GENERAL ATOMIC DIVISION
GENERAL DYNAMICS CORPORATION

John Jay Hopkins Laboratory for Pure and Applied Science
P. O. Box 608, San Diego 12, California

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THERMAL DESIGN OF THE MGCR CORE

Work done by:

J. T. Rogers
R. Katz

Report written by:

J. T. Rogers
R. Katz

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THERMAL DESIGN OF THE MGCR CORE

1.0 CORE THERMAL PERFORMANCE

1.1 Core Thermal Performance Surveys

The MGCR prototype core is designed to satisfy the following performance requirements:

Coolant:	Helium
Core inlet temperature:	745°F
Core outlet temperature:	1300°F
Reactor inlet pressure:	1120 psia
Reactor outlet pressure:	1080 psia
Thermal power:	74.4 Mw

The core is required to produce this performance with a maximum cladding hot spot temperature of 1500°F. To permit selection of a reference design, survey studies were made for various core lengths, fuel rod diameters and assigned core pressure drops. The surveys also included several reactor power outputs and two different core pressure levels. The studies used the method of analysis described in reference (1), which is based on a chopped sine axial power distribution, and employs non-dimensional geometric parameters specific to the type of fuel element used. The ranges of the variables studied were:

Core length, ft:	4, 5, 6.37, 7.5, 8.5, 9.5
Fuel rod outer diameter, in:	1/4, 3/8, 1/2
Pressure loss, psi:	10 to 60
Thermal power, Mw:	46, 54, 63, 74.4
Inlet pressure, psia:	810, 1120

The different power levels were dictated by the shaft horsepower outputs considered, 20,000 and 30,000 SHP and the two core outlet temperatures considered, 1300°F and 1500°F. The surveys were based on the use of fuel element geometry consisting of 18 fuel rods and a central dummy rod in a hexagonal array contained in a hexagonal shroud. Calculations

were also made for a fuel element geometry consisting of 18 fuel rods and a central dummy rod in a circular array contained in a circular shroud. All studies assumed that full core length fuel elements are used. Typical results of the surveys are shown in Figure 1, where the number of fuel elements, fuel element pitch, maximum fuel temperature and fuel rod clearance-to-diameter ratio are plotted as functions of the core pressure drop for a given core inlet and outlet temperature, cladding hot spot temperature, core power, core inlet pressure and core pressure drop, core diameter and rod size. From such plots core designs meeting mechanical and thermal requirements, such as a minimum moderator block ligament dimension of 1/2" and a minimum rod clearance-to-diameter ratio of 0.10, were selected for nuclear studies by the Reactor Physics Section.

1.2 Core Reference Design

The nuclear studies (reference 2) led to the selection of the reference design as representing an economic optimum considering fuel cycle costs and capital costs of the core, pressure vessel and pressure vessel internals. Although there is little to choose between the two designs considered, with respect to power costs, the design using 3/8" rods has slightly lower costs, (although the difference between the two is probably within the accuracy of the calculations) and is preferred for mechanical reasons and so was selected tentatively for the reference design.

The reference design uses a fuel element employing a circular rod array in a circular fuel channel. The reasons for the choice of this arrangement are discussed in the Section 2.0.

The reference design thermal characteristics are listed in Table I:

TABLE I

REFERENCE DESIGN THERMAL CHARACTERISTICS

Number of fuel elements:	308
Heat transfer area, ft ² :	3,450
Maximum heat flux, Btu/hr ft ² :	160,000*
Average heat flux, Btu/hr ft ² :	73,600
Maximum-to-average power generation ratio:	
Radial within element:	1.15
Radial over core:	1.507
Total over core:	1.893*
Core thermal power, Mw:	74.4
Core mass flow, lb/sec:	102.0
Hottest channel heat transfer coefficient, Btu/hr ft ² °F:	984**
Core inlet temperature, °F:	745
Core outlet temperature, °F:	1300
Reactor inlet pressure, psia:	1120
Reactor outlet pressure, psia:	1080
Core pressure drop, psi:	27.2**
Maximum cladding temperature, °F:	1500*
Maximum fuel temperature, °F:	1900*
Rod clearance-to-diameter ratio:	0.134
Equivalent hydraulic diameter, ft:	0.0127
Reynolds number, central channel:	28,450**
Maximum velocity, central channel, ft/sec:	263**

*Based on chopped sine axial power distribution.

**With no moderator bypass flow.

1.3 Actual Power Distribution

As stated previously, the thermal performance surveys were based on the use of a chopped sine axial power distribution. From typical axial flux distributions for the previous graphite moderated core for a control rod insertion corresponding to the hot, beginning-of-life condition it was determined that the use of the chopped sine axial power distribution gave realistic results, in that the calculated hot spot temperature was not very different for the two power distributions. Figure 2a shows the maximum cladding temperature in the hottest channel for a case where the coolant outlet temperature was 1500°F . The design was selected using a chopped sine power distribution with a design hot spot temperature of 1700°F . The hot spot temperature calculated from the actual power distribution is seen from Figure 2a to be 1670°F . This paradox is explained by the facts that a) the power generation peaks towards the colder end of the core with the control rods entering from the hot end; b) the maximum-to-average power generation ratio in the unrodded portion of the core is improved over that for a chopped sine distribution over the entire core because of the increased leakage from the unrodded region; and c) the region of the core occupied by control rods still generates about 20% of the total core power. Axial power distribution, hottest channel cladding temperature and radial average coolant temperature are shown in Figure 2a as functions of distance through the core for a typical control rod insertion of about 40%. These results confirm the validity of employing the chopped sine axial power distribution for the survey calculations. Later results for axial power distributions typical of the start-of-life for the BeO moderated core, do not give such good agreement between the maximum cladding temperatures for the chopped sine and for the actual axial power distributions. See Figure 2b, where the calculated maximum hot spot temperature is 1585°F for the actual power distribution and 1500°F for the chopped sine power distribution. However the trends indicated by surveys using the chopped sine power distribution will still be valid.

The cladding and coolant temperatures for the typical rod insertion power distribution were calculated using both a step-by-step hand method and a digital computer method, the Cope code, programmed for the IBM 704 computer. The Cope code calculates cladding and coolant temperatures for a given fuel element geometry for any given axial and radial power distribution. It can be used to select the required channel orificing by apportioning the flow distribution radially so as to produce either uniform cladding hot spot temperatures or uniform coolant outlet temperatures from each channel. Typical calculations indicate no great difference in hot spot temperatures in each channel for uniform channel outlet temperatures and vice versa. For uniform outlet temperatures from each channel, the cladding hot spot temperature is highest in the channel with maximum power generation, falling off slightly in the other channels. This behavior is to be expected since, with the flow distributed in proportion to the power generation to produce the uniform channel outlet temperatures, the heat transfer coefficient does not fall off as rapidly as the flow, being proportional to $w^{0.8}$, so that the film temperature drop is reduced in other channels below that in the maximum power generation channel.

The Cope code has been used to predict the required flow distribution to provide uniform channel outlet temperature for a typical radial power distribution including the local radial power distributions within control rod cells. Calculations have shown that an orifice distribution sized to produce uniform channel outlet temperatures at design conditions will produce flow distributions at part loads which do not depart significantly from the distribution at full loads. Some departure from uniform channel outlet temperatures occurs, but the effect is small down to about 25% of full load power, the difference between the hottest channel outlet temperature and the core average outlet temperature being about 25°F at this point.

It should be pointed out here that, because of the excellent heat transfer properties of the helium coolant the high mass flow

through the core and the small equivalent diameter of the fuel element passage, very high heat transfer coefficients, of the order of $1000 \text{ Btu/hr ft}^2 \text{ }^\circ\text{F}$, are obtained in the hottest channel. The resulting film temperature drops, in spite of the relatively high core heat fluxes, are not very large, being, for instance, about 113°F , on the average, and 160°F as a maximum in the hottest channel. At the cladding hot spot location, the heat flux is, of course, below the maximum, and is generally close to the average, so that the film temperature drop at this point will be of the order of 120 to 130°F . Also, the coolant temperature rise is almost complete by the point at which the hot spot occurs. Therefore, the cladding hot spot temperature is governed mostly by coolant temperature rise and to a considerably lesser degree by the coolant film drop. Therefore, the cladding hot spot temperature will be less sensitive to uncertainties in the heat transfer coefficient in the MGCR than in other gas-cooled reactors where the film drop is higher. Of course, it will be somewhat more sensitive to uncertainties in the flow distribution.

1.4 Core Temperatures During Core Life-time

Although, as it was pointed out previously, typical power distributions corresponding to the control rod insertion for the hot, beginning-of-life condition result in hot spot temperatures not significantly different from those calculated for a chopped sine power distribution, it is realized that power distributions corresponding to end-of-life or intermediate conditions may produce hot spot temperatures considerably in excess of 1500°F for a core designed for a maximum of 1500°F hot spot temperature for the beginning-of-life condition, particularly if no fuel shuffling occurs and if the control rods move as a bank. Until a reference design had been chosen it was not feasible to investigate conditions other than start-of-life. Now such data have become available but are different from those used to choose the reference design since they are based on a more up-to-date calculation of the nuclear

characteristics of the core allowing for more realistic estimates of poison in the core for the latest design arrangement. A possible core arrangement required to produce the required performance with a maximum hot spot temperature of about 1500°F at the start-of-life differs from the reference design in the following respects:

Number of fuel elements:	348
Rods clearance-to-diameter ratio:	0.100
Maximum-to-average power generation ratio:	
Radial over core:	1.645
Total over core:	2.698
Core pressure drop, psi:	39
(Other characteristics which change and which depend on the above are not listed.)	

Time has not permitted the revision of the reference design to incorporate these changes. Figure 3 shows the axial power distributions for the start-of-life condition, the end-of-life condition, and for the intermediate condition of maximum control rod insertion which results as the burnable poison is consumed in competition with the formation of fission product poisons. Figure 4 shows the resulting cladding and coolant temperature along the hottest channel for the three reactor conditions, calculated using the Cope code. It can be seen that the hot spot temperature reaches 1660°F at full control rod insertion and 1650°F at the end-of-life for an initial hot spot temperature of about 1500°F. Of course, these temperatures are for no fuel re-arrangement during the core life-time and for control rod motion as a bank. In addition, no change in radial power distribution was considered which ignores the reduction in total heat generation in the initially hottest channel as burnup proceeds. Steps to reduce the hot spot temperatures during burnup include a) re-arrangement of fuel, which includes radial movements of full length elements; the turning of full length elements end-for-end, or the re-design of the fuel elements as

shorter elements, permitting both axial and radial re-shuffling; b) control rod programming; c) non-uniform burnable poison distribution; d) orifice re-arrangement; and e) selection of the reference design to meet the worst burnup conditions, rather than the start-of-life conditions. As information is just being developed in this area, it has not been investigated at all so far, so that little can be said other than to point out the problem and to list means that may be taken to overcome it.

1.5 Capability for Higher Core Outlet Temperatures

An investigation was made of the capability of a core designed for the initial gas outlet temperature of 1300°F to produce higher outlet temperatures. This investigation was confined to the thermal performance only, mechanical factors not being considered. The analysis was made on a simplified basis by assuming component efficiencies, fractional pressure drops and regenerator effectiveness remained constant and by neglecting bleed flows. A maximum clad temperature of 1700°F and a minimum inventory of 50% were imposed as limitations. The results are shown in Figure 5. It can be seen that the maximum clad temperature of 1700°F is reached at a reactor outlet temperature of 1480°F at the full turbine shaft horsepower of 32,000.

Further increases in reactor outlet temperature can be achieved only by decreasing inventory and power. At the minimum limit of 50% inventory, the power has been reduced to 62.5% of full power and the reactor outlet temperature has increased to slightly over 1490°F. The analysis is only approximate, but does indicate that it should be possible to closely approach the ultimate desired reactor outlet temperature of 1500°F at full shaft horsepower with the present reactor core reference design.

1.6 Hot Channel Factors

In all core thermal calculations the hot channel factors given in Reference 4 were used. The hot channel factors are listed in Table II:

TABLE II
ASSUMED HOT CHANNEL FACTORS

	<u>F_C</u>	<u>F_F</u>
Orifice precision:	1.00	1.05
Radial flux change with time:	1.10	1.10
Axial flux change with time:	1.00	1.05
Fuel passage change with time:	1.05	1.05
Heat transfer correlation:	1.00	1.20
Coolant non-mixing in channel:	1.00	1.15
Fuel loading non-uniformity:	1.00	1.03

The factor F_C is an allowance for departure of flow in the hot-test channel from the design value. The factor F_F is an allowance for departure of the film temperature drop in the hottest channel from the design value.

There are a number of ways of combining component hot channel factors to produce overall hot channel factors for design (5, 6, 7, 8). It is recognized that at this stage of the design the hot channel factors are factors of safety which account for the designers' ignorance of certain aspects of the design as well as for normal statistical deviations from design values. For instance, it is expected that the fuel element heat transfer and fluid flow experimental and analytical program described in Section 2.0 of this report, will enable the component factors for the heat transfer correlation and for fluid non-mixing to be reduced to the extent that they will truly represent the uncertainties (for example, 2 or 3 standard deviations) in the predicted values of these quantities and will not include the "ignorance factor" which is now applied. Since it is not justified at this stage to treat the above component hot channel factors statistically and since it was felt that to multiply all the component factors together to find an overall factor, thus

implying that all the deviations are cumulative, would be too conservative, the overall hot channel factor was determined by adding the component deviations. The resulting design hot channel factors are:

$$F_C = 1.15$$

$$F_F = 1.63$$

These values have been used throughout the design and may be thought of as reasonable safety factors. As more experimental and analytical information becomes available, the factors will be revised to truly represent statistical uncertainties. An analysis such as that given in Reference 7 will then be applied to predict the probability of occurrence of a certain cladding temperature range.

Safety factors have been used in the design to allow for uncertainties in the pressure drop due to improper flow distribution, F_P and to ignorance of the friction factor correlation, F_f . These factors are:

$$F_P = 1.20$$

$$F_f = 1.20$$

One additional safety factor to allow for uncertainties in the temperature drop across the fuel body, F_Q , has been used. Its value is 1.20.

2.0 FUEL ELEMENTS

2.1 Description of the Fuel Element and Thermal Performance Considerations

The MGCR prototype fuel element consists of 19-3/8" diameter rods arranged in a circular array and contained in a circular shroud which accurately defines the outer boundary of the flow channel. The central rod is unfuelled. The central rod and the twelve rods in the outer ring each have one helical spacer with a

pitch of 7.5 inches. The six rods in the inner ring each have two helical spacers of the same pitch. The individual rods are positioned by a spacer grid at either end of the element, arrayed to permit relative axial movement between individual rods and between the rods and the shroud. Dimensions and further details of the fuel element are given in Section 3.3.4 of this report. A diagram showing a cross section of the element with equations for quantities important in the thermal analysis of the element is shown in Figure 6.

Initially, the reference fuel element consisted of 19 rods in a hexagonal array in a hexagonal flow channel. The initial preference for this configuration was based on the assumption that the ratio of local to average heat transfer coefficients would be better than the ratio in a circular array fuel element because the flow passage size is more uniform. Also, less flow stratification would be expected with the hexagonal array. However, evidence has accumulated which shows that the amount of flow streaming and the heat transfer coefficient distribution in a rod bundle using a circular array is probably acceptable for a gas-cooled fuel element. A discussion of this evidence follows.

Experiments have been conducted on a 5/3 scale electrically-heated, air-cooled model fuel element in the General Atomic Laboratory (9, 10). To express the results of these experiments in a manner which facilitates comparisons a factor F_E , which may be likened to a hot channel factor, is defined:

$$F_E = \frac{T_{wM} - T_o}{T_{wC} - T_o} \quad 1.$$

where:

T_{wM} is the measured cladding surface temperature at a point.

T_{w_c} is the calculated cladding surface temperature at the same point.

T_o is the coolant inlet temperature to the model fuel element.

In the tests, the power to the individual rod heaters was adjustable independently to produce any desired radial power distribution within the bundle. Surface temperatures were measured at a number of points with thermocouples and also were investigated using temperature sensitive paints. Clad temperatures at a point were calculated by three methods:

- a) Method "a" used average values for the heat flux and the coolant temperature at an axial point.
- b) Method "b" used local values for the heat flux and average values for the coolant temperature at an axial point. (This assumption implies perfect coolant mixing.)
- c) Method "c" used local values for the heat flux and local values for the coolant temperature assuming no coolant mixing.

Tests were run with elements equipped with helical spacers and with no spacers. A power distribution simulating a typical nuclear power distribution across the element was used in some tests. Results for the helical spacer-equipped test elements are shown in Table III:

TABLE III
AIRFLOW TEST STAND ELEMENTS CLADDING TEMPERATURE FACTORS, F_E

A. Uniform Power Distribution

<u>Rod</u>	<u>Hexagonal Array</u>		<u>Circular Array</u>
	<u>Thermocouple Measurement (2)</u>	<u>Temperature Sens. Paint Estimate (3)</u>	<u>Temperature Sens. Paint Estimate (5)</u>
Center	1.21	1.23	1.36
Outer Ring - Corner Inner Surface	1.05	1.16	1.22
Outer Ring - Corner Outer Surface	0.94	1.14	1.15
Outer Ring - Side Inner Surface	1.02	-	-

B. Typical Nuclear Power Distribution

<u>Rod</u>	<u>Hexagonal Array</u>				<u>Circular Array</u>
	<u>Thermocouple Measurement (4)</u>	<u>Temperature Sens. Paint Estimate (5)</u>			<u>Temperature Sens. Paint Estimate (5)</u>
Center	0.93	1.05	1.20	1.29	0.96
Outer Ring - Corner Inner Surface	1.16	1.29	1.19	1.25	1.04
Outer Ring - Corner Outer Surface	0.97	1.41	1.26	1.04	1.06
Outer Ring - Side Inner Surface	1.07	(6)	(7)	(8)	(6)

NOTES:

1. Typical nuclear power distribution for the hexagonal array:

<u>Rod</u>	<u>Relative Power</u>
Center	1.0
Middle ring	1.14
Outer ring, corner	1.60
Outer ring, side	1.42

2. Average of four tests.
 3. Average of three tests.
 4. Average of two tests.

5. One test only.
 6. Calculation by method "a".
 7. Calculation by method "b".
 8. Calculation by method "c".
-

Because of the large scatter of the data between the thermocouple measurements and the semi-quantitative temperature-sensitive paint measurements and because of the paucity of data no definite conclusions can be drawn from the results shown in Table III. However, it appears that there is not a very significant difference between the results for the hexagonal array and the results for the circular array fuel element; at least, differences are probably within experimental errors.

The friction factors for circular and hexagonal array geometries have been measured at General Atomic and the results are shown in Figure 7. In the Reynolds number range of the MGCR prototype, the friction factor averages about 25% above the smooth tube correlation for both geometries. The circular array geometry gives higher friction factors than the hexagonal array geometry although one would expect the opposite because of the more open flow area in the circular fuel element.

Experience on the heat transfer program at Aerojet-General Nuclear for the ML-1 reactor has indicated that the heat transfer and flow characteristics of a 19-rod circular array fuel element with helical spacers in a circular shroud are quite satisfactory. Average heat transfer coefficients are about 17 to 23% higher than would be predicted by the Colburn equation for a smooth round tube and friction factors are about 36 to 38% greater than would be predicted for a smooth round tube. The test elements had a rod clearance to diameter ratio of about 0.136, almost identical to that in the MGCR reference design, 0.134.

The AGN and the reported GA experiments are for helical spacer arrangements which are different from the MGCR prototype reference fuel element spacer arrangement. The AGN tests are for fuel elements a) with spacers on all except the center rod and b) with spacers on all rods in the outer ring and on every second rod in the middle ring. The original GA spacer arrangement consisted of spacers on the center rod and on the rods in the outer ring. However, the geometries are sufficiently similar to the reference design geometry to permit the assumption that the heat transfer and friction factor characteristics will be somewhat similar.

Another factor to be considered in evaluating the heat transfer performance of a fuel element is the degree of coolant mixing between the various flow channels in the element. The experimental work at General Atomic (9) has indicated that helical spacers of the original design tend to reduce local cladding hot spot temperatures by improving coolant mixing across the test fuel element.

Although the spacer arrangement in these tests is not the same as that in the MGCR reference fuel element, one would expect the same tendency in the reference fuel element. On the other hand, calculations made at AGN using the Hectic code indicate that a change in the degree of turbulent eddy diffusivity of heat and momentum between channels from that expected in the ML-1 fuel element, to zero interchange (no mixing) only increases the cladding hot spot temperature from about 1450°F to about 1465°F. Also, increasing the degree of turbulent interchange by a factor of 5 or 6 above that expected only lowers the hot spot temperature to about 1425°F. These results indicate that a fuel element of this general type does not appear to be too sensitive to the degree of mixing between channels, at least under the ML-1 conditions. Of course, as pointed out in Section 1.0 of this report, the MGCR fuel element would be expected to be more sensitive to flow distribution than the ML-1 fuel element, where

the film temperature drop is relatively much greater mostly because of the nitrogen coolant.

Experimental heat transfer and friction factor work on the General Atomic HTGR Project has indicated that in a typical section of an infinite hexagonal array of circular rods without spacers the measured average heat transfer coefficients and friction factors do not fall much below the values predicted for flow in smooth tubes for rod clearance-to-diameter ratios of as low as 0.015, much smaller than the reference design value for MGCR. These results do not agree with the analytical results of Deissler and Taylor (11), which predict a significant falling-off in average heat transfer coefficients beginning at clearance-to-diameter ratios as high as 0.100. The conclusion that can be drawn from these results is that the equivalent hydraulic diameter concept which was used to correlate the HTGR results (as well as all other previously-discussed results) is applicable for rod geometries with clearance-to-diameter ratios such as in the MGCR fuel element. Further experimental work on the HTGR project indicates that the coolant velocity outside the laminar and buffer sub-layers in the constricted regions between rods does not decrease greatly from that in the more open regions for rod clearance-to-diameter ratios as low as 0.015. The HTGR results (12) show that the local radial mean velocity at the narrowest part of the gaps is about 50% of the overall mean velocity at a Reynolds number of 20,000. With the relatively much larger gaps in the MGCR reference design element, the decrease in local velocity will be much less. Experiments to confirm this statement are in progress. The bulk coolant temperature in the narrow regions also would not be greatly different from the values in the more open regions since the turbulent eddy diffusivities for heat and momentum are the same or almost the same. These facts are important when considering the application of out-of-pile experimental results to the varying surface heat flux and temperature conditions encountered in a reactor, as will be discussed in Sub-section 2.2.

It was concluded from a consideration of the points mentioned in the foregoing discussion that the circular array fuel element has a sufficiently good heat transfer and flow performance to warrant its selection for the MGCR reference design. Because of the better structural properties of the circular shroud and the probably lower costs of moderator blocks with circular fuel channel holes, the circular element was preferred and so was selected for the reference design. However, the hexagonal array element will be retained as a backup element

2.2 Thermal Performance Experimental Program

The results of the General Atomic heat transfer experiments in the airflow test stand emphasized the necessity of obtaining more intensive and detailed information.

Therefore, a more detailed experimental and analytical investigation of heat transfer and flow distribution for the 19 rod bundle fuel element was planned. The philosophy behind the investigation is to attempt to explore the complex interaction of phenomena in the fuel element heat transfer and flow distribution processes by separating the component effects and investigating each separately.

To carry out this program a four-times full size model of the 19 rod bundle fuel element was designed and fabricated. This model element will be tested in both circular and hexagonal shrouds with the fuel rods in circular and hexagonal arrays respectively. The shrouds are fabricated of plexiglass to permit visual examination of the flow distribution during mixing tests. The 1 1/2 inch outer diameter rods are fabricated from aluminum tubes and are not heated. Helical spacers were applied to the aluminum tubes by the chem-milling process.

The large scale test element was designed for use in the airflow test stand. The airflow test stand blower has ample capacity to permit coverage of the Reynolds number range of interest.

Four basic types of experiments will be performed with the large scale model. The four experiments are described briefly below. The latter two require the replacement of one or more of the standard aluminum tubes by special rods which are described under the appropriate headings. In addition to the four experiments on the unheated rod bundle, experiments will be conducted on a heated rod bundle using a configuration and spacer arrangement representative of the actual MGCR fuel element.

The ultimate object of the analytical and experimental program is to permit a prediction of the fuel element cladding maximum temperature under any given operating condition in the Maritime Gas-cooled Reactor, and to set confidence limits on this prediction. To make this prediction the local heat transfer coefficient in a fuel element must be predicted and, equally as important, the local coolant temperature distribution must be predicted. The latter prediction requires a knowledge of the coolant flow distribution and the degree of mixing between various flow filaments. The effect of temperature level on all these effects must be known. The outlined program is designed to enable the accurate prediction of hot spot temperatures in the MGCR to be made.

2.2.1 Velocity Distribution and Mixing Experiment

In this experiment, investigations of velocity distribution and mixing will be made. Velocity distributions will be measured using a pitot-static tube. Mixing will be investigated visually by means of tufts or similar devices and will be investigated quantitatively by introducing a traceable substance upstream and detecting it downstream. These experiments should yield an understanding of the flow distribution and of the channelling of individual streams and the effect of spacers, rod array configurations, fabrication tolerances, and shroud shape on these phenomena under isothermal conditions.

2.2.2 Friction Coefficient Experiment

In this experiment, the pressure drop will be measured and the friction coefficient calculated. The results of this

experiment will extend the results of the friction coefficient experiment for the present 5/3 full size model fuel element to helical spacer arrangements more representative of the proposed reactor arrangement. It is also planned to measure pressure losses associated with various proposed inlet and exit spider designs.

2.2.3 Mass Transfer Experiment

In this experiment, the local and average heat transfer coefficients around the periphery of the fuel rod at various locations in the test element will be determined. The method employed will be to measure local and average mass transfer by coating a test section of a rod with naphthalene and measuring the amount sublimated into the air stream and deducing local and average mass transfer coefficients during runs at various Reynolds numbers. By employing the analogy between mass and heat transfer, the corresponding heat transfer coefficients may be calculated. Results will give an insight into the local effects of spacers, into the variation of heat transfer around a rod due to the proximity of other rods and of the shroud, into the effect of fabrication tolerances and into the relative effects of circular and hexagonal arrays. Preliminary tests on an annular geometry indicate good absolute accuracy can be obtained.

2.2.4 Transient Heat Transfer Experiment

To obtain accurate predictions of average heat transfer coefficients on a given section of fuel rod in a given location in the fuel element bundle a transient technique will be employed. In this experiment a copper section of a test rod will be heated to about 300°F above the air stream temperature. The decrease of temperature of the copper section in a flow at a given Reynolds number will be measured by internal thermocouples. From a plot of rod temperature versus time the average heat transfer coefficient can be calculated. This method should predict

average heat transfer coefficients on a given location of a given rod quite accurately. The effects of rod location, rod array and shroud shape, and fabrication tolerances will be determined.

2.2.5 Heated Fuel Element Experiment

The results of the four preceeding experiments will be utilized in an attempt to predict hot spot temperatures and locations in a heated model of the actual fuel element. These predictions will be checked by measuring local cladding and coolant temperatures and local coolant velocities. Modifications to the calculation methods will be determined by the agreement between measured and predicted results. Information will also be obtained on the effect of various power distributions across the fuel bundle and, to some extent on the effect of temperature level.

2.2.6 Local Velocity Profile Experiment

In addition, local velocity profiles in the constricted region between rods will be measured on the large scale HTGR flow stand using c/d ratios typical of the MGCR fuel element. While these measurements will apply only to regions without spacers they will give an insight into the local velocity and temperature distributions to be expected with MGCR fuel element configuration.

It is anticipated that the described program will enable reliable predictions of fuel element cladding hot spot temperatures to be made. The program of course, is not fixed and will be modified as the need arises and as results become available.

As mentioned earlier, to develop the mass transfer technique and to obtain a measure of its absolute accuracy, mass-transfer experiments were performed using a simple annular geometry.

Typical results for a concentric annulus are shown in Figure 8. This curve shows the ratio of local circumferential average heat transfer coefficient to the fully-developed heat transfer coefficient as a function of the non-dimensional distance downstream from the beginning of the naphthalene test section. The points were calculated using the analogy between mass and heat transfer. The curve shows the starting-length effect typical of developing "thermal" and "mass transfer" boundary layers. The solid line shows typical results obtained by Boelter, et al., (13), for directly-measured heat transfer in the entrance length of a tube with a short calming section, which configuration is representative of the present mass-transfer tests. The agreement is seen to be quite good.

A typical circumferential variation of local-to-average mass or heat transfer is shown in Figure 9, for an eccentric annulus with an eccentricity, ϵ of 0.74. The fall-off in heat or mass transfer where the annulus narrows down at an angular position of about 180° is evident.

To determine the ability of the mass transfer technique to predict relative effects, the circumferential average amount sublimated as a function of non-dimensional distance downstream is shown in Figure 10 for a concentric annulus and for an annulus of eccentricity 0.74. The reduction in average mass transfer at any point for the eccentric annulus below the mass transfer for the concentric annulus is obvious. From the similarity between mass and heat transfer one would deduce a similar reduction in heat transfer for the eccentric annulus. Deissler and Taylor (14) predict such a reduction analytically. However, the measured reduction is less than the predicted reduction in the ratio of about 2.4 for the same eccentricity for a uniform peripheral wall temperature

distribution. Although no firm conclusions can be drawn from the few tests so far performed, the pessimism of Deissler and Taylor's analysis is similar to the pessimism of their analysis based on the same method for flow parallel to an infinite array of circular rods, as found on the HTGR project.

The absolute accuracy of the mass transfer tests is expected to be quite good. In Figure 11 are plotted results of a sample test run. The value of $C_m Sc^{2/3}$ is plotted against non-dimensional distance from the start of the naphthalene section, where C_m is the mass transfer Stanton number and Sc is the Schmidt number. In the fully-developed region $C_m Sc^{2/3}$ is about 0.0022. For the same Reynolds number, the value of $St Pr^{2/3}$, the equivalent heat transfer parameter, which, by the analogy between mass and heat transfer, should be the same, is 0.00213 from the Colburn equation. The measured value thus is within 3.3% of the expected value. Development work on the transient technique using an identical annular geometry to that used in the development of the mass transfer technique has started.

The proposed experimental program will provide sufficient information to permit good predictions of the fuel element cladding hot spot temperature and to set confidence limits on these predictions. Certain factors which effect the fuel element thermal performance cannot be fully investigated in the current experimental program. The most important of these factors are the effect of temperature dependent properties and the effect of the non-uniform surface heat flux.

However, it is anticipated that neither of these factors will exert much influence on the heat transfer coefficients nor on the flow distribution. The effect of temperature dependent properties on the heat transfer coefficient can be allowed for by employing conventional correction factors for gases of the form $\left(\frac{T_s}{T_c}\right)^n$, where T_s and T_c are the mean absolute cladding and

coolant temperatures respectively and the exponent n has a value of about 0.58 (15). Since the ratio $\frac{T_s}{T_c}$ is about 1.075 for the MGCR reference design and since the exponent is less than 1.0, the effect of this correction is small, so that an accurate experimental determination is not necessary. Similarly, it is expected that the flow distribution will not be disturbed significantly by the temperature dependent properties of the coolant.

The experimental heat transfer program will be carried out under conditions of essentially constant surface temperature. (By analogy the mass transfer technique is equivalent to heat transfer under constant surface temperature conditions if the sublimating substance is at a uniform temperature.) Most conventional correlations are either for uniform surface temperature or for uniform surface heat flux conditions. In a reactor fuel element, of course, neither condition is fulfilled. However, for fluids with Prandtl numbers close to 1.0, theoretical analysis indicates that heat transfer coefficients calculated for both conditions are within 5% of each other which indicates that the heat transfer coefficient for gases is not very sensitive to the surface conditions (16, p. 218). Furthermore, analysis by Kays and Nicoll (17), has shown that for fully established turbulent gas flow in a heat transfer system with cosine heat flux distribution, which may be taken as representative of a reactor, the heat transfer coefficients are nearly identical to those for constant heat flux conditions.

The effect of circumferential heat conduction in the fuel element cladding will not affect the heat transfer coefficients nor the flow distribution to any significant extent.

It is pointed out that the effect of operating temperature levels and the power distribution across an element on flow distribution can be assessed by analytical means using, for instance, the Hectic code, and utilizing input data determined from low temperature experiments. Also, the existence of high surface heat fluxes

will not produce unpredictably high cladding temperatures in the regions of narrow gaps between the fuel rods, since, as was pointed out earlier, experimental evidence shows that coolant velocities and bulk temperatures in constricted regions even smaller than those in the MGCR fuel element will not be greatly different from the coolant temperature in the more open regions. Experiment 2.2.6, of course, will give more information on this effect. In addition, the helical spacers should help to prevent such hot regions.

2.3 Mechanical Design Considerations in Fuel Element Thermal Performance

Calculations have been made to estimate the effect of fuel channel tolerances on cladding hot spot temperatures. Preliminary estimates for the hexagonal fuel assembly indicated that a 1% decrease in the distance across the flats of the assembly, about 0.020", would result in an increase of hot spot temperature of about 10%, or 150°F, above the design temperature. This estimate was based on the assumption that only the hottest channel departed from the nominal geometry and that this departure had a negligible effect on the core pressure drop.

More recent and more detailed calculations employing the Cope code have been made for the circular array fuel element and for power distributions corresponding to typical control rod insertions. For a core design which meets the most recent power distribution conditions, a decrease in hot channel flow area of 15% below the nominal, which is approximately equivalent to a decrease in fuel channel shroud diameter of about 0.067" in 2.000", i.e., about 3.3%, resulted in an increase in hot spot temperature for the start-of-life condition of about 80°F above the design value. From these calculations it was concluded that increases in hot spot temperature would not be significant if the fuel channel shroud diameter is kept within 0.010" of the nominal value, with tolerances on other dimensions of the fuel element assembly set in proportion.

The MGCR reference fuel element has an integral shroud which accurately defines the flow passage and, with the end spiders, maintains a clearance between the outer ring of rods and the shroud itself equal to one half the inner clearances between rods. It is necessary that the shroud be integral with the fuel element to maintain the proper outer clearances. If the shroud were not integral with the element, mechanical considerations of inserting the 76 inch long fuel element in its channel and particularly of removing it after possible thermal- and irradiation-induced distortion would require a large outer clearance, of 0.050 inches or even greater. The large outer clearance would cause coolant flow bypassing of the central region of the fuel element. This effect would increase cladding hot spot temperature. Conservative calculations, assuming no mixing between flow in the outer clearance region and the central region, predict an increase of about 200°F in cladding hot spot temperature for an outer clearance of 0.050" rather than the reference design value of 0.026". Other assumptions used which add to the conservatism of the calculations are the neglect of circumferential heat flow in the outer ring of rods and the assumption that 100% rather than about 92% of the heat is generated in the fuel. The effect of coolant mixing, circumferential heat conduction and the generation of a portion of the heat in the moderator would be to reduce the estimated increase in cladding hot spot temperature. However, there would undoubtedly be a sufficient increase to require limiting the outer clearance to something less than 0.050" which would hinder, if not prevent, ready removal of irradiated elements from core. For this reason an integral fuel element shroud is required. The integral shroud arrangement lends itself very well to the bypass flow scheme for cooling the moderator, discussed in Section 3.0.

Calculations using the Hectic code are planned to investigate in detail the problem of fuel channel tolerances and permissible variations from the reference design.

The primary function of the helical spacers on the fuel rods is to maintain the proper rod clearances. The primary function of the spacers therefore is essentially mechanical. The presence of the spacers with their 7.5" pitch should effectively prevent the unstable bowing effects encountered in the ORNL EGCR fuel elements (18). Furthermore, as previously mentioned, for the clearance-to-diameter ratio of the MGCR fuel element and for somewhat lower clearance-to-diameter ratios there is no large decrease in local heat transfer coefficients in the narrow portions of the gaps between fuel rods. Also, coolant flow distribution within a fuel element appears to be fairly uniform. Therefore, there should be no great tendency to thermal bowing instability in the MGCR fuel element.

2.4 Fuel Rod Internal Design

The fuel rod is to be fabricated using a creep shrink process to apply the cladding. The creep shrink process prestresses the cladding. Under operating conditions the effect of the pre-stress is to produce a low stress in the cladding and to produce a radial pressure between the cladding and the fuel body. This radial pressure has a beneficial thermal effect since it ensures excellent thermal contact between the cladding and the fuel body, producing lower and more predictable fuel body temperatures.

To predict the fuel element internal temperatures, the thermal conductivity of the fuel body and the effect of temperature and irradiation on this quantity must be known. For UO_2 fuel bodies a design value of $1.0 \text{ Btu/hr ft}^2 \text{ }^\circ\text{F/ft}$ for irradiated material at temperatures in the range of 2000 to 3000 $^\circ\text{F}$ is used (19, 20, 21). No experimental values are now available for $\text{UO}_2\text{-BeO}$ fuel bodies. An estimate of the thermal conductivity of such mixtures was made. The estimate assumed isotropic dispersion of uniform UO_2 pellets in the BeO matrix. If it is assumed that the UO_2 pellets are on a cubical lattice, the estimated thermal conductivity in non-dimensional form is given by the equation:

$$\frac{k_E}{k_B} = 1 - v_u \left\{ \frac{\frac{k_B}{k_u} - 1}{1 + v_u^{\frac{1}{3}} \left(\frac{k_B}{k_u} - 1 \right)} \right\} \quad 2.$$

where:

k_E is the effective conductivity of the mixture.

k_B is the conductivity of the BeO.

k_u is the conductivity of UO_2 .

v_u is the volume fraction of UO_2 .

Figure 21 gives a plot of k_E/k_B as a function of k_B/k_u with volume fraction of UO_2 as a parameter. Using a design value of 1.0 Btu/hr ft² °F/ft for the thermal conductivity of UO_2 and a design value of 10 Btu/hr ft² °F/ft for irradiated BeO in the temperature range of 1000 to 1500°F (22), the effective conductivity for a UO_2 -70 v/o BeO mixture is about 6.2 Btu/hr ft² °F/ft. However, the UO_2 pellets will not necessarily be oriented on a cubical lattice. To allow for other lattice configurations the previous equation can be amended to:

$$\frac{k_E}{k_B} = 1 - v_u \left\{ \frac{\frac{k_B}{k_u} - 1}{1 + a_u v_u^{\frac{1}{3}} \left(\frac{k_B}{k_u} - 1 \right)} \right\} \quad 3.$$

where:

a_u is the ratio of the projected area occupied by UO_2 normal to the heat flow path in a cubical lattice to that occupied by UO_2 in the actual lattice configuration.

The orientation of the UO_2 theoretically can vary from full-area plates normal to the heat flow path to full-length plates parallel to the heat flow path. The values of a_u for these

limiting conditions are 0.448 and 1.495 respectively for a UO_2 -70 v/o BeO mixture which means that k_E theoretically can vary from 2.7 to 7.3 Btu/hr ft² °F. Obviously, the UO_2 particle orientation will not be at the extremes of the range. Work by the MGCR Materials Section indicates that, with Al_2O_3 as a diluent, the UO_2 is present as a discontinuous phase when the volume percentage of diluent is about 70% or more. Since the assumed cubical lattice orientation of the UO_2 particles will not necessarily occur, a design figure, lower than the calculated 6.20, of 5.0 Btu/hr ft² °F/ft for the thermal conductivity of UO_2 -70 v/o BeO was chosen. This figure lies in the middle of the theoretically possible range.

The combination of the creep-shrink process for the cladding and the high fuel body thermal conductivity produces quite low fuel body temperatures compared to those resulting if UO_2 alone is used. For instance, for the reference design, the calculated maximum fuel body temperature is about 1900°F, for UO_2 -70 v/o BeO compared to about 2710°F for UO_2 . Thus, fission product retention will be improved not only due to the structure of the UO_2 -BeO mixture, but also due to the considerably lower fuel body temperatures.

An analysis has been made of the pressure developed in the hot-test fuel rod due to the buildup of gaseous and volatile fission products. The fuel rods are provided with hollow extensions through the reflector at the cold end which serve as voids for the accumulation of gaseous and volatile fission products. Voids are also present in the fuel region of the rods since the fuel material density is less than the theoretical density and since there will probably be small gaps between the pellets. It is assumed that these voids can be assumed to be connected since evidence shows that the governing mechanism of fission product escape from ceramic fuel bodies is diffusion within the fuel particles themselves and that when an atom of a fission product species reaches the surface of a fuel particle, it is

free to move very rapidly along the grain boundaries to the surfaces of the pellets (23, 24). Any cracking which would occur, for instance, that due to thermal stresses within UO_2 (25) could help to assure connection of the void regions, even with creep-shrunk cladding. With the voids assumed to be connected, the internal pressure throughout the fuel rod and its extension is uniform.

Therefore, it is assumed that the fuel rod can be divided into two regions, the fuel region at a uniform temperature equal to the mean temperature of the fuel body, and the extension region at a uniform temperature approximately equal to the core inlet temperature. It is also assumed that the fuel rod will be evacuated during manufacture prior to welding of the end cap to the rod to eliminate any pressure contribution due to the heating of initially-contained gases.

In determining those fission products which contribute to pressure buildup in the rods, only stable and very long lived species with half lives of the order of years were considered. The method of calculation used was to compute the partial pressure of each species using the total chain fission yields taken from Table 1-4 of Reference 26. The partial pressure of the species considered are calculated by:

$$p_i = \frac{3.1 \times 10^{10} P \alpha_R y_i R_0 T_H t}{N_A V_H \left\{ 1 + \frac{V_C}{V_H} \frac{T_H}{T_C} \right\} n_R} \text{ } \approx \text{ } \text{atm} \quad 4.$$

where:

p_i is the partial pressure of the i^{th} fission product species, atmospheres.

P is the total core power, 74.4×10^6 watts.

α_R is the ratio of the average power density in the hottest rod to the average power density in the core, 1.73.

- y_i is the total fractional chain yield of the i^{th} fission product species.
- R_0 is the universal gas constant, 82.0 cc.-atm/gm-mole $^{\circ}\text{K}$.
- T is temperature, $^{\circ}\text{K}$.
- τ is time of irradiation, 7.14×10^7 seconds (2.27 years).
- r is the fraction release of a species.
- N_A is Avogadro's number, 6.023×10^{23} atoms/gm-mole.
- V is volume, cc.
- N_R is the number of fuel rods in the core, 5544.

Subscripts:

- H refers to the hot fuel region.
- C refers to the cool reservoir region.
- $T_H = 1037^{\circ}\text{K}$ (1405°F)
- $T_C = 670^{\circ}\text{K}$ (745°F)
- $V_H = 6.16$ cc
- $V_C = 9.51$ cc

For all the species considered, the vapor pressures (27, Figure A-5) for the temperatures of the fuel region and of the reservoir region were compared to the calculated partial pressure of the species for various assumed gaseous fission product releases. A species for which the calculated partial pressure is greater than the vapor pressure at the fuel region temperature will condense in the fuel region and its actual partial pressure will equal its vapor pressure. A species for which the calculated partial pressure is less than the vapor pressure at the fuel region temperature, but greater than the vapor pressure at the reflector temperature, will tend to be volatile in the fuel region, but to condense in the reservoir region. Therefore, such a species will tend to migrate to the cooler region because of the existing concentration gradient, and will condense there. A species for which the calculated partial pressure is less than the vapor pressure at the reservoir temperature will be volatile everywhere.

Those species which condense in either region will contribute their vapor pressures only to the buildup of pressure. The contributions of such species to the total pressure will be negligible. Those species which are volatile everywhere contribute their calculated partial pressures to the total pressure. Equation 4, of course, assumes that all volatile components act as perfect gases. This assumption is accurate for the rare gases Xenon and Krypton. Only small errors will be introduced by this assumption for the vaporized components.

Using the foregoing assumptions the only fission products which contribute to pressure under the MGCR prototype conditions are Xenon, Krypton, Bromine, Selenium and Iodine.

Substituting values for the MGCR prototype end-of-life conditions in equation 4 the pressure within the hottest rod can be calculated by:

$$p = 94 \text{ r atmospheres}$$

This pressure corresponds to a void volume of 12%, of which 5% is provided by the voids in the fuel region and the remainder by the fuel rod extension in the cold end reflector.

The calculated pressure for a given per cent release of fission products is shown in Figure 13. For a total release of about 3%, the estimated internal pressure in the hottest rod is only about 40 psi. If a conservative design figure of 10% release is used the internal is only 138 psi. If the fuel rods are not evacuated before the welding on of the end caps a pressure increment of 39 psi results, which gives a conservative maximum total internal pressure of 177 psi.

The major uncertainties in the analysis are:

- a. The assumption that all void volumes are connected.
- b. The assumption of uniform mean temperatures for the two

regions. This assumption probably gives conservative results.

- c. The amount of migration and condensation of the volatile fission products.
- d. The contribution of shorter-lived isotopes to the pressure. This contribution should not be large, because total chain yields are considered which in effect account for short-lived precursors.
- e. The amount of fission products released. A release of 3% is thought to be realistic for UO_2 fuel bodies, with a lower release to be expected from the UO_2 -BeO fuel bodies. Thus, the pressure predicted for a release of 10% is a conservative value.

The actual effect of these uncertainties is not known at this moment. However, it is felt that the present analysis gives an upper limit to the maximum gaseous fission product pressure to be expected in the hottest MGCR fuel element.

Bursting tests have been made at General Atomic on seamless Inconel and weld-drawn Hastelloy X tubing 3/8" O.D. by 0.010" wall thickness. Test temperatures ranged from 1800 to 2100°F and internal pressure loading rates ranged from 0.1 to 50 psi per second. The tests were made in air. The tests showed that the bursting pressure of as-received Inconel tubing for a pressurization rate of 10 psi per second, decreased from 900 psi at 1800°F to 400 psi at 2100°F. Hastelloy X tubing gave bursting pressures somewhat higher (up to 30%) than those for Inconel. Annealing also improved the bursting strength.

From these results it is apparent that the fuel element cladding will be able to withstand internal pressure due to gaseous and volatile fission products under all normal operating conditions and even for lengthy periods following a loss-of-coolant accident.

However, there is a possibility of considerably higher pressure being developed within the fuel element cladding. Pressures up

to 1120 psia could develop by slow in-leakage of helium coolant through minute imperfections in the cladding. Following operational pressure decreases or a loss-of-coolant accident a significant pressure difference would exist across the cladding. Detailed studies of these cases have begun.

2.5 Fuel Element Transient Temperatures

Studies have been made of the fuel rod cladding temperatures to be expected as a function of time following a loss-of-coolant accident (28). These studies assume that the reactor is shut down immediately on the loss-of-coolant accident and that the fuel elements are cooled only by radiation to the BeO moderator. Radiant exchange view factors were determined by the crossed-strings technique (16, p. 66). The heat source was determined from the Wigner-Way equation and allowance was made for the effect of delayed neutrons for an assumed negative reactivity addition at shutdown. It was assumed that the reactor had been operating for an infinite time prior to shutdown. Allowance was made for the fraction of heat generated in the moderator. Analyses were made for no thermal resistance at the surface of the moderator blocks and for a thermal resistance equivalent to that of a stagnant helium gap 0.012" thick. The latter case allows for the presence of the fuel element shroud.

To obtain conservative results, the heat capacity of the fuel elements was neglected, i.e. the heat transfer system was in quasi-equilibrium with the heat radiation rate from the element equal to the heat generation rate in the elements. This assumption does not involve serious errors for times longer than about 30 minutes after shutdown. To allow conservatively for shorter periods, adiabatic conditions were assumed with all the heat generated in the fuel elements being stored therein. Emissivities of all surfaces conservatively were assumed to be 0.5. The studies show that, because of the high heat capacity of the BeO moderator, the hottest fuel rod cladding temperature should not exceed a value of 2100°F for periods of 30 minutes to one hour

after a loss-of-coolant accident, without any emergency cooling being applied. The large pressure differentials which can develop across the fuel element cladding following a loss-of-coolant accident (Section 2.4) emphasize the importance of these studies. An accurate evaluation of the temperature history of the cladding is necessary to determine whether the cladding will be able to maintain its integrity for the required length of time following a loss-of-coolant accident. Therefore, more detailed analyses which will reduce the present amount of conservatism have been started.

A preliminary analysis has been made to evaluate the consequences of a situation in which the normal cooling is lost but in which the coolant system maintains its integrity permitting emergency cooling to be applied. The analysis was based on the assumptions:

- a. Radiation to the moderator is ignored.
- b. At any axial position, the fuel body is at a uniform average temperature.
- c. The thermal resistance of the fuel body is lumped at the fuel body cladding interface.
- d. At any axial position, the cladding is at a uniform average temperature.
- e. The heat capacity of the fuel body is considered, but that of the cladding is not.
- f. The heat capacity of the coolant is ignored.
- g. At any axial position, the heat generation rate in a fuel rod is uniform.
- h. Axial heat conduction is ignored in fuel body and cladding.
- i. The emergency coolant flow is constant, as is the coolant heat transfer coefficient.
- j. The heat generation rate is assumed constant at a mean value for a given time increment. The analysis is repeated for a series of time increments. The heat generation rate is determined from the Wigner-Way equation and from the effect of delayed neutrons following a negative

reactivity addition of 5% at shutdown.

- k. The fraction of heat generated directly in the fuel was taken as 60%

The resulting equations requiring solution are:

$$\frac{dT_F}{dt} + \frac{4\bar{U}_F}{\rho_F C_F d_i} (T_F - T_S) = \frac{g_F(x,t)}{\rho_F C_F} \quad 5.$$

$$T_S = \frac{1}{1 + \frac{h_H d}{\bar{U}_F d_i}} T_F + \frac{1}{1 + \frac{\bar{U}_F d_i}{h_H d}} T_g \quad 6.$$

$$\frac{dT_g}{dx} - \frac{h_H \pi d}{w' C_g} (T_S - T_g) = 0 \quad 7.$$

where:

T_F is the mean temperature of the fuel body at a point x .

T_S is the mean temperature of the cladding at a point x .

T_g is the mean temperature of the coolant at a point x .

$g_F(x,t)$ is the heat generation rate in the fuel.

ρ is the density.

C is the specific heat.

h_H is the heat transfer coefficient for the flow around the hottest rod.

$\bar{U}_F$ is the thermal conductance of the fuel body from the mean temperature to the surface temperature.

d is the outside diameter of the cladding.

d_i is the diameter of the fuel body.

w' is the flow associated with the hottest fuel rod.

x is the distance to a point from the entrance to the core.

The boundary and initial conditions are:

at $t = 0$, $T_F = T_{F0}$, the mean steady state fuel temperature at x .

at $x = 0$, $T_g = T_{g1}$, constant (set equal to zero).

Applying the Laplace transform technique the above equations may be combined to give:

$$\frac{dT_g}{dx} + c\left(1 - \frac{b}{p+b}\right)\bar{T}_g = \frac{c\bar{q}_v(x)}{pC_F A(p+b)} + \frac{cT_{F0}(x)}{p+b} \quad 8.$$

where:

$\bar{q}_v(x)$ is the mean heat generation rate over the period concerned.

$\bar{T}_g$ is the Laplace Transform of T_g .

p is the transform variable.

$$c = \frac{h_H \pi d}{w' G_g} \left(\frac{1}{1 + \frac{h_H d}{U_F d_i}} \right)$$

$$b = \frac{4 U_F}{p C_F d_i} \left(\frac{1}{1 + \frac{U_F d_i}{h_H d}} \right)$$

Now, the heat generation and the initial temperature of the fuel body are both functions of distance from the entrance of the core. The equation has been solved for a constant q_v and T_{F0} and has also been solved for a chopped-sine power distribution (assumed not to change with time following shutdown) and for the resulting fuel body initial temperature distribution. The final solution for the cladding temperature for the constant $\bar{q}_v$ and T_{F0} case is given by:

$$T_s = \frac{\bar{q}_v}{p C_F} \tau + T_{F0} - \frac{1}{1 + \frac{U_F d_i}{h_H d}} \left(\frac{\bar{q}_v}{p C_F} + \frac{4 U_F}{p C_F d_i} T_{F0} \right) e^{-cx} \int_0^x e^{-bt} I_0(2\sqrt{axt}) dt$$

$$- b \frac{\bar{q}_v}{p C_F} e^{-cx} \int_0^x \int_0^t e^{-bt} I_0(2\sqrt{axt}) dt' dt$$

$$- \frac{1}{1 + \frac{U_F d_i}{h_H d}} T_{F0} e^{-cx} e^{-bt} I_0(2\sqrt{axt}) \quad 9.$$

where:

$$a = bc$$

I_0 is the modified Bessel function of the first kind of zero order.

Values of T_s were computed at the core exit for a number of increments of time up to 30 minutes following shutdown. The average power generation rate in the hottest fuel rod was used. The results are shown in Figure 14, where the calculated hottest cladding temperature is plotted against time. While this calculation is for a uniform average power generation, the results should not be greatly different for a chopped sine power distribution. Computations for a chopped sine power distribution have not been completed as yet. The calculation predicts a maximum temperature of about 2260°F at about 13 minutes after shutdown. It is quite evident that such a temperature would not be reached in actuality since radiation to the moderator has been ignored. The predicted temperature for radiation cooling only is also shown. It is obvious that the combined effect of convection and radiation will be to produce temperatures below the lower of the two curves at any time after shutdown. Therefore, the maximum cladding temperature under emergency cooling conditions should not exceed 1900°F. Of course, these calculations are approximate and quite preliminary. It is planned to develop or adapt a computer code to handle transient cooling problems allowing for both radiation and convective cooling.

3.0 MODERATOR

In the MGCR reference design, the moderator is cooled mainly by a flow of helium through an annulus between the fuel element shroud and the surface of the fuel channel hole in the moderator block. The moderator cooling flow is in parallel with the fuel element cooling flow.

For the MGCR reference design about 8.1% of the fission energy release is released directly in the moderator under operating conditions

The moderator and fuel element cooling flows are divided in the same ratio as the moderator and fuel element heat generation rates. This flow arrangement, of course, produces equal temperature rises in each stream and therefore does not affect the thermodynamic performance of the cycle.

3.1 Conductances of Various Shroud Arrangements

Initial studies of moderator temperature were made assuming cooling by means of the fuel channel flow only. These studies were also used to investigate acceptable fuel element shroud arrangements. The shroud arrangements studied included:

- a. A simple 0.010" thick metallic shroud with a minimum gap between the shroud and the moderator block (0.010").
- b. A thin metallic shroud with a BeO or Al_2O_3 liner with an effective thickness of about 0.135" with the same 0.010" gap between the shroud and the moderator block.
- c. A corrugated metallic shroud to produce better structural characteristics with an effective overall thickness of about 0.135", with a total metal thickness of 0.010", the remainder being a stagnant helium gap.

The overall thermal conductances of the shrouds were determined for a typical MGCR fuel element geometry and thermal performance. The overall conductances were calculated allowing for coolant boundary layer conductances, metal and ceramic conductivities, and gap conductances. Gap conductances were determined to be mainly due to stagnant gas conductivity, the effect of natural convection being negligible and the effect of radiation being small for the gap widths and temperatures considered. In calculating the overall thermal conductances safety factors were applied to the boundary layer conductance. These safety factors allowed for dimensional inaccuracies in the flow passage, uncertainties in the heat transfer correlation, flow uncertainties and for non-mixing of the coolant. The overall safety factor applied to the boundary layer thermal conductance was taken as

1.40. The conductivity values used in evaluating the other components of the overall thermal conductance were chosen with similar conservatism. The calculated resistance for the arrangements studied are given in Table IV:

TABLE IV
FUEL ELEMENT SHROUD ARRANGEMENTS

Arrangement	U, Overall Conductance Btu/hr ft ² OF
A. 0.010" metal, 0.010" stagnant helium gap:	190
B. 0.135" effective BeO thickness, 0.010" stagnant helium gap:	159
C. 0.135" effective Al ₂ O ₃ thick- ness, 0.010" stagnant helium gap:	112
D. 0.135" effective corrugated arrangement, 0.125" stagnant helium, 0.010" metal, plus 0.010" stagnant helium gap:	36

3.2 Moderator Temperatures

In the analysis, since the moderator heating is mainly due to primary gammas, it was assumed, as a first approximation, that the gross moderator heating distribution over the core was similar to the thermal flux distribution. Making this assumption, and approximating the thermal flux distribution by a chopped sine axial distribution, and allowing for hot channel factors, the moderator surface temperature in the hottest channel is given by:

$$T_M - T_{gi} = \frac{F_{sr} \alpha_r R_M Q_r \sin \phi}{U_{sr} N A_{sr}} + \frac{F_c \alpha_r \Delta T}{\pi \alpha_r L} \left\{ \frac{L''}{L} \sin \Delta - \cos \phi \right\} \quad 10.$$

where:

Q_T is the total reactor power, Btu/hr

R_M is the fraction of reactor power released in the moderator.

α_T is the ratio of maximum-to-average power generation in the core.

α_R is the radial maximum-to-average power generation.

F_C is a hot channel factor to allow for uncertainties in the fluid flow.

F_{SK} is a hot channel factor to allow for uncertainties in the temperature drop across the shroud.

N is the number of fuel elements.

A_{SK} is the heat transfer surface from the shroud to the coolant per element.

ΔT is the coolant temperature rise across the core, $^{\circ}\text{F}$.

T_M is the moderator temperature, $^{\circ}\text{F}$.

L is the core length, ft.

$L'' = L + \delta_R$, ft.

δ_R is the reflector savings, ft.

$$\Delta = \frac{\pi \delta_R}{L''}$$

$$\phi = \frac{\pi(\delta_R + y)}{L''}$$

y is distance from core entrance, ft.

U_{SK} is the overall shroud conductance from the moderator to the coolant, Btu/hr sq ft $^{\circ}\text{F}$.

To find the average moderator temperature at any point, y , equation 1 can be modified by replacing U_{SH} in the first term by $\bar{U}_M$ where:

$$\frac{1}{\bar{U}_M} = \frac{1}{U_{SK}} + \frac{F_C}{F_{SK}} \bar{R}_M \quad 11.$$

where:

$\bar{R}_M$ is the thermal resistance from the location of the average moderator temperature to surface of the moderator hole.

F_Q is a safety factor to allow for uncertainties in the estimate of the moderator block thermal resistance.

By approximating a moderator block cross section by an annular configuration equal in area to the actual moderator block cross section, and by assuming a constant internal heat source across a moderator block, the average temperature at a cross section and the location of the point of average temperature can be determined. The ratio of thermal resistance from the point of block average temperature to the inner surface of the block to the overall thermal resistance of the block is calculated to be:

$$\frac{\bar{R}_M}{R_{MT}} = \frac{\frac{r_o^2 \ln r_o}{r_o^2 - r_i^2} - \frac{3r_o^2 - r_i^2}{4r_o^2}}{\ln r_o - \frac{r_o^2 - r_i^2}{2r_o^2}} \quad 12.$$

where:

R_{MT} is the overall thermal resistance of the moderator block.

r_i is the inner radius of the block (actual, or fictitious if hole is hexagonal).

r_o is the fictitious equivalent outer radius of the block.

The value of the overall thermal resistance of the moderator block is:

$$R_{MT} = \frac{r_i}{2k_M} \left\{ \frac{2r_o^2 \ln r_o}{r_o^2 - r_i^2} - 1 \right\} \quad 13.$$

where:

k_M is the thermal conductivity of the moderator, Btu/hr-sq ft $^{\circ}$ F/ft.

The axial average of the cross sectional average temperature in a moderator block can be calculated from equations 10 and 11

and is given by:

$$\bar{\bar{T}}_M - T_{gi} = 2 F_{sh} \frac{\alpha_T R_M Q_T \cos \Delta}{U_M N A_{sh} \pi 2 \Delta} + F_c \frac{\alpha_T \Delta T}{\pi \alpha_R L} \cos \Delta \quad 14.$$

where:

$\bar{\bar{T}}_M$ is the average moderator temperature in the hottest channel.

The average moderator temperature over the whole core is given by:

$$\bar{\bar{T}}_M - T_{gi} = \frac{\bar{\bar{T}}_M - T_{gi}}{\alpha_R} \quad 15.$$

where:

$\bar{\bar{T}}_M$ is the average moderator temperature over the whole core.

The maximum moderator temperature in the core can be found by modifying equation 10 by replacing U_{sh} by U_{MT} where:

$$\frac{1}{U_{MT}} = \frac{1}{U_{sh}} + \frac{F_c R_{MT}}{F_{sh}}$$

and by solving for T_M at the hottest point whose location can be found by:

$$\phi_{max} = \arctan\left(-\frac{\pi \alpha_R Q_T R_M}{U_{MT} N A_{sh} \Delta T} \frac{F_{sh}}{F_c} \frac{L}{L''}\right) \quad 16.$$

Values used in the present calculations are:

$$Q_T = 254 \times 10^6 \text{ Btu/hr (74.4 Mw)}$$

$$R_M = 0.081 (57).$$

$$\alpha_T = 1.893$$

$$\alpha_R = 1.507$$

$$\begin{aligned}
 F_c &= 1.15 \\
 F_q &= 1.20 \\
 F_{sa} &= 1.20 \\
 N &= 308 \\
 A_{sa} &= 3.505 \text{ sq ft} \\
 \Delta T &= 555^\circ\text{F} \\
 T_{fi} &= 745^\circ\text{F} \\
 L &= 6.37 \text{ ft} \\
 \frac{L''}{L} &= 1.375 \\
 r_2 &= 1.140 \text{ inches} \\
 r_c &= 2.165 \text{ inches} \\
 h_m &= 10 \text{ Btu/hr ft}^2 \text{ }^\circ\text{F/ft}
 \end{aligned}$$

Steady state full power moderator temperatures as a function of U_{Sh} , the overall shroud conductance are shown in Figure 15. Note that these temperatures are for a hexagonal fuel channel. However, with the assumptions of the analysis, there will be no significant difference in the temperatures for a circular or hexagonal fuel channel, if all other factors do not change.

Using the values of U_{Sh} given in Table IV, Figure 15 indicates that the average moderator temperature in the hottest channel and the average moderator temperature at the core center line in the hottest channel are about 2010°F and 2200°F , respectively for the corrugated metallic shroud arrangement. The other shroud arrangements studied result in corresponding temperatures of about 1470°F and 1550°F and lower.

These results show that shroud conductances of the magnitude of that for the corrugated metallic arrangement would not be acceptable for schemes using only fuel channel flow for cooling the moderator. Because of the high average moderator temperature in the hot channel, the loss-of-coolant problem is magnified since the initial moderator temperature is very high and there is a large thermal resistance between the fuel element and the moderator. Since the rods must radiate the shutdown heat to the

moderator it is apparent that they would soon reach very high temperatures. Also, for low shroud conductances the average moderator temperature over the whole core is high. For instance for the corrugated metallic arrangement, the core average moderator temperature is about 1650°F . For the other shroud arrangements, the core average moderator temperature is about 1225°F or lower. Thus, for the low conductance shroud the average neutron temperature would be considerably higher than that for the other shrouds, which would have an adverse effect on core reactivity.

From the foregoing considerations it was tentatively decided that an overall shroud conductance of less than $125 \text{ Btu/hr ft}^2 ^{\circ}\text{F}$ would not be acceptable for the MGCR reference design. This conductance is approximately equivalent to that of a stagnant helium gap about $0.016''$ thick, in the temperature range of interest.

The moderator temperatures for the selected shroud conductance are given in Table V:

TABLE V
MODERATOR TEMPERATURES
FUEL CHANNEL COOLING, $U_{\text{Sh}} = 125 \text{ Btu/hr ft}^2 ^{\circ}\text{F}$

<u>Temperature and Location</u>	<u>Value, $^{\circ}\text{F}$</u>
Maximum, in hottest channel:	1670
Average at core center line, hottest channel:	1510
Surface, at core center line hottest channel:	1400
Average, hottest channel:	1440
Average, over core:	1200

As discussed in Section 2.0, mechanical considerations require a minimum gap between the fuel element shroud and the moderator block hole of about $0.050''$. A stagnant helium gap of this size would produce an effective thermal conductance of only about 30

Btu/hr ft² °F between the fuel element and the moderator. One way of solving this problem would be to provide flexible metallic fins on the shrouds which would improve the gap conductance and would not interfere with insertion and removal of the fuel element. It was estimated that the effective conductance of a stagnant gap 0.050" wide could be increased to that of a gap 0.016" wide by providing angled fins filling about 15% of the perimeter of the gap, which would bear against the moderator hole surface. Allowance was made for imperfect contact between the surfaces. No allowance was made for the increase in effective conductance due to radiation across the gap following a loss-of-coolant, which increases the overall value of the conductance about 10 to 15%. These fins would increase the volume of metal in the core by about 1.3 cu ft, or 33%.

3.3 Bypass Cooling of Moderator

It became apparent from the moderator cooling studies that an arrangement employing the fuel channel flow only would not be satisfactory because of the relatively high moderator temperatures, even with a reasonably high shroud conductance. A bypass cooling arrangement appeared to offer a means of ensuring significantly lower moderator temperatures and was selected for further study. Such a cooling arrangement removes the requirement of a high effective shroud and gap thermal conductance under operating conditions, since the moderator is cooled directly by coolant flow over its surface. However, in spite of the lower initial moderator "sink" temperature for the radiation of heat from the rods in the loss-of-coolant accident, good effective shroud and gap thermal conductance is still a requirement following a loss-of-coolant accident to prevent the fuel rod cladding temperatures from rising too rapidly. Pending more analysis on the effect of shroud conductance on the loss-of-coolant accident the requirement of shroud conductance equal to 125 Btu/hr ft² °F or more was retained. Since it appeared that a conductance of this magnitude could be maintained reasonably with a gap

0.050" wide and since such a width appears to satisfy the mechanical clearance requirements, it was used for the initial studies of bypass cooling. It was again assumed that the gap conductance was brought to the required value by the use of flexible fins.

With the flows divided in proportion to the heat generation rates in the fuel and the moderator, the calculated fuel element cladding hot spot temperature is actually somewhat lower than the design value of 1500°F , since all previous core performance calculations have conservatively assumed that all the heat was generated in the fuel element. The core pressure drop is reduced about 10% and the pressure drop in the bypass channel is about 45% of the main channel pressure drop in the hottest channel. Therefore, an "orifice" will be needed in the bypass channel to properly proportion the flows. The presence of the "orifice" makes the bypass flow less dependent on bypass channel tolerances for proper flow proportioning. It might be advantageous to enhance this effect by increasing the annular gap width above 0.050 inches. However, some freedom in the control of the flow proportions is possible, since the excellent thermal conductance between the streams will tend to stabilize the temperatures of the streams. Since the importance of the coolant temperature rise outweighs the importance of the film temperature drop in determining the surface temperature (and hence all temperatures) in the hotter regions of the core, all fuel element and moderator temperatures will tend to be stabilized with the proposed moderator cooling scheme. The various moderator temperatures calculated with bypass cooling are given in Table VI, where they are compared with previous results from Table V for cooling by fuel channel flow only for otherwise similar conditions:

TABLE VI
MODERATOR TEMPERATURES

<u>Temperature and Location</u>	<u>Values ($^{\circ}$F) for</u>	
	<u>Fuel Channel Cooling</u>	<u>Bypass Cooling</u>
Maximum in hottest channel:	1670	1480
Average at core center line, hottest channel:	1510	1250
Surface at core center line, hottest channel:	1400	1140
Average, hottest channel:	1440	1210
Average, over core:	1200	1060

It can be seen that the maximum moderator temperature is reduced about 190° F by bypass cooling. The average temperature at the core center line is reduced about 260° to about 1250° F, which is beneficial in a loss-of-coolant accident in that the initial sink temperature to which the fuel rods radiate the maximum heat is that much lower. The core average moderator temperature is reduced about 150° which improves the neutron economy somewhat. Considerable more work is required to investigate various annular gap widths and other aspects of this scheme. However, it does appear that a bypass cooling arrangement does produce reasonable moderator temperature levels.

Recent calculations have indicated that thermal stresses produced in the moderator blocks by cooling only at the surfaces of the fuel channel holes will probably be too high. Excessive thermal stresses are generated irrespective of the core module shape since the circumferential tensile thermal stresses at the hole are as large as the axial stresses due to the gross non-uniform temperature distribution across the module (for nine element square and seven element hexagonal modules). Due to conservative assumptions in the analysis and uncertainties in many of the

property values, the exact magnitude of the thermal stresses is only approximately known. A great deal more work is necessary on this problem.

3.4 Temperature Stability of Bypass and Fuel Coolant Streams

An analysis has been made to examine the temperature stability of the parallel flow streams with the bypass flow arrangement. The analysis considered heat flow into the fuel element cooling stream from the fuel element and into the moderator cooling stream from the moderator for the hottest channel. In addition it was assumed that the two streams at any axial point were not at the same temperatures due to departure of the flow proportioning from the ideal and that heat flow could occur from one stream to the other. It was assumed that the axial heat generation in both the fuel and the moderator followed a chopped sine distribution. With these assumptions, the problem requires the solution of the two simultaneous differential equations:

$$\frac{dT_{FM}}{dy} + \frac{U_{FM} P_S}{W_{FM} C_p} (T_{FM} - T_{GF}) = \frac{q_{oFM} A_M}{W_{FM} C_p} \sin \phi \quad 17.$$

$$\frac{dT_{GF}}{dy} + \frac{U_{FM} P_S}{W_{GF} C_p} (T_{FM} - T_{GF}) = \frac{q_{oGF} A_F}{W_{GF} C_p} \sin \phi \quad 18.$$

where:

T_G is the coolant temperature.

U_{FM} is the overall thermal conductance from the fuel channel stream to the moderator channel stream.

P_S is the perimeter of the shroud.

q_o is the maximum volumetric heat generation rate in a channel.

A is the cross sectional area of either the fuel or the moderator region associated with one fuel channel.

W is the flow in a channel.

C_p is the specific heat at constant pressure of the coolant.

Other symbols as on Page 40.

Subscripts:

M moderator channel.

F fuel channel.

Using the Laplace transform method, the equations may be solved for T_{gM} and T_{gF} . The solution for T_{gF} is:

$$T_{gF} = C_F \frac{\frac{W_F}{W_M} (1 + R_{MF}) \cos \Delta + \frac{C_F}{B_F^2 \left(\frac{W_F}{W_M} + 1 \right)^2 + 1} \left[\frac{(1 - R_{MF} \frac{W_F}{W_M}) \{ \cos \Delta - B_F \left(\frac{W_F}{W_M} + 1 \right) \sin \Delta \} e^{-B_F \left(\frac{W_F}{W_M} + 1 \right) \frac{T_{gF}}{L}}}{\frac{W_F}{W_M} + 1} - \left\{ B_F^2 \frac{W_F}{W_M} (1 + R_{MF}) \left(\frac{W_F}{W_M} + 1 \right) + 1 \right\} \cos \phi + B_F (1 - R_{MF} \frac{W_F}{W_M}) \sin \phi \right]}{19.}$$

The solution for T_{gM} is similar.

The non-dimensional temperature difference between the two streams at any point in the hottest element is found to be given by:

$$\frac{T_{gF} - T_{gM}}{Q_{TF}/W_F C_p} = \frac{C_F (1 - R_{MF} \frac{W_F}{W_M})}{B_F^2 \left(\frac{W_F}{W_M} + 1 \right)^2 + 1} \left\{ \left[\cos \Delta - B_F \left(\frac{W_F}{W_M} + 1 \right) \sin \Delta \right] e^{-B_F \left(\frac{W_F}{W_M} + 1 \right) \frac{T_{gF}}{L}} - \cos \phi + B_F \left(\frac{W_F}{W_M} + 1 \right) \sin \phi \right\} \quad 20.$$

where:

Q_{TF} is the total heat generated in the fuel in the hottest channel.

W_F is the total coolant flow to the fuel element channels.

C_p is the coolant specific heat at constant pressure.

$$R_{MF} = \frac{q_{gM}}{q_{gF}}$$

$$B_F = \frac{U_{FM} A_{MS}}{\pi \alpha_R W_F C_p} \frac{L''}{L}$$

$$C_F = \frac{F_c \alpha_T}{\pi \alpha_R} \frac{L''}{L}$$

where:

A_{ws} is the total surface area of the shroud, $N P_S L$
All other terms are as previously defined.

Obviously, if $R_{MF} = \frac{U_{FM}}{U_F}$ the flow is properly proportioned and

$$\frac{T_{GF} - T_{GM}}{Q_{TF}/U_{TF} C_p} = 0$$

at all values of y as it should.

The value of $\frac{T_{GF} - T_{GM}}{Q_{TF}/U_{TF} C_p}$ at the core exit has been calculated for

the MGCR reference design conditions for various perturbations to the proper flow distribution. The results are shown in Figure 16, where $T_{GF} - T_{GM}$ at the core exit is plotted against percentage

change in the fuel channel flow rate from the ideal value. It is obvious that the high heat transfer coefficients on each side of the shroud, producing a high value for U_{FM} (of the order of 350 Btu/hr ft² °F at design conditions) impart excellent temperature stability to both streams. Since the importance of the coolant temperature rises outweighs the importance of the film temperature drops in determining the surface temperatures (and hence all the temperatures) in the core all fuel elements and moderator temperatures will tend to be stabilized with the bypass cooling scheme. The fuel cladding and moderator surface temperatures can be calculated from the coolant stream temperatures by the equations:

$$T_{FS} - T_{GF} = \frac{F_{FF} \alpha_T Q_{TF}}{h_{MF} N A_{LF}} \sin \phi \quad 21.$$

$$T_{HS} - T_{FM} = F_{FM} \frac{\alpha_T Q_{TM}}{h_{HM} N A_{HM}} \sin \phi \quad 22.$$

where:

F_{FM} is the film drop hot channel factor

Q_T is the total heat generation in a region.

h_{HM} is the heat transfer coefficient in the hottest channel.

N is the total number of fuel channels.

The maximum cladding temperature has been calculated for the flow perturbation of - 10% from the normal fuel channel flow. The calculated hot spot temperature is 1506°F, which confirms the assertion that all temperatures within the core tend to be stabilized under all foreseeable flow deviations.

It is obvious from the foregoing discussion that the flow stability of the two streams will be excellent also.

3.5 Tension Tube Cooling

In the MGCR reference design the moderator is arranged in 11" square cross section modules each containing nine fuel channel holes on a square pitch. The modules are composed of 3" thick plates stacked to a total height of 90". The plates are tied together by four pre-stressed tension tubes which provide sufficient integrity to the module to resist the rolling and pitching inertia loads encountered in the shipboard reactor. A drawing of the module is shown in Figure 20 of the report. The tension tubes are pre-stressed in the cold state and the tubes must maintain the required axial load on the moderator blocks under operating conditions. Therefore, their temperatures must be maintained at a sufficiently low level to ensure that the pre-stress load is not relaxed to an unacceptable extent due to differences in expansion coefficients of the tension tube

material and the BeO or due to creep relaxation. The tension tubes are hollow with cooling provided by helium flows through the tubes. In the initial studies of the tension tube cooling, an attempt was made to provide some cooling for the moderator as well by means of the tension tube flow. Figure 17 shows a sketch of a typical section of a moderator block. Because of symmetry, it may be assumed that there is no heat flow over the surfaces marked $\frac{dT}{dn} = 0$. To determine the amount of heat flowing to each of the two cooled surfaces, a modified linear approximation was used. Preliminary studies indicated that sufficiently low tension tube temperatures could only be obtained if the coolant outlet temperature from the tension tubes was lower than the 1300°F from the fuel channel tubes. Therefore, in the determination of the relative heat flows, it was assumed that the two streams could be at different temperatures at a given distance from the core inlet. To allow for the fact that there is more cooling area at the fuel channel hole than at the tension tube, the calculated heat transfer coefficient at the tension tube is reduced in proportion to the ratio of heat transfer areas. With these assumptions, the position of maximum temperature on the line of centers of the two holes, x_m , was found from the modified linear analysis. The result is:

$$\frac{x_m}{l} = \frac{\frac{U_M}{c U_{TT}} \left(1 + \frac{c U_{TT} l}{2 k_M} \right)}{1 + \frac{U_M}{c U_{TT}} \left(1 + \frac{c U_{TT} l}{k_M} \right)} + \frac{U_M}{f o l} \left\{ \frac{T_{gTT} - T_{gM}}{1 + \frac{U_M}{c U_{TT}} \left(1 + \frac{c U_{TT} l}{k_M} \right)} \right\} \quad 23.$$

where:

x_m is the position of maximum temperature along the line of centers, measured from the edge of the fuel channel hole.

l is the distance between the edges of the two holes along the line of centers.

U_{TT} is the overall heat transfer coefficient at the tension tube.

$\bar{g}_o$ is the cross sectional average heat generation rate in the moderator at any axial position (assumed constant at any axial position).

C is the ratio of tension tube heat transfer area to the fuel channel hole heat transfer area.

$T_{gT} - T_{gM}$ is the average temperature difference between the two streams.

Other symbols are as before.

A line is drawn through the point x_m in Figure 17 perpendicular to the line of centers and is extended to meet the no-flux surfaces. It is then assumed that all heat generated on one side of this line flows to the cooling surface on the same side of the line. This is of course an approximation only, but for the initial survey studies this assumption will give results of the proper order of magnitude. Once the basic arrangement has been determined from the preliminary studies, more accurate analyses will be carried out. With this assumption, the ratio of heat flows to the two surfaces is:

$$\frac{Q_{TT}}{Q_M} = \frac{4\left(1 + \frac{r_{TT}}{l} - \frac{r_m}{l}\right)^2 - \pi\left(\frac{r_{TT}}{l}\right)^2}{2\left(1 + \frac{r_{TT}}{l} - \frac{r_m}{l}\right)^2 - \pi\left\{\left(\frac{r_{TT}}{l}\right)^2 + \left(\frac{r_m}{l}\right)^2\right\}} \quad 24.$$

For notation, see Figure 17.

A plot of this equation for the MGCR prototype conditions is given in Figure 18, which also gives the ratio of the heat flow to the tension tube to the total heat generated in the moderator.

Again using a chopped sine axial power distribution, the tension tube temperature in the hottest channel is given by the equation:

$$T_{TT} - T_{gi} = F_{TF} \frac{\alpha_T P_T Q_m'}{h_{TM} A_T} \sin \phi + F_{TC} \frac{\alpha_T P_C Q_m'}{\pi \alpha_{HT} g L} \frac{1}{L} \left\{ \cos \Delta - \cos \phi \right\} \quad 25.$$

where:

$R_T Q_{TM}'$ is the average heat flow to the tension tube in one channel.

W_{TT}' is the average coolant flow to a tension tube.

A_T is the heat transfer area of a tension tube.

Other symbols as before.

Writing the values of heat and coolant flows associated with the tension tubes in terms of their ratios to the total heat and coolant flows associated with the moderator, and using the method of reference 1, the maximum tension tube temperature in the hottest module becomes:

$$(T_{TT} - T_{gi})_{max} = \frac{F_{TC} \alpha_T}{\pi \alpha_R} \frac{Q_{TT}}{Q_M} \frac{W_M}{W_{TT}} \Delta T \left\{ \sqrt{\lambda_{TT}^2 + 1} + \cos \Delta \right\} \quad 26.$$

where:

ΔT is the average temperature rise across the core, °F.

$$\lambda_{TT} = C d_i^{0.8} \left(\frac{W_{TT}}{W_M} \right)^{0.2}$$

d_i is the inside diameter of the tension tube, ft.

$$C = \frac{\pi^{0.8}}{4} \frac{F_{TF}}{F_{TC}} \frac{\beta_3}{L''} \left(\frac{R_M \alpha_R W_T}{n_T} \right)^{0.2}$$

F_{TF} is the film drop hot channel factor for the tension tube.

F_{TC} is the tension tube flow hot channel factor.

$$\beta_3 = 57.5 \frac{Pr^{0.6}}{\mu^{0.2}} \quad \text{a coolant property value.}$$

Pr is the Prandtl number.

μ is the absolute viscosity, lb/hr ft.

n_T is the total number of tension tubes in the core.

Calculations indicated that in order to ensure adequate cooling of the tension tubes by providing turbulent flow conditions within them, the tube inner diameter should be $3/8$ " or less with the probable flow distributions required. With a $3/8$ " inner diameter tension tube, calculations were made to determine the maximum and average tension tube temperatures which could be achieved if the condition of equal stream temperatures is imposed. Allowance was made for a clearance gap of 0.010" between the outer surface of the tension tube and the moderator block surface. This gap was assumed to be filled with stagnant helium. These calculations showed that the amount of moderator heat flow to the tension tube cooling stream would be about 4% for the reference design conditions. The resulting calculated maximum tension tube temperature is about 1470°F . Since this temperature is considerably above the level required to ensure proper tension tube action at operating conditions it is evident that the condition of equal stream temperatures must be relaxed, since little gain can be achieved by reducing d_i .

This fact is emphasized by considering the theoretical lower limit of tension tube temperature resulting for an infinite value of the heat transfer coefficient in the tension tube. The lower limit for the temperature is of course 1300°F , if no flow hot channel factor is assumed; with a flow hot channel factor of 1.15 the lower limit is about 1385°F . To investigate the temperature levels obtainable in the tension tube, the moderator cooling flow was assumed to be proportioned so that 10% was directed through the tension tubes. The proportion of heat flowing to the tension tubes from the moderator was determined to be about 4.65% and the temperature rise of the coolant through the tension tubes is about 258°F . The resulting maximum tension tube temperature is about 1092°F , with an average tension tube temperature of about 986°F . The effect of the lower temperature rise of the coolant in the tension tubes is counteracted by the higher temperature rise in the moderator cooling stream in the fuel element shroud annulus, which, as discussed

previously, would also result in a slightly higher temperature rise of the fuel channel flow. The over-all result is no over-all change in core mixed mean outlet temperature.

The small proportion of heat generated in the moderator which flows to the tension tube under the preferred design conditions suggests that it would not be difficult to reduce this heat flow to an even smaller fraction by increasing the clearance gap between the tension tubes and the moderator block to about 0.025" to 0.035", thus effectively insulating the tension tube from the moderator block. Calculations show that increasing the gap from 0.010" to 0.030" with 10% of the moderator coolant flow directed through the tension tubes lowers the heat flow to the tension tube stream to about 3.3% and the temperature rise of this stream to about 183°F with an average tension tube temperature of 916°F. These estimates consider heat generated in the moderator only and not that generated in the tension tube itself, for which information is not currently available.

It is obvious that considerably more work is necessary in this area, but the preliminary results indicate that adequately low tension tube temperatures should be feasible.

As mentioned earlier, calculations show that the thermal stresses in the moderator blocks will probably be too high if cooling only at the fuel channel hole surface is provided. Other cooling surfaces, such as additional holes through the BeO blocks, may have to be provided. However, this probable requirement does not alter the recommended cooling arrangement for the tension tubes, namely that they should be thermally insulated from and cooled separately from the moderator.

3.6 Diffusion of Helium through Beryllia

The moderator coolant bypass flow is in direct contact with the moderator. Due to the low molecular weight of the helium coolant and the high temperatures and pressures existing in the core,

diffusion of helium through the moderator could preclude orificing the bleed flow properly and also could result in starving the fuel element flow if the diffusion were an appreciable fraction of the bleed flow.

A conservative estimate of the diffusion of helium through the beryllia moderator may be made by assuming the temperature and pressure of the moderator bypass flow to be at the average core conditions of 1022°F and 1200 psia, respectively, and the pressure differential to be 30 psi across a 1 inch thick section. (This pressure differential is approximately equal to the total pressure drop across the core.) In addition, the pressure differential will be assumed to exist across the total moderator surface exposed to the coolant, or 478 square inches.

The quantity of gas diffusing through a porous media is given by: (29)

$$Q = K \frac{\Delta p}{\bar{p}} \frac{A}{L} \quad 27.$$

and the diffusion constant is given by:

$$K = B_0 \frac{\bar{p}}{\mu} + \frac{4}{3} K_0 \sqrt{\frac{8RT}{\pi M}} \quad 28.$$

where:

- Q = quantity diffusing (cm³/sec)
- K = diffusivity (cm²/sec)
- Δp = pressure difference (dynes/cm²)
- $\bar{p}$ = average pressure (dynes/cm²)
- A = area of diffusion (cm²)
- L = diffusion path length (cm)
- μ = viscosity (poise)
- R = universal gas constant

- T = Absolute temperature
 M = Molecular weight
 B_0 = Coefficient for viscous flow (cm^2)
 K_0 = Coefficient for slip flow (cm)
 ρ = gas density, gm/cm^3

The values of the coefficients B_0 and K_0 have been determined experimentally in Reference 29 for helium diffusing through a beryllia specimen having a density of 2.81 gm/cc which is 92.9% of the theoretical density. The values are:

$$B_0 = 2.13 \times 10^{-15} \text{ cm}^2$$

$$K_0 = 1.73 \times 10^{-10} \text{ cm}$$

Using these experimentally determined values and a pressure of 1200 psia and a temperature of 1022°F, the diffusion coefficient is found to be:

$$K = 4.58 \times 10^{-4} \text{ cm}^2/\text{sec} = 7.10 \times 10^{-5} \text{ in}^2/\text{sec}$$

The mass flow is given by:

$$\dot{m} = \rho Q = K \Delta p \frac{M}{RT} \frac{A}{L}$$

and with the assumed values the mass flow is found to be:

$$\dot{m} = 1.28 \times 10^{-7} \text{ lb/sec.}$$

The average bleed flow for cooling the moderator is 2.66×10^{-2} lb/sec. The calculated diffusion is only 0.001% of the average bleed flow and, therefore, the diffusion of the coolant through the moderator will be insignificant for beryllia having 92.9% or greater theoretical density. Of course, the BeO plates must be bonded together to prevent leakage between the plates.

4.0 CONTROL RODS

An investigation of the cooling requirements for the control rods was made, the criteria for cooling for the preliminary study being based on limiting the control rod clad temperature to 1500°F.

The current rod design is a cruciform type with 3/8" x 6" blades. The clearance between the blade surface and the moderator is 1/8". The heating rate was based on conservative estimates and resulted in a maximum power generation at the tip of 13.6 watts/cm³ for 30% insertion under hot clean conditions.

From symmetry considerations the clad temperature was calculated assuming half of the heat generated in each blade was transmitted to the adjacent coolant channel. As a further assumption the heat generation was taken to be constant along the blade span. Therefore, from these two assumptions the heat transfer problem can be treated on the basis of a one-dimensional analysis.

The clad temperature is given by:

$$T_c - T = \frac{q}{h} \frac{S}{P} \quad 29.$$

where:

T_c = clad temperature

T = coolant temperature

q = power generation rate

h = heat transfer coefficient

S = heat generation cross section

P = Heat transfer perimeter

The power density decreases exponentially from the control rod tip so

$$q = q_0 e^{-\xi} \quad 30.$$

where q_0 is the maximum heat generation rate.

The coolant temperature is:

$$T - T_0 = \frac{SL}{\dot{m} C_p} \int_0^{\xi} e^{-\xi} d\xi \quad 31,$$

where:

T_0 = initial coolant temperature

L = heated length

$\dot{m}$ = coolant flow rate

C_p = specific heat of coolant (constant pressure)

$\xi = \frac{X}{L}$ = fractional heated length

The coolant temperature may then be written:

$$T - T_0 = \frac{SL}{\dot{m} C_p} \int_0^{\xi} (1 - e^{\xi}) \quad 32,$$

The heat transfer coefficient will be assumed to be given by the relation:

$$h = 0.023 \frac{k}{D_e} Re^{0.8} Pr^{0.4}$$

where:

k = coolant conductivity

D_e = equivalent hydraulic diameter of the coolant passage

Re = Reynolds number based on the equivalent diameter

Pr = Prandtl number

The Reynolds number may be written:

$$Re = \frac{4\dot{m}}{\mu P_f}$$

where:

μ = coolant viscosity

P_f = friction perimeter of coolant passage

Assume the initial coolant temperature and coolant flow to be

$$\begin{aligned} T_0 &= 1200^\circ\text{F} \\ \dot{m} &= 0.01 \text{ lb/sec (0.08 lb/sec total for the control rod)} \end{aligned}$$

The coolant properties are:

$$\begin{aligned} k &= 0.17 \text{ Btu/ft hr } ^\circ\text{F} \\ \mu &= 0.096 \text{ lb/ft hr} \\ Pr &= 0.7 \end{aligned}$$

The geometrical factors are:

$$\begin{aligned} R_f &= 1 \text{ ft} \\ \frac{S}{P} &= \frac{1}{64} \text{ ft} \\ L &= 1.9 \text{ ft} \\ S &= \frac{1}{128} \text{ ft} \end{aligned}$$

The Reynolds number is:

$$Re = \frac{4(0.01)(3600)}{(0.096)(1)} = 1500$$

The heat transfer coefficient is:

$$h = \frac{0.17}{(1/64)} (0.023) (1500)^{0.8} (0.7)^{0.4} = 61 \text{ Btu/ft}^2 \text{ hr } ^\circ\text{F}$$

The clad temperature is given by:

$$T_c - T_\infty = 335 e^{-5}$$

and the coolant temperature is

$$T - T_0 = 436 (1 - e^{-5})$$

ξ	$T - T_0$	$T_C - T$	T_C
.1	41	303	1544
.2	78	274	1552
.4	140	224	1564
.6	196	184	1580
.8	240	151	1591
1.0	275	123	1598

The results for forced convective cooling with several bleed flow rates are shown in Figure 19.

It is seen from Figure 19 that a coolant bleed flow of approximately 0.12 lb/sec is required to limit the control rod clad temperature to a maximum of 1500°F. Clad temperatures are not shown for the first 10% of the control rod length since this part of the control rod heat transfer is dictated by an entrance length heat transfer coefficient and even though the heating rate is a maximum at the rod top the clad temperature will be a minimum due to the very high heat transfer coefficient at the tip.

For a 0.08 lb/sec bleed flow the Reynolds number is 1500 and the flow will be laminar. The laminar heat transfer coefficient was calculated to be 88 Btu/ft² hr °F while the turbulent correlation for the same Reynolds number gives 61 Btu/ft² hr °F, therefore, the turbulent heat transfer coefficient was used in the calculations to give conservative estimates of the clad temperature.

Free convective heat transfer was investigated to determine its ability to cool the control rods without providing a bleed flow. However, the free convection heat transfer coefficient was found to be approximately 6 Btu/ft² hr °F and being an order of magnitude less than the forced convective heat transfer coefficient it was deemed too small for the scheme to be feasible.

The calculated clad temperatures do not include any safety factors, but the results are on the conservative side since the heating rate used

is conservative and the one-dimensional analysis which neglects conduction along the axial length of the control rod gives maximum temperatures.

The required bleed flow of approximately 0.12 lb/sec is for the hottest control rod and if it is assumed that the heat generation in the control rods follows the radial max-to-ave power density in the core the total bleed flow for the control rods is about 1% of the primary coolant flow rate.

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FIGURE 1

CORE DESIGN PARAMETERS, 19-ROD HEXAGONAL BUNDLE

FIGURE 2a
AXIAL TEMPERATURE AND POWER DISTRIBUTION

FIGURE 2b

AXIAL TEMPERATURE AND POWER DISTRIBUTION

FIGURE 3
AXIAL THERMAL POWER DISTRIBUTION
WITH BURNABLE POISON

AXIAL TEMPERATURE DISTRIBUTION WITH CORE LIFETIME

FIGURE 5

EFFECT OF CHANGES OF CYCLE CONDITIONS ON THE MAXIMUM CLADDING TEMPERATURE AND ON THE REACTOR OUTLET TEMPERATURE

A = COOLANT FLOW AREA
 c = MINIMUM CLEARANCE BETWEEN
 FUEL RODS
 d = FUEL ROD DIAMETER
 D = OUTSIDE DIAMETER OF FUEL
 ASSEMBLY
 D_1 = SEE FIGURE
 D_2 = SEE FIGURE
 $p = 1 + \frac{c}{d}$ = PITCH-TO-DIAMETER RATIO
 P = HEAT TRANSFER PERIMETER
 P_f = FRICTION PERIMETER

$$\frac{D_1}{d} = 2p$$

$$\frac{D_2}{d} = 3.864p$$

$$\frac{D}{d} = 4.864p$$

$$\frac{P}{d} = 56.55$$

$$\frac{P_f}{d} = 65.28p + 9.690$$

$$\frac{A}{d^2} = 6.079p^2 + 25.00p - 27.42$$

$$\frac{P_f}{P} = 1.154p + 0.1714$$

FIGURE 6
 FUEL ELEMENT GEOMETRICAL PARAMETERS

FIGURE 7a

FRICTION FACTOR FOR 19-ROD HEXAGONAL BUNDLE

FIGURE 7b
FRICTION FACTOR FOR CIRCULAR 19-ROD BUNDLE

FIGURE 8

LOCAL-TO-FULLY DEVELOPED HEAT TRANSFER COEFFICIENTS
 BY MASS TRANSFER TECHNIQUE IN ANNULI

Re = REYNOLDS NUMBER BASED ON THE
EQUIVALENT DIAMETER

h = LOCAL HEAT TRANSFER COEFFICIENT

$\bar{h}$ = AVERAGE HEAT TRANSFER COEFFICIENT

$\epsilon = \frac{d}{(R_2 - R_1)} = \text{ECCENTRICITY}$

FIGURE 9

MASS TRANSFER IN ECCENTRIC ANNULI

FIGURE 10
 MASS TRANSFER IN ECCENTRIC ANNULI

FIGURE 11

MASS TRANSFER COEFFICIENT AXIAL VARIATION

FIGURE 12

THERMAL CONDUCTIVITY OF UO_2 - BeO MIXTURE UO_2 PARTICLES
 UNIFORMLY DISPERSED ON A CUBICAL LATTICE IN BeO MATRIX

FIGURE 13
MAXIMUM PRESSURE IN HOTTEST FUEL ROD DUE TO
GASEOUS AND VOLATILE FISSION PRODUCTS

FIGURE 14

ESTIMATED CLAD TEMPERATURE IN EMERGENCY COOLING

FIGURE 15
MODERATOR TEMPERATURES

FIGURE 16
TEMPERATURE STABILITY OF COOLANT STREAMS FOR
BY-PASS COOLING OF MODERATOR

FIGURE 17

DIAGRAM OF TYPICAL MODERATOR SECTION

FIGURE 18
RELATIVE HEAT FLOWS IN MODERATOR

FIGURE 19

CONTROL ROD CLAD TEMPERATURE

Figure 20