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MASTER

T A R G E T

A Program for a 1000-Mw(e) High-temperature
Gas-cooled Reactor

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
MAY 31, 1963

June 26, 1963

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GENERAL ATOMIC
DIVISION OF
GENERAL DYNAMICS

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June 26, 1963

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INTRODUCTION

This report covers the work performed by the General Atomic Division of General Dynamics Corporation under U. S. Atomic Energy Commission Contract No. AT(04-3)-167, Project Agreement No. 17. This Project Agreement calls for preliminary design and related exploratory development of a 1000-Mw(e), gas-cooled, nuclear power reactor incorporating advanced technology. This program, which has been designated TARGET (Thermal Advanced Reactor, Gas-cooled, Exploiting Thorium), is based on the concept of the High-temperature Gas-cooled Reactor (HTGR) system developed by General Atomic.

The related exploratory development effort is aimed at a 1000-Mw(e) gas-cooled reactor plant that could be placed in operation in the 1970's. The plant would demonstrate such new design features as:

1. Breeding or near breeding of fuel in a high-temperature, gas-cooled, thermal reactor using a Th- U^{233} fuel cycle.
2. Advanced reactor fuel elements, which may include new and simpler configurations and new methods of processing and fabricating to extend fuel lifetime and reduce fuel costs.
3. A gas-turbine-driven circulator powered by the main coolant helium.
4. Alternative plant arrangements to reduce plant capital costs, including such possibilities as alternative materials for pressure vessels, and the assembly of the reactor and steam-generating equipment in a single containment.
5. A supercritical-pressure steam cycle, which is particularly adaptable to a 1000-Mw(e) plant and to the inherent high-temperature capabilities of the HTGR system.

TASK I. PRELIMINARY DESIGN STUDY

During the quarter, the reference design for the 1000-Mw(e) high-temperature gas-cooled reactor power plant discussed in the previous quarterly report has taken a more definite shape. This design, which was chosen after a very brief review of possible alternatives, is based on use of a concrete pressure vessel and gas-turbine-driven circulators (turbo-circulators).

Work has now been started on a more detailed evaluation of various alternative concepts, which include the following:

1. The use of a spherical steel pressure vessel with six separate steam generator units inside a close-fitting secondary containment.
2. The use of series-steam-turbine-driven circulators.
3. The use of single gas reheat instead of double steam reheat.
4. Alternative fuel elements having fuel particles intended to release a large percentage of the fission products.
5. A fission-product trapping system suitable for the above type of fuel elements.

Figures 1.1, 1.2, and 1.3 illustrate for the reference design the general layout of the nuclear steam supply of the plant. Construction and maintenance have been simplified by a new arrangement in which two steam generators and a circulator comprise a self-contained unit that can be dismantled in place and completely removed. This eliminates the necessity for the partition between individual steam-generator-circulator units shown in Figs. 1.1 and 1.2 of the previous quarterly report GA-4072. Except for a simple core containment structure, all components can be removed from the pressure vessel for maintenance. Preliminary plant layouts for both concrete and steel vessel concepts are shown in Figs. 1.4 through 1.9.

STEAM-CYCLE STUDIES

A comparative study is under way to determine the economic feasibility of double steam reheat versus single gas reheat. For this

Fig. 1. 1--Plan section of TARGET 1000-Mw(e) reactor plant circulator and boiler arrangement (6 circulators)

Fig. 1.2--Radial section of TARGET 1000-Mw(e) reactor plant
circulator and boiler arrangement (6 circulators)

Fig. 1. 3--Radial view of TARGET 1000-Mw(e) reactor plant circulator and boiler arrangement (6 circulators)

Fig. 1. 4--Plant arrangement for prestressed concrete vessel (basement)

Fig. 1. 5--Plant arrangement for prestressed concrete vessel (third floor)

Fig. 1. 6--Plant arrangement for prestressed concrete vessel (section A-A)

Fig. 1. 7--Plant arrangement for spherical steel vessel (third floor)

Fig. 1. 8--Plant arrangement for spherical steel vessel (section A-A)

Fig. 1.9--Plant arrangement for spherical steel vessel (section B-B)

study two reference cycles have been established:

1. Double steam reheat with a turbocirculator and concrete pressure vessel; and
2. Single gas reheat with a series-steam-turbine-driven helium circulator and steel reactor pressure vessel.

Included also in these comparative studies is an economic evaluation between turbocirculator and series-steam-turbine-driven helium circulator.

The cycle performances have been calculated for these two reference cycles and the results are summarized in Table 1.1. The heat balance for the double-steam-reheat cycle with the turbocirculator is shown in Fig. 1.10.

Table 1.1

CALCULATED PERFORMANCE FOR REFERENCE STEAM CYCLES

	Double steam reheat with <u>turbocirculator</u>	Single gas reheat with series steam- <u>turbine drive</u>
Reactor thermal power, Mw.	2270	2270
Throttle pressure, psig.	3515	3515
Throttle temperature, °F.	1050	1050
First reheat temperature, °F.	1000	1050
Second reheat temperature, °F.	1000	-----
Throttle steam flow, 10 ⁶ lb/hr.	6.46	7.01
Condenser pressure, in. Hg abs.	1.0	1.0
Turbine arrangement	CCQF 52 in.	CCQF 52 in.
Net station output, Kw.	1067.03	1031.09
Net station heat rate, Btu/Kw-hr.	7261	7511
Net station efficiency, %	47.00	45.44

As part of the comparative study, preliminary cost estimates are to be prepared for the 1000-Mw(e) turbine plants based on the two reactor-plant concepts. Arrangements have been made for the assistance of a consulting engineering firm on this preliminary evaluation of the conventional portion of the plant.

Several other steam cycles are being evaluated for their performance and, where possible, their costs will be synthesized from the reference-cycle cost estimates.

Fig. 1. 10--Heat-balance diagram for plant with turbocirculators

A number of steam-cycle improvements, such as an increased number of feedwater extractions, the utilization of hydrogen and oil coolers in the regenerative feedwater system, and the application of a third reheat, were investigated and the resulting efficiency changes determined. The combined effect results in a net station efficiency of 48.18% for the double-steam-reheat reference cycle with a turbocirculator. The net station efficiency is increased to 49.5% for the same cycle with 4500/1100/1050/1050°F steam conditions. Although these efficiency improvements are technically feasible, they are not necessarily economically justified.

The performance with a series-steam-turbine-driven circulator topping the main turbine has also been investigated. The drive turbine is assumed to be located within the superheater and thus does not reduce the available energy across the prime mover. The part-load behavior has been assessed for two different cases:

1. Topping a constant-throttle-pressure main turbine;
2. Topping a gliding-throttle-pressure main turbine.

Two types of blower-turbine controls have been assumed for the constant-throttle-pressure case:

1. Back-pressure control by main-turbine throttle, BT;
2. Nozzle-area control of blower turbine, NOZBT.

Figure 1.11 shows the helium flow resulting with these control schemes. The graph also shows the helium flow line for constant hot-helium temperature and constant transition-zone location in the steam generator. In the case where the main-turbine throttle pressure controls the blower-turbine output (BT), the helium flow line follows the constant-hot-helium flow line very closely. There exists no margin for load changes. The blower turbine and its control are very simple in contrast to the nozzle-area-controlled case. The nozzle-area-controlled topping blower turbine (NOZBT) yields a helium flow line similar to that resulting from the series turbine drive in the cold reheat line. Ample control margin is available.

In the case of a gliding-pressure-controlled main turbine (GLPBT), the helium flow line follows the nozzle-area-controlled turbine closely down to the load point of minimum throttle pressure. Three cases having different minimum main-turbine throttle pressures (1500, 2000, and 2500 psia) are shown on Fig. 1.11. Below this load point, the blower turbine output is determined by the main-turbine throttle valve. Excess turbine power is available for control purposes in all three instances for operation with constant-hot-helium temperature.

Fig. 1.11--Relative helium flow versus relative steam flow (BT = back-pressure controlled by constant main-turbine throttle pressure; NOZBT = nozzle-area-controlled blower turbine, constant main-turbine throttle pressure; GLPBT = blower turbine topping a gliding-throttle-pressure main turbine--pressure ratios refer to main-turbine inlet throttling)

Figure 1. 12 shows the resulting system head curve for the blower-turbine-control schemes discussed above. The flattest system curve is achieved by a nozzle-area-controlled blower turbine (NOZBT) in which the blower-turbine inlet pressure is kept constant. The back-pressure-controlled blower turbine (BT) yields a steeper system characteristic due to a varying pressure drop across the blower turbine. The steepest system characteristic results from a gliding-pressure-control scheme (GLPBT). This is undesirable for a system fed by a constant-speed boiler feed pump. The system curve can be flattened by the introduction of throttle valves at the main-turbine and blower-turbine inlets, which at the same time reduces the excess turbine power (see Fig. 1. 11).

STEAM-GENERATOR DESIGN

Sizing and designing of the tube bundles for the reference design (double steam reheat, concrete pressure vessel) has been completed. Design of the header-to-vessel connections, tube supports, baffling, and bundle support is in progress. Two high-pressure steam reheaters were chosen because the weight of a single unit for full-plant duty would be excessive (approximately 500 tons).

The size of a steam generator for a plant based on a steel pressure vessel with six loops (single gas reheat) has been tentatively established for plant layout studies. This sizing is proceeding on the basis of parallel operation of reheater and superheater with respect to gas flow and utilizes tube bundles similar to those for the concrete pressure vessel.

FISSION-PRODUCT CONTROL

As a result of analytical and experimental work indicating the possible desirability of complete removal of the fission-product krypton, xenon, iodine, and bromine from the core, it has become necessary to evaluate a large range of possible helium purification systems. To facilitate this, a method has been developed to evaluate easily a large range of possible fuel-element purge-flow rates through a purification system, a large spectrum of trap sizes and shapes, and various temperature levels. Using this method, satisfactory combinations of the above variables have been identified and further work is being done on them.

Analysis shows that the heat-generation rate of the above fission products is approximately 40 Mw(e); of this, approximately 30 Mw(e) would be generated with very short delay times. Thus, one of the most attractive combinations would remove this initial heat in a high-temperature delay bed followed by lower-temperature delay beds to remove the parents of long-lived plateout producing daughters.

Fig. 1. 12--Relative system head versus relative steam flow (BT = back-pressure controlled by constant main-turbine throttle pressure; NOZBT = nozzle-area-controlled blower turbine, constant main-turbine throttle pressure; GLPBT = blower turbine topping a gliding-throttle-pressure main turbine--pressure ratios refer to main-turbine inlet and blower-turbine inlet throttling)

A possible method of accomplishing the high-temperature delay is to include an 8-ft-long delay bed underneath each fuel element, but physically separate from the fuel element. This trap would run between 600° and 800°F, and would remove the iodines, bromines, and the short-lived xenons and kryptons. A preliminary evaluation has shown that by using the main coolant as a heat sink the heat-transfer capabilities of these traps are satisfactory. One additional benefit of these traps is that the heat generated in them would be of a usable quality.

A satisfactory design of the lower-temperature-level delay beds has been worked out. This would call for 12 delay beds 2-1/2 ft in diameter by 30 ft long located within the pressure vessel. These would consist of layers of charcoal interspersed between extended-surface-area-type heat exchangers. These beds would completely remove the noble-gas parents of objectionable plateout activity.

The main loop activities resulting from the above described processing of the purge stream present problems which are currently under study.

TASK II. RELATED EXPLORATORY DEVELOPMENT PROGRAM

II-A. ADVANCED FUEL DEVELOPMENT

Design

Cross-sectional views of eight different advanced fuel-element concepts were prepared during this period; they are shown in Figs. 2. 1 through 2. 8. All but one, the bare unpurged type, can utilize purge-gas streams to limit fission-product leakage into the main coolant gas of the reactor. The design studies have included the use of both carbide and oxide fuel particles. Oxide fuel is particularly interesting as a fission-product releasing fuel to improve the neutron economy of the reactor.

Experiments were performed to determine the crushing resistance of graphite spacer rings in line contact at high loads. Matching HLM-85 graphite cylinders 4.5 in. in outside diameter and having 1-in. -wide spacer rings with a 4.7-in. outside diameter were loaded in compression at the spacer-ring surfaces. Three specimens tested withstood contact loads of 5800 lb before even very slight crushing marks were observed. Spacer contact forces are not expected to exceed 600 lb in a typical large HTGR.

Core performance studies were made on four fuel-element designs--an enlarged Peach Bottom type (Fig. 2. 1), a bare unpurged type (Fig. 2. 2), a packed-bed type (Fig. 2. 4), and a hollow annular type (Fig. 2. 5). These studies indicate the interrelationship of power density, maximum fuel temperature, and core pressure drop for each case. The results are shown in Figs. 2. 9 through 2. 12. The relationship between diameter of core, number of fuel elements, and power density is shown in Fig. 2. 13. In all four cases the following reactor parameters were used:

Reactor thermal power	2337 Mw
Fuel-element pitch	4.70 in.
Radial flux, max/mean	1.30
Main coolant pressure	450 psia
Coolant inlet temperature	750° F
Coolant exit temperature	1500° F
Fuel-element active length	15.5 ft
Total fuel-element length	20 ft

A study was made of the temperature rise across a gap between the fuel and the sleeve. In the range from 0 to 0.005 in., each 0.001 in. of

Fig. 2. 1--Enlarged Peach Bottom-type fuel element

Fig. 2. 2--Bare, unpurged fuel element with BeO

Fig. 2. 3--Multihole-matrix fuel element; can be self-purged

Fig. 2. 4--Packed-bed fuel element

Fig. 2.5--Coextruded, annular fuel element; can be self-purged

Fig. 2.6--Annular, radially purged fuel element

Fig. 2. 7--Fuel element having oxide fuel in matrix

Fig. 2.8--Compacted oxide-powder fuel element

Fig. 2.9--Core pressure drop versus power density for Peach Bottom-type fuel elements.

Fig. 2.10--Core pressure drop versus power density for bare, unpurged fuel element

Fig. 2. 11--Core pressure drop versus power density for packed-bed central purge fuel element

Fig. 2. 12--Core pressure drop versus power density for annular-matrix, hollow fuel element

Fig. 2. 13--Diameter of core and power density versus number of fuel elements

gap is worth about 30°F in temperature rise. Because of increased radiant heat transfer with large gaps, the temperature rise tends to flatten out when the gap reaches approximately 0.10 in. However, the temperature rise is about 600°F across a 0.10-in. gap. A plot of temperature rise versus gap is shown in Fig. 2.14. The fraction of the heat transfer that is due to radiation is plotted in Fig. 2.15.

Initial data have been obtained on two spacer-ring geometries in the spacer pressure-loss test. The loss coefficient for a spacer having a 0.944-in. approach, a 0.944-in. straight section, and a 0.944-in. diffuser length is plotted in Fig. 2.16. A similar plot for a spacer having a 1.889-in. approach, a 1.889-in. straight section, and a 1.889-in. diffuser length is shown in Fig. 2.17.

The apparatus for a back-diffusion test on a packed-bed type graphite specimen is nearly completed. The purge flow is radially inward in this test. Krypton-85 is used as a tracer element. A cylindrical specimen is also being fabricated to measure steam-exposure effects on back diffusion. The test specimen is a 2-ft section of HLM-85 graphite having two resin impregnations; the helium permeability is in the 10^{-2} cm²/sec range.

Tentative system gaseous and plateout activities were calculated for the TARGET reactor. The calculations were made for the eight different fuel-element designs under consideration. These calculations indicate a considerable spread in system activities for the different designs.

Graphite Development

Graphite Evaluation. A number of approaches to developing a source of sleeve material are being explored with the Speer Carbon Company; they are listed in Table 2.1. All the graphites in the group under investigation are made from the same raw materials and differ only in the standard sizes normally produced, in the mix proportions, and in the number of impregnations. The pore structures resulting from the different treatments are shown in Fig. 2.18. The back diffusion test results are given in Fig. 2.19. From the two figures, it may be seen that the materials with most of the pores in the larger sizes are most responsive to inflow (a small ΔP causes a large drop in the krypton permeation coefficient) and those with a greater proportion of small pores are less responsive. All the materials pictured in Fig. 2.19, however, are acceptable for the fission-product-control requirement.

Tests on 711-T show a tensile strength about 10% higher than for HLM-85, and X-ray diffraction indicates a graphitic structure. As thermal conductivity values for 710-G were about 17% low at the temperature of interest, this value is being determined on 711-T. Thermal expansion data on 711-GS (a single pitch impregnation of 711-G) are summarized in table 2.2.

Table 2.1

CANDIDATE SLEEVE MATERIALS FROM SPEER CARBON COMPANY

Graphite	Description	Density	Permeability at R. T., 1/2 atm $\bar{P}$ (cm^2/sec)	Back Diffusion Character	Summary
710-G	Stock made with no ungraph- itizable filler. No pitch impregnation. Small diameters only.	1.78	5×10^{-2}	Excellent at low ΔP (no data at high ΔP).	Permeability too high for present designs.
710-G	One furfuryl impregnation to base stock.	1.82	3×10^{-3}	Approaches refer- ence graphite performance.	A suitable material from porosity requirements.
711-T	Base formula same as 710-G but made to 2-1/2-in. diam. Two pitch impregnations.	1.82	3 to 7×10^{-3}	Excellent at low ΔP .	A suitable material if permeability can be kept in low range.
711-TS	Same as 711-T but three pitch impregnations.	1.86	1 to 6×10^{-3}	Not yet tested. Expected better than 711-T.	Should be excellent at lower permeabilities.
780-S	Same materials as 710-G, but larger proportion of coarse particles in mix. One pitch impreg- nation. Made to diameter of interest.	1.75- 1.80	3×10^{-2}	Not yet tested. Expected to be like 710-G.	Permeability too high for present designs.

Fig. 2.14--Temperature rise across gap versus gap size for a multihole-matrix fuel element

Fig. 2. 15--Fraction of heat transfer across gap by radiation

Fig. 2. 16--Results of spacer pressure-loss test

Fig. 2. 17--Results of spacer pressure-loss test

Fig. 2. 18--Pore structure of Speer graphites

Fig. 2.19--Back-diffusion characteristics of Speer Graphites (710-711 series)

Table 2.2
THERMAL EXPANSION OF 711-GS GRAPHITE

Orientation	Coefficient (10^{-6} in. / in.)		
	300°C	700°C	950°C
Parallel	1.8	2.5	3.0
Perpendicular	3.2	4.0	4.3
Ratio, perpendicular/parallel	1.8	1.6	1.4

It is concluded that these materials have potential for sleeves where machinable geometries are required. However, studies of backflow characteristics at reactor temperature and pressure, stability during steam attack, and irradiation stability must be made before the materials can be considered fully evaluated.

Impregnation. On the basis of a study to determine the heat-treat temperature necessary on General Atomic extruded graphite prior to impregnation, a treatment to about 2000°C has been selected for the reference process.

A 16-in. length of General Atomic extruded stock (3-1/2 in. OD by 2-3/4 in. ID) was taken through all the steps of the impregnation process. The stock had an as-graphitized density of 1.47 g/ml before processing and a final density of 1.66 g/ml. A 300 cp monomer was used; blow-through was 11%; carbon gain was 10%; and the final permeability was 9×10^{-5} cm²/sec. The effects of the treatment on pore structure are given in Fig. 2.20; the back-diffusion character is given in Fig. 2.21, along with the reference Peach Bottom graphite and the results from an earlier impregnation of 1-in. diam. extruded stock. A comparison of the back-diffusion profiles for the small and large General Atomic extruded stock indicates that the process did not alter during scaleup.

Extrusion. Following modifications to the 500-ton extrusion press, a 19-ft tube (3-1/2 in. OD) was extruded. Straightness and green density (1.81 g/cm²) were good, but circumferential discontinuities resulted from "surging" in the extrusion chamber. Various die and press modifications are being studied to learn the cause of this surging during the extrusion of tubular stock. Different die-entry angles are being investigated by means of die inserts. A facility for billeting the mix prior to extrusion is being set up to improve temperature control and the uniformity of material flow to the die, to speed loading of the press, and to control fuel distribution in the fueled graphite.

Fig. 2. 20--Pore spectra of General Atomic extruded graphite before and after impregnation
(3-1/2-in. OD tube)

Fig. 2.21--Back-flow characteristics of General Atomic extruded graphite

Investigations of the effects of time and temperature on the extrusion mix while standing in the press showed that at 400°F the mix rapidly degrades and becomes unextrudable. By achieving closer temperature control and uniformity, successful extrusions were made after the mix had been retained in the chamber for 2 hr at 300°F.

Means of reusing heels and trimmings from extruded stock are being investigated. It has been found that the green extrusions can be ground and re-extruded with no change in the behavior or green densities of the extrusions.

Short (16-in.) sections of tubular extruded graphite have been successfully baked and graphitized; however, densities are only 1.4 to 1.5 g/cm³. These studies indicate that the means of support needs to be slightly modified to prevent crack formation as the tubes shrink.

An investigation of possible stresses in tubular graphite was made by slitting samples along one side and measuring the resultant change in the circumference of the tubes. HLM-85 had a calculated tensile stress (snap ring) of 4 psi before impregnation and 26 psi after impregnation. General Atomic extruded, baked, and graphitized stock had a stress of 300 psi (this corresponded to a change in gap width of +21 mils). After impregnation the stress in the General Atomic stock was reduced to 65 psi.

Radiation Studies. A number of experimental graphites prepared as possible fuel matrix materials by hot molding or hot working were irradiated at about 450°C (Capsule 1G1) and 1400°C (Capsule 2G2) to an exposure of about 2×10^{21} nvt ($E > 0$, 18 Mev). A reference graphite (NPR needle coke graphite manufactured by National Carbon Company) was included for comparison with Hanford data. The capsules were uninstrumented drop-in type; thus, the temperature was not controlled or monitored. Dose wires were included to check the calculated flux values. The temperatures are uncertain to $\pm 150^\circ\text{C}$.

The fabrication data on the experimental graphites are given in Table 2.3. The changes in dimensions are given in Table 2.4. The contraction data of the NPR reference graphite included in the 450°C capsule (1G1) do not agree with Hanford data obtained on similar graphite that was irradiated at 475°C in instrumented capsules. There are no Hanford data at 1400°C for comparison. The only explanation of the higher contraction in the perpendicular direction over that in the parallel direction in NPR and AGOT extruded graphite is a possible mixing of the samples during the experiment. Caution should be used when comparing the data among the various samples within a single capsule because of the axial-temperature and fast-flux gradients (capsules were 7 in. long) existing in the test hole during irradiation. However, the relative magnitude of the dimensional changes can be ascertained from these experiments.

Table 2. 3

FABRICATION DATA ON EXPERIMENTAL SAMPLES IN CAPSULE 1G1 AND 2G2

Sample Type	Raw Materials		Forming Method	Final Heat Treatment (°C)	Remarks
	Filler	Binder			
NPR (reference)	Needle coke	Pitch	Extrusion	>2800	Graphite under extensive irradiation testing at Hanford.
Fuel matrix (control)	Graphite flour	10% pitch	Hot molding	1800	Standard fuel-matrix graphite developed for Peach Bottom.
Fuel matrix (prepyrolyzed binder)	Graphite flour	25% pitch	Hot molding	1800	Pitch-graphite mixture pyrolyzed at 200°C for 16 hr before fabrication.
Fuel matrix (impregnated)	Graphite flour	25% pitch	Hot molding	1800	Fuel matrix control sample impregnated with furfuryl alcohol after fabrication. Impregnant residue was heated to 900°C.
AGOT (control)	Petroleum coke	Pitch	Extrusion	>2800	National Carbon standard nuclear nuclear moderator graphite.
Hot-worked AGOT	Petroleum coke	Pitch	Extrusion	>2800	Worked in compression parallel to the extrusion axis at about 2700°C to a true strain of ~50%.

Table 2.4

DIMENSIONAL CHANGES IN PERCENT OF EXPERIMENTAL GRAPHITES

IRRADIATED TO APPROXIMATELY 2×10^{21} nvt ($E > 0.18$ Mev)

NPR (reference)	Fuel Matrix (control)	Fuel Matrix (prepyrolyzed binder)	Fuel Matrix (impregnated)	AGOT (control)	AGOT* (hot worked)
450±150°C, Parallel to Forming Axis, Capsule 1G1					
-0.55 (a)	+0.10 (c) +0.18 (c)	+0.02 (c) +0.14 (c)	-0.36 (c) -0.36 (c)	-0.61 (a)	+0.59 (c)
450±150°C, Perpendicular to Forming Axis, Capsule 1G1					
-0.53 (c)	-0.74 (a)	-0.86 (a)	-0.98 (a)	-1.36 (c)	-0.85 (a)
-0.79 (c)	-0.74 (a)	-0.83 (a)	-0.98 (a)		-0.71 (a)
-0.84 (c)	-0.75 (a)	-0.82 (a)			
	-0.78 (a)	-0.82 (a)			
1400±150°C, Parallel to Forming Axis, Capsule 2G2					
	+0.08 (c) -0.02 (c)	+0.22 (c) +0.36 (c)	-0.18 (c) -0.65 (c)	-0.16 (a)	+2.19 (c)
1400±150°C, Perpendicular to Forming Axis, Capsule 2G2					
-0.67 (c)	-1.30 (a)	-1.27 (a)	-1.63 (a)	-1.33 (c)	-1.92 (a)
-0.11 (c)	-1.21 (a)	-1.45 (a)	-1.44 (a)		-1.91 (a)
	-1.42 (a)	-0.85 (a)			
	-1.54 (a)	-1.70 (a)			

NOTE: The direction of measurement with respect to crystal orientation (a or c) is given in parentheses after each measurement. The preferred crystal orientation of the hot pressed material is 90° to that of the extruded graphites.

*Hot working an extruded graphite in compression parallel to the extrusion axis apparently reverses the crystal orientation and the contraction anisotropy corresponds to a molded sample. The dimensions of the hot-worked specimens were 0.250 and 0.375 in.; all others were either 0.5 or 1 in.

The high degree of crystal orientation imparted by the hot-molding technique is observed from these data. Impregnating the hot-pressed material reduces the anisotropy ratio. Hot pressing apparently reverses the crystal orientation, and the graphite is thus quite plastic during the hot working.

The magnitude of contraction in the radial direction (perpendicular to molding) of the molded and hot-worked graphite was relatively high when irradiated at $\sim 1400^{\circ}\text{C}$. The radial contraction of these samples is also relatively high when irradiated at 450°C if they are compared to the Hanford data on needle coke graphite, but they appear to be only slightly higher when compared to NPR samples in the same capsule. The increased anisotropy imparted by the molding operation or hot working is probably responsible for the increased contraction in the radial direction rather than a gross difference in the basic graphite structure.

X-ray diffraction analyses were performed on several of these graphites. Slight expansion of the layer planes and slight contraction of the a-axis was observed in samples that had been irradiated at $\sim 450^{\circ}\text{C}$. Duplicate samples that had been irradiated at $\sim 1400^{\circ}\text{C}$ exhibited a similar change in the a-axis, but little change in the layer-plane dimensions. Thermal-conductivity measurements on the irradiated graphites from the same capsules showed severe reductions in conductivities at temperatures below 1000°C . These irradiation effects were annealed out on heating at 1100° to 1300°C .

Capsules G5 and G6, which contain samples of 1-in. -diameter extruded graphite of various pore structures and heat treatments, continued to be irradiated in GETR. They will be discharged next quarter after having received a fast flux exposure of $\sim 2 \times 10^{21}$ nvt.

Fueled Materials

Carbide Fuel Systems. Further evaluations were conducted on the effect of coated particle concentration on the physical properties of hot-pressed and extruded graphite-matrix fuel bodies. A sharp decline in strength for hot-pressed compacts at loadings greater than 30 vol-% was reported last quarter. This loss of strength was not observed in current studies with extruded bodies. However, while there was negligible change in both crushing and shear strengths, the extruded bodies were much weaker at low fuel concentrations than their hot-pressed counterparts. Typical values are given in Table 2.5.

Thermal-conductivity measurements in the radial direction were performed on the hot-pressed and extruded bodies, and the results are summarized in Table 2.6.

Table 2. 5
STRENGTH OF FUEL BODIES

Vol-% of Coated Particles	Crushing Strength (psi)		Shear Strength (psi)	
	Hot Pressed	Extruded	Hot Pressed	Extruded
0	6900	4200	2800	1600
10	6700	3700	2800	1600
20	6500	3700	2500	1600
30	5800	3800	2500	1600
40	3700	3900	1800	1600

Table 2. 6
RADIAL THERMAL CONDUCTIVITY OF FUEL BODIES

Vol-% of Coated Particles	Conductivity at 1200° C [Btu/(hr)(ft)(° F)]		Conductivity at 1500° C [Btu/(hr)(ft)(° F)]	
	Hot Pressed	Extruded	Hot Pressed	Extruded
0	30. 2	21. 8±2	24. 2	16. 9±1
10	20. 5	16. 9	18. 9	12. 1
20	24. 2	19. 4	20. 5	14. 5
30	19. 4	9. 7	17. 9	7. 3
40	17. 2	7. 3	15. 7	6. 0

The conductivity of the hot-pressed compacts is considerably higher than that of the corresponding extruded bodies. The addition of coated particles appreciably lowers the conductivity of both types of compacts; the effect of the coated particles is most pronounced at lower temperatures (see Figs. 1. 2 and 1. 3).

Oxide Fuel Systems. The investigation of the use of agglomerated oxide particles in place of coated carbides to reduce fuel costs for advanced fuel-element designs was continued during the quarter. Fuel compacts containing agglomerated (but unsintered) fuel particles of $\text{ThO}_2\text{-UO}_2$ exhibited extremely high release rates in fission-product release tests, and appear promising as fission-product-emitting fuels. However, prolonged exposure to high temperature and irradiation may result in sintering of the particles or conversion to carbide and lead to poorer fission-product emission.

An initial study was made of the stability of $\text{ThO}_2\text{-UO}_2$ particles during fabrication of hot-pressed graphite-matrix fuel compacts at various temperatures and atmospheric conditions. A 1-in.-diameter hot-pressed

compact of conical design was used as the test specimen. This compact, when heated by electrical resistance, showed a thermal gradient between the center and ends, thus giving a range of temperature exposures over a single compact. Diagrams of the specimens (which contain 14 temperature sighting holes) are shown in Figs. 2. 22 and 2. 23.

At temperatures well below that required for processing the graphite matrix (1800°C), the oxide particles showed a tendency to convert to the carbide in vacuum. This conversion was observed by sectioning the test compact after exposure and allowing it to stand in a humid atmosphere, thus reconverting to oxide any of the particles that had been converted to carbide. These reconverted particles were easily distinguished by visual observation. A compact sintered in vacuum at temperatures up to 1680°C (Fig. 2. 22) showed that conversion to carbide occurred at temperatures above about 1350°C .

Under 0.5 atm of carbon monoxide, however, conversion to the carbide was not noted until approximately 1950°C , well above the desired processing temperature (Fig. 2. 23). It therefore appears feasible to process fuel compacts containing oxides at the usual temperatures by the simple expedient of heat treating them under a partial atmosphere of CO.

Additional tests are being conducted to determine the fission-product release characteristics of oxide particles in graphite matrix compacts after annealing under various conditions of time, temperature, and atmosphere. Currently, fission-product-release tests are being conducted on three hot-pressed graphite compacts containing particles of agglomerated thorium-uranium oxides. Two of these compacts have been heated for 100 hr at 1300° and 1500°C to determine the effect of sintering on the ability of the oxide particles to release fission products. The third compact, which has received no heating other than that encountered in the hot pressing process (1000°C), will serve as control. The two test compacts were heated isothermally in an atmosphere of helium containing small amounts of CO and H_2 .

The uranium and thorium oxides in general use in the nuclear industry today are ceramic grades that have been developed to sinter and densify at temperatures far below their melting point. However, if these powders are calcined (heat treated), the temperature at which sintering occurs is increased. If the metal oxides are heated up to their melting point, then cooled and reground, the powders are extremely difficult to sinter at temperatures below the melting point. This material is usually referred to as fused metal oxide. Such oxide powders are commercially available, but are more expensive. Samples of heat-treated and fused uranium and thorium oxides are on order for evaluation.

Fig. 2. 22--Presized (Th, U)O₂ particles in hot-pressed graphite compact heated in vacuum

Fig. 2.23--Presized (Th, U)O₂ particles in hot-pressed graphite compact heated in 0.5 atm CO

Recycled Fuel. Preliminary analysis of the problems involved in the handling of recycled thorium-uranium reactor fuels shows U^{232} buildup to be the principal radiation hazard. In thorium-uranium fuel cycles, U^{232} is the direct result of a high energy $n, 2n$ reaction in U^{233} , or the indirect result of the same reaction in Th^{232} followed by beta decay and a subsequent n capture in the intermediate nuclide Pa^{231} . Based on an estimated U^{233} $n, 2n$ cross section of 0.4 b above 7.8 Mev, or 0 for lower energies, and a Th^{232} $n, 2n$ cross section of 0.5 b for $E \geq 7.78$ Mev, 0.42 b for $4.72 \leq E \leq 7.78$ Mev, 0 for $E < 4.72$ Mev, the U^{232} buildup after 10 years is approximately 50 ppm of U^{232} in U. The Th:U ratio was 15:1 for this calculation. It is estimated that 40 ppm of U^{232} would create a radiation dose rate of 5 mr/hr within 15 days after final separation, measured at one meter from a 1 kg source.

Direct hand contact 10 to 40 hr per week is limited to dose rates of 500 mr/40 hr and total body exposure is limited to 100 mr/40 hr. Thus, direct handling of these materials by ordinary methods could only be accomplished within a few days after separation from the original reactor radiation contaminants. To reduce the dose rate to 2 mr/hr 35 days after separation, 5.47 cm of lead or 29.7 cm of concrete would be required for 1 kg of U containing 40 ppm U^{232} .

The principal hazard in handling of U^{232} is the strong beta and gamma radiation resulting from its daughter products Bi^{212} and Pb^{212} . These materials can be eliminated if the intermediate Pb^{212} is eliminated.

Radiation hazards are not only the result of fission products or radioactive decay, but also occur from an α, n reaction with many of the light elements. Particularly harmful are Li, Be, F, Na, Al, Si, Cl, Ar, Mg, and K. When these elements are present in concentrations between 4 and 800 ppm in a kilogram U^{233} sphere, depending on the element, a neutron dose rate exceeding 1000 mr/40 hr can be produced by these elements at the surface of the sphere 3.5 months after processing the uranium.

Bremsstrahlung X-rays and beta emissions can be handled by the shielding required for the α , γ , and n radiations. In natural or slightly enriched uranium fuels, U^{237} is also a radiation problem.

II-B. TURBOCIRCULATOR DEVELOPMENT

Performance

The primary circuit of the gas-cooled reactor consists of the reactor, the steam generator, and a means for circulating the gas between the two, so that the heat produced in the reactor is transferred to the working medium from which power is produced. Three different means of providing the work required for pumping the gas around the circuit are (1) an electric motor driving through a fluid coupling, (2) a steam turbine in series with the main steam turbine, and (3) a turbocirculator in which the work results from a lowering of the thermodynamic potential of the gas stream. The first two methods result in thermodynamic losses to the cycle, whereas the turbocirculator does not affect the thermal efficiency of the plant because all of the energy involved is rejected at a potential above that at which the steam plant operates. Slightly higher temperatures are required in the gas stream, however, to keep the heat-transfer surface requirements of the steam generator from increasing.

The top sketch in Fig. 2.24 is a flow diagram of a gas-cooled reactor with a turbocirculator. The heat produced in the reactor is transferred to the steam in the steam generator. Heat is rejected from the cycle through the condenser, while the electrical energy produced in the generators is the work output. The steam conditions and the work output determine the thermal performance.

The bottom sketch in Fig. 2.24 is a flow diagram of a gas-cooled reactor with a circulator driven by an electric motor. The energy for the motor had to be produced by the plant, and although the thermal equivalent of this work remains in the cycle, additional heat is required in the reactor. There is an added amount of heat rejected in the condenser for the same net output as in the previous case, because work has been degraded to heat. A larger amount of steam flow results.

In the case of a gas-cooled reactor with a series-steam-turbine drive, the energy for driving the circulators originates in the steam. As in the previous case, additional reactor heat is required, there is a greater amount of rejected heat in the condenser, and for the same net output more steam flow is required. However, the electric generator is the same as in the case of the turbocirculator.

Fig. 2.24--HTGR flow arrangements showing two means of driving the helium circulators

Externally driven circulators require, in addition to an increase of reactor heat, increased steam flow when compared to the turbocirculator. Since the circulators are within the free-body boundary of the plant, it is only the second law losses which affect the energy interchanges over this boundary and, consequently, the thermal efficiency. Figure 2.25 shows the required additional reactor heat for $\Delta P = 12$ psi as a function of plant thermal efficiency for both series-steam-turbine-driven and electric-motor-driven circulators as compared to a turbocirculator. Three different efficiencies are depicted in the case utilizing the motor because they depend on the particular design that may result. The energy interchanges within the free-body boundary result in increased steam flow requirements which are shown on Fig. 2.26 as a function of plant thermal efficiency. This incremental steam-flow increase is a direct index of the required capacity increase for the various plant components that externally driven circulators require. Points representing the Peach Bottom, ESADA, and TARGET plants for the same pressure loss of 12 psi are shown on both curves.

It is possible to combine these results and weight them so as to arrive at an over-all plant incremental increase in capital cost if a breakdown between the reactor, steam generator, and remainder of the conventional plant, and electric generation and transformer is assumed. Let

Reactor = 35% of plant capital cost,
 Conventional - electrical + steam generators = 60%,
 Electrical = 5%.

With these assumptions of cost breakdown, Fig. 2.27 can now be constructed to show the plant capital cost increase as a function of thermal efficiency for the various methods of circulating the primary coolant gas. These curves apply only approximately to the incremental costs for the plant and cannot be used for the over-all costs because plant capital cost is nonlinear.

In the analysis, it was assumed that the steam generator for externally driven circulators is identical with that for the turbocirculator. This implies that the reactor inlet and exit temperatures are increased by approximately 60° F. This could result in a penalty in the reactor but can be compensated for with a slight increase in pumping power. Moreover, it is possible for the steam generator to be altered to operate with a lower log-mean-temperature difference which would require more heat-transfer surface or a change in the gas flow in the steam generator so that a higher effectiveness would compensate for this temperature difference. In any event, this temperature increase is not taken into account in this study.

Fig. 2.25--Incremental reactor-heat increase versus plant thermal efficiency

Fig. 2.26--Incremental steam-flow increase versus thermal efficiency

Fig. 2.27--Incremental capital-cost increase versus plant thermal efficiency

Preliminary Design

Although the preliminary design is still being actively pursued, some of the results can be presented at this time. The continuing design effort may necessitate changes to alleviate problem areas as they arise.

Figures 1.2, 1.3, and 2.28 show the components of the turbocirculator in a concrete pressure vessel. The turbine is on top with the hot gas from the reactor flowing into the turbine inlet and discharging down. The flow accelerates into the blade passages and begins the diffusion process after the two-stage turbine. This flow is interrupted by the multiple compressor-inlet passages, the outside of which are suitably shaped to prevent too great a rate of diffusion. The area schedules of both flow passages are given in Fig. 2.28. The flow continues to the end of the diffusion process and discharges into the steam-generator inlet as shown in Fig. 1.2. The gas flows upward through the steam generator and enters the compressor inlet through the previously mentioned multiple inlets through the hot-gas passage. The flow accelerates into the blading, is compressed by the five-stage compressor, and then diffuses in a conical diffuser that discharges at the bottom of the duct into the reactor. The gas flows of both components are in the same direction, so that the thrust loads from each of the machines subtract from each other, resulting in a minimum thrust level. The use of concentric passages allows the long axial diffusing passages, which are necessary for efficient diffusers, to remain within the height envelope of the reactor. The inlets and diffuser passages will require further design and development to ensure that the rate of diffusion is such that there will be no flow separation in the passages. Although other designs have been and are being investigated, this one appears at present to be the most promising.

For the configuration discussed, a number of aerodynamic designs were computed, and the details of the optimum one are given in Table 2.7.

Table 2.7

DETAILS OF OPTIMUM AERODYNAMIC DESIGN OF TURBOCIRCULATOR

Compressor

Number of stages	5
Pressure ratio	1.142
Temperature rise	69.6° F
Blading effective (calculated)	91.3%
Diffuser effective	85%
Diffuser pressure loss	1.22 psi
Compression effective including diffuser	89.4%

Inlet Temperature	1142°R
Blade diameter ratio	0.82

Turbine

Number of stages	2
Pressure ratio	0.899
Temperature drop, T	69.6°F
Diffuser effective (calculated)	75%
Diffuser pressure loss	1.23 psi
Blading effective	92.9%
Turbine effective	
including diffuser	90.3%
Blade diameter ratio	0.80
Inlet temperature	196°R

II-C. ALTERNATE FUEL-CYCLE DEVELOPMENT

Fuel-cycle Survey

An extensive survey of the physics properties and fuel-cycle costs of possible TARGET cores initiated in the previous quarter was continued. In addition to the two fuel-element types considered previously (the Peach Bottom type with coated particles and a graphite spine and the Peach Bottom type with coated particles and a BeO spine) two more types were investigated. These differ only in the use of uncoated particles, which are assumed to release the more volatile fission products (isotopes of Se, Te, Br, I, Kr, Xe, Rb, Cs, Sr, Ba, Ag, Cd, Eu, and Sm) sufficiently rapidly that neutron absorption in these isotopes can be ignored. Although it is not clear that an element can be developed that will completely validate this assumption, the survey does provide a measure of the incentive for such a development program.

Fuel-fabrication costs for the "releasing" fuel elements were assumed arbitrarily to be the same as for the "retaining" types; namely $\$25 \times 10^6$ per core for graphite-spine elements, $\$33 \times 10^6$ for BeO-spine elements (BeO at \$10/lb, no BeO recovery), as a basis for reference. Unless these fabrication costs are reduced by more than a factor of two, the fuel-cycle cost will be lower at an exposure life of six years than at shorter lifetimes; therefore the six-year cycle was chosen as the basis for most of the calculations. A plant load factor of 82.2%, corresponding to 300 full-power days per year, and a power density of 7.0 kw/liter of core volume were assumed for all cases. Working capital was charged at 10% and based upon 50% of the core inventory; the core fabrication cost is assumed to include charges for working capital required by the fabricator. The other components of the fuel cost were computed on the basis of the USAEC Cost

Fig. 2.28--Turbocirculator arrangement with turbine at top

Handbook recommendations for the case of government-owned fuel. The effect of private ownership of fuel was approximately accounted for by increase of the use charge to 10% applied to the entire uranium inventory, including U^{233} bred in situ.

It should be noted that the results reported in the previous quarterly report were based upon lower power densities. In addition, they assumed leakages characteristic of a uniformly loaded core and assumed no end-of-cycle control-rod insertion. The present results are in these respects more realistic.

When the uranium is recycled and the power density and lifetime are fixed, the only independent variable is the thorium content. For the graphite-moderated cases, the carbon-thorium atomic ratio was varied from 170 to 250. For the cases with a BeO spine, the Be/C ratio was fixed at 0.349, and the Be/Th ratio varied from 32 to 44. Although these ranges do not include quite all the cases of interest, they are sufficient to provide for a reasonable evaluation of the characteristics of the four fuel types considered.

In Fig. 2.29 the conversion ratio is shown for these thorium loadings. The higher conversion ratios for the high thorium loadings are primarily due to the lower poisoning effect of the fission products. Since the η , or neutron yield per fuel absorption, is lower for the heavily loaded cases, a point is reached at which the conversion ratio cannot be further increased by adding fuel. The graphite-moderated, fission-product-retaining fuel, having the highest U/Th ratio of the four types considered, reaches this point at approximately C/Th = 170 with a conversion ratio of 0.78. The graphite-BeO-releasing fuel is capable of achieving conversion ratios above 1.0 at loadings slightly heavier than those considered in the present survey; the maximum conversion ratio for this fuel type may be achievable only with a different fuel design, since the volume fraction of particles in the compact exceeds 30% for Be/Th ratios less than about 28.

Figures 2.30 and 2.31 show for government-owned and privately owned fuel, respectively, the total fuel-cycle cost together with the fuel-fabrication working capital cost for the thorium loadings covered in the survey. If the minima of these curves are compared, it is found that the cost advantage for fission-product-releasing fuel types is 0.26 mills for government-owned, graphite-moderated fuel, and 0.33 mills for privately owned, graphite-moderated fuel. The graphite-BeO cases appear to have about the same differences, although the cost minima are not available. The advantage of the graphite cases as compared to the BeO-graphite cases is of the order of 0.2 mills for all cases, and is due to the cost of the BeO. Note that Figs. 2.30 and 2.31 include a working capital charge that is usually quoted separately from the fuel-cycle cost; it is included with the total fuel-cycle cost here, since it is proportional to the fuel-fabrication cost. The working capital charge for the all-graphite element is 0.173 mills and for the graphite-BeO element is 0.240 mills.

Fig. 2.29-- Conversion ratio versus thorium loading

Fig. 2.30--Total fuel-cycle cost plus interest (10%) on working capital versus thorium loading (Government-owned fuel).

Fig. 2.31--Total fuel-cycle cost plus interest (10%) on working capital versus thorium loading (privately owned fuel)

In Fig. 2.32 a combined plot of the results shown in Fig. 2.29 and Fig. 2.30 has been constructed. It appears that the conversion ratio can be increased from 0.76 to 0.99 without changing fuel cost (1.325 mills) by use of both BeO spines and uncoated particles. Figure 2.33 is a cross-plot of Figs. 2.29 and 2.31 for privately owned fuel. The cost penalty associated with increasing conversion ratio by heavier fuel loadings is obviously much greater under private ownership. The cost minima themselves occur at only slightly lower conversion ratios, however.

Figure 2.34 shows the effects of exposure life and fabrication cost on the cost and conversion ratio of two arbitrarily selected loadings, both graphite-moderated. The results for releasing fuel indicate that reducing the exposure life will always mean increased costs; however, for retaining fuel a core fabrication cost of less than $\$12.5 \times 10^6$, depending upon the loading, may admit an optimum exposure life of less than six years. Very short lifetimes will always show higher costs, if only because of the shipping, reprocessing, ex-reactor use charge, and working capital components.

The cost components for a few selected cases are shown in Table 2.8. Government-owned fuel is assumed, and the costs refer to an equilibrium cycle without present-worth (annuity-formula) corrections. The fabrication cost refers to the initial core ($\$25$ or $\$33 \times 10^6$) and uranium and thorium prices are assumed to have their present values. The lifetime is assumed to be 1800 days at full power (1050 Mw(e)).

Fission-product Release

A code has been written to process nuclear data on fission products. For each fission product for which multigroup cross-section data are available (at present 69) the code generates absorption rates in a specified spectrum. The concentration of the fission product is determined as a function of time; this depends upon the yield, the decay rate, and the release rate, as well as upon the absorption rate. The code then determines the release rate that would be required to reduce total absorptions in the fission product to a specified fraction of fuel (U and Th) absorptions, for example 10^{-4} . This provides for each fission product a criterion to aid in the evaluation of the experimentally determined release properties of the fuel materials.

Reactor Kinetics

As a preliminary to a more complete kinetics study, nuclear kinetics parameters including neutron lifetimes, delayed neutron fission yields, and their associated decay constants were calculated for representative core compositions. A case with Be/Th = 32 and with volatile fission-

Fig. 2. 32-- Total fuel-cycle cost plus interest (10%) on working capital versus conversion ratio (Government-owned fuel)

Fig. 2.33--Total fuel-cycle cost plus interest (10%) on working capital versus conversion ratio (privately owned fuel)

Fig. 2.34-- Total fuel-cycle cost plus interest on working capital as a function of core fabrication cost and exposure (Government-owned fuel)

Table 2. 8
FUEL COST COMPONENTS

	Case							
	I	II	III	IV	V	VI	VII	VIII
C/Th	250	230	230	230	---	---	---	---
Be/Th	---	---	---	---	44	32	44	32
Volatile FP release	no	no	yes	yes	no	no	yes	yes
Reprocessing, mills/kw-hr	0.046	0.046	0.043	0.047	0.054	0.070	0.053	0.063
Shipping, mills/kw-hr	0.030	0.032	0.032	0.036	0.041	0.059	0.045	0.056
Net depletion and losses, mills/kw-hr	0.379	0.364	0.183	0.160	0.281	0.190	0.087	0.053
Fabrication, mills/kw-hr	0.551	0.551	0.551	0.551	0.762	0.762	0.762	0.762
Use charge, mills/kw-hr	0.155	0.162	0.093	0.109	0.162	0.230	0.089	0.150
Total fuel cost, mills/kw-hr	1.161	1.155	0.902	0.903	1.300	1.311	1.036	1.084
Working capital, mills/kw-hr	0.173	0.173	0.173	0.173	0.240	0.240	0.240	0.240
Total, mills/kw-hr	1.334	1.328	1.075	1.075	1.540	1.551	1.276	1.324

product release was first calculated. This case is characterized by a neutron lifetime of 3.14×10^{-4} . For the lower conversion ratio case with $C/Th = 230$ and no fission-product release, the neutron lifetime was found to be 2.13×10^{-4} . The inhour equation was solved for these two cases, and the resulting variation of inverse period with reactivity is shown in Fig. 2.35

II-D. ALTERNATE MATERIALS OF CONSTRUCTION

Configuration

Prestressed concrete pressure vessels of various internal shapes have been under general consideration. The shape selected for a more detailed examination was a cylinder with its axis vertical capped by flat heads top and bottom. This configuration was selected because it provides a pressure vessel (for an arrangement with the steam generators situated around the periphery of the core) with (1) strength because of its shape, (2) minimal internal surface area to minimize insulation and cooling-system costs, (3) minimal interior free volume to minimize helium inventory, and (4) a core-to-operating floor height that would allow use of existing refueling-machine concepts. The exterior shape of this vessel will in part reflect the internal shape modified to compensate for prestressing tendon-end anchorage requirements.

Materials

A survey of the properties of the construction materials as affected by the intended usage is being carried on.

At present no information has been found that definitely indicates significant radiation damage to concrete. Most information appears to show that the temperature rise induced by radiation has the most significant effect on its properties.

The significance of neutron embrittlement of steels as it might affect the internal liner of a prestressed concrete pressure vessel is under consideration. Present information on the irradiation of prestressing tendon steels does not show significant damage; however, present design criteria require that the prestressing tendons be shielded by enough prestressed concrete that irradiation effects will be negligible when compared to the internal liner.

Insulation and Cooling System

The insulation presently under consideration is a reflective gas-space type consisting of gas spaces with thermal convection currents

Fig. 2.35--Representative core compositions

inhibited. Other means of insulating are available, but not as much information is available on their suitability in gas-cooled reactors nor on their thermal conductivity in helium atmospheres.

Work in progress consists of evaluating the probable costs of various thicknesses of reflective gas-space-type insulation, based on thermal conductivities extrapolated from available data. In conjunction with this, a study is under way to determine the heat-transfer characteristics of various methods of installing cooling pipes.

These two studies will allow the selection of a heat rejection system that, for the temperature differences desired, would result in a combined cost of the insulation and cooling system that would be a good first approximation to the minimum for the type of insulation under consideration.

Stress Analysis

The stress analysis of a reference prestressed-concrete pressure vessel was begun and is continuing using stress analysis concepts very similar to those used by British and French designers. These concepts utilize analytical methods based on thin-shell analysis modified somewhat to adapt to the fact that the proportions of the vessel actually place it in the thick-wall category. This approach is necessary primarily because of the lack of techniques for more precisely analyzing thick-walled vessels.

Comparison of a prestressed-concrete pressure vessel considered by the Grands Travaux de Marseille with the reference design being prepared at General Atomic, shows a high degree of correlativity in wall thicknesses and prestressing-tendon requirements. This is understandable since, as mentioned previously, the analytical methods are similar.

Reports of model testing done in England and France indicate that this method of analysis leads to very conservative designs. In addition, some of the reports contain information that seems to indicate that even higher factors of safety against failure could be obtained with smaller amounts of construction materials and still retain the advantages of low elastic stresses at design pressures.