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ADVANCED BERYLLIUM OXIDE CONCEPTS
PROGRESS REPORT

for the period April 1 through June 30, 1963

RELEASED FOR ANNOUNCEMENT
IN NUCLEAR SCIENCE ABSTRACTS

U. S. Atomic Energy Commission
Contract AT(04-3)-187

September 17, 1963

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GENERAL ATOMIC
DIVISION OF
GENERAL DYNAMICS

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I. INTRODUCTION

Studies were initiated in October, 1962, under U.S. Atomic Energy Commission Contract AT(04-3)-187, on the potential of beryllium oxide as a reactor moderator material and on advanced reactor concepts utilizing beryllium oxide. This progress report contains a summary of the technical accomplishments for the period April 1 through June 30, 1963.

The purpose of the advanced reactor concepts part of this activity is to give technical orientation to the utilization of the Experimental Beryllium Oxide Reactor (EBOR) as an experimental device within the Commission's beryllium oxide experimental program. Particularly it is to provide a basis for judgment as to selection, scheduling, and evaluation of the various moderator and fuel materials to be tested in EBOR.

URANIUM-THORIUM REACTOR

The primary interest during the quarter was again on the uranium-thorium reactor. As noted in the previous quarterly report, the uranium-thorium cycle coupled with beryllium oxide moderation in a reactor with a power output of 500 electrical megawatts showed a definite breeding potential. However, breeding systems do not give the lowest fuel-cycle costs. The extension of the excellent neutron economy offered by beryllium oxide moderation to smaller cores with lower power outputs was indicated. The work during this period has therefore been concentrated on a 200-megawatt (electrical) reactor using a beryllium oxide spine, $\text{BeO-UO}_2\text{-ThO}_2$ fuel compact, and a graphite sleeve. The preliminary engineering-design work is presented together with an estimated capital cost for the plant. The physics work was concentrated on the determination of the fuel-cycle costs, and the materials effort centered on the determination of the fuel-fabrication costs for the plant.

PLUTONIUM REACTOR

The second area of interest centered on the plutonium-fueled system. As noted in the last quarterly report, the proper use of a combination of the plutonium isotopes for fuel required that the neutron spectrum in the reactor be suitably adjusted so as to have the macroscopic capture rates in Pu^{239} and Pu^{240} approximately equal. A homogeneous concept was developed

which appeared to yield excellent burnup-lifetime characteristics and a large prompt negative temperature coefficient, and in addition, used fuel which had isotopic concentrations typical of singly-cycled fuel in low-enrichment uranium reactors. The reactor had linear dimensions of five to six feet (length and diameter) and had a power rating of 50 to 70 thermal megawatts.

Efforts during this period were concentrated on determining the feasibility of adding small volume fractions of zirconium hydride to the core in order to achieve similarly good lifetime and temperature-coefficient characteristics from a smaller core. The work to date indicates that, although it appears possible from the engineering standpoint to have such a mixed moderator system, the ZrH_n being located heterogeneously but dispersed homogeneously throughout the core, the required reactor physics characteristics are not easily obtained. Further study in this area is required prior to establishing feasibility in all areas.

2. URANIUM-THORIUM REACTORS

ENGINEERING

Most of the effort was concentrated on a 200-megawatt (electrical) helium-cooled reactor with semihomogeneous fuel elements consisting of a fuel compact of $\text{UO}_2\text{-ThO}_2\text{-BeO}$ inside a graphite sleeve, with an inner beryllia spine. The influence of several parameters on the core design was studied, and a conceptual design was established. An estimate of the capital cost for this plant was prepared according to basic AEC ground rules.

Core Design

The values of the parameters or the range of parameters used in the design are shown in Table 1. The values given for thermal conductivities are before irradiation. Table 2 gives values of the relevant properties for graphite, oxide fuel compact, and BeO before and after irradiation. The relative coolant pressure drop ($\Delta p/p$) for the active core is about 1 percent. If not otherwise specified, the average coolant pressure is 30 atmospheres.

The influence of several parameters on core design was studied for a 500-megawatt (thermal) reactor. The nominal maximum fuel-compact temperature was fixed at 1700°C , but the actual temperature distribution in the fuel element was calculated for a few cases using actual gaps, variable thermal conductivity, heat transfer by thermal radiation, and irradiation effects on thermal properties. It was assumed that there is no cross flow and that the mass flow is the same in all channels.

Table 3 shows the variations of fuel-element and core dimensions versus the average core power density for a nearly constant core length-to-diameter ratio. The lowest coolant void fraction corresponds to about 6 kilowatts/liter, while 15 kilowatts/liter gives a fuel-compact thickness of 1/2 inch, which is close to the minimum desirable value. Core length and diameter as well as fuel-element diameter all decrease with increasing power density, while the number of fuel elements increases. The maximum thermal stress in the sleeve is proportional to the maximum surface heat flux and remains much smaller than the rupture tensile stress for graphite.

Table 1

RANGE OF PARAMETERS USED IN THE DESIGN OF THE
200-MW(E) REACTOR

Average core power density	5-25 kw/liter
Coolant void fraction	9.35-25%
Thickness of graphite sleeve, e	1/4 in.
Fraction of BeO mixed with fuel	1/2
Total thermal power	500 Mw
Axial maximum/average flux	4/3
Radial maximum/average flux	5/4
Thermal conductivity of sleeve	0.52 w/cm-°C
Thermal conductivity of fuel compact, K_F	0.13 w/cm-°C
<u>Temperature drop in sleeve-compact gap^a</u>	3.0
Temperature drop in sleeve	
Nominal maximum temperature of fuel compact ^a	1700°C
Helium-coolant inlet temperature	350°C
Mixed mean coolant-outlet temperature	750°C
Coolant temperature rise in hot channel, ΔT	500°C
Average coolant pressure	30 atm
<u>Pumping power corresponding to active core length.</u>	0.5%
Thermal power in fuel channel	

Dimensionless number:

$$\frac{N_s^3}{F} \rho_i \rho_{av} C_p^3 \left(\frac{e^2 \Delta T}{K_F^2} \right) = 50$$

N_s = average Stanton number for hot channel

F = average friction factor for hot channel

ρ_i = reactor inlet-coolant density

ρ_{av} = average coolant density in hot channel

C_p = coolant specific heat at constant pressure.

Fuel-element length	8-16 ft
Fuel-element diameter	3-6 in.
Fuel-compact thickness	0.4-1.0 in.

^aExcept for Table 4 where actual values were computer calculated.
(see Figures 1 and 2).

Table 2

PROPERTIES OF GRAPHITE, BERYLLIA, AND FUELED BERYLLIA
BEFORE AND AFTER IRRADIATION
AT A TOTAL EXPOSURE OF 10^{21} NVT (> 1 MEV)

Material	Thermal Conductivity, w/cm-°C	Thermal Expansion, cm/cm-°C	Dimensional Change, % of Diameter
Graphite sleeve, impregnated			
nonirradiated	0.52	5.4×10^{-6}	0
irradiated	0.43	5.4×10^{-6}	-0.1
BeO with additives for nuclear use			
nonirradiated	0.17 at 1600°C	10.8×10^{-6}	0
irradiated	0.16	10.8×10^{-6}	+0.2
Fueled BeO 20 vol-% oxide fuel			
nonirradiated	0.13 at 1600°C	10.8×10^{-6}	0
irradiated	0.12	10.8×10^{-6}	+0.2

Table 4 studies the effect of fuel-element length on a core of given power density (15 kilowatts/liter average). The coolant void fraction is proportional to the fuel-element length, while the fuel-element diameter decreases only slightly with increasing length. The maximum surface temperature of the fuel element remains nearly constant (about 1000°C), as well as the maximum surface heat flux (about 70 watts/centimeter²). The number of fuel elements is nearly decreased by a factor of two by doubling the fuel-element length, while the core diameter is decreased by 40 percent.

Table 5 shows the influence of power density on a core of given length (15.5-foot active length). Doubling the power density corresponds to about doubling the coolant void fraction and halving fuel-element diameter and fuel-compact thickness. The maximum fuel temperatures stay nearly constant, while the number of fuel elements increases by about 40 percent and the core diameter decreases by about 40 percent. The maximum surface heat flux only increases by 30 percent by doubling the core power density.

Figure 1 shows the variations of the maximum fuel-compact temperature versus the thickness of the initial cold gap between graphite sleeve and compact for several power densities. This gap decreases at operating temperature and after irradiation due to the differential thermal-expansion coefficients of beryllium oxide and graphite and also because of dimensional changes due to radiations (see Table 2). Figure 2 shows these variations in gap dimensions for the 16-kilowatt/liter fuel element of Table 5. The maximum fuel compact temperature allows for an additional 100° to 150°F at the hot spots. The BeO spine temperatures for Table 5 are, at most, 3600°F. The initial cold gaps indicated in Table 5 are such that they become

Table 3
200-MW(E) He-COOLED BeO-MODERATED REACTOR
(Core length/diameter $\cong$ constant)

Average core power density, kw/liter	6.4	9.6	12.8	16
Coolant void fraction, %	10	12.25	14.15	15.8
Outer diameter of fuel element,				
cm	17.8	13.8	11.6	10.0
in.	7.0	5.425	4.59	3.93
Thickness of fuel compact,				
cm	2.44	1.90	1.59	1.27
in.	0.96	0.75	0.625	0.50
Diameter of BeO spine,				
cm	11.4	8.6	7.1	6.2
in.	4.5	3.345	2.76	2.42
Length of fuel element,				
m	5.10	4.66	4.25	3.92
ft	16.7	15.3	13.9	12.85
Power in hot channel, Mw	1.13	0.92	0.795	0.665
Maximum power/unit length,				
w/cm	2950	2630	2460	2250
Btu/hr-ft	3.07×10^5	2.74×10^5	2.56×10^5	2.34×10^5
Maximum surface heat flux,				
w/cm ²	53	60.5	66.8	71.5
Btu/hr-ft ²	1.68×10^5	1.91×10^5	2.12×10^5	2.26×10^5
Fraction of C in core, %	12.5	15.5	18	21.5
Maximum thermal stress in sleeve				
Rupture tensile stress in C	0.144	0.165	0.18	0.193
Number of fuel elements ^a	555	680	785	940
Active core diameter, ^a				
m	4.40	3.8	3.45	3.2
ft	14.5	12.5	11.3	10.5
Core length/diameter ^a	1.155	1.23	1.23	1.22

^a500 Mw(t) with radial maximum/average flux = 5/4.

Table 4

200-MW(E) He-COOLED BeO-MODERATED REACTOR
(15 kw/liter average core power density)

Active fuel-element length, ft	8	10	12.5	15.5
cm	243.8	304.8	381	472
Coolant void fraction, %	10	12.6	15.6	19.3
Outer diameter of fuel element, in.	4.0	3.9	3.8	3.7
cm	10.16	9.905	9.65	9.395
Fuel-compact thickness, in.	0.51	0.50	0.485	0.465
cm	1.295	1.27	1.23	1.18
Spine diameter, in.	2.40	2.32	2.25	2.19
cm	6.10	5.90	5.70	5.55
Fraction of C in core, %	23.5	24.0	24.5	25.3
Rod clearance, relative, %	0.4	1.8	3.7	6.0
mils	16	68.5	140	222
cm	0.04	0.178	0.36	0.565
Maximum fuel-element surface temperature, °F	1839	1841.7	1866.4	1848
°C	1004	1005.5	1007	1009
Power in hot channel, Mw	0.41	0.505	0.62	0.76
Maximum power per unit length, $10^5 \times \text{Btu/hr-ft}$	2.33	2.30	2.26	2.24
w/cm	2240	2210	2170	2150
Maximum surface heat flux, $10^5 \times \text{Btu/hr-ft}^2$	2.23	2.25	2.27	2.30
w/cm ²	70.1	70.6	71.3	72.3
Number of fuel elements ^a	1515	1240	1010	830
Active core diameter, ^a ft	13.65	12.22	10.92	9.8
cm	416	373	333	300
Core length/diameter ^a	0.585	0.82	1.14	1.58

^a500 Mw(t) with radial maximum/average flux = 5/4

Table 5

200-MW(E) He-COOLED BeO-MODERATED REACTOR
(Active length of fuel elements = 15.5 feet)

Average power density, kw/liter					
Core	8	10	12	14	16
Hot channel	10	12.5	15	17.5	20
Coolant void fraction, %	11.1	13.5	15.9	18.2	20.4
Outer diameter of fuel element					
in.	6.2	5.1	4.4	3.90	3.45
cm	15.75	13.0	11.2	9.9	8.75
Fuel-compact thickness,					
in.	0.835	0.675	0.575	0.50	0.435
cm	2.12	1.715	1.46	1.27	1.1
Spine diameter,					
in.	3.955	3.188	2.67	2.355	2.04
cm	10.0	8.1	6.78	5.96	5.2
Fraction of C in core, %	15.8	18.5	21.2	24	26.6
Rod clearance,					
relative, %	1	2.4	3.8	5.25	6.75
mils	62	122	163	205	233
cm	0.16	0.31	0.425	0.52	0.745
Maximum fuel-element surface temperature,					
°F	1790	1810	1826	1832	1855
°C	976	987	996	1005	1012.5
Power in hot channel, Mw	0.985	0.91	0.845	0.785	0.73
Maximum power per unit length,					
$10^5 \times \text{Btu/hr-ft}$	2.84	2.63	2.435	2.26	2.1
w/cm	2780	2570	2380	2220	2060
Maximum surface heat flux,					
$10^5 \times \text{Btu/hr-ft}^2$	1.83	1.99	2.125	2.24	2.35
w/cm ²	57.5	62.5	66.8	70.5	74
Maximum fuel-compact temperature,					
°F	3350	3370	3228	3225	3205
°C	1840	1852	1775	1773	1760
Initial (cold) gap between sleeve and compact,					
mils	37.5	31	25	22.5	20
cm	0.095	0.079	0.063	0.057	0.05
Number of fuel elements ^a	630	690	745	800	826
Active core diameter, ^a					
ft	13.45	12.0	10.95	10.3	9.5
cm	410	366	334	314	290
Core length/diameter ^a	1.15	1.29	1.415	1.5	1.63

^a500 Mw(t) with radial maximum/average flux = 5/4

Fig. 1-- Variation of maximum fuel-compact temperature with initial cold gap for various power densities before (NR) and after (R) irradiation (see Table 5)

Fig. 2-- Variation of gap thickness and maximum fuel-compact temperature with initial cold gap for irradiated and non-irradiated elements (see Table 5)

nearly closed at the end of the fuel-element life in the reactor. With the values used in the computations, the maximum spine temperature is about the same before or after irradiation (within 50°F).

Figure 3, corresponding to Table 5, gives the power density, fuel-element diameter, and void fraction corresponding to a given lattice pitch for fuel elements of given length. It shows how a $(\text{BeO-}\text{UO}_2\text{-ThO}_2)$ graphite-clad fuel element could be substituted for another graphite-clad fuel element $(\text{C-UC}_2\text{-ThC}_2)$ in a reactor where the pitch is fixed as well as the relative pumping power and the fuel-element length. The coolant void fraction and fuel-element diameter will change, as well as the power density, which means a change in thermal power rating for a core of given diameter. For example, taking a 4.7-inch pitch gives an average power density of 11.5 kilowatts/liter with a 4.54-inch fuel element, while a typical all-graphite core could have a power density of 7.5 kilowatts/liter with a 4.5-inch fuel element (with a thicker sleeve and a lower maximum internal fuel temperature). The increase in total power could therefore be about 50 percent for the same reactor-vessel size. Obviously, larger (or more) pumps and heat exchangers would be required.

Figures 4 to 9 show the variations versus power density of the following parameters: coolant void fraction, rod clearance, fuel-element diameter, fuel-compact thickness, fraction of graphite in the core, maximum surface heat flux, maximum surface temperature, and hot-channel power. The number of fuel elements, the core diameter, and length-to-diameter ratio are also plotted for a 500-megawatt (thermal) core. Coolant characteristics, pumping fraction, and maximum fuel-compact temperature remain constant.

Figure 10 shows the variations versus power density of the minimum fuel-element length corresponding to zero clearance for rods arranged in a hexagonal array. Doubling the power density about halves the corresponding minimum length.

The influence of maximum fuel-compact temperature is shown on Figure 11 for given coolant void fraction, core power density, and ratio of pumping to thermal power. Core length and diameter for a given power output and fuel-compact thickness are not very sensitive, but the fuel-element diameter increases by 25 percent while the number of fuel elements (for a given total power) decreases by 75 percent when the temperature rises from 1500° to 1800°C .

Figures 12 to 14 indicate how some reactor characteristic parameters vary with coolant pressure for various assumptions. Note that a 40-percent increase in the pressure has the same effect as doubling the pumping fraction. When the core dimensions are fixed for a given total power output, Figure 12 shows that doubling the pressure corresponds to a decrease in coolant void

Fig. 3--Variation of power density, fuel-element diameter, and coolant void fraction with lattice pitch (see Table 5)

Fig. 4--Variation of coolant void fraction and relative rod clearance with power density and active length of fuel elements

Fig. 6--Variation of graphite fraction in fuel element and maximum surface heat flux with power density and active length of fuel elements

Fig. 7--Variation of maximum fuel-element surface temperature with power density and active length of fuel elements

Fig. 8--Variation of hot-channel power and number of fuel elements of a 500-megawatt (thermal) reactor with power density and active length of fuel elements

Fig. 9--Variation of active core diameter and core length-to-diameter ratio of a 500-megawatt (thermal) reactor with power density and active length of fuel elements

Fig. 10--Variation of fuel-element length with power density for minimum coolant void fraction

Fig. 11--Influence of maximum fuel-compact temperature on reactor core characteristics

Fig. 12--Variation of 200-megawatt (electrical) reactor parameters with coolant pressure for fixed power density (and therefore core diameter), fuel-element length, and pumping fraction

from 21.5 percent to 13 percent, an increase in fuel-element diameter from 3.95 to 4.75 inches, and therefore a 30-percent decrease in the number of fuel elements. For given fuel-element dimensions, Figure 13 shows that doubling the coolant pressure does not affect the core diameter very much, but increases the power density by about 25 percent and decreases the coolant void from 18.4 to 13.7 percent. The number of fuel elements for a given total power is only decreased by 17 percent.

Finally, Figure 14 indicates that when the fuel-element length and the hot-channel power (or number of fuel elements for a given thermal output) are given, the void fraction is independent of the coolant pressure. The maximum fuel-compact temperature, coolant inlet and outlet temperatures, and pumping ratio are all fixed. Doubling the coolant pressure reduces by about 1/3 both core diameter and fuel-element diameter, while the power density is increased by 75 percent.

The proposed fuel element for the 200-megawatt (electrical) reactor (Figure 15) is similar to a typical element of the General Atomic High-Temperature Gas-cooled Reactor. As noted, the first-generation design would use an impermeable graphite sleeve to contain the fission products. Further development of BeO is required in order to replace this graphite sleeve with a BeO sleeve.

Choosing a typical design discussed previously in this section, the over-all outside diameter of the fuel assembly would be 4.59 inches. There will be an upper and lower reflector section, each 1-1/2 feet long. The over-all length of the assembly, including the reflector sections, would be 17.5 feet.

The annular fuel compacts are a composite of 10 volume-percent heavy metal oxides (UO_2 - ThO_2) and 90 volume-percent BeO. These will be fabricated by a cold-press and sintering method and, as proposed, subsequent grinding may not be necessary since the fit to the graphite sleeve must be kept quite loose to compensate for the shrinking of the graphite under irradiation. The fuel compacts will have an inside diameter of about 2.80 inches and an outside diameter of about 4.05 inches. They will be about 2 inches thick.

The center spine will be BeO and will be fluted to allow the passage of a bleed flow of helium to sweep out fission products. Helium from the primary coolant stream will be pulled into the fuel assembly through a hole at the top. The helium will flow through the flutes in the center spine, passing the fuel compacts and then through an internal fission-product trap.

Fission-product release studies from the Maritime Gas-cooled Reactor capsules indicate that most of the fission products will remain in the fuel

Fig. 13-- Variation of 200-megawatt (electrical) reactor parameters with coolant pressure for fixed fuel-element dimensions and pumping fraction

Fig. 14--Variation of 200-megawatt (electrical) reactor core parameters with coolant pressure for fixed number of fuel elements, fuel-element length, and pumping ratio

Fig. 15--Proposed fuel element for a 200-megawatt (electrical) uranium-thorium, helium-cooled reactor

compacts. The internal fission-product trap is expected to remove all but the noble gases, which will be carried out to an external low-temperature trapping system. The low-permeability graphite sleeve will limit the contamination of the main coolant stream, due to cross flow of the internal purge stream.

Conceptual Plant Design

Figure 16 shows a simplified flow diagram for the 200-megawatt (electrical) reactor power plant. The helium leaves and returns from the reactor through two concentric ducts. For the purpose of this study, vertical, single-pass, once-through heat exchangers having helium circulators integral with the top head have been chosen. The exchangers do not incorporate steam separation or water recirculators; however, each steam generator contains a reheater. The helium circulators are top-mounted and integral with the steam generators. Preliminary calculations indicate that two heat exchangers can adequately handle the required performance. However, it may be found with further study that two such heat exchangers are so large that they require field fabrication. If further calculations indicate this to be the case, it may be more economical to develop a system incorporating three loops. The hot helium (1380°F) will enter the steam generator through the inner concentric duct and flow upward into the central section containing the tube bundles. Typically the tube bundles will consist of pendant "U" tubes in the superheater and reheater and "W" tubes in the evaporator and economizer. The gas is discharged from the heat-transfer section through the top shroud and into the helium circulator. The gas passes through the circulator diffuser, downward to the bottom of the heat exchanger, and exits through the outer annulus of the concentric duct. Some of this cooler gas cools the shell of the steam generator. Internal insulation would probably be used on the inside of the steam-generator shroud which contains the heat-transfer section.

An additional loop, not shown on the simplified flow diagram, will be required for emergency cooling. The scheme under consideration would utilize a water-cooled heat exchanger and depend on natural convection for the helium flow. More advanced core designs utilizing all of the beryllium oxide, thereby eliminating the graphite from the core, would allow considerable simplification of the system in that air would be used as the emergency coolant.

The helium circulators have been assumed to be steam-turbine driven; however, future engineering studies will compare the steam-driven circulators with electrical and helium-turbine types.

The turbine plant is conventional and similar to a modern fossil-fueled plant, the only modification being to provide steam for the circulator turbines. The circulator turbines are in series with the turbine generator and represent two stages of the high-pressure turbine exhaust of a standard turbine. Steam

Fig. 16--Simplified flow diagram of a 200-megawatt (electrical), uranium-thorium, helium-cooled reactor

exhausting from the primary coolant circulators passes through reheaters and then returns to the intermediate-pressure section of the main turbine. Because of the consequences of a failure in the heat-removal system, more reliability will probably have to be built into the feedwater system than is normally done.

Major process systems not shown on the simplified flow diagram will be the helium-purification and storage systems. The helium-purification system will provide removal of chemical impurities such as oxygen, carbon dioxide, etc., and removal of short-lived fission products which give rise to long-lived solid daughter products, thereby limiting contamination of the primary helium loop. It is anticipated that solid fission products escaping from the fuel bodies will be collected in the fission-product trap, which is integral with the fuel element.

One approach to the pressure-vessel design is shown in Figure 17. A spherical vessel was chosen at this time because (1) a quick analysis indicated that the cost of a spherical and a cylindrical vessel were roughly the same and (2) more information is at hand on spherical vessels as a result of other work being carried out at General Atomic.

At this point, the material under consideration for the vessel is SA212B. This material was chosen because of its wide use as a reactor-vessel material and because of the relative few constraints imposed upon it from a fabrication point of view. Vessels of the size under consideration will very likely have to be field fabricated. The material chosen, SA212B, does not require preheat for welding, which gives it some advantage over other materials having a slightly higher stress allowable. Future optimization studies will consider other materials in detail, as well as other approaches to reactor-vessel design.

The fuel handling for this concept will be accomplished by using the nozzles provided for the control-rod-drive mechanisms. During fuel handling, the reactor will be shutdown and control-rod mechanisms will be removed from those ports which will be used during fuel reloading. It is anticipated that one-quarter or less of the core will be reloaded at each refueling shutdown. Since there are 25 nozzles, the reloading machine will be designed in such a way that it can handle 30 to 35 of the fuel elements directly below any particular nozzle. The size of the pressure vessel can be significantly reduced by devising a reloading scheme which will, first, remove the elements directly below the nozzle and, second, allow the lateral movement of other elements to a position directly below the nozzle. Lateral movement of fuel elements above the core would obviously make the vessel about twice as large. Reflector sections will very nearly approximate the physical characteristics of the fuel element, so that the reloading machine will be able to service the reflector in the same manner as it handles the rest of

Fig. 17--Pressure vessel for a 200-megawatt (electrical), uranium-thorium, helium-cooled reactor

the core. Since there is graphite in the core, it is important that air not be allowed to enter the reactor vessel during the refueling operation. This may be accomplished by having the fuel-handling machine seal against the top of the refueling nozzle. The helium pressure inside the reactor vessel during refueling will be maintained slightly below atmospheric pressure to prevent the escape of contamination products. The afterheat from the shutdown core at the time of refueling will be approximately 1 percent of the reactor's normal thermal output. This will require one of the helium circulators to be operating to remove the afterheat. For this purpose an auxiliary package boiler will be required. The next step of the development will be to refine the design of the reloading machine so that time and motion studies can be developed and these data equated against the cost of reactor shutdown time, fuel-element life, and flux flattening, so that the most economical approach to the reloading procedure can be developed.

It is presently anticipated that there will be nominally 50 control rods for this reactor. The conceptual design of the pressure vessel as shown in Figure 17 shows 25 nozzles. Each nozzle will allow a penetration of two control-rod mechanisms. These mechanisms have not been worked out in detail, but preliminary investigations have shown that available designs can be adapted to this type of service.

Cost Analysis

Based on the preceding design, a rough cost estimate for the 200-megawatt (electrical) system was made. The estimate was made in accordance with AEC ground rules estimating procedure and format, all of which are contained in the AEC cost evaluation guide. (1)

The estimate showed that the capital cost for the plant less the cost of fuel was \$240 per net kilowatt. The estimate is believed to be sufficiently accurate to warrant optimism. There are a number of areas such as the pressure vessel, secondary containment, and steam generators where more detailed engineering optimization could result in further reducing the capital costs. Since the reactor plant equipment accounts for nearly 60 percent of the direct construction costs, it is not unreasonable to expect that cost reductions in this area could result from improvements in engineering design. The fuel-cycle costs, including the fuel fabrication cost, are discussed in the Physics Section below; however, they are also summarized in Table 6 to aid in estimating the cost of energy from the plant.

PHYSICS

Previous work on the uranium-thorium systems was concentrated on large reactors where economic breeding was a definite possibility. It was

Table 6

SUMMARY OF COSTS

(200-Mw(e) Beryllium Oxide Thorium-Uranium Reactor
Nuclear Power Plant)

Capital cost per kw(e)	\$240
Cost of energy at 0.8 load factor, mills/kw-hr	
Capital charge at 14.5%	5.0
Insurance	0.1
Operating and maintenance	0.5
Fuel-cycle cost	<u>1.3</u>
Total power cost	6.9

then pointed out that beryllium oxide, with its excellent moderating properties, is a good material for smaller reactors, where emphasis is placed on the lowest fuel-cycle cost, and breeding would be subject to a cost penalty due to the increased neutron leakage. During this quarter, efforts were placed on the survey of a 200-megawatt (electrical) system, with emphasis on obtaining the lowest possible fuel-cycle cost irrespective of conversion ratio. The results of the survey confirm the fact that in this power range the BeO-moderated system is economically very attractive. Fuel-cycle costs of 1.0 to 1.2 mills/kilowatt-hour were calculated, with fuel burnups of 1.5 to 2.0 fifa,* and conversion ratios of 0.75 to 0.85. If the fuel-element fabrication cost is reduced, the minimum fuel-cycle cost is associated with higher conversion ratios. Thus as fuel fabrication technology is improved over the years, it will be desirable to increase the conversion ratio in future core reloads, even to the point where thermal breeding in a core of this size becomes economically attractive.

Description of Survey

In this survey, four different fuel-element designs were chosen, as detailed in the Engineering Section. The power densities for these designs were 6.4, 9.6, 12.8, and 16.0 kilowatts/liter. Calculations were run for fuel loadings given by specific powers of 500, 750, and 1000 kilowatts/kilograms, with a range of thorium-uranium ratios at each specific power. One parameter that was not varied in this study was the degree of heterogeneity of the fuel element, or the volume fraction of the fuel compact to the spine plus fuel compact. In all designs this was kept constant at about 0.5. The fuel management scheme was the same as had been used before; i. e., the core is divided into four zones that are reloaded one at a time. The

* Fissions per initial fissile atom.

feed fuel is fresh for each reload, with a fissionable isotope composition of 90 percent U^{233} and 10 percent U^{235} . Cycle efficiency was assumed to be 40 percent in each case, consistent with a thermal power output of 500 megawatts.

The reactivity-lifetime and cost calculations were made with the zero-dimensional survey code GAD,⁽²⁾ similar to previous studies. For the purpose of survey work, the zero-dimensional code is adequate. Leakage accounts for only 3 to 5 percent of the total neutrons in the system, and a more accurate treatment of the leakage will not produce significant changes in the results of the survey. Actually, leakage is factored into the calculation by assuming an energy-independent bare-core buckling to describe the neutron flux shape. The results are therefore on the conservative side, since the reflector will no doubt contribute positively to the reactivity.

Fuel-cycle Costs

The cost data used in these calculations are essentially the same as those used before. The fabrication costs are discussed in more detail in the Materials Section. In the 200-megawatt (electrical) system, where the conversion ratio is less than 1.0, the fabrication cost is a smaller percentage of the total than it was in the larger breeders, but it is still the most significant item. The longer irradiation times have succeeded in reducing the fabrication cost at the expense of increasing the depletion or loss-of-fuel cost close to the point where these gains and losses offset each other, i.e., the minimum cost is reached. Therefore, the design point for this minimum cost is affected by the initially assumed fabrication costs, as will be illustrated.

Figures 18 to 20 give the basic results of the survey, showing fuel-cycle costs versus conversion ratio for various power densities and for specific powers of 500, 750, and 1000 kilowatts/kilogram. The fuel fabrication costs used here are the high ones, as described in the Materials Section. Figures 21 to 23 show the same information, using the lower fuel fabrication costs. Comparison of these sets of curves shows the desirability of increasing the conversion ratio as the fuel fabrication change is reduced. It is also shown that as the conversion ratio decreases from 1.0, the power density for minimum cost increases. At high conversion ratios with relatively short burnups, the inventory cost is reduced by going to lower power densities and thus increasing the irradiation time. As the conversion ratio is reduced and irradiation time gets progressively longer, this gain is overridden by other considerations which lead to higher power densities.

By taking the minimum cost point at each power density for Figures 18 to 23 and plotting cost versus power density for varying specific powers, Figure 24 is produced. The cost-versus-power-density curves appear to be

Fig. 18--Variation of fuel-cycle cost with conversion ratio at a specific power of 500 kilowatts/kilogram (high fuel fabrication cost assumed)

Fig. 19--Variation of fuel-cycle cost with conversion ratio at a specific power of 750 kilowatts/kilogram (high fuel fabrication cost assumed)

Fig. 20--Variation of fuel-cycle cost with conversion ratio at a specific power of 1000 kilowatts/kilogram (high fuel fabrication cost assumed)

Fig. 21--Variation of fuel-cycle cost with conversion ratio at a specific power of 500 kilowatts/kilogram (low fuel fabrication cost assumed)

Fig. 22--Variation of fuel-cycle cost with conversion ratio at a specific power of 750 kilowatts/kilograms (low fuel fabrication cost assumed)

Fig. 23--Variation of fuel-cycle cost with conversion ratio at a specific power of 1000 kilowatts/kilogram (low fuel fabrication cost assumed)

Fig. 24--Variation of minimum fuel-cycle cost with power density for fixed specific powers

very flat at the minima. For the two sets of cost data given, a power density of 12.5 kilowatts/liter appears to be optimum. The optimum specific power appears to depend on the assumed fabrication cost. Calculations were made with a series of arbitrary fabrication costs, and the results are plotted in Figure 25, showing the tendency to go to higher specific powers as costs are reduced. The same sort of data can be produced that show the tendency towards lower power densities as the costs are reduced, and this effect is illustrated in Figure 26. The breakdown of the fuel-cycle cost for the 12.8-kilowatt/liter, 750-kilowatt/kilogram design is given in Figure 27 as a function of conversion ratio. Both the high-cost and low-cost figures are given. The curves illustrate the overriding importance of the fabrication costs, particularly at high conversion ratios. The burnup cost, of course, increases with decreasing conversion ratios and eventually becomes the dominating factor.

As mentioned in the previous progress report, the basic cost data (exclusive of fabrication charges) are similar to the standard information given in Ref. 1. Table 7 is a list of these numbers. The table is broken down into the AEC-based and industry-based charges. These figures are typical costs based on previous experience, most of which has been with metal-clad uranium fuels for water-moderated reactors. In dealing with BeO-ThO₂-UO₂ elements, most of these cost data will still apply, with the possible exception of reprocessing costs. Chemical reprocessing of such a mixture is not well understood at the present time, and, with the added complication of toxicity and the high neutron activity of the BeO/U²³³ mixture, it is probable that the reprocessing costs used are perhaps too low. Based on the present reprocessing plant charge of \$17,000 per day, the reprocessing costs for the 200-megawatt (electrical) plant vary between 0.05 and 0.3 mill/kilowatt-hour, as illustrated in Figure 27. Increasing the reprocessing charge will naturally increase the over-all fuel-cycle cost. However, the reprocessing cost is small, particularly at the design point, and it would require a considerable increase in plant charge before the design parameters are affected.

Nuclear Characteristics

The nuclear characteristics of the selected reactor design (12.4 kilowatts/liter, 750 kilowatts/kilogram, selected on the basis of the more optimistic cost data) can now be examined in more detail. Table 8 gives the major nuclear parameters for the selected design, and Figure 28 gives the variation of these parameters with conversion ratio. The figures are for the equilibrium cycle. Fuel reloading would be necessary about every two years, with a fuel life of about eight years (assuming an 80-percent load factor). The fast nvt received by the fuel element is on the average 3.27×10^{21} neutrons/centimeter³. The highest fast-flux irradiation to which BeO has been submitted in capsule tests to date is 2×10^{21} neutrons/centimeter³. Based on the available irradiation test results, it does not

Fig. 25--Variation of optimum specific power with fuel-compact fabrication charge for fixed power densities

Fig. 26--Variation of optimum power density with fuel-compact fabrication charge for fixed specific powers

Fig. 27--Cost breakdown of a 200-megawatt (electrical) reactor core

Table 7

BASIC COST DATA AND OPERATING PARAMETERS

<u>Industry-Based</u>		<u>AEC-Based</u>	
Plant load factor	80%	Post-irradiation Storage	120 days
Shipping time		Loss during reprocessing	1%
AEC to fabricator	20 days	Loss during final conversion	0.3%
Fabricator to reactor	20 days	Reprocessing plant rate	44 kg U/day
Reactor to reprocessing	20 days	Use charge on fuel	4.75%
Conversion rate	150 kg U/day	Uranium price (U-233 + U-235)	\$13,000/kg
Time in fabrication	180 days	Conversion to UF ₆	\$5.60 kg U
Pre-irradiation storage	30 days	Shipping charge	
Spare-fuel fraction at site	0.10	Reprocessing to AEC	1.50/kg U
Losses during initial conv.	1%	Reprocessing plant Charge	\$17,000/day
Losses during fabrication	1%		
Recycle scrap to AEC	10%		
Shipping charge			
AEC to fabricator	\$1.50/kg U		
Fabricator to reactor	\$0.15/kg fuel element		
Reactor to reprocessing	\$16./kg fuel element		

Table 8

NUCLEAR DESIGN PARAMETERS FOR 200-MW(E) CORE

Power data

Power output	500 thermal megawatts
	200 electrical megawatts
Power density	12.8 kw/liter
Specific power	750 kw/kg of feed fuel

Core size

Core length	13.9 ft
Core diameter	11.3 ft
Core volume	1394 ft ³
Total length (incl. reflector)	17.5 ft
Total diameter (incl. reflector)	14.0 ft
Fuel-element diameter	4.6 in.
No. of elements	784

Composition

Void	14.1%
Spine	32.5%
Compact	35.6%
Sleeve	17.8%

<u>Isotopic composition per 1/4 core</u>	<u>Feed</u>	<u>Final</u>
U-233, kg	150	89.2 (after decay of Pa-233)
U-235, kg	16.7	10.3
Th-232, kg	2817	2553
BeO/U ratio	1006	1723
Th/U ratio	17.0	26.3
C/U ratio	256	438

Nuclear PerformanceExcess reactivity

Beginning of cycle	1.140
Equilibrium xenon	1.112
Cycle lifetime (at full power)	578 days
Fuel lifetime (at full power)	2312 days
Fuel burnup, fifa	1.803

Average flux

>1.0 Mev	1.63×10^{13}
1 Mev $\rightarrow$ 1 ev	1.47×10^{14}
1 ev $\rightarrow$ 0	4.01×10^{13}

Average nvt

>1.0 Mev	3.27×10^{21}
1 Mev $\rightarrow$ 1 ev	2.95×10^{22}
1 ev $\rightarrow$ 0	8.05×10^{21}

Avg. fissions per cc of compact	7.72×10^{19}
Avg. conversion ratio	0.807

Fig. 28--Variation of nuclear parameters with conversion ratio

appear that irradiation damage in BeO will be a problem. There may, however, be problems with the graphite sleeve and the BeO-graphite dimensional relationships which may be solved by mechanical design, or will provide an upper limit to the fuel exposure. Figure 29 shows approximately the relationship between the fuel-cycle costs and fast-neutron exposure for the 200-megawatt (electrical) system. This relationship is practically independent of the design parameters in the ranges involved in this study. If the average exposure limit were set at 2×10^{21} neutrons/centimeter³, the minimum fuel-cycle cost would be 1.48 mills/kilowatt-hour and 1.06 mills/kilowatt-hour for the high- and low-fabrication cost figures, respectively. Figure 29 also illustrates that, in the high-fabrication-costs case, the minimum cost of 1.2 mills/kilowatt-hour is achieved at exposures of 4.5×10^{21} neutrons/centimeter³, whereas one can achieve the same results at about one-third the exposure using the cheaper fuel elements. Therefore, any manufacturing procedure that enhances the fuel lifetime should be examined carefully to determine whether the added cost involved increases or decreases the fuel-cycle cost. Any restriction on the exposure of the fuel element will affect the design point for minimum cost, as the conversion ratio will have to be increased and power density decreased as the exposure is decreased in order to reach the minimum fuel-cycle cost.

Conclusion

It thus appears that the minimum fuel-cycle cost that can be achieved for a 200-megawatt (electrical) system is 1.2 mills/kilowatt-hour or, using more optimistic fabrication charges, 1.0 mill/kilowatt-hour. Several cost-reduction techniques can be applied such as more complicated fuel management, fission-product purging, and reuse of BeO, as discussed in the previous progress report. Thermal breeding in this size of core is physically possible, although it does not appear economical unless reductions are made in the fuel fabrication charges. In any event, it would be possible to increase the conversion ratio as the fabrication costs are reduced, until a breeding situation is achieved.

MATERIALS

Work continued on the development of BeO and fuel-element materials which would be applicable to the uranium-thorium reactor concept. The primary emphasis of the experimental work has shifted to the use of urania-thoria fuel particles, studies of methods of improving the BeO matrix (additives and grain size effects), studies of fabrication methods for producing the fuel-compact shapes of interest to the BeO-uranium-thorium reactor concept, and design of irradiation experiments incorporating UO₂-ThO₂ compositions in a BeO matrix.

Fig. 29--Relationship between cost and nvt for 200-megawatt (electrical) designs

In examining the fuel-cycle costs for the 200-megawatt (electrical) reactor, the materials and fabrication costs were estimated using the assumptions developed in the last quarterly report. (2) The fuel-element concept used in these studies consists of a $\text{UO}_2\text{-ThO}_2\text{-BeO}$ fuel compact with an inner BeO spine using a graphite sleeve and graphite end fittings. The core fabrication costs for four of these cases discussed in the Engineering Section are shown in Figure 30. It is apparent that the fabrication of the fuel compacts will represent the major cost item, and fabrication methods to reduce this cost are an important goal of the development program.

The proposed use of a graphite sleeve for the fuel-element cladding has been based primarily on its availability in long lengths at relatively low cost, its excellent mechanical properties and thermal shock resistance, and its present state-of-the-art including sufficient data on its irradiation behavior to predict reliable operation under the conditions of the conceptual designs. Development work in progress at General Atomic and elsewhere on obtaining a suitable pore structure and low permeability would, of course, be directly utilized in specifying the graphite for the sleeves. Graphite also appears to be compatible in contact with the BeO at the inside sleeve temperatures of 800° to 900°C . (2)

A major restriction on the graphite in the temperature range and at the fast-neutron exposure expected in these designs appears to be the dimensional contraction of the graphite tubes. Although very small dimensional changes are expected at exposures of less than 10^{21} nvt ($E > 0.18$ Mev) (all exposure values reported for energies greater than 0.18 Mev), measurable contraction is expected at higher exposures. (3,4,5) At exposures of 2×10^{21} nvt and in the temperature range of 800° to 900°C , length changes of less than minus 0.2 percent and small diameter changes are expected. The irradiation behavior of the tube samples of type HLM-85 graphite (used as sleeve material for the Peach Bottom reactor) has been examined. At 7×10^{21} nvt and 800° to 900°C , length changes of over minus 0.8 percent and diameter changes of about minus 0.3 to minus 0.6 percent were determined. (3)

Needle-coke graphites and CSF-grade graphites have recently been reported by Hanford as having undergone exposures of up to 14×10^{21} nvt and at temperatures of 425° to 800°C . (5) These studies have shown an increase in the contraction rate with increasing neutron exposure on samples cut parallel to the extrusion axis (length dimension of sleeve). The results at 800°C and 7×10^{21} nvt are in general agreement with the HLM-85 data. There appears to be a decrease in the rate of contraction at irradiation temperatures around 800°C with samples cut transverse to the extrusion axis (diameter of sleeve). These results at 800°C and 7×10^{21} nvt are in general agreement or somewhat lower than the HLM-85 data. The contraction rate will generally decrease with increasing temperatures and appears to be minimized at the operating temperature (800° to 900°C) of the sleeve.

Fig. 30--Relative costs of a 200-megawatt (electrical) reactor core with BeO fuel elements (1/4-inch graphite sleeve)

The contraction of the graphite-sleeve diameter, together with the expected BeO and fuel-compact expansion, have been included in the fuel-element design to produce a very close fit at the end of life. This results in a significant gap at startup which eases the fuel-element assembly but results in an initially high fuel and spine temperature. These high temperatures, particularly in the spine, are in the right direction in reducing the irradiation damage to the BeO.

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3. PLUTONIUM REACTORS

ENGINEERING

Efforts during this period were concentrated on the possible use of small volume fractions of zirconium hydride in the plutonium-beryllium oxide core as a means of achieving reactivity characteristics similar to those of somewhat larger cores containing only beryllium oxide as the moderator.

Two problems related to the mechanical design of such a core were considered during this period. The first was concerned with the usual requirement that heat transfer be accomplished within the limits of pressure drop and fuel-element surface temperature. The second considered the arrangement of the zirconium hydride moderator in such a way that loss of hydrogen with time would be reduced to an acceptable minimum. Design conditions for the core were as shown in Table 9.

Table 9

DESIGN CONDITIONS FOR THE $\text{PuO}_2\text{-BeO-ZrH}$ REACTOR

Power rating	50 Mw(t)
Dimensions	"Square" cylinder, 4.5-ft dia. by 4.5 ft high
Core composition	80 vol-% $\text{PuO}_2\text{-BeO}$ and cladding, 8 vol-% ZrH_n , 12 vol-% void
Coolant	Helium
Inlet temperature	600°F
Outlet temperature	1200°F
Helium flow rate	63.6 lb/sec

The use of metal fuel-element cladding was assumed because of the plutonium. However, in an effort to make maximum use of beryllium oxide as a structural material, thereby reducing the amount of structural metal in the core, the thermal-stress equation for bodies with a volume heat source was solved by setting the power density and the materials properties and then solving for the thickness of body to stay within the allowable design stress.

Slab and cylinder models were used; the results gave nominally 1/8-inch-thick slabs or 1/4-inch-diameter cylinders. This results in approximately four times the surface area needed for heat transfer and thus four times as much metal cladding in the core. In this case, heat fluxes are too low to permit efficient use of the heat-transfer area, and the temperature difference from surface to gas is too low to take advantage of the properties of the coolant gas. Any effort to increase the heat flux would simply cause greater thermal stress in the fuel body, resulting in still thinner bodies and more cladding surface. In order to decrease the amount of cladding and make better use of surface area for heat transfer, the possibility of using an asymmetrical temperature distribution, thereby freeing the fuel body to distort and relieve thermal stresses, was investigated. Only plates or, to a lesser extent, split annuli can be handled in this way. Solid cylinders or complete annuli cannot be "turned loose" to relieve thermal stress. If the temperature gradient through the body were linear, deflection of the plate would completely relieve thermal stress. However, since the temperature gradient in the volume heat source is parabolic, some thermal stresses remain, as shown in Figure 31. Using the same limit on working stress, i. e., 4000 psi maximum tension in the BeO-fuel ceramic, the "free plate" design permits fuel-body thickness to be doubled. However, this still gives too much surface area by a factor of two.

If the thermal-stress equation is solved by setting the fuel-element dimensions to give good heat flux independent of thermal stress, the resulting fuel bodies would be 1-inch-thick slabs or 2-inch-diameter cylinders. In either case the resulting thermal stress is greater than 100,000 psi, compared to the allowable 4000 psi. Thus the design can be based on limiting thermal stress, letting surface area and heat flux be dependent variables, or it can be based on good heat transfer, letting thermal stress fall where it may.

It may be possible for partly homogeneous cores to be designed where good heat flux is obtained within the working strength limits of the ceramic, but, in the case of the fully homogeneous design, solutions of the thermal-stress and heat-transfer equations are so far apart that no possibility of compromise exists. Since the principal reason for attempting to use the ceramic fuel bodies as structural members is to reduce the amount of metal structure in the core and since thermal stress causes the exact opposite result, it was decided to consider only the core which could be designed for efficient heat transfer. In this case, the ceramic is grossly over-stressed and cannot be counted on for structural support. In fact, it would be preferable to have the ceramic in a semi-plastic or glassy state to prevent its fragmentation and ratcheting during thermal cycling of the reactor.

Fig. 31--Relative thermal stress in flat plates with a volume heat source, showing different stress patterns for restrained and unrestrained plates

With the core built up of 2-inch-diameter metal-clad solid cylinders, 565 cylinders are required to contain the specified volume of fuel and BeO. This gives an effective heat-transfer area of 1330 square feet with an average heat flux of 35.7 Btu/ft²/second. This is well within the good heat-transfer design limits.

The arrangement of the ZrH in the core is influenced by the opposing requirements of a high degree of homogeneity for best results from a reactor physics standpoint and low surface-to-volume ratio for best retention of hydrogen. Retention of hydrogen also limits the placement of ZrH to the lowest temperature regions of the core. It would be possible to arrange separate cooling of the ZrH by use of parallel cooling streams or a two-pass arrangement in which the coolant flows first through the ZrH and then reverses direction and returns through the fuel elements. Both possibilities were regarded with disfavor because of complexity, redundant structures, and high void volume. Since the coolant is the heat sink, it is the lowest temperature point in any region of the reactor. Thus, if the ZrH were a separate body surrounded by reactor coolant and not a part of the fuel elements or a path for heat flow, it would be at the lowest temperature possible without separate cooling. The simplest possible arrangement for integrally cooled ZrH would be an alternating pattern of 2-inch-diameter cylinders in which every 10 fuel elements would have an attendant ZrH cylinder. This would give a ZrH surface-to-volume ratio of 2, which is desirable for hydrogen retention but would result in undesirable reactor physics characteristics. Another possible arrangement is one in which a small ZrH rod is placed in each tricusp formed by three adjacent fuel cylinders. This gives a surface-to-volume ratio of 8, which appears to be an acceptable compromise between the requirements of reactor physics and hydrogen retention. Although the ZrH rods are exposed to reactor coolant, they also "see" the fuel-cylinder surface from a thermal radiation standpoint and are conservatively assumed to be at the same temperature. Therefore, the fuel-surface temperature sets the temperature of ZrH and the loss rate of hydrogen. Temperature profiles were calculated using cosine distributions of heat flux with maximum-to-average ratios of 1.25 and 1.33 in the axial and radial directions, respectively. Results are given in Figure 32. These show that the cladding-surface temperature along an average flux flow path peaks at 1320°F, while the surface temperature along the center of the core peaks at 1560°F. While the former represents an acceptable maximum temperature from a standpoint of hydrogen loss, the latter is much too high. In an effort to reduce the maximum temperature of ZrH, the idea occurred of omitting ZrH rods in the high-temperature center core region. This gives three effects, all of which are beneficial:

1. It removes ZrH from the high-temperature region where it is subject to quick damage.

Fig. 32--Variation of fuel-element cladding temperature with distance along core length for different radial positions

2. It provides a means of power shaping by essentially making the neutron spectrum become more thermal in the radial direction.
3. It permits flow adjustment between radial core regions.

A close-packed bundle of cylinders of equal diameter has a fixed void ratio, and radial flow adjustment is limited because cross flow between passages can overcome the effect of inlet orificing. Insertion of small cylinders in the tricusps gives the designer a degree of freedom to control flux, heat-transfer coefficient, and coolant flow rate, and thus may represent a means of improving other cores which are based on close-packed rods.

The proposed core design configuration consists of 2-inch-diameter fuel cylinders interspersed with 1/2-inch-diameter ZrH rods in the outer three-fourths of the core. Zirconium hydride is omitted in the center one-fourth of the core. Peak fuel-element surface temperature is 1330°F in the center of the core. This is within the temperature limits for Type-316 stainless steel. Cladding thickness was determined by assuming a maximum stress of 30 percent of the yield strength of Type-316 stainless steel at 1500°F or 3600 psi. Design load was assumed to be a 1-g sidewise acceleration with the cylinder simply supported at the ends. The only strength attributed to the ceramic fuel-moderator is that it keeps the cross section circular and prevents buckling of the cladding as the cylinder deflects under load. On this basis, the cladding thickness was calculated to be 0.020 inch. An equal thickness was arbitrarily assumed for the cladding of the ZrH rods.

Loss of hydrogen from ZrH was estimated by dividing the ZrH into two equal regions which were assumed to be at 1100° and 1200°F, respectively. It was assumed that the hydrogen pressure inside the cladding of any rod was in equilibrium with the hottest part of the ZrH within the rod. It was also assumed that the diffusion rate of hydrogen through the cladding is related to the mean cladding temperature. If the ZrH has $N_H = 5 \times 10^{22}$, the resulting partial pressures of hydrogen are 6×10^{-4} atmospheres and 2.5×10^{-3} atmospheres in the 1100° and 1200°F regions, respectively. The resulting permeation rates through 20-mil-thick stainless steel cladding are 4.63×10^{-4} and 9.78×10^{-4} pounds/foot²-year. This amounts to a loss of 2.4 percent of the original hydrogen in a three-year core life.

The loss of hydrogen from ZrH can be used to advantage as an ultimate shutdown mechanism, since, as the temperature increases, the rate of hydrogen loss increases disproportionately. If the cladding remains intact, an increase in temperature from 650° to 750°C will result in an increase in hydrogen loss by a factor of approximately 300. If the cladding is punctured, the loss rate will be even higher. Should the core begin to overheat, the increased loss of hydrogen will give a large negative reactivity addition. This rules out the possibility of a walk-away accident, and if ZrH cladding

is chosen with a melting point lower than the fuel-element cladding, any positive activity increment due to melting of fuel-element cladding can be offset by the loss of hydrogen.

PHYSICS

Previous investigations have shown that a BeO-moderated reactor using plutonium isotopes as fuel has good reactivity-lifetime characteristics for a core having diameter and length of 5 feet and for a core having diameter and length of 6 feet. The good lifetime characteristics were due primarily to the use of the Pu^{240} isotope as a burnable, fertile isotope, in which case the fissile isotope Pu^{241} was built up to replace some of the Pu^{239} being burned. However, it was found that for smaller core sizes, 4-foot diameter and length, the neutron spectrum became so epithermal due to the higher neutron leakage that the potential advantages of the plutonium fuel could not be obtained.

Essentially, it was not possible to incorporate enough Pu^{240} in the fuel to be useful as a fertile isotope and still have sufficient excess reactivity to obtain a long operating lifetime. Therefore, it was decided to determine the feasibility of obtaining the good lifetime characteristics of the larger cores by adding some small volume fractions of ZrH_n moderator to the BeO moderator in the small cores. Due to its better moderating properties, the addition of relatively small amounts of hydrogen to the BeO moderator could be used to reduce the neutron leakage from the smaller core and yield a spectrum corresponding closely to that of the larger all-BeO core. The addition of too much hydrogen would soften the thermal spectrum so that the Pu^{240} could again not be used as an effective burnable poison and fertile material. As previously reported, a reasonable design criterion is that the ratio of neutron captures in Pu^{240} to captures in Pu^{239} should be close to unity or the Pu^{240} either builds up in concentration during life or becomes depleted during burnup. In either case, if this ratio is too far from unity, the lifetime of the core would be adversely affected. In particular, if this ratio is appreciably below unity, then Pu^{240} is built up during burnup and acts as a poison being continuously added to the reactor.

To check the feasibility of adding ZrH_n moderator, some preliminary nuclear investigations were made for two different 4-foot-diameter cores which were chosen as examples. For these example cases, preliminary physics calculations were made to determine the variation in k_{eff} and the plutonium-isotope capture ratio as a function of the amount of hydrogen added to the core.

The first core studied, herein designated as core 4-A, was chosen to be representative of a high-power-output [70 megawatts (thermal)], more

heavily loaded core with an assumed coolant void fraction of 0.30. The second core, designated 4-B, was picked to have the same void fraction as the larger core previously analyzed; i. e., 0.12, which at the power density of the larger 6-foot core (14 kilowatts/liter), would correspond to a power of 20 megawatts. Practical cores with these dimensions would have power densities between these two values. The effect of varying amounts of hydrogen moderator on both the initial k_{eff} and on the ratio of captures in Pu^{240} to captures in Pu^{239} were evaluated at specific powers of 450 and 900 kilowatts/kilogram for core 4-A and at 450 kilowatts/kilogram for core 4-B. For calculational purposes, reactor powers of 67.5 and 20 megawatts were assumed for 4-A and 4-B, respectively. In the calculations performed, it was assumed that the plutonium isotope abundance was 0.78, 0.12, and 0.10 for Pu^{239} , Pu^{240} , and Pu^{241} , respectively.

Results

Core 4-A

The variation in k_{eff} and in the isotope capture ratio, R , is shown in Figure 33 as a function of the hydrogen/fuel atom ratio for both of the specific powers considered. In the calculations, the BeO volume fraction was kept constant at 0.60 for both specific powers. The ratio of Be/ $\text{Pu}^{239} + \text{Pu}^{241}$ was 175 and 350 at 450 kilowatts/kilogram and 900 kilowatts/kilogram, respectively. From the plots of k_{eff} and R shown in Figure 33, it is seen that, although the capture ratio R for 900 kilowatts/kilogram is near unity, the loading is not high enough to go critical. When the fissile loading is increased to 150 kilograms, the figure shows that the hydrogen-to-fissile-material atom ratio must be greater than about 18 for the reactor to go critical. However, the capture ratio beyond this point is 0.80 or less and the flat reactivity-lifetime characteristics desired probably could not be achieved with this value of R .

It is interesting to note that at both specific powers considered, the value of R has a maximum at about a hydrogen-to-fissile-material atom ratio of 18. This is due to the fact that, with no hydrogen in the system, the reactor is undermoderated and the epithermal capture rate in Pu^{239} is relatively high. On the other hand, if too much hydrogen is present, then the thermal spectrum becomes too soft and the low-energy capture rate in Pu^{239} is increased. A measure of the variation of the over-all neutron spectrum is the percent of total fissions which occur at thermal energies (below 2.38 electron volts). In the 6-foot core this is about 65 percent. At 450 kilowatts/kilogram in the 4-foot core (core 4-A), the corresponding values are 13, 48, and 64 percent at hydrogen-to-fissile atom ratios of 0, 17.5, and 35.0, respectively. The over-all spectrum in the 4-foot core is about the same as for the larger core when the hydrogen-to-fissile atom

Fig. 33--Variation of reactivity and isotope capture ratio with hydrogen/fuel atom ratio (core 4-A) at specific powers of 450 and 900 kilowatts/kilogram

ratio is 35. However, the thermal-spectrum softening for this case can be seen in Figure 34. The effect of hydrogen addition to the thermal-neutron spectrum is shown in Figure 33 for the 450-kilowatt/kilogram specific power. In Figure 34, $\phi(E)$ is plotted against energy for hydrogen-to-fissile atom ratios of 0, 17.5, and 35. The spectra are all normalized to one thermal source neutron. Also shown is the spectrum for a 6-foot core. The effect on the spectrum of the hydrogen addition as well as the plutonium resonances at 0.3 and 1.054 electron volts is readily apparent.

An auxiliary calculation was done in which the assumed Pu^{240} content was increased by 4/3 its original value in an effort to see if the value of R could be increased significantly without bringing k_{eff} down too low. This was done for the 450-kilowatt/kilogram specific power and a hydrogen-to-fissile atom ratio of 35. The result was that k_{eff} was reduced down to 1.04 and R increased only up to 0.825.

Core 4-B

The same analysis as described above was done for the 20-megawatt, 4-foot core at a specific power of 450 kilowatts/kilogram. The variation in k_{eff} and in the isotope capture ratio R is shown for core 4-B in Figure 35 as a function of the hydrogen/fuel atom ratio. It is seen for this core that the ratio R continuously decreases with addition of hydrogen to the reactor. Since both the specific power and the power density are the same for core 4-B as for the 6-foot core when no hydrogen is present in the 4-foot core, both the thermal spectra and the over-all spectra are almost the same for the two cores. The addition of hydrogen to core 4-B has the effect of increasing the magnitude of the thermal spectrum, and at the same time the extra hydrogen softens the thermal spectrum and increases the Pu^{239} capture rate in the 0.3-electron-volt resonance.

The results shown in Figure 35 indicate that for a hydrogen/fuel ratio of about 46 the k_{eff} is 1.06 and R is 0.90. This value of R may be high enough that the core would have good reactivity-lifetime characteristics. Burnup studies will be performed in the future for this case.

The effect of the hydrogen addition to core 4-B is shown in Figure 36, in which the thermal-neutron spectrum is plotted against energy.

Conclusion

For the two 4-foot-core cases studied, the addition of ZrH_n moderator has a strong effect on reducing the neutron leakage from the core and, consequently, increasing the k_{eff} . In general, this is accomplished at the cost of reducing the ratio of captures in Pu^{240} to captures in Pu^{239} . The results indicate that the reactivity-lifetime characteristics will be better

Fig. 34--Variation of $\phi(E)$ with energy (core 4-A) for several hydrogen/fuel atom ratios

Fig. 35--Variation of reactivity and isotope capture ratio with hydrogen/fuel atom ratio (core 4-B)

Fig. 36--Variation of $\phi(E)$ with energy (core 4-B) for several hydrogen/fuel atom ratios

for the lower-void, lower-power core. A slightly larger core, for example 4.5-foot-diameter, will be investigated in the future.

The results given herein were from zero-dimensional static calculations and were done mainly to investigate the effect of several parameters on the ability to use ZrH_n addition to good advantage. Burnup studies will be performed for the 4-B core and also for a slightly larger, 4.5-foot-diameter, core to determine the actual potential of the ZrH_n addition to the basic BeO-moderated core.

MATERIALS

The materials problems connected with the use of zirconium hydride in the BeO-PuO₂-fueled gas-cooled reactor were considered during this period. The ZrH was considered for use as a metal-clad moderator element separated from the BeO and fuel element. It was desired that the hydrogen density be as high as possible and that a minimum loss of hydrogen occur during the core lifetime while operating at temperatures in the range of 500° to 650°C. The rate of hydrogen loss is dependent on the hydride composition, the operating temperature, the fuel-element geometry, the cladding materials and thickness, and the type of barrier coatings employed. The rate of hydrogen loss which is tolerable is determined largely by reactor-control considerations, but a value of 1 to 2 percent per year was selected.

Thus, for each design considered, it is necessary to calculate the rate of hydrogen loss to see if it is less than 1 to 2 percent per year; or, given all the materials variables and the fuel-element geometry, it is necessary to calculate the maximum operating temperatures permitted to fall below the maximum rate of hydrogen loss.

The general properties and the pressure-composition-temperature relationships for the hydrogen-zirconium system have been investigated⁽¹⁻⁴⁾ and indicate that significant hydrogen losses can occur under the contemplated design conditions, which involve large surface-to-volume ratios. The H-Zr phase diagram as proposed by Beck⁽¹⁾ shows that above about 60 atom-percent hydrogen ($\text{ZrH}_{1.4}$), a stable hydride phase (epsilon) occurs and exists over a wide composition range approaching stoichiometric ZrH_2 . Up to $\text{ZrH}_{1.4}$, a two-phase alloy exists for which equilibrium hydrogen pressure over these alloys is independent of hydrogen content. At about $\text{ZrH}_{1.4}$, the equilibrium hydrogen pressure increases rapidly with increasing hydrogen content. The relationship between the equilibrium or dissociation pressure, temperature, and hydrogen/zirconium ratio are shown in Figure 37.

Fig. 37--Dissociation pressure isochores of zirconium hydride (see NAA-SR-5943, Ref. 4)

The composition $\text{ZrH}_{1.4}$ ($N_{\text{H}} = \sim 5.2 \times 10^{22}$ atoms/centimeter³) is a desirable alloy from the point of view of minimizing hydrogen loss. If a higher hydrogen content is desired, a composition of about $\text{ZrH}_{1.7}$ ($N_{\text{H}} = \sim 6 \times 10^{22}$ atoms/centimeter³) can be readily made which will, of course, be subject to over an order of magnitude greater rate of hydrogen loss under the same conditions as the alloys with compositions up to $\text{ZrH}_{1.4}$. The selection of the maximum hydrogen content then depends on specific design and operating conditions.

The rate of hydrogen loss is directly proportional to the surface-to-volume ratio of the moderator element. In the case of rods, the rate of hydrogen loss is inversely proportional to the square of the diameter. The desirability to "mix" the moderator in the core for neutronic advantage has led to design concepts with relatively small diameters (2- to 0.5-inch outside diameters) or thin plates which have high surface-to-volume ratios.

The hydriding of zirconium is accomplished by the simple diffusion of pure hydrogen under controlled temperature and pressure conditions. No difficulties are foreseen in achieving compositions up to $\text{ZrH}_{1.7}$ if the thickness or diameter are under 1 inch.

The primary purpose of the cladding will be to provide structural strength to the hydride element and to restrict the loss of hydrogen by providing a diffusion barrier. For the temperature range of interest, less than 1200°F, the use of stainless steel or a nickel-base alloy may be considered from a structural point of view. Since order-of-magnitude differences in hydrogen diffusion and permeation losses can occur between various stainless steels or nickel-base alloys, it is important to consider the retention contribution of the metal cladding itself. However, since design and performance considerations indicate that a high surface-to-volume ratio and high temperatures are required, the use of a metal cladding by itself would lead to a prohibitively high rate of hydrogen loss in the high-temperature core regions. It appears that a reduction in hydrogen loss of several orders of magnitude may be obtained through the use of an enamel-like coating.⁽⁴⁾ This coating would be applied directly to the metal cladding prior to assembly of the hydride element. With the enamel-like coating providing most of the barrier to the diffusion of hydrogen, the metallic cladding would provide the surface for applying the diffusion-barrier coating and would be sized strictly for the structural and minor gas corrosion considerations.

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