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OAK RIDGE SCHOOL OF REACTOR TECHNOLOGY

REACTOR PRODUCING 3000°F STEAM

FOR PROCESS HEAT

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REACTORS-POWER

AEC RESEARCH AND DEVELOPMENT REPORT

OAK RIDGE SCHOOL OF REACTOR TECHNOLOGY

Lewis Nelson, Director

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PREFACE

In September, 1955, a group of men experienced in various scientific and engineering fields embarked on the twelve months of study which culminated in this report. For nine of those months, formal classroom and student laboratory work occupied their time. At the end of that period, these seven students were presented with a problem in reactor design. They studied it for ten weeks, the final period of the school term.

This is a summary report of their effort. It must be realized that, in so short a time, a study of this scope can not be guaranteed complete or free of error. This "thesis" is not offered as a polished engineering report, but rather as a record of the work done by the group under the leadership of the group leader. It is issued for use by those persons competent to assess the uncertainties inherent in the results obtained in terms of the preciseness of the technical data and analytical methods employed in the study. In the opinion of the students and faculty of ORSORT, the problem has served the pedagogical purpose for which it was intended.

The faculty joins the authors in an expression of appreciation for the generous assistance which various members of the Oak Ridge National Laboratory gave. In particular, the guidance of the group consultant, Paul R. Kasten, is gratefully acknowledged.

Lewis Nelson

for

The Faculty of ORSORT

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ACKNOWLEDGMENT

We wish to express our appreciation to our advisor, P. R. Kasten, for his encouragement and assistance in this project. In addition, we would like to thank the following ORNL personnel for their assistance on special phases of this project: M. L. Nelson, W. E. Kinney, C. E. Curtis, L. M. Doney, W. K. Ergen, and E. G. Alexander.

GROUP LEADER'S ACKNOWLEDGMENT

I am grateful to each member of the group for their competent and conscientious contributions to this study. It was a real pleasure to work with them. While this study represents a team effort, with each member contributing something to each chapter of the report, I think it is pertinent to record that the primary responsibilities for the various chapters in the body of this report were as follows:

III, IV	R. W. Ritzmann
V	F. J. Remick
VI	J. E. Schmidt
VII	W. J. Roberts
VIII, IX	J. S. Lagarias

J. O. Roberts contributed much as "troubleshooter", dividing his efforts between the various aspects of the study as the situation demanded, with particular emphasis on Chapter V. It was originally planned that he would be responsible for chapters on the fuel cycle and hazards evaluation but it soon appeared that the group was in danger of spreading out its efforts too thinly and he agreed to the change.

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ABSTRACT

This report presents a conceptual design for a reactor producing 3000°F, 400 psi steam to supply process heat for high temperature endothermic chemical reactions. Specifically, the supplying of heat to a coal hydrogenation plant is studied but other applications are possible. Such a market for nuclear heat is economically interesting because the competition is with the relatively expensive oxygen combustion of coal.

It was assumed that in the present state of the art a ceramic heat source could not contain nuclear fuel in an open cycle due to the hazard of fission product leakage into the process stream. This assumption limited the high temperature heat source to the order of 10% of the total reactor power. The particular design studied was found to be not economically feasible due to the unusually large inventory of enriched uranium required. However, it is felt that with the suggested changes nuclear process heat from this type of reactor could be competitive if the excess power produced could be sold economically.

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CHAPTER I
INTRODUCTION

Ever since the controlled release of nuclear fission energy became a reality the possibility of using nuclear reactors as sources of very high temperature heat has excited the imagination of scientists and engineers since, in principle, this energy can be released at practically any temperature level. In addition to the interest in heat engines with higher thermodynamic efficiency there is the interest of the chemical engineer in better ways of supplying the heat of reaction for high temperature endothermic chemical processes. Despite speculations concerning nuclear pebble beds, fluidized beds and radiant heaters, however, reactors which have been built to date have operated, with few exceptions, at quite conservative, conventional temperatures. The reasons for this are, of course, as much ascribable to the infancy of high temperature technology in general as of nuclear technology in particular.

The present study presents a conceptual design of a reactor to supply high temperature steam to a chemical process. In order to specify sizes, temperatures, pressures, costs, etc., a particular chemical process was chosen, namely, the coal gasification process producing "synthesis gas", a mixture of carbon monoxide and hydrogen. Such a synthesis gas has been widely used as a raw material for "Fischer-Tropsch" reaction processes and as a source of hydrogen for ammonia synthesis and methanation reactions. During World War II at least 85% of the German aviation gasoline was made by coal hydrogenation with hydrogen obtained from synthesis gas.

Though coal hydrogenation has not yet found commercial application in this country, in 1944 Congress directed the Bureau of Mines to furnish

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industry with the basic information and cost data necessary for the development of a synthetic fuel industry. The results were published in a Bureau of Mines report (R. I. 4564) in 1949. This report provided the basic information necessary to design a totally integrated chemical plant producing 30,000 barrels/day of synthetic liquid fuels. It was decided to integrate the proposed nuclear plant with such a coal hydrogenation plant. While the 30,000 bbl/day output is equivalent to only that of a medium sized petroleum refinery, the power and steam requirements are considerably larger than those considered for most nuclear plants proposed to date.

The economic impetus for studying the application of nuclear heating to coal gasification is the comparatively high fuel cost of the present process. Coal plus air cannot be used to supply the heat of reaction because the high temperatures required are not readily obtainable when the oxygen is diluted with the nitrogen in the air and because the nitrogen cannot be separated easily from the product gases. As a result, it is necessary to use coal plus relatively pure oxygen as the fuel. Based on coal at \$3.56/ton and oxygen at \$6.00/ton, this "premium" fuel costs \$1.32 MMBTU (73% of which is oxygen cost) for "high temperature" (above 2000°F) heat. Since the basic nuclear fuel cost would be \$0.26/MMBTU (U^{235} cost of \$17/gm), there is reason to hope that, even after allowing for inventory, chemical processing, operating, capital and other charges in excess of those for a conventional plant, nuclear process heat might be strongly competitive.

Because of the high temperatures involved, the present study has assumed that the process heat source would have to be a ceramic; also, since gaseous fission products would diffuse through ceramic materials at these temperatures the ceramic could not contain fissionable material if the radioactivity of the high temperature steam were to be at a minimum.

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The latter assumption is rather severe since it means that the high temperature heat must be supplied by neutron and gamma heating of the ceramic. This means that at most approximately 14% of the total reactor heat could be produced in the ceramic. Actually, in the design reported here about 9% of the reactor heat would be generated in the sintered magnesium oxide, which would act both as moderator for the reactor and as high temperature heat source for the process steam. Thus, most of the reactor heat must be utilized as "low temperature" heat (either directly or as electrical power), part of which could be consumed within the coal hydrogenation plant and the remainder of which would have to be sold externally.

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CHAPTER II

SUMMARY

The proposed high temperature reactor differs from most conventional power reactors in at least two important respects: (1) An attempt has been made to maximize the heat generation in the non-fuel bearing regions of the reactor, inasmuch as this region is the source of the high temperature heat. In a conventional power reactor large amounts of heat generated outside of the fuel region would ordinarily be considered undesirable. (2) A ceramic material has been used both as a moderator and as the high temperature heat exchange medium. Only a very few materials were found to meet the simultaneous requirements of performing both functions satisfactorily. Of these, magnesium oxide appeared to be the best choice though it is not a very good moderator by ordinary standards.

Thermal Balance

As conceived in this study a single reactor would operate at a gross power of 400 thermal megawatts, about 35 MW of this being generated in the MgO ceramic moderator and used to heat steam from 2040°F. to 3000°F. Four such reactors would be required for an integrated coal hydrogenation plant producing 30,000 bbl/day of liquefied products. The four reactors would supply 140 MW of high temperature heat (i.e., above 2040°F) to the coal gasification part of the plant, which would produce hydrogen for the hydrogenation process. The remaining 1460 MW of heat would be removed from the reactor by the circulating fused salt fuel, transferred to sodium and then to water to produce steam at 1000°F and 1800 psia. This high pressure steam would be used to drive turbines to generate electricity at 37.5% thermal efficiency (beyond the primary fused-salt-to-sodium heat exchanger, the heat transfer and power generation system envisioned would be very much like that designed by another 1956 ORSORT summer study group for a "600

MW Fused Salt Homogeneous Reactor Power Plant"). Part of the steam would be withdrawn after partial expansion in the turbines to 700°F and 450 psia, heated to 2040°F and 431 psia by regenerative heat exchange with the 2240°F gaseous products of the coal gasification reaction, and passed through coolant channels in the ceramic moderator of the nuclear reactor to heat it to 3000°F and 430 psia. This 3000°F steam would be reacted with 600°F coal to produce a 2240°F mixture of H_2 , CO_2 and excess H_2O (68 mol %) whose heating value would be nearly completely recovered by first passing it through the regenerative heat exchange step (mentioned above) and then using it to preheat boiler feed water. Other steam would be withdrawn after expansion in the turbines to supply various low temperature heat requirements of the coal plant (a total of 149 MW) so that the net electrical output would be about 492 MW. Of this, 168 MW would be needed to drive pumps and compressors and supply other electrical requirements of the coal plant, leaving 324 MW available for sale. The regenerative heat exchange between the gasification products and the incoming steam before passing it through the reactor was highly significant in the overall thermal balance. If the steam were to be heated to 3000°F from 1000°F instead of 2040°F by radiation heating, eight reactors instead of four would be required and there would be an additional 1460 MW of heat (578 MW electricity) to be sold externally.

Engineering Aspects

The reactor envisioned would have ninety 2-inch i.d., 2 1/4 in. o.d. Inconel tubes, arranged in a triangular array on 12 in. centers, passing through a ten foot diameter by twelve foot high cylindrical matrix of hexagonal shaped blocks of MgO . The reactor core is reflected on its side with eight inches of beryllium oxide and the whole is contained in.

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Fig. 2.1
ARTISTS CONCEPTION

Fig. 2.2
FLOW SCHEMATIC

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Fig. 2.3
SCHEMATIC HORIZONTAL CROSS-SECTION

a four inch thick, twelve foot diameter by eighteen foot long steel vessel. A fused salt fuel (50 mol % NaF, 45 ZrF₄, 5 UF₄) circulates through the reactor in the Inconel tubes at 17.4 ft/sec and is heated from 1050°F to 1200°F by internal heat generation at an average rate of 550 watts/cc. It delivers its heat to sodium in external heat exchangers.

Figure 2.1 is an artist's conception of the reactor vessel with its primary heat exchangers. It is intended to convey an impression of physical size rather than mechanical detail since this study did not include a detailed mechanical design. Figure 2.2 indicates the flow of fuel and steam through the reactor. Figure 2.3 is a schematic horizontal cross-section through the reactor illustrating the hexagonal lattice. Two possible lattice arrangements are indicated, one based on triangular unit cell, the other on a hexagonal unit cell.

The ceramic moderator is perforated with 1/4 inch holes on 3/4 inch triangular centers to allow passage of the steam, the hole size and spacing determined by heat transfer and thermal stress considerations, primarily the latter. Figure 5.1 indicates the "fine structure" of the lattice. The 2 1/4 in. o.d. tubes are in 4 in. diameter holes in the ceramic block, the annulus being a "dead space" acting as a thermal barrier between the tube and the much hotter ceramic. The annulus might be filled simply with stagnant steam or, preferably, with a very low density material on the order of 5 lb/ft³ aluminum silicate "wool" or 8.5 lb/ft³ Aeorgel silica insulation but made of something with still higher temperature and chemical resistance, presumably MgO itself. The heat sources in the ceramic moderator are estimated as shown in Table 2.1. After allowing for heat losses from the moderator by conduction, convection and radiation, about 8.7% of the total fission energy is utilized as high temperature heat,

compared with approximately 14% if all the neutron and gamma energy were deposited in the moderator. The fractional utilization of this maximum potential high temperature heat would be increased if smaller fuel tubes were used and it is not claimed that the 2 inch size on 12 inch centers is optimum. A change to smaller tubes with the same spacing would result in higher power densities and flow rates than were felt advisable. In addition, an increased uranium concentration would be required for criticality and, assuming that the external circuit design would remain unchanged, the already high inventory would become even higher. Decreasing the distance between tubes would be complicated by the necessity of isolating the 1200°F ceramic.

Reactor Physics

Because of the comparatively poor moderator used the reactor's neutron spectrum is half thermal, half intermediate in character, i.e. approximately half the fissions result from absorptions at energies above thermal. The estimated average fluxes at 400 MW are given in Table 2.2. Fluxes and multiplication constants were obtained by a combination of hand calculations for a 22 group bare homogenized reactor and ORACLE calculations for a three group, three region reactor. The hand calculations provided flux-averaged cross-sections for the ORACLE while the ORACLE provided disadvantage factors and reflector savings for the hand calculations. Ten percent poison in the fuel was assumed applicable to all groups, as an approximation. In a more detailed study, more attention should be paid to calculating both thermal poisons at 2000-3000°F and intermediate poisons. Though the fuel is at an average temperature of 1125°F, thermal group cross-sections in the fuel and tube wall were taken at 2600°F, the average temperature of the moderator. The temperature coefficient of reactivity of the fuel is negative but that of the moderator is positive, requiring special precautions in controlling

Table 2.1

<u>Source</u>	<u>Total Energy</u> (mev/fission)	<u>Energy Deposited</u> <u>in Moderator</u>
Fission Fragment Kinetic Energy.	168.	0.
Neutron K. E.	5.	4.
Prompt Fission Gammas	7.5	5.
Capture Gammas		
-from uranium1.5	7.8	6.8 { 1. 0.6 2.3 2.9
-from poisons0.9		
-from Inconel2.4		
-from MgO3.0		
Fission Product Betas	7.	0.
Fission Product Gammas	8.4	2.3
Other Capture Product Radiations	<u>2.</u> 206.	<u>0.</u> 18.

Table 2.2

Average Fluxes at 400 Megawatts

<u>Group</u>	<u>Fuel</u>	<u>Moderator</u>
Thermal (0.14 ev)	5×10^{13}	9×10^{13}
Intermediate (0.14-9118 ev)	3×10^{14}	4×10^{14}
Fast (9118- 10^7)	3×10^{14}	1.5×10^{14}

the steam flow through the moderator during reactor power transients or use of control rod. The latter results because of the much greater latitude in blanket temperature variation in comparison with fuel temperature.

Figure 8.7 indicates the radial variation of the three group fluxes as calculated by the ORACLE for a homogenized reactor core with BeO reflector on the sides. Figure 8.6 shows a similar plot for a unit cell. Figure 2.4 illustrates the manner in which flux in the homogenized reactor varies with neutron energy. Flux per unit lethargy is plotted as a function of lethargy so that the area under the curves is proportional to the integrated flux for each group.

The critical fuel concentration is estimated to be 5 mol % UF_4 of 93% enrichment. The fuel hold-up in the reactor core is 670 liters, with 2100 in the heat exchangers and about 2200 in the rest of the plumbing system for a total of 5000 liters. This amounts to a total inventory of approximately 1840 kilograms of enriched uranium for a 400 MW reactor, which compares unfavorably with 240 kg for the "600 MW Fused Salt Homogeneous Reactor Power Plant" designed by another 1956 ORSORT study group. The large enriched uranium inventory would be reduced if BeO could be used as a moderator but this ceramic is not stable to steam above 2200°F. Use of ZrO_2 , Al_2O_3 or SiC as ceramic moderator would increase the U^{235} inventory significantly. In addition, SiC is not stable to steam above 2700°F.

Economics

The large inventory of enriched uranium turns out to be the controlling factor in the costs of high temperature heat from this reactor. Presumably the difference in power costs between this reactor and one like the "600 MW Fused Salt Homogeneous Reactor Power Plant" would have to be born by the high temperature heat customer. On this basis the estimated high temperature heat costs from this reactor are:

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U ²³⁵ burn-up	\$0.26 MMBTU
Operation, Maintenance and Amortization.	0.48
Inventory and Chemical Reprocessing.	<u>2.01</u>
TOTAL	\$2.75

Since this is not competitive with a coal-oxygen cost of \$1.32/MMBTU, this approach to nuclear process heat is not economically feasible unless a much smaller inventory can be achieved. It would appear that this reactor design should be reevaluated for stationary fuel elements since a factor of inventory reduction of five could probably be achieved, though at the expense of added complexity in design, operation, refueling, chemical processing, etc. This would reduce inventory and chemical processing charges to \$0.35/MMBTU and the total to \$1.09/MMBTU.

The above costs were based on the assumption that the excess power produced could be sold externally on a self-supporting basis. This condition is essential since it is calculated that a one mil/KWH loss on the external power sold would be equivalent to adding \$4.12/MMBTU to the cost of the high temperature heat.

CHAPTER III

ECONOMIC CONSIDERATIONS

An estimate of the costs of the heat produced by the high temperature portion of this reactor are as follows:

	<u>\$/MMBTU</u>
Fuel, U ²³⁵ burn-up	0.26
Operating & Maintenance	0.13
Chemical Reprocessing	0.89
Inventory	1.12
Amortization	0.35
Administrative Expense and Profit	-----
TOTAL	<u>\$2.75/MMBTU</u>

The total, \$2.75/MMBTU, compares with present costs of \$1.32/MMBTU obtained by burning coal in oxygen. The nuclear heat costs are unfavorably high because of the large uranium inventory associated with the circulating homogeneous fuel with an external heat exchange circuit. The uranium inventory for this reactor is 1840 kg. of 93% enriched uranium versus 240 kg. for a homogeneous fused salt reactor with an internal heat exchange circuit. If the inventory of the reactor presented in this report were only 20% as large, by using some sort of internal heat exchange circuit or solid fuel elements, the chemical reprocessing and inventory costs would be reduced by \$1.66/MMBTU bringing the total cost down to \$1.09/MMBTU. If fuel were placed in the high temperature heat zone to eliminate the necessity of selling power externally, total costs would amount to \$1.15/MMBTU, enough below existing costs to warrant further consideration.

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Basis for Comparison

Since the purpose of this study was to determine the economic and technical feasibility of producing high temperature process heat from a nuclear reactor, no attempt was made to estimate the costs of the power producing section or the reactor presented herein. It has been assumed that the reactor designed by another ORSORT 1956 summer problem group, "600 MW Fused Salt Homogeneous Reactor Power Plant" (FSR), is the most economical fused salt power-producing reactor capable of being designed with present technology, and that the high temperature heat produced by this reactor would have to bear all expenses which were greater than those estimated for this FSR. This merely states that one is not willing to pay more for power than can be obtained from the most economical fused salt reactor and that, therefore, any extra costs must be assigned to the high temperature heat on an incremental basis. While performing the cost analysis in this manner is entirely correct, it results in a rather severe economic penalty for this reactor design because of the additional expense in the power producing section will be reflected by a factor of approximately ten ($\frac{100 - 8.7}{8.7}$) on the high temperature portion of the reactor. This is a direct result of limiting the high temperature heat to gamma ray and neutron heating from the reactor core.

Fuel Cost

The value of the U^{235} burned is a straightforward calculation and is shown below. The value of uranium was taken to be \$17/gram of U^{235} in 93% enriched material.

Heat release per fission = 200 MEV/fission

$$1 \text{ MEV} = 1.520 \times 10^{-16} \text{ BTU}$$

$$\frac{\sigma_t}{\sigma_f} \text{ of } U^{235} = 1.19$$

Avagadro's number = 6.023×10^{23} atoms/gram mol

$$\frac{10^6}{200 \times 1.520 \times 10^{-16}} \times \frac{235}{6.023 \times 10^{23}} \times 1.19 = 0.0153 \text{ g. } U^{235}$$

burned/MMBTU

$$\text{Fuel Cost} = 0.0153 \times 17 = \$0.26/\text{MMBTU}.$$

Operating & Maintenance Cost

The operating and maintenance cost for operating the high temperature portion of the reactor was assumed to be the same as for the power producing section on an equivalent kilowatt basis. While the operating and maintenance cost on the high temperature portion would not include those for the turbines and generator plant, they would include those of the more intricate chemical plant and regenerative heat exchanger. These two factors were assumed to cancel one another. Operating and maintenance cost was taken to be 1.2 mills/KWH. This is the cost necessary for competitive 10.0 mills KWH power as presented by James A. Lane (1) and is the cost used for estimating the FSR.

$$1 \text{ KWH} = 3,413 \text{ BTU}$$

$$\text{Thermal efficiency} = 37.5\%$$

$$\text{Operating \& Maintenance Cost} = \frac{10^6}{3,413} \times 1.2 \times 10^{-3} \times 0.375 = \$0.13/\text{MMBTU}.$$

Chemical Reprocessing Cost

It was assumed that the fuel would have to be completely removed from the reactor and the uranium recovered once per year, and that the cost would include only the cost of reclaiming the uranium. The cost of replacing the fused salt was not considered because the inventory of fused salt in this reactor is approximately the same as that in the FSR so that the high

temperature portion of the plant would not be penalized. However, since the uranium concentration in the fused salt of this reactor is much higher, the cost of processing the extra uranium was assigned to the high temperature portion of the plant.

The chemical reprocessing cost was assumed to be \$0.50/gram of U^{235} . Present costs for a full scale reprocessing plant are estimated to be \$1.00/gram. The \$0.50/gram cost was used in anticipation that future technology and larger reprocessing plants would lower the cost significantly. For example, if a successful continuous liquid-liquid (e.g. fused salt to liquid metal) extraction technique were to be developed, the fuel could be reprocessed continuously while the reactor was in operation. In this event \$0.50/gram would probably be a very generous allowance for chemical reprocessing. The cost is calculated below.

High temperature heat from one reactor = 35 MW = 119.6 MMBTU/hr.

Assume 330 stream days per year (90% load factor).

Heat load from one reactor = $119.6 \times 24 \times 330 = 946,000$ MMBTU/yr.

U^{235} inventory in FSR = 240 kg. of 93% enriched uranium or
160 kg. per 400 MW.

Fused salt fuel solution contains 5 mol % or 10.5 wt % enriched uranium.

Total volume of entire fuel system = 5000 liters.

Weight of U^{235} in fused salt of one reactor = $5000 \times 1000 \times 3.5/1000 \times 0.105 = 1,840$ kg. of 93% enriched uranium.

Incremental additional fuel to be reprocessed = $1,840 - 160 = 1,680$ kg.

Incremental cost for reclaiming U^{235} = $1,680 \times 1000 \times 0.50 = \$840,000/\text{yr.}$

Chemical reprocessing cost = $\frac{840,000}{946,000} = \$0.89/\text{MMBTU.}$

Inventory Charges

The inventory charge was calculated using an interest rate of 4% per year based on uranium inventory valued at \$17 per gram of U^{235} . Again the inventory over the required by the reference FSR must be assigned the cost of the high temperature heat.

$$\begin{aligned}\text{Value of additional uranium inventory} &= 1,680 \times 100 \times 0.93 \times 17 \\ &= \$26,600,000.\end{aligned}$$

$$\text{Interest on incremental inventory} = 26,600,000 \times 0.04 = \$1,060,000/\text{yr}.$$

$$\text{Inventory chargeable to high temperature heat} = \frac{1,060,000}{946,000} = \underline{\underline{\$1.12/\text{MMBTU}}}.$$

Amortization Charges

Since no attempt to estimate the capital cost of this reactor was made, the estimation of amortization expense is difficult. To give an idea of its possible magnitude, a capital cost of \$250/KW of electricity (\$94/KW of heat at 37.5% thermal efficiency) was assumed. Amortization expenses were estimated at 10% of this cost per year.

$$\text{Capital cost of high temperature section} = 35 \times 1000 \times 94 = \$3,300,000.$$

$$\text{Amortization charges} = 3,300,000 \times 0.10 = \$330,000/\text{yr}.$$

$$\text{Amortization charges} = \frac{330,000}{946,000} = \underline{\underline{\$.35/\text{MMBTU}}}.$$

The above estimate is probably low since no allowance for the increased cost of the coal gasification plant has been taken into account and since this reactor is more complicated than a conventional reactor. However, the fact that \$94/KW included generating equipment would tend to offset these increases.

Administrative Expenses and Profit

No charges for administrative expense or profit have been stipulated. The addition of the high temperature section would incur no additional

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administrative expense in the plant. The coal and oxygen costs used to arrive at the value of present high temperature heat (see Chapter IV) were plant costs and did not include either administrative expense or profit.

Dependence of System on Sale of Power to Surrounding Community

Table 3.1 shows the heat distribution of the four reactor nuclear power-high temperature station described in this report. Because the coal hydrogenation plant power requirements are below the output of the nuclear power station, 324 MW of electrical capacity must be sold outside the plant. This study did not consider the implications of this restriction in detail. The sale of excess power from a coal hydrogenation plant may be a difficult task however. A coal hydrogenation plant would undoubtedly be located at the mine site of an inexpensive coal to eliminate coal transportation charges for the relatively large tonnages involved. This means that any excess power would have to compete against power from inexpensive coal in a sparsely populated area. If the electricity from the power section of this plant were not competitive, it would presumably have to be sold at competitive prices and the loss on the electricity assigned to high temperature heat. This would amount to \$4.12/MMBTU for each 1 Mill/KWH loss on electricity.

Electrical generating capacity of power plant = 492 MW.

$$492 \times 10^3 \times 24 \times 330 = 3.9 \times 10^9 \text{ KWH/yr. electricity produced.}$$
$$\text{Yearly loss} = 3.9 \times 10^9 \times 1 \times 10^{-3} = \$3,900,000/\text{yr.}$$

Additional cost of high temperature heat = $\frac{3,900,000}{946,000} = \$4.12/\text{MMBTU}$.

Obviously it is a dire necessity that the electricity be competitive. Even if power were competitive, it is likely that the owner of the coal hydro-generation plant, probably a coal or petroleum company, would object to being in the position of also being a public utility. The amount of 19% of the total reactor heat would have to be obtained as high temperature heat if

Table 3.1

Nuclear Plant Power and Heat Distribution

Gross reactor heat (4 x 400)	1,600 MW-heat
Less high temperature heat	-140 MW-heat
Less Hydrogenation Plant net heat consumption (see Table 4.3)	<u>-149 MW-heat</u>
REACTOR HEAT FOR POWER PRODUCTION	1,311 MW-heat
 Total power production at 37.5% thermal efficiency = $1,311 \times 0.375 =$	 492 MW-elect.
Less Hydrogenation Plant electrical consumption (see Table 4.3)	<u>-168 MW-elect.</u>
ELECTRICAL CAPACITY TO BE SOLD OUTSIDE	324 MW-elect.

the sale of electricity were to be eliminated. Thus, in order to avoid selling electricity, fuel would have to be placed in the high temperature portion of the reactor or a chemical process which uses only 8.7% high temperature heat would have to be found.

Costs Without Penalty for Power System

The above analysis shows that the chemical reprocessing and inventory charges are prohibitively high for the reactor design developed in this study. Obviously the large inventory of fuel and the limitation of obtaining the high temperature heat from only gamma ray and neutron heating seriously hinder the economy of this reactor. If solid fuel elements were used in place of the fused salt it is conceivable that the inventory could be reduced by a factor of five ($\frac{5000}{670} = 7.4$). The solid fuel element inventory would then be approximately 370 kg., and chemical reprocessing and inventory charges would be as follows:

Fuel inventory chargeable to high temperature section = $370 - 160 = 210$ kg.

Cost of reprocessing solid fuel elements assumed to be \$1.00/g.

Unit cost of chemical reprocessing = $\frac{210 \times 1000 \times 0.95 \times 1.00}{946,000} = \$0.21/\text{MMBTU}$.

Unit cost of inventory = $\frac{210 \times 1000 \times 0.93 \times 17 \times 0.04}{946,000} = \$0.14/\text{MMBTU}$.

Assuming fuel, operating and maintenance, and amortization costs would remain the same, total cost would amount to \$1.09/MMBTU which is competitive with heat from coal and oxygen.

If the extra electricity could not be sold and a ceramic bearing fuel were used so that all of the heat would be obtained as process heat, the high temperature heat would have to bear all of the chemical reprocessing and inventory charges. In such a reactor, it is conceivable that the fuel

inventory would be approximately 240 kg., the critical mass of the FSR. Chemical reprocessing and inventory charges would then be \$0.25 and \$0.16/MMBTU respectively.

$$\text{Unit cost of chemical reprocessing} = \frac{240 \times 1000 \times 1.00}{946,000} = \$0.25/\text{MMBTU}$$

$$\text{Unit cost of inventory} = \frac{240 \times 1000 \times 0.93 \times 17 \times 0.04}{946,000} = \$0.16/\text{MMBTU}.$$

Thus total cost would be \$1.15/MMBTU which is enough below present expenditures for heat from coal and oxygen to warrant further study.

References

- (1) Lane, James A. "Economics of Nuclear Power", Session C.3, Paper No. 590A (Un-476).

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CHAPTER IV

INTEGRATION OF NUCLEAR AND COAL HYDROGENATION PLANTS

Introduction

The decision to apply nuclear heat to coal gasification was based on several factors. The two most basic reasons were that the summer project group wished to study the application of a neutronic device to supplying the process heat of a commercial chemical process and to explore the nuclear physics and materials problems at elevated temperatures. A third was that a nuclear reactor may have an economic advantage as a producer of process heat. Presently, the chemical process industry obtains a major portion of its high temperature heat from burning a fuel with relatively pure oxygen. The fuel cannot be burned with air because the diluent nitrogen lowers the combustion temperature attainable. This is the result of having to heat the nitrogen to flame temperature, as well as supplying the higher heat losses associated with the larger combustion equipment required. In addition, the nitrogen dilutes the product gases and increases the work of compression in processes which operate at elevated pressures. The expense of obtaining relatively pure oxygen and the incomplete combustion of fuels at elevated temperatures, increase the cost of this heat several times over that obtained by burning a fuel in air. Because of these factors, it was anticipated that nuclear heat might compete on the process heat market. The fourth basic requirement was that steam be the heat transport medium to the high temperature process and therefore be the coolant for the high temperature portion of the reactor.

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The chemical process was chosen with the above four factors in mind. It was further decided that a specific process be chosen in order that the problems of integrating the nuclear plant with the chemical plant would receive proper consideration, and in order that the relative economic factors could be calculated. The coal gasification process satisfied the basic requirements and was convenient in that literature describing the process and commercial plants was readily available. The reactions involved in the gasification of coal by oxygen are as follows:

The reactants are combined in such a manner that the heat liberated by reactions (1) and (2) equals the heat absorbed by reaction (3). Reaction (4) takes place to satisfy the required chemical equilibrium conditions. Because of this, approximately 75% of the combustion takes place by equation (1). In the gasification of coal by nuclear steam, equations (1) and (2) would be eliminated and the endothermic heat required by equation (3) would be supplied by sensible heat in the steam above the reaction temperature, 2240°F. The CO₂-CO-H₂ (or, if the CO₂ is scrubbed out, CO-H₂) product gas mixture is called "Synthesis Gas".

The coal gasification process had an added attraction in that synthesis gas is used as a raw material for Fischer-Tropsch synthesis, the manufacture of Oxo alcohol, and the manufacture of hydrogen for ammonia synthesis and methanation reactions. Thus the perfection of a nuclear-reactor coal-gasification process would have immediate commercial application. The Atomic

Energy Commission has also expressed interest in coal gasification and is conducting a joint research program with the Bureau of Mines' Appalachian Experiment Station, Morgantown, West Virginia. Their work to date has dealt with materials testing in a reactor simulating ceramic fuel elements.

While it was desired to produce "Nuclear Synthesis Gas" for a commercial process now operating in the United States, no information giving a complete plant material, heat and power balance was readily found. Therefore, it was decided to supply the high temperature heat requirements of the coal gasification step in a 30,000 bbl/day hydrogenation plant. A comprehensive report for such a plant was issued by the Bureau of Mines (1) in 1949 as a result of a directive by the 78th Congress. While coal hydrogenation is not being practiced commercially in this country today, a plant was operated by Union Carbide and Carbon Chemicals Company for an extended period of time at Institute, West Virginia, as a source of "tailor-made" organic chemicals, and 85% of German aviation gasoline was made by coal hydrogenation during World War II. The coal industry has pursued the possibility of converting solid coal into liquid gasoline for many years. The synthesis gas used as a raw material for hydrogen production in a hydrogenation plant could serve a Fischer-Tropsch or Oxo-alcohol process equally well. Only the total heat and power requirements for the total plant would change.

Choice of Heat Transport Medium

The heat transport medium, as used in this report, is the gas which passes through the high temperature zone of the nuclear reactor, where it is heated up and then immediately transported to the high temperature process where it gives up its sensible heat to an endothermic chemical reaction. The heat transport medium is also the high temperature reactor coolant.

As stated earlier, one of the basic criteria in design of the reactor was that steam should be the heat transport medium. This restriction was imposed because steam is the most frequently used reactant and heat transfer medium in the process industry. Because water is inexpensive, open cycle steam flow can be used without incurring prohibitive transport medium costs. Open cycle flow is almost mandatory in chemical processes because the chemicals would contaminate the medium for reuse. Steam has three other important advantages. One, the boiler feed water can be pressurized and the water vaporized at the reaction pressure, avoiding the mechanical work of compression. Two, steam can readily be separated from gaseous reaction products by condensation. Three, it has good nuclear properties, offering little poisoning and therefore gaining little radioactivity. Finally, since the process industry is familiar with steam, it would be more willing to accept it as a nuclear heat transport medium than a foreign transport medium. Its main disadvantages lies in its corrosiveness and in the possible inability to recover the heat of condensation of the water in the product.

Even though steam was used in this study, consideration was given to using air or inert gases as the heat transport medium. These gases are advantageous because corrosion problems can be minimized; this single advantage should not be underrated. However, the use of gases would necessitate compressing them to reaction pressure and, if a closed cycle were used, for recycling them through the reactor loop. Inert gases would probably be too expensive for an open cycle system; also, the inert gas or the nitrogen contained in air (if air were used) would have to be removed from the product gas, presumably by low temperature fractionation. The volume of product gases would be much greater than that of the air fed to an oxygen plant, and one incentive for attempting nuclear heating would

be destroyed. Air, per sec, would be an inexpensive, open cycle medium, but the chemical energy released when the fuel burns with the contained oxygen would be large compared to the sensible heat that could be imparted to the gas so that only a minor fraction of nuclear heat could be delivered. This brings out the thought that one may be able to successfully combine nuclear and chemical heat by using nuclear heat up to some intermediate temperature and then adding a small amount of oxygen to elevate the temperature to the desired higher temperature level. Since this is merely a problem of optimizing nuclear and chemical energy, it was not considered in this study.

Consideration was also given to using synthesis gas from the coal gasification step as a heat transport medium. Compressors would be required to recycle the gas, a gas separation process would have to be added to remove the product CO and H_2 from the CO_2 and, since CO_2 is also a reaction product, the gasification reaction would be suppressed. This would increase the heat required in the gasification step because the amount of the $CO + H_2O \rightarrow CO_2 + H_2 + 17,700 \text{ Btu/lb.-mol}$ reaction would be decreased. Because of the required gas compression and the required volume of equipment, and since using synthesis gas as the heat transport medium would not be applicable to other chemical processes, this approach was abandoned.

The use of a gaseous heat transport medium presents one difficulty not yet mentioned, that is, the increased size of reaction equipment required. If any portion of a gaseous process must be operated at elevated pressures, it is desirable to operate the entire process at the highest practical pressure. In cases where a reactant is either a liquid or solid, the work of compression can be materially reduced. Latest coal gasification

technology has been directed toward increasing gasifier pressure from 45 to 370 psia. (2) The CO_2 scrubbers operate better at 370 psia, and virtually all reactions in which synthesis gas is used take place above 370 psia. While it has been assumed that the reactor can produce high temperature, pressurized (430 psia) steam without difficulty, the material balance (Table 4.2) shows that when coal is gasified by steam the volume of gases leaving the gasifier before the steam is condensed is increased by a factor of four over the volume of gases produced by oxygen gasification. Therefore, steam coal gasification may increase the size of coal gasifier by a factor of four. As long as the high temperature heat is delivered to a process by a gaseous heat transport medium the chemical equipment size will probably be large.

Coal Hydrogenation Plant Using Oxygen-Coal Gasification

Figure 4.1 is a flow sheet of a 30,000 bbl/day coal hydrogenation plant using oxygen coal gasification. (1) Dried coal is fluidized with 98.7% oxygen and blown into the coal gasification units along with the stoichiometric amount of steam. The reaction proceeds smoothly at 2240°F . The hot gases are withdrawn, cooled to 805°F . in a waste heat boiler and the entrained dust removed. The gas steam is then divided, 2630 MSCFH going to the Hydrocarbon-Cracking Plant as fuel gas and the remainder going to a Water-Gas Shift Plant.

The Water-Gas Shift Plant converts the CO to CO_2 and H_2 and removes the CO_2 by scrubbing with water at 370 psia. The reactions are as follows:
 Water-Gas Shift reaction: $\text{CO} + \text{H}_2\text{O} \xrightarrow{800-900^\circ\text{F}} \text{CO}_2 + \text{H}_2 + 17,700 \text{ Btu/lb.-mol}$
 @ 25°C . Fe catalyst

CO_2 removal reaction: $\text{CO}_2 + \text{H}_2\text{O} \xrightarrow{(1) \text{ 370 psia}} \text{H}_2\text{CO}_3 \text{ (in solution)}$

Since the reaction is exothermic, some heat can be removed from this.

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Fig. 4.1. 30,000 bbl/day Coal-Hydrogenation Plant Using Oxygen Coal Gasification.

plant. The product hydrogen for coal hydrogenation for this size plant amounts to 3,660 MSCFH.

The Hydrocarbon Cracking Plant produces synthesis gas by cracking light hydrocarbon gases with heat supplied by burning the 2630 MSCFH of synthesis gas with air. The cracking reactions are:

After the waste heat is recovered and the CO shifted, the product hydrogen amounts to 4,870 MSCFH.

The two hydrogen streams join and are used to hydrogenate 225 tons/hr. of coal, producing 30,000 bbl/day of liquid petroleum products.

Coal Hydrogenation Plant Using Nuclear Steam Coal Gasification

Figure 4.2 is a flow sheet of the same 30,000 bbl/day coal hydrogenation plant using high temperature steam heated in a nuclear reactor to gasify the coal. While this flow sheet appears more complicated than Figure 4.1 the only new equipment that has been added to the plant is the nuclear reactor and the regenerative heat exchanger. Heat recovery is important in this process and the extra detail shows how the recovery could be accomplished.

Dried coal is preheated to 600°F., its softening point, and sent to the gasifiers. Steam is collected from the waste heat boilers and from the turbines of the power plant. The total steam stream is heated to 2040°F. in a regenerative heat exchanger, and the 2040°F. steam is further heated to

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Fig. 4.2. 30,000 bbl/day Coal-Hydrogenation Plant Using Nuclear Steam Coal Gasification.

3000°F. in the high temperature portion of the nuclear reactors. The hot steam is sent into the coal gasifiers where it gives its sensible heat to the reaction and reacts with the coal. The coal gasifier operates at an exit temperature of 2240°F. and 420 psia, the pressure being 50 psi higher than in the oxygen coal gasification process to account for the pressure drop across the regenerative heat exchanger and boiler feed water exchangers.

The regenerative heat exchangers consist of a bank of ten ceramic lined vessels 5 feet in diameter by 30 feet long, packed with a ceramic packing. Their operation is cyclic. The hot cycle consists of passing the hot gas from the gasifiers through the cool ceramic packing for about two minutes. The packing heats to near the gas inlet temperature; the hot gas is then diverted to another exchanger and the cool 450 psia steam is introduced into the bed countercurrently. The cold cycle lasts about $2\frac{1}{2}$ minutes during which the ceramic gives its heat up to the steam. Allowing one-half minute for cycle changing, the entire cycle lasts five minutes. This type of exchanger will operate at temperatures considerably higher than 2240°F. and has found commercial application in the United States. (3)

The hot synthesis gas which has been cooled to an average temperature of 1045°F. in the regenerative heat exchanger, is sent through a waste heat boiler where it is further cooled to 805°F. and then through dust removal equipment. A portion of the stream, 35%, is then sent to a water-gas plant, the heat recovered, and the CO₂ removed, producing 2020 MSCFH of product hydrogen. The remaining 65% of the stream is cooled to its dew point, 400°F., and again split. One stream, 37% of the grand total, is cooled to 100°F. to condense out the water and the resulting 2630 MSCFH of

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gas used as fuel gas for the hydrocarbon-cracking plant. The remaining 122 tons/hr. of gas contains 88.5 tons/hr. of steam and is substituted for the 88.5 tons/hr of steam fed to the hydrocarbon-cracking plant on the oxygen-coal gasification flow sheet. This substitution merely enables the exchange of steam contaminated with synthesis gases for uncontaminated steam which may be fed to the nuclear reactor. (In normal practice, this steam is used internally by the hydrocarbon-cracking plant.) This substitution also helps to alleviate the heat recovery problem explained later. The hydrogen leaving the hydrocarbon-cracking plant amounts to 6510 MSCFH, 1640 MSCFH having come in with the steam, and 4870 MSCFH being produced from the light hydrocarbon gas.

The total hydrogen produced is 8530 MSCFH. The coal hydrogenation and distillation plants remain unchanged.

Coal Gasification Material Balances

Table 4.1 shows the material balances for the gasification of coal by oxygen and steam. The basic flows and compositions for the oxygen coal gasification step were obtained from the Bureau of Mines Report (1). The subject report tabulates basic data for several types of coal. Since "Wyoming bituminous" coal was selected as their base case, it was used exclusively in this report. Its composition is shown in Table 4.2 The type of coal used would have little effect on this study.

The equilibrium constant for the reaction given below was obtained from the literature (4):

It should be noted that steam is a reactant in this equation, and, therefore, the use of steam as the heat transport medium drives the reaction

TABLE 4.1

Material Balances

OXYGEN COAL GASIFICATION PLANT MATERIAL BALANCE

	Wyoming Bit. Coal As Purch. Tons/hr.	Coal as Fed to Gasifier Tons/hr.	Oxygen Tons/hr.	MSCFH	Steam 350 psig. Saturated Tons/hr.	Vaporized Scrubbing Water Tons/hr.	Residue Tons/hr.	Total Synthesis Gas			Fuel Gas to Hydrocarbon Cracking Plant MSCFH	Product Hydrogen to Coal Hydrogenation	
								Tons/hr.	MSCFH	Vol. %		MSCFH	Vol. %
<u>Total</u>	<u>102.3</u>	<u>95.5</u>	<u>79.3</u>	<u>1952</u>	<u>14.8</u>	<u>1.1</u>	<u>5.3</u>	<u>185.4</u>	<u>7150</u>	<u>100.0%</u>	<u>2630</u>	<u>3660</u>	<u>100.0%</u>
<u>Mat. Coal</u>	<u>86.0</u>	<u>86.0</u>											
H ₂	4.7	4.7	-	-	-	-	-	5.4	2118	29.6	779	3440	94.0
C	68.5	68.5	-	-	-	-	2.6	-	-	-	-	-	-
N ₂	1.5	1.5	1.0	28	-	-	-	2.5	70	1.0	26	44	1.2
O ₂	10.5	10.5	78.3	1924	-	-	-	-	-	-	-	-	-
S	.8	.8	-	-	-	-	.1	-	-	-	-	-	-
H ₂ O	13.6	6.9	-	-	14.8	1.1	-	14.4	630	8.8	232	-	-
Ash	2.7	2.6	-	-	-	-	2.6	-	-	-	-	-	-
CO ₂	-	-	-	-	-	-	-	25.4	454	6.4	167	37	1.0
CO	-	-	-	-	-	-	-	136.1	3820	53.4	1405	113	3.1
CH ₄	-	-	-	-	-	-	-	0.8	41	0.6	15	26	0.7
H ₂ S	-	-	-	-	-	-	-	0.8	17	0.2	6	-	-

NUCLEAR STEAM COAL GASIFICATION MATERIAL BALANCE

	Wyoming Bit. Coal As Purch. Tons/hr.	Coal as Fed to Gasifier Tons/hr.	Steam 3000°F. 430 psia. Tons/hr.	Residue Tons/hr.	Hot Wet Synthesis Gas Tons/hr.	MSCFH	Water Cond. From Synthesis Gas Tons/hr.	Total Synthesis Gas			Fuel Gas to Hydrocarbon Cracking Plant MSCFH	Product Hydrogen to Coal Hydrogenation	
								Tons/hr.	MSCFH	Vol. %		MSCFH	Vol. %
<u>Total</u>	<u>53.8</u>	<u>50.2</u>	<u>384</u>	<u>2.8</u>	<u>431.4</u>	<u>20170</u>	<u>298.5</u>	<u>132.9</u>	<u>7150</u>	<u>100.0%</u>	<u>2630</u>	<u>3660</u>	<u>100.0%</u>
<u>Mat. Coal</u>	<u>45.2</u>	<u>45.2</u>											
H ₂	2.5	2.5	-	-	10.8	4223	-	10.8	4223	59.1	1553	3560	97.3
C	36.0	36.0	-	1.4	-	-	-	-	-	-	-	-	-
N ₂	0.8	0.8	-	-	0.8	22	-	0.8	22	.3	8	14	0.4
O ₂	5.5	5.5	-	-	-	-	-	-	-	-	-	-	-
S	0.4	0.4	-	-	-	-	-	-	-	-	-	-	-
H ₂ O	7.1	3.6	384	-	312.9	13650	298.5	14.4	630	8.8	232	-	-
Ash	1.5	1.4	-	1.4	-	-	-	-	-	-	-	-	-
CO ₂	-	-	-	-	70.9	1267	-	70.9	1267	17.7	466	34	0.9
CO	-	-	-	-	35.5	998	-	35.6	990	14.0	367	52	1.4
H ₂ S	-	-	-	-	0.4	10	-	0.4	10	0.1	4	-	-

TABLE 4.2

Composition of Coal Gasified

Type of Coal: Wyoming Bituminous
Price: \$3.56/ton, at mine entrance

	<u>As</u>	<u>Dried Coal</u>
	<u>Purchased</u>	<u>As Fed to Gasifier</u>
	<u>100.0 Wt. %</u>	<u>100.0 Wt %</u>
MAF COAL	<u>84.1</u>	<u>90.1</u>
H ₂	4.6	4.9
C	66.9	71.8
N ₂	1.5	1.6
O ₂	10.3	11.0
S	.8	.8
H ₂ O	13.3	7.2
Ash	2.6	2.7

to the right. Addition of steam also tends to reduce the endothermic heat required for the gasification reaction. ($C + H_2O \longrightarrow CO + H_2 - 56,500 \text{ Btu/lb.-mol}$). The use of a reactant as a heat transport medium, therefore, has the advantage of driving the reaction to completion.

This advantage is also present in another reaction which occurs in coal gasification, namely:

In oxygen coal-gasification process, the presence of CO and H results in an equilibrium concentration of methane being formed. While methane is not particularly harmful, it is an impurity and contributes to the light hydrocarbon gas which must be separated by low temperature fractionation. The balance of the excess steam in the nuclear gasification material balance is sufficient to free the synthesis gas of any detectable methane. The gasification of coal by steam is also advantageous in that less coal is consumed and therefore less sulphur appears in the synthesis gas. Sulphur is particularly objectionable in the hydrogenation plant.

Examination of the gas compositions tabulated on the material balance show that the steam gasification yields a gas 3.3% richer in hydrogen than obtained by oxygen gasification. No credit for this was taken in this study. The nuclear steam coal gasification material balance was calculated integrally with the heat balance because of the effect of the steam on the reaction and also on the heat requirements.

Steam Coal Gasification Heat Balance

The heat balance for the nuclear steam synthesis gas plant was calculated using Bureau of Standards heats of formation and heat content

data as tabulated in the "Chemical Engineers Handbook" (5), "Gas Tables" (6), and "Steam Tables" (7). Since the material and heat balances depended upon one another, the balances were solved by letting the amount of coal, steam, and carbon monoxide in the product gas be unknown and solving the cubic equation resulting by satisfying the chemical equilibrium relations, material balance and heat balance. A 2% heat loss was allowed in the gasifiers and transfer lines. The calculations are straightforward, but rather involved; therefore they are not presented here. The significant numbers are shown on the flow sheet, Figure 4.2, and are reflected in the plant heat balance discussed later.

The endothermic heat of reaction can be calculated from the steam flow, as follows:

Enthalpy of steam at 3000°F. = 33,839 Btu/lb.-mol

Enthalpy of steam at 2240°F. = 24,954 Btu/lb.-mol

Enthalpy given to endothermic reaction = 8,885 Btu/lb.-mol

Steam flow rate = 384 tons/hr.

Endothermic heat of reaction = $\frac{384 \times 2000}{18.02} \times 8,885 \times 10^{-6} = 379 \text{ MMBTU/hr.}$

The effect of steam temperature on steam fed to the gasifier was then calculated by assuming that the heat of reaction remained constant. This assumption is very good since a large excess of steam is already present and, therefore, the amount of CO₂ in the product will not change appreciably.

Pounds of steam required per pound of coal fed to gasifier =

$$\frac{379 \times 10^6}{50.2 \times 2000} \times \frac{18.02}{(H_T - H_{2240})}$$

where $(H_T - H_{2240})$ stands for the difference in steam enthalpy in Btu/lb.-mol between the hot steam temperature and 2240°F. The results have been

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plotted on Figure 4.3. The desirability of going to high temperatures to lower the quantity of steam required is obvious.

The heat load of the nuclear reactor for heating steam to 3000°F. can be calculated as follows:

Enthalpy of steam at 3000°F. = 33,839 Btu/lb.-mol

Enthalpy of steam at 2040°F. = 22,732 Btu/lb.-mol

Enthalpy given to steam = 11,107 Btu/lb.-mol

Assume a 1% thermal heat loss in the nuclear reactor.

Total heat load of high temperature zone of reactor =

$$\frac{384 \times 2000}{18.02} \times 11,107 \times 1.01 \times 10^{-6} = 478 \text{ MMBtu/hr. or } 140 \text{ MW.}$$

The enthalpy of the hot wet synthesis gas leaving the coal gasifier was calculated at several temperatures and the curve of total enthalpy versus temperature is shown in Figure 4.4. This curve enables a rapid determination of the heat given up to the regenerative heat exchanger and waste heat boiler. A similar curve, Figure 4.5, was plotted for the hydrogen product stream and the fuel gas stream. The enthalpy of the gases in the fuel gas stream includes the enthalpy of gas which goes to the hydrocarbon cracking plant; the enthalpy of the condensate includes only the portion passing through the condensing heat exchanger. These curves permit rapid evaluation of any heat recovery system.

Permissible Cost of High Temperature Heat

Once the material and heat balance are calculated, the value as determined by dollars presently spent for high temperature heat divided by reactor high temperature heat load can be determined. This will be termed the permissible cost which can be assigned to the nuclear reactor.

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FIGURE 4.3

HIGH TEMPERATURE STEAM REQUIRED
VERSUS TEMPERATURE OF STEAM

ENDOTHERMIC HEAT OF REACTION = 3,760 BTU/LB
COAL FED TO GASIFIER

COAL AS FED CONTAINS 71.8 WT. % CARBON

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Comparison of the two material balances shows that 48.5 tons/hr. (102.3 - 53.8) of coal and 79.3 tons/hr. of oxygen are saved, and that 369 tons/hr. (384 - 14.8) of additional water are used. The cost of coal, \$3.56/ton, was taken from the reference Bureau of Mines report (1) and is the cost at the mine entrance. The cost of oxygen, \$6.00/ton, was an average value taken from industrial literature and includes all plant costs of manufacturing oxygen by low temperature fractionation of air. The cost of boiler feed water, \$0.25/M-gal., was taken from industrial experience. Since the above are all plant costs they do not include allowances from either administrative expense or profit.

Assuming that all heat contained in the synthesis can be recovered down to 100°F. and that the boiler feed water is delivered at 100°F., the savings can be calculated as follows:

	<u>\$/hr.</u>
Coal savings = 48.5 x 3.56	172.7
Oxygen savings = 79.3 x 6.00	475.8
Less cost of boiler feed water = $\frac{369 \times 2000}{8.33 \times 1000} \times 0.25 =$	<u>22.2</u>
Total savings	<u>\$626.3/hr.</u>
Reactor heat load	475 MMBtu/hr.
<u>Permissible cost of high temperature heat</u> = $\frac{626.3}{475} =$	

$$= \$1.32/\text{MMBtu for } 3000^{\circ}\text{F. steam.}$$

The effect of steam temperature on the permissible cost of the heat can be calculated by using Figure 4.3 (which shows steam requirements versus temperature), and calculating the reactor heat load for the steam flow

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required. The results of such a calculation are shown in Figure 4.6. The curve is fairly level until 2600°F., below which the permissible cost drops rapidly since the heat content of the gasifier product gas is larger than the energy required for heating the boiler feed water and the extra heat must be dumped. In this design, heating of the boiler feed water provides a large heat sink for recovering the heat contained in the gasifier product gases. This is a result of obtaining the high temperature heat by gamma-ray and neutron heating and using the locally deposited fission energy to produce power. Consequently, 100% of the heat in the product gases can be recovered at temperatures of practical interest.

Permissible Cost of High Temperature Heat With Ceramic Fuel Elements Bearing a Fissionable Material

If a ceramic bearing fuel were used, 100% of the fission energy would be obtained as high temperature heat and the nuclear reactor would not be part of the power plant. The large heat sink is thereby eliminated and this is reflected in the curve in Figure 4.6. Waste heat boilers can produce only a portion of the steam required because the pressure of the steam entering the regenerative heat exchanger would be at a higher pressure than the pressure of steam leaving the gasifier; consequently its boiling point would be higher than the dew point of the condensing steam and the exchanger ΔT would "pinch". The percent heat recovery can be approximated as follows:

Boiling point of 450 psia steam = 456°F.

Assume waste heat boilers are designed for 20°F. approach to boiling point.

FIGURE 4.6
EFFECT OF STEAM TEMPERATURE
PRODUCED BY NUCLEAR REACTOR
ON VALUE OF HIGH TEMPERATURE HEAT

Heat in hot wet synthesis gas down to 476°F. (See Figures 4.4 and 4.5) = (115 - 5) + (388 - 295) = (318 - 255) = 266 MMBtu/hr.

Heat required to raise reaction steam to boiling point = 384 x 2000 x (456 - 100) x 10⁻⁶ = 273 MMBtu/hr.

Heat in synthesis below 476°F. = 295 + 255 = 550 MMBtu/hr.

Heat recovery efficiency = $\frac{266 + 273}{266 + 550} \times 100\% = 66\%$.

This means that 130 tons/hr. (384 - 0.66 x 384) of 450 psia steam would have to be purchased from a power plant. Assuming that this steam cost \$0.40/M-lb. (an inexpensive steam cost for a large coal-fired boiler plant), the value of the high temperature reactor heat would be:

	<u>\$/hr.</u>
Coal savings = 48.5 x 3.56	172.3
Oxygen savings = 79.3 x 6.00	475.8
Less cost of boiler feed water =	
= $\frac{(384 - 130 - 14.8) \times 2000}{8.33 \times 1000} \times 0.25$	= -14.4
Less cost of steam = 130 x $\frac{2000}{1000}$ x 0.40	= 104.0
Total savings	\$529.7/hr.
Reactor heat load	475 MMBtu/hr.
Value of high temperature heat = $\frac{529.7}{475}$	\$ 1.11/MMBtu

A heat recovery calculation for the steam used above or below 384 tons/hr. shows the following incremental efficiency:

Enthalpy of steam at 1045°F. and 375 psia	=	1547.2 Btu/lb.
Enthalpy of steam at 476°F. and 375 psia	=	<u>1232.1 Btu/lb.</u>
Superheat recoverable from steam	=	315.1 Btu/lb.

Heat required to raise water from 100°F. to 456°F. = 368.8 Btu/lb.

Enthalpy of water at 100°F = 68.0 Btu/lb.

Heat recovery efficiency = $\frac{315.1 - 368.8}{1547.2 - 68.0} \times 100\% = 46.3\%$

Using these efficiencies and Figure 4.3, the effect of steam temperature on the permissible cost of the high temperature reactor heat can be calculated. The results of these calculations are shown in Figure 4.6. The permissible cost values lie below those for heat obtained by gamma ray and neutron heating and the curve has a steeper slope; however, this effect comes into play only if the nuclear plant were divorced from the power plant.

Choice of Steam Temperature

With the above data on steam requirements (Figure 4.3), the permissible cost of high temperature heat (Figure 4.6), and the physical properties of the possible ceramic nuclear moderators (Chapters V and IX), a choice of steam temperature and reactor outlet temperature could be made. A temperature of 3000°F. was chosen as the lowest temperature practical for delivering heat to the coal gasifiers. Figure 4.3 shows that the steam requirements rise fairly sharply below a temperature of 3000°F. and Figure 4.6 shows that the permissible cost of heat, if the fuel were in the ceramic, falls rapidly below 3000°F. Although the steam consumption could be lowered appreciably by going to 3200°F., physical property data on the ceramic moderators considered was listed up to only 2730°F. (1500°C). It was felt that further extrapolation was unjustified. While the strength of the ceramic moderators is low at 2700°F., it is encouraging that the stress to strength ratio decreases as the temperature

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increases (see Chapter V). It was assumed that this continued at the higher temperatures considered.

Regenerative Heat Exchanger Design

The design of the regenerative heat exchanger is important to the economy of the process. If the use of a regenerative heat exchanger were not possible, the nuclear reactor would have to heat the steam from 1050°F. to 3000°F. instead of 2040°F. to 3000°F. This would double the size of the nuclear plant and halve the permissible cost of the high temperature heat.

The exchanger was designed by assuming that a 200°F. temperature approach could be attained. A packing consisting of $\frac{1}{2}$ in. alumina Berl Saddles was chosen, on the basis that alumina has a low coefficient of thermal expansion enabling it to withstand thermal shock and that Berl Saddles have a very high heat transfer area per unit volume of bed and a low pressure drop per foot of bed length (8). It was also decided that 30 psi would be the maximum permissible pressure drop across the bed. The size of the exchanger was then determined using methods for calculating the viscosity and thermal conductivity (9), pressure drop (10), heat transfer coefficient (11), and size (12), found in the literature. The calculation process was one of successive iteration and showed that a bank of approximately ten exchangers 5 ft. in diameter by 30 ft. long would be required with a 2 minute heating cycle and $2\frac{1}{2}$ minute cooling cycle. Since these exchangers seemed fairly large and the cycle time short, it was decided not to attempt to design for a closer temperature approach.

The effect that the closeness of temperature approach has on the system and the permissible cost of high temperature heat is shown on

Figure 4.7. High temperature heat efficiency as used in this graph is the sensible heat of the steam above the reaction temperature, 2240°F. , divided by the sensible heat supplied to the steam by the nuclear reactor. The value of the heat is obtained by dividing the net coal and oxygen savings by the reactor heat load. The graph shows that at 200°F temperature approach the high temperature heat efficiency is 79.4%.

Coal Hydrogenation Plant Power Requirements

The total power and heat requirements for the 30,000 bbl/day coal hydrogenation plant are shown in Table 4.3. They were obtained from the Bureau of Mines report, R. I. 4564, (1) by eliminating the power consumed by the oxygen plant and leaving the power requirements of all other units the same. No attempt was made to evaluate the change in power requirements for the coal handling, coal gasification, or power plant, since these changes would not affect the design of the reactor. The process steam requirements were based on the steam required for nuclear gasification plus the process steam requirements of the coal hydrogenation plant after the requirements of the coal gasification and hydrocarbon cracking plants were adjusted. The amount of heat returned to the process plant for boiler feed water heating was based on recovery of 100% of the heat down to 100°F. in the product synthesis gas stream. The nuclear plant heat and power distribution is shown in Table 3.1.

Activation of Steam

The nuclear steam can acquire two types of activity, γ and β^{-} activity. Gamma activity results from the decay of O^{19} formed when the O^{18} naturally occurring in the steam captures a neutron and from the

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FIGURE 4.7

EFFECT OF REGENERATIVE HEAT EXCHANGER
APPROACH TEMPERATURE ON VALUE
OF HIGH TEMPERATURE HEAT

TABLE 4.3

30,000 bbl/day Coal Hydrogenation Plant Power
and Heat Requirements

POWER REQUIREMENTS

	<u>Horsepower</u>	<u>M. W. -Elect.</u>
Low-temperature off-gas separation	10,000	7
Hydrocarbon-steam cracking	15,600	12
Coal gasification	16,970	13
Hydrogen compression and purification	75,420	56
Coal hydrogenation section	73,360	55
General and auxiliary plant section	<u>34,020</u>	<u>25</u>
Total Power Consumption	225,370	168

HEAT REQUIREMENTS

<u>Process Steam Requirements</u>	<u>M-lbs/Hr.</u>	<u>M.W. -Heat</u>
450 psia. (a) 700°F.	514	199
370 psia. (a) 490°F.	106	37
45 psia. saturated	<u>360</u>	<u>120</u>
Gross Heat Consumption	980	356

<u>Heat Returned to Power Plant</u>	<u>MM-Btu/Hr.</u>	<u>M.W. -Heat</u>
Hydrogen product stream		
890°F. to dew point, 395°F.	75	22
395°F. to 100°F.	243	71
Synthesis gas stream		
850°F. to dew point, 400°F.	113	33
400°F. to 100°F.	<u>274</u>	<u>81</u>
Total Heat Returned	705	<u>207</u>
NET HEAT CONSUMPTION		149 MW- Heat

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decay of N^{16} formed when the N^{15} naturally occurring in the air entrained in the steam captures a neutron. Beta activity results from the decay of O^{19} and H^3 formed when the H^2 naturally occurring in the steam captures a neutron. The magnitude of these activities is calculated below:

Atomic density of hydrogen in steam = 6.25×10^{19} atoms/cc.

ϕ_{Th} , thermal flux = 0.5×10^{14} atoms/cm²-sec.

ϕ_I , intermediate flux = 3×10^{14} atoms/cm²-sec.

1 curie = 3.7×10^{10} disintegrations/sec.

Length of reactor = 12 feet

Velocity of fuel through reactor = 17.4 ft./sec.

Retention time in reactor = $12/17.4 = 0.69$ seconds.

γ activity from O^{19} :

Natural abundance of O^{18} in oxygen = 0.204%

O^{18}

σ act at thermal energy, 0.14 ev. = 89×10^{-6} barns

O^{18}

σ act at average intermediate energy, 0.60 ev. = 43×10^{-6} barns

Half life of O^{19} = 29 seconds giving off 1.6 Mev. gamma ray

70% of time.

γ activity = $\frac{6.25}{2} \times 10^{19} \times 0.00204 \times [(0.5 \times 0.89 \times 89) + (3 \times 43)] \times$

$\times 10^{14} \times 10^{-6} \times 10^{-24} \times 0.69 \times \frac{0.693}{29} \times$

$\times \frac{1}{0.70 \times 3.7 \times 10^{10}} \times \frac{2}{6.25} \times 10^{-19} \times 0.6023 \times$

$\times 10^{24} \times 454 = 3 \times 10^{-3}$ curies/lb. of steam at exit of reactor.

Y activity from N^{16} :

It is assumed that the boiler feed water is deaerated down to 50 ppm.

Natural abundance of N^{15} in nitrogen = 0.365%.

N^{15}

σ act at thermal energy, 0.14 ev. = 10×10^{-6} barns

N^{15}

σ act at average intermediate energy, 0.60 ev. = 4.9×10^{-6} barns

Half life of N^{16} = 7.4 seconds giving off 6.13 Mev. gamma ray.

Y activity from N^{16} = $\frac{6.25}{2} \times 10^{19} \times 50 \times 10^{-6} \times 0.00365 \times$

$\left[(0.5 \times 0.89 \times 10) + (3 \times 4.9) \right] \times 10^{14} \times 10^{-6} \times 10^{-24} \times 0.69 \times$

$\times \frac{0.693}{7.4} \times \frac{1}{3.7 \times 10^{10}} \times \frac{2}{6.25} \times 10^{-19} \times 0.6023 \times 10^{24} \times 454 =$

= 0.2×10^{-6} curies/lb. of steam at exit of reactor.

The above calculations show that the activity from O^{19} which produces a 1.6 Mev. gamma ray is very low and would be easy to shield. The pipe and vessel walls would afford adequate shielding. The activity from the air entrained in the steam would be completely negligible.

β^- activity from O^{19} :

Half life of O^{19} = 29 seconds giving off 2.9 Mev. beta ray 70% of time and 4.5 Mev. beta ray 30% of time.

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The calculation is the same as for γ activity from O^{19} except that the 70% factor is omitted; therefore, the combined β^- activity from $O^{19} = 4 \times 10^{-3}$ curies/lb. of steam at exit of reactor.

β^- activity from H^3 :

Natural abundance of H^2 in hydrogen = 0.015%

H^2

σ act at thermal energy, 0.14 ev. = 240×10^{-6} barns.

H^2

σ act at average intermediate energy, 0.60 ev. = 116×10^{-6} barns.

Half life of $H^3 = 12.4$ years giving off 0.0189 Mev. beta ray.

$$\begin{aligned} \beta^- \text{ activity} &= 6.25 \times 10^{19} \times 0.00015 \times [(0.5 \times 0.89 \times 240) + (3 \times 116)] \times \\ &\times 10^{14} \times 10^{-6} \times 10^{-24} \times 0.69 \times \frac{0.693}{12.4 \times 365 \times 24 \times 3600} \times \frac{1}{3.7 \times 10^{10}} \times \\ &\times \frac{2}{6.25} \times 10^{-19} \times 0.6023 \times 10^{24} \times 454 = 0.00012 \times 10^{-6} \text{ curies/lb.} \end{aligned}$$

of steam throughout the entire system.

The calculations show that the β^- activity is also negligibly small. In addition, any beta activity would be entirely stopped by the piping and vessel walls. In addition, the half life of the most appreciable activity, O^{19} is short enough that it would completely die out a few minutes after the reactor was shut down.

The only other source of radioactivity might come from boiler scale entrained in the steam or a rupture in the fuel pipe and a leakage of fuel into the steam. Most of the entrained boiler scale would be dropped out

in the steam plenum chamber below the reactor, or caught in the regenerative heat exchanger packing. A ruptured fuel element is protected against to a certain extent by maintaining the steam pressure higher than the fuel pressure so that a leak would leak steam into the fuel rather than fuel into the steam. Even if fuel should leak into the steam, there are two places where the solids or liquids would be removed. The regenerative heat exchanger presents a large surface area to the gas so that any solids or liquids would tend to accumulate on the packing material. The dust removal apparatus also provides an excellent removal system for solids. The dust removal system takes all ash out of the hot gas before sending the gas to the water gas shift plant where dust would poison the catalyst. Any gaseous fission products would be carried down through the system, and eventually decay to cadmium. This would not be serious.

Conclusions

Present costs for high temperature process heat obtained by burning a fuel in oxygen are in the vicinity of \$1.32/MMBtu, high enough to provide a possible attractive market for high temperature heat produced by nuclear reactors. The integration of a high temperature nuclear reactor with a coal gasification plant offered no unusual technical difficulties; however, it was found that a very efficient heat recovery system was necessary if the high value of high temperature nuclear reactor heat was to be preserved. The nuclear steam coal gasification plant was somewhat larger than the oxygen coal gasification plant, largely because it was necessary to use regenerative heat exchangers to pre-superheat the steam to be super-heated in the reactor. On the other hand, nuclear steam gasification of coal produces a purer synthesis gas; methane contamination is eliminated, and

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sulphur and nitrogen contamination cut in half. The steam reactor coolant used in this study gained very little radioactivity and the little that was acquired would be entirely shielded by the normal piping and equipment.

Economic considerations showed that the obtaining of total fission energy as high temperature heat would be preferable to gamma ray and neutron heating; also, the use of fission heat to supply chemical reactor heat directly would seem even more attractive. For example, if powdered coal were blown through the reactor, the reactor would operate at only a few degrees above reaction temperature, where material properties are more favorable and the flux spectrum more thermal. In addition, the heat would be transferred at 100% efficiency. Lastly, in the gasification of coal, the equipment required for the plant could be materially reduced over that presented in this report. Present coal gasifiers cost approximately \$100/KW of heat released in the gasifier. This cost could be used to build a major portion of the nuclear reactor and greatly reduce the additional amortization usually incurred with the use of neutronic devices.

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ENCLOSURE

CHAPTER V

ENGINEERING ASPECTS OF THE CERAMIC MODERATOR SYSTEM

Introduction

During the initial stages of the high temperature process heat reactor study it was decided that fuel-containing ceramics could not be incorporated in the unit, since experimental evidence indicates that diffusion of fission product gases through the ceramic at high temperature would be excessive. Ceramic materials are required since the study objective is to furnish steam for high temperature chemical processes at 3000°F, and present metallurgical technology does not offer any metals to withstand temperatures of this magnitude.

With the limitation that the ceramic cannot contain fuel, the energy available for the attainment of high temperatures is limited to that associated with gamma rays, fast neutrons and neutron captures. It was on this basis that various ceramics were investigated. Listed below are some of the criteria for selecting a proper ceramic and configuration to obtain the desired project objectives:

1. The ceramic must moderate neutrons adequately and have a low enough capture cross-section so that criticality may be obtained in a reactor of reasonable size and fuel concentration.
2. It must attenuate the gamma flux in a reasonable distance so that the sensible energy will be released in the reactor proper.
3. Preferably, the neutrons absorbed should yield high energy gammas or betas to aid heating.
4. The attenuation of radiation should be such that uniform generation would be realized, thus simplifying heat removal and decreasing thermal stresses.

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5. The ceramic should have temperature mechanical strength and have long life in a steam atmosphere.

6. Fabrication of elements should be simple and yet satisfy the heat removal requirements.

Long life in a steam atmosphere and the criticality requirement effectively eliminated all materials except zirconia (ZrO_2) and magnesia (MgO). These materials were evaluated according to the above criteria, and some of the results of the investigation are recorded in this report.

Summary

The problem of producing high temperature heat by means of neutron moderation and gamma absorption in the ceramic moderator has been investigated and the results are herein recorded. The moderator configuration proposed is basically a hexagonal ceramic brick with a central opening containing a 2 inch diameter fuel tube with a surrounding 1 inch thick annulus of low density insulation. Many (253) small holes in the unit ceramic brick cell carry the steam coolant which removes the heat. Thermal stress criteria are considered to control the coolant hole size. However, some compromise has to be made in order that heat transfer requirements may be realized without excessive pressure losses. It was found upon approximating each coolant hole as a hollow cylinder that zirconia would suffer excessive thermal stresses and fail. Magnesia moderator with $\frac{1}{4}$ inch diameter coolant holes on $\frac{3}{4}$ inch centers is the recommended material and configuration. Pressure stresses were assumed not to exist due to the porous nature of the ceramic.

In order to calculate the thermal stresses the internal heat generation rate distribution was obtained. At a reactor power of 400 MW an average

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heat generation rate of 1.6×10^5 Btu/hr ft³ was obtained with an axial peaking factor of 1.57 and a cell peaking factor of 2.3 for magnesia (3.6 for zirconia). This is based on the assumption that 9% of the fission energy is realized as heat in the ceramic (Table 5.3), and that the axial heat generation rate approximates a chopped cosine distribution (Figure 5.9) with a flat radial distribution. The resulting axial temperature distribution is found on Figure 5.10 where it can be seen that the maximum ceramic temperature is 3120°F three feet from the hot reactor exit. The exit steam temperature is 3074°F. The maximum thermal stresses are tangential and tensile in nature as can be observed from Figures 5.6 and 5.7. The maximum tangential stresses for various coolant hole configurations can be found on Figure 5.8.

The methods used for determining the heating in the cell are discussed in the sections entitled Gamma Heating and Neutron Heating. The gamma heating calculations for points in the cell assume a uniform source distribution in an infinite cylindrical source and are corrected for self absorption in the fuel and exponential attenuation in the Inconel tube wall. Prompt fission gammas; fission product decay gammas, and capture gammas from uranium, fuel poisons, inconel and moderator are included. It was found that the major source of energy from neutron captures came from capture gamma rays given off, which represent the equivalent binding energy of the neutron. The resulting decay gammas were investigated and found to contribute negligibly to the heat source; therefore, these were not included in this report.

The internal heating resulting from capture gammas in Inconel and in the moderator has been assumed to be uniform over the reactor and estimated to be 113 Btu/hr ft³ per megawatt. Heating from neutrons, prompt and

decay gammas and non-fission capture gammas as a function of distance from the center of a fuel tube can be found in Figures 5.2 and 5.3. To obtain the total heating at any point the contribution of surrounding fuel tubes must be considered. The internal heat generation along a line connecting two fuel tubes is shown on Figure 5.4 while Figure 5.5 gives the heat generation along a line extending from a fuel tube through the center of a unit triangular cell. The rapid attenuation of gammas in zirconia along with zirconia's adverse physical properties result in excessive thermal stresses.

The heat transfer and pressure drop problems were investigated for a configuration of $\frac{1}{4}$ inch coolant holes on $\frac{3}{4}$ inch centers and the results were quite favorable. It was found that each cell would have 253 coolant holes (10% void volume) and the steam velocity required to pass 96 tons steam/hr through the reactor would be about 40 ft/sec. Because of the low fluid density the pressure drop, including inlet and outlet losses of 1.5 velocity heads, would be only about 1 psi. The flow is turbulent and the heat transfer coefficient across the steam film would be $71.6 \text{ Btu/hr ft}^2 \text{ } ^\circ\text{F}$. The coolant cross sectional flow area would be $.086 \text{ ft}^2$ per cell, and the heat transfer area would be approximately 200 ft^2 per cell. With the steam entering at 2000°F , the exit temperature would be 3074°F for a reactor operating at 400 MW.

In further studies of this reactor, it is recommended that the attenuation of gammas born in the fuel tube be re-evaluated considering a 2 inch diameter source with two shields (the insulation and the ceramic) using the methods given in Reference 24.

In addition, the extent of the flux flattening caused by the fuel tubes extending beyond the reactor ends should be investigated as it will lower heat generation peaking and thermal stresses. Also, the distribution

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of capture gammas could be better determined by a Monte Carlo type calculation. Spectrum hardening should be investigated (an equilibrium spectrum was assumed here) as this would tend to flatten the heat generation rate in the unit cell with respect to the results given herein. The thermal stresses would then be somewhat lower than given here.

It is recommended that either or both of the following be incorporated in the design of the coolant flow pattern: (1) In each cell the density of coolant holes should be increased near the fuel tube and decreased in regions far from the fuel tubes. (2) The coolant flow should be so regulated that no radial heat flow would occur. Both suggestions would tend to lower any heat generation peaking and thus thermal stresses in the reactor.

Conclusion

It appears that useful high temperature heat may be produced in the reactor considered in this report. No insurmountable mechanical difficulties appear associated with using magnesia as the ceramic material. However, physical property data is rather limited at present and more would have to be obtained in order that more advanced analysis could be continued.

Thermal Stresses

As suggested, a possible ceramic configuration is that of hexagonal bricks roughly 7 inches across flats as can be seen on Figure 5.1 in half scale. For the initial investigation it was assumed that the element was iso-thermal except for the local temperature gradients around the coolant holes. Approximating the local area around a coolant hole as a hollow cylinder with uniform internal generation and with internal cooling, one obtains the following temperature distribution:

Let $2a$ = cylinder O.D. (ft)

2b = cylinder I.D. (ft)

$$T = T(r) - T(a) \text{ } ^\circ\text{F}$$

$$G = \text{internal generation rate (Btu/hr ft}^3\text{)}$$

$$K = \text{thermal conductivity (Btu/hr ft } ^\circ\text{F)}$$

The general transfer equation (steady state) was given by

$$G - K \nabla^2 T = 0 \quad (5.1)$$

In cylindrical coordinates with radial symmetry the following relation exists

$$\nabla^2 T = \frac{1}{r} \frac{\partial}{\partial r} \left(r \frac{\partial T}{\partial r} \right) \quad (5.2)$$

$$\int d\left(r \frac{\partial T}{\partial r}\right) = - \int \frac{G r}{K} dr$$

$$r \frac{\partial T}{\partial r} = - \frac{G r^2}{2K} + C$$

$$\frac{\partial T}{\partial r} = - \frac{Gr}{2K} + \frac{C}{r}$$

$$T = - \frac{Gr^2}{4K} + C \ln r + C' \quad (5.3)$$

The boundary conditions were taken as :

$$\left. \frac{dT}{dr} \right|_{r=b} = 0$$

$$T|_{r=a} = 0$$

Thus T was given by

$$T = \frac{G}{2K} \left[b^2 \ln \frac{r}{a} - \frac{1}{2} (r^2 - a^2) \right] \quad (5.4)$$

From references 1, 3, 4, and 6 one obtains

$$\sigma_r = \frac{\alpha E}{1-\nu} \left[\frac{1}{r^2} \right] \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b r T dr - \int_a^r r T dr \right] \quad (5.5)$$

$$\sigma_\theta = \frac{\alpha E}{1-\nu} \left[\frac{1}{r^2} \right] \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b r T dr + \int_a^r r T dr - Tr^2 \right] \quad (5.6)$$

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$$\sigma_z = \frac{\alpha E}{1-\nu} \left[\frac{2}{b^2 - a^2} \int_a^b r T dr - T \right] \quad (5.7)$$

where σ_r = radial stress (psi)
 σ_θ = tangential stress (psi)
 σ_z = longitudinal stress (psi)
 α = linear coefficient of expansion ($\frac{\text{in}}{\text{in}} \text{ } ^\circ\text{F}^{-1}$)
 ν = Poisson's ratio
 E = modulus of elasticity (psi)

Substituting the expression for temperature distribution in the above yielded the following results: ⁶

$$\sigma_r = \frac{E \alpha G}{16 K(1-\nu) r^2} \left[\left(\frac{r^2 - a^2}{b^2 - a^2} \right) (4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4) + 2b^2r^2 (1 - 2 \ln \frac{r}{a}) - 2a^2(b^2 + r^2) + r^4 + a^4 \right] \quad (5.8)$$

$$\sigma_\theta = \frac{E \alpha G}{16 K(1-\nu) r^2} \left[\left(\frac{r^2 + a^2}{b^2 - a^2} \right) (4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4) - 2b^2r^2 (1 + 2 \ln \frac{r}{a}) + 2a^2(b^2 - r^2) + 3r^4 - a^4 \right] \quad (5.9)$$

$$\sigma_z = \frac{-E \alpha G}{16 K(1-\nu)} \left[8b^2 \ln \frac{r}{a} - 4r^2 + 4a^2 - \frac{2}{b^2 - a^2} (4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4) \right] \quad (5.10)$$

To facilitate rapid calculations the following substitutions were made;

$$r = na \quad \text{and} \quad b = sa$$

Then,

$$\sigma_r = \frac{E \alpha G a^2}{16 K (1-\nu)} \frac{1}{n^2} \left[\frac{n^2-1}{s^2-1} (4s^4 \ln s - 3s^4 + 4s^2 - 1) + 2n^2 s^2 (1 - 2 \ln n) - 2(s^2 + n^2) + (n^4 + 1) \right] \quad (5.11)$$

$$\sigma_\theta = \frac{E \alpha G a^2}{16 K (1-\nu)} \frac{1}{n^2} \left[\frac{n^2+1}{s^2-1} (4s^4 \ln s - 3s^4 + 4s^2 - 1) - 2n^2 s^2 (1 - 2 \ln n) + 2(s^2 - n^2) + (3n^4 - 1) \right] \quad (5.12)$$

On Figures 5.6 and 5.7 the dimensionless quantity $\frac{16 K (1-\nu) \sigma}{E \alpha G a^2}$ versus n has been plotted for $s = 2$ and $s = 3$.

The physical properties of zirconia and magnesia for various temperatures are compiled in Table 5.1. The maximum thermal stresses in both materials at a temperature of 2500°F for various values of inside diameter have been investigated and the data is plotted on Figure 5.8. The plot is such that the stress can be found for any value of internal generation. Only the maximum tangential stress has been plotted since it is the largest thermal stress with this particular configuration. For comparison the maximum thermal stress which would occur in spherical pellets at this temperature have been included on the curve. For a pellet the dimension (a) would represent the sphere radius. The tangential stress in a spherical pellet with uniform internal generation is found to be

$$\sigma_\theta = \frac{\alpha E G}{15 K (1-\nu)} (2r^2 - a^2) \quad (5.13)$$

Therefore, the maximum stress would be where $r = a$, or;

$$\sigma_{\theta \max} = \frac{\alpha E G}{15 K (1-\nu)} a^2 \quad (5.14)$$

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Figure 5.8 reveals that for a given b/a ratio the thermal stresses in MgO are 4 to $4\frac{1}{2}$ times lower than in zirconia. Although zirconia has a larger tensile strength it is only on the order of 1.6 times larger at $2500^{\circ}F$. Therefore, for a given value of generation rate, magnesia would result in the least stress and the largest factor of safety.

It has not been discussed whether the approximation of hollow cylinders, internally cooled, is a reasonable initial approach to the problem of calculating thermal stresses about the coolant holes. Figures 5.6 and 5.7 indicate that the maximum tangential stress occurs at the inner radius and that the maximum radial stress occurs near the inner radius. Therefore, although the actual temperature distribution should be determined by a relaxation procedure, the results obtained by the approximation of hollow cylinders should not be greatly in error. If the maximum stress occurred at the outer radius and if it were tensile in nature, the relaxation of temperatures and stresses would be a necessity.

For the reactor under consideration, operating at 400 MW, there would be ninety 2 inch diameter fuel tubes and the axial length would be 12 feet. This is .370 megawatts per foot of length. The problem is to find how much of this one could realize as heat in the ceramic as a result of degrading the energy of the neutrons and gammas.

From information in the analysis section of this report the disposition of neutrons born in fission is assumed to be as shown in Table 5.2.

TABLE 5.1
PROPERTIES OF MAGNESIA

Temperature		K	α	E	G	TS	EL	ν
$^{\circ}\text{C}$	$^{\circ}\text{F}$		10^{-6}	10^6	10^6	10^3	10^3	
25	77		5.5	30.5		14.0		.35
30	86				11.2		12.1	.35
200	392		7.4	30.5		14.2		.35
400	752		7.4	30.0		15.2		.35
600	1112	1.9	8.2	29.5		16.0		.35
800	1472	1.7	8.3	27.5		14.2		.35
1000	1832	1.5	8.7	21.0	8.0	11.5	5.6	.35
1100	2012	1.5			5.6	10.0		
1200	2192	1.5	8.7	10.0	4.9	8.0		
1300	2372	1.5	8.7	4.0	3.3	6.0	.38	
1400	2552	1.5						
1500	2732							

References

- K = thermal conductivity (Btu/hr ft $^{\circ}\text{F}$) (10)
- α = coefficient of thermal expansion ($\frac{\text{in}}{\text{in}} ^{\circ}\text{F}^{-1}$) (12)
- E = modulus of elasticity (psi) (12)
- G = modulus of rigidity (psi) (13)
- ν = Poisson's ratio (12)
- TS = tensile strength (psi) (12)
- EL = elastic limit (psi) (13)

TABLE 5.1 (cont'd)
PROPERTIES OF ZIRCONIA

Temperature		K	α	E	TS	CS	ν
$^{\circ}\text{C}$	$^{\circ}\text{F}$		10^{-6}	10^6	10^3	10^3	
25	77		3.7	21.5	20.0	300.	.29
200	392		3.7	20.3	16.8		.29
400	752		5.9	20.1	17.5		.29
600	1112	.45	6.1	19.8	17.6		.29
800	1472	.48	5.4	22.0	16.0		.29
1000	1832	.52	6.7	19.9	14.8	171.	.29
1100	2012	.55			13.5		
1200	2192	.56	7.2	16.0	12.1	114.	
1300	2372	.63	5.0	11.8	10.2		
1400	2552	.65				18.5	
1500	2732					2.8	

CS = compressive strength (psi)

TABLE 5.2

DISPOSITION OF NEUTRONS BORN IN FISSION

1.0	fission capture
.3	non-fission capture in uranium
.13	poison capture (10% of U captures)
.32	leakage
.3	Inconel pipe wall capture
.4	moderator captures
.01	other fuel captures
<u>2.46</u>	neutrons per fission in U-235

From one fission about 7.5 Mev are released as gammas, and about 67% of this energy escapes the fuel tube (discussed later in Gamma Heating). At the Nuclear Cross Section Advisory Group Meeting at Argonne Laboratory on June 8, 1956 it was revealed that $3.6 \pm .9$ Mev per fission were given off in fission product decay gammas in the first 1600 micro-seconds. An additional 4.8 ± 1 Mev is given off over a period of hours. Thus of the energy available only 67% of 3.6 Mev would be deposited in the ceramic. Reference 16 suggests that 5 Mev are given off for every non-fission capture in uranium. Table 5.3 lists the remaining assumptions made with regard to capture gamma energies (see Refs. 17 and 18).

TABLE 5.3

HEAT SOURCES IN CERAMIC

Source of Gamma	Energy Available	Interactions per fission	%Absorbed In Ceramic	Energy (Mev)
Prompt Fission	7.5	1.0	67	5.02
Fission Product	3.6	1.0	63	2.28
Non Fission Capture	5.0	.3	67	1.00

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Poison Capture	7.0	.13	67	.61
Inconel Capture	8.0	.3	95	2.28
Moderator Capture	7.6	.44	97.5	2.96
Neutron Moderation (Not gamma source)	5.0		77	<u>3.85</u> 18.00 Mev

Near the center of the reactor, where essentially all gammas entering the ceramic are absorbed approximately 9% of the power output would be realized as heat in the ceramic moderator. If a power level of 400 MW per reactor is assumed a uniform generation rate of 1.6×10^5 Btu/hr ft³ is realized. At 2500°F, for $\frac{1}{2}$ inch and $\frac{1}{4}$ inch diameter holes, this can be shown to result in maximum stresses as follows:

TABLE 5.4
MAXIMUM STRESSES AT 2500°F

	$\frac{1}{2}$ Inch Diameter		$\frac{1}{4}$ Inch Diameter		TS
	<u>b/a = 2</u>	<u>b/a = 3</u>	<u>b/a = 2</u>	<u>b/a = 3</u>	
MgO	540 psi	2,700 psi	135 psi	675 psi	4500
ZrO ₂	2300 psi	12,300 psi	560 psi	2,460 psi	9000

TS = tensile strength at 2500°F

In all cases except one it is seen that the tensile strength is greater than the maximum thermal stress, and therefore, no difficulty appears to exist. However, the assumption of uniform generation is a false one. The flux in the radial direction is essentially flattened by the reflector. However, in each unit cell (which includes a fuel tube and surrounding ceramic) there is a generation peaking factor of approximately 2.3 maximum to average for MgO and 3.6 maximum to average for ZrO₂. Assuming a cosine distribution in the

axial direction (with extrapolation distances of 2 cm at each end) one finds that the ratio of maximum to average is about 1.57. Actually the ratio would be much less, because the tubes extending beyond the reactor ends would contain fuel, and thus would act as a multiplying reflector with a sizeable returning currents. Using the chopped cosine distribution should yield the worst possible peaking ratio and the most conservative design. The product of cell and axial maximum to average values result in a generation peaking factor of 3.6 for MgO and 5.7 for ZrO₂. Since the thermal stresses in hollow cylinders are directly proportional to the generation rate in the material, applying the above peaking factors to the average thermal stresses in Table 5.4 should give good approximation to the maximum thermal stress. The stresses obtained are given in Table 5.5.

TABLE 5.5
MAXIMUM STRESSES AT 2500°F INCLUDING PEAKING FACTORS

	$\frac{1}{2}$ Inch Diameter		$\frac{1}{4}$ Inch Diameter		TS
	$b/a = 2$	$b/a = 3$	$b/a = 2$	$b/a = 3$	
MgO	1,944 psi	9,720 psi	486 psi	2,430 psi	4500
ZrO ₂	13,110 psi	70,110 psi	3,192 psi	14,022 psi	9000

From Table 5.5 it can be seen that $\frac{1}{2}$ inch diameter coolant holes result in excessive thermal-tensile stresses except for MgO hollow cylinders on 1 inch centers. Coolant holes of $\frac{1}{4}$ inch diameter, especially in MgO, gave the largest factor of safety, and are therefore recommended.

Because most properties of materials above 2500°F cannot be found in present literature an investigation was made to see how the factor of safety would vary if the ceramic was considered to be at some other temperature

between 1000°C and 1372°C. The maximum thermal stresses include the product of the axial and unit cell peaking factors (i.e., 3.6 for MgO and 5.7 for ZrO₂). Only 1/4 inch diameter coolant holes on 3/4 inch centers were investigated. The results are listed in Table 5.6.

TABLE 5.6

FACTORS OF SAFETY AT VARIOUS TEMPERATURES

Temp. °C	MgO	ZrO ₂
1000	.42	.18
1200	.62	.18
1300	1.16	.33
1372	1.85	.64

Table 5.6 reveals that for the configuration chosen ZrO_2 would have thermal stresses which would be too large. However, the above assumed that an outer to inner diameter ratio of 3 would be used. It is seen that the factor of safety increases with temperature, and therefore, above $1372^{\circ}C$, or $2500^{\circ}F$, it is extrapolated that MgO with this configuration would be safe. It is realized that an upper limit would be reached where this no longer would be true. Reference 19 gives specific encouragement to this extrapolation by providing evidence that above $1500^{\circ}C$ the thermal conductivity values rise rapidly. An empirical fit to this rise is expressed by: (Rm. Temp. to $1800^{\circ}C$)

$$K = \frac{18.8}{(T - 125)} + 8.5 \times 10^{-36} T^{10} \quad (5.15)$$

where T is expressed in °K ($300 < T < 2100$)

K is expressed in cgs units

Another investigation was made in which the thermal stresses were determined at specific points along the steam path and compared with the tensile strength at that point. The previous results indicate that the thermal stress problem in the second half of the reactor length is not as acute as is the first half. The results given in Table 5.6, if extrapolated to higher temperatures, showed increasing factors of safety, even if one assumed a constant generation rate of 3.6 times 1.6×10^5 Btu/hr-ft³ for MgO. Actually the internal generation at the points of higher temperature would be less than this value, and therefore, the problem should be further diminished. Figure 5.9 is a plot of the internal heat generation rate versus distance from the inlet of the reactor. Figure 5.9 was obtained by assuming a chopped cosine distribution for the flux in the axial direction. Thus, the internal generation rate is based on $\bar{G} = 1.6 \times 10^5$ with a maximum of 2.5×10^5 Btu/hr-ft³. Figure 5.10 was used to obtain the temperature distribution along the length of the reactor. After selecting several temperatures from Figure 5.10 between inlet temperature and 2500°F (highest temperature where properties are known) and approximate values of G from Figure 5.9, the thermal stresses at these points were calculated. The stresses given are the maximum, tensile thermal stresses in a MgO ceramic assuming a hollow cylinder with a b/a ratio of 3, and a hole diameter of $\frac{1}{4}$ inch. The tensile strengths were taken from Table 5.1 and the factors of safety were calculated. The results are listed in Table 5.7.

TABLE 5.7

ACTUAL STRESSES ALONG REACTOR LENGTH

Temperature (°F)	G (Btu/hr-ft ³)	Stress (Psi)	Strength (Psi)	Safety Factor
2012	$.15 \times 10^5$	836	10,000	12.0
2192	1.28×10^5	2,860	8,000	2.8
2372	1.90×10^5	1,700	6,000	3.5
2500	2.25×10^5	950	4,500	894 482

ENCLOSURE

It is seen that the most critical portion of the reactor from a thermal stress point of view is roughly $2\frac{1}{2}$ feet from the steam entrance. However, it seems safe to assume that the proposed configuration of $\frac{1}{4}$ inch coolant holes on $3/4$ inch centers is mechanically feasible.

Gamma Heating

Heating in the ceramic moderator has been broken down into 6 different sources of gamma photons.

1. Prompt fission gammas
2. Fission product decay gammas
3. Uranium non-fission capture gammas
4. Fuel poison capture gammas
5. Inconel capture gammas
6. Moderator capture gammas

The following discussion explains the methods used and assumptions made in determining the gamma heating in the ceramic moderator. Both magnesia and zirconium ceramics were investigated.

Prompt Fission Gammas

Reference 20 gives the spectrum for prompt gammas from U^{235} fission as follows;

$$N(E) = 8.2 e^{-E} \quad \text{Photons/Mev. Fission} \quad (5.16)$$

$$\int_{.2}^8 N(E) dE = 7.59 \quad \text{Photons/fission} \quad (5.17)$$

$$\int_{.2}^8 N(E) E dE = 7.49 \text{ Mev/fission} \quad (5.18)$$

It was therefore assumed that every fission yielded 7.5 photons of 1 Mev average energy or a total of 7.5 Mev per fission. However, some of this energy would be lost due to self absorption in the 2 inch diameter fuel rod. References 21 and 22 are investigations of self absorption of gammas by homogeneous cylinders with uniformly distributed sources. The uniform source distribution is not realized in the fuel tubes, but information on heating at distances of 1 radius away from 1 inch fuel rods indicate less than 5% error with uniform source being assumed*. On this basis the uniform source assumption was used. References 21 and 22 contain plots in which the fraction of gammas escaping can be determined if the cylinder radius and the fuel absorption coefficient are known. To find the appropriate energy absorption coefficient for the fuel (properly weighted for the prompt gamma spectrum) the following analysis was made. The average absorption coefficient was calculated from

$$\bar{\mu}_e = \frac{\int_{.2}^8 N(E) E \mu_e(E) dE}{\int_{.2}^8 N(E) E dE} \quad (5.19)$$

The upper integral was evaluated by numerical integration and Table 5.8 contains the pertinent values used in the calculation. Fuels containing both 4 and 6 mol% UF_4 were considered.

* Personal communication from L. G. Alexander, ORNL

TABLE 5.8

μ_e , GAMMA ENERGY ABSORPTION COEFFICIENT IN FUEL

Energy (Mev)	4%	6%
.2	.566	.739
.4	.120	.208
.6	.122	.139
.8	.105	.118
1.0	.096	.105
2.0	.079	.085
3.0	.076	.081
4.0	.075	.081
5.0	.075	.083
6.0	.077	.084
7.0	.078	.085
8.0	.079	.087
$\bar{E}$ 1 Mev	$\bar{\mu}_e = .097 \text{ cm}^{-1}$	$\bar{\mu}_e = .123 \text{ cm}^{-1}$

The values of the absorption coefficients are listed at the end of the above table. These gave transmission values of 0.757 for 4% fuel and 0.715 for 6% fuel. To determine the portion of this passing through the Inconel shell the assumption of exponential attenuation in a slab was made. The shell was 1/8 inch thick and an absorption coefficient of 0.205 was used. The transmission values to the outside of the pipe now becomes 0.705 for 4% fuel and 0.671 for 6% fuel.

To obtain the attenuation in the ceramic the information given in

References 23 and 24 was considered. Reference 24 contained the most readily usable data, and therefore, this report is based on its data.

The 2 inch fuel tube is surrounded by a 1 inch thick annulus of low density insulation. The insulation was assumed to absorb none of the gammas. Upon checking this assumption (slab attenuation with $\mu_e = 0.026$ and a density of 0.125 gm/cc) it was found that the assumption of no absorption was quite valid. Therefore, a 4 inch diameter source tube was mocked up with the same transmission as was previously found. Only the case of 6% fuel was investigated. A 4 inch source tube would have a mocked up absorption coefficient of 0.075 cm⁻¹ corresponding to a transmission of 67%. The average absorption coefficient in the ceramic for the prompt gamma spectrum also had to be evaluated. This was found for MgO and ZrO₂ in the same manner as was done for the fuel. Table 5.9 contains the pertinent information involved in the calculation.

TABLE 5.9

GAMMA ENERGY ABSORPTION COEFFICIENTS IN CERAMIC

Energy (Mev)	MgO	ZrO ₂
.2	.087	.584
.4	.089	.196
.6	.088	.152
.8	.086	.134
1.0	.083	.128
2.0	.071	.109
3.0	.064	.107
4.0	.060	.109
5.0	.057	.112

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6.0	.056	.116
7.0	.055	.119
8.0	.054	.123
$\bar{E} = 1 \text{ Mev}$	$\bar{\mu}_e = .074 \text{ cm}^{-1}$	$\bar{\mu}_e = .136 \text{ cm}^{-1}$

It is seen that the average energy absorption coefficient for 1 Mev gammas is 0.074 in MgO and 0.136 in ZrO₂. In other words the zirconia would attenuate the gammas more rapidly than would the magnesia. Using the development on page 360 of Reference 24 the results listed in Table 5.10 were obtained. The development approximates the source as a line at an effective self attenuation distance within the source. The information in Table 5.10 is for the middle of the reactor, where the source tube would in effect be viewed as an infinitely long source.

The gamma flux at any point distant from the surface of the source is expressed by,

$$\Gamma = \frac{S_v R_o^2}{2(u+z)} F(90^\circ, b_2) \quad (5.20)$$

where $S_v = Y \mathcal{F}$

$Y = 7.5 \text{ Prompt gammas/fission}$

$\mathcal{F} = \text{fission density} = \sum_{i=1}^3 \phi_i \Sigma_{fi}$

$R_o = \text{source cylinder radius}$

$a = \text{distance from surface of source}$

$z = \text{self attenuation distance (See Ref.)}$

$$F(90^\circ, b_2) = \int_{0^\circ}^{90^\circ} e^{-b_2 \sec \theta} d\theta$$

$$b_2 = \sum_i \mu_i t_i + \mu_s z \quad (\text{see Ref. 24})$$

μ_s = fuel absorption coefficient

Γ = gamma flux Photons/cm² sec

Using this expression and the curves in the quoted reference the gamma flux at various distances from the center of the source was obtained and the results are listed in Table 5.10. The values of flux are normalized to a source strength of 1 photon born per cubic centimeter per second.

TABLE 5.10

PROMPT GAMMA FLUX AT DISTANCES FROM CENTER OF FUEL TUBE

Distance (cm)	MgO	ZrO ₂
5.08	1.7 - 3.4	1.7 - 3.4
13.33	.37	.192
20.00	.11	.042
30.00	.03	.006
50.00	.004	.0002

The flux values of 5.08 cm, the surface of the source, considered that 67% of the photons born in a disk 4 inches in diameter and 1 cm high would arrive at the surface. The lowest value is based on a radially directed outgoing current while the highest value was based on an isotropically directed outgoing current. The actual case would be somewhere between the two. The final value was selected as the one which would yield 67% of the source energy if one integrated the resulting internal generation curve from 5.08 cm to infinity. The 67% figure was used interchangeably with the number flux and the energy flux because an equilibrium spectrum of 1 Mev per photon was assumed.

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It was found that the curve of Γ_r versus r can be fitted by two exponentials in both the case of MgO and of ZrO_2 . It must be remembered that the flux must be reduced by a factor of 4 since the 2 inch diameter source was mocked up as a 4 inch diameter source. The final fit for MgO was

$$\Gamma(r) r = 2.1 S_v \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \quad (5.21)$$

$$\Gamma(r) = \frac{15.75}{r} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \quad (5.22)$$

For ZrO_2 the fit was

$$\Gamma(r) r = 3.08 S_v \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \quad (5.23)$$

$$\Gamma(r) = \frac{23.1}{r} \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \quad (5.24)$$

Now

$$G(r) = \Gamma(r) \bar{E} \cdot \frac{1}{\mu_e} \text{ Mev/cm}^3 \text{ sec} \quad (5.25)$$

where $\bar{E} = 1 \text{ Mev}$

$$\mu_e (\text{MgO}) = .074 \text{ cm}^{-1}$$

$$\mu_e (\text{ZrO}_2) = .136 \text{ cm}^{-1}$$

Making the above substitutions the following expressions are obtained for the variation of internal generation with distance (r) from the center as a result of prompt gamma attenuation in the ceramic moderator.

In MgO

$$G(r) = \frac{1.16}{r} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.26)$$

In ZrO_2

$$G(r) = \frac{3.15}{r} \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.27)$$

Fission Product Decay Gammas

Reference 25 gives the spectrum for fission product decay gammas from U^{235} fissions as follows:

$$N(E) = 8 e^{-1.35 E} \quad \text{Photons/mev fission} \quad (5.28)$$

From which

$$\int_0^{\infty} N(E) dE = 6 \quad \text{Photons/fission} \quad (5.29)$$

$$\int_0^{\infty} N(E) E dE = 4.52 \quad \text{Mev/Fission} \quad (5.30)$$

$$\bar{E} = \frac{\int_0^{\infty} N(E) E dE}{\int_0^{\infty} N(E) dE} = .75 \quad \text{Mev/photon} \quad (5.31)$$

On the basis of this information, it was assumed that there were 6 photons given off per fission, having 0.75 Mev average energy (a total of 4.5 Mev per fission). The average energy absorption coefficient in the fuel and in the ceramic for the fission product decay gamma spectrum was determined as before, namely,

$$\bar{\mu}_e = \frac{\int_0^{\infty} N(E) E \mu_e dE}{\int_0^{\infty} N(E) E dE} \quad (5.32)$$

Listed in Tables 5.8 and 5.9 are the pertinent data for determining the average absorption coefficients. Table 5.11 contains the results of the numerical integration for both MgO and ZrO_2 and for fuel of 4% and 6% UF_4 .

TABLE 5.11

AVERAGE ENERGY ABSORPTION COEFFICIENTS (CM^{-1}) FOR F.P. DECAY GAMMAS

<u>Fuel (4%)</u>	<u>Fuel (6%)</u>	<u>MgO</u>	<u>ZrO₂</u>	
.120	.145	.080	.154	894 090

ENCLOSURE

Transmissions to the inside of the Inconel shell were found to be 71.5% for 4% fuel and 67.5% for the 6% fuel. Outside the Inconel shell these become 67% for and 63.3% respectively. Assuming a 4 inch source with these transmission values gave mocked up absorption coefficients of 0.075 cm^{-1} for the 4% fuel and 0.087 cm^{-1} for the 6% fuel.

In the same manner given previously (Prompt Fission Gamma section) the flux at various distances from the center of a source cylinder was found. The results are listed in Table 5.12.

TABLE 5.12
FISSION PRODUCT DECAY GAMMA FLUX

<u>Distance (cm)</u>	<u>Flux (MgO)</u>
5.08	1.6 - 3.2
13.33	.37
20.0	.12
30.0	.028
50.0	.003

If the information in Table 5.12 is compared with that in Table 5.10, it is seen that the fission product decay gammas are attenuated almost exactly like the prompt fission gammas. This is expected since the average energies are close (1 Mev and 0.75 Mev). For simplicity, the flux was assumed to be attenuated in the same manner.

Therefore, for MgO

$$\Gamma(r) = \frac{2.1 S_v}{r} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \quad (5.33)$$

where $S_v = Y \bar{\gamma}$

$Y = 6$ fission product gammas/fission

$$\bar{\gamma} = \sum_{i=1}^3 \phi_i \Sigma_{fi}$$

$$\bar{\Gamma}(r) = \frac{12.6}{r} \bar{\gamma} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \quad (5.34)$$

$$\text{Now, } G(r) = \bar{\Gamma}(r) \bar{\mu}_e \bar{E} \quad (5.25)$$

$$\bar{\mu}_e = .080 \text{ cm}^{-1}$$

$$\bar{E} = .75 \text{ Mev}$$

Therefore, for MgO

$$G(r) = \frac{.75}{r} \bar{\gamma} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.35)$$

For ZrO_2

$$\bar{\Gamma}(r) = 18.5 F \left[2.17 e^{-.282 r} + e^{-.142 r} \right] \quad (5.36)$$

$$\bar{\mu}_e = .154 \text{ cm}^{-1}$$

$$\bar{E} = .75 \text{ Mev}$$

Therefore, for ZrO_2

$$G(r) = \frac{2.13}{r} \bar{\gamma} \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.37)$$

Non-Fission Captures in Uranium and Fuel Poison Captures

For every fission in the fuel the assumption was made that 0.3 neutrons were captured in the uranium and do not result in fission, and that 0.13 neutrons were captured in poisons in the fuel. From Table 5.3 this gives (0.3×5) plus (0.13×7) or 2.4 Mev per fission. The energies listed in Table 5.3 are due to the energy given off as a result of the binding energy of the captured neutron. The decay gammas of the excited atom were investigated

and found to be quite small. Reference 16 suggests that the non-fission captures in uranium peaks at 2.5 Mev. Taking the common fission product poisons and using information from Reference 18, the gamma spectrum from these sources could perhaps be represented by 1 photon of 2.5 Mev of energy. Because the actual spectrum is not known and not having better information it was assumed that these photons are attenuated in the same manner as are the prompt fission gammas. This should be a conservative assumption, because the actual attenuation should be much less and thus give a more uniform heat generation and a lower cell peaking factor.

Therefore, for MgO

$$\Gamma(r) = \frac{2.1 S_v}{r} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \quad (5.33)$$

$$\text{where } S_v = Y \overline{\tau}$$

$$Y = 1 \text{ Photon/fission}$$

$$\overline{\tau} = \sum_{i=1}^2 \phi_i \Sigma_{fi}$$

$$\text{Now } G(r) = \Gamma(r) \overline{\mu_e} \overline{E}$$

$$\text{where } \overline{\mu_e} = .068 \text{ cm}^{-1}$$

$$\overline{E} = 2.5 \text{ Mev}$$

Therefore,

$$G(r) = \frac{.37 \overline{\tau}}{r} \left[2.17 e^{-.185 r} + e^{-.074 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.38)$$

For zirconia where

$$\overline{\mu_e} = .108 \text{ cm}^{-1}$$

$$\Gamma(r) = \frac{3.08 \overline{\tau}}{r} \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \quad (5.39)$$

$$G(r) = \frac{1.01 \overline{\tau}}{r} \left[2.71 e^{-.282 r} + e^{-.142 r} \right] \text{ Mev/cm}^3 \text{ sec} \quad (5.40)$$

Inconel Capture Gammas

For every fission in the fuel it was assumed that 0.3 neutrons were captured in the Inconel tube wall. Assuming Inconel to have the composition of 77% Nickel, 15% Chromium and 8% Iron, and using Reference 18 to obtain the capture gamma energies in each of these materials it was found that approximately 8 Mev per capture would be released in the form of high energy capture gammas. This means that (0.3×8) or 2.4 Mev/fission would be released. Assuming that 95% of this energy is absorbed in the ceramic gives 2.28 Mev/fission available for heating. This energy was spread out uniformly over the ceramic in the cell. The results for both MgO and ZrO_2 were;

$$G(r) = .064 \frac{1}{2} \text{ Mev/cm}^3 \text{ sec} \quad (5.41)$$

Moderator Capture Gammas

For every fission in the fuel the assumption was made that 0.4 neutrons were captured in the ceramic moderator. Reference 17 reveals that approximately 7.6 Mev per capture would be released in the form of capture gammas. Therefore, (0.4×7.6) or 3.04 Mev/fission would be obtained. About 97.5% of this energy would be absorbed in the ceramic so that 2.96 Mev/fission would be available for heating. The fuel would absorb very little energy since it occupies roughly 2 1/2% of the volume. Also, the absorption coefficient for the fuel and ceramic were assumed to be the same. The heating rate for both MgO and ZrO_2 was then obtained as

$$G(r) = .082 \frac{1}{2} \text{ Mev/cm}^3 \text{ sec} \quad (5.42)$$

Neutron Heating

To estimate the fraction of the total neutron energy deposited in the ceramic and its spatial distribution, the following assumptions were made:

1. The fast neutron flux can be represented as a function of energy and position by use of "Fermi Age" theory ^{28,29}.
2. All neutrons are born as a line source at the center of the fuel pipe.
3. The fuel and MgO ceramic have the same slowing down properties.
4. The system is homogeneous and has a density equal to 75 percent of theoretical density.
5. Inelastic scattering is negligible.

With these assumptions the flux at energy E_n and at space point r for a line source of one neutron per second per centimeter of length was given by

$$\phi(r, E_n) \simeq \frac{1}{\Sigma_T E_n} \int_{E_s=E_n}^{10 \text{ Mev}} \frac{P(E_s) P(E_s \rightarrow E_n)}{4 \pi \tau(E_s \rightarrow E_n)} e^{-\frac{r^2}{4 \tau(E_s \rightarrow E_n)}} dE_s \quad (5.43)$$

where

$P(E_s)$ = fission neutron spectrum

$$= \sqrt{\frac{2}{\pi e}} \sinh \sqrt{2E_s} e^{-E_s}$$

$P(E_s \rightarrow E_n)$ = probability of non-capture on slowing down from E_s to E_n .
 = 1 (for 10 to 0.1 Mev range)

$\tau(E_s \rightarrow E_n)$ = Fermi age to E_n

Σ_T = total macroscopic cross section at E_n

r = radius in centimeters from line source

The approximation that $P(E_s \rightarrow E_n)$ be unity results from the negligible absorption cross section in the high energy range. A plot of τ versus energy, for both MgO and ZrO₂, is shown in Figure 5.10 where τ was obtained from nuclear calculations and is discussed in that section.

The internal heat generation was obtained from the expression

$$G_1(r) = \int_0^{\infty} \Sigma_s \phi(r, E_n) E_n \left(\frac{1 - \alpha}{2} \right) dE_n \quad (5.44)$$

where

$G_1(r)$ = heat generation at r in Mev per cm³ per sec for a unit line source

Σ_s = macroscopic scattering cross section

α = macroscopic total cross section

$E_n (1 - \alpha)/2$ = average energy loss per elastic collision

α = maximum fractional energy loss per neutron per collision

$$= \left(\frac{A - 1}{A + 1} \right)^2$$

Equations 5.43 and 5.44 were evaluated numerically over the range 10 Mev to 5, 3, 1, and 0.1 Mev for MgO. A plot of $G_1(r)$ versus r on semi-log paper gave a curve that could be expressed as the sum of exponentials for $r > 1$ cm.

The total heating, Q , was then obtained by use of the expression

$$Q = 2 \pi \int_0^{\infty} G_1(r) r dr \quad (5.45)$$

where $G_1(r)$ was adjusted near the origin (in the fuel) to give a total integrated heating of 2 Mev per second for a source of 1 neutron per centimeter per second. This gave

$$G_1(r) = 0.0075 e^{-0.176r} + 0.0165 e^{-0.637r} + 0.24 e^{-2.56r} \quad (5.46)$$

The fraction of energy escaping the fuel, F , was then given by

$$F = \frac{\int_r^{\infty} G_1(r) r dr}{\int_0^{\infty} G_1(r) r dr} \quad (5.47)$$

where the lower limit, r , is the radius of the fuel pipe.

Table 5.13 presents the results of computations of the escape fraction for MgO ceramic:

TABLE 5.13
FRACTION OF NEUTRON ENERGY BEYOND GIVEN RADIUS

F	$r(\text{cm})$
0.85	1
0.77	2.54
0.68	4

In an effort to obtain an upper limit to the fraction of energy escaping the fuel pipe, Equations 5.43 and 5.44 were evaluated for a system which was considered to have the same nuclear properties as the fuel solution the expression for $G_1(r)$ obtained for this case was

$$G_1(r) = 0.002 e^{-0.099r} + 0.0058 e^{-0.288r} + 0.015 e^{-1.05r} \quad (5.48)$$

Since the fraction escaping the physical fuel pipe was all that was desired, (5.48) was not adjusted to give an integrated total heating of 2 Mev.

The energy escape fraction for a 2.54 cm fuel pipe was computed to be 0.89 for the all-fuel system in comparison with the fraction 0.77 for the fuel-MgO system above (Table 5.13). The difference was in the expected direction since the Fermi Age from 10 Mev to 0.1 Mev is approximately twice as great for the fuel as for MgO. The true value is probably somewhere between the

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two values.

The assumption that all the neutrons are born at the center of the fuel pipe leads to some error and tends to underestimate the fraction leaving the pipe. This can be corrected by integrating the line source over the pipe and introducing an expression for the source distribution across the pipe:

$$G_2(r) = 2 \pi \int_0^R S(R) G_1(r) dR \quad (5.49)$$

where

R = radius of fuel pipe

$S(R)$ = fission density across pipe

$G_2(r)$ = modified heating function

$G_1(r)$ = heating defined in 5.44

The integral in Equation 5.49 was not evaluated since time was not available to do the required numerical reduction. Because the fission density would be greatest near the surface of the fuel pipe a larger neutron fraction than indicated above would escape. Therefore, if the 0.77 fraction were used, the results for the total heat generation in the ceramic would be conservative; the resulting thermal stresses, however, would be greater than those calculated in another section of this report. As a compromise, it was assumed that no heating occurred in the insulating annulus about the fuel pipe and that the minimum fraction, 0.77, was used to represent the energy escape from the pipe.

Since the expression for $G_1(r)$ above assumed a homogeneous ceramic, a new heating function, $G_3(r)$, was obtained to compensate for the insulating annulus. In its derivation, the heating at the outer surface of the annulus was obtained by straight geometrical attenuation:

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and

$$(1 - \alpha)/2\xi \approx A / A + 2 \quad (5.54)$$

$$\Sigma_s \approx \Sigma_T \quad (5.55)$$

Therefore, the difference in $G_1(r)$ for ZrO_2 and MgO was, to a first approximation, due to the difference in τ . Since τ is larger for ZrO_2 than for MgO , but smaller than that of the fuel (see Fig. 5.10), the fraction of energy escaping the fuel pipe in a ZrO_2 system would be between 0.77 and 0.89. The attenuation would be approximately the same as for MgO . Hence, the assumption that the neutron heating was the same for ZrO_2 and MgO does not appear to have introduced a large error.

The above expression for the heating rate can be written in engineering units as

$$\begin{aligned} G_4(r) &= 3.91 \times 10^4 G_3(r) \\ &= \text{Btu/hr ft}^3 \text{ MW} \end{aligned}$$

Heat Transfer

From the preceding discussion on thermal stress problems it was concluded that to maintain stresses within the strength limits of the materials it would be necessary to limit the size of the coolant holes to 1/4 inch diameter on 3/4 inch centers. This section is primarily an investigation of the heat removal problem and its status with 1/4 inch holes on 3/4 inch centers.

There are two major thermal resistances in the "blanket" causing appreciable temperature gradients. These are associated with the temperature drop across the steam surface film and the temperature drop across the ceramic. Also of interest (because it is a measure of the heat removal rate) would be the

steam temperature rise which could take place from the viewpoint of physical heat transfer. The following discussion concerns the determination of the temperature differences for the proposed reactor operating at 400 MW.

Steam Temperature Rise

The steam temperature rise ΔT_s is found by the following expression,

$$\int_0^L G(L) V' dL = W C_p \Delta T_s(L) \quad (5.56)$$

where $G(L) = \text{Btu/hr ft}^3$

$V' = \text{ft}^3/\text{unit length of cell}$

$W = \text{lb steam/hr per cell}$

$C_p = \text{Btu/lb } ^\circ\text{F}$

Assuming a chopped cosine for the flux in the axial direction as plotted on Figure 5.9, G was equal to $2.5 \times 10^5 \sin \frac{\pi L}{12}$. For our unit cell, $V' = 0.78$, and based on data in Reference 27, the average C_p between 2000°F and 3000°F would be 0.65. From the plant flow sheet it is seen that 96 tons of steam per hour would pass through the reactor. Since there are 90 cells in the reactor, W would be equal to 0.593 lb steam/sec per cell. For this condition,

T_s was found to be

$$\Delta T_s(L) = 140 \int_0^L \sin \frac{\pi L}{12} dL \quad (5.57)$$

The results of the integration are plotted in Figure 5.11. Thus the steam would rise 1074°F and so if the inlet temperature were 2000°F , the outlet temperature would be 3074°F .

Temperature Drop Across Film

The film temperature drop, ΔT_f , was found from the relationship

$$G(L)V' = hA' \Delta T_f(L)$$

$$\text{where } h = \text{Btu/hr ft}^2 \text{ } ^\circ\text{F}$$

$$A = \text{ft}^2/\text{unit length of cell}$$

The value of A' for the unit cell was $16.55 \text{ ft}^2/\text{ft}$. An average film coefficient h was determined from the Dittus Boelter equation for the steam at an average condition of 2500°F . It was assumed that the film coefficient (h) would not vary along the length of the reactor. Thus,

$$\frac{hD}{K} = .023 \left(\frac{VD\rho}{\mu} \right)^{.8} \left(\frac{C_p\mu}{K} \right)^{.4} \quad (5.59)$$

$$\text{where } C_p = .65 \text{ Btu/lb } ^\circ\text{F}$$

$$D = .0208 \text{ ft}$$

$$V = 40 \text{ ft/sec}$$

$$\rho = .176 \text{ lb/ft}^3$$

The values of viscosity (μ) and conductivity (K) at these high temperatures were obtained from Reference 28. This reference furnished the expressions

$$10^5 \mu = \frac{A_o \sqrt{T}}{1 + A\tau/10^{A\tau}} \quad \text{poises} \quad (5.60)$$

and

$$10^5 K = \frac{C_o \sqrt{T}}{1 + C\tau/10^{C\tau}} \quad \text{cal/sec cm } ^\circ\text{C} \quad (5.61)$$

$$\text{where } A_o = 1.501$$

$$C_1 = 12$$

$$A = 446.8$$

$$\tau = 1/T$$

$$A_1 = 0$$

$$T = ^\circ \text{Kelvin}$$

$$C_o = 1.546$$

$$C = 1,737.3$$

After correcting for pressures (see pages 459 and 468 of Reference 7) it was found that $\mu = 34.5 \times 10^{-6}$ lb/sec ft and $K = .081$ Btu/hr-ft- $^{\circ}$ F. For the above conditions, the Reynolds Number is 4250, and h was found to be 71.6 Btu/hr ft 2 $^{\circ}$ F. The temperature drop, ΔT_f was found to be,

$$\Delta T_f (L) = 164 \sin \frac{\pi L}{12} \text{ } ^{\circ}\text{F} \quad (5.62)$$

Adding this temperature to the steam temperature for the same position the ceramic surface temperature T_{cs} was obtained. The results are given in Figure 5.11.

$$T_{cs}(L') = 2000 + \Delta T_s(L') + \Delta T_f(L') \text{ } ^{\circ}\text{F} \quad (5.63)$$

The maximum ceramic surface temperature was found to be 3100 $^{\circ}$ F near the exit end of the reactor, which would be only 26 degrees hotter than the highest steam temperature. The distance between the position at which the highest steam and ceramic surface temperatures occur would be about 1 foot.

Temperature Drop Across Ceramic

As derived in another section, the temperature drop across a hollow cylinder with assumed uniform internal generation is given by,

$$\Delta T_c = \frac{G}{2K} \left[b^2 \ln \frac{r}{a} - \frac{1}{2} (r^2 - a^2) \right] \quad (5.4)$$

For this case

$$r = b$$

and

$$b = 3a$$

Therefore,

$$\Delta T_c = 2.95 \frac{G a^2}{K} \quad (5.64)$$

where $a = 1/8$ inch

$$K = 1.5 \text{ Btu/hr ft } ^\circ\text{F (MgO)}$$

Therefore,

$$\Delta T_c = 53.5 \sin \frac{\pi L}{12} \quad ^\circ\text{F} \quad (5.65)$$

The above expression was used to evaluate T_c for various points along the length of the reactor. This temperature difference was added to the ceramic surface temperature (T_{cs}) to obtain the maximum ceramic temperature, (T_{cm}).

$$T_{cm}(L') = 2000 + \Delta T_s(L') + \Delta T_f(L') + \Delta T_c(L') \quad (5.66)$$

TABLE 5.14

TEMPERATURE DROPS WITHIN THE REACTOR

<u>Distance From Inlet (ft)</u>	<u>T_s ($^\circ\text{F}$)</u>	<u>T_f ($^\circ\text{F}$)</u>	<u>T_c ($^\circ\text{F}$)</u>	<u>Total ($^\circ\text{F}$)</u>
1	18	42	14	74
2	72	82	27	181
3	157	116	38	311
4	268	143	46	457
5	398	159	52	609
6	537	164	54	755
7	676	159	52	887
8	805	143	46	994
9	916	116	38	1070
10	1002	82	37	1121
11	1005	42	14	1111
12	1074			1074

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Table 5.14 is a compilation of the results plotted in Figure 5.11. The maximum temperature of the ceramic would be on the order of 3121°F. From this short analysis, it appears that heat transfer, in itself, presents no major problem with the configuration proposed. The velocity of 40 ft/sec, film coefficient of 71.6 Btu/hr ft² °F and outlet temperature of 3074°F are all satisfactory.

Pressure Drop

The coolant hole configuration of 1/4 inch holes on 3/4 inch centers presented no major heat transfer problems so the final criteria for the acceptability of this configuration lies in the loss of pressure in driving the coolant through the reactor. Page 158 of Reference 7 furnished an expression for the pressure drop in tubes for gases and vapors, namely,

$$\frac{P_1^2 - P_2^2}{2 R_g T_m} = \frac{G^2}{\alpha g_c} \left[\ln \frac{v_2}{v_1} \right] + \frac{f_m L G^2}{2 g_c r_h} \quad (5.67)$$

where P_1 = inlet pressure (psf)
 P_2 = outlet pressure (psf)
 R_g = Gas constant (ft lb/lb °F)
 T_m = mean temperature
 G = mass flow rate (lb/sec ft²)
 α = 1 for turbulent flow
 g_c = conversion factor (ft lb/lb sec²)
 v = specific volume (ft³/lb)
 f_m = friction factor
 L = length of flow path (ft)
 r_h = hydraulic radius = D/4 (ft)

For this particular case let

$$P_2 = SP_1$$

$$f_m = .01$$

$$R_g = 85.7$$

$$L = 12$$

$$T_m = 2960$$

$$r_h = .0052$$

$$G = 6.9$$

$$J = \frac{RT}{P}$$

$$g_c = 32.17$$

Therefore,

$$\frac{v_2}{v_1} = \frac{1.41}{s}$$

Therefore,

$$(1-s^2) = .00236 + .00007 e^{-s}$$

$$s = 0.9985$$

$$P_2 = 419 \text{ Psi}$$

Based on these results, it appears that the pressure drop across the reactor would be only approximately 1 psi, not an excessive loss. Assuming entrance and exit losses to be on the order of a total of 1.5 velocity heads, the pressure loss would not be significantly altered. The low pressure loss is due to the low density of the coolant (on the order of 0.176 lb/ft^3). It is recommended that the actual friction factor be measured experimentally, and also the effect of a series of short cell blocks placed end for end should be studied.

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Figure 5.1

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ENCLOSURE

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TOTAL INTERNAL HEAT GENERATION
VERSUS DISTANCE ALONG A-A

Figure 5.4

TOTAL INTERNAL HEAT GENERATION
VERSUS DISTANCE ALONG B-B

Figure 5.5

THERMAL-TANGENTIAL STRESSES IN HOLLOW TUBES

FIGURE 5.7

ASSUMED AXIAL VARIATION OF THE AVERAGE CELL GENERATION
RATE FOR A POWER LEVEL OF 400 MW.

$$\bar{G} = 1.6 \times 10^5 \text{ Btu/hr-ft}^3$$

FIGURE 5.9

VARIATION OF STEAM AND CERAMIC TEMPERATURES ALONG AXIAL LENGTH
OF REACTOR FOR MgO CERAMIC AND POWER LEVEL OF 400 MW.

FIGURE 5.11

MAXIMUM CERAMIC TEMPERATURE

CERAMIC SURFACE TEMPERATURE

STEAM TEMPERATURE

DISTANCE FROM REACTOR INLET (Ft.)

CHAPTER VI
THE FUEL AND POWER SYSTEM

Fuel System

The process heat reactor fuel system considered contained molten NaF-ZrF-UF₄ fuel (50-45-5 mol %), circulated throughout two inch i.d. Inconel pipes set on twelve inch triangular centers in a ceramic moderator. The mean temperature of the fuel was 1125°F with the reactor operating.

The gross reactor heat output was four hundred megawatts. Approximately 8.7% of the fission heat (35 megawatts) was found to be deposited in the reactor's ceramic moderator "superheater" and the remainder liberated in the fused salt. The core fuel volume was 670 liters, and the fluid velocity through the ninety fuel pipes was 17.4 feet per second. Eight 1900 gpm pumps connected in parallel would supply the one hundred foot head needed to circulate the fuel through the primary circuit.

The maximum wall temperature of the seamless Inconel fuel tubes would be 1257°F if seven-eighths of an inch of expanded magnesium oxide insulation were used to insulate the tubes. Under these conditions only 0.8% of the energy generated within the ceramic was transferred back to the fuel system.

A calculation of the thermal stresses in the Inconel fuel pipes showed them to be small in comparison to the stresses due to the steam pressure on the external pipe surface. These stresses plus an allowance for corrosion were the basis for choosing a one-eighth inch pipe thickness. However, corrosion data at reactor temperatures would be required to verify whether the thickness is adequate.

Four primary heat exchangers, 28 inches in diameter and 15.5 feet long having a total holdup of 2100 liters were required to remove the reactor heat. Adding the volume associated with the external piping increased the total fuel volume holdup to 5000 liters.

Core Arrangement

The reactor core, Figs. 2.2-2.3, consisted of a triangular array of ninety, twelve foot long, two inch i.d. and 0.125 inch wall Inconel pipes located on twelve inch centers. These pipes were in MgO blocks having a hexagonal cross section, approximately seven inches across the flats. Alternate ceramic blocks, Figure 5.1 have the necessary four inch diameter holes in the axial direction through which the fuel pipes and seven-eighths on an inch thick annulus of expanded, eight pounds per cubic foot, MgO insulation were located. All blocks have small steam passage holes.

The core lattice formed a right cylinder twelve feet long and ten feet in diameter. At either end of the twelve foot long core a two foot plenum chamber was provided for entrance and removal of the high temperature steam, (see Figure 2.2) surrounded the sides of the ten foot diameter core lattice region. The core lattice region plus the surrounding reflector were contained in a four inch wall, twelve foot diameter pressure vessel.

Flow Diagram

The reactor flow diagram is shown in Figure 6.1. Fuel enters the reactor at 1050°F and would be heated to 1200°F in a single upward pass through the reactor core. Two intermediate sodium loops were considered between the fuel circuit and the steam cycle. The operating temperatures for the sodium cycles are shown in the diagram. Feedwater at 450°F and 2000 psi would be pumped to the steam generator.

FIGURE 6.1
FLOW DIAGRAM

Two sodium loops were needed since two conflicting requirements had to be met. The minimum temperature in the first sodium loop must be maintained above the melting point of the fuel fluid to avoid the possibility of fuel freezing. Also, since the Na temperature leaving the steam generator would be relatively low, to prevent the occurrence of film boiling in the steam generator a secondary sodium loop would be required.

Power Plant

It was assumed that fairly conventional steam generating equipment would be used for the production of power. Therefore, this report does not include a thorough study of the system past the primary circuit.

A more complete design of a steam power plant using steam at 1000°F and 800 psi has been worked out by another group (ORSORT 1956, "600 Fused Salt Homogeneous Reactor Power Plant", summer project study group). This other group has designed the steam turbine cycle, sized the pumps required for the various loops, and estimated the piping requirements. Their results were, therefore, used here whenever applicable.

The objective of this study was to develop a feasible reactor design for production of high temperature process heat by means of radiation heating; thus, the details of the external system were relatively unimportant here. In view of this objective it was decided to adhere wherever possible to materials and designs that have been tested in actual reactors.

Fuel Selection

The fused salt fuel was selected because it best met the requirements specified for this reactor. Since only 8.7% of the reactor power was utilized in the generation of high temperature steam (3000°F), efficient use of the remainder would be essential from an overall economic viewpoint.

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The steam produced by a fused salt reactor operating at an outlet temperature of 1200°F would be suitable for use in present day steam plants, which have relatively high efficiencies. Because it has a comparatively low density and is a poor moderator, fused salt would also permit a relatively large fraction of the available radiation heating to be deposited in the moderator and be utilized in the generation of high temperatures. A further requirement met by this fuel is that it can be made to go critical in a poorly moderated reactor of reasonable size (12 foot diameter). Disadvantages of the fuel, however, should be noted. The high fuel inventory of fluid fuel would seriously affect the economics of the plant. Also, some of the decay gammas would be lost from a circulating fuel.

The sodium-zirconium-uranium fluoride fuel was selected from among the fused salt types because it has been extensively studied and has in addition been tested under operating reactor conditions. The particular composition selected was done so on the recommendation (Ref. 5) of the ORNL Chemistry Division. Other fused salt fuels were eliminated on the basis that too little information about them is presently available, rather than that their known properties make them less attractive.

The specific fuel chosen for this study was considered to have the following properties:

Composition (mol %)	50NaF-45 ZrF ₄ -5 UF ₄
Density (gm/cc)	3.33 + (0.036) (%U) - 0.0009(T-607°C)
Melting point (°C)	521
Specific heat (BTU/lb°F)	0.31 (550 - 850°C)
Thermal Conductivity (BTU/hr. ft°F)	1.5 (550 - 750°C)
Viscosity (centipoises)	16 at 627°C 10 at 727°C

(The above values for the fuel properties were extrapolated from data (Ref. 3) published in the Reactor Handbook.)

FIGURE 6.2
TEMPERATURE DISTRIBUTION IN FUEL,
PIPE WALL AND INSULATION

Fuel Container Selection

Inconel has been studied more than any other material for use with fluoride fuels and has shown the most promise. Though resistant, Inconel is somewhat attacked by the fuel. This attack has been described (Ref. 4) as; "The attack of fluoride mixtures on Inconel and stainless steels involves selective leaching of chromium from the surface of the hot portion of the apparatus." Data about the long time corrosion resistance on Inconel have not been obtained, however, the corrosion rates in the temperature range of (1050 to 1200°F) are believed (Ref. 5) to be much lower than those measured at higher temperatures. This must be substantiated by experiment. Some data (Ref. 4) shows that the chromium content of this metal is appreciably altered to a depth of 20 mils in 500 hours at 1500°F. On the advice of the ORNL Metallurgy Division the present design considered Inconel as the fuel container material.

Other container materials could also be used to contain fluoride fuels at reactor conditions. These are 304 stainless steel clad with nickel and some nickel-molybdenum alloys. Nickel gives these materials corrosion resistance, while the other components are used to give strength. On this basis nickel-clad stainless would be the best choice. The product, however, has as yet not been produced in suitable form and its use would thus be dependent upon technological improvements. Nickel-molybdenum alloys are in the early stages of development and possible use of these alloys also awaits further experimentation.

The intermediate sodium-loops would be constructed of 304 stainless steel.

Heat Generation in Fuel Pipe Wall

The radiation heat generation rates in the pipe wall and in the insulation were calculated for the midpoint of the reactor lattice. These generation rates were used to calculate the temperature distribution (Figure 6.2) in the fuel, pipe wall and insulation. The results show a maximum pipe wall temperature of 1257°F which is a safe value for the stresses and corrosion encountered. Table 6.1 lists the pertinent material properties, heating rates and flow conditions involved.

TABLE 6.1

TEMPERATURE DISTRIBUTION CALCULATION DATA

	<u>Insulation</u>	<u>Pipe</u>	<u>Fuel</u>
Generation rate (MEV/cc.sec) . . .	8.7×10^{11}	6.3×10^{13}	5.38×10^{15}
Thermal Cond. (BTU/hr.ft.°F) . . .	0.077	12.8	1.5
Density (Grams/cc)	0.125	7.9	3.5
Viscosity (lbs/hr.ft)			30.3
Specific Heat (BTU/lb.°F)			0.31
Outer Radii (cm)	5.08	2.86	2.54
Temperature (°F)	2850@5.08 cm		1125@ t
Reynold's Number			71,000
Prandtl's Number			6.26

Calculation of Generation Rates

A uniform rate of heat generation was assumed in the fuel pipes. A maximum to average factor of 1.57 was used to obtain the maximum heat generation rate. This factor of 1.57 accounts for the axial flux distribution but did not correct for nonuniformity in the radial direction. Radial flux distributions obtained from ORACLE calculations indicated that the radial flux distribution was fairly flat. The heating generation rate for the fuel was found as follows:

$$\begin{aligned} G (\text{maximum}) &= \text{peaking factor} \times \text{power density} \times \text{conversion factor} \\ &= 1.57 \times 0.549 \text{ (kw/cc.)} \times \frac{1000}{1.603 \times 10^{-13}} \text{ (MEV/sec. kw)} \\ &= 5.38 \times 10^{15} \text{ (MEV/cc. sec.)} . \end{aligned}$$

The generation rates in the pipe wall and insulation were calculated using the following sources:

a. Prompt fission gammas.

$7\frac{1}{2}$ one-mev gammas per fission.

71.5% escape the fuel.

67.1% escape the pipe wall.

(μ/ρ) inconel = 0.0263.

(μ/ρ) insulation = 0.026.

b. Decay gammas.

4.8 0.75-mev gammas per fission in core.

67.5% escape the fuel.

63.3% escape the pipe wall.

(μ/ρ) inconel = 0.027.

(μ/ρ) insulation = 0.027.

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c. Fuel non-fission-capture-gammas.

5 mev/capture, 0.3 captures/fission, otherwise, same as (a).

d. Fuel poison-capture-gammas.

8 mev/capture, 0.13 captures/fission, otherwise same as (a).

e. Moderator capture-gammas.

7.6 mev/capture, 0.4 captures/fission.

Assumed distributed uniformly over reactor.

f. Inconel capture-gammas were ignored.

g. Neutron-slowng-down-energy.

G_r in pipe 0.0079*

G_r in insulation $\frac{0.125}{2.74} \times 0.0045^*$.

*These values were obtained from the calculation of slowing-down energy deposition based on a homogeneous MgO core (see Chap. 5). The G_r in the insulation was reduced by the ratio of its density to the MgO density. G_r was calculated to fit the following equation:

$$G = G_r \times 50.6 \times \left(\frac{\text{fissions}}{\text{cc. sec.}} \right),$$

where

G_r = (mev/cc. sec.) for a one-neutron-per-second line source.

The determination of all of these sources is explained in detail in Chapter V. These energy sources give the following generation rates in the pipe wall and insulation:

TABLE 6.2

RADIATION HEATING GENERATION RATES ($\frac{\text{MEV}}{\text{cc. sec.}}$)

<u>Sources of Internal Generation</u>	<u>Pipe Wall</u>	<u>Insulation</u>
Prompt fission gammas	2.42×10^{13}	2.94×10^{11}
Decay gammas	1.39	0.59
Fuel non-fission-capture-gammas	0.48	0.39
Fuel poison-capture-gammas	0.32	1.54
Moderator capture-gammas	0.96	1.29
Neutron slowing-down-energy	0.75	1.95
Total generation rates (assumed uniform)	6.3×10^{13}	8.7×10^{11}

Calculation of Temperature Distribution

Internal heat generation in the fuel, pipe wall and insulation was assumed to be uniform. The temperature distribution in the fuel due to the volume heat source within the fluid was calculated according to the method outlined by Poppendiek and Palmer (Ref. 6). The expanded magnesium oxide insulation proposed was assumed to have a density of 8 lbs/cu. ft. and a thermal conductivity of 0.077 Btu/hr.ft.^{°F}, which is the conductivity of steam. This insulation would be comparable to Aerogel silica in density and thermal conductivity, but presumably could resist steam at the temperatures in the insulation. The thermal conductivity of Aerogel silica is approximately that of air, therefore, it was assumed that the MgO insulation would have a conductivity equal to that of the steam filling its pores.

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The fuel centerline temperature was conservatively set at 1125°F at the midpoint of a fuel tube although the bulk mean fluid temperature at this point is 1125°F . The ceramic temperature in contact with the insulation was set at 2850°F which is 100°F above the value calculated (see Chapter 5) elsewhere. Thus the results here should be conservative.

The pipe film-coefficient, $3460 \text{ Btu/hr.sq.ft}^{\circ}\text{F}$, was obtained from the Dittus-Boelter equation (Ref. 7). The results were tabulated in Table 6.3.

The heat flow from the moderator through the inner pipe wall, into the fuel was found to be $855 \text{ Btu/hr. per foot of fuel tube}$. This represents a loss of 0.8% of the heat generated within the ceramic. This corresponds to the energy loss by heat transfer through the entire ninety core fuel tubes. This heat loss from ceramic to fuel would be very sensitive to actual thermal insulating properties of the lightweight insulation considered.

Stresses in the Inconel Fuel Tubes

A check on the stresses in the Inconel fuel tubes was performed to compare these stresses with the allowable values at the temperatures involved.

The stresses due to external pressure on a thick wall pressure vessel were calculated (Ref. 11) for the condition of 400 psi external pressure and no internal pressure. This is a more severe condition than actually would exist. A maximum tangential stress of 3800 psi was calculated at the inner wall.

Thermal stresses were calculated (from the equations developed in Chapter V) for hollow cylinders with uniform internal generation in which the outer wall temperature gradient was considered zero. A uniform heat generation rate was used in the calculation. This rate considered heat flow

TABLE 6.3

FUEL AND INSULATION TEMPERATURE DISTRIBUTION

Centerline fuel temperature. . . . 1125°F

Fuel internal generation ΔT 's

Laminar Sublayer	1.1°F
Buffer Layer	36.8°F
Inner Turbulent Layer	5.1°F
Outer Turbulent Layer	29.3°F
Fuel film ΔT due to heat loss from moderator	0.5°F
Fuel film ΔT due to heat generation in insulation	0.1°F
Fuel film ΔT due to heat generation in pipe wall	3.1°F

Inner pipe wall temperature 1200°F

Pipe ΔT due to internal heat generation	36.4°F
Pipe ΔT due to heat loss from moderator	10.5°F
Pipe ΔT due to heat generation in insulation	10.0°F

Outer pipe wall temperature 1257 °F

Insulation ΔT due internal generation	572°F
Insulation ΔT due to heat loss from moderator	1020°F
Ceramic temperature at midpoint of core	2850°F

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from the ceramic to the fuel plus the internal generation rate calculated previously. With this heat generation rate, (11.45×10^5 Btu/hr cu ft), the maximum tangential stress, again occurring at the inner wall, was 760 psi. In both calculations, the radial stresses were lower than the tangential stresses. The total tangential stress at the inner wall, 4560 psi, can be compared to the Inconel stress data reported in Table 6.4 (Ref. 9).

TABLE 6.4
INCONEL STRESS DATA

Temperature	1200°F	1400°F
Ultimate tensile strength	65,000	27,500 psi
Yield strength (0.2% offset)	27,500	17,000 psi
Stress-rupture at 1000 hr.	12,000	5,500 psi
0.1% creep in 10,000 hr.	5,500	1,700 psi

TABLE 6.5
INCONEL PROPERTIES USED IN STRESS CALCULATIONS

Thermal Conductivity, K	1.5 Btu/hr.sq.ft.°F
Young's Modulus, E	31×10^6 psi
Poisson's Ratio	0.3
Temperature Coefficient of Expansion, α	6.4×10^{-6} in./in.°F

Primary Heat Exchanger Design

In designing the primary heat exchanger an attempt was made to minimize the holdup in the heat exchanger while keeping the pressure drop in the primary circuit at a value for which pumps were designed (Ref. 10).

The head loss (ΔF) in the heat exchanger and headers (including expansion and contraction losses in the headers) was found from,

$$(Eq. 1) \quad \Delta F = (4fL/D + 1.5) \times V^2/2g$$

The tube side heat transfer coefficient and the shell side heat transfer coefficient were found (Ref. 12) from

$$(Eq. 2) \quad h_{TS} D/k = 0.023 (DV/k)^{0.8} (C_p \mu/k)^{0.4} \quad \text{or} \quad h_{TS} = 398 \frac{(V')^{0.8}}{(D''_{TS})^{0.2}}$$

and

$$(Eq. 3) \quad h_{SS} = \frac{443}{D''_{EQ}} \left\{ 7 + 1.15 (D''_{EQ} V'_{SS})^{0.8} \right\},$$

where, h_{TS} = tube side h

h_{SS} = shell side h

D''_{TS} = tube side i.d. (inches)

D''_{EQ} = shell side equivalent diameter (inches)

$V'_{TS,SS}$ = tube side and shell side velocities (ft./sec.).

Since the heat exchanger load for this reactor was to be 365MW (1235×10^6 Btu/hr.), the fused salt flow rate and the sodium flow rates would be 26.8×10^6 and 33.2×10^6 lbs/hr. Therefore, the number of tubes (X) can be computed from,

$$X = 6270/V'_{TS} D''_{TS}^2$$

The number of unit cells would be twice this number when the pipes are in a triangular lattice. Since the total shell side area equals the area of a cell times the number of cells, the shell side velocity would be related

to the tube side velocity as shown by Eq. 7, in which A''_{SS} is the shell side unit cell area in square inches.

$$(Eq. 7) \quad V'_{SS} = 2.16 D_{TS}^2 V'_{TS} / A''_{SS} .$$

The shell side equivalent diameter was taken to be four times the area divided by the wetted perimeter.

Heat Exchanger Calculations

- Assumptions:
- a. A 50 psi, 33 foot head, loss was assumed to be permissible across the heat exchanger.
 - b. The tubes had 0.375 o.d. and 0.035 walls.
 - c. Unit cell dimensions (Ref. 8) were set at 3/8" tubes on 5/8" centers in a triangular lattice.
 - d. The tube side velocity was assumed to be 9.2 ft/sec.

From Eq. 2 the tube side heat transfer coefficient was found to be 2980, and from Eq. 3 the shell side coefficient was found to be 8990. These two plus the wall coefficient combined to give an overall coefficient of 1286 Btu/hr.sq.ft.^{°F}. This necessitated the use of 7350 tubes, 15.5 feet long, to remove the heat with the allowable pressure drop through the exchanger. To keep the heat exchanger diameter within a reasonable size it was proposed to use four, 28-inch diameter and 15.5 foot long exchangers, with a combined holdup of 1650 liters in the fuel tubes.

Primary Circuit Holdup

Calculation of the fuel holdup in the core and the heat exchangers was performed rather carefully. However, piping holdup in this tentative design was difficult to accurately estimate. The estimate obtained for fuel holdup appears to be a minimum reasonable value obtainable for this system.

Holdup in reactor core	670 liters
Holdup in reactor but outside core region.	330 liters
Holdup in heat exchanger tubes	1650 liters
Holdup in heat exchanger heads	450 liters
Holdup in 33 feet of piping (Fuel velocity assumed same as core velocity in this piping).	<u>1900 liters</u>
TOTAL FUEL HOLDUP	5000 liters

Alternate Designs Considered

During the course of the study several designs were considered. The design described previously was the most promising of those studied. Some of the other designs will now be presented in order that the design decisions may be justified in more detail.

Core Types

The choice of a lattice type core (Fig. 6.3a) was primarily based upon optimization of gamma and neutron heating. Thermal stress problems and physical arrangement difficulties also affected the decision. Annuli and cylinders were also considered (see Fig. 6.3b, c, d) as possible core arrangements. An annular fuel region with two-pass-steam-flow through the core was second in order of preference. Thermal stresses in such an annulus type reactor appeared to present severe problems, however, insulation and fuel-inlet and exit problems would be less troublesome than those encountered in the lattice type core.

Fertile Blanket

A ceramic clad thorium oxide moderator region would be very advantageous in this design. A suitably canned fertile material in the high temperature superheater region would increase the proportion of the reactor power avail-

FIGURE 6.3

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ALTERNATE CORE ARRANGEMENTS

FUEL
CERAMIC
(steam passage)
MODERATOR
(no steam passage)

a. Single Steam Pass Lattice Core

b. Single Steam Pass Annular Core with Separate Moderator

FUEL
CERAMIC
(steam passage)

c. Single Steam Pass Cylindrical Core

d. Double Steam Pass Annular Core

able for high temperature heat and would enable some fuel conversion. Both of these would aid the economics of a high temperature process heat reactor. With the present technology, however, a leak tight ceramic coating material does not exist for canning the thorium oxide. The possible leakage of fission products into the process steam precludes the use of a fertile material in the moderator region of the reactor at the present time. Developments in ceramic coatings may be found in the future, which would make this type of reactor very attractive.

Fuel Types

A wide range of fuels (clad U^{235} , aqueous, liquid metal, and fused salt) were considered before the fluoride fused salt was selected. Several considerations especially important for this application had to be constantly kept in mind. Fuels had to be rated on the gamma leakage obtainable as a function of the fuel geometry and on the amount of neutron slowing down that could be obtained in the ceramic rather than in the fuel. To have the largest percent possible of the non-fission captures occur in the ceramic seemed at first to be desirable, but since this effect would increase the critical mass, this type of optimization was much less advantageous than indicated at first glance.

Associated with solid fuels are large gamma attenuation coefficients, so gamma leakage from such a fuel would be relatively low. Solid high temperature fuel elements of the type presently being studied by General Electric were considered in the early stages of the conceptual design. These elements might be used to heat steam in the core to 2300°F . Following this pass the radiation heating could add another 200°F , giving steam at 2500°F . Several shortcomings were seen in this scheme. The resultant steam at

2500°F would not be high enough for use in a process such as the coal gasification process. Corrosion resistance of these high temperature fuel elements is unknown since the available data are for air.

Other canned solid fuel elements were also considered. The desirability of having a low pressure fuel region and of producing high quality steam for by-product power weighed heavily against use of water cooled fuel elements. Sodium-cooling showed some promise, but having the necessary sodium surrounding the fuel element would reduce the radiation heating in the ceramic. Burnup, fuel replacement, and control problems also appeared in the study of solid fueled reactors. On these bases solid fuels were abandoned in favor of a homogeneous circulating fuel. Glasstone (Ref. 2) mentions several of the advantages of this fuel type.

The LMFR fuel is the only liquid metal fuel which has been extensively studied. This fuel, enriched uranium dissolved in bismuth, cannot be used because the solubility of uranium in bismuth is insufficient to keep the reactor critical and yet have sufficient gamma leakage from the fuel for energy deposition in the ceramic. The large gamma absorption coefficient of bismuth severely limits its usefulness as a fuel in this application.

Aqueous fuels, such as the uranyl sulphate solutions studied at Oak Ridge National Laboratory do not appear applicable for the application here, since the fuel system would have to be operated at a considerable pressure in order to obtain suitable steam for production of by-product power, and this pressure would be undesirable from the standpoint of gamma escape and neutron economy. Aqueous fuels would also absorb a large fraction of the slowing down energy of the neutrons.

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CHAPTER VII

REACTOR ANALYSIS AND CONTROL

Introduction

Three group, three region nuclear calculations were made for both the "homogenized" reactor and the "unit cell" reactor. Group constants were flux averaged over a unit cell by means of disadvantage factors obtained from ORACLE flux plots for the "unit cell" reactor.

The reactor kinetic equations were used to evaluate reactor behavior using temperature coefficients obtained from a three group, diffusion theory calculation for a bare reactor. The non- $1/v$ behavior of samarium, xenon, and uranium was considered by use of analytical approximations to the absorption cross sections. A constant precursor-population-to-power-generation ratio was assumed. Solutions were obtained for the temperatures resulting in the ceramic blanket and in the fuel as a consequence of step changes in the power level. Results indicated undesirable changes in temperature distributions following reactivity additions, such that coupling of fuel and blanket power-removal rates should be required for safety, or control rods would be required.

A proposed procedure for start up and shut down in view of the reactor kinetics is discussed.

Evaluation of Group Constants

The three group, three region code for the ORACLE¹ was employed for criticality and flux shape determinations. In the three energy group representation, Group I extended over Ryewash² lethargy groups 1 through 7, Group II over 8 through 21, and 'thermal' Group III was taken as group 22. This corresponded to taking a thermal base temperature of approximately 2600°F.

Reactor constants in Group I and II were flux averaged over the discrete lethargy intervals in the following manner:

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$$\bar{\Sigma}_a, \bar{\Sigma}_f \text{ or } \bar{D} = \sum_i (\Sigma_{ai}, \Sigma_{fi} \text{ or } D_i) \phi_i / \sum_i \phi_i \quad (1)$$

$$\phi_i = (F_i + \frac{S_i}{2}) \Delta u_i / \bar{\Sigma}_t \Sigma_{ti} \quad (2)$$

$$F_i = F_{i-1} + S_{i-1} - (\Sigma_{a,i-1} + D_{i-1} B^2) \phi_{i-1} \quad (3)$$

= number of neutrons slowing down into sub-group "i", normalized

S_i = number of neutrons born in sub-group "i", normalized

$$\bar{\Sigma}_{x1} = [1 - \sum_i (\Sigma_{a,i} + D_i B^2) \phi_i] / \sum_i \phi_i = \bar{\Sigma}_{R1} - \bar{\Sigma}_{a1} \quad (4)$$

$$\bar{\Sigma}_{x2} \sum_i \phi_i = \bar{\Sigma}_{x1} \sum_i \phi_i - (\bar{\Sigma}_{a2} + \bar{D}_2 B^2) \sum_i \phi_i = (\bar{\Sigma}_{R2} - \bar{\Sigma}_{a2}) \sum_i \phi_i \quad (5)$$

The fission spectrum weighing factors, S_i , were defined in Group I by Watt's spectrum³, and were zero in Group II. The function Σ_x is essentially a cross-section for transfer from one group to the next.

The macroscopic cross sections in the individual Eyewash subgroups were flux averaged over a unit cell by use of disadvantage factors, ζ , computed from ORACLE results. The general expression used was:

$$\Sigma_{g,i} V^{\text{total}} = \Sigma_{g,i}^{\text{Ceramic}} V^{\text{Ceramic}} + \zeta_i \Sigma_{g,i}^{\text{Fuel}} V^{\text{Fuel}} + \frac{1}{2} (1 + \zeta_i) \Sigma_{g,i}^{\text{Shell}} V^{\text{Shell}} \quad (6)$$

$$\zeta = \phi^{\text{Ceramic}} / \phi^{\text{Fuel}} \quad (7)$$

V = Volume of region considered

In computing the disadvantage factors, use was made of ORACLE flux averages computed for a reactor composed of a centralized fuel region unit cell reactor. This central region consisted of fused salt with radius equal to that of the pipes proposed for the heterogeneous reactor. The

surrounding region was composed of an annulus of fuel-free ceramic of radius equivalent to a unit cell. The outer region was homogenized ceramic and fuel. An Inconel shell was inserted between regions I and II, with no shell between II and III, although Inconel was homogenized in with the ceramic-fuel third region.

The disadvantage factor for a group was defined as the ratio of the average flux in region II to that of region I. In order to apply a continuously varying ξ to the computations of Equation (6), the three ξ_j 's obtained from the mocked up unit cell were plotted against energy on log log paper, the points connected by a straight line, and sub ξ_1 's read for each Eyewash lethargy interval. (Figure 7.1). This, of course, is a simplification of the actual behavior of ξ . The actual variation with energy would be a more complicated function of energy; with local variations due to resonance effects. The approximation of the relation by a straight line on log log paper did not account for resonance effects, but should give a general description of the overall functional form of the disadvantage factor.

Parametric studies were made to indicate the effect of variation of pipe radius, cell radius, and fuel concentration. In computing the homogenized regions, iterations were made by applying successively computed ξ 's to the macroscopic cross sections in the region. Little effect was noticed, as might be expected.

Average densities were computed at 2600°F in the ceramic, and at 1125°F in the fuel. Microscopic cross sections were read at 2600°F for Group III under the assumption that the neutron temperature would be that of the ceramic and not that of the fuel or reflector.

"Hot-clean" and "hot-dirty" constants were computed and criticality

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FIGURE 7.1

determinations and flux plots made. Hot-dirty was defined as 5% poisoned fuel at temperature; this 5% poisoning was assumed independent of lethargy. Poisoning of 10% was also studied.

Streaming

The physical arrangement of the reactor with the array of fuel pipes surrounded by a "dead" space of stagnant steam would result in some neutron streaming, and complicate the calculations. However, the extension of the fuel pipes above and below the ceramic bed and the location of insulating material on the pressure vessel domes was assumed to counter-act the streaming effect and no effort was made to introduce correcting factors in the calculations.

The flux shapes in the axial direction were assumed to be chopped cosines, again ignoring the complication of streaming. In reality, the extension of the fuel pipes past the ceramic bed might well tend to flatten the flux near the surface, although no accurate way of predicting the functional form exists. At best, any modification of the buckling would be guesswork.

Reactor Poisoning

ORACLE flux plots indicated that the proposed model was essentially intermediate hence the normal method of computing poisoning cannot be used with confidence. A method consisting of an iterative calculation might be made: (1) from ORACLE averaged fluxes, the thermal flux can be computed, given the power density, (2) A thermal samarium and xenon concentration can then be computed.⁴ (3) Using these concentrations, poisoning in each lethargy group can be introduced, and new reactor constants obtained. (4) New flux averages can then be obtained, and the process repeated until "convergence" occurs. For greater "accuracy", other poisons should be included. The

necessity for going to such effort arises from the indications that intermediate poisons are by no means ignorable.⁵

The method actually used for this reactor was not so complete. For a thermal-to-intermediate power ratio of unity, a thermal flux was computed for a total power density of 500 w/cc, from which

$$\Phi_{th} \cong 4 \times 10^{13}$$

A thermal xenon poisoning was then computed and found to be

$$P_x \cong 0.028$$

Samarium was taken as giving a poisoning of 0.012 at thermal⁶ independent of flux. This was added to the Xenon contribution, and the total raised to 5% to account for the other fission products and corrosion products. To approximate the intermediate poisons, the 5% poisoning was taken as applicable in all energy groups. No effort was made to refine the calculations further. An additional calculation was made assuming a 10% poisoning.

Any advantage from the circulation of the fuel as an aid in removing volatile and short lived poisons was neglected.

Temperature Coefficient of Reactivity

The physical separation of the reactor into two regions -- "cool" fuel pipes and "hot ceramic" -- indicates the definition of separate temperature coefficients for each region. In general a temperature change in the ceramic would result in modified L^2 , B^2 , τ , and cross sections, both in the blanket (due to density changes) and in the fuel (due to the change in the thermal base). Changes in the fuel zone temperature would affect the uranium density primarily.

In an attempt to take into account the intermediate character of the reactor, a critical equation of the form of the bare, three group critical equation was assumed, namely:

$$k \approx \frac{\eta_2 f_2 P_{NA1}}{(1+B^2 \tau_1 - \eta_1 f_1 \frac{\Sigma_{a1}}{\Sigma_{R1}})(1+B^2 \tau_2)} + \frac{\eta_3 f_3 P_{NA2}}{(1+B^2 \tau_1 - \eta_1 f_1 \frac{\Sigma_{a1}}{\Sigma_{R1}})(1+B^2 \tau_2)(1+L_3^2 B^2)} \quad (8)$$

$$k \approx k_1 + k_2 \quad (9)$$

where

$$\eta_i = \nu \Sigma_{fi} / \Sigma_{ai}^u \quad (10)$$

$$f_i = \Sigma_{ai}^u / \Sigma_{ai}^{total} \quad (11)$$

$$P_{NA1} \approx \Sigma_{X1} \Sigma_{a2} / \Sigma_{R1} \Sigma_{R2} \quad (12)$$

$$P_{NA2} \approx \Sigma_{X1} \Sigma_{X2} / \Sigma_{R1} \Sigma_{R2} \quad (13)$$

$$B^2 = (\pi/\tilde{H})^2 + (2.405/\tilde{R})^2 \quad (14)$$

$$\tau_i \approx D_i / \Sigma_{Ri} \quad (15)$$

$$L_3^2 = D_3 / \Sigma_{a3} \quad (16)$$

Equation (8) is, of course, not applicable to the reflected reactor; however, it was assumed that results derivable from it will be indicative of the thermal behavior of the model proposed.

The location of the thermal base near 0.14 ev ($\sim 2600^\circ\text{F}$) did not allow

the simplifying assumption that f_3 , the "thermal utilization", is temperature independent, for both samarium and xenon exhibit rapidly decreasing absorption cross sections in the 2000° -- 3000°F range. In order to account for this behavior in the poisoned reactor, approximations to the cross sections were made of the form

$$\sigma_{aj}(T) \approx \sigma_{aj}(T_0) e^{-\beta_j(T-T_0)} \quad (17)$$

Plots of σ_a versus temperature on semi-log paper showed that the form of Equation (17) gave good agreement with experimental data. Values found for the parameter β were:

$$\beta_{Sm} = 1.52 \times 10^{-3} / ^\circ F$$

$$\beta_{Xe} = 9.8 \times 10^{-4} / ^\circ F$$

$$\beta_{U235} = 1.85 \times 10^{-4} / ^\circ F$$

$$\beta_{1/v} = 3.42 \times 10^{-4} / ^\circ F$$

All elements other than uranium, samarium, and xenon were considered to obey a $1/v$ dependence.

Defining γ , the temperature coefficient of reactivity, to be representable by

$$\gamma = \frac{\partial \rho}{\partial T} / T_0 \quad (18)$$

where, as usual,

$$\rho = (k-1)/k \quad (19)$$

$$\frac{\partial \rho}{\partial T} = \frac{1}{k^2} \frac{\partial k}{\partial T} \quad (20)$$

and defining $P_{NL1} = (1 + \beta^2 T_1 - \eta_1 f_1 \frac{\sigma_{a1}}{\Sigma R_1})^{-1}$ (21)

$$P_{NL2} = (1 + \beta^2 T_2)^{-1}, \quad P_{NL3} = (1 + \beta^2 T_3)^{-1} \quad (22, 23)$$

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FIGURE 7.2

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$$\text{then } \gamma \approx \frac{1}{R} \left(\frac{1}{P_{NL1}} \frac{\partial P_{NL1}}{\partial T} + \frac{1}{P_{NL2}} \frac{\partial P_{NL2}}{\partial T} \right) + \frac{f_1}{f_2^2} \left(\frac{1}{f_2} \frac{\partial f_1}{\partial T} \right) + \frac{f_2}{f_2^2} \left(\frac{1}{f_3} \frac{\partial f_2}{\partial T} + \frac{1}{P_{NL3}} \frac{\partial P_{NL3}}{\partial T} \right) \quad (24)$$

$$\frac{1}{P_{NL1}} \frac{\partial P_{NL1}}{\partial T} \approx -\frac{4}{3} B^2 \zeta_1 \alpha_V^C P_{NL1} \quad (25)$$

$$\frac{1}{P_{NL2}} \frac{\partial P_{NL2}}{\partial T} \approx -\frac{4}{3} B^2 \zeta_2 \alpha_V^C P_{NL2} + (B^2 D_3 P_{NL2} / \xi \Sigma_{t3} T_0) \quad (26)$$

$$\frac{1}{P_{NL3}} \frac{\partial P_{NL3}}{\partial T} \approx -L_3^2 B^2 P_{NL3} \left\{ \frac{4}{3} \alpha_V^C + (\beta_u + \beta_{xe} P_{xe} + \beta_{sm} P_{sm} + \beta_{nu} P_{nu}) \frac{\Sigma_{a3}^u}{\Sigma_{a3}^T} \right. \\ \left. + \frac{1+\zeta_3}{2} \frac{\Sigma_{a3}^{inc}}{\Sigma_{a3}^T} + \zeta_3 \frac{\Sigma_{a3}^c}{\Sigma_{a3}^T} \right\} \quad (27)$$

$$\frac{1}{f_2} \frac{\partial f_2}{\partial T} \approx -\alpha_V^F + (\alpha_V^F \Sigma_{a3}^F / \Sigma_{a3}^T) + (\alpha_V^C \Sigma_{a3}^C / \Sigma_{a3}^T) \quad (28)$$

$$\frac{1}{f_3} \frac{\partial f_3}{\partial T} \approx -\alpha_V^F + (\alpha_V^F \Sigma_{a3}^F / \Sigma_{a3}^T) + (\zeta_3 \alpha_V^C \Sigma_{a3}^C / \Sigma_{a3}^T) - \beta_u + \beta_{nu} \Sigma_{a3}^{nu} / \Sigma_{a3}^T + (\zeta_3 \beta_c \Sigma_{a3}^c / \Sigma_{a3}^T) \\ + (1+\zeta_3) \beta_{inc} \Sigma_{a3}^{inc} / 2 \Sigma_{a3}^T + (\beta_u + \beta_{xe} P_{xe} + \beta_{sm} P_{sm} + \beta_p P_p) \Sigma_{a3}^u / \Sigma_{a3}^T \quad (29)$$

where α = volume coefficient of thermal expansion

β = parameter defined in (17)

$\beta_{nu}, \beta_{inc}, \beta_c$ vary as $1/v$ (assumed)

$P_{NA2} \neq f(T)$

P = Poisoning fraction

ζ = disadvantage factor defined previously

Subscripts and superscripts:

F = fuel

Inc = inconel

c = ceramic

u = uranium

Nu = non-uranium components in fuel

Xe = xenon

Sm = samarium

p = other poisons

Numerical values computed for the temperature coefficients were:

$$\gamma_{\text{ceramic}} = + 4 \times 10^{-5} \text{ per degree Fahrenheit}$$

$$\gamma_{\text{fuel}} = - 7 \times 10^{-5} \quad " \quad " \quad "$$

The positive coefficient of the ceramic blanket resulted from the rapid decrease in the poisoning with the increase in the thermal base temperature. So long as large temperature variations in both ceramic and fuel are allowed, the overall negative coefficient indicates that reactivity additions can be controlled; however, restriction of the allowable temperature range of a region - as, for instance, the fuel, which must operate above the liquidus temperature as a lower limit, and is restricted on the high temperature side to below the pipe wall yield temperature-- would conceivably give an overall positive coefficient by forcing the reactor to obey the blanket alone.

Reactor Kinetics

The disquieting features of the system are the positive blanket temperature coefficient and the short neutron lifetime characteristic of intermediate systems. The short lifetime may not be detrimental, however, since it will give a faster reactor response to any rate function change in reactivity. The reactivity would be added in small overall amounts because of the necessity of keeping the temperature fluctuations within the allowed bounds, and because of the physical operations associated with reactivity changes the reactivity would be added as a rate function. Reactor response to reactivity variations would be satisfactory if the fuel and blanket temperature increased at the same rate; however, as indicated below, an increase in the reactor power could cause the blanket temperature to rise faster than the fuel temperature, leading to an effectively positive temperature coefficient and unstable reactor operation. Under such cir-

circumstances control rods would be required.

Assuming the kinetic equations describing the system to be of the space and energy independent form, the equations are

$$\frac{\partial N}{\partial T} = \dot{N} \approx -\Sigma_{a2} N_2 v_2 - \Sigma_{a3} N_3 v_3 - B^2 (D_2 N_2 v_2 + D_3 N_3 v_3) + (1-\beta) \nu (\Sigma_{f2} N_2 v_2 + \Sigma_{f3} N_3 v_3) + \lambda C \quad (30)$$

$$\dot{C} \approx -\lambda C + \beta (\Sigma_{f2} N_2 v_2 + \Sigma_{f3} N_3 v_3) \quad (31)$$

$$(C_{s1})_1 \dot{\Theta} = {}_1P - {}_{o1}P \quad (32)$$

$$(C_{s2})_2 \dot{\Theta} = {}_2P - {}_{o2}P \quad (33)$$

where:

$$N = N_2 + N_3$$

N = neutron population

Σ_{qi} = macroscopic cross section

v = neutron velocity

D = average diffusion coefficient

B^2 = geometric buckling

β = delayed neutron fraction (no circulation factor)

λ = delayed neutron precursor decay constant

C = delayed neutron precursor number density

${}_iP$ = power production rate (watts/cc)

${}_{oi}P$ = power removal rate (watts/cc)

C_s = constant sp. ht. x density x vol. ratio x 2.32

$\Theta = T_i - T_{i0} (^{\circ}F)$

Subscripts to left have the following regional indication:

01 = fuel,

02 = ceramic

1 = fuel

2 = ceramic

to right: 2 = intermediate

3 = thermal

Defining:

$$P_2 = \omega N_2 \quad (34)$$

$$P_3 = \omega N_3 \quad (35)$$

$$w = \text{conversion factor} \quad (36)$$

then, after noting that (from ORACLE results)

$$P_2 \approx 1.3 P_3 \quad (37)$$

the kinetic equations reduce to

$$P^0 = -\left(\frac{1}{2.3}\right)\left(\frac{1.3}{\ell_2} + \frac{1}{\ell_3}\right)P + \left(\frac{1-\beta}{2.3}\right)\left(\frac{1.3\ell_2}{\ell_2} + \frac{\ell_3}{\ell_3}\right)P + \lambda\omega C \quad (38)$$

$$\dot{C} = -\lambda C + \frac{\beta}{\omega} \left(\frac{1.3 \rho_2}{\rho_2} + \frac{\rho_3}{\rho_3} \right) P \quad (39)$$

$$C_{s1} \dot{\Theta} = \dot{P} - \dot{\phi} \quad (40)$$

$$C_{sz} \cdot {}_2\Theta^{\circ} = {}_2P - {}_{02}P(t) \quad (41)$$

$$Q_i \triangleq [\Sigma_{di} N_i (1 + L_i^2 B^2)]^{-1} \quad (42)$$

In this study the mean lifetime, ℓ , will be defined by the relation

$$\frac{1}{l} \cong \frac{1}{2.3} \left(\frac{1.3}{l_2} + \frac{1}{l_3} \right) \quad (43)$$

$$\frac{h}{l} \cong \frac{1}{2.3} \left(\frac{1.3 h_2}{l_2} + \frac{h_3}{l_3} \right) \quad (44)$$

The multiplication constant, k , will vary with temperature according to the relation

$$k \cong 1 - |\gamma_1|_1 \Theta + |\gamma_2|_2 \Theta \quad (45)$$

Also, to a first approximation, the power removal rate in the ceramic, for a fixed flow rate, becomes

$${}_{02}P(t) = {}_{02}P_0 (1 + h_2 \Theta) \quad (46)$$

[A better definition of l_2 and l_3 might be to use flux-averaged values:

$$l_2 \cong \frac{\sum_i^{1,2} l_i \Phi_i}{\sum_i^{1,2} \Phi_i} \quad (47)$$

$$l_3 \cong \frac{\sum_i^{1,2,3} l_i \Phi_i}{\sum_i^{1,2,3} \Phi_i} \quad (48)$$

Numerical values computed for both MgO and ZrO_2 ceramic blanket were similar and of the order

$$l_2 \cong 8 \times 10^{-6} \text{ seconds}$$

$$l_3 \cong 3.4 \times 10^{-4}$$

$$l \cong 1.4 \times 10^{-5}$$

The equations were linearized by making the following substitutions:

$$\frac{1P}{0.1P} \simeq 1 + x \quad (49)$$

$$\frac{2P}{0.2P} \simeq 1 + y \quad (50)$$

$$\frac{C}{P} = z \quad (51)$$

$$\frac{\dot{C}}{P} \simeq (\dot{z} + z \cdot \dot{x}) , (\dot{z} + z \cdot \dot{y}) \quad (53)$$

$$\frac{1P}{2P} = \text{constant} = \frac{b}{1-b} \quad (54)$$

The mathematical system then becomes

$$-\frac{\beta}{\ell} \simeq \dot{x} - \omega \lambda z + \left(\frac{1-\beta}{\ell}\right) \gamma_1 \theta - \left(\frac{1-\beta}{\ell}\right) \gamma_2 z \theta \quad (55)$$

$$\simeq \dot{y} - \omega \lambda z + \left(\frac{1-\beta}{\ell}\right) \gamma_1 \theta - \left(\frac{1-\beta}{\ell}\right) \gamma_2 z \theta \quad (56)$$

$$\frac{\beta}{\ell \omega} \simeq z \cdot \dot{x} + (\dot{z} + \lambda z) + \frac{\beta \gamma_1}{\ell \omega} \theta - \frac{\beta \gamma_2}{\ell \omega} z \theta \quad (57)$$

$$\simeq z \cdot \dot{y} + (\dot{z} + \lambda z) + \frac{\beta \gamma_1}{\ell \omega} \theta - \frac{\beta \gamma_2}{\ell \omega} z \theta \quad (58)$$

$$x - \frac{C_{s1}}{0.1P_0} \dot{\theta} = 0 \quad (59)$$

$$y - \frac{C_{s2}}{0.2P_0} \dot{\theta} - h_2 \theta = 0 \quad (60)$$

If the approximation that the ratio of precursor population to number density is assumed constant in Equations (55) through (60),

$$z = z_0 = \frac{C_0}{P_0} = \frac{\beta h}{\lambda l \omega} \quad (61)$$

then one obtains

$$\dot{x} + \frac{\gamma_1}{l} {}_1\Theta - \frac{\gamma_2}{l} {}_2\Theta = 0 \quad (62)$$

$$\dot{y} + \frac{\gamma_1}{l} {}_1\Theta - \frac{\gamma_2}{l} {}_2\Theta = 0 \quad (63)$$

$$x - \frac{C_{s1}}{b P_0} {}_1\dot{\Theta} = 0 \quad (64)$$

$$y - \frac{C_{s2}}{(1-b) P_0} {}_2\dot{\Theta} - h\Theta = 0 \quad (65)$$

where "b" is introduced as the fraction of total power generated in the ceramic.

For a step change in steam flow to the blanket, approximated by assuming a step change in the power removal rate from the blanket, the solution for ${}_2\Theta$ is

$${}_2\Theta \approx A \left[\frac{\alpha}{\alpha^2 + \delta^2} e^{-\alpha t} - (\alpha^2 + \delta^2)^{-1/2} \cos\{\delta t - \tan^{-1} \frac{\delta}{\alpha}\} \right] \quad (66)$$

where

$$A = \frac{-b(1-b)P_0^2 y_0}{\lambda C_{s1} C_{s2}} \quad (67)$$

$$\alpha = \frac{(1-b)h P_0}{C_{s2}} \quad (68)$$

$$\sigma^2 = \left[\frac{b r_1}{\lambda C_{s1}} - \frac{(1-b)k_2}{\lambda C_{s2}} \right] P_0 \quad (69)$$

$$\approx \frac{b r_1 P_0}{\lambda C_{s1}} \quad (70)$$

Equation 66 indicates that, to the approximations considered, the reactor is oscillatory, hence stable to fluctuations in blanket flow conditions. This, of course, is true only if the indicated temperature swing is less than the melting point of the blanket, and the fuel temperature response does not reach the liquidus temperature. Also, the effect of delayed neutrons would be to damp the oscillation in the real reactor.

The solution for a step change in blanket temperature is similar to Equation 66. Again, the reactor indicates a stable reaction to blanket variation -- however, again also, this is subject to temperature swing limitations.

If the equations are not linearized, but the fuel-blanket power removal systems are coupled, and the precursor population remains constant, then:

$$\dot{P} = \left[-\frac{\beta}{\lambda} - \frac{(1-\beta)}{\lambda} k_1 \theta + \frac{(1-\beta)}{\lambda} k_2 \theta \right] P + \frac{\beta}{\lambda} P_0 \quad (71)$$

$$C_{s1} \dot{\Theta}_1 = {}_1P - {}_{o1}P \quad (72)$$

$$C_{s2} \dot{\Theta}_2 = {}_2P - {}_{o2}P \quad (73)$$

$${}_1P = m {}_2P \quad (74)$$

$${}_1\dot{\Theta} \approx m \frac{C_{s2}}{C_{s1}} {}_2\dot{\Theta} \equiv m {}_2\dot{\Theta} \quad (75)$$

Equations (71) through (75) above reduce⁷ to the form

$$\frac{dP}{d\Theta} = \frac{(a_1 + b_1 \Theta)P + \frac{\beta}{2} P_0}{c_1(P - P_0)} \quad (76)$$

which can be solved by the method of isoclines. With an overall negative coefficient, the power will have the shape given below:

(no time dependence shown, however)

The coupling of the fuel and blanket power removal, indicated by Equation (74), therefore lends stability to the system within the limits imposed by the restricted fuel temperature swing. However, if so much reactivity

is added that at some time the effective temperature coefficient is positive, the relation becomes:

Operation and Control

From the brief study of reactor kinetics an indication of reactor behavior and control requirements can be obtained. Since neither freeze out nor burn out of the fuel pipes can be allowed, it would be necessary to use control rods rather than depend upon the temperature coefficient for inherent control. A further stabilizing measure would be to couple the blanket steam flow to the fuel power demand to alleviate difficulties associated with blanket temperature would be undergone by adjusting the steam flow to the blanket as the reactor were brought up to power.

Start up of the poisoned reactor would be similar except decay gamma heating would be utilized in place of external steam. The necessity of keeping the blanket above 1500°F arises from the samarium cross section, which has a maximum at about that temperature. The blanket temperature coefficient of reactivity would therefore be different above and below this temperature. Since $\beta_{xe} P_{xe}$ is ordinarily greater in magnitude than

$\beta_{Sm} P_{Sm}$, then a positive β_{Sm} rather than a negative one can, as shown in Equation (17) et eq., result in a negative blanket coefficient below 1500°F and a positive coefficient above. This could lead to some difficulty for the operator on start up, hence requiring a pre-heat of the blanket above 1500°F would be desirable.

Shut down can be obtained by insertion of control and safety rods, or in an emergency, by insertion of poisons directly into the circulating fuel. Temporary shut down can be accomplished by ramping the fuel power demand to zero followed by adjustment of the blanket temperature to a value slightly above 1500°F. The blanket can be maintained at this temperature by auxilliary cooling. Since the salt will be maintained molten by heat leakage from the blanket and reactor after heat, the reactor will go subcritical due to the greater negative reactivity insertion due to blanket cooling than the insertion of positive reactivity due to limited fuel cooling, if any.

References

1. ORNL- 55-1-76
2. ORNL-1925
3. ORNL 56-4-53
4. Glasstone and Edlund, "The Elements of Nuclear Reactor Theory", p.329-339, Van Nostrand, N.Y., 1952.
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CHAPTER VIII

ORACLE CALCULATIONS

The ORNL digital computer (ORACLE) was used to make parametric studies of several of the variables affecting the reactor being considered. The multiplication constant and the flux distribution for various proposed designs were calculated in this manner. Several other investigations were considered such as gamma heating calculations, measurement of temperature coefficient, etc., but could not be made because of lack of time.

ORACLE Code

A three-group, three-region code (1) (with shells) was used in all critical calculations. This code solves the multi-group diffusion equations of the general form

$$D_i \nabla^2 \phi_i - \sum_{R,i} \phi_i + \sum_{X,i-1} \phi_{i-1} = 0 \quad ,$$

with the boundary conditions that the neutron re-entrant current shall be zero at the outer boundary and that the flux and current shall be continuous at the boundary surfaces.

The three group calculations assumed a fast group from 10 Mev to 9118 ev which included all of the fission neutron yield spectrum. The intermediate group was 9118 ev to 0.14 ev, and the thermal group consisted of a Maxwell-Boltzmann distribution in which the most probable velocity of a neutron corresponded to 0.14 ev.

Fast groups constants were obtained by appropriate flux-averaging of cross sections over Eyewash (2) Code groups 1-7 ("fast") and 8-21 ("intermediate"). Thermal cross sections were calculated about 0.14ev (2600°F).

The code has provision for including shells between regions or for eliminating shells entirely. Both methods were used and the effect of an Inconel shell on the multiplication constant was determined in this manner.

One group, hand calculations were made to establish relative orders of magnitude to determine the size of the reactor. Machine calculations were then made to determine the effect of dimensional changes on the multiplication constants. In this manner, it was possible to make parametric studies on the effect of annulus size, fuel concentration, different moderators, and reflector thickness of both the reactivity and the flux distribution within the reactor. It was also possible to compare the relative effectiveness of an annular cylindrical reactor with a heterogeneous reactor composed of fuel-filled pipes evenly spaced in a ceramic moderator.

Initial Investigations of Annulus Geometry

The first problem of concern was whether or not a reactor with a ceramic moderator could be designed which would go critical in a reasonable size. A cylindrical annular arrangement was selected for the preliminary investigation with a central beryllium core, an annular fuel region, and a ceramic moderator region. A four mol % fused salt fuel was considered. Various arrangements were investigated in the first 32 ORACLE runs. Data for these calculations were tabulated in Table 8.1.

A number of interesting conclusions were drawn from these initial parameter studies. The most important of these was that a reactor of this type apparently can go critical and still not be unreasonably large. Secondly, zirconia as a reflector added about 3% more reactivity than alumina as a reflector for these cases. The results of some of these tests are shown in Figure 8.1 in which the multiplication constant is plotted as

ORACLE Data
(TABLE 8.1)(PAGE 1)

Test No.	R ₁		t ₁	R ₂		t ₂	R ₃		k	Disadvantage Factor			Comments
	cm	Material	cm	cm	Material	cm	cm	Material		L ₁	L ₂	L ₃	
1	45	Be(QMV)	.3175	8	4% fuel	.3175	97	Al ₂ O ₃	1.300				Shell constants incorrec
2	50						92		1.301				
3	55						87		1.302				
4	60			↓			82		1.302				
5	45			10			95		1.340				
6	↓			12			93		1.372				
7	↓			14			91		1.400				
8	50			10			90		1.341				
9	↓			12			88		1.373				
10	↓			14			86		1.398				
11	55			10			85		1.342				
12	↓			12			83		1.372				
13	↓			14			81		1.398				
14	60			10			80		1.342				
15	↓			12			78	↓	1.372				
16	↓			14			76		1.398				
17	45			8			97	ZrO ₂	1.338				
18	50			↓			92		1.339				
19	55			↓			87		1.339				
20	60			↓			82		1.339				
21	45			14			91		1.427				
22	50			↓			86		1.427				
23	55			↓			81	↓	1.427				
24	60			↓			76	↓	1.426				
25	14	(4% fuel)	↓	30	Be(QMV)	↓	106	Al ₂ O ₃	1.247				↓
26	45	Be(QMV)	0	8	4% fuel	0	97	↓	1.373				No shell
27	↓		0	↓		0	97	ZrO ₂	1.410				No shell
28	30		.3175	↓		.3175	112	Al ₂ O ₃	1.286				Shell constants incorrect
29	↓		↓	10		↓	110		1.327				
30	↓		↓	12		↓	108	↓	1.360	↓	↓	↓	
31	↓		↓	14		↓	106	↓	1.387				

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768
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TABLE B.1 PAGE 2

Test No.	R ₁ (cm)	Material	t ₁ cm	R ₂ cm	Material	t ₂ cm	R ₃ cm	Material	k	Disadvantage Factor	Comments
										1 2 3	
32	25	Be (QMV)	.3175	8	(4% fuel)	.3175	117	Al ₂ O ₃	1.082		
33								MgO	1.150		
34								ZrO ₂	1.170		
35								SiC	1.012		
36	20						122	Al ₂ O ₃	1.058		
37	25				3% fuel		117		1.016		
38					2% fuel		117		0.893		
39				6	4% fuel		119		0.987		
40				4			121		0.861		
41		P ₂ O ₅		8			117		1.058		
42	60	ZrO ₂		14			76	ZrO ₂	1.180		
43	68						68		1.172		
44	45						91		1.190		
45	70			10			70		1.058		
46		Al ₂ O ₃						Al ₂ O ₃	0.946		
47		MgO						MgO	1.106		
48	150	homog. MgO	0	5	BeC	0	5	BeO	1.110		4% fuel
49		ZrO ₂							0.987		in homog.
50		Al ₂ O ₃							0.811		region
51				15					0.829		
52				25					0.842		
53	140			5					0.795		
54	100			5					0.725		
55	150	SiC		5					1.100		
56	140	Al ₂ O ₃		5	Al ₂ O ₃			homog. Al ₂ O ₃	0.747		
57	2.54	4% fuel	.3175	13.5	ZrO ₂	.3175	134	.. ZrO ₂	0.959		
58	3.7			12.3					0.962		
59	4.3			11.7					0.962		
60	5.1			10.9					0.963		
61	70	SiC		10	4% fuel		70	SiC	0.921		
62	150	hom. ZrO ₂	0	5	BeO	0	5	BeO	1.051		5% fuel
63	8.7	5% fuel	.3175	12.3	ZrO ₂	.3175	134	hom. ZrO ₂	1.027		

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Table 8.1 Page 3

Test No.	R ₁		t ₁	R ₂		t ₂	R ₃		k	Disadvantage			Comments
	cm	Material	cm	cm	Material	cm	cm	Material		L ₁	L ₂	L ₃	
62	25	Be	.3175	8	4 % fuel	.3175	117	ZrO ₂	1.210				Shell constants A indicate further
63	↓	↓		↓	↓		↓		1.313				Shell constants B refinement needed
64	↓	↓		↓	↓		↓		1.386				No shell
65-75	Not applicable because of incorrect cross section data												
76	2.54	(6 % fuel)	0	13.56	MgO	0	133.9	hom. MgO	1.102	.577	1.075	1.26	No shell
77			.3175						1.100	.501	1.048	1.68	Shell constant C
78									1.100	.472	1.16	1.77	Shell constant A
79									1.003	.454	1.05	1.69	Shell constant C new disad. factor
80									1.101	.483	1.04	1.68	Shell constant B (n.g.)
81									1.198				Shell constant C (n.g.)
82	↓	↓		2.22	low density, MgO		11.02	MgO					
83	3.58			13.56	MgO		133.9	homog. MgO	1.269	.580	1.21	2.25	Same as 77 with increased fuel
84	2.54	(6 % fuel)		13.14	MgO		134	homog. MgO	1.154	.4655	1.056	2.094	
85	↓	↓	↓	13.14	ZrO ₂		134	homog. MgO	0.937	.4823	1.056	2.171	
86	140	hom. MgO	0	5	hom. MgO		5	hom. MgO	1.158				
87	150			5	BeO		5	BeO	1.190				
88				15	BeO		5	BeO	1.214				
89				25	BeO		5	BeO	1.230				
90				45	BeO		5	BeO	1.247				
91	↓	↓	↓	25	BeO		5	BeO	0.993				
92	2.54	6 % fuel	.3175	13.14	MgO		134	hom. MgO	1.151	.5066	1.024	1.874	
93	↓	4.8 % fuel	↓		MgO		134	hom. MgO	1.152	.490	1.024	1.874	4.8 % fuel in region 1 only
94	150		0	25	MgO		5	hom. MgO	1.202				
95	2.54	3.6 % fuel	.3175	13.14	MgO		134	hom. MgO	1.150	.5241	.994	1.656	3.6 % fuel in region 1 only

Figure 8.1

EFFECT OF FUEL ANNULUS SIZE ON MULTIPLICATION CONSTANT

$R_1 = 45 \text{ cm. Be (QMV)}$

$R_2 = \text{Fuel Annulus (4\%)}$

$R_3 = (105 - R_2) \text{ cm}$

a function of annulus size and nature of the reflector material. The curves show that the annulus volume or the fuel concentration could be reduced and that it would still be possible to have the reactor go critical.

Conclusions Drawn

At this point it was decided to eliminate the use of beryllium in the moderator if it were at all possible. Values for the physical properties of beryllia had indicated that it could not stand up to steam atmosphere at 3000°F without volatilizing. This meant that if this material were used it would have to be operated at temperatures of 2200°F or less. Also, a large portion of the neutron moderation heat would be deposited in the beryllia and this energy was to be maximized in the ceramic operating at high temperature.

The effect of reducing the size of the fuel-bearing annulus was to be investigated further. It was recognized that it would be difficult to physically construct a reactor 12 feet high with an annular region much smaller, but the effect of a smaller annulus was investigated as a part of the overall parameter studies.

A third decision made was to investigate the possibility of using other ceramic materials, namely SiC and MgO. Again it was known that silicon carbide probably could not have a long life in a steam atmosphere at temperatures above 2700°F but the problem of criticality was still not resolved for all possible geometric arrangements under consideration at this time.

A fourth conclusion was to investigate the possibility of using beryllia as a reflector surrounding the high temperature ceramic and thus reduce the critical mass.

Fifth, and most important from the point of radiation heating, an investigation was to be made to see whether the fuel could be concentrated in pipes

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instead of in an annular region. If this could be done, then it would be possible to moderate more of the fast neutrons in the ceramic region as well as permitting an increased number of fission gammas to leave the fuel region. This meant that there would be more capture in the Inconel, however, because of the greater amount of metal that would be required. Such captures would increase the heating in the ceramic, but would also increase the critical mass.

These five parameters were investigated in ORACLE tests 32-63. Input data and resulting k are shown in Table 8.1.

Comparison of Annulus and Pipe Geometries

The data show that the fuel concentration would probably have to be increased and a good moderator would be required if the reactor were to attain criticality with a pipe arrangement. The particular pipe lattice chosen was a triangular arrangement with the pipe centerlines on 12 inch centers.

The pipe-type arrangement did not result in as high a multiplication constant as did an annulus arrangement with the same amount of fuel, but was much more to be preferred if it would go critical, since the energy generated within the moderator was to be maximized. The results obtained for the different geometries thus showed the annulus arrangement was preferable from the standpoint of criticality but that the pipe arrangement was preferable for moderator heating.

The pipe arrangement was selected on the basis of later results.

Beryllium as an Inner Moderator

ORACLE runs 42-48 considered the cases where the beryllium in the inner reflector region (annulus arrangement) was replaced by the ceramic under

•

•

2

was known to have good reflector properties. Magnesia was selected because

avings for these two materials. Figure 8.2 illustrates their relative

Figure 8.2
FLUX DISTRIBUTION
ORACLE RUN 16

$R_1 = 70\text{cm}$ (MgO)
 $R_2 = 10\text{cm}$ (4% Fuel)
 $R_3 = 70\text{cm}$ (MgO)

Figure 8.3

Effect of Reflector Thickness on Multiplication Constant

Conclusion Drawn

A beryllia reflector should be used about the ceramic moderator region. It would probably have to be internally cooled so that it would not be exposed to temperatures greater than 2300°F.

Choice of Ceramic

The effectiveness of four ceramic materials was investigated in ORACLE runs 45-47, 62, 88, and 89, using an annular design. Figure 8.4 show the relative effectiveness of magnesia, zirconia, alumina, and silicon carbide graphically. While this geometric arrangement was later abandoned the relative effectiveness of the different ceramic materials is significant. In this investigation all variables except the ceramic moderator material were held fixed. As shown in case D, the highest multiplication constant was obtained when magnesia was the moderator. Since gamma and neutron heating calculations had already indicated that the fuel inventory would be large, the choice between the ceramics was based on their relative effectiveness as moderators.

Conclusion Drawn

Magnesia would be the most desirable moderator-ceramic of those materials investigated as far as criticality is concerned.

Effect of Fuel Concentration

ORACLE runs 27, 32, 88, 94 and 95 considered the effect of changing fuel concentration for both types of geometries considered. Typical results obtained in this case are shown in Table 8.2. As expected, increasing the fuel concentration resulted in a larger multiplication constant. Concentrations studied were limited to those which experimental investigations have shown to be feasible.

FIGURE 8.4

RELATIVE EFFECTIVENESS OF
CERAMIC MODERATORS AT 2600°F

Ceramic Moderator

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TABLE 8.2

Effect of Fuel Concentration On Multiplication Constant

CASE A		CASE B	
$R_1 = 25 \text{ cm (Be)}$		$R_1 = 2.54 \text{ cm Fuel}$	
$R_2 = 8 \text{ cm (Fuel)}$		$R_2 = 13.5 \text{ cm ZrO}_2$	
$R_3 = 117 \text{ cm (Al}_2\text{O}_3)$		$R_3 = 134 \text{ cm ZrO}_2, \text{ pipes, and}$	
		fuel homogenized	
Fuel			
(mol % U^{235})	k	(mol % U^{235})	k
2	0.89		
3	1.02	4	0.96
4	1.08	5	1.03

Fuel concentration studies in the pipe lattice geometry assumed that the disadvantage factor did not change with fuel concentration. This was not the case, but time did not permit a more detailed investigation.

Conclusion Drawn

A 5% fuel concentration would be required for the pipe-lattice geometric arrangement with magnesia as the moderator-ceramic. This would permit a maximum k of approximately 1.1.

Pipe Size

ORACLE investigations as to effect of pipe size on criticality were subject to error because of errors introduced in the homogenization of the regions outside a single lattice cell. Ultimately the choice of pipe size was determined from the maximization of moderator heating rather than from criticality consideration. Decreasing the pipe size increased the critical mass due to capture losses in the Inconel, although it also increased the gamma rays energy escaping into the moderator as well.

To maximize gamma heating however, it was decided to use the smallest possible pipe size that would maintain criticality.

Conclusion Drawn

Use two inch i.d. Inconel piping with a 1/8 inch wall thickness.

Calculation of Disadvantage Factors

Parametric studies made with tests 68-72, 75, 88, 89, 97, and 99, investigated the effect of fuel concentration and pipe size on the disadvantage factor, a_{eta} , which is a consequence of a heterogeneous cell arrangement. The results are shown in graphical form (Figure 8.5) based on average flux levels as calculated by the ORACLE. The disadvantage factor for the thermal flux increased with fuel concentration while the disadvantage

Figure 8.5

Apparent Disadvantage Factor Vs. Fuel Concentration

$R_1 = 2.54$ cm Fuel

$R_2 = 13.1$ cm MgO

$R_3 = 134$ cm
homog. MgO

(with 6% fuel in all cases)

Thermal
Region

Intermediate Region

Fast Region

Fuel Concentration in Mol Percent

Disadvantage
Factor

factor of the fast flux decreased slightly. The disadvantage factor for the intermediate flux was not materially changed with changes in the fuel concentration.

An attempt was made to evaluate the disadvantage factor for a single 2" i.d. tube (6% fuel) in a non fuel-bearing ceramic region of low and high density mangesia (test 82) but the resultant multiplication constant was so low that the ORACLE was unable to give a decipherable answer.

A rigorous examination of the disadvantage factors would have to be iterative in nature. First, a disadvantage factor has to be assumed, group constants evaluated based on this assumption, and a flux pattern obtained. A new disadvantage factor would then be obtained from the flux distribution and a new calculation made. A more exact disadvantage factor can be obtained in this manner. In this report only one iteration was made.

Shell Constants

Considerable difficulty was experienced in the evaluation of the effect of the shell constants on criticality. Tests 1, 17, 26, 27, 63-83, 88 and 96 were all primarily concerned with the investigation of the effect of the shell constants on k . An assumption inherent in the three-group, three-region code is that there is at least one scattering collision in the shell. To simplify the calculations, it was also assumed that there was isotropic scattering in the shell. In spite of this, an anomaly occurred wherein neutrons appeared to be generated in the shell. This is seen in Figure 8.6 for the intermediate flux.

The conclusion which was finally drawn after the extended investigation of the shell constants was that the presence of the shell reduces the multiplication constant but that the magnitude of the reduction could not be

fully determined. It was further concluded that the nature of the shell constants used in the code were not fully understood by the writers here, and hence could not be properly evaluated.

Lattice Cell Studies

A thorough investigation to optimize the lattice structure on the basis of nuclear considerations was not made. It was found necessary to obtain the lattice structure by considering the thermal stresses and gamma heating maximization first and then adjusting the nuclear parameters (i.e., fuel concentration) to this arrangement. Flux distribution patterns were obtained for several proposed arrangements. Figure 8.6, for example, shows the distribution for a triangular 12" pitch lattice with 2" i.d. pipes. Gamma heating calculations were also carried out for this geometry. It is not claimed that the triangular pipe arrangement would be the most effective arrangement; however, if chosen the results given in this report should be valid.

Homogeneous Reactor Calculations

A number of ORACLE calculations were made in which the fuel, ceramic moderator, and pipes were homogenized. The resultant multiplication constants were higher than would actually exist because the proper disadvantage factors were not used. Figure 8.7 shows the effect of a 30 cm BeO reflector on a homogenized reactor. It is seen that there is an appreciable flattening of the thermal flux but that the reflector has little effect on the fast or intermediate flux.

Other Calculations

Gamma Heating: Heat generation rates in the ceramic due to gamma heating were obtained through hand calculations. It was felt that these hand calculations (based on a simplified math model) were sufficiently accurate for this study.

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FIGURE 8.7

FLUX DISTRIBUTION

ORACLE RUN 89

$R_1 = 150\text{cm}$ Homogenized MgO , pipes & 6% Fuel

$R_2 = 30\text{cm}$ BeO Reflector

Temperature Coefficient of Reactivity: It was possible to calculate the effect of temperature changes on reactivity using the three-group, three-region code. This could be done by adjusting the cross sections as a function of energy. This was not done because of time limitations and is merely mentioned as one of the studies which would have been carried out if there was a more extensive investigation.

REFERENCES

1. ORNL CF-55-1-76, "Description and Operating Manual for Three-Group, Three-Region Code for the ORACLE".
2. ORNL-1925.

CHAPTER IX

PHYSICAL AND NUCLEAR PROPERTIES OF CERAMIC MODERATORS

Introduction

One of the problems encountered in this investigation was the proper choice of a ceramic material for the process heat reactor. Normally, a ceramic is used for high temperature applications. This is certainly true in furnace wall linings, rocket nozzles, crucibles, and other similar applications where temperatures greater than 1000°F are encountered. Although a ceramic was chosen in the present instance, its high temperature resistance was only one of several properties which the material must have.

It was found convenient to evaluate the various ceramic materials considered in three primary categories, physical properties, nuclear properties, and gamma and neutron heatability. These are discussed in detail as they affect the problem. The need for knowing the physical and nuclear properties of the ceramics is obvious. The gamma and neutron heating properties became especially significant when the decision was made to maximize this source of heat energy and use it as the superheat.

Ceramic-Clad Fuel Elements

The most desirable arrangement from the thermal and nuclear point of view would have been to have the fuel embedded in, coated with, or combined with a ceramic material. A sort of pebble bed arrangement could be envisioned as a possible arrangement. An alternate method would have been to put fissionable material in a ceramic. In this manner roughly 185 MW of the 200 MW per fission would be generated inside a ceramic and could be transferred by thermal conduction to the surface of the pellets and thus to the gas.

These proposals were recognized as the most desirable conditions insofar as the generation of heat and the subsequent transfer to the gaseous medium (steam) were concerned. Unfortunately, the technology of ceramics has not advanced to the point where this method could be used.

The condition that there was to be no leakage of fission products to the steam ruled out the possibility of cladding the fuel of any fissionable material with a ceramic. One possibility was considered of maintaining a small positive flow of steam through a porous ceramic fuel element. This small flow of steam would be separately mainfolded and would carry off the fission product gases. A fuel element of this type was not explored in detail at this time because of the earlier stipulations and because the ceramics investigated were already near their permissible maximum thermal from heat generation by radiation heating alone. Other complications were also recognized such as maintaining a positive flow, porosity, and mechanical design limitations.

Gaseous Fission Product Leakage

No suitable ceramic has been developed as yet which is known to be gas tight at temperatures on the order of 2500-3000°F. At these temperatures a number of the fission product elements will be gaseous and would contaminate the steam unless otherwise prevented. The most notable of these would be iodine, krypton, xenon, and ruthenium.

Experimental studies on the diffusion of Xe^{133} in a MgO-Ni cermet showed gaseous diffusion to increase exponentially with temperature.¹ The mechanism of diffusion is apparently of the vacancy interchange type occurring along grain boundaries. Apparently all known ceramics are porous to some extent with the gas leakage always increasing with temperature.

The recently developed KT silicon carbide has a low porosity with discontinuous holes so that it is impermeable under most conditions although little data were available at elevated temperatures. There is still approximately 5% free silicon remaining in the silicon carbide², which would probably flow and increase the porosity at 3000°F. This, and other considerations precluded the use of KT silicon carbide for this reactor. It is clearly evident that if a gas-tight ceramic can be developed or if some degree of leakage can be tolerated that it is eminently more desirable to have the fuel in the ceramic. At the present stage of development, it is felt that this is not the case and accordingly the reactor was designed with the fuel in a metal-clad region, the metal being in a relatively low temperature region.

Physical Properties of Ceramics

The physical properties of the ceramics which were of primary importance in the initial calculations included melting point, thermal conductivity, density, chemical resistance and mechanical strength. These, of course, are not the only considerations but served to eliminate many otherwise suitable materials.

Chemical Resistance

The coolant used in the reactor being steam, it was necessary to have a ceramic material having good resistance to chemical reaction. This eliminated graphite as a possible ceramic.

The possibility of coating graphite with a ceramic film such as molybdenum disilicide was considered but discarded because of the lack of experimental information. Tests to date indicate that thin films that are non-porous, physically strong and resistant to steam at 3000°F have not been developed.

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Most carbides with the exception of SiC were discarded because of the poor chemical resistance at temperatures above 2500°F; SiC seems to be limited to temperatures below 2900°F, primarily because of an SiC-SiO₂ reaction in the presence of water vapor giving off CO and SiO gaseous vapors.

Most oxides have excellent stability in oxidizing atmospheres and are considered to be most satisfactory for high temperature applications.

BeO was considered for certain ceramic parts of this reactor even though it volatilizes in the presence of steam at temperatures greater than 2600°F (1400°C). BeO is quite suitable at temperatures below 1500°F (800°C) and was considered mainly as a reflector material.

Melting Temperatures

The ceramic should have a melting temperature at least 500°F higher than the maximum steam temperature if it is to have sufficient physical strength at the operating temperature. This means that the melting point should be at least 3500°F and preferably higher. This eliminated a large number of ceramics from consideration.

Thermal Conductivity

Unfortunately while oxides have good oxidation resistance, they usually have relatively lower thermal conductivities than other refractory materials. Also the effect of radiation at room temperature is to reduce the thermal conductivity of ceramics. The more complex materials are more susceptible to radiation damage than simple materials. This consideration influenced the choice of ceramic to one of a fairly simple structure.

Table 9.1 shows experimental measurements which have been made on several ceramics. There is reason to believe, however, that much of the radiation damage which results in a reduction of thermal conductivity is

annealed out at high temperatures and that the thermal conductivity of pure materials is a function of temperature only.

TABLE 9.1
THERMAL CONDUCTIVITIES OF IRRADIATED CERAMICS (3)
at 35 - 45°C

Thermal Conductivity (10^{-4} cal. sec $^{-1}$ C $^{-1}$)

<u>Material</u>	<u>No. Irradiation</u>	<u>Nvt 2×10^{20}</u>
Single crystal Al_2O_3	300	300
Hot-pressed Al_2O_3	300	300
BeO	300	300
Spinel ($MgO-Al_2O_3$)	170	100
Forsterite ($2MgO-SiO_2$)	180	43
Mica	21	13
ZrO_2	33	25
Zircon No. 475	120	21

The assumption is made, therefore, that irradiation does not materially affect the thermal conductivity of simple ceramic structures at the reactor temperatures considered here.

Commercial Oxides

Table 9.2 lists the physical properties of commercial refractories which were of primary interest in the choice of ceramic. The four most promising

TABLE 9.2

(4)
PROPERTIES OF COMMERCIAL REFRACTORY OXIDES

Material	Formula	Melting Point °C	Limit of usefulness in Oxidizing atmospheres °C	Coefficient of Thermal Expansion		Thermal Shock Resistance				Stability		
				°C	Ex10 ⁷	Temp. Foros-C x °C	ity %	10 ⁴		Carbon	Metals	Reducing atm.
Alumina	Al ₂ O ₃	2015	1950	(20-1580)	80	1200	23	57	Good	Fair	Good	Good
Beryllia	BeO	2550	2400	(20-1400)	95	1200	5-10	393	Excellent	Excellent	Good	Excellent
Calcia	CaO	2600	2400	(20-1200)	136	1000	9	170	Fair	Poor	Fair	Poor
Magnesia	MgO	2800	2400	(20-1400)	140	1200	22	61	Fair	Good	Fair	Poor
Silica (crystalline)	SiO ₂	1728	1680	(300-1100)	30	1100		38	Excellent above 370	Poor	Poor	Poor
Zirconia	ZrO ₂	2600	2500	(20-1200)	55	1200	28	22	Fair	Fair	Good	Good

were beryllia, alumina, magnesia, and zirconia. Table 9.3 lists the physical properties of these and other ceramics considered which might be suitable for lower temperature applications, burnable poisons, control rods, etc. A general summary follows of five refractory ceramics: zirconia, beryllia, magnesia, alumina, and silicon carbide.

Beryllia

Beryllia has good thermal stability and a high resistance to reduction. It has a high thermal conductivity and a uniform expansion making it very resistant to thermal shock. Its mechanical properties remain fairly constant with temperatures up to 2900°F.

The toxicity of beryllia makes it necessary to take extra precautions to prevent the inhalation of dust or fumes.

Beryllia can be fabricated by ramming, casting, pressing, and extrusion. Essentially non-porous beryllia refractories can be produced by a process of sinter recrystallizations.

Beryllia has a relatively low vapor pressure but is readily volatilized by water vapor at temperatures above 2950°F.

Magnesia

Magnesia with a melting point of 2800°C (500°F) has a high thermal expansion but in oxidizing atmospheres can be used at a higher temperature than alumina. Because of its high vapor pressure, it cannot be used in vacuum above 1600-1700°C (3100°F).

MgO has a tendency to hydrate in contact with moisture, but this has been overcome by a high temperature "dead burning" process which develops a stable form of magnesia. "Dead-burned" grain magnesite is used in the construction of the hearths and bottoms of basic open-hearth steel furnaces. Pulverized MgO of greater than 99% purity is commercially available.

ENCLOSURE

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Table 9.3

Physical Properties of Ceramics

Material	Melting Point or Dissociation temp. °F	Specific Gravity	Thermal Coeff. of Expansion linear 10 ⁶ * (0-100°C)	Thermal Conductivity BTU/in/sec/°F x 10 ⁻⁴	Modulus of Elasticity E 20°C (kg/cm ² x 10 ⁻⁶)	Tensile Strength kg/cm ² 20°C	Compressive Strength	Poissons Ratio	Safe Continuous Operation °F	Hardness (Knoop Scale) kg/mm	Specific Heat
MgO	5100	3.65	10.5*	.78(2552°F)	2.8	210		.03	4300		.23
ZrO ₂	4900	5.73	3-6	.274(1800°F)	1.76	1250	200 @ 1500°C		4200		.15
Al ₂ O ₃	3750	4.0	8*	.728(2552°F)	3.3	464 @ 1300°C	1000 @ 1500°C	.32	3540	2000	.17
SiC	4160	3.17	4.5*	2.4(2012°F)				.3	2750	2500	.20
BeO	4500	3.02	5-8*	2.0(2190°F)	3.9	10 @ 1300°C	1200 @ 1500°C	.38			.29
Be ₂ C	3850	2.44	15								
SiO ₂	3100	2.32	7	.277(2400°F)					2950		.18
CaO	4700	3.32	8	.016				.17	4300		
ThO ₂	5500	9.69	9*	.28(1472)	1.26		100 @ 1500°C	.32			.06
SiN	5300		6.93								
B ₄ C	4350	2.5	5			1580	18,000		2650	2250	

Magnesia is generally not troubled with crystalline inversions. It is considered less stable than alumina in contact with most metals.

Small amounts of impurities in MgO decrease its strength appreciably at high temperatures. The strength and rigidity of MgO is inferior to those of alumina and zirconia at high temperatures.

MgO can be fabricated by slip casting, extrusion, pressing and by fusion. It decreases in strength and rigidity about 1000°C and develops serious plasticity above 1300°C (2370°F). While it is inferior in these respects to alumina and zirconia, it is still superior to most other common oxide refractories.

At very high temperatures both magnesia and zirconia are superior to alumina in compressive strength. Table 9.4 gives the limiting temperature for load as follows:

TABLE 9.4

Material	Total Porosity	Failure Temp. (°F)* for Load of 40 psi
Al ₂ O ₃	23	3110°F
MgO	22	4380
ZrO ₂	26	3540

* 10% deformation

Zirconia

Zirconia has a low thermal conductivity and a high fusion point (2677°C) which makes it an excellent material for the construction of high temperature

furnaces. It is stable in oxidizing and moderately reducing atmospheres. In the pure state, zirconia inverts in crystalline structure which causes large volume changes where it is heated or cooled. Zirconia can be stabilized to give moderately good thermal shock resistance by the addition of small amounts of calcia or magnesia with only a slight loss in refractory properties. Commercial stabilized zirconia contains approximately 5% CaO to stabilize the crystalline form and has a melting point of approximately 2500°C (4530°F).

Stabilized zirconia is available as a porous grain (50% porosity) with a bulk specific gravity of 2.5, or as a dense grain of electrically fused material with a bulk specific gravity of 4 - 4.4.

Stabilized zirconia can be fabricated by slip casting, ramming, dry pressing or extrusion.

Alumina

Alumina has excellent stability in oxidizing and reducing atmospheres and is the most used pure oxide. It has a melting point of 3750°F and has been used in shapes from small laboratory crucibles to massive furnace parts. Its thermal conductivity is not quite as good as beryllia but is superior to that of magnesia. Alumina has excellent resistance to attack by steam.

Alumina can be fabricated by slip casting, extruding, or pressing. It has been used to make a large variety of shapes of all sizes.

At very high temperatures the compressive strength of alumina is lower than either zirconia or magnesia. Insulating brick of high porosity have been used for thermal insulation where high temperatures and reducing atmospheres prohibit insulating firebrick.

Silicon Carbide

Silicon carbide is extremely hard and has very high strength as well as good thermal conductivity. The recently developed KT silicon carbide is

said to be essentially impermeable due to a discontinuous arrangement of holes. It is almost wholly silicon carbide, with less than 5% free silicon.

KT silicon carbide can be formed by pressing, extruding, or casting. The pieces do not change dimensionally on firing, and little difficulty is experienced in making intricate shapes. This material is resistant to oxidation, high heat shock, chemical attack and is highly impermeable.

Nuclear Properties

In the selection of ceramic materials for a high temperature reactor, one is limited to a very few materials which possess suitable nuclear as well as physical properties. Normally one thinks of ceramics only in terms of their high temperature physical properties. At temperatures above 2800°F, the number of refractories is fairly limited for one reason or another. In the presence of water vapor, the number of ceramic materials which will stand up is still further reduced. If on top of this, one stipulates the nature of the nuclear properties, then the applicable ceramic is almost unique.

Absorption Cross Sections

Table 9.5 lists the macroscopic thermal cross section of selected ceramic materials which have relatively low absorption cross sections. Since the ceramic must moderate the neutron flux, it is important that the losses due to absorption be small if the available reactivity is limited. BeO has the lowest thermal cross section while MgO is almost five times larger and alumina fifteen times higher.

Neutron Effects on Ceramics

In general, neutron bombardment on ceramics results in an atomic interaction with the degree of interaction depending on the individual cross

TABLE 9.5

Thermal Neutron Absorption Cross-Sections of Selected Ceramics *

Material	$N \sigma_a$ cm^2/cm^3
BeO	.0007
SiC	.0116
MgO	.0033
Al_2O_3	.010
ThO_2	.132
CaO	.182
SiO_2	.0064
ZrO_2	.0186
BaO	.028
SrO	.032

* thermal (0.14 ev)

sections of the component materials. Some work has been done which indicates that there is a fast-neutron effect on silica structures in which the silicon-oxygen bond is particularly vulnerable. However, studies made with fission-spectrum neutrons on zirconium indicate that this effect is not as pronounced as indicated above even after flux bombardments as high as 3.6×10^{20} fast neutrons/cm².

X-ray examination of ceramics indicates little change in structure in alumina, spinel, forsterite and zirconia, whereas a slight expansion takes place in zirconium, silicon carbide, and BeO¹. Zirconium also suffers a loss in crystallinity.

Diffusion Properties

The diffusion properties of the ceramics of most interest are shown in Table 9.6. Diffusion coefficients were determined from the fission spectrum energies to 0.14 (2500°F) ev. Below this energy level the neutrons were assumed to be thermalized since the moderator was assumed to be at high temperature.

Neutron Moderation

Since a significant portion of the heat generated in the ceramic arises from the moderation of the high energy neutrons, i.e., from the fission energies down to the Kev region, it would be desirable to have a ceramic composed of high atomic number elements to help distribute this energy uniformly. In this energy range heavy elements are the most effective inelastic scatterers. The minimum excitation energy, the energy of the lowest excited state above the ground state, is usually greater than 0.1 Mev for moderate high mass elements. Hence these elements can only scatter inelastically neutrons having a higher energy than this. An inelastic

TABLE 9.6

NUCLEAR DIFFUSION PROPERTIES OF CERAMIC MODERATORS

Material	L	M	D_F	D_T^*	Σ_{aT}^*	Fermi Age
	(cm)	(cm)	(cm)	(cm)	(cm ⁻¹)	(cm ²)
BeO	48	50	.714	.647	2.76×10^{-4}	137
MgO	36	44	1.19	1.18	8.97×10^{-4}	615
Al ₂ O ₃	21	33	1.27	1.30	2.94×10^{-3}	647
ZrO ₂	25	36	1.11	1.12	1.80×10^{-3}	654
SiC	28	39	1.52	1.43	1.83×10^{-3}	753

* Thermal energy at 2500°F (0.14 ev)

scattering collision results in the emission of a gamma ray which is absorbed in the ceramic generating heat in the process.

A good moderator is one in which there is a considerable decrease in neutron energy per collision and is measured by the slowing down power. Table 9.7 lists the slowing down power of the four ceramic materials of interest. It is evident that beryllia has the largest slowing down power and there is not too much difference between the other materials.

Although the slowing down power gives a satisfactory indication of the moderation of neutrons, it ignores the absorption of neutrons by the ceramic. The moderating ratio, $(\frac{\xi \Sigma_s}{\Sigma_a})$, is a more accurate measure of the effectiveness of the moderator. Table VII shows that next to BeO, MgO is clearly the most effective of the remaining ceramics in moderating neutrons with a minimum of neutron absorption.

A good moderator is also necessary to reduce the fast neutron leakage from the reactor. If the moderator is too effective, however, most of the neutrons will be slowed down near the fuel and the neutron heat generation will be peaked in this region. It is desirable from the heat removal point of view to moderate high energy neutrons throughout the ceramic so as to spread out the energy. The highest moderating ratio, which is usually strived for in conventional reactors, is not the primary consideration here. It is important to spread out the heat generation in the ceramic. In this respect, the proposed reactor is fundamentally different from conventional reactors.

The less effective the ceramic is as a moderator, the greater will be the fast neutron leakage. This may be minimized to some degree by putting

TABLE 9.7

SLOWING DOWN PROPERTIES OF CERAMIC MODERATORS

Moderator	Slowing Down Power* (cm ⁻¹)	Moderating Ratio
BeO	.0911	1400
MgO	.0282	188
Al ₂ O ₃	.0285	54
ZrO ₂	.0267	80
SiC	.0319	126

* To 0.14 ev (2600°F)

a reflector outside the ceramic moderator region. BeO would be an excellent material for this purpose.

Gamma Heating

The main source of heat in the ceramic is in the degradation of gamma rays, whether they originate from the inelastic scattering of fast neutrons, prompt fission gammas, or capture gammas in the fuel and ceramic. The gamma attenuation concentrates the heat generation near the sources of the gamma rays. In this case, this will be in the regions nearest the fuel where the primary and delayed fission gammas originate.

Table 9.8 shows the energy deposited in the ceramic as determined by the energy absorption coefficient, μ_a , for 1 Mev gamma rays. The energy deposition is a function of the gamma ray energy so the table is meant to be indicative only. At low gamma energies all the energy will be absorbed in the immediate region but at higher energies, the energy is spread out. Table 9.8 lists the absorption constants for the materials considered in this reactor and also tabulates μ_a/ρ if different density materials are to be used.

Summary

The physical and nuclear properties of a ceramic must be considered in the selection of a proper ceramic material. The most important of the properties are:

- 1) Moderate physical strength at 3000°F.
- 2) Chemically resistant to steam at 3000°F.
- 3) Low thermal neutron absorption, depending on reactivity available.

TABLE 9.8

Energy Absorption Coefficients for 1 Mev Gamma Rays *

Material	For Pr Ht Rctr Conditions	$\frac{\mu_a}{\rho}$	$\frac{(\text{cm}^2)}{(\text{gm})}$
	$\mu_a (\text{cm}^{-1})$		
ZrO ₂	0.021	0.0249	
Al ₂ O ₃	0.0935	0.0276	
MgO	0.083	0.0268	
SiC	0.0752	0.0280	
BeO	0.069	0.0270	

* Assuming photo-electric and pair production interactions to be pure absorption.

- 4) Good fast neutron attenuation.
- 5) Good neutron moderation.
- 6) Good gamma ray attenuation.
- 7) Resistant to radiation damage.

Other considerations such as availability, cost, manufacture, etc., are, of course, important but are not considered in detail.

Beryllia has the most desirable nuclear properties but volatilizes in the presence of steam at elevated temperatures. It would be eminently suitable as a reflector material, however, and is considered for this purpose.

Of the other materials, SiC also must be discarded for physical reasons because of its short life at 3000°F.

Zirconia and magnesia are similar in most physical properties. Magnesia is better moderator while zirconia is the better gamma ray attenuator. The selection of either material depends on whether neutron heat or gamma ray heat is to be maximized. A possible compromise might be to use both materials.

From the point of criticality and critical mass, magnesia would be more desirable as the moderator. Less fuel would be required if magnesia were used. This and the other factors previously mentioned, led to the selection of magnesia as the ceramic for the present process heat reactor.

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