U
300	2.326	292.3
500	2.359	269.2
800	2.340	240.9
1200	2.307	212.1
1700	2.285	184.6
2300	2.278	158.4
3000	2.272	136.5

the worth of a single rod. If we apply this correction factor (which we intend to verify in future studies) to our results, we find that at room temperature the total worth of all control rod pairs is 0.280 Δk . This value is 11% larger than that (0.253 Δk) quoted in the FGS PSAR. The agreement is reasonable considering the simple cell model used in our calculations.

E. Effect of Xe and Sm on Isothermal Coefficient

One-dimensional cell calculations were performed to determine the effect of ^{135}Xe and ^{149}Sm on the isothermal temperature coefficient. These two fission products are the most significant in an HTGR with respect to their effect on both reactivity and the temperature coefficient of reactivity. Calculations were performed for five different cases including equilibrium and peak Xe with no Sm, equilibrium and peak Sm with no Xe, and equilibrium Xe and Sm.

The four-region fuel pin cell model described in Table VII was used in the calculations. Average atom densities of Xe and Sm in the fuel stick were computed using the fission yield and half-life data shown in Table XXIV. The fission yields are recommended¹⁹ values for the thermal fission of ^{235}U and half-life (decay constant) values are from Ref. 20. The yield data for ^{135}I and ^{149}Pm are based on the assumption that these nuclides are formed directly from fission rather than from decay of their parents. This is a good approximation because ^{135}Te and ^{149}Nd have much shorter half-lives (19.2 s and 6 228 s, respectively) than their corresponding daughter products.

Using the above yield and decay data, and a total equilibrium power level of 3 000 MW(t) at a

core average temperature of 1 200 K, the following average concentrations of Xe and Sm in the fuel stick are obtained:

Isotope	Atom Density ($10^{24}/\text{cm}^3$)	
	Equilibrium	Peak
^{135}Xe	8.343×10^{-10}	1.699×10^{-9}
^{149}Sm	5.584×10^{-9}	9.590×10^{-9}

Peak values are based on the assumption of a rapid shutdown from full power after equilibrium Xe and Sm have been established. The peak Xe concentration occurs about nine hours after shutdown and Sm reaches 90% of its maximum in about four and one-half days.

Results of the k-calculations at various isothermal cell temperatures for each of the five cases are plotted in Fig. 15. Also shown for comparison are results obtained previously for the clean, unpoisoned core. The results shown in Fig. 15 do not include the effects of burnable poison or control rods. Isothermal temperature coefficients derived from the curves in Fig. 15 are plotted in

Fig. 15. Effect of Xe and Sm on k_{∞} .

TABLE XXIV
FISSION YIELD AND DECAY DATA FOR
 ^{135}Xe AND ^{149}Sm

Isotope	Fission Yield (%)	Half-Life (s)	Decay Constant (s^{-1})
^{135}I	6.31	2.371×10^4	2.924×10^{-5}
^{135}Xe	0.20	3.302×10^4	2.100×10^{-5}
^{149}Pm	1.06	1.912×10^5	3.626×10^{-6}
^{149}Sm	0	Stable	0

Fig. 16. Except for Sm at 300 K, these results indicate that both Xe and Sm make a positive contribution to the temperature coefficient over the entire temperature range. Equilibrium Xe reduces the magnitude of the temperature coefficient by 2% at 300 K and by 39% at 1 200 K. The effect of equilibrium Sm is a 3% increase at 300 K and a 7% decrease at 1 200 K. With equilibrium Xe and Sm, the magnitude of the temperature coefficient is increased by 1% at 300 K and reduced by 45% at 1 200 K. This indicates that the effects of Xe and Sm are essentially additive (separable). The largest effect on the temperature coefficient occurs at 1 200 K for the peak Xe case. For this case the coefficient is reduced by 75% (from -3.86×10^{-5} to -0.97×10^{-5}).

The contributions of various nuclides to the temperature coefficient for the equilibrium Xe and Sm case are given in Table XXV. Both Xe and Sm are primarily thermal neutron absorbers and their absorption cross sections exhibit strong non-1/v variations in the thermal energy range. The effect of these nuclides on the temperature coefficient arises from the shift in the thermal neutron spectrum to higher energies with increasing core temperature. Thus these nuclides affect primarily the moderator component of the net isothermal coefficient. Because a majority of the absorptions in ^{232}Th occur

Fig. 16. Effect of Xe and Sm on isothermal temperature coefficient.

TABLE XXV
CONTRIBUTION OF VARIOUS NUCLIDES TO ISOTHERMAL COEFFICIENT WITH EQUILIBRIUM Xe AND Sm
($10^{-5}/\text{K}$)

T(K)	^{232}Th	^{235}U	^{135}Xe	^{149}Sm	Other ^a	Total
300	-6.06	-5.02	+0.44	-0.17	+0.21	-10.60
500	-4.75	-3.96	+0.94	0	+0.23	-7.54
800	-3.28	-2.38	+1.33	+0.17	+0.13	-4.03
1200	-2.41	-1.15	+1.19	+0.20	+0.03	-2.14
1700	-1.95	-0.57	+0.84	+0.15	-0.05	-1.58
2300	-1.62	-0.44	+0.58	+0.10	-0.07	-1.45
3000	-1.26	-0.47	+0.34	+0.05	-0.07	-1.41

^aFrom total minus contributions from nuclides listed.

above the thermal energy range, its contribution to the isothermal temperature coefficient is not greatly affected by the presence of Xe and Sm. This can be seen by comparing Table XXV with Table XIV which gives the contributions of ^{232}Th and ^{235}U for the clean, unpoisoned core. On the other hand the contribution from ^{235}U , which is principally a thermal absorber, is affected by the presence of Xe and Sm. This is particularly true above about 500 K where the contributions from Xe and Sm become increasingly important compared to that from ^{235}U .

The positive contribution of ^{135}Xe to the isothermal coefficient increases with temperature up to about 800 K, and then decreases beyond this temperature. This behavior is consistent with the absorption cross section of ^{135}Xe which is almost constant up to about 0.1 eV and then falls sharply between 0.1 and 1.0 eV. The contribution from ^{149}Sm is negative at 300 K, zero at 500 K, and positive beyond 500 K with the largest positive contribution occurring at 1 200 K. This behavior is also consistent with the absorption cross section of ^{149}Sm which has a resonance at ~ 0.1 eV (~ 15 000 barn peak) and another at ~ 0.9 eV (~ 2 000 barn peak) with a deep valley (~ 100 barn floor) between the two resonances.

F. Effect of ^{233}U on Isothermal Coefficient

The beginning-of-life (BOL) core does not contain ^{233}U , but 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 near 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 $C/(^{235}\text{U}+^{233}\text{U})$ 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 detailed core compositions at the EOEC is not available in the FGS literature.

The one-dimensional fuel pin cell model (Table VII) 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. Results of the k -calculations are summarized in Fig. 17. It is clear from this figure that the reactivity worth of ^{233}U is greater than that for ^{235}U on an 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 dependence of $\bar{\sigma}_a$, the ratio $\bar{\sigma}_a(233)/\bar{\sigma}_a(235)$ varies from 1.10 at 300 K to 1.49 at

Fig. 17. Effect of ^{233}U on k_∞ .

3 000 K as shown in Fig. 18. The ratio $\bar{\eta}(233)/\bar{\eta}(235)$ is essentially independent of temperature and is equal to about 1.10. The results in Fig. 17 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. 17 are shown in Fig. 19. 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 50% of the power in ^{233}U . Thus, considering only the fissile uranium isotopic split, the results for the ^{233}U isotopic fraction of 0.50 are representative of the EOEC for the nonrecycle case. For this case the magnitude of the temperature coefficient is reduced by 10% at 300 K and 33% at 1 200 K compared to the values for the BOL core (no ^{233}U). At higher temperatures the reduction is even larger and beyond about 2 750 K the temperature coefficient becomes positive.

The separate effects of burnable poison, control rods, and equilibrium Xe and Sm on the isothermal temperature coefficient were discussed previously. Both the burnable poison and inserted

Fig. 18. ^{233}U to ^{235}U effective thermal absorption cross-section ratio (50% ^{233}U case).

Fig. 19. Effect of ^{233}U on isothermal coefficient.

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 (except perhaps the central control rod pair) 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 (see Table XXV), will be present at the EOE. From these considerations, one would expect the temperature coefficient for the EOE to be less negative than that shown in Fig. 19 for the 50% ^{233}U case.

There are additional factors that must be taken into account at the EOE before we can make a more definite statement. One factor is that the contributions from other nuclides (fission products and actinides) must be considered at the EOE. Other fission products that are taken into account by the vendor include ^{143}Nd , ^{103}Rh , ^{151}Sm , ^{148}Pm , ^{131}Xe , ^{147}Pm , ^{145}Nd , ^{133}Cs , ^{99}Tc , ^{152}Sm , ^{153}Eu , ^{150}Sm , ^{95}Mo , ^{154}Eu , ^{155}Eu , and a nonsaturating aggregate to represent other fission products. With respect

to relative reactivity importance at the EOE, these fission products follow ^{135}Xe and ^{149}Sm in the order listed.¹⁹ In addition to ^{233}U and those actinides that are present in the initial core (^{232}Th , ^{234}U , ^{235}U , ^{236}U , and ^{238}U), the vendor takes into account the effects of several other actinides produced during burnup including ^{233}Pa , ^{237}Np , ^{239}Np , ^{238}Pu , ^{239}Pu , ^{240}Pu , and ^{241}Pu .

We do not expect that these additional fission products and actinides will make large contributions to the temperature coefficient. This is based on the vendor's analysis¹⁸ of the Ft. St. Vrain (FSV) isothermal coefficient at 1200 K after a 1-yr burn. That analysis indicates that the five largest contributors are ^{232}Th , ^{235}U , ^{135}Xe , ^{233}U , and ^{149}Sm . The net contribution from other sources (^{233}Pa , ^{103}Rh , ^{151}Sm , ^{237}Np , leakage, and "other") is the same as that from ^{149}Sm (but negative), which is the least important of the top five contributors.

Another factor that must be considered is that the C/U_{fissile} atom ratio, where $U_{\text{fissile}} = ^{235}\text{U} + ^{233}\text{U}$, is larger at EOE than at BOL. In other words, the total fissile loading at EOE is smaller than that at BOL so that the multiplication factors at EOE are smaller than those shown in Fig. 17 for the 50% ^{233}U case. The smaller total fissile loading could affect the temperature coefficient in two ways. First, the lower fissile loading changes the fraction of neutrons absorbed in fuel material as compared to other absorbers. Second, the lower fissile loading results in a softer thermal neutron spectrum at any given temperature. We are performing calculations with a more realistic EOE total fissile loading. Preliminary indications are that the results shown in Fig. 17 for the 50% ^{233}U case are not significantly affected by the lower fissile fuel loading.

Still another factor that must be considered is that neutron leakage, which makes a negative contribution to the temperature coefficient (see Sec. III. A.), may be sufficient to keep the isothermal coefficient at EOE from becoming positive. However, the thorium loading at EOE is about 14% smaller than that at BOL (Table 4.3-8 of Ref. 2) and this results in a less negative coefficient. These

factors are being taken into account in calculations currently in progress.

G. Beginning-of-Cycle Isothermal Coefficient Using GAC and LASL Cross Sections

The effects of burnable poison, control rods, xenon, samarium, and ^{233}U on the isothermal temperature coefficient were studied separately in order to obtain a detailed understanding of these effects and to evaluate their relative importance. In this section, the combined effects of the initial burnable poison loading, seven control rod pairs, and equilibrium xenon and samarium are considered. This particular condition corresponds to the beginning of the initial cycle (BOIC) after xenon and samarium have equilibrated but before significant amounts of ^{233}U have been produced. Results for the BOIC are first compared with those obtained previously for the unpoisoned core. Independently generated cross sections were also used to compute the BOIC temperature coefficient and the results are compared with those obtained using GAC cross sections.

The seven-region one-dimensional control rod cell model described previously (Table XXI) was used to calculate the multiplication factor as a function of core temperature. This model represents the control rod explicitly and the amount of homogenized core medium (region 7 of the model) associated with the control rod corresponds to the case in which seven control rod pairs are present in the core. Region 7 also contains equilibrium xenon and samarium and burnable poison. The burnable poison was homogenized using shielding factors obtained from the poison pin cell model (Table XVIII).

Isothermal temperature coefficients computed for the BOIC conditions described above are shown in Fig. 20. For comparison purposes, the results for the unpoisoned core are also shown in the figure. The net contribution of the various poisons to the isothermal coefficient is positive in the temperature interval 800 - 1400 K and negative elsewhere. These results indicate that the separate effects of burnable poison, seven control rod pairs, and equilibrium xenon and samarium are essentially additive. Also shown in Fig. 20 are the values given in the FGS PSAR at room and design temperature. These values are in better agreement with the LASL results for the unpoisoned core than with the results for the BOIC.

Fig. 20. Net effect of burnable poison, seven control rod pairs, and equilibrium Xe and Sm on isothermal coefficient.

Generation of cross sections from the GAC nine-group library by interpolation is a fast procedure. However, the initial creation of a broad-group

Fig. 21. Comparison of $k_{\infty}(T)$ using the LASL and GAC cross sections.

library requires a large amount of data processing (see Sec. II.B.). For this reason, and because the LASL cross section processing system (Fig. 1) is only preliminary, nine-group LASL cross sections were generated at only three temperatures (300, 1 200, and 3 000 K). As in the other calculations, GAC cross sections were generated for seven different core temperatures.

Multiplication factors obtained with the LASL and GAC cross-section sets are shown in Fig. 21. Note that the multiplication factors obtained with the LASL cross sections are smaller than those obtained with the GAC cross sections. The differences range between 0.012 Δk at 300 K and 0.026 Δk at 3 000 K. Both the magnitude and direction of these differences are reasonable considering the fact that heterogeneity effects have not yet been included in the LASL cross sections (see Sec. II.B.). Self-shielding in the fuel pins and in the fertile particles in the resonance energy range are expected to be the most important of these effects. Therefore inclusion of these effects should result principally in a smaller group three absorption cross section for thorium. A detailed comparison of the LASL and GAC cross sections shows that this is indeed the case.

The reactivity change between 300 and 1 200 K is almost the same with both cross-section sets while the change between 1 200 and 3 000 K is 25% larger using the LASL cross sections. In other words, there is little difference between the average temperature coefficients in the temperature range 300 - 1 200 K but in the range 1 200 - 3 000 K the LASL cross sections yield an average coefficient that is 25% more negative. This is illustrated in Fig. 22 where the isothermal temperature coefficient is plotted as a function of core temperature.

Because the Doppler effect in thorium is the principal contributor to the temperature coefficient, and because self-shielding effects in general attenuate changes in broad-group cross sections with temperature, the inclusion of grain and fuel pin self-shielding effects in the broad-group cross sections should result in a smaller (less negative) temperature coefficient over the entire temperature range. From GAC results (Fig. Q4.3.2.3-2 of Ref. 1), one would expect the effect to be 10 - 20%. Our results shown in Fig. 22 are consistent with

Fig. 22. Comparison of isothermal coefficient using LASL and GAC cross sections.

this above 1 200 K. Below 1 200 K, however, it appears that the LASL cross sections may yield a somewhat less negative temperature coefficient than the GAC cross sections when fuel self-shielding effects are taken into account in the LASL sets. As mentioned in Sec. II.B., work is in progress to incorporate such effects into the LASL cross section processing code system.

Shielding factors computed at 1 200 K for the burnable poison pins and the control rod absorber compact using the LASL cross sections are compared in Table XXVI with those obtained with the GAC cross sections. The general agreement in these shielding factors is primarily a reflection of the agreement in the broad-group boron cross sections. Since boron is primarily a thermal absorber whose cross-section variation is well known, these results also provide an indirect integral check of the thermal neutron spectrum over which the cross sections were averaged.

IV. POWER DISTRIBUTIONS

One- and two-dimensional models were used to calculate radial and axial power distributions at design conditions. Most of the one-dimensional calculations were performed using nine-group cross sections. Cross sections for the nine-group energy

TABLE XXVI
COMPARISON OF BURNABLE POISON AND CONTROL ROD
SHIELDING FACTORS OBTAINED USING THE LASL AND GAC
CROSS SECTIONS (1 200 K)

Group	Burnable Poison		Control Rod	
	LASL	GAC	LASL	GAC
1	0.983	0.982	0.650	0.630
2	0.997	0.997	0.568	0.541
3	0.987	0.986	0.181	0.179
4	0.946	0.946	0.0526	0.0532
5	0.915	0.914	0.0313	0.0313
6	0.838	0.825	0.0157	0.0138
7	0.715	0.713	0.00699	0.00680
8	0.597	0.596	0.00386	0.00379
9	0.443	0.431	0.00194	0.00198

structure, which was defined previously in Table IV, were interpolated for appropriate temperatures and compositions from the nine-group MICRON library described in Sec. II.B. Nine-group flux spectra from the one-dimensional calculations were used to collapse the cross sections to four energy groups according to the scheme 1, 2-3, 4, 5-9. The four-group cross sections were then used in two-dimensional (r-z) calculations.

A. One-Dimensional Radial Calculations

The one-dimensional (cylindrical) model used to calculate the gross radial power profile is described in Table XXVII. Zone 1 includes the central refueling region, Zone 2 the ring of six 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) at the core/reflector interface. Boundary conditions are reflective at the center and vacuum (no return) at the outer boundary. All calculations were performed without axial buckling.

Material compositions of the radial zones are volume-weighted averages over the axial and azimuthal directions. The thin buffer region 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. This buffer loading is used in the outer four rows of fuel pins in the standard fuel columns on the core periphery and in all fuel pins in control columns at the core/reflector interface. Except for the buffer region, a uniform radial and azimuthal fuel loading is used in the initial core. Relative to the core-average values, this results in the radial zoning for uranium and thorium shown in Table XXVII. Subsequent reload segments* employ more detailed radial zoning.

An examination of the equilibrium equations for ^{135}Xe and ^{149}Sm shows that the dominant spatial variable is the fissile atom density. Consequently, the radial zoning for these two nuclides was taken to be the same as that for uranium.

At the beginning of the initial cycle after xenon and samarium have equilibrated, control rods are present in the central refueling region (Ring 1)

* For refueling purposes the core is divided into four nearly equal segments each containing a uniform distribution of refueling regions. The core is refueled at the rate of one segment per year.

TABLE XXVII
ONE-DIMENSIONAL, SIX-REGION RADIAL MODEL OF FULL REACTOR

Zone	Description	Outer Radius (m)	Mesh Intervals	Relative Radial Zoning		
				Uranium	Thorium	Burnable Poison
1	Core Ring 1	0.503	6	1.012	0.947	1.475
2	Core Ring 2	1.330	9	1.012	0.947	0.820
3	Core Ring 3	2.192	10	1.012	0.947	1.147
4	Core Ring 4	4.075	20	1.012	0.947	0.894
5	Core Ring 5	4.219	4	0.841	1.731	1.806
6	Radial Reflector	5.248	21	0.0	0.0	0.0

and in six of the twelve refueling regions in Ring 3. These control rods were homogenized into their respective radial zones using self-shielding factors obtained from an auxiliary cell calculation (see Sec. III.D.) in which the control rod is represented explicitly. The distribution of burnable poison pins in the initial core, when averaged over the axial and azimuthal directions, results in the relative radial zoning shown in Table XXVII. As was the case for the control rods, self-shielding factors from an auxiliary cell calculation (see Sec. III.C.) were used to homogenize the burnable-poison pins into the five radial core zones.

Microscopic cross sections for the core materials correspond to initial core-average compositions and temperatures ($C/^{235}\text{U}$ atom ratio = 4 920, ^{232}Th atom density in the fuel stick = 0.00142 atoms/b-cm, fuel temperature = 1 160 K, and moderator temperature = 1 010 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 XXVIII. 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:

Component	Worth (Δk)
Equilibrium Xe and Sm	-0.039
Seven control-rod pairs	-0.037
Burnable poison	-0.099

Of the total worth for equilibrium Xe and Sm, 85% or -0.033 Δk is due to Xe. This value is in good agreement with the equilibrium Xe worth of -0.032 Δk reported by the vendor. The worth of the seven

control-rod pair configuration, but with the central rod pair inserted only halfway, can be inferred to be -0.027 Δk from Tables 4.3-23 and 4.3-24 of Ref. 2. Based on LASL results for the worth vs axial position of the central rod pair (see Sec. V.A.), the vendor results imply a worth of -0.031 Δk for the seven rod pair configuration. The LASL value of -0.037 Δk , when corrected for interaction effects between the two rods in a pair (see Sec. III.D.), is in exact agreement with the -0.031 Δk value. Finally, the computed burnable poison worth at design conditions agrees well with that (-0.10 Δk) reported in the GASSAR.

Normalized gross radial power profiles, $P(r)/\bar{P}$ where $\bar{P}$ is the volume-weighted average, are shown in Fig. 23 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 when inserted. Radial zoning of the burnable-poison pins has a similar but much smaller effect.

Case 1 corresponds to the hot-critical configuration at the beginning of the initial cycle (BOIC). Except for fuel depletion effects and perhaps partial insertion of the central rod pair, Case 4 is representative of the configuration at the end of the initial cycle (EOIC). Figure 23 shows that the gross radial peaking factor, $P_{\text{max}}/\bar{P}$, is 1.33 at BOIC. If the central (regulating) rod

TABLE XXVIII
SUMMARY OF ONE-DIMENSIONAL RADIAL CALCULATIONS

Case	Description	k_{eff}
1	Reference calculation with burnable poison, seven 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

Fig. 23. Normalized gross radial power distributions from one-dimensional model.

pair is not fully out at the EOIC, the peaking factor should be smaller than that (1.71) computed for Case 4. We have not yet calculated the effect of fuel depletion on the radial power profile at the EOIC. As can be seen from Fig. 23, the rapid depletion regions can be expected to shift from the outer to the inner core regions as the control rods are withdrawn during the cycle. This will tend to maintain similarity between the BOIC and the EOIC radial fuel distributions. Changes in fuel distribution due to depletion in any case should have a smaller effect on the power distribution than do control rods.²¹ 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. Taking into account the above considerations, 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 homogenized C/U atom ratio in the control column is almost twice as large as that for the six surrounding standard columns. In general all fuel sticks within a given refueling region are loaded with the same amount of fuel. However, there are only 76 fuel sticks in a control element as compared with 132 in a standard element. As a consequence of the difference in C/U, the thermal neutron flux (and therefore the power per fuel stick) should peak at the center of the control column when the control rod pair is not present. Insertion of the control-rod pair depresses the thermal neutron flux in the control column and the power per fuel stick should peak at the outer boundary of the refueling region.

A one-dimensional (cylindrical) two-region cell model of a refueling region (Table XXIX) was used to evaluate the intra-refueling-region power tilt. Reflective and isotropic return boundary conditions were applied, respectively, to the inner and outer boundaries of the cell. Calculations were performed

TABLE XXIX
ONE-DIMENSIONAL CELL MODEL OF A REFUELING REGION

<u>Region Description</u>	<u>Outer Radius (cm)</u>	<u>Mesh Intervals</u>
Control Fuel Column	19.0	17
Six Standard Fuel Columns	50.3	23

both with and without the control rod pair present in the control column. In the former case the control rod pair was homogenized into the control column taking into account self-shielding in the rods as computed for a single rod (see Sec. III.D.). Results of the refueling region calculations are shown in Fig. 24 where the fuel stick power (normalized to the average) is plotted as a function of radial position within the refueling region. The local peak/average value is 1.09 with the control rod pair present and 1.12 if the rod pair is not present. Hence, the rod pair shifts the location of the power peak from the center to the outside of the refueling region but has only a small effect on its maximum value.

Another local effect is the power distribution near a burnable poison pin. This effect, which was

Fig. 24. Normalized power per fuel stick within a refueling region.

evaluated with the burnable poison cell model (Table XVIII), results in a local peak/average value of 1.01. Thus, we obtain a maximum local peaking factor of 1.13 (1.12×1.01). 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 1.93 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 may not be 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.

In addition to the overall radial peaking factor, which is applied to the entire fuel stick in the hot-channel thermal analysis, there is a small variation in the fission density from the center to the surface of the fuel stick. We have used the fuel stick cell model (Table VII) to compute this distribution. Results are shown in Figs. 25 and 26

Fig. 25. Effect of fuel loading on the radial power distribution within a fuel stick with the core at 1200 K.

Fig. 26. Effect of core temperature on the radial power distribution within a fuel stick containing the average fuel loading.

where the curves are arbitrarily normalized to unity at the center of the fuel stick. Figure 25 shows the results for the maximum, average, and minimum fuel loadings in the FGS initial core at 1200 K. Note that the radial variation is rather small; central and surface values differ by only 1.0% for the typical fuel stick at 1200 K. This variation is due to self-shielding of thermal neutrons within the fuel stick (thermal neutrons account for about 90% of the fissions) and hence the effect increases with the uranium loading in the fuel stick. For a given fuel loading, the shielding effect decreases with increasing core temperature as shown in Fig. 26.

B. One-Dimensional Axial Calculations

A typical vertical column of fuel and reflector elements contains two top reflector elements, eight fuel elements, and two bottom reflector elements. In both the top and bottom reflectors, the inner layer of reflector elements (i.e., those adjacent to the active core) are half-length elements and those in the outer layer are full-length elements. This configuration was represented with a one-dimensional (slab) axial model in which material compositions were volume-averaged in the transverse (r - θ) plane. Three axial fuel zones and top and bottom reflectors are included in the model as shown in Table XXX (the top boundary of the top reflector is at $Z=0$). Vacuum boundary conditions are applied at the top and bottom boundaries of the model.

TABLE XXX
ONE-DIMENSIONAL AXIAL MODEL OF FULL REACTOR

Zone	Description	Lower Boundary (m)	Mesh Intervals	Relative Axial Zoning		
				Uranium	Thorium	Burnable Poison
1	Top reflector	1.189	20	0.0	0.0	0.0
2	Top four core layers	4.361	36	1.201	1.127	1.200
3	Next three core layers	6.740	27	0.852	0.921	0.800
4	Bottom core layer	7.533	9	0.639	0.732	0.800
5	Bottom reflector	8.722	14	0.0	0.0	0.0

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 are for graphite temperatures of 610 K and 1 030 K, respectively, which correspond to the average inlet and outlet helium temperatures for the FGS design.

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. Relative to the core-average loadings, the axial zoning for uranium, thorium, and burnable poison is as shown in Table XXX. Axial zoning for equilibrium Xe and Sm was assumed to be the same as that for uranium as explained previously. The burnable poison pins and the seven control rod pairs were homogenized into the three axial core zones using shielding factors computed from the appropriate cell models (see Secs. III.C. and III.D.).

Calculations performed with the axial model are summarized in Table XXXI. The reactivity worths of equilibrium Xe and Sm and of the burnable poison are in good agreement with those obtained from the radial model. This implies that radial homogenization of these components, which are present in all the radial core regions, is a good approximation. This is not true of the seven control rod pairs which are present only in Rings 1 and 3 of the radial model.

Normalized gross axial power profiles $P(Z)/\bar{P}$, are shown in Fig. 27 for Cases 3 and 5. The average

TABLE XXXI
SUMMARY OF ONE-DIMENSIONAL AXIAL CALCULATIONS

Case	Description	k_{eff}
1	Reference calculation with burnable poison, equilibrium Xe and Sm, and seven 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	1.042
4	Same as 3 but without burnable poison	1.140
5	Same as 1 but with seven control rod pairs inserted only halfway into the core	1.035

axial profile with the control rods completely inserted is basically the same as that with the rods

Fig. 27. Normalized gross axial power distributions from one-dimensional model.

completely withdrawn. Equilibrium Xe and Sm and the initial burnable poison loading have only a small effect on the axial power shape because the relative axial zoning for these components is similar to that for uranium. Thus the axial profile for Case 3 is representative of Case 1 through 4. The axial profile for Case 5 shows the average effect of inserting the seven 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.

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 about 1.5 both with no control rods and with seven control rod pairs inserted halfway into the core. These values, as well as the general shapes of the curves, are in good agreement with those reported by the vendor.²¹

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 Fig. 27 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 the fuel element layer are represented explicitly as shown in Table XXXII. Periodic boundary conditions were applied at the cell boundaries. Results of the cell calculation for a fuel element layer containing the average initial fuel loading are shown in Fig. 28. These results yield a local axial peaking factor of 1.10 for the initial core. Because of the more rapid depletion

Fig. 28. Normalized local axial power distribution in a fuel element layer.

at the ends of the fuel pins, these local power spikes will become less pronounced with fuel age.

C. Two-Dimensional (r-z) Gross Power Distribution

The nine-group flux spectra from the one-dimensional radial and axial calculations were used to collapse the nine-group cross-section sets to four-group sets for use in a two-dimensional (r-z) calculation of the unrodded core. As described in Sec. 11.B., the four-group structure contains a single thermal group and three fast groups. The r-z model contains six radial zones and five axial zones as described in Table XXXIII. Note that the dimensions of the radial and axial zones are the same as those used in the one-dimensional models (Tables XXVII and XXX) but a coarser mesh was used in the two-dimensional calculations. Radial and axial zoning of fuel and burnable poison are the same as in the one-dimensional models.

Average axial and radial power distributions from the two-dimensional model were obtained by volume-averaging the two-dimensional distribution in the transverse direction:

$$\bar{P}(r) = \int P(r,z) \, dV_z / \int dV_z$$

$$\bar{P}(z) = \int P(r,z) \, dV_r / \int dV_r$$

Using the same group structure and mesh spacing, but

TABLE XXXII

ONE-DIMENSIONAL CELL MODEL OF A FUEL BLOCK LAYER

Region Description	Lower Boundary (cm)	Mesh Intervals
Unfueled top layer	2.95	4
Fueled portion	76.35	32
Unfueled lower layer	79.30	4

TABLE XXXIII
TWO-DIMENSIONAL (r-z) FULL-REACTOR MODEL

Radial Zone	Outer Boundary (m)	Mesh Intervals	Axial Zone	Lower Boundary (m)	Mesh Intervals
Core Ring 1	0.503	3	Top reflector	1.189	10
Core Ring 2	1.333	5	Core layers 1-4	4.361	18
Core Ring 3	2.192	5	Core layers 5-7	6.740	13
Core Ring 4	4.075	10	Core layer 8	7.533	6
Core Ring 5	4.219	3	Bottom reflector	8.722	10
Radial Reflector	5.248	7			

with compositions volume-averaged in the transverse direction, one-dimensional radial and axial profiles were computed for comparison with the two-dimensional results.

The radial distributions from the one- and two-dimensional calculations are almost indistinguishable when plotted. They typically agree within 0.5% except near the reflector/core interface where they differ by 2%. As shown in Fig. 29, 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 components of the power distribution are separable to a good approximation.

Core neutron leakages computed from the one- and two-dimensional four-group models also agree very well as shown in Table XXXIV. Results obtained with the more detailed nine-group one-dimensional models are also shown. These results confirm the adequacy of the four-group models. Note that the

TABLE XXXIV
CORE NEUTRON LEAKAGE FOR UNRODDED CONFIGURATION

Model	Core Leakage/Source Neutron (%)		
	Axial	Radial	Total
Two-dimensional four-group	1.15	1.19	2.34
One-dimensional four-group	1.16	1.17	2.33
One-dimensional nine-group	1.16	1.16	2.32
CASSAR ^a (r-z)	-	-	2.9

^aFrom Table 4.3-24 of Ref. 2.

total core leakage reported in the CASSAR is 2.9% as compared to the LASL-computed value of 2.3%.

V. PERTURBATION AND REACTIVITY WORTH CALCULATIONS

All the calculations described in this section were performed using four energy groups and the two-dimensional (r-z) model described in Table XXXIII. The four-group cross sections are those appropriate at design conditions and were obtained by collapsing the nine-group MICROX sets using flux spectra computed in one-dimensional geometries. In all cases, compositions correspond to the initial fuel and burnable poison distributions with equilibrium xenon and samarium. Various control rod configurations are used in the rod worth calculations (see Sec. V.A.) and in the core drop investigation (see Sec. V.B.). Kinetics parameters (see Sec. V.C.) and material worth distributions (Sec. V.D.) were computed for the unrodded case.

Fig. 29. Normalized gross axial power distribution from one- and two-dimensional models (no control rods).

A. Control Rod Worths

Reactivity worths of several control rod configurations including one, seven, and seventy-three (all) rod pairs were calculated. The single rod pair case is for the central refueling region which comprises Ring 1 of the model (see Table XXXIII). For the seven rod pair case, rods are present in the central refueling region and in six of the twelve refueling regions in Ring 3. Effective (self-shielded, control rod cross sections were calculated using shielding factors obtained from the one-dimensional control rod cell model described in Sec. III.D.

Multiplication factors and the implied control rod worths from the two-dimensional calculations are summarized in Table XXXV. A diffusion theory calculation of the no rods case yielded $k = 1.047$, which differs by only $0.001 \Delta k$ from the transport theory result of 1.048 . Therefore diffusion theory is adequate for homogenized full-reactor models of the large HTGR.

When adjusted for the interaction effect between the two rods in a pair (see Sec. III.D.), the calculated single rod pair worth is the same as that ($-0.005 \Delta k$) reported in the CASSAR. In addition to its total worth, we also calculated the worth vs axial position of the central rod pair. The normalized worth vs axial position curve obtained from these calculations is shown in Fig. 30. Note that insertion into the bottom half of the core accounts for 60% of the total worth of the rod pair and that there is some residual worth (about 2% of the total worth) at the top of the core. These results are in good agreement with those shown in Fig. 4.3-4 of Ref. 2.

The reactivity worth of the seven rod pairs as computed with the two-dimensional model is within $0.001 \Delta k$ of that computed with the one-dimensional

Fig. 30. Reactivity worth vs axial position for the central control rod pair.

radial model and is in good agreement with the vendor result (see Sec. IV.A.). Agreement between the one- and two-dimensional models implies that there is little change in axial core neutron leakage between the unrodded and seven rod pair configurations. That this is indeed the case is illustrated in Table XXXVI where the radial, axial, and total core neutron leakages are given for the various control rod configurations as computed from the

TABLE XXXVI
CORE NEUTRON LEAKAGE FOR VARIOUS CONTROL ROD CONFIGURATIONS

Rod Pairs Inserted	Core Leakage/Source Neutron (%)		
	Axial	Radial	Total
None	1.15	1.19	2.34 (2.9) ^a
Seven	1.13	1.70	2.83 (3.6) ^b
All	1.25	0.91	2.16

^aFrom Table 4.3-24, Ref. 2.

^bFrom Table 4.3-23, Ref. 2.

TABLE XXXV
SUMMARY OF TWO-DIMENSIONAL CONTROL ROD WORTH CALCULATIONS

Case	Multiplication Factor	Worth (Δk)
No rods	1.048	-
One rod pair	1.042	-0.006
Seven rod pairs	1.012	-0.036
All (73) rod pairs	0.787	-0.261

two-dimensional model. The total core leakages computed for the unrodded and seven rod pair configurations are about 20% smaller than those reported in the GASSAR.

The seven rod pair configuration corresponds to the hot-critical case at the beginning of the initial cycle with equilibrium xenon and samarium. As shown in Table XXXV, we have computed a multiplication factor of 1.012 for this case. This value must be adjusted for the fact that we used a ^{235}U loading that is 6.8% too high. As discussed in Sec. II.C., this adjustment amounts to $-0.014 \Delta k$. Another adjustment required is that due to the interaction effect between the two rods in a pair. This effect reduces the rod pair worth by 15% (see Sec. III.D.), which translates to a $+0.005 \Delta k$ adjustment on k for the seven rod pairs. Taking into account the above two adjustments, we obtain a multiplication factor of 1.003 for the BOC hot-critical configuration.

For the all-rods-in case, we compute a worth of $-0.261 \Delta k$ which reduces to $-0.222 \Delta k$ when adjusted for rod interaction. The value of $-0.258 \Delta k$ reported in the GASSAR for the middle of the initial cycle (MOIC) is 16% larger than our adjusted result for the BOIC. For a fixed geometrical configuration and core temperature, the reactivity worth per unit mass of poison material is generally not constant but decreases with increasing total poison mass. Therefore the control rod worth should increase during the cycle as burnable poison is depleted (depletion of the control rods is negligible because they are heavily self-shielded). Whether this effect can produce a 16% difference between the BOIC and MOIC control rod worths remains to be determined.

B. Core Drop Calculations

If the three graphite posts that support the central refueling region are postulated to fail, the refueling region would drop a distance of about 2 meters to the bottom of the exit plenum. This distance corresponds to the height of about 2.5 fuel blocks. The reactivity change resulting from such a hypothetical geometrical rearrangement was calculated. Except for the central control rod pair, all control rods were in their withdrawn position.

Calculations were performed with the central control rod pair at various axial positions. This rod pair was assumed to remain in its original position after the refueling region was dropped (control

rod support is independent of that for the refueling region). In the dropped (perturbed) configuration, that portion of the central refueling region that extended below the bottom reflector was not included in the perturbed model. This should be a good approximation because the reactivity worth of materials beyond the bottom reflector is small (material worth distributions are reported in Sec. V.D.).

Reactivity changes obtained from the calculated multiplication factors for the reference and perturbed configurations are summarized in Table XXXVII. These results show that the reactivity change is small and negative regardless of the position of the control rod pair. Examination of the material changes that occur in each axial mesh interval as a result of the drop shows that one should expect negative reactivity effects above the core midheight and positive effects below the midheight. The results in Table XXXVII indicate that these effects are essentially compensatory.

If the thermal flux is not greatly perturbed by the region drop, the worth of the control rod pair should not change greatly (fully inserted, the worth of the central rod pair is about 90c). In this case, the net reactivity change due to the refueling region drop should not be strongly dependent on the axial position of the rod pair. From the results in Table XXXVII, this appears to be the case.

Because the net reactivity changes are small, the accuracy of the k -difference results is limited by problem convergence. One of these cases was verified by exact perturbation theory using direct and adjoint angular fluxes, respectively, for the perturbed and unperturbed configurations.

TABLE XXXVII
REACTIVITY CHANGE DUE TO CENTRAL REFUELING
REGION DROP

Central Rod Pair Position	$10^{-4} \Delta k$	Cents ^a
Completely withdrawn	-3.2	-4.9
2.5 blocks into core	-2.9	-4.5
4 blocks into core	-3.1	-4.8
6 blocks into core	-1.4	-2.2
8 blocks into core	-1.9	-2.9

^aOne dollar = 0.0065 $\Delta k/k$.

The reactivity effect of a hypothetical collapse of the entire core, which would require simultaneous failure of all the core support posts, depends on the control rod configuration at the time of collapse. In terms of reactivity effects, dropping of the entire core is equivalent to bank withdrawal, over a distance of about 2.5 fuel blocks, of whatever rods are present at the time of collapse. In the following discussion, we consider three different initial configurations and we assume that the axial worth curve for a bank of control rods is the same as that shown in Fig. 30 for the central rod pair.

1. Reactor initially subcritical with all control rods completely inserted

From Fig. 30, the reactivity insertion can be estimated to be 30% of the total bank worth. If we use the maximum worth (0.258 Δk) of all control rods as reported in Table 4.3-13 of Ref. 2, we obtain a maximum reactivity insertion of 0.077 Δk . This is less than the minimum shutdown margin of 0.085 Δk reported by the vendor (Table 4.3-13, Ref. 2). Therefore the reactor would remain subcritical in this case.

2. Reactor initially critical at room temperature with 19 control rod pairs fully inserted

Based on the results in Sec. V.A., we estimate a total worth of 0.072 Δk for the 19 control rod pairs. This yields a reactivity insertion of 0.022 Δk or about \$3.3 for bank withdrawal a distance of 2.5 blocks from the fully inserted position.

3. Reactor initially critical at design temperature with seven control rod pairs partially inserted

For this case, we assume that the initial axial position of the control rod bank is such that the reactivity insertion is maximized when the bank is withdrawn a distance of 2.5 fuel blocks. The reactivity insertion for this case can be estimated from Fig. 30 to be 50% of the total bank worth. Using a total worth of 0.031 Δk for seven rod pairs (see Sec. V.A.) this yields a reactivity insertion of 0.015 Δk or \$2.4.

C. Kinetics Parameters

Kinetics parameters including the effective prompt neutron generation time ($\bar{\Lambda}$), delayed neutron fractions (β_i , $i = 1, \dots, 6$), and decay constants (λ_i , $i = 1, \dots, 6$) were calculated for the FGS beginning of life (BOL) core. Values for $\bar{\Lambda}$ and β_i

were computed using the DAC perturbation theory code, which is an unpublished multi-dimensional version of the one-dimensional S_n perturbation code DAC1. Two-dimensional (r-z), four-group, S_4 , direct and adjoint angular fluxes computed with the TWOTRAN-II code were used in the DAC calculations. Compositions used in the model correspond to the BOL fuel and burnable poison distributions with equilibrium xenon and samarium but no control rods.

Energy boundaries, prompt and delayed neutron spectra, and neutron velocities for the four-group energy structure are shown in Table XXXVIII. Prompt and delayed fission spectra were obtained from information given in Ref. 22. Velocities for the four-group structure were obtained by averaging over fine-group fast and thermal neutron spectra generated, respectively, with the MC² and GLEN codes (see Sec. II.B.).

Calculation of the effective delayed-neutron fractions and decay constants for a given reactor requires a knowledge of the β_i and λ_i for the various fissioning isotopes present in the system. In our computations, we have used the six-group delayed-neutron data given by Keepin²² for thermal fission of ²³⁵U and fast fission of ²³²Th. In the BOL core, 99.8% of the fissions occur in ²³⁵U and the remainder (0.2%) is essentially all in ²³²Th.

Kinetic parameters calculated at the LASL, along with those reported by the vendor, are summarized in Table XXXIX. The LASL value of $\bar{\Lambda}$ is within 5% of that reported by GAC. Because $\bar{\Lambda}$ is sensitive to the group velocities, particularly that in the thermal group (group 4), this agreement provides an indirect verification of the fine-group spectra computed with the MC² and GLEN codes.

As one would expect from the fact that most of the fissions occur in ²³⁵U and the fact that the

TABLE XXXVIII
PARAMETERS FOR FOUR-GROUP ENERGY STRUCTURE

Group	Lower Energy Boundary (eV)	Fission Spectrum Prompt	Fission Spectrum Delayed	Neutron Velocity (cm/s)
1	1.83×10^5	1.0	0.948	1.148×10^9
2	17.6	0.0	0.052	3.054×10^7
3	3.93	0.0	0.0	3.925×10^6
4	0.0	0.0	0.0	5.804×10^5

TABLE XXXIX
KINETIC PARAMETERS FOR BOL CORE^a

	Prompt Generation Time (s)		Total Delayed Neutron Fraction	
	LASL	GAC	LASL	GAC
	4.3×10^{-4}	4.1×10^{-4}	6.5×10^{-3}	6.5×10^{-3}
<i>i</i>	$\bar{\beta}_i$		$\bar{\lambda}_i$ (s ⁻¹)	
	LASL	GAC	LASL	GAC
1	0.22×10^{-3}	0.22×10^{-3}	1.24×10^{-2}	1.24×10^{-2}
2	1.43×10^{-3}	1.47×10^{-3}	3.05×10^{-2}	3.05×10^{-2}
3	1.28×10^{-3}	1.27×10^{-3}	1.11×10^{-1}	1.11×10^{-1}
4	2.58×10^{-3}	2.57×10^{-3}	3.01×10^{-1}	3.01×10^{-1}
5	0.75×10^{-3}	0.75×10^{-3}	1.14	1.14
6	0.27×10^{-3}	0.27×10^{-3}	3.01	3.01

^aGAC values are from Table 4.3-22 of Ref. 2.

delayed neutron spectrum in the four-group structure does not differ greatly from the prompt neutron spectrum, the values of $\bar{\beta}_i$ and $\bar{\lambda}_i$ are essentially those for ²³⁵U. The LASL values for $\bar{\beta}_i$ and $\bar{\lambda}_i$ are in very good agreement with those reported by GAC.

D. Material Worth Distributions

We have used the two-dimensional direct and adjoint angular fluxes for the unrodded BOL configuration to compute small-sample worth distributions for various materials using perturbation theory.

The materials for which worth distributions were computed, and their maximum specific worth, are given in Table XL. In general, the location of the maximum worth is near the center of the reactor core.

Radial and axial worth distributions for ²³⁵U are shown in Figs. 31 and 32, respectively. The radial distribution is that for the axial mesh interval nearest the midheight of the core and the axial distribution is that for the first radial mesh interval. In the radial dimension, the core

TABLE XL
MAXIMUM REACTIVITY WORTHS FOR VARIOUS MATERIALS

Material	Maximum Worth (c/g)
Uranium-235	$+ 1.34 \times 10^{-2}$
Uranium-233	$+ 2.16 \times 10^{-2}$
Protactinium-233	$- 1.13 \times 10^{-2}$
Thorium-232	$- 6.25 \times 10^{-4}$
Xenon-135	$- 1.65 \times 10^{-2}$
Boron - Natural (unshielded)	$- 7.06 \times 10^{-1}$
Boron - Natural (shielded in burnable poison pin)	$- 4.74 \times 10^{-1}$
Boron - Natural (shielded in control rod)	$- 1.01 \times 10^{-2}$
Silicon - Natural	$- 5.46 \times 10^{-5}$
Carbon-12	$+ 2.86 \times 10^{-5}$
Oxygen-16	$+ 1.27 \times 10^{-5}$
Water (H ₂ O)	$+ 9.04 \times 10^{-4}$

Fig. 31. Radial reactivity worth distribution for ²³⁵U at Z = 4.3 m.

Fig. 32. Axial reactivity worth distribution for ^{235}U at $R = 0.08$ m.

extends from 0 to 4.22 m, and in the axial dimension the core extends from 1.19 to 7.53 m (see Table XXXIII). The reactivity worth for ^{235}U is positive everywhere and the worth curves generally reflect the thermal flux distribution. Peaking at the core/reflector interfaces is evident in both the radial and axial profiles. Radial zoning of the burnable poison depresses the radial thermal flux profile near the center of the core.

The radial and axial worth profiles for ^{233}U are very similar to those for ^{235}U , but the specific worth of ^{233}U is about 1.6 times higher than that for ^{235}U . In other words, the reactivity worth of 1.0 gram of ^{233}U is equivalent to 1.6 grams of ^{235}U .

Note from Table XL that the specific worth of ^{233}Pa is negative but of the same absolute magnitude as that for ^{235}U . However, ^{233}Pa decays to ^{233}U by beta emission with a 27-d half-life. Therefore, the maximum net reactivity change due to decay of ^{233}Pa is about $+3.29 \times 10^{-2}$ c/g or about 2.5 times higher than the worth for ^{235}U .

The shapes of the worth distributions for ^{233}Pa are similar to those for ^{232}Th , which are shown in Figs. 33 and 34. Thorium has a negative specific worth whose magnitude is about 20 times smaller than that for ^{235}U . Because thorium is principally a resonance neutron absorber, the worth profiles reflect the resonance neutron flux distribution which does not exhibit a reflector "bump."

The shapes of the ^{235}U worth curves (Figs. 31 and 32) are representative of all materials that are primarily thermal neutron absorbers. Thus, except

Fig. 33. Radial reactivity worth distribution for ^{232}Th at $Z = 4.3$ m.

for the sign, the curves for xenon, boron, and silicon are very similar to those for ^{235}U . On a unit mass basis, the reactivity effect of ^{135}Xe is about 230 times larger than that of unshielded natural boron. The reduction in the specific worth of boron in the burnable poison pins and in the control rods is consistent with the thermal flux shielding factors for these components (see Tables XIX and XXII).

The reactivity effects of carbon, oxygen, and water are primarily due to neutron moderation. That is, these materials tend to soften the thermal neutron spectrum and this results in an increase in reactivity in the BOL core. Radial and axial worth distributions for water are shown in Figs. 35 and

Fig. 34. Axial reactivity worth distribution for ^{232}Th at $R = 0.08$ m.

Fig. 35. Radial reactivity worth distribution for H_2O at $Z = 4.3$ m.

36. Note that the worth of water becomes slightly negative in the reflector where absorption effects are more important than moderation effects. The curves for ^{12}C and ^{16}O are similar to those for water except that the reactivity worth is positive everywhere.

Compared to carbon, the maximum specific worth of oxygen is two times smaller, whereas that of water is about thirty times larger. These results reflect the relative efficiency of hydrogen, carbon, and oxygen as neutron moderators. Using the maximum specific worth for water, about 110 kg of water is required to yield a one dollar positive reactivity addition. The corresponding mass for carbon is about 3 500 kg.

Fig. 36. Axial reactivity worth distribution for H_2O at $R = 0.08$ m.

VI. FUTURE WORK

Incorporation of double-heterogeneity effects into the independent LASL cross-section processing code system is in progress. When this work is completed, new broad-group cross-section sets will be generated and some of the temperature coefficient calculations will be repeated. Reactivity worths of differences in the GAC and the LASL cross sections for the major nuclides will be computed using two-dimensional perturbation theory. Based on these results and other considerations, a determination will be made of the acceptability of the GAC cross-section data base and processing codes.

The magnitude of the interaction effect between the two control rods in a rod pair will be confirmed. This will be done using the two-dimensional (planar), triangular-mesh, transport theory TRIPLET code.²³

Fuel depletion and fission product buildup calculations will be performed to determine detailed core compositions at the EOC for the fuel management scheme specified by the vendor. These calculations will be performed either with a three-dimensional flux-depletion code (e.g., the CITATION code²⁴), or by linking a three-dimensional flux code (e.g., the VENTURE code⁵) with a point-depletion code (e.g., the CINDER code²⁵). After the fuel depletion studies are completed, neutronic characteristics of the EOC core will be calculated.

Long-term objectives are to study the importance of space-time effects in the large HTGR, investigate its stability against xenon oscillation, and generally to maintain an updated capability to provide neutronics-related input for accident delineation and consequence studies.

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