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Process Heat Applications**

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PREDICTED NUCLEAR HEATING AND TEMPERATURES
IN GAS-COOLED NUCLEAR REACTORS FOR PROCESS HEAT APPLICATIONS

by

G. E. Cort, J. C. Vigil, and R. J. Jiaconoletti

ABSTRACT

The high-temperature gas-cooled nuclear reactor (HTGR) is an attractive potential source of primary energy for many industrial and chemical process applications. The HTGR core designs which have been developed for electric power generation using the Rankine cycle operate at relatively low exit gas temperatures, 1033 K (1400°F). Depending on the chemical process, reactor exit gas temperatures up to 1477 K (2200°F) may be required for the nuclear process heat system. Significant modification of current HTGR core design will be required to achieve the required elevations in exit gas temperatures without exceeding the maximum allowable temperature limits for the fuel material. A preliminary evaluation of the effects of various proposed design modifications by predicting the resulting fuel and gas temperatures with computer calculational modeling techniques is reported. The design modifications evaluated are generally those proposed by the General Atomic Company (GAC), a manufacturer of HTGRs, and some developed at the LASL. The GAC modifications do result in predicted fuel and exit gas temperatures which meet the proposed design objectives. Future additional effort is indicated which should deal with alternative designs differing significantly from the design studied, and should include an evaluation of their relative resistance to possible accident conditions or off-design operation.

I. INTRODUCTION

In 1974 the Energy Research and Development Administration (ERDA) initiated a studies program to assess the incentives and needs for the development of a very-high-temperature gas-cooled reactor (VHTR) for high-temperature process heat, electric power generation and other energy applications.¹ The study program has included conceptual designs of reactors and has involved reactor contractors, government agencies, and national laboratories. The gas-cooled VHTRs being considered are those of the pebble bed or the prismatic block core design; and the latter is subdivided into the multi-hole graphite block or "hot block" and the pin-in-block or "cold block." The "hot-block" design is that used in the Fort St. Vrain (FSV) and other commercial HTGRs developed by General Atomic Company (GAC). To increase the outlet temperature of its

HTGRs, GAC has proposed² several design modifications which elevate the exit gas temperatures without a corresponding increase in the maximum fuel temperature. These core modifications have been evaluated at the Los Alamos Scientific Laboratory (LASL). The input data and assumptions were verified by literature survey, limiting case calculations, and review of alternative methods. Independent calculations which model the core neutronic and thermally were performed using existing LASL computer programs. Evaluation of the "cold-block" design and the pebble bed core are in progress and current results are included in this report.

II. PROPOSED MODIFICATIONS

The GAC reference HTGR design and its fuel-temperature limitations are described in Refs. 3 through 5. Core and fuel-cycle parameters for this

design and for the proposed design modifications are summarized in Table I. Five different modifications are considered by GAC corresponding to helium outlet temperatures of 1033, 1144, 1255, 1366, and 1477 K (1400, 1600, 1800, 2000, and 2200°F). The first three designs are based on current HTGR technology in that maximum fuel temperatures do not exceed that for the reference 3000 MW(t) commercial HTGR. Feasibility of the last two designs requires advancements in fuel technology to increase the temperature capability of the fuel. Alternatively, an advanced fuel-management scheme could be used to reduce the difference between the maximum fuel temperature and the helium exit temperature. Proposed modifications to the reference design to obtain the desired helium outlet temperature are summarized below.

Design I -- 1033 K Exit Temperature. This is essentially the same as the commercial HTGR design with the helium inlet temperature raised to 644 K (700°F) and the flow rate increased to 1484 kg/s (11.76×10^6 lbm/h).

Design II -- 1144 K Exit Temperature. The major design change is in the fuel block where the number of fuel sticks is increased from 132 to 210 and the number of coolant holes from 72 to 108. The fuel and coolant volumes and the minimum graphite web thickness between holes is the same as in Design I. In addition, the helium inlet temperature is increased to 683 K (770°F) and the flow rate is reduced to 1251 kg/s (9.93×10^6 lbm/s).

Design III -- 1255 K Exit Temperature. The fuel block design is the same as Design II, but with the following added features: (1) a three-year rather than a four-year fuel cycle, (2) reduction in the average carbon-to-thorium atom ratio from 240 to 200, (3) TRISO fertile particles rather than BISO, and (4) inlet helium temperature of 773 K (932°F) with flow rate reduced to 1197 kg/s (9.50×10^6 lbm/h). Changes (1) and (2) both reduce nuclear power-peak-ing effects (and thus the maximum fuel temperature) which result from differences in the fissile content of fresh and discharged fuel. The use of TRISO fertile particles is predicted to reduce irradiation-caused shrinkage of the fuel sticks by a factor of three. This results in a reduced temperature difference across the fuel-moderator gap and hence a lower fuel temperature.

Designs IV and V -- 1366 and 1477 K Exit Temperatures. These are the same as Design III except that improved fuel technology will allow maximum fuel temperatures above the 1683 K (2569°F) value for the reference design. The maximum fuel temperature is 1806 K (2791°F) for Design IV and 1927 K (3009°F) for Design V. For Design IV the helium inlet temperature is 839 K (1050°F) and the flow rate is reduced to 1094 kg/s (8.68×10^6 lbm/h). The corresponding values for Design V are 922 K (1200°F) and 1038 kg/s (8.24×10^6 lbm/h). An axial push-through fuel-management scheme² has been suggested by GAC as an alternative to improvement of the temperature capability of the fuel. Compared

TABLE I
CORE AND FUEL-CYCLE PARAMETERS
FOR FIVE PROCESS HEAT DESIGNS AND A REFERENCE 3000 MW(t) HTGR (FGS)

	Reference	Design I	Design II	Design III	Design IV	Design V
Process T, K (°F)	---	922(1200)	1033(1400)	1144(1600)	1255(1800)	1366(2000)
Outlet T, K (°F)	1414(1366)	1033(1400)	1144(1600)	1255(1800)	1366(2000)	1477(2200)
Inlet T, K (°F)	591(605)	644(700)	683(770)	773(932)	839(1050)	922(1200)
$\bar{T}$ fuel, K (°F)	1163(1634)	1184(1671)	1199(1698)	1303(1886)	1405(2069)	1509(2257)
$T_{\max}$ fuel, K (°F)	1683(2570)	1651(2513)	1630(2475)	1679(2562)	1806(2792)	1927(3010)
$\bar{T}$ moderator, K (°F)	1013(1364)	1043(1418)	1111(1541)	1219(1734)	1323(1922)	1430(2114)
He flow, kg/s (10^6 lbm/h)	1415(11.23)	1484(11.78)	1251(9.93)	1197(9.50)	1094(8.68)	1038(8.24)
Fuel/coolant holes per block	132/72	132/72	210/108	210/108	210/108	210/108
Type fissile/fertile particles	TRISO/BISO	TRISO/BISO	TRISO/BISO	TRISO/TRISO	ADVANCED	ADVANCED
Equilibrium C/Th atom ratio	240	240	240	200	200	200
Fuel cycle, yr	4	4	4	3	3	3

to the reference design, this fuel-management scheme would eliminate radial power peaking due to differences in fuel age and would allow the attainment of a more ideal axial power distribution. However, this scheme was not incorporated by GAC into Designs IV and V because it would involve a large increase in fuel handling during refueling.

III. EVALUATION OF MODIFICATIONS - NEUTRONIC ANALYSIS

The proposed modifications outlined in Sec. II and discussed in detail in Ref. 2 were evaluated as described in this and the following section by verifying that input data and assumptions are realistic from literature surveys, limiting case calculations, etc., and by independent calculations which model the core neutronically and thermally performed on existing computer programs at LASL. The average axial power distributions must be available before the temperature prediction calculations of average core temperatures can be made to verify the design values. In addition, the maximum radial power-peaking factor must be available for the calculation of hot-channel and maximum fuel temperature predictions for design value comparison. Gross radial and axial power distributions were calculated using one-dimensional neutron transport models (DTF-IV code⁶), supplemented by a two-dimensional calculation to verify the r - z separability (TWO1RAN-II code⁷), and auxiliary one-dimensional calculations to predict local effects. The adequacy of the calculational models used in the power-distribution analyses was verified by performing calculations which modeled the reference (FGS) design and produced the reported design values. These calculations, described in the Appendix, provided the necessary assurance that the calculational model was adequate for the subsequent evaluation of the design modifications for process-heat applications.

A. Axial Power Distributions

The GAC computations of the core outlet helium temperatures for Designs I through V are based on the axial power profiles given in Table 6-2 of Ref. 2. These profiles are plotted in Fig. 1 along with the LASL model results for the reference design (see Appendix). In Ref. 2, axial power profile data for Designs I-V are given only at the center

Fig. 1. Normalized axial power profiles for the reference (FGS) design and process-heat Designs I, II, and V (profiles for Designs III and IV are within the envelope defined by those for II and V).

and bottom of each fuel-element layer. Therefore, the LASL model results for the reference design were used to extrapolate the GAC data over the entire core height, as shown in Fig. 1. The predicted axial profiles are progressively steeper for Designs I through V with those for II through V grouped together in a rather narrow band. In addition, the predicted profiles for Designs II through V are steeper than that predicted for the reference design. The profile predicted for Design I is very similar to that for the reference design in the bottom half of the core, but the reference design profile is steeper in the top half. These differences in profiles can be obtained by changes in the reference axial fuel distribution. The required fuel distribution changes are not specified in the GAC study;² the predicted axial power profiles were not calculated for particular axial loadings, but instead are those profiles required to yield the desired fuel and outlet temperatures. However, the axial profiles used by GAC for Designs I through V are consistent with those for three axial fuel zones as specified in the reference design.⁵ The three axial zones consist of the top four fuel-element layers (zone 1), the next three layers (zone 2), and the bottom layer (zone 3).

By adjusting the relative axial fuel loadings in the three zones described above, the axial power profiles used by GAC for Designs I through V can be approximated. Some of the uranium and thorium axial distributions for which power profiles were calculated are summarized in Table II. These distributions are specified relative to core-average uranium and thorium loadings. The axial power profiles were calculated with a one-dimensional model using the DTF-IV code and nine-group cross sections generated with the MICROX code,⁸ as in the reference design calculations (Appendix).

The calculations show that the axial power profile is sensitive to both the uranium and the thorium axial distributions. The effect of changing only the uranium distribution is illustrated in Fig. 2. The axial fuel distribution for Case A corresponds to that for the FGS initial core; Case B has the same thorium distribution but a flatter uranium distribution than Case A. Figure 3, which shows the power profiles for Cases A and C, illustrates the effect of changing only the thorium distribution. The uranium distribution for Case C is the same as that for Case A, but Case C has a uniform (flat) thorium distribution (Table II).

Using only three axial fuel zones, it is clear from Figs. 2 and 3 that a wide range of axial power shapes can be obtained by independently varying the uranium and thorium distributions. Similarity between power profiles can be maintained by simultaneous adjustment of the uranium and thorium

TABLE II
URANIUM AND THORIUM AXIAL DISTRIBUTIONS^a

Case	Uranium			Thorium		
	Zone 1	Zone 2	Zone 3	Zone 1	Zone 2	Zone 3
A ^b	1.201	0.851	0.639	1.127	0.920	0.732
B	1.071	0.964	0.824	1.127	0.920	0.732
C	1.201	0.851	0.639	1.000	1.000	1.000
D	1.040	0.990	0.870	1.025	0.989	0.932
E	1.000	1.000	1.000	1.000	1.000	1.000
F	1.071	0.964	0.824	1.040	0.980	0.900
G	1.071	0.964	0.824	1.025	0.989	0.932
H	1.232	0.810	0.643	1.120	0.923	0.751

^aRelative to core-average uranium and thorium loadings.

^bCorresponds to fuel distribution for FGS initial core.

Fig. 2. Axial power profiles for two different uranium distributions but the same thorium distribution (Cases A and B).

Fig. 3. Axial power profiles for two different thorium distributions but the same uranium distribution (Cases A and C).

distributions. This is shown in Fig. 4 where the power profiles for Cases A and D are plotted. Both the uranium and thorium distributions for Case D are flatter than those for Case A (see Table II). As expected, the discontinuity in the power density between axial fuel zones is proportional to the discontinuity in the uranium loading.

The axial power profile used by GAC for Design I (Table 6-2 of Ref. 2) is compared in Fig. 5 to that calculated for a flat fuel distribution (Case E). The small asymmetry in the calculated power profile for Case E is due to the fact that the bottom reflector is at a higher temperature than the top reflector. This results in a harder thermal spectrum, and consequently a smaller fission density, at the bottom of the core as compared to the top of the core. It may be concluded from Fig. 5 that Design I would require an essentially flat axial fuel distribution. By a process of trial and error, two fuel distributions have been determined (Cases F and G) which yield power profiles that bracket the GAC Design I values over most of the core height and that have the same general features (Fig. 6). Cases F and G have the same uranium distribution but slightly different thorium distributions (Table II).

Because fuel is depleted more rapidly in the high power regions, the axial power profile will tend to become flatter during the fuel cycle. Based on the results shown in Fig. 4.3.2-5 of Ref. 5, the power profile for Case G (Fig. 6) should be expected

Fig. 4. Axial power profiles for two different uranium and thorium distributions designed to yield the same general shape (Cases A and D).

Fig. 5. Comparison of axial power profile used by GAC for Design I and that for a flat fuel distribution (Case E).

Fig. 6. Comparison of axial power profile used by GAC for Design I and those for Cases F and G.

to approach more closely that used by GAC for Design I as fuel is depleted. Therefore, Case G appears to be the more appropriate initial fuel loading for Design I than Case F.

The axial power profile computed for Case H corresponds rather closely with that used by GAC for Design V (see Fig. 7). Because the GAC profiles for Designs II-V are very similar (Fig. 1), only small changes in the uranium and thorium distribution for Case H would be required to obtain power profiles for Designs II through IV. Therefore, axial power profiles similar to those used by GAC can be obtained for all five designs by adjusting the fuel distribution in the three axial zones described previously.

Fig. 7. Comparison of axial power profile used by GAC for Design V and that computed at LASL for Case H.

Accordingly, the reported thermal analyses of the five designs were based on the axial power profiles given in Table 6-2 of Ref. 2.

The effects of fission products, burnable poison, control rods, and fuel depletion were not included in these calculations. Xenon and samarium (the most important fission products) and burnable poison have little effect on the axial profile for the FGS initial core (see the Appendix). Axial zoning of the burnable poison could be used to modify the axial power profile produced by a given fuel distribution. In practice, however, the burnable-poison zoning is used to reinforce the power shape produced by the fuel distribution.⁹ This minimizes changes in the power profile as the burnable poison is depleted.

Partially inserted control rods have a large effect on the local axial power profile. However, fewer than 10% of the control rods are present in the core during a burnup cycle. Furthermore, depletion of the burnable poison compensates for fuel burnup over a large part of the cycle so that major withdrawal of the shim rods is not required until late in the cycle. Therefore, the unrodded axial power profile is representative of the entire core over most of the fuel cycle and of a large part of the core during the remaining part of the cycle.

B. Radial Power-Peaking Factors

Gross, local, and overall radial power-peaking factors used by GAC for the hot-channel thermal

analysis of the five process-heat designs and the reference design are given in Table III. The gross peaking factor applies to the entire refueling region, in which the channel is located, the local peaking factor is a measure of the power tilt within that refueling region, and the overall peaking factor is the product of the gross and local factors. As was the case with the axial power profiles, the radial peaking factors were not calculated for specific loadings in the horizontal plane, but were based on GAC extrapolations of the reference (FGS) design.

For Designs I and II, the proposed modifications to the reference core design should not affect the radial peaking factors. Based on the maximum peaking factors (1.6 gross, 2.0 overall) given in the FGS PSAR, which were calculationaly verified (see the Appendix) for the initial core, it would appear that the overall peaking factors for these two designs are low by about 15%. The gross peaking factor in particular appears to be too small, but maybe achievable by using more radial fuel zones than in the reference design.

For Designs III through V the radial peaking factors used by GAC reflect reductions in the Design I and II values resulting from a decrease in the U/Th atom ratio (from 240 to 230) and a decrease in the fuel residence time (from four to three years). The 20% increase in the Th loading is reported² to reduce the gross radial peaking factor by 2.4% due to reduced age-peaking effects. That is, the higher Th load increases the conversion ratio which tends to reduce the difference in fissile content between fresh and discharged fuel. Operating the core on a three-year cycle instead of a four-year cycle also reduces age-peaking effects. This is expected² to

TABLE III
GAC HOT-CHANNEL RADIAL PEAKING FACTOR

Design	Gross, F	Local, F _c	Overall, F _H = F × F _c
FGS	1.6	1.25	2.0
I	1.297	1.315	1.706
II	1.297	1.315	1.706
III	1.201	1.263	1.516
IV	1.201	1.263	1.516
V	1.201	1.263	1.516

reduce both the gross and local peaking factors by 4 to 5%. The net effect of these changes is a 12% decrease in the overall peaking factors.

The allowances made by GAC for reduced age-peaking in Designs III-V do not appear to be unreasonable. More precise verification of the hot-channel radial peaking factors used by GAC would require extensive neutronic and thermal calculations. Detailed power distributions in the horizontal plane at various times in the fuel cycle would be required for these calculations. This in turn would require that fuel-management and depletion calculations be performed in two-dimensional (hexagonal) geometry to determine core compositions during the fuel cycle. Identification of the hot channel then would also require thermal analyses at the various times in the fuel cycle. Because the maximum fuel temperatures can be reduced by using a more detailed radial fuel zoning scheme, this extensive calculational effort is not justified. Calculations were made, however, to verify the predicted hot-channel temperature distributions and peak fuel temperatures that result when the GAC radial peaking factors are used.

In addition to the gross and local radial power-peaking factors, which apply to the entire fuel stick, there is a small (<1.5%) radial variation in the fission density across the fuel stick (see the Appendix). This variation is caused by nuclear self-shielding of thermal neutrons which account for about 90% of the fissions in the fuel stick. The thermal analysis indicates that the effect on predicted maximum fuel temperatures is negligible (<1 K).

IV. THERMAL ANALYSIS

The thermal analysis effort was divided into three parts: the first, a verification of modeling assumptions, boundary conditions, and material properties; second, a parameterization of gross effects of radial power flattening on coolant temperatures to simplify comparison of alternative schemes; and third, a calculation of detailed temperature distributions using the LASL computer calculational models for comparison with the GAC-predicted² results.

A. Modeling Assumptions, Properties, Boundary Conditions

1. Heating Distribution. Based on the conclusions reported in Sec. III, the axial power distribution and radial peaking factors were assumed to be the same as those used by GAC in Ref. 2. This is probably the most important of the boundary conditions, and the most difficult to verify. Total heating in moderator graphite due to gamma deposition was assumed to equal 10% of the total heating in the fuel rods.⁵

2. Coolant Bypass Flow. Coolant flow bypassing the fueled portion of the core through the side reflector and poison rods was set at 5.5%. The approximately 3% of coolant flow which goes through the longitudinal gaps between moderator blocks was included with the coolant channel flow, because it is equally as effective in cooling the fuel. This will cause a slight (6%) overestimate of the core pressure drop which does not significantly affect the predicted temperatures. Detailed two-dimensional temperature models are required to determine the effect on core temperatures because of this assumption, but an upper limit is an increase of 17 K if the bypass flow were totally ineffective in cooling the fuel.

3. Unit Cell. The unit cell in the calculational model consists of a small triangular segment which includes one-twelfth of a coolant channel and one-sixth of a fuel rod (Fig. 4-9 of Ref. 3). The 2:1 fuel-to-coolant hole ratio is not endlessly repeated across the core because the regular pattern is broken at the edges of moderator blocks, fuel pickup holes, etc. Therefore, the actual ratio of fuel to coolant holes is less, about 1.83 for Design I. The heat flux into the average coolant channel is based on this lower ratio because it depends on the total coolant flow and total thermal power. However, a conservatively high heat flux into the coolant channels of a unit cell away from the edges of moderator blocks should be based on the higher 2:1 ratio. The heat flux in the unit cell is higher than the average by $2/1.83 = 1.09$. The corresponding figure for the more finely divided 210-fuel hole block of Designs II through V is 1.02.

The fuel sticks are 61 mm (2.4 in.) long and are stacked 12 high in the 787-mm (31-in.)

moderator block. The length of the coolant channel is thus 1.07 times the total length of the fuel rods. In the OPTION calculational model, like GAC's,³ it is assumed that the heat flux is distributed the full length of the coolant channel. Therefore, the values of the maximum local heat flux and radial temperature differences in these models are underestimated by a factor of 1.07.

Although the predicted coolant temperatures are not affected at all, the predicted maximum fuel temperatures based on an average heat flux must be corrected. The predicted temperature differences between fuel rod centerline and coolant calculated by the computer programs are therefore multiplied by $(1.09 \times 1.07) = 1.17$ for Design I and by 1.09 for Designs II through V.

4. Properties

a. Gas. Helium viscosity, thermal conductivity, and compressibility factor were taken from standard reference tables. Recent reassessments¹⁰ of transport properties for common gases were included.

b. Fuel Rod. The thermal conductivity of fuel rods was assumed constant at $6.9 \text{ W/m}^{\circ}\text{K}$ ($4 \text{ Btu/h-ft-}^{\circ}\text{F}$). This is the same as the design value used by GAC³ for HTGRs. It is based on experimental measurements¹¹ of fuel rod conductivity over a range of temperatures. The fuel rods contain various concentrations, ranging from zero to approximately 36% by volume, of graphite shim particles for the purpose of controlling the concentration of fissile material. The parameter that has the greatest influence on fuel rod conductivity is the volume fraction of graphite shim particles. At a temperature of 1573 K (2372°F) and with 0% graphite shim particles, the measured thermal conductivity is as quoted above. It is higher at all lower temperatures and/or increased shim content. The effect of neutron irradiation on the thermal conductivity has not been measured; but based on theoretical considerations,¹¹ it should be decreased only at temperatures below 1273 K (1831°F). Since over 80% of the fuel is at higher temperatures,³ the design value appears to be reasonable. If the thermal conductivity could be characterized as a function of shim content, temperature, irradiation fluence, and irradiation temperature, it would be desirable to include these effects in analysis

models. The result could very well be an increase in core temperature capability (by a maximum of 80 K (144°F)) at the cost of slightly more complex analyses, since maximum heat generation and minimum fuel conductivity are not likely to occur at the same core location.

c. Moderator Graphite. References 3 and 12 summarize thermal conductivity data for H-451 and H-327 nuclear purity grade near-isotropic graphites. At elevated irradiation and measurement temperatures the thermal conductivity does not fall below $34.6 \text{ W/m}^{\circ}\text{K}$ ($20 \text{ Btu/h-ft-}^{\circ}\text{F}$) at fast neutron ($E > 0.18 \text{ MeV}$) fluences up to $6 \times 10^{21} \text{ nvt}$. At higher fluences (greater than HTGR maximum levels) there is a further decrease with the onset of breakaway expansion associated with the disintegration of the structure.¹³ However, at the intermediate fluences, the conductivity is rather insensitive to further irradiation after the sharp initial decline. In this study, as in Ref. 2, the thermal conductivity was held constant at $27.7 \text{ W/m}^{\circ}\text{K}$ ($16 \text{ Btu/h-ft-}^{\circ}\text{F}$).

5. Boundary Conditions

a. Mesh Spacing. The finite element model for the AYER¹⁴ heat-conduction computer program is described in Fig. 8. The model consists of 24 finite elements and 48 nodes. The same calculation was repeated with a finer mesh containing four times the number of elements and the calculated maximum fuel temperatures were within 1 K. The outer surfaces of the model are adiabatic except

Fig. 8. AYER finite element model of unit cell.

at the coolant hole where a specified coolant temperature and heat-transfer coefficient are taken from the flow model results. An internal thermal resistance at the fuel-moderator gap is also included.

b. Film Heat-Transfer Coefficient. The heat-transfer correlation for turbulent flow used in this analysis is¹⁵

$$Nu = 0.029 Re^{0.7} Pr^{0.4},$$

where Nu = Nusselt number, Re = Reynolds number, and Pr = Prandtl number. This is the same correlation used by GAC for HTGR design³ and has the advantage of simplicity. It is based on bulk coolant properties and does not include entrance effects or effects of coolant property variation across the film. A comparison between various heat-transfer correlations can be made by normalizing them as follows:

$$\frac{Nu}{Re^{0.8} Pr^{0.4}} = 0.021 \quad \text{Coates}^{15} \quad (1)$$

$$\frac{Nu}{Re^{0.8} Pr^{0.4}} = 0.023 \left(\frac{T_w}{T_b} \right)^{-F} \quad \text{Taylor}^{16} \quad (2)$$

$$F = 0.57 - \frac{1.59}{(x/D)}$$

$$\frac{Nu}{Re^{0.8} Pr^{0.4}} = 0.021 \left(\frac{T_w}{T_b} \right)^{-0.5} \times \left[1 + \left(\frac{x}{D} \right)^{-0.7} \right] \quad \text{McEligot}^{17} \quad (3)$$

where T_w = channel wall temperature, T_b = coolant bulk temperature, x = axial distance from inlet, and D = channel diameter. The wall-to-bulk temperature ratio, T_w/T_b , is shown in Fig. 9 for the hot channel in Design I.² Using this ratio, the corresponding normalized Nusselt number is given in Fig. 10. Equation (1) is a reasonable compromise, generally being bracketed by Eqs. (2) and (3). For other coolant channels with reduced T_w/T_b , Eq. (1) becomes increasingly conservative.

Fig. 9. Wall-to-bulk temperature ratio.

Fig. 10. Normalized Nusselt number correlation.

c. Fuel-Moderator Gap Conductance. Heat transfer across the fuel-moderator gap was calculated using an equivalent conductance, computed from

$$h_{\text{gap}} = \frac{\lambda}{\delta} + \epsilon^* \sigma \frac{T_f^4 - T_m^4}{T_f - T_m} \quad (4)$$

where λ = helium thermal conductivity at the average gap temperature, δ = the gap width (assumed uniform axially and circumferentially) 0.13 mm (5 mils), σ = Stefan-Boltzman constant, T_f = local fuel temperature, T_m = local moderator temperature, and ϵ^* = the effective emissivity of the two surfaces

$$= \left[\frac{1}{\epsilon_f} + \frac{1}{\epsilon_m} - 1 \right]^{-1}$$

where ϵ_f and ϵ_m are normal total emissivities of the fuel rod and moderator graphite, respectively. The emissivities are 0.8 for both surfaces, constant with temperature. This is a reasonable, if slightly conservative, value^{3,11,18} for graphite. The gap conductance is dominated by the first term in Eq. (4), since radiation accounts for only 16% of the total heat transferred at 1667 K (2540°F) (and less at lower temperatures). For larger gap dimensions, such as are predicted³ to occur with four-year-old BISO/TRISO fuel rods, the radiation term may increase to 30%.

Figure 11 shows the gap conductance versus temperature for two types of fuel and three fuel ages. The dashed line, representing the conductance for unirradiated fuel with a 0.13 mm (5 mil) gap at operating temperature was used in this study for all calculations. It was also used by GAC² for

Curve	Bead type	Fuel age, yrs	Gap, mm (mils)
A	B/T	0	0.13 (5)
B	T	2	0.17 (6.8)
C	T	4	0.20 (8)
D	B/T	2	0.25 (10)
E	B/T	4	0.30 (11.8)

Reference 2

--- Design I
 --- Design II-IV

Fig. 11. Fuel-moderator gap conductance.

Designs II through IV. The two dotted lines are the values used by GAC for Design I.² The latter are more realistic to use for an active core, but result in a slightly misleading comparison when Design I is to be compared with the others, in studies such as this. The effect of the gap used by GAC in Design I versus the uniform 0.13 mm gap is to increase maximum fuel temperatures by about 24 K (42°F).

d. Fluid Flow. The boundary conditions used for the fluid flow calculations in coolant channels were: the inlet gas temperature and pressure, the exit pressure, channel diameter, and inlet and exit loss coefficients as specified by GAC.² The flow distribution to the parallel coolant channels in different refueling regions is controlled by a variable orifice to achieve equal exit gas temperatures from each refueling region. In the model, using the LASL computer program OPTION,¹⁹ the inlet orifice coefficients were so adjusted. The friction factor for turbulent flow in smooth circular channels is from the Blasius equation

$$f = 0.079 \text{ Re}^{-0.25}$$

where Re is the local Reynolds number based on the fluid bulk temperature.

B. Results

1. Effect of Column Power Tilt on Local Flow and Exit Temperature

Each of the 73 refueling regions is individually orificed to achieve uniform exit gas temperatures. The orifices are controllable and are varied during reactor life to compensate for effects of fuel burnup and reloading. If the power associated with each coolant channel in the region were uniform and equal to the region average, maximum fuel temperatures in the reactor would be much reduced. For Design I, for example with a column power tilt (peak/average for the region) of 1.315, the maximum fuel temperature is 393°F higher than if the power tilt were 1.0.

A second-order (but nonnegligible) contribution to the temperature nonuniformity is the flow reduction in the hot channel caused by higher friction and acceleration losses with increased gas temperature. The flow reduction and coolant gas temperature can be calculated analytically with good accuracy enabling efficient and low cost

parametric studies of the cost/benefit ratios with various schemes for minimizing power tilt.

Using the energy and momentum equations, together with the fact that both the region's average and hot channel must have the same pressure drop, there results

$$\frac{\dot{m}_H}{\dot{m}} = \left[\frac{\frac{T_2}{T_1} \left(1 + \frac{C_F}{4} \right) + \left(\frac{C_F}{4} - 1 \right)}{\left(\frac{T_2}{T_1} \right) \left(1 + \frac{C_F}{4} \right) + \left(\frac{C_F}{4} - 1 \right)} \right]^{1/2} \quad (5)$$

where $\dot{m}$ = mass flow rate; T = gas temperature, and C_F = friction coefficient fL/D . Subscripts 1 and 2 represent inlet and outlet conditions, respectively, and subscript H represents the hot channel. The unsubscripted values of $\dot{m}$ and T_2/T_1 represent the average condition for the region. Advantage was taken of the fact that the normalized pressure drop $\Delta P/P$ is small, although pressure effects could have been included. Because $\dot{m}_H/\dot{m}$ is close to unity and f is a weak function of $\dot{m}$ for turbulent flow, a constant (average) value is used for C_F in Eq. (5).

Substituting the Blasius relation for f and rearranging,

$$\frac{\dot{m}_H}{\dot{m}} = \left[\frac{F_H + C}{F + C} \right] \quad (6)$$

where F = region peak-to-average power relative to the entire core, F_H = column peak-to-average power relative to the entire core (product of F and the column power tilt), C = a constant = $\frac{1.3055 T_1 \dot{m} C_p}{Q}$, $\dot{m}$ = core average flow per channel, Q = core average power per channel, C_p = specific heat of helium, and T_1 = gas inlet temperature. The coolant flow in the hot channel and average channel of a region calculated by GAC² compares very well with the results of Eq. (6), as given below.

Core (Ref. 2)	Column Power Factor, F_H	Region Power Factor, F	Flow Rate $\dot{m}/\dot{m}_H$	
	F_H	F	Eq. 6	Ref. 2
Designs I, II	1.706	1.297	1.069	1.064-1.069
Designs III-V	1.516	1.201	1.050	1.052-1.053

In other words, with the same flow orifice and pressure drop, the hot channel can carry five to seven percent less flow than the average in a region.

Using the same analysis, the effect on exit gas temperatures can be found,

$$\frac{T_{2H} - T_1}{T_2 - T_1} = \frac{F_H}{F} \left[\frac{F_H + C}{F + C} \right] \quad (7)$$

Results given below show good agreement with the computer codes.

Core (Ref. 2)	$\frac{T_{2H} - T_1}{T_2 - T_1}$	
	Eq. (7)	Ref. 2
Designs I, II	1.400	1.400-1.406
Designs III-V	1.326	1.328-1.330

The effect of power tilt on the maximum fuel temperature is a little more complicated because it involves the axial power distribution. Figure 12 shows the normalized exit temperature versus column power tilt, with the two results from Ref. 2 superimposed.

Fig. 12. Normalized exit gas temperature versus column power tilt.

2. Unit Cell Temperature Calculations

The finite element heat-conduction computer program, AYER,¹⁴ was used to calculate temperatures in the unit cell shown in Fig. 8. A typical plot of temperature isotherms is given in Fig. 13. This figure represents the hot channel of Design I. A summary of the temperature differences is given below.

	ΔT (K)	ΔT (°F)	%	GAC Ref. 2 ΔT (°F)
Fuel rod	161	289	30	273
Fuel-moderator gap	91	163	17	163
Moderator graphite	76	136	14	140
Coolant film	213	384	39	384
Total	541	972	100	960

These temperature differences are nearly the same (within a few degrees) of GAC's calculations using different methods but the same boundary conditions and properties.

The equivalent graphite conductance for the web of the unit cell between the fuel and coolant holes is defined as $C_g = q''_w / \Delta T_m$ where q''_w is the heat flux at the coolant channel wall and ΔT_m is the temperature difference through the moderator graphite. Since the thermal conductivity of graphite used in these models is constant, C_g will also be constant for fixed geometry. Using the results described above for Design I, $C_g = 5680 \text{ W/m}^2 \cdot \text{K}$

Fig. 13. Isotherms in unit cell for GAC Design I, hot channel.

(1000.2 Btu/h-ft²-°F) versus 5718 W/m²·K used by GAC^{2,3} (used for all five designs). The AYER result for the 210-hole fuel element in Designs II through V is $C_g = 6581 \text{ W/m}^2 \cdot \text{K}$ (1160 Btu/h-ft²-°F). This difference can account for a maximum of 10 K (18°F) in comparing Ref. 2 results with those included here.

Both the fuel rod ΔT and the moderator graphite ΔT are directly proportional to the material's thermal conductivity. A less conservative set of values (Sec. IV.A.4) for the thermal conductivities would therefore reduce the ΔT s tabulated above accordingly.

3. Axial Temperature Distribution

OPTION¹⁹ is a LASL-developed computer program to simulate systems characterized by heat transfer between solid heat-generating parts and fluid (usually gas) flowing in a network of passages. Such systems are typified by gas-cooled nuclear reactors of the VHTR type. A typical problem which the OPTION program will solve is one having parallel or branching flow passages of different cross section with transverse thermal communication between the flow passages and solids. For the VHTR designs in this report, the parallel flow passages in the reactor core consist of constant diameter circular channels with varying inlet loss coefficients. The power input to each channel from the surrounding solid material varies axially and from channel to channel. The six flow channels surrounding the fuel pickup hole in each moderator block that are smaller in diameter than the average (Figs. 4-9 of Ref. 3) can be modeled explicitly, or (in this case) for preliminary calculations, lumped as an equivalent number of standard channels. To calculate the average core temperatures, it is sufficient to model only one axial flow passage and its corresponding solid material (unit cell). The total core flow and power are obtained by multiplying the results for the single channel by the total number of equivalent channels (accounting for the few that are smaller than average). The solid material can be modeled explicitly using a finite-difference mesh similar to that in Fig. 8. The program is capable of modeling sections of a complete moderator block (or blocks) accounting for the heat conduction at inhomogeneities near poison holes, fuel pickup hole, and the edges of the

moderator block. For these preliminary calculations the detailed two-dimensional heat conduction calculations were done by the much faster computer program AYER, as described in Sec. 2. Using the equivalent thermal conductance for the unit cell from the AYER calculation as described above, a simple concentric annular region model was developed. For the same local boundary conditions (coolant temperature, convection heat-transfer coefficient, and heat generation) the annular model gives the same temperature differences in the solid materials as the AYER finite element model. The fuel-moderator gap is included in the annular ring model with a gap conductance given by Curve A of Fig. 11. The simplified conduction model for the solid materials greatly speeded the calculations enabling all five designs to be analyzed in less than one minute of 7600 computer time.

To calculate the maximum core temperatures with this model, the equivalent average channels are connected in parallel with two additional coolant channels. The first additional channel has a radial peak-to-average power, F , corresponding to that of the highest powered region. The second has a peak-to-average power, $F \times F_c$, corresponding to the channel in that region with maximum power tilt. The values for F and F_c corresponded to those used by GAC in accordance with the conclusions of Sec. III.B, and shown in Table III. The coolant mass flow rate in the first additional channel is set by the requirement to achieve a uniform exit gas temperature from all refueling regions in the core, and the specified heat generation rate, F times the average. The inlet loss coefficient was the smallest possible value, corresponding to that of a fully open flow control valve.³ The calculated overall pressure drop for this channel, given the helium mass flow rate, inlet and exit loss coefficients, flow channel geometry and friction factor, and prescribed power input is the overall pressure drop for the core to which all other channels (and refueling regions) must conform. The inlet loss coefficient for the equivalent average channels is adjusted by the computer program (corresponding to the adjustment in flow control valve setting that would take place in an operating reactor) such that the core pressure drop is matched. The flow, power

level, and exit temperature in this equivalent channel is, of course, set by the requirement that it be "average."

Finally, the second additional channel with power relative to average of $F \times F_c$ has the same inlet loss coefficient as the first because it is in the same refueling region. Its heat generation and overall pressure drop were set by the above constraints. The dependent variables are the mass flow rate and exit temperature, calculated by OPTION. Corresponding solid material temperatures are also calculated as described for the average channel, except with the appropriate boundary conditions, to complete the problem. The solid materials surrounding each of the parallel channels in the OPTION model were assumed to be thermally isolated from each other. This is an assumption which is probably conservative, but not excessively so considering that the heat-generation rate typically changes slowly across a refueling region. The hottest channels would thus tend to be grouped together and be relatively far away from any cooler channels.

The axial power distribution used was obtained from Ref. 2 in accordance with the conclusions in Sec. III.

In Figs. 14 through 18 predicted axial temperature distributions are presented for the coolant gas and fuel in both the average channel and hottest channel in the core. These figures indicate that the calculational results presented herein are in reasonable agreement with those of Ref. 2 and, therefore, given that the fuel performance is as expected, the core exit gas and process temperatures predicted in Ref. 2 can be achieved. Table IV summarizes the peak fuel temperatures for the five designs.

Two small corrections to the fuel temperatures made by GAC³ were not included here and could account for a maximum of 15 K (27°F) in the temperature difference between GAC and OPTION results. The first is a correction (-0.8%) to account for the heat deposited outside the core in reflectors and metal structure. The second is a correction to the moderator-equivalent heat conduction (-2%) to account for the six channels per fuel block which are smaller than the typical diameter (Figs. B-1 through B-3 of Ref. 2). The smaller correction was neglected for conservatism; however, in the latter

Fig. 14. Temperatures for Design I.

case, it is felt that a more detailed analysis is needed in order to determine the proper size of correction needed, if any.

Finally, it is noted that a more complete design analysis of a VHTR for process heat applications should consider the time-temperature history of the fuel including effects of control rod movement on local power peaking. The fuel-failure and fission product release model must ultimately integrate this analysis with burnup and irradiation effects to determine the actual temperature capability of the core.

Fig. 15. Temperatures for Design II.

4. Conclusions

The fuel element and reactor design modifications identified in Ref. 2 and summarized in Sec. II do result in a VHTR core design that could produce exit helium gas temperatures up to 1500 K (2200°F), as reported in Ref. 2 (temperatures above 1290 K (1862°F) would require improved fuel technology). A detailed design study of a particular core might profitably be based on a nominal set of material properties and design basis assumptions (as opposed to the "conservative" values used herein and in Ref. 2). For example, the thermal conductivities

Fig. 16. Temperatures for Design III.

of fuel rods and moderator blocks, discussed in Sec. IV.A, are conservative, near minimum values considering the range of graphite shim particles, temperatures, and irradiation effects. If the thermal conductivity for solid materials surrounding the hot channel were modeled on the basis of its actual shim content, temperature, and irradiation history, the fuel temperatures would be reduced. Similar statements can be made about the effects of other modeling details. However, there is always the risk that some of the reported or measured values could be in error and affect the actual temperatures experienced in the reactor. Since the fuel performance is strongly dependent on its operating temperature, an assessment of the risks of

Fig. 17. Temperatures for Design IV.

local overtemperature due to the propagation of accumulated random errors in expected properties, heat generation rate, coolant flow rate, boundary conditions, etc., should be made. The treatment of manufacturing tolerances, measurement errors, and modeling uncertainties could then be treated on a probabilistic basis, resulting in a quantitative assessment of the thermal design margin. Such methods are finding increasing acceptance throughout the nuclear industry.^{20,21,22}

Fig. 18. Temperatures for Design V.

V. ALTERNATIVE DESIGN MODIFICATIONS

Although the number of potential core design modifications is rather extensive, and should even include the "pebble bed" concept, those treated in this section are limited to designs which could be considered a logical extension of the GAC VHTR core designs presented in Ref. 2. These design modifications can be placed into three categories: (1) Those based on improved fuel rod thermal conductivity, (2) Those based on improved coolant heat-transfer characteristics, and (3) Those based on the pin-in-block or "cold block" concept.

A. Improved Fuel Thermal Conductivity

The "design" value for pitch-bonded fuel rods¹¹ is 6.9 W/m·K (4 Btu/h-ft·°F). One reason for this low value is due to the lower heat treatment temperature limits of the SiC layer in the TRISO fuel particles. At these temperatures, the matrix of the fuel stick does not fully graphitize (crystallize); and as a result, the thermal conductivity (λ) of the structure is low. In experiments performed at LASL, higher heat treatment temperatures have been demonstrated to be beneficial in increasing the crystallinity of the graphite matrix and thus increasing λ . For example, a heat treatment temperature of 2200°C (versus 1800°C) increases the room temperature value of λ by a factor of at least three. The higher heat

TABLE IV

TEMPERATURE PREDICTIONS FOR PROCESS HEAT REACTORS

	Temperature K (°F)				
	Design I	Design II	Design III	Design IV	Design V
Process temperature	922 (1200)	1033 (1400)	1144 (1600)	1256 (1800)	1367 (2000)
Fuel exit gas temperature					
GAC* average	1062	1178	1291	1406	1518
Hot channel	1228	1379	1461	1592	1715
OPTION average	1056	1173	1284	1397	1511
Hot channel	1220	1373	1452	1580	1705
Peak fuel temperature in average channel					
GAC*	1328	1324	1433	1545	1656
OPTION	1334	1365	1471	1579	1686
Peak fuel temperature in hot channel					
GAC*	1652	1631	1679	1807	1928
OPTION	1651	1672	1716	1841	1963

* Ref. 2.

treatment temperatures may be accommodated by TRISO particles in which the SiC has been replaced by ZrC. These experiments are directly related to the HTGR fuel stick matrix since the matrix is made up entirely of pitch binder which has been heat treated at 1800°C. A more effective manner of improving the heat transfer within a fuel stick is to use a matrix with some fraction of fully graphitized flour as starting material. Past experience in the Rover program indicates that improvement in the value of λ in such a fuel matrix could be as much as a factor of ten over that now obtained in HTGR fuel rods. This could be done by extruding the coated particles in graphite, a technique which was highly developed at LASL during the course of the Rover program. For an assumed increase in the fuel rod's thermal conductivity by a factor of five, the maximum fuel temperature for the hot channel can be decreased by 128 K (321°F) for Design I. The corresponding decrease in maximum fuel temperature for Designs II through V is 71 K to 33 K (127°F to 59°F).

A second advantage of replacing the SiC diffusion barrier with ZrC is the potential for having the fuel particles withstand higher operating temperatures. The key question that must be answered is whether or not acceptable dimensional stability and fission product retention can be achieved at the radiation fluences and burnups expected in the VHTR, and current programs are directed toward this question.

B. Alternative Coolants with Improved Heat-Transfer Characteristics

Mixtures of helium with other inert gases (argon, krypton, and xenon) can have improved heat-transfer coefficients for a given fuel geometry and coolant inlet and outlet temperatures. This has been documented elsewhere,²³ but the derivation is straightforward. For turbulent flow in tubes, the Dittus-Boelter form of the heat-transfer correlation is

$$Nu = C (Re_d)^{0.8} (Pr)^{0.4}$$

where Nu = Nusselt number, hd/λ , Re_d = Reynolds number based on tube diameter $\dot{m}d/A\mu$, Pr = Prandtl number, C = empirical constant, h = heat transfer coefficient, λ = gas thermal conductivity, $\dot{m}$ = mass flow, μ = viscosity, and d, A = tube diameter and

flow area. For fixed geometry, the equation can be normalized to a corresponding equation for pure helium:

$$\frac{h_2}{h_1} = \frac{\lambda_2}{\lambda_1} \left(\frac{\dot{m}_2 \mu_1}{\dot{m}_1 \mu_2} \right)^{0.8} \left(\frac{Pr_2}{Pr_1} \right)^{0.4}$$

where subscript 2 refers to the binary gas mixture and subscript 1 refers to pure helium. When the reactor power and the gas inlet and outlet temperatures are fixed, the gas mass flow $\dot{m}$ is inversely proportional to its specific heat. For an ideal gas, the specific heat is inversely proportional to the molecular weight, therefore, $\dot{m} \propto MW$, where MW is the molecular weight.

$$\frac{h_2}{h_1} = \frac{\lambda_2}{\lambda_1} \left(\frac{MW_2 \mu_1}{MW_1 \mu_2} \right)^{0.8} \left(\frac{Pr_2}{Pr_1} \right)^{0.4}$$

For various mixture ratios, the thermal conductivity, viscosity, and Prandtl number is available in the literature^{24,25} as a function of the equivalent molecular weight of the mixture. By substituting the appropriate values, Fig. 19 is obtained. This shows that for a mixture of helium-xenon, the heat-transfer coefficient can be increased by 35% at a molecular weight of about 30, or for He-Kr by about 24% at a molecular weight of about 20, or for He-Ar by about 10% at a molecular weight of about 15. Based on the previous analysis, an increase in the coefficient of heat transfer by 25% would result in

Fig. 19. Relative heat-transfer coefficient versus molecular weight.

a reduction of the maximum fuel temperature in the hot channel of approximately 57 K (103°F) for Design I or 39 K (70.5°F) to 31 K (55°F) for Designs II through V.

Increasing the coolant's molecular weight will increase the pressure drop and pumping power and by a similar procedure, using the Blasius equation for the turbulent friction factor,

$$\frac{\Delta P_2}{\Delta P_1} = \left(\frac{MW_2}{MW_1} \right)^{0.75} \left(\frac{v_2}{v_1} \right)^{11.25}$$

This relationship is plotted in Fig. 20. The curves for the three combinations of gases are indistinguishable. If He-Kr of molecular weight 20 were used as the coolant for Design I, for example, the maximum fuel temperatures would be reduced by about 57 K (100°F) at a cost of an increase by a factor of 3.5 in pressure drop throughout the loop (assuming fixed geometry) and pumping power. This could eliminate this method of temperature reduction, depending on the tradeoffs. The same consideration would apply if the binary mixture were used in the secondary loop and not circulated through the reactor. The He-Xe mixture is not suitable for use in the primary coolant loop because of the high neutron absorption cross section for xenon. This would be no bar to its use in the secondary loop, however.

Fig. 20. Relative pressure drop versus molecular weight.

C. "Cold Block" Design

There are, of course, a number of viable alternative core designs which take advantage of the improved heat transfer possible when the coolant circulates directly around the fuel. The Westinghouse design²⁶ for the proposed VHTR is one example. The design discussed here has the advantage that no large extrapolations of technology are necessary. All of the knowledge available regarding manufacturing, properties, and performance of standard HTGR pitch-bonded fuel rods is directly transferable. At the same time, the benefits derived from the modifications in Secs. A and B, above and in Ref. 2 are still available. The discussion below is directed at Design I with the 132-fuel-hole moderator block, but it could be applied equally as well to the 210-fuel-hole blocks of Designs II through V. This concept could be tested experimentally in an operating HTGR or VHTR, side by side with the standard fuel design. It would require, however, the replacement of top and bottom reflector blocks along with the fuel column during a refueling cycle of the reactor.

1. Discussion

The standard 132-fuel-hole moderator block used in GAC's Design I process heat reactor has a maximum fuel temperature of 1652 K (2513°F) in the hot channel (radial peaking factor of 1.706) with an outlet temperature of 1228 K. The core average outlet temperature is 1033 K (1400°F). At the axial location where the peak fuel temperature occurs, the gas temperature is 1075 K. The local ΔT to the center of the fuel rod (577 K) is composed of the following parts: Gas film ΔT = 299 K, graphite moderator ΔT = 83 K, fuel stick to moderator gap = 101 K, and fuel stick ΔT = 163 K.

2. Proposed Changes

The moderator and gap ΔTs are eliminated by eliminating the 72 coolant channels and increasing the diameter of the hole containing the fuel stick to 22.06 mm (the fuel sticks are 15.88 mm in diameter).

The total flow area in the 132-annuli is the same as in the original 72 coolant holes. The surface area for heat transfer on the 132 fuel rods is 40% greater than in the 72 coolant holes. After correcting for different hydraulic diameters and Nusselt number correlations for the annulus, the

film ΔT at the location of peak fuel temperatures is reduced to 200 K. The local peak fuel temperature, given the same fuel cycle and nuclear power peaking, is reduced by 215 K (387°F). However, because of the changes, the axial location of the peak fuel temperature is shifted axially and the new maximum is 1479 K, a net reduction of 173 K (311°F).

The same relative reduction in fuel temperature will occur throughout the core; so to a first approximation, the average exit gas temperature can be increased by this amount and maximum fuel temperatures will not change.

A comparison between fuel temperatures for the "hot block" versus "cold block" using boundary conditions for Design I is given in Fig. 21. The solid lines are for the "hot block" as calculated by GAC.² The OPTION calculation for the "cold block" assumed flow in an annulus surrounding the fuel rod as described above. The model contains two approximations (which tend to cancel) that can be improved in future studies. The first is that all of the heat is assumed to be generated in the fuel rod and none in the moderator. If 10% of the heat were generated in the moderator,⁵ the ΔT s in the fuel rod and gas boundary layer adjacent to it would be reduced accordingly; however, the local gas bulk temperature would remain the same. On the other hand (the second approximation), the Nusselt number correlation was not adjusted to account for the flow annulus instead of a circular channel which would increase the ΔT in the gas boundary layer by about 10%.

3. Mechanical Design and Manufacturing

The fuel sticks can be centered in the annulus in any of several ways. Centering need not be perfect. The easiest way is to drill the hole to about the same size as presently--slightly larger than the fuel stick. Then pull a broach through the fuel block which would enlarge the hole except for three small triangular ribs projecting to the fuel stick (Fig. 22). Broaching is a one-step operation and all 132 holes can be done simultaneously on a multiple machine. Since the 72 coolant holes are eliminated, the costs of manufacturing the moderator block should remain essentially constant. The refueling cycle and method of removing spent fuel from the reactor need not be changed. The fuel

Fig. 21. Temperatures for modified Design I.

block must include a method of axially supporting the column of fuel sticks during refueling. Also, the helium coolant flow must be channeled around the axial support and into the top of the annulus in the next lower block. There are many inexpensive designs that could perform satisfactorily. A threaded plug containing flow holes which exit to a counterbored plenum is one such design (Fig. 23). Since the number of holes drilled into the block is reduced from 210 to 138 (including six poison holes) the hole pitch is increased from 22.99 mm to 28.36 mm, eliminating two rows of holes across the hex. This allows ample room for the flow annulus.

Fig. 22. Ribbed centering device.

Fig. 23. Method of axially supporting fuel sticks.

4. Safety

Two safety-related issues arise because of the flow annulus. First, in the event of a loss-of-flow accident, the fuel rod is not as well coupled to the massive moderator graphite with its large heat capacity. This is mitigated to some extent, since the larger helium gap (3.1 mm versus 0.13 mm) can transfer the decay power (at 5 h after the accident) with a radial ΔT on the order of 20 K at the maximum. In effect, the beads would fail and release fission products about 12 minutes sooner. If emergency core cooling is started, the fuel could be kept cooler with the same flow capacity, an advantage for the proposed design.

The second potential problem involves possible flow blockage in the annulus due to fuel rod

breakage with irradiation, etc. The pitch-bonded fuel rods used in Ft. St. Vrain and intended for Fulton have demonstrated⁵ good integrity under exposures exceeding 8×10^{21} n/cm². The experience gained from the postoperative examination of fuel rods removed after refueling standard "hot block" HTGRs will aid immeasurably in assessing this potential problem. The fact that the fuel rods will always tend to change dimensions and alter the flow passage geometry during operation could be accommodated by the adjustable flow orifice, and by reloading the core at refueling such that each fuel column contains a mixture of old and new fuel (thus "averaging" the flow impedances). A thermal analysis of the effects of flow blockage, considering heat conduction to adjacent unblocked coolant channels should be conducted. As a first approximation, assuming 100% flow blockage in an annulus (an unlikely possibility) and that the heat is conducted only as far as the six surrounding coolant channels, the calculated maximum fuel temperature increase in the blocked channel is about 222 K (400°F). Conduction to channels beyond the six surrounding ones would tend to reduce this value. The probability of blockage should be low enough that several adjacent channels cannot all be blocked. In any case, the VHTR core could be designed with the conventional "hot block" design; and, as experience is gained on fuel performance and dimensional stability, the improved "cold block" fuel could be introduced and evaluated without risking large-scale flow blockages in an entire core.

VI. SUMMARY AND CONCLUSIONS

The HTGR core designs which have been developed for electrical power production can be successfully adapted to produce higher exit gas temperatures for process heat applications. The modifications suggested in Ref. 2 can result in exit gas temperatures approaching 1255 K (1800°F) with present fuel. Improved fuel, having higher temperature capability, could permit exit gas temperatures as high as 1477 K (2200°F). Alternative modifications such as fuel rods with higher thermal conductivity, coolant gas with a higher heat-transfer coefficient, and a "cold block" core design could be made to improve the higher temperature capability. The ultimate design of reactors containing the

proposed modifications requires more detailed analysis to account for control rod movement, fuel burn-up, etc. In a power-flattened core with many similar parallel coolant channels, a probabilistic approach to the treatment of manufacturing tolerances, design uncertainties, and modeling assumptions can result in a quantitative assessment of the margin between operating temperature and

material failure limits. This approach would be adapted to the design of the process heat VHTR, regardless of the choice of design concept. Further, the various alternative designs should be evaluated on the basis of their relative resistance to possible accident and/or malfunction conditions as well as on economic and nominal performance parameters.

APPENDIX

POWER DISTRIBUTION CALCULATIONS FOR THE GAC REFERENCE (FGS) DESIGN

As mentioned previously, the five GAC process heat designs are based on modifications to the Fulton Generating Station (FGS) 3000 MW(t) reference design. We have used one- and two-dimensional models to calculate radial and axial power distributions for the FGS initial core. These calculations were performed to establish the adequacy of our models, methods, and data. In particular, the calculations established that a one-dimensional model is adequate for parametric studies of the unrodded axial power profile, and also provided verification of the radial power-peaking factors for the FGS initial core.

All of the one-dimensional calculations were performed with the DTF-IV code⁶ in S_4 approximation using nine-group cross sections. Cross sections for the nine-group energy structure, which includes five fast and four thermal groups as shown in Table A-I, were generated for appropriate temperatures and compositions with an interpolation code from a library produced with the MICROX code.⁸ Neutron flux spectra from the one-dimensional calculations were used to 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 a two-dimensional (r-z) calculation with the TWOTRAN-II code.⁷

The one-dimensional (cylindrical) model used to calculate the gross radial power profile is described in Table A-II. 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

TABLE A-I
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

TABLE A-II
ONE-DIMENSIONAL RADIAL MODEL (FGS)

Zone	Description	Outer Radius (cm)	Relative Radial Zoning		
			Uranium	Thorium	Burnable Poison
1	Core ring 1	50.3	1.012	0.947	1.475
2	Core ring 2	133.0	1.012	0.947	0.820
3	Core ring 3	219.2	1.012	0.947	1.147
4	Core ring 4	407.5	1.012	0.947	0.894
5	Core ring 5	421.9	0.841	1.731	1.806
6	Radial reflector	524.8	0.0	0.0	0.0

standard fuel columns). Zone 4 includes the remainder of the active core excluding a thin buffer region (Zone 5) at the core-reflector interface. The thin buffer region (Zone 5) contains a lighter uranium loading and a heavier thorium loading than the rest of the core to reduce the power spike near the core-reflector interface. Material compositions of the radial zones are volume-weighted averages over

the axial and azimuthal directions. Except for the buffer region, a uniform radial and azimuthal fuel loading is used in the initial core.

At the beginning of the initial cycle, control rods are present in the central refueling region (Ring 1) and in 6 of the 12 refueling regions in Ring 3. These control rods were homogenized into their respective radial zones using self-shielding factors obtained from an auxiliary cell calculation in which the control rod is represented explicitly. 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. The distribution of poison pins in the initial core, when averaged over the axial and azimuthal directions, results in the relative radial zoning shown in Table A-II. As was the case for the control rods, self-shielding factors from an auxiliary cell calculation were used to homogenize the burnable-poison pins into the five radial core zones.

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

Four different calculations were performed with the radial model as described in Table A-III. Normalized gross radial power profiles, $P(r)/\bar{P}$ where $\bar{P}$ is the volume-weighted average, are shown in Fig. A-1 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

Fig. A-1. Normalized gross radial power distributions for the reference case (Case 1), without Xe and Sm (Case 2), without control rods (Case 3), and without control rods or burnable poison (Case 4).

radial profile, and result in a flatter radial power distribution. Radial zoning of the burnable-poison pins has a similar but much smaller effect.

Case 1 corresponds approximately to the hot-critical configuration at the beginning of the initial cycle (BOIC). Except for fuel depletion effects, Case 4 is representative of the configuration at the end of the initial cycle (EOIC). Figure A-1 shows that the gross radial peaking factor, $P_{\max}/\bar{P}$, is 1.33 at BOIC and 1.71 at EOIC. The EOIC value will probably be smaller than 1.71 because the more rapid fuel depletion in the high-power regions will tend to flatten the radial power distribution. In addition, one would expect subsequent fuel cycles to have smaller gross radial peaking factors than the initial cycle because more detailed radial zoning is used in reload segments.* The PSAR⁵ gives a maximum (over all cycles) gross radial peaking factor of 1.6. Our results for the initial cycle appear to be consistent with this value.

The overall radial peaking factor is required to calculate hot-channel temperatures including the maximum fuel temperature. In addition to the gross peaking factor, the overall peaking factor includes local peaking effects. In the initial core the most important of these local effects is the power tilt within a refueling region. This intra-refueling

* 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 A-III
ONE-DIMENSIONAL RADIAL CALCULATIONS

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

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 these differences 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) cell model of a refueling region was used to evaluate the intra-refueling region power tilt. Calculations were performed 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. Results of these calculations are shown in Fig. A-2 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. Thus, 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 evaluated with an appropriate cell model, 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 (1.71×1.13) 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

Fig. A-2. Normalized local fuel-stick power distribution within a refueling region.

segment to four years for the oldest segment. This age-peaking effect increases the local peaking factor; however, the overall radial peaking factor is probably not larger than that for the initial cycle because the gross peaking factor is expected to be smaller, as explained previously. The PSAR⁵ 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 a one-dimensional (cylindrical) cell model, in which the fuel stick is represented explicitly, to compute the fission distribution within the fuel stick.

Results of the computations are shown in Fig. A-3 where we have plotted the fission density within the fuel stick for the maximum, average, and minimum fuel loadings in the FGS initial core at 1200 K. The curves have been arbitrarily normalized to unity at the center of the fuel stick. Note that the radial variation is rather small; central and surface values differ by only 1.0% for the typical

Fig. A-3. Radial power distribution within a fuel stick with the core at 1200 K.

fuel stick. 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.

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

As in the radial model, microscopic cross sections for the core materials correspond to core-average compositions and temperatures. However, microscopic cross sections for the top and bottom

reflectors correspond to graphite temperatures of 610 K and 1030 K, respectively. Thus the top and bottom reflector temperatures were assumed to be the same as 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 A-IV. Axial zoning for equilibrium Xe and Sm was assumed to be the same as that for uranium. The burnable-poison pins and the seven control-rod pairs were homogenized into the three axial core zones using effective (self-shielded) cross sections.

Calculations performed with the axial model are summarized in Table A-V. The reactivity worths of equilibrium Xe and Sm and of the burnable poison are in good agreement with those obtained from the radial model. Thus, 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. Although the axial model cannot be used to calculate absolute control-rod worths, it should be adequate for computing relative worths as a function of axial position. The above results indicate that the worth of the seven control-rod pairs inserted halfway into the core is about one-third of the worth with the rods fully inserted.

TABLE A-IV
ONE-DIMENSIONAL AXIAL MODEL (FGS)

Zone	Description	Lower Boundary (cm)	Relative Axial Zoning		
			Uranium	Thorium	Burnable Poison
1	Top reflector	118.9	0.0	0.0	0.0
2	Top four core layers	436.1	1.201	1.127	1.200
3	Next three core layers	674.0	0.852	0.921	0.800
4	Bottom core layer	753.3	0.639	0.732	0.800
5	Bottom reflector	872.2	0.0	0.0	0.0

TABLE A-V
ONE-DIMENSIONAL AXIAL CALCULATIONS

Case	Description	k_{eff}
1	Reference calculation with burnable poison, equilibrium Xe and Sm, and 7 control-rod pairs fully inserted	1.017
2	Same as 1 but without Xe and Sm	1.056
3	Same as 1 but without control rods	1.042
4	Same as 3 but without burnable poison	1.140
5	Same as 1 but with 7 control-rod pairs inserted only halfway into the core	1.035

Normalized gross axial power profiles, $P(Z)/\bar{P}$, are shown in Fig. A-4 for Cases 3 and 5. The average axial profile with the control rods completely inserted is basically the same as that with the rods completely withdrawn. Equilibrium Xe and Sm and the initial burnable-poison loading have only a small effect on the axial power shape because the relative axial zoning for these components is essentially the same as that for uranium. Thus, the axial profile for Case 3 is representative of Cases 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 in Ref. 9.

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. A-4 do not include this local peaking effect. However, this effect was calculated with a one-dimensional (slab) cell model in which the fueled and unfueled portions of a fuel element layer were represented explicitly. Results of the cell calculation for a fuel element layer containing the average initial fuel loading, shown in Fig. A-5, yield a local axial peaking factor of 1.10 for the initial core. Because of the more rapid depletion at the ends of the fuel pins, these local power spikes will become less pronounced with fuel age.

As mentioned previously, the one-dimensional flux spectra were used to collapse the nine-group cross-section sets to four-group sets for use in a two-dimensional calculation of the unrodded initial core. The r-z model contains six radial zones and five axial zones with dimensions identical to those used in the one-dimensional models (Tables A-II and A-IV). Average axial and radial power distributions from the two-dimensional model agree well with results obtained from one-dimensional models in which compositions were averaged in the transverse directions. The radial distributions from the one- and two-dimensional calculations are practically identical since they typically agree within 0.5% except near the radial reflector-core interface where they

Fig. A-4. Normalized gross axial power distribution.

Fig. A-5. Normalized local axial power distribution in a fuel-element layer.

differ by 2%. As shown in Fig. A-6, 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 and one-dimensional models are adequate for use in parametric studies of the power distribution.

Fig. A-6. Normalized gross axial power distributions from one- and two-dimensional calculations (no control rods).

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