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UC-80 — Reactor Technology
TID-4500 (18th ed.)

THE ORNL GCR-3, A 750-Mw(e) GAS-COOLED
CLAD-FUEL REACTOR POWER PLANT
A Joint Design Study

OAK RIDGE NATIONAL LABORATORY

operated by

UNION CARBIDE CORPORATION

for the

U.S. ATOMIC ENERGY COMMISSION

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A Joint Design Study by Oak Ridge National Laboratory
and

\ Tennessee Valley Authority
Combustion Engineering, Inc.
Westinghouse Electric Corporation

Study directed by
M. Dender and W. R. Gall

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OAK RIDGE NATIONAL LABORATORY
Oak Ridge, Tennessee
operated by
UNION CARBIDE CORPORATION
for the
U. S. ATOMIC ENERGY COMMISSION

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ABSTRACT

An advanced, gas-cooled, clad-fuel reactor power plant to generate 750 Mw of electricity has been designed as a study of the potential capability of that system.

The graphite-moderated reactor generates 1908 Mw of heat in 1062 fuel channels 21 ft long for a power density of 5.5 kw/liter. Gas temperatures entering and leaving the reactor are 574 and 1150°F, respectively, operating at 420 psia. Steam at 2415 psia and 950°F with reheat to 1000°F drives a 763-Mw(e) turbogenerator and also four 31,000-hp blower drive turbines and the boiler feed pumps. Net thermal efficiency of the plant is 39.4%. Estimated direct cost of construction is \$110,267,000, or \$147 per kilowatt net electric output. Fuel-cycle costs at 20,000 Mwd per metric ton of uranium are 1.46 mills/kwhr, operating and maintenance costs are 0.39 mill, and fixed charges range from 1.80 to 4.65 mills, depending on method of financing. Total power generation costs at an 80% load factor range from 3.65 to 6.50 mills/kwhr.

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Artist's Conception of GCR-3.

THE ORNL GCR-3, A 750-Mw(e) GAS-COOLED, CLAD-FUEL REACTOR POWER PLANT

1. SUMMARY

A study was undertaken by the Oak Ridge National Laboratory, jointly with the Tennessee Valley Authority, Combustion Engineering, Inc., and Westinghouse Electric Corporation, to evaluate the future potential of gas-cooled reactors of the Experimental Gas-Cooled Reactor¹ type (EGCR). The information obtained from the EGCR research and development program and related programs was applied to the GCR-3, a large-scale, gas-cooled, graphite-moderated reactor power plant of the clad-fuel type. A necessary part of the evaluation was a determination of whether the GCR-3 would be economically competitive with conventional coal-fired plants. It is hoped that this study will also provide a guide for the EGCR operating program that will best serve the interests of advanced designs.

Technical Feasibility

As a result of the study, the following significant conclusions were reached:

1. The technology developed during the design and construction of the EGCR supports the feasibility of building a large helium-cooled power reactor of the GCR-3 type with a net electrical output in the range of 500 to 1000 Mw and using fuel comparable to that in the EGCR to produce a gas temperature of 1200°F and steam conditions of 950°F, 2400 psi, and 1000°F reheat, leading to a net thermal efficiency of 39.4%.

2. The power density in the reactor can be increased from 2 kw/liter in the EGCR to 5.5 kw/liter in the GCR-3 design while accomplishing the above improvements in performance.

¹M. Bender, "The Experimental Gas-Cooled Reactor Design," Franklin Institute, Philadelphia, Monograph No. 7, Gas-Cooled Reactors, pp. 67-78, Franklin Institute, Lancaster, Pa., 1960.

3. Major refinements in the EGCR design concept are unnecessary, but a bolder engineering approach to the design of some components is required.

4. The major equipment required for such large plants is within the capability of United States manufacturers, although their experience is limited to types and sizes other than those required for the GCR-3.

5. If hydrogen were substituted for helium as coolant for GCR-3 but the number and size of channels, the inlet and outlet temperatures, and the pumping power remained the same, the reactor would deliver the same power output but the pressure required would be only 200 psia. This would reduce metal thicknesses required throughout the primary system by a factor of 2 and result in considerable saving of capital cost.

6. Preliminary hazards studies indicate that a reactor of the GCR-3 type can be designed and operated so that the amount of activity released following a maximum credible accident could be maintained at levels that would not cause an excessive hazard to the general public in the vicinity of the reactor. Based on the most adverse meteorological conditions, a leakage rate of less than 0.5% per day is required if the location of the reactor is such that the exclusion distance is 250 meters and the low-population-zone distance is 5000 meters.

Economic Evaluation

The cost of electrical energy from the GCR-3 design is within the 4.0- to 9.0-mill/kwhr cost range currently reported for fossil-fuel power stations in the continental United States.² Costs for conventional power stations having a single turbine-generator unit of the GCR-3 capacity are supported by very limited experience, but several 500-Mw(e) units have recently been put into operation and 900- and 1000-Mw(e) units are

²Dan Braymer, "12th Steam Station Cost Survey," Elec. World, 156(14): 69 (October 1961).

being built, respectively, by the Tennessee Valley Authority at Bull Run, Tennessee, and the Consolidated Edison Company of New York at New York City. Energy costs trend toward the lower end of the cost range in such stations but are not beyond the reach of the GCR-3 nuclear power system. The predicted energy cost is affected by the method of financing, the assumed indirect construction costs, the plant availability factor, the fuel cycle, and the operating expense. No single value of the energy cost can adequately account for all these factors. This study has therefore treated the energy costs on a number of bases.

Currently the Pacific Gas and Electric Company's Bodega Bay boiling-water nuclear power station is the most popular point of reference for nuclear systems. Table 1.1 presents the energy costs for several sizes of reactor systems of the GCR-3 type in comparison with the Bodega Bay estimates. The advantage of the higher thermal efficiency of the GCR-3 plant (39.4% vs 31%) is illustrated by the lower fuel costs for equivalent fuel lifetimes. The large size of the GCR-3 system also works to its advantage, but the effect cannot be completely meaningful until designs of larger water-cooled reactor systems of this type are developed. The fuel-cycle advantage of the GCR-3 indicates that this water system requires a lower capital investment of about \$25 per kilowatt (electrical) to achieve energy costs as low as those of the GCR-3. This, of course, does not allow for the higher efficiencies attainable from superheated boiling-water reactor systems, but the technology of such systems is still undeveloped and should be compared with more advanced gas-cooled systems in a similar developmental state.

The Atomic Energy Commission has developed a standardized cost-estimating procedure for its purpose in comparing the various nuclear power systems under study, and this procedure has been used as an alternate basis for the power cost estimate, even though it is believed to be unusually conservative for large power networks. This alternate estimate shows that the capital cost of the GCR-3 can be as low as \$181 per kilowatt (see Table 14.5) if improvements attainable by the GCR-3 design are realized. Since the fuel-cycle costs (16.9 cents per million Btu)

Table 1.1. Comparison of Power Costs for Various Sized GCR-3 Plants and Bodega Bay
Using Bodega Bay Top Charge Factors^a

	GCR-3						Bodega Bay - 313 Mw(e)
	Conservative Estimate			Optimistic Estimate			
	500 Mw(e)	750 Mw(e)	1000 Mw(e)	500 Mw(e)	750 Mw(e)	1000 Mw(e)	
Direct Cost, per kw(e)	\$173.3	\$147.3	\$131.1	\$141.7	\$119.1	\$105.8	\$153.2
Capital Cost, per kw(e)	\$211.9	\$180.1	\$160.3	\$173.3	\$145.6	\$129.4	\$187.0
Energy Cost, mills/kw(e)							
Fixed charges at 13.2% and 0.9 plant factor	3.6	3.1	2.7	3.0	2.44	2.2	3.20
Fuel cost							
at 15,000 Mwd/MT	1.60	1.60	1.60	1.60	1.60	1.60	2.00
at 20,000 Mwd/MT	1.46	1.46	1.46	1.46	1.46	1.46	b
Operating and insurance cost	<u>0.60</u>	<u>0.48</u>	<u>0.38</u>	<u>0.60</u>	<u>0.48</u>	<u>0.38</u>	<u>0.7</u>
Total energy cost							
at 15,000 Mwd/MT	5.8	5.2	4.7	5.2	4.52	4.2	5.9
at 20,000 Mwd/MT	5.7	5.0	4.6	5.1	4.38	4.1	b

^a"Summary of Estimated Capital and Annual Costs and Cost of Energy for Bodega Bay Nuclear and Conventional Fossil Fuel Plants," Atomic Energy Clearing House, 8(12): 4-8 (March 19, 1962).

^bFuel costs at 20,000 Mwd/MT are not known for the Bodega Bay reactor systems; power costs are thus not predicted for this fuel lifetime.

for the GCR-3 system are among the lowest reported for recent nuclear power systems, they make the system very competitive when compared with investor-owned fossil-fuel power systems, even using the AEC's conservative cost-estimating procedures. Based on 39.4% thermal efficiency, a capital charge rate of 14.5%, and equal operating costs, the GCR-3 at the above costs will compete with fossil-fuel power plants costing \$125 per kilowatt when fossil fuels cost 30 cents per million Btu and with plants costing \$150 per kilowatt when the fuel costs are 24 cents per million Btu.

Design Description

The Reactor

The reactor (Fig. 1.1) is designed to be helium-cooled, graphite-moderated, and fueled with UO_2 clad with stainless steel in seven-rod clusters. The reactor core consists of hexagonal blocks of moderator graphite (Fig. 1.2) of varying lengths approximating half the overall height of core and reflector and assembled in such a way that joints between upper and lower blocks are not in the same plane for any two adjacent columns. The moderator blocks are bored to an inside diameter of approximately 6 9/16 in. to receive the 6.5-in.-OD, 4.94-in.-ID fuel-element sleeves. They are keyed symmetrically to each other and to the reflector cylinder, which is constructed of large columns of graphite extending the full length of the core and reflector and clamped together by temperature-compensated bands about the circumference. The moderator blocks are about 9.4 in. across flats (Fig. 2.17), three alternate sides having keyways the whole length and the other three sides having a permanently installed key. All moderator blocks can be removed and replaced by new blocks or can be moved to other positions in the core to provide uniform effects of radiation. A discharge chute in the center of the reactor permits the fuel elements and moderator blocks to be lowered into a chamber below the reactor. The reactor vessel may be either spherical or cylindrical (a spherical one is shown in Figs. 1.1 and 1.2), but a cylindrical vessel would be 10 ft higher and 18 ft smaller in diameter than the spherical one.

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Fig. 1.1. Cutaway View of the Reactor.

Fig. 1.2. Horizontal Section of Core Arrangement.

The Coolant Loops

The general arrangement of the reactor coolant system and the secondary-containment vessels are shown in Figs. 1.3 and 1.4; design data are given in Table 1.2.

Four identical loops, each designed to remove 500 Mw of heat from the system under normal operating conditions, are used to circulate the coolant gas. Arrangements using both horizontal and vertical steam generators were studied. The layout shown in Figs. 1.3 and 1.4 is the horizontal arrangement, which consists of two horizontal containment vessels, each holding two steam generators and two gas circulators, and of a spherical containment vessel for the reactor. The three containment vessels are interconnected by pipe tunnels. The equipment is arranged to provide sufficient vertical head for circulation of gas by natural convection when the reactor is shut down.

In preparing this large-scale design, experience gained in other gas-cooled reactor designs was utilized, including the following:

Advanced Gas-Cooled Reactor (AGR):³ the British contemporary of the EGCR, using similar fuel materials but CO₂ as the coolant and having a different physical arrangement and different fuel element design.

Magnox Reactors:⁴ the British Calder Hall design and its descendants, including Berkeley, Bradwell, Hinkley Point, Hunterston, Dungeness, Trawsfynydd, and Sizewell.

Electricité de France Reactors (EDF):⁵ the French contemporaries of the British Magnox reactor designs, unique mainly in the method of loading and the utilization of concrete reactor vessels.

³"Advanced Gas-Cooled Reactor," Nucl. Eng., 6(59): 151 (April 1961).

⁴"Berkeley Nuclear Power Station," Nucl. Power, 7(73): 47 (May 1962).

⁵M. Bienvenu et al., "The Second Nuclear Power Station," Nucl. Power, 5(48): 118 (April 1960).

Fig. 1.3. Preliminary Plan of Main Gas Loops.

Fig. 1.4. Sections of Main Gas Loops.

Table 1.2 GCR-3 Design Data

General

Total thermal power	2000 Mw
Reactor thermal power	1908 Mw
Heat added by blowers	92 Mw
Total electric power generated	763 Mw
Net electric power output	750 Mw
Net thermal efficiency of plant	39.4%
Coolant	Helium
System operating pressure	415 psia
Number of loops	4
Total system pressure drop	24.3 psi
Total gas flow rate	9.1×10^6 lb/hr
Helium required to fill system	1.5×10^6 ft ³ (STP)

Reactor Core

Inlet gas temperature	577°F
Outlet gas temperature	1150°F
Maximum cladding surface temperature	1350°F
Height of core	21 ft
Channel diameter (inside fuel sleeve)	4.94 in.
Maximum power per channel	2.26 Mw
Average power per channel	1.80 Mw
Number of fuel channels	1062
Number of control rod channels	72
Total number of channels	1134
Lattice pitch (triangular)	9.4 in.
Core diameter	27 ft 9 in.
Average power density in core	5.55 kw/liter
Reflector thickness	2 ft
Outside diameter of reflector	31 ft 9 in.
Overall height of core and reflector	24 ft

Table 1.2. (continued)

Fuel Element

Fuel	UO ₂
Enrichment	3%
Cladding material	Type 304 stainless steel
Cladding thickness	0.015 in.
Outside diameter of cladding	1 in.
Number of rods per element	7
Length of element	10 ft 6 in.
Graphite sleeve dimensions	4.94 in. ID by 6.5 in. OD

Weights in Core

Moderator graphite	777,000 lb
Reflector graphite	621,000
Graphite sleeves	<u>254,000</u>
Total graphite	1,652,000 lb
UO ₂	218,000 lb
Stainless steel (approximate)	35,000 lb

Reactor Vessel (Alternates)

Material	A212B carbon steel	
	<u>Spherical</u>	<u>Cylindrical</u>
Inside diameter	52 ft 6 in.	34 ft 9 in.
Inside height	52 ft 6 in.	62 ft 6 in.
Design pressure	440 psig	440 psig
Design temperature	650°F	650°F
Nominal wall thickness	4 in.	5 3/8 in.
Top head thickness	5 1/4 in.	3 3/8 in.
Bottom head thickness	4 in.	2 3/4 in.
Weight of vessel	1,284,700 lb	1,228,000 lb

Table 1.2. (continued)

Steam Generators:^a once-through
type with reheater

Gas conditions:

Flow rate 2,275,000 lb/hr

Temperatures

Inlet 1150°F

Outlet 550°F

Pressures

Inlet 420.7 psia

Outlet 415 psia

Steam conditions:

	<u>Boiler</u>	<u>Reheater</u>
Flow rate	1.452×10^6 lb/hr	1.036×10^6 lb/hr
Temperatures		
Inlet	450°F	540°F
Outlet	950°F	1000°F
Pressures		
Inlet	3032 psia	440 psia
Outlet	2490 psia	413.2 psia
Pinch point Δt	62°F	150°F
Total heat transfer	1.443×10^9 Btu/hr	0.263×10^9 Btu/hr

Gas Circulators^a

Speed range 2000 to 3720 rpm

Type of drive Steam turbine

	<u>Design</u>	<u>Operating</u>
Suction pressure	414.7 psia	414.7 psia
Suction temperature	550°F	550°F
Flow rate	633 lb/sec	633 lb/sec
Speed	3720 rpm	3600 rpm
Pressure rise	27.5 psi	24.3 psi
Power consumption	34,300 hp	30,700 hp
Efficiency (approximate)	0.86	0.85
Discharge temperature	580.7°F	577.4°F

Table 1.2. (continued)

Primary Piping

Material	A212B carbon steel	
Type of expansion joints	Hinged bellows	
	<u>Reactor Inlet</u>	<u>Reactor Outlet</u>
Inside diameter	68 in.	74 in.
Insulation thickness	0	3 in.
Design pressure	470 psig	470 psig
Approximate length	180 ft	144 ft
Pressure drop	1.45 psi	1.10 psi
Wall thickness	15/16 in.	1.00 in.

^aData for one of four units.

Design Criteria

In the mechanical design layouts of the reactor and its cooling system the following criteria were followed:

1. Control rods are driven from the top of the vessel. Gas flowing down through the drive nozzles cools them and the drive mechanisms. The control rods are cooled by gas flowing upward through the control rod channel.

2. The fuel elements and the graphite are completely removable except for the outer reflector.

3. Removal of the fuel is accomplished with the reactor shut down and the pressure reduced to approximately atmospheric. Cooling of the reactor during this operation is accomplished by operation of one of the loops.

4. Fuel elements will be removed by a fuel-transfer machine, which is inserted through a large central nozzle at the top of the reactor vessel and is designed to reach all fuel channels in the core. The machine will transfer the fuel elements to other channels or to a discharge chute in the center of the reactor and replace them with fresh ones.

5. The reactor vessel wall is cooled on the outside by circulation of nitrogen within the containment space and is insulated internally in order to maintain the vessel wall temperature below 650°F.

6. Fuel discharged from the reactor is placed in a fuel storage pool for decay prior to removal for processing.

7. All secondary containment is inside the primary system shield and is charged with an atmosphere of nitrogen cooled by space coolers provided with water from the condenser circulating-water system.

8. All parts of the system outside the reactor vessel and its immediate shield space are serviced by direct maintenance techniques and with the reactor shut down.

Design Parameters

The reactor vessel and core were designed on the basis of refueling the reactor during shutdown. The fuel-cycle analysis indicates that reactor-shutdown refueling imposes a penalty of approximately 10% of the fuel-cycle cost for an equilibrium fuel cycle (see Chap. 3 of this report). Elimination of this penalty would be equal to elimination of a direct construction cost of \$3,700,000, so the advantage of on-stream refueling would depend upon how much less than \$3,700,000 the extra equipment would cost. Not more than \$3,700,000 of the direct construction cost can be justified on the basis of AEC cost-estimating practice to eliminate this fuel-cycle penalty, but within this cost limit some further investigation of on-stream fueling is justifiable. The GCR-3 study shows that the loss of plant availability for shutdown refueling would be about 4.8%, a value consistent with the 6.4 to 17% range observed⁶ in fossil-fuel systems of comparable capacity. Any fraction of this availability loss that could be salvaged through on-load fueling would represent a further cost saving but cannot be justified under the present rules defining the plant availability factor. Other considerations affecting selection of GCR-3 design parameters were the effect of plant thermal efficiency on fuel-cycle cost and the effect of plant size on capital cost per unit of installed capacity.

In order to achieve the maximum fuel economy, the thermal efficiency in the GCR-3 should be comparable with that of modern conventional steam power plants. The thermal efficiency reported⁶ for new steam plants ranges from 42 to 31%, with several in the vicinity of 38%. In order to achieve 38% efficiency, or more, it is necessary to use steam at high pressure and temperature with reheat and regenerative feedwater heating (typical conditions: 1000°F, 2400 psi, 1000°F reheat). A study of the relationship between temperature and

⁶Dan Braymer, "12th Steam Station Cost Survey," Elec. World, 156(14): 69 (October 1961).

percent of heat transferred (Fig. 1.5) shows that the gas temperatures at the entrance and exit of the steam generator should be at or above 1150 and 550°F, respectively. This allows a feedwater inlet temperature of only about 450°F for a steam pressure of 2400 psig, whereas most modern steam stations using this pressure and firing coal require the feedwater temperatures to be above 500°F. The temperatures and pressures shown in Table 1.3 and Fig. 1.5 were selected as a compromise between best steam cycle conditions and minimum gas pumping power.

In a recent analysis of power economics made by Westinghouse⁷ for the utility industry, it was found that the optimum size of power-generating units (i.e., single steam generator-turbine generator combination) approaches 1000 Mw(e) for large power systems or for systems with large interconnected power capability. (There are known to be two conventional units under construction at this time of rated electrical capability close to 1000 Mw.) In the same study it was found that the higher capital cost of nuclear power plants would cause their optimum size to tend to be larger than that of the conventional plant.

A further consideration in the power capacity selected was the cost advantage that would be realized by the falling ratios of engineering, general-and-administrative, and other supplementary costs of construction to total direct cost with increasing size, as indicated in Fig. A.1. The ratio of capital cost to direct construction cost for a total direct cost of \$50,000,000 is 1.621, whereas it is only 1.49 for a direct cost of \$100,000,000.⁸ Also, it was anticipated that the total cost of the plant would increase in proportion to some fractional exponent of the power output as long as all the heat could be generated in one reactor. In view of these considerations, it was decided to set the reactor thermal power at 2000 Mw and to determine the electric-power-generating capability of the system. It was estimated that 2000 Mw would result in a net electric power capability of approximately 700 Mw.

⁷As included in the statement by C. H. Weaver, Vice-President of Westinghouse, before the Joint Committee on Atomic Energy, March 21, 1962.

⁸Based on "Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025 (Vol. 1), Table 106.1, March 15, 1962.

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Fig. 1.5. Relation Between Temperature and Heat Transfer.

Table 1.3. Reactor Design Parameters

<u>Parameter</u>	<u>Range of Interest</u>	<u>GCR-3 Values</u>
Reactor thermal power	700-3000 Mw	2000 Mw
Coolant material	He, H ₂ , CO ₂	He
System pressure	300-500 psia	400 psia
Core inlet temperature	450-600°F	577°F
Core outlet temperature	1075-1225°F	1150°F
Fuel rod diameter	3/4-1 in.	1 in.
Number of rods per channel	7-19	7
Channel diameter	3-6 in.	5 in.
Lattice pitch	8-10 in. square	9.4 in. triangle
Core length	15-25 ft	21 ft
Ratio of pumping power to gross power output	0.05-0.15	0.10
Ratio of loop capacity to system capacity	1/8-1/4	1/4
Steam pressure	1400-3500 psia	2400 psia
Steam temperature	950-1050°F	950°F
Steam reheat temperature	950-1050°F	1000°F
Feedwater temperature	300-550°F	450°F

The heat-removal capability of both helium and hydrogen was investigated for inlet temperatures of 450 to 600°F and for outlet temperatures of 1075 to 1225°F. The effects of pumping power and pressure on performance were calculated for both coolants: 10% pumping power for hydrogen and 8, 10, and 12% for helium; pressures of 100 to 250 psia for hydrogen and 300, 400, and 500 psia for helium. However, the optimum values were not determined since the effect on cost was not calculated.

Ideally, an economic evaluation of the design concept would include optimization of all the design parameters for minimum power cost. A few of the more important parameters with the ranges of interest and the values selected for GCR-3 are given in Table 1.3. Determination of the effect of any one parameter on the final cost of power requires completion of a design for at least three values of that one parameter and estimation of the costs of all components affected. Furthermore, the type of equipment chosen enters into the optimization since decisions must be made on such questions as steel vs concrete pressure vessels, once-through vs forced-circulation boilers, spherical vs cylindrical pressure vessels, centrifugal vs axial-flow circulators, electrical vs steam turbine vs gas turbine circulator drives, on-load vs shutdown refueling, top vs bottom fuel and control rod access, and single vs multiple containment arrangement.

Obviously, the range of conditions is so broad that much of the evaluation must necessarily be based on an educated judgement of the design parameters most important to the economic evaluation and of the equipment selection having the greatest bearing on the ultimate power costs. The approach used consisted of examining a single large plant that appeared to be designed within the limits of current gas-cooled reactor technology while holding the performance constraints of EGCR-type fuels. On this basis the values given in Table 1.3 were selected. Important component arrangements and design features were examined in considerable detail in order to establish a sound basis for the capital-cost estimate, but the study does not claim to have established the optimum design for any feature.

A parameter study by Perry⁹ indicated that the capital cost tended to level off as a function of power density above about 10 kw/liter and that there was little gain above 5 kw/liter. It was decided therefore to design for a power density in the range between 5 and 8 kw/liter.

Results of Study

Thermal Performance (Chapter 2)

With maximum fuel-cladding surface temperature at 1350°F and a maximum internal UO₂ temperature of 3000°F, a power density of 5.5 kw/liter and an exit gas temperature of 1150°F are attained by use of roughened cladding surface and a gas pressure of 400 psia. With this reactor exit temperature and a steam generator exit gas temperature of 550°F, the selected steam power cycle has a thermal efficiency of 38.2%. When credit is taken for compressor work input of 92 Mw to the system and 13 Mw(e) is allowed for electrically driven auxiliaries, the overall net thermal efficiency for the plant is 39.4%.

Fuel Cycle (Chapter 3)

With a uranium enrichment of 3% and the shutdown refueling done in four steps during the fuel lifetime, with the used fuel moved toward the center of the core, a fuel reactivity lifetime of approximately 20,000 Mwd per metric ton of uranium can be attained. When the void volume in the fuel element is equal to the UO₂ volume and 20% of the fission gases is assumed to be released into the void, the resulting internal pressure at 20,000 Mwd/MT is 207 psia. Thus the fuel element life is not expected to be limited by internal pressure buildup of fission-product gases.

⁹A. M. Perry, "Economic Effects of Gas-Cooled Reactor Parameters," USAEC Report ORNL CF-59-12-40, Oak Ridge National Laboratory, December 9, 1959.

Reactor Vessel (Chapter 4)

In the study by Combustion Engineering, Inc., it is concluded that technology in the United States today is adequate for construction of reactor vessels of plate thicknesses up to 5 1/2 in. by final assembly at the site. The vessel for the GCR-3 is 52 1/2 ft in inside diameter, with a 4-in.-thick wall.

Refueling (Chapter 5)

Although refueling the reactor when it is shut down results in a 4.8% penalty in availability, on-load fueling is shown not to be essential to establish a competitive position for the GCR-3 concept. However, some economic incentive does exist for this method of refueling, and a further study should be made to determine the feasibility and cost of on-load refueling.

Steam Generators (Chapter 8)

In another study by Combustion Engineering, Inc., it was found practical to fabricate at the manufacture's shop a once-through type of steam generator with a reheater for a total steam-generating capacity of 500 Mw per unit. The completed unit can be shipped by water to the site, and a means has been devised for unloading at the site and moving it to its foundation. However, the assembled unit weighs 565.5 tons, about equally divided between tubes and shell, and will be difficult and expensive to handle at the site. Some savings may be achieved by shipping tube bundles separately and installing them in the shell at the site.

Gas Circulators (Chapter 9)

Axial-flow gas circulators of three sizes were designed by Westinghouse Electric Corporation for GCR-3 conditions. The three sizes are based on two-, four-, and eight-loop arrangements for the same total power removal. All three sizes are within present

manufacturing capability; the most economical size for the GCR-3 is the four-loop size. Aerodynamic designs are well established. Oil-buffered seals are proposed, and have the advantage of some experimental data having been obtained on them.

Containment (Chapter 13)

The reactor-containment vessel is proposed to be of steel-lined reinforced concrete, which will also serve as the biological shield around the reactor. Designed for an internal pressure of 60 psig in the event of an accident, the combined containment and shield will cost \$2,217,000, or \$2.91 per kilowatt of net electricity.

Cost (Chapter 14)

A conservative estimate of the direct cost of construction of the GCR-3 plant, based on experience in construction of the EGCR and including allowances for many uncertainties due to extrapolation of the design, is \$110,267,000, or \$147 per net kilowatt of electricity. The ultimate potential of the system, however, should be based on a more optimistic estimate in which it is assumed that all uncertainties are removed by experience and firm cost data are established. On the latter basis it is estimated that direct construction cost could be as low as \$89,316,000, or \$119 per kilowatt. Indirect costs vary from 22.3% used in the Bodega Bay estimate,¹⁰ to 36%¹¹ for the Rural Electrification Administration, to 52%¹¹ for investor-owned utilities. The lowest indirect cost, 22.3%, yields a capital cost of \$146 per kilowatt, and the highest cost, 52%, yields a capital cost of \$181 per kilowatt. The capital cost of \$181 per kilowatt results in a capital charge of 3.7 mills/kwhr (Table 14.5), and total fixed charges of 3.87 mills/kwhr.

¹⁰"Summary of Estimated Capital and Annual Costs and Cost of Energy for Bodega Bay Nuclear and Conventional Fossil Fuel Plants," Atomic Energy Clearing House 8(12): 4-8 (March 19, 1962).

¹¹"Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025(Vol. 1), Table 106.1, March 15, 1962.

The annual operating cost is estimated to be 0.389 mill/kwhr. Fuel-cycle costs can vary from 2.06 mills/kwhr for a fuel lifetime of 10,000 Mwd/MT to 1.45 mills/kwhr for 25,000 Mwd/MT; a value of 1.46 mills/kwhr for 20,000 Mwd/MT is used as the reference basis for computing GCR-3 power costs.

The total power cost determined from the above estimates was 5.72 mills/kwhr and reflects a fixed cost of 3.87 mills/kwhr, an operating and maintenance cost of 0.39 mill/kwhr, and a fuel cost of 1.46 mills/kwhr.

A study of the effects of size on all parts of the detailed cost estimate for GCR-3 indicates a reduction of approximately 12.5% in the capital cost per unit of power output when the plant capacity is raised to 1000 Mw(e) and an increase of 22% when the capacity is decreased to 500 Mw(e) (see Fig. 1.6 and Table 14.5). This relationship appears to hold regardless of whether the conservative or the optimistic estimate is used in the valuation. If the assigned top charges are those used for Bodega Bay, then a slight difference in the percent change in capital cost as a function of capacity is noted [11% decrease at 1000 Mw(e) and 18% increase at 500 Mw(e)].

Safety Considerations (Chapter 15)

The study has established that safety of the public can be assured by the provision of pressure- and leak-tight containment around the primary coolant system, filling that containment with nitrogen, and providing a completely self-sufficient system for shutdown cooling. Provisions to further guarantee the integrity of the reactor, fuel elements, steam generators, etc., are therefore based on economic and operating considerations.

Fig. 1.6. Effect of Capacity on Capital Investment.

2. CORE DESIGN

Fuel Element

The GCR-3 fuel assembly (shown in Fig. 2.1) consists of a seven-element cluster of UO_2 pellets clad with 0.015-in.-thick type 304 stainless steel. The cluster is supported within a graphite sleeve that has an outside diameter of 6.5 in., an inside diameter of 4.94 in., and an overall length of 10 1/2 ft. The top spider is attached to the sleeve with pins so that the fuel can be charged and discharged by a grapple arrangement which lifts the assembly by the top spider. In addition to the cluster being spaced and centered within the sleeve by the top and bottom spiders, it is centered by separate spacers provided at nine axial locations along the elements, which prevent excessive bowing of the elements. Low-height circumferential ribs on the surface of the fuel elements will serve as turbulence promoters. The size and shape of the ribs will depend on the results of an experimental program now in progress.

The UO_2 pellets are of 0.97-in. outside diameter, 0.74-in. inside diameter, and 1-in. length. Because of the length of the elements, four internal segmenters will be attached to the element walls to divide the stacked UO_2 pellets into five equal lengths and to reduce the problem of axial differential expansion of the pellets and the cladding. Since the inside-to-outside diameter ratio of the pellets is much larger than the ratios tested in the EGCR irradiation program, the pellets may require an internal support to prevent chips of the UO_2 from falling into the center of the pellets. The present design utilizes a thin ceramic tube to hold the UO_2 in place.

Fuel Element and Core Thermal Analysis

The effects of inlet core gas temperature, outlet core gas temperature, pumping power, channel size, and system pressure on the core thermal performance were determined. Although the experimental work on the circumferential ribs is incomplete, information available

Fig. 2.1. Conceptual Drawing of GCR-3 Fuel Element.

in the literature¹ is sufficient to indicate the effectiveness of such surfaces. The results of the study were based on the assumption that the value of the Stanton number can be increased by a factor of 2 with a resulting increase of 3 in the friction factor of the roughened surfaces. Other assumptions used in the calculations were the following:

Gross thermal efficiency	40%
Core height	21 ft
Blower efficiency	87%
Core pressure loss	2/3 of system loss

The full 21-ft-core length was used in calculating the frictional losses. The effective fuel element length for heat-transfer surface area was assumed to be only 90% of the core length, or 18.9 ft. Ten percent of the core pressure loss was assumed to be at the entrance and exit of the core and in the top and bottom reflectors. The loss at the spacers and between assemblies was assumed to be 0.3 of the velocity head (the gas density was taken at the average temperature in the core), and the entrance loss to the bottom fuel assembly and the exit loss from the top assembly to be the same as for the EGCR, 0.183 and 0.32 of the velocity head (the gas density was based on the actual inlet and exit temperatures).

The total core head losses can be expressed as

$$\text{system } \Delta H = 778 \times C_p \times \Delta T \times Z \times E_{th} \times E_B,$$

where

C_p = specific heat of gas,

ΔT = core gas temperature rise,

Z = percent of plant output used by blowers,

E_{th} = plant thermal efficiency,

E_B = blower efficiency.

¹V. Walker, "The Improvement of Fuel Element Heat Transfer by Surface Roughening," Nucl. Eng. 6(59): 144 (April 1961).

In the parametric analysis the temperature rise across the blower was neglected. Therefore the pressure rise across the blowers can be written as $\Delta P = \rho_i \Delta H$, and the core pressure loss due to the fuel elements as

$$\Delta P = 0.9 \times 2/3 \times \rho_i \times 778 \times C_p \times \Delta T \times Z \times E_{th} \times E_B . \quad (1)$$

The core pressure loss is also equal to the sum of the frictional expansion and contraction losses:

$$\Delta P = 4f \frac{\rho_a V_a^2}{2g} (L/d_e) + 19 \times 0.3 \rho_a \frac{V_a^2}{2g} + 0.183 \rho_i \frac{V_i^2}{2g} + 0.32 \frac{\rho_o V_o^2}{2g} , \quad (2)$$

where

- f = friction factor,
- ρ_a = density at average temperature,
- ρ_i = density at inlet temperature,
- ρ_o = density at outlet temperature,
- L = channel length,
- d_e = channel equivalent diameter,
- V_a = average velocity,
- V_i = inlet velocity,
- V_o = outlet velocity.

Equating Eqs. (1) and (2), the expression for the channel inlet velocity becomes

$$V_i^2 = \frac{0.9 \times 2g \times T_i \times 2/3 \times 778 \times C_p \times \Delta T \times E_{th} \times E_B}{[4f (L/d_e) + 5.7] T_a + 0.183 T_i + 0.32 T_o} ,$$

where T_a , T_i , and T_o represent, respectively, the average, inlet, and outlet core gas temperatures.

The total channel gas weight flow is the product of the inlet density, velocity, and channel flow area:

$$W = \rho_i V_i A ,$$

and the total heat input to the gas in the channel is

$$\begin{aligned} Q &= W C_p (T_o - T_i) \\ &= \rho_i V_i A C_p (T_o - T_i) . \end{aligned} \quad (3)$$

In reactors of the GCR-3 type about 5% of the total fission energy is deposited in the graphite core structure outside the fuel elements. Since this heat will be removed by a separate parallel gas flow through the core, the channel output is increased by 5% without increasing the core pressure drop and without adding to the heat flux from the fuel elements. Thus the actual channel output is 5% greater than that given by Eq. (3).

The heat-transfer coefficient was determined from

$$h = 2 \times \frac{0.023GC_p}{(N_{Re})^{0.2} \times (N_{Pr})^{0.67}}, \quad (4)$$

where

C = mass velocity,

C_p = specific heat of gas,

N_{Re} = Reynolds number,

N_{Pr} = Prandtl number.

The factor of 2 in the equation is the assumed increase for the surface roughening.

The axial power in the reactor was assumed to be that shown in Fig. 2.2.

For the case in which helium was the coolant, the variables studied and their ranges are given below:

System pressure	300, 400, 500 psia
Core inlet gas temperature	450, 500, 550, 600°F
Core outlet gas temperature	1075, 1150, 1225°F
Pumping power as percentage of gross plant output	8, 10, 12%
Channel diameters	4.5 to 5.0 in.

The results of the study with helium used as the coolant are shown in Figs. 2.3 through 2.7 for various pressures and pumping powers. The plane shown cutting through the planes of channel size represents the condition in which the maximum surface temperature of the fuel elements in the maximum power channel is 1350°F. The 1350°F was determined by using heat-transfer coefficients based on bulk gas temperature properties and not on the surface temperature. Also shown on each

Fig. 2.2. Axial Power Distribution.

Fig. 2.3. Maximum Fuel Channel Thermal Output with Helium at 400 psia and 8% Pumping Power.

Fig. 2.4. Maximum Fuel Channel Thermal Output with Helium at 400 psia and 10% Pumping Power.

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Fig. 2.5. Maximum Fuel Channel Thermal Output with Helium at 400 psia and 12% Pumping Power.

Fig. 2.6. Maximum Fuel Channel Thermal Output with Helium at 300 psia and 10% Pumping Power.

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Fig. 2.7. Maximum Fuel Channel Thermal Output with Helium at 500 psia and 10% Pumping Power.

figure is a limiting q/L value, which represents an average heat rate of 66,000 Btu/hr per foot length of individual element, or a maximum of 99,000 Btu hr⁻¹ ft⁻¹. This linear heat rate will limit the central UO₂ temperatures to 3000 to 3500°F. From past experience it has been found that the heat-transfer coefficient should be based on the gas properties evaluated at the surface temperature. This factor will add about 100°F. No allowance has been made for hot-channel or hot-spot effects nor for any temperature asymmetries within the elements. Detailed analyses including all these additional factors are not warranted until more experimental work is done.

Based on the results of the EGCR hot-channel and temperature asymmetry analyses, some local surface temperatures would be expected in the range 1550 to 1600°F, with a very small probability of temperatures above 1650°F. The maximum allowable temperature for the stainless steel cladding is 1800°F.

Calculations were made for hydrogen as the coolant in order to investigate the possible reduction in system pressure for core power densities approximately the same as those found for helium. These calculations were limited to the case in which the pumping power was equal to 10% of the gross plant output. The range of values used for inlet and exit core coolant temperature and channel sizes was the same as for helium. The system pressure was varied from 100 to 250 psia. The results for hydrogen are shown in Figs. 2.8 through 2.11. Since the fuel element length and heat-transfer surface area were the same for all cases, both the linear heat rating and the heat flux are directly proportional to the channel power output. For the assumed element dimensions each megawatt of heat generated in a channel requires a linear heat rate of 36,800 Btu hr⁻¹ ft⁻¹ and a heat flux of 140,000 Btu hr⁻¹ ft⁻².

In analyzing the internal fuel temperatures and fission-gas pressure buildup, the volume of UO₂ per length of element was assumed to be the same as for the EGCR and the cladding thickness was taken as 0.015 in. The outside and inside diameters of the UO₂ fuel body were approximately 0.97 and 0.74 in., respectively. The contact resistance between the

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Fig. 2.8. Maximum Fuel Channel Thermal Output with Hydrogen at 100 psia and 10% Pumping Power.

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Fig. 2.9. Maximum Fuel Channel Thermal Output with Hydrogen at 150 psia and 10% Pumping Power.

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Fig. 2.10. Maximum Fuel Channel Thermal Output with Hydrogen at 200 psia and 10% Pumping Power.

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Fig. 2.11. Maximum Fuel Channel Thermal Output with Hydrogen at 250 psia and 10% Pumping Power.

UO₂ and the cladding was assumed to be the equivalent of a 0.001-in. gas gap, and the thermal conductivity of the UO₂ was assumed to be 1.5 Btu hr⁻¹ ft⁻¹ (°F)⁻¹. The internal temperatures for a heat rating of 99,000 Btu/hr per foot length and for two gas compositions within the elements (i.e., with pure helium, A, and with a gas mixture having a thermal conductivity one-fourth that of pure helium, B) are as follows:

	A	B
Thermal conductivity of gas in the fuel element, Btu hr ⁻¹ ft ⁻¹ (°F) ⁻¹	0.218	0.059
Temperature difference across cladding, °F	40	40
Temperature difference across gas gap, °F	150	560
Temperature drop through UO ₂ pellet, °F	1300	1300
Cladding temperature, °F	1350	1350
Maximum nominal internal temperature, °F	2840	3250

A number of approximations were made in calculating the above temperatures. Actually, the maximum fuel element temperature and maximum heat generation rate in a channel do not occur at the same location. Also, the heat transferred by radiation between the UO₂ and the cladding was neglected. These two effects will offset the error made in using the nominal surface temperature of 1350°F. It is also interesting to note that if the UO₂ were a solid cylinder without the central hole the temperature drop through the pellet would have been 5200°F.

The void volume in the center of the UO₂ pellets is about 1.35 times the UO₂ volume. Because of the rather thin UO₂ section, some central ceramic filler may be required to hold the UO₂ in place. Taking the volume of the nonfueled ceramic filler to be 35% of the UO₂ volume, the void volume for storage of fission gases is then equal to the UO₂ volume. Table 2.1 gives the internal fuel element pressure as a function of fuel burnup and the percentage fission-gas release for a central UO₂ temperature of 3200°F and an initial fill pressure of

14.7 psia. Considering that the maximum release of fission gases from the EGCR fuel elements is expected to be about 10%, the use of the large central hole should prevent the internal pressure from being a limiting factor in the fuel element life.

Table 2.1. Internal Fuel Element Pressure
Relative to Burnup and Release of Fission Gases

Fission-Gas Release (%)	Internal Pressure (psia) at Burnup (Mwd/MT) of			
	10,000	15,000	20,000	25,000
20	155	182	207	234
40	207	259	311	362
60	259	337	414	492
80	311	414	517	621
100	363	492	621	751

The core density that can be achieved will depend on both the radial peak-to-average power ratio and the lattice pitch. The average channel heat output is equal to the output of the maximum channel divided by the radial peak-to-average ratio. The average core power density as a function of the radial peak-to-average power ratio and triangular lattice pitch for each megawatt of output from the maximum channel is given below:

Lattice Pitch (in.)	Power Density (w/cm ³) for Peak-to-Average Power Ratio of		
	1.2	1.3	1.4
9.0	2.88	2.66	2.47
9.5	2.58	2.38	2.22
10.0	2.33	2.15	2.00

Sizing the Core

From the results of the thermal analysis it is possible to select a core size for any power level desired and for any choice of inlet and outlet temperatures within the range of temperatures given in Figs. 2.3

through 2.11. Based on an assumed radial maximum-to-average power ratio of 1.2, curves were plotted (Fig. 2.12) for the number of channels required to generate 1000 Mw of heat and for the channel diameters as functions of inlet and outlet temperatures for helium- and hydrogen-cooled reactors at the pressures and pumping power ratios given in the curves of maximum channel output. No optimization has been attempted. Figure 2.12 presents this information for helium at 400 psia with the pumping power 10% of the gross plant output. For a reactor inlet temperature of 577°F and an outlet temperature of 1150°F, the number of channels required for 1000-Mw thermal output is 531 and the diameter is 4.94 in. Since the power to be provided by the reactor is only 1908 Mw, 1062 channels provide a margin of capacity that makes it unnecessary for the maximum power channel to be pushed to the level calculated.

The core diameter as a function of the number of fuel channels is plotted in Fig. 2.13; allowance is made for one of every 16 lattice positions to be occupied by a control rod. For 1062 fuel channels at a 9.4-in. lattice pitch, the core diameter is 27 ft 9 in.

The effects of system pressure and of percent pumping power on reactor size are shown in Figs. 2.14 and 2.15. The size of the core decreases with increase of either pressure or pumping power or both. It remains to be determined at what point the increased cost balances the economies gained through decreasing the size by these means. A power density of 6.5 kw/liter is achieved at 500 psia with 10% pumping power.

Sizing the Reactor Vessel

Space is required above the top surface of the reflector to permit withdrawal of fuel elements and moderator blocks. Study layouts indicate that this space could be obtained by leaving a clearance of about 17 ft between the top of the reflector at the outermost channel and the inner surface of the vessel insulation directly above. With space allowed below the reflector for the support grid, an inside diameter of 52.5 ft was determined for the GCR-3 vessel (as shown in Fig. 2.28). This creates a large surplus volume around the sides of the reflector, which can be eliminated through use of a cylindrical vessel. The

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Fig. 2.12. Required Number and Diameter of Fuel Channels for a 1000-Mw Helium-Cooled Reactor at 400 psia and 10% Pumping Power. Surface temperature, 1350°F.

Fig. 2.13. Core Diameter as a Function of Number of Fuel Channels for Cylindrical Core with Triangular Lattice.

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Fig. 2.14. Effect of System Pressure on Reactor Size and Core Power Density.

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Fig. 2.15. Effect of Percent Pumping Power on Reactor Size and Core Power Density.

cylindrical vessel, however, because of the smaller radius of its heads must be higher to provide the required 17-ft clearance at the edge of the core. Therefore the inside dimensions of the cylindrical vessel were established as 34 ft 9 in. in diameter and 62 ft 6 in. in height.

Helium vs Hydrogen as Coolant

Figure 2.16 is a plot of system pressure vs power density for helium and hydrogen derived from Figs. 2.4 and 2.6 through 2.11. The ratio of pumping power to total power is the same for all points on both curves, 0.10. The ratio of carbon to uranium is kept the same for all points, the lattice pitch being adjusted to compensate for changes in channel diameter.

Since the pressure required in the hydrogen system to achieve the GCR-3 power density of 5.5 kw/liter is only 164 psia (400 psia required for helium), two advantages over the GCR-3 would appear possible in a hydrogen-cooled reactor at the same power density: (1) The increased core diameter made possible by the reactor vessel being twice the diameter of the GCR-3 vessel would result in a thermal capability of 13,000 Mw, which, at 40% thermal efficiency, could be converted to 5200 Mw of electric power; (2) the reduction in wall thickness would reduce the cost of all primary equipment, representing a considerable savings in material and welding costs over those for a helium system.

The application of hydrogen as a reactor coolant has not been investigated further than this point in the GCR-3 study. Other avenues for capitalizing on the improved heat-transfer properties of hydrogen are (1) increasing the gas outlet temperature for the reactor without changing the cladding hot-spot temperature established for helium at 1150°F bulk helium outlet temperatures and (2) decreasing the pressure loss and thus the pumping power in the core by reducing gas velocities at GCR-3 pressure and temperature conditions until the fuel-cladding hot spots in the hydrogen-cooled system approach those of the higher-pressure-loss helium system. These approaches also offer opportunity to reduce capital equipment cost while improving thermal efficiency.

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Fig. 2.16. System Pressure vs Core Power Density for Helium and Hydrogen.

Since the available information on compatibility between reactor construction materials and hydrogen is limited, it is difficult to judge whether the advantages of this coolant can be effectively utilized, and it was mainly this consideration that prevented further exploration of hydrogen-cooled reactor systems in the GCR-3 design study. It is worth pointing out that the critical safety question related to hydrogen explosions appears to be effectively answered by the inert atmosphere surrounding the primary reactor coolant system in the GCR-3 design. The appraisal of hydrogen can therefore be concentrated on its performance capability without reference to safety.

Description of the Graphite Core

The major structural requirements of the core stem from the supposition that reactor graphite will have a shorter design life than other major portions of the plant because of fast-neutron damage and the resulting induced stresses and dimensional changes. It thus becomes necessary to remotely remove and replace the damaged graphite at regular intervals and yet design the core for maximum structural stability with the least possible number of different shapes and sizes arranged so that a minimum amount of time will be required for graphite handling.

The proposed site location is in a zone 1 earthquake-probability region, for which the core should be designed to withstand 0.083-g horizontal acceleration simultaneously with 0.041-g vertical acceleration and dominant periods of from 0.1 to 0.3 sec (ref 2). To best resist earthquakes the core should behave dynamically as a single mass unit.

The core is a circular cylinder of graphite (see Fig. 1.1) 24 ft tall and 31 ft 9 in. in diameter. The outer 2 ft (approximate) around the side and the 1 1/2 ft at the top and bottom end comprise the reflector region. The inner region or moderator graphite houses the fuel elements and control rods in a regular 9.4-in. triangular pitch pattern. The fuel elements require a vertical 5-in.-ID channel.

²S. E. Moore, "Seismic Considerations in Nuclear Power Plants," Nucl. Safety 3(4): 58-63 (June 1962).

The moderator is composed of hexagonal cross-section columns, with a single fuel channel or control rod channel through the center of each column. In order that the assembled core will resemble a monolithic structure with respect to dynamic forces, gas forces, and temperature cycles, the hexagonal moderator cylinders are keyed and slotted on alternate faces (see Fig. 2.17). This keying arrangement allows any number of graphite pieces to be removed without the remaining pieces falling out of position.

Refueling is to be done during shutdown. At the same time, graphite that has been damaged beyond usefulness from fast-neutron exposure, etc., will be replaced. In order to minimize the shutdown time for refueling and graphite replacement, the components should be as large as possible. However, it is desirable to switch fuel from the top half of the reactor to the bottom half and vice-versa for operational reasons. Therefore the fuel elements are each one-half the total length. During refueling it is necessary that there be enough room inside the reactor pressure vessel for a fuel element to be lifted above the top of the core. In order for the size of the pressure vessel not to be increased above that required for fuel handling, the moderator blocks are also limited to about half the length of the core.

The moderator cylinders are made in three different lengths: 11, 12, and 13 ft. Two of them are stacked on end in each channel position as shown in Fig. 2.18, that is, an 11-ft length and a 13-ft length, a 13-ft length and an 11-ft length, and two 12-ft lengths. These three sets of blocks are arranged on a triangle, with the result that the dividing line between the two lengths never occurs at the same axial position for adjacent channels. The lower fuel element in each fuel channel rests on an 18-in.-long piece of graphite that is 4.25 in. in inside diameter and 6.5 in. in outside diameter. This piece of graphite is slipped into the hexagonal cylinder and rests on the bottom support plate.

The side reflector is composed of 72 full-length columns and one row of solid hexagonal cylinders similar to the moderator cylinders. The reflector columns are keyed to the hexagonal pieces in the same way that the hexagonal pieces are keyed together. Four-inch-square keys that fit

Fig. 2.17. Core Lattice Arrangement.

into keyways on the radial faces of the reflector columns act as gas leakage barriers and add additional stability. The keys and keyways run the full length of the columns.

Temperature-compensated bands that are supported by the cylindrical gas seal surround the entire core assembly. These bands will transmit horizontal forces between the core and the pressure vessel without restricting the radial temperature growth of the graphite stack or the gas seal.

Fast-Neutron Damage

The fast-neutron exposure distribution in the core will produce both distortion and internal stresses in the graphite. In a heterogeneous core such as this the fast-neutron flux distribution may be thought of as having two components that must be considered: (1) the local distribution through each member, which can cause nonuniform internal strain, and (2) the overall distribution, which can cause gross distortion in graphite.

The local fast-neutron flux distribution in the graphite for an average power channel (1.8 Mw) is shown in Fig. 3.9. Calculations of the Wigner stresses were done by using an anisotropic, elastic, circular cylinder model in a condition of plane strain. The axial and transverse damage growth coefficients that were used were

$$\gamma_{\text{axial}} = -2.5 \times 10^{-7} \text{ in. in.}^{-1} \text{ Mw}^{-1} \text{ per adjacent ton of U,}$$

$$\gamma_{\text{transverse}} = -1.2 \times 10^{-7} \text{ in. in.}^{-1} \text{ Mw}^{-1} \text{ per adjacent ton of U.}$$

These values were assumed to be independent of exposure and are based on preliminary evaluations of Hanford data for EGCR needle-type graphite.³ As more data are accumulated for higher exposures, these coefficients may change. Recent data indicate that it may be necessary to consider changes in coefficients as a function of exposure when more refined analyses are warranted.

³Letter from J. M. Davidson, Hanford Laboratories Operation, Richland, Washington, to W. J. Larkin, Oak Ridge Operations Office, USAEC, Oak Ridge, Tennessee, July 18, 1961, Subject: Preliminary Results of Second Irradiation of EGCR Graphite.

The use of a graphite sleeve as a part of the fuel element reduces the maximum stresses induced in the hexagonal cylinders, with the amount of reduction depending upon the fuel sleeve thickness. Therefore a study was made to determine the sleeve thickness associated with making maximum use of the graphite in terms of the ratio of induced stress to fracture stress while limiting the maximum stress in the hexagonal cylinder to a value less than that in the sleeve. Figure 2.19 shows the maximum stresses in the axial and tangential directions in the same average-power channel for both the sleeve and the cylinder as a function of sleeve thickness after one year of full-power operation.

An upper limit to the sleeve thickness is determined by the total exposure of the graphite when the fuel is depleted, at which time the Wigner stresses can be no higher than the fracture stress. The total exposure of the sleeve depends on the fuel program, which has not yet been completely worked out.

On the other hand, the sleeve must be thick enough so that the column will suffer less damage than the sleeve. This places a lower limit on the sleeve thickness at 0.6 in. The graphite section left in the moderator cylinders must also be thick enough for practical remote handling. The sleeve thickness has been tentatively set at 0.75 in. Figure 2.20 shows the stress distribution in the 0.75-in. sleeve and graphite column.

In addition to the development of internal stresses, the columns will bow because of the overall radial flux gradient in the reactor. The region of maximum bowing occurs just inside the reflector, where the flux gradient is steepest, but the outer portion of the reflector will not bow at all and will therefore be permanently installed monolithic columns. Unfueled solid hexagonal cylinders similar to the moderator cylinders are used between the fueled moderator and the outer reflector in the region of the steepest gradient and should suffer the greatest bowing damage. They can be replaced, or if economics and operating procedure allow, can be switched diametrically across the reactor to reverse the bowing tendency. The overall flux distribution will determine the frequency of shifting and of replacement required to control the amount of bowing to acceptable limits.

Fig. 2.19. Maximum Wigner Stresses in the Sleeve and Moderator Cylinder as a Function of Sleeve Thickness for Average-Power Channel After One Year of Full-Power Operation.

Fig. 2.20. Stress Distribution in the Sleeve and Moderator Cylinder for Average-Power Channel for One Year of Full-Power Operation.

Dimensional Requirements

The machining costs of the components will be the major portion of the graphite cost. It is therefore important that tolerance requirements be as liberal as possible. The operating requirements on the assembled core are (1) that it be assembled and dismantled remotely, (2) that specific pieces of the moderator be removed and replaced easily during shutdown, and (3) that the core be dynamically stable at operating temperature, that is, that the core configuration be maintained in a safely operable condition despite seismic disturbances.

The cross-sectional dimensions of the hexagonal cylinders and reflector columns, the tolerances on these dimensions, and the clearances around the keys in the keyways are the factors that determine the maximum movement of a graphite piece and the maximum possible gap width between two adjacent pieces. In order to determine the maximum displacements and gap widths that may exist, a preliminary study was made based on the assumptions that (1) only horizontal translations may occur, (2) each block is identical, having the smallest cross section admissible by the tolerances, (3) no irradiation-induced distortion is present, (4) the reflector columns are fixed in place, and (5) the entire stack is at room temperature.

The cylinder dimensions used in the study were the following:

Width across the flats	9.364 in.
Average gap between cylinders	0.036 in.
Width of keys	0.968 in.
Width of keyways	1.000 in.
Height of keys	0.375 in.

Four cases were studied. The critical location in the core is shown in Figs. 2.21 through 2.24, and the condition for each case and the results of the studies are listed in Table 2.2.

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Fig. 2.21. Graphite Tolerance Study, Case 1.

Fig. 2.22. Graphite Tolerance Study, Case 2.

Fig. 2.23. Graphite Tolerance Study, Case 3

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Fig. 2.24. Graphite Tolerance Study, Case 4.

Table 2.2. Conditions for Four Cases Studied and Resultant Maximum Displacement and Gap Width

	Maximum Displacement (in.)	Maximum Gap Width (in.)
Case 1: cylinders in each 60° sector move radially away from the center (see Fig. 2.21)	0.162	0.082
Case 2: cylinders move along the 0° coordinate with the center block fixed and the cylinders keyed to the reflector (see Fig. 2.22)	0.256	0.072
Case 3: cylinders move along the 0° coordinate with blocks keyed to reflector but center block free (see Fig. 2.23)	0.342	0.072
Case 4: cylinders move along the 0° coordinate with blocks <u>not</u> keyed to reflector but center block free (see Fig. 2.24)	0.378	0.360

As a result of these studies two important conclusions were reached:

1. The cylinders must be keyed to the reflector. Case 4 shows that if the cylinders are not keyed the possible gap width is 0.360 in. whereas the height of the key is only 0.375 in. In this case positive contact is not assured if a cylinder is removed, or when the reactor is at operating temperature, because of the relative expansion between the support plate and the graphite.

2. In every one of the four cases the position of the cylinder is determined by the width of the gap between the keys and the keyways. Any forces that would tend to shake the core would be resisted entirely by the keys in shear. Consequently, the width of the keys was increased to $1.438^{+0.000}_{-0.006}$ in. and the width of the keyways was changed to

1.5 $\begin{smallmatrix} +0.000 \\ -0.006 \end{smallmatrix}$ in. so that horizontal loads would be carried on the faces of the hexagonal pieces.

Core Restraint System

Since the core is a composite structure made up of small pieces of graphite, it must be held together in some manner. The design criteria for the restraint system are the following:

1. An unincumbered top core surface must be provided. This is the most restrictive functional requirement. Handling fuel and graphite remotely requires that any given piece of graphite or fuel element be approached from the top of the reactor. If handling time is to be minimized, remote operations must not include unlatching, removing, or bypassing structural components.

2. A stable core structure must be provided by transmitting forces between the graphite stack and the pressure vessel at reactor operating conditions. Distortion, shifting, or breaking of graphite pieces should be prevented. The core must always be able to receive control rods for shutdown.

3. Replacement of moderator graphite must be provided for, which requires that the restraint structure be designed to allow adequate working space at shutdown temperature for replacement operations and to avoid binding the components together so that they cannot be removed singly.

4. The system must be easily assembled and adjusted in the field and must be dependable and maintenance-free.

Of the several possibilities considered, temperature-compensated bands around the perimeter of the reflector best fulfill the above requirements. A band is made of two or more materials that have different thermal coefficients of expansion and are arranged so that the expansion of the composite structure is governed by the difference in the thermal expansion coefficients. Several different low-expansion materials might be used, including Invar, Ni-span-C, and pure molybdenum. For this study Invar and type 304 stainless steel were selected as representative materials. The thermal expansion curves for these materials are shown in Fig. 2.25.

Fig. 2.25. Thermal Expansion Coefficients of Invar, Type 304 Stainless Steel, and EGCR-Type Graphite. Type 304 stainless steel coefficients from 70 $^{\circ}\text{F}$ to indicated temperature from Timken Digest of Steels, USS Steels for Elevated-Temperature Service; Invar coefficients from 32 $^{\circ}\text{F}$ to indicated temperature from Mechanical Properties of Metals and Alloys, Dept. of Commerce Circular C447; EGCR graphite curves calculated from room-temperature data.

A conceptual design is shown in Fig. 2.26 for which the bimetallic portion is 80% of the overall length. The diametral expansion of the band and the graphite as a function of the temperature is shown in Fig. 2.27. Since the diameter of the band decreases as the temperature increases, in the range up to 600°F, whereas the diameter of the core increases, initial adjustments must be made at room temperature to satisfy criteria 2 and 3 above. Springs are provided to compensate for the differences in diameter changes between the restraint band and the stack at the other temperatures.

Title 1 design work should include a structural analysis of the bands, including seismic forces. Laboratory tests to determine materials compatibility will probably be necessary before the design of the band can be specified. Complete temperature-expansion curves and creep curves will also be needed.

Core Support Structure

The graphite core rests on a carbon steel segmented flat plate 2 in. thick supported on a grid structure (see Fig. 2.28). The grid structure is a triangular network of carbon steel beams 36 in. deep by 1 1/2 in. wide. A short cylindrical skirt 24 in. high, 1 1/2 in. thick, and 340 in. in diameter transmits the load to the pressure vessel and supporting structure. The total weight of the core graphite, fuel elements, and structural steel supported by the grid structure is approximately 2,000,000 lb.

The deflection of the grid structure at the reactor center line was limited to 1/4 in. A simplified method based on previous analyses of square grid structures⁴ was used to calculate the stresses and deflection in the grid structure. The analysis was adequate to determine the number and size of the beams required. The maximum stress in the grid structure is approximately 13,000 psi, which is below the allowable stress of 17,500 psi for ASTM-A212, grade B, carbon steel at 600°F. The allowable stress governing the design of

⁴Oak Ridge National Laboratory, "GCR Semiann. Prog. Rep. Dec. 31, 1958," USAEC Report ORNL-2676, pp. 15-17.

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Fig. 2.26. Conceptual Design of Bimetallic Temperature-Compensating Band.

Fig. 2.27. Diametral Expansion of Band and of Graphite as a Function of Temperature.

the grid structure may, however, be the critical stress for crack propagation after long exposure to fast neutrons. When the exposure of the steel in the support plate and grid structure has been calculated, the stress values should be compared with the critical values.

Control Rods

The final design of the control rods will depend on the design of the shock absorber or the manner in which the rods are stopped in the event of a support cable breaking.

For the case in which the shock absorber is located at the top of the core, the control rod design will be essentially the same as for the EGCR. The rods will be about 24 ft long and 4 in. in outside diameter, with an absorber section 21 ft long. In the absorber section, annular rings of hot-pressed B_4C , each 3 in. long by $1/4$ in. thick, will be encased in type 304 stainless steel tubing. The tubing will be vented through a metallic filter so that no pressure differential can exist across the tubing wall.

The control rod will be made up of segments and will be sufficiently flexible to ensure entry of the rods into the core even if the channels are not straight. The segments will be supported by a stainless steel rod, which will occupy a central position within the segments. The central rod will be the primary structural element and will be designed to withstand the deceleration rate imposed by the shock absorbers.

If space limitation at the top of the core makes it essential for the shock absorber to be placed at the bottom of the channel, the above-described design may have to be modified. The difficulty arises from the tendency of the segments to jackknife and damage either the rods or the graphite channel. This can be prevented by reducing the flexibility of the rods, and a degree of flexibility can probably be determined for a satisfactory design. Another consideration is that the need for a shock absorber occurs only when a support cable breaks, which should be a rare event. This, coupled with the provision in the core design for removal of the entire graphite block containing the control rod, would allow a flexible design to be used and the core block to be removed if the cable were to break.

3. GCR-3 PHYSICS

The portion of the physics analysis of the GCR-3 core thus far completed had a twofold purpose: to provide intermediate information required for current plant design work such as fast-neutron distributions needed for materials damage studies and power density distributions needed for heat-transfer and shielding studies and to generate fuel-cycle cost information for later use in the overall optimization of the reactor plant with respect to the cost of producing electricity. The fuel-cycle analysis consisted of a general survey study to determine the attainable fuel lifetimes and fuel-cycle costs for a range of lattice pitch and fuel enrichment without specific reference to the details of fuel and poison management and of a detailed examination of a number of particular fuel and poison management programs to provide information about such items as maximum-to-average power density, fuel feed rate, and average discharge exposure. These calculations were made for a reference-case reactor having a 9.4-in. hexagonal pitch and 3% fuel enrichment.

Throughout both phases of the study a number of important design criteria were taken as fixed due to considerations other than those involving core physics. The design criteria dictated by the desire for high-power-density and good heat-transfer characteristics were (1) a thermal reactor power of 2000 Mw, (2) a cooling channel diameter of approximately 5 in., (3) a fuel cluster consisting of six rods hexagonally located on a $1\frac{2}{3}$ -in. pitch about a central seventh rod, (4) a rod outer diameter of 1.00 in. and an average stainless steel cladding thickness of 0.016 in., and (5) a hexagonal reactor lattice with a reference-case triangular pitch of 9.4 in. In addition, considerations involving fabrication of the pressure vessel dictated a reactor core height of 21 ft, a reactor core diameter of 27 ft 9 in. for the reference-core lattice, and a radial reflector thickness of 2 ft. For the reference-case lattice pitch of 9.4 in., these criteria fixed the total number of channels in the reactor at 1135, of which 73 may be used for control purposes. The total core loading in these studies was taken as 87.04 metric tons of uranium.

Methods of Calculation and Input Data

The calculational methods used in the physics analysis were largely those developed for analyzing the EGCR core and fully described in the Gas-Cooled Reactor Project Quarterly Reports.^{1,2} All lifetime calculations were done on the IBM-7090 and with the LT-1 lifetime program.³ For the detailed study of the effects of fuel and poison management, use was made of the FUEL-MOVE program⁴ for the IBM-7090 developed at MIT. Numerous quantities previously computed for the EGCR were, to a large extent, used as input data for these calculations.

Infinite Multiplication Factor

With a system containing only U^{235} as fissionable material used as an illustration, the infinite multiplication factor is defined as

$$k_{\infty} = \eta_{th} \epsilon p f + \eta_e^{25} \epsilon p' (1 - p_{25}) , \quad (1)$$

where

$$\eta_{th} = \frac{\nu \bar{\Sigma}_f}{\bar{\Sigma}_a} \text{ for thermal neutrons in fuel,}$$

ϵ = the total number of fast neutrons produced per neutron produced by fission of U^{235} ,

$p = p_{28} p_{25} p_{ss} p_{gr}$, resonance escape probability in slowing down to thermal energy (including $1/v$) for U^{238} , U^{235} , stainless steel, and graphite,

¹Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. Mar. 31, 1961," USAEC Report ORNL-3102, pp. 3-9.

²Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. Dec. 31, 1961," USAEC Report ORNL-3254, pp. 5-11.

³C. A. Preskitt and R. S. Carlsmith, "LT-1, A General Reactor Lifetime Program for the IBM-7090," USAEC Report ORNL CF-61-8-92, Oak Ridge National Laboratory, August 29, 1961.

⁴N. B. McLeod et al., "The Effect of Fuel and Poison Management on Nuclear Power Systems," USAEC Report NYO-9715, New York Operations, September 15, 1961.

f = thermal utilization, ratio of total thermal fuel absorptions to total thermal absorptions,

$$\eta_e^{25} = \nu \frac{I_F}{I_A} \text{ for } U^{235},$$

$p' = p'_{28} p'_{ss} p'_{gr}$, resonance escape probability (including $1/v$) for slowing down to 6 ev for U^{238} , stainless steel, and graphite.

The value 6 ev which enters into the calculation of p' was selected because it lies just below the lowest resonance of U^{238} , although the calculation is not greatly sensitive to the exact energy used.

In the above model for k_∞ the assumption is made that all U^{235} absorptions occur below 6 ev. The product $\epsilon p'$ then gives the number of neutrons that arrive below the last U^{238} resonance, and $\epsilon p'(1 - p_{25})$ is the number of nonthermal U^{235} absorptions. When these absorptions are multiplied by η_e^{25} , the number of neutrons produced by all nonthermal U^{235} fissions is obtained. For epithermal neutrons it is assumed that there is no flux fine structure in the lattice cell. For thermal neutrons the P-3 approximation is used to determine the radial fine structure.

The above two-term expression of k_∞ arises from the basic assumption that the flux spectrum may be represented as the sum of two components:

$$\phi(E) = \phi_{th}(E) + \phi_e(E). \quad (2)$$

In terms of this model all reaction rates are calculated separately for the thermal and epithermal components. The thermal reaction rate is obtained in terms of an average cross section $\bar{\sigma}$ such that

$$\bar{\sigma} \int \phi_{th}(E) dE = \int \sigma(E) \phi_{th}(E) dE. \quad (3)$$

For the epithermal reaction rate a total resonance integral is defined as

$$I = \int_0^{E_0} \sigma(E) \phi_e(E) dE + I_{meas}(E_0), \quad (4)$$

and a resonance escape probability is then calculated from this resonance integral in the usual way. The flux spectrum $\phi(E)$ is calculated from 10^{-4} to 5 ev by neutron thermalization methods based on the free-gas model. The decomposition into two components is described in ref 2. The

epithermal component $\phi_e(E)$ is obtained by first calculating a flux spectrum by using an absorption cross section that varies as $1/v$ and has the same value at 2200 m/sec as the exact cross section. A Maxwellian component that has been fitted to this flux distribution, by least squares, over the energy range 0.5 to 1.5 times the energy at its peak is then subtracted. The remaining, or epithermal, component thus obtained is subtracted from a second total flux spectrum obtained by neutron thermalization calculations based on the exact energy variation of the cross section. The flux distribution obtained by this process is the thermal component of the spectrum. The thermal component is in general not a true Maxwellian distribution but for the free-gas model approaches it in the limit as the energy variation of the cross section approaches $1/v$. Average cross sections for the nuclides computed in the above manner are listed in Tables 3.1 and 3.2 and are the values used in the current calculations.

U²³⁸ Resonance Integral

The excess resonance integral of U²³⁸ at room temperature was calculated according to the formula of Hellstrand,⁵

$$RI_{exc} = 4.05 + 26.6 \sqrt{\frac{S}{M}}, \quad (5)$$

and found to have a value of 16.74 barns. The temperature coefficient of the U²³⁸ effective resonance integral in EGCR has been reported⁶ to be

$$\frac{1}{I} \frac{dI}{dT} = 2.90 \times 10^{-3} T^{-0.48} (^\circ K). \quad (6)$$

We used this result in our present studies to obtain a value of 18.01 barns for the excess U²³⁸ resonance integral at 800°K. The total resonance integral (excess plus $1/v$) of U²³⁸ is then 19.66 barns at 800°K.

⁵E. Hellstrand, "Measurements of the Effective Resonance Integral in Uranium Metal and Oxide in Different Geometries," J. Appl. Phys. 28(12):1493 (December 1957).

⁶F. C. Engesser, T. J. Oakes, and R. E. Heineman, "PCTR Measurements of the EGCR Fuel Temperature Coefficient," USAEC Report HW-63766, Hanford Atomic Products Operation, February 3, 1960.

Table 3.1. Nuclear Constants Used in All Calculations

$$T_n = 900^\circ\text{K}$$

Component	Reaction	Cross Sections (barns)				
		σ_o	I_{meas}	$\bar{\sigma}_{\text{th}}$	I	σ_s
Xe ¹³⁵	Absorption	2.536×10^6	0.0	1.476×10^6	4.057×10^4	4.3
Sm ¹⁴⁹	Absorption	3.612×10^4	0.0	4.255×10^4	3.063×10^3	5.0
U ²³⁵	Absorption	682.56	370.0	316.9	438.5	10.0
Pu ²³⁹	Absorption	1042.6	478.5	1388.0	1487.0	9.6
Pu ²⁴¹	Absorption	1400.0	781.0	1158.0	1123.0	10.0
U ²³⁸	Absorption	2.71	0.0	1.368		8.3
Pu ²⁴⁰	Absorption	295.0	8850.0	148.9	9030.0	10.0
Pu ²⁴²	Absorption	30.0	1015.0	15.15	1033.0	10.0
U ²³⁶	Absorption	7.0	256.0	3.534	260.3	10.0
Graphite	Absorption	4×10^{-3}		2.019×10^{-3}	2.438×10^{-3}	4.65
Type 304 stainless steel	Absorption			1.5167	2.88	
U ²³⁵	Fission ($\nu = 2.43$)		271.0	268.10	328.04	
Pu ²³⁹	Fission ($\nu = 2.84$)		319.0	854.86	907.44	
Pu ²⁴¹	Fission ($\nu = 3.06$)		567.5	838.89	863.19	

Table 3.2. Nuclear Constants for Stable Fission Products

Fission-Product Pseudoelement ^a	Fission Yield	Cross Sections (barns)				
		Γ_{exc}	$g\sigma_0$	σ_{th}	$\Gamma_{1/v}$	Γ_{total}
U ²³⁵	0.1124	71.0	216.8	109.5	132.2	203.2
	0.2100	283.1	31.5	15.9	19.2	302.3
	0.0582	905.5	140.9	71.2	85.9	991.5
	0.0295	2450.0	129.2	65.2	78.7	2529.0
Pu ²³⁹	0.1170	90.9	204.7	103.3	125.0	216.0
	0.2027	277.4	31.5	15.9	19.2	297.0
	0.0860	918.4	170.0	85.7	103.4	1022.0
	0.0600	2400.0	147.0	74.1	89.5	2490.0

^aE. A. Nephew, "Thermal and Resonance Absorption Cross Sections of the U²³³, U²³⁵, and Pu²³⁹ Fission Products," USAEC Report ORNL-2869, Oak Ridge National Laboratory, January 18, 1960.

Fast-Fission Factor

An estimate of the fast-fission effect for the GCR-3 fuel element configuration was obtained by the method of Carlvik and Pershagen.⁷ Due to its nearness to unity and to the preliminary nature of the studies being reported here, no attempt was made to further refine this estimate. Throughout the calculations the value $\epsilon = 1.01$ has been used.

Streaming Corrections

In recent work by Benoist⁸ a method for calculating the diffusion coefficient of both fast and thermal groups is described. This method takes into account the effects of neutron streaming through voids. Unfortunately, due to the complex geometry of the GCR-3 fuel cluster, the formulas quoted by Benoist cannot be directly applied. Therefore, we have used Benoist's method by making the simplifying assumptions that all flux disadvantage factors in the lattice cell are unity and (furthermore) that the mean free path of neutrons is identical in the moderator and fuel rod. For a single cylindrical rod in a cylindrical channel (see Fig. 3.1) Benoist's formulas then reduce to

$$\frac{D_k}{D_{gr}} - 1 = \frac{V_{void}}{V_{total}} + \frac{V_{void}}{V_{total}} \frac{c-a}{\lambda_{m,th}} (Q_k + Q'_k - q_k), \text{ for } k = r \text{ or } z, \quad (7)$$

and

$$\frac{\tau_k}{\tau_m} - 1 = \frac{2 V_{void}}{V_{solid}} + \frac{V_{void}}{V_{solid}} \times \frac{c-a}{\lambda_{m,f}} (Q_k + Q'_k - q_k), \quad (8)$$

where Q_k , Q'_k , and q_k are tabulated functions depending upon the parameters α , η , and γ , with $\alpha = a/c$, $\eta = a/\lambda_u$, and $\gamma = c/\lambda_m$, and the volume terms are self-explanatory.

⁷I. Carlvik and B. Pershagen, "The Fast Fission in a Cylindrical Fuel Element," Swedish Report AEF-70, AB Atomenergi, Dept. of Physics, Stockholm, Sweden, November 1956.

⁸P. Benoist, "General Formulation of the Coefficient of Diffusion in a Heterogeneous Medium Containing Cavities," French Report SPM No. 522, Commissariat a l'Energie Atomique, December 1958.

Fig. 3.1. Model for Lattice Cell Used in Streaming Calculations.

Equation (7) is very similar to the formulation of Behrens'⁹ in that it consists of a density correction term together with a correction term due to the finer details of diffusion. In our application of Eqs. (7) and (8) to the actual seven-rod cluster the void volume was taken to be the total void volume, whereas the contribution of the second term of Eqs. (7) and (8) was calculated on the model of a single UO_2 rod of reduced density having a radius equal to the envelope radius of the cluster. The overall correction to the fast and thermal diffusion coefficients has been calculated in two steps:

1. diffusion coefficient and age correction terms calculated by means of Eqs. (7) and (8) for the core mixture alone without control rod voids taken into account,
2. the average core diffusion coefficients then obtained by the relation

$$\bar{D}_{\text{core}} = \frac{D_z}{3} + \frac{2D_R}{3} ,$$

and from this the average mean free path in the core mixture computed.

Equations (7) and (8) are then reapplied to a unit control rod cell surrounded by a moderator having the properties of the average core mixture, and the total correction factor in the axial and radial directions is the product of the two separate factors. These methods were used for calculating the streaming correction factors for the 9.4-in. hexagonal lattice:

$$D_R/D_{\text{gr}} = 1.3363$$

$$D_z/D_{\text{gr}} = 1.4483$$

$$\tau_R/\tau_{\text{gr}} = 1.7336$$

$$\tau_z/\tau_{\text{gr}} = 1.8497$$

⁹D. J. Behrens, "The Migration Length of Neutrons in a Reactor," British Atomic Energy Authority Report AERE-T/R-877, February 1952.

It is fully recognized that the above values are uncertain due to the many simplifying assumptions and approximations made. An effort is being undertaken to develop methods that will permit the application of Benoist's techniques directly and in full detail to the actual GCR-3 cluster geometry.

Effective Multiplication Factor

The LT-1 program extends the analysis of the infinite multiplication factor to a finite reactor by applying leakage corrections. A number of assumptions must be made in order to determine the distribution of absorptions in the various nuclides from the nonleakage and resonance escape probabilities. These assumptions, which determine the sequence of events undergone by fast neutrons during the course of thermalization, are built into the LT-1 program and are as follows:

1. The resonance structure of U^{238} is located entirely above an energy E_0 .
2. All epithermal absorption occurs below energy E_0 , excepting that which occurs in the U^{238} resonances and that portion of the U^{238} $1/v$ cross section lying above E_0 .
3. All neutron leakage corresponding to $\tau \leq \tau_1$ occurs before the U^{238} resonance absorption. Here, τ_1 is a partial neutron age corresponding to an energy range extending from source energy to the energy corresponding to the beginning of the U^{238} resonance region.
4. All leakage corresponding to $\tau_1 < \tau \leq \tau_{th}$ occurs after the resonance absorption and before the remaining epithermal absorption.

For the remaining epithermal absorption, no preferred sequence of absorptions in the various nuclides is assumed. The composite nonescape probability is computed for all nuclides, and assignment of the absorptions by individual nuclides is made on the basis of the ratio of its macroscopic resonance integral to that of the combined macroscopic resonance integral of all the nuclides. It should be noted that the number of neutrons reaching thermal, $\exp(-B^2\tau_{th}) \prod_j p_j$, does not depend upon the order in which absorption in various nuclides or leakage occurs. The above assumptions are made only in order to determine the number of neutrons lost to each individual component.

General Survey of GCR-3 Core Lifetimes as a Function
of Lattice Pitch and Enrichment

In the general survey study of fuel lifetimes for the GCR-3 core, both batch and equilibrium fuel lifetimes were computed for a range of lattice pitch and enrichment. A batch cycle is defined by the condition that all of the fuel is loaded at the same time and that all of it is removed when the effective multiplication factor falls to a specified value. An equilibrium cycle implies continuous loading and discharging of the fuel in such a way that a uniform composition of fuel varying from zero to maximum exposure is maintained at all times in all parts of the reactor. Starting with specified nuclide concentrations, the equilibrium lifetime is defined as the particular exposure for which the compositions, averaged over time up to that exposure, constitute a system whose multiplication factor equals a selected value.

In both the batch and the equilibrium cycle calculations by the LT-1 lifetime program,¹⁰ the fuel composition is uniform over the volume of the reactor at any given time. This implies a fundamental mode solution for the flux and consequently minimum buckling. Any departure from a spatially uniform fuel distribution, such as zoning, must necessarily increase the buckling and hence the overall neutron leakage. If, in addition, the fuel management is such as to concentrate the most exposed fuel at the center of the core, then an additional penalty in reactivity will arise. The resulting lower attainable lifetimes for such systems are characteristic of the penalties that must be paid for power flattening requirements. In order to make the survey lifetime calculations more representative of actual reactor conditions, we have anticipated this penalty by arbitrarily designating end of life as being $k_{\text{eff}} = 1.02$, and thus have anticipated the overall loss of core reactivity due to specific details of fuel and poison management to be 2%. The results for fuel and poison management programs discussed in "Studies of Specific Fuel Management Programs," this chapter, show that this figure is quite accurate enough for the intended purpose.

¹⁰C. A. Preskitt and R. S. Carlsmith, "LT-1, A General Reactor Lifetime Program for the IBM-7090," USAEC Report ORNL CF-61-8-92, Oak Ridge National Laboratory, August 29, 1961.

A further loss in attainable lifetime in the actual reactor must necessarily arise from the requirement of refueling and discharging at discrete time intervals rather than on a truly continuous basis. This method of refueling and discharging is necessitated by the requirement that, in order for the reactor to operate for a finite period of time, the initial reactivity of the core just after the refueling operation must exceed that value designated as end of life in the equilibrium cycle. Reactivity control must then be provided by control rods rather than by old fuel, resulting in loss of lifetime. This may be seen differently by observing that the greater the average feed time interval the higher will be the exposure of the freshest fuel at feed time ($k = 1.02$), which means that the oldest fuel in the reactor must have a smaller exposure than is allowed with a continuous feed.

In the present parameter study, lifetime calculations were performed for three different fuel enrichments for each of eight different lattice pitches - fuel enrichments of 2.2, 2.5, and 3.0 wt %, and triangular lattice pitches of 9.134, 9.40, 9.671, 10.208, 11, 12, 13, and 14 in. The diameter of the reactor core implied by this range of pitches varies from 27.75 ft for the 9.4-in. pitch to 41.27 ft for the 14-in. pitch. In each of the 24 cases outlined above the core height was taken as the reference design value of 21 ft. No attempt was made to determine the optimum core height for each of the different core diameters since it was felt that the net effect of leakage is not large enough to have a major influence on the selection of lattice parameters. The total fuel loading in the reactor was held constant at 87.04 metric tons of uranium for all cases.

The results of the lifetime calculations are shown in Fig. 3.2, where the effects of varying the carbon-to-uranium ratio can be clearly seen. At low lattice pitches the decrease in resonance escape probability due to undermoderation more than balances the increase in thermal utilization. A loss of reactivity and consequently a decrease in fuel lifetime result. At high lattice pitches the decrease in thermal utilization due to an increased number of absorptions in graphite is the dominant factor. A decrease in lifetime due to overmoderation thus results. For a given fuel enrichment the optimum lattice pitch with respect to fuel

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Fig. 3.2. GCR-3 Equilibrium Fuel Lifetime vs Lattice Pitch and Fuel Enrichment.

lifetime can thus be selected from the broad maximum of the curve shown in Fig. 3.2.

Figure 3.3 shows the equilibrium lifetime for the reference-case lattice (9.4-in.) pitch as a function of fuel enrichment. It is seen here that no practical limitations are imposed on the attainable lifetime by reactivity considerations. The real limitation on exposure may well be dictated by metallurgical considerations over much of this range. In any event, the criterion for selecting the optimum reactor parameters is not maximum fuel lifetime but minimum overall power generation costs. For example, two factors cause the optimum lattice pitch with respect to power generation costs to differ from that with respect to fuel lifetimes: (1) an increase in pitch can cause the reactor capital costs to increase greatly and thus adversely affect the overall power generation costs; (2) as is shown in Fig. 3.2, an increase in pitch results in a decrease in the conversion ratio of the reactor, which causes relatively more expensive fuel to be consumed and thus adversely affect fuel-cycle costs.

Fuel-Cycle Costs Resulting from General Survey

The significance of the lifetime calculations becomes more apparent when the results are expressed in terms of fuel-cycle costs. All cost analyses were done on the IBM-7090 by use of a modified version of the EPITAPH code.¹¹ The bases for the analysis and the cost parameters used were those set forth in the Guide to Nuclear Power Cost Evaluation.¹² The relevant cost parameters used throughout these calculations (Table 3.3) are those values presently set by industry and the AEC as being representative of current conditions. The conversion and fabrication cost of fresh fuel elements of \$52 per kilogram of uranium was based on EGCR experience.

¹¹L. L. Bennett, "EPITAPH -- A Reactor Fuel Cost Calculation," USAEC Report ORNL CF-61-3-105, Oak Ridge National Laboratory, March 21, 1961.

¹²Kaiser Engineers, Guide to Nuclear Power Cost Evaluation, Vol. 4, "Fuel Cycle Costs," USAEC Report TID-7025, March 15, 1962.

Fig. 3.3. GCR-3 Lifetime vs Fuel Enrichment for a 9.4-in. Triangular Pitch.

Table 3.3. Parameters Used in Fuel-Cycle Cost Analysis

<u>Conversion and Fabrication</u>	
Conversion and fabrication, \$/kg of U	52
Separation plant charge, \$/day	17,000
Uranium conversion, \$/kg of U	5.60
Plutonium conversion, \$/g	1.50
<u>Shipping Charges</u>	
Fresh-fuel shipping charge, \$/kg of U	1.50
Spent-fuel shipping charge and transit to the AEC, \$/kg of U	17
<u>Time Factors</u>	
Conversion and fabrication plant capacity, MT of U per 30 days	4
Separation plant capacity, MT of U per day	1
Uranium and plutonium conversion capacity, MT of U per day	1
Preirradiation shipping and storage time, days	70
Spent-fuel decay time, days	120
Spent-fuel shipping time, days	20
<u>Loss Factors</u>	
Conversion and fabrication losses, %	2.0
Separation losses, uranium and plutonium, %	1.0
Conversion losses, %	
Uranium	0.3
Plutonium	1.0
<u>General</u>	
Use charge for uranium, %	4.75
Interest rate on working capital, %	12.0
Base charges for enriched-uranium, as UF ₆	(a)
Plutonium value, \$/g	8
Thermal-to-electrical efficiency	0.40
Plant load factor	0.80
Thermal power of core, Mw	1908

^aJuly 1, 1962, schedule used in calculations.

Fuel-Cycle Costs for Continuous-Feed Fuel Cycles

The GCR-3 fuel-cycle costs for an idealized equilibrium cycle are shown in Fig. 3.4 as a function of fuel enrichment and lattice pitch. The parameters used in the calculations are taken from Tables 3.3 and 3.4. For a given fuel enrichment, the hidden variable in these curves is that of exposure. It can be seen from Fig. 3.4 that for a specified enrichment a considerable decrease in fuel cycle costs can be achieved by increasing the lattice pitch from 9.4 in. to about 12 in. However, it must be noted that the decrease in fuel-cycle costs results primarily from an increase in fuel exposure to values which may be unattainable due to metallurgical limitations of the fuel element. If, for example, the real limitation on fuel exposure is set by metallurgical considerations to be in the range 15,000 to 25,000 Mwd per metric ton of uranium, many of the fuel-cycle costs plotted in Fig. 3.4 will be unattainable.

Table 3.4. Fuel-Cycle Costs for 9.4-in. Lattice Pitch and 3% Enrichment for an Idealized Continuous-Feed Equilibrium Cycle

Item	Cost (mills/kwhr)	
	For \$8 per g of Pu	For \$12 per g of Pu
U ²³⁵ burnup	0.8490	0.8490
Plutonium credit	-0.2672	-0.4006
Fabrication	0.2272	0.2272
Inventory	0.1385	0.1385
Reprocessing	0.1541	0.1541
Shipping	0.1187	0.1187
Losses	0.0313	0.0339
Interest on working capital	0.0711	0.0711
Total	1.3227	1.1919

Fig. 3.4. GCR-3 Fuel-Cycle Costs for Idealized Equilibrium Cycle vs Lattice Pitch and Fuel Enrichment.

Figure 3.5 shows fuel-cycle cost as a function of pitch for constant fuel exposure. The hidden variable in these curves is that of fuel enrichment. From Fig. 3.5 it is seen that the incentive for going toward a higher pitch is considerably reduced. For example, for an exposure limitation of 20,000 Mwd per metric ton of uranium, a maximum decrease in fuel-cycle costs of about 0.05 mill/kwhr can be achieved by increasing the lattice pitch from 9.4 in. to 10 or 11 in. Since lifetime is not a variable in a typical curve shown in Fig. 3.5, the variation in fuel-cycle cost is primarily associated with variations in the cost of burning U^{235} and in the credit from plutonium production. With a fixed enrichment the variation of lattice pitch has a large effect on lifetime and hence fuel-cycle cost; but with a fixed lifetime an increase in lattice pitch, for example, results in a decrease in enrichment and at the same time a decrease in the conversion ratio. Both factors give relatively small changes in fuel-cycle cost and, in addition, are opposing effects. The net result is a rather weak dependence of fuel-cycle cost on lattice pitch, if the fuel lifetime is held constant. These results must be integrated with capital-cost data in order for the lattice parameters to be finally optimized. It appears unlikely, however, that the optimum lattice pitch will differ greatly from the present design value of 9.4 in.

Figure 3.6 shows the total fuel-cycle costs for the reference-case pitch of 9.4 in. as a function of fuel enrichment. Results obtained for values of both \$12 and \$8 per gram of plutonium illustrate the effect of this parameter on the overall fuel-cycle cost. Table 3.4 gives an itemized breakdown of the individual cost components for the case of a 9.4-in. pitch with 3% enriched fuel.

Fuel-Cycle Costs for Discontinuous-Feed Cycles

The fuel-cycle costs quoted above are for an idealized equilibrium cycle with continuous feed. Higher fuel-cycle costs will necessarily be associated with discontinuous-feed cycles because of their reduced lifetimes. Figure 3.7 shows the penalty that must be paid in core lifetime as the fuel loading scheme approaches a batch cycle. It is of interest to analyze the fuel-cycle costs as a function of N , where N is

Fig. 3.5. GCR-3 Equilibrium Cycle Fuel Costs vs Lattice Pitch and Exposure.

Fig. 3.6. GCR-3 Fuel-Cycle Costs vs Enrichment for a 9.4-in. Triangular Pitch and an Idealized Equilibrium Cycle.

Fig. 3.7. Reduction in Core Lifetime Resulting from Discontinuous Loading Cycles.

the number of equally spaced fuel loadings during core life, in order to determine the incentive for providing continuous on-line fuel-handling equipment. With the parameters listed in Table 3.3, fuel-cycle costs were determined for $N = 1, 2, 3, 4, 5$, and ∞ . A certain degree of arbitrariness exists as to the most reasonable values to assign to the holdup time incurred in the conversion and fabrication of the fuel elements as well as to the time required for postirradiation conversion. These times affect the inventory and interest items of the fuel-cycle costs. It is felt that some penalty in fabrication and conversion times ought to be inherent in the low- N -value feed cycles since larger batch sizes of fuel must be accumulated before feeding. The arbitrary values shown below were used for these calculations, and were based on a maximum fabrication plant uranium throughput rate of 4 MT/30 days and the assumption that 4-MT batches of uranium in the form of UF_6 can be obtained singly from the AEC as required by the fabrication plant:

<u>N Values</u>	<u>Holdup Time (days)</u>	
	<u>Fabrication and Conversion</u>	<u>Postirradiation Conversion</u>
1	345	93
2	180	49
3	135	35
4	105	27
5	90	23
∞	30	5

Figure 3.8 shows the calculated fuel-cycle cost for the above discontinuous-feed cycles over a range of lifetimes and fuel enrichments. All calculations are for a lattice pitch of 9.4 in. and are based on a thermal-to-electrical efficiency of 0.40, with thermal power of the core taken to be 1908 Mw. The fuel-cycle-cost calculations are based on a plutonium value of \$8/g, and the AEC price schedule of July 1962 is used to determine the base charges for enriched uranium. Considering that an $N = 4$ cycle should be allowable without on-line charging equipment, these curves show that the penalty associated with the discontinuous cycle is approximately 10% of the fuel-cycle cost. The significance of this result

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Fig. 3.8. General Survey of GCR-3 Fuel-Cycle Costs vs Fuel Enrichment for Discontinuous-Feed Cycles.

can be evaluated fully only when it is integrated with capital-cost estimates and other design considerations for the fuel-handling equipment.

Control Rod Worth

The control rods in the GCR-3 core are situated at fuel lattice positions and are arranged in a triangular array, which gives one control rod for each 15 loaded fuel channels. The proposed control rods are similar to those in the EGCR except for an increased diameter. The absorbing elements of the rod are B_4C annular pellets with an outside diameter of 4.0 in. and an inside diameter of approximately 3.5 in.

The estimated shutdown worth of the rods is based on two-group supercell calculations of the control rod lattice and thus ignores the effect of the rods on neutron leakage. Since the total nonleakage probability is 0.967, the supercell model should be quite accurate.

The rods were assumed to absorb all incident thermal neutrons and to have the same blackness for nonthermal neutrons as the EGCR rods have.¹³ In terms of the boundary condition at the control rod surface, the values used for the extrapolation lengths were $l_{ext} = 0.901 \lambda_{tr}$ for thermal neutrons and $l_{ext} = 1.650 \lambda_{tr}$ for nonthermal neutrons, where λ_{tr} is the transport mean free path in the fuel lattice.

A single fuel enrichment of 3% and three lattice pitches were considered in the calculations. In each case the rod worth, Δk , was determined in an idealized continuous-feed equilibrium fuel cycle, but the worth is expected to be rather insensitive to details of the fuel cycle. The results are given below:

Triangular Lattice Pitch (in.)	Δk
9.13	0.174
9.67	0.169
10.21	0.159

¹³Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. June 30, 1961," USAEC Report ORNL-3166, p. 3.

The adequacy of the rod bank is determined by comparing the rod worth with the control requirements for fuel cycles of interest. These requirements must provide for the excess reactivity required to compensate for equilibrium xenon poison, fuel and moderator temperature change between operating and room temperature, subcritical shutdown margin, and allowance for fuel lifetime.

Calculations of the reactivity change due to moderator temperature change are not yet available, but based on experience from the analysis of EGCR this temperature coefficient is expected to be positive and thus enters in a negative sense in the consideration of the control requirements given below:

<u>Reactivity Component</u>	<u>Δk</u>
Fuel temperature	+0.025
Equilibrium xenon	+0.038
Shutdown margin	+0.030
Moderator temperature	-()
Total	< +0.093

For a lattice pitch of 9.4 in. the results shown above for rod worth indicate that the available control is $\Delta k = -0.172$. The amount of control available for fuel lifetime is thus $0.172 - 0.093 = 0.079$ plus the contribution from moderator coefficient.

In a continuous-feed equilibrium cycle the allowance for fuel lifetime would be quite small, at most approximately 0.01 as an operating margin in the event of breakdown of the fuel-charging equipment. For the discontinuous feed envisioned for GCR-3, the requirement depends on the frequency of feed, and for the out-in cycles currently analyzed the results are those given below:

<u>Fraction of Core Loaded</u>	<u>Interval Between Loadings (days)</u>	<u>Control Required After Loading, Δk</u>
1/1	522.0	0.174
1/2	365.3	0.127
1/3	270.9	0.090
1/4	214.4	0.076
1/5	177.1	0.061

These results indicate that the present control rods should be adequate for discontinuous cycles in which one-fourth, or less, of the core is charged at each loading. Cycles in which more than one-fourth of the core is loaded are generally of little interest if minimum fuel-cycle cost is an objective.

Fast-Neutron Spatial Distribution

In order to estimate the strains and stresses resulting from irradiation-induced dimensional changes in graphite, the spatial distribution of fast neutrons has been computed for a GCR-3 lattice cell. Studies have indicated¹⁴ that the damage to graphite caused by fast-neutron collisions is due principally to neutrons with energies greater than about 0.18 Mev and that the dimensional changes are generally considered to be proportional to the integrated flux above this energy. The arguments supporting this model are based on energy transfer, the neutrons having lost about 90% of their initial energy, rather than on a distinct threshold effect.

The fast-neutron distributions reported here were calculated by means of multigroup diffusion theory, with 20 groups being concentrated in the range above 0.18 Mev. The hexagonal lattice cell was approximated by a circle enclosing the same area, and the six outer rods of the cluster fuel element were replaced by an equivalent annulus with a thickness equal to the rod diameter. The entire problem is thus reduced to one spatial dimension. The resulting neutron distribution is shown in Fig. 3.9.

Studies of Specific Fuel Management Programs

The FUEL-MOVE¹⁵ program for the IBM-7090 was used to study the gross features of a number of fuel management techniques for the reference

¹⁴Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. Dec. 31, 1959," USAEC Report ORNL-2888, p. 15.

¹⁵N. B. McLeod et al., "The Effect of Fuel and Poison Management on Nuclear Power Systems," USAEC Report NYO-9715, New York Operations, September 15, 1961.

Fig. 3.9. Fast-Neutron Flux ($E > 0.18$ Mev) in a GCR-3 Lattice Cell for the Average Axial Position in a Channel Generating 1.8 Mw.

GCR-3 core. This code is a fast-running two-dimensional, two-group, fuel-depletion code especially designed for survey studies. It evaluates the important gross characteristics of reactor performance for particular fuel and poison management programs. FUEL-MOVE is actually written as two separate codes. In the FUEL code the homogenized unit cell properties of the core are evaluated as a function of flux time and stored for subsequent use by the MOVE code. The MOVE code represents fuel by its flux-time properties. It evaluates flux and power-density distributions, control poison requirements, criticality factor, and average core properties throughout fuel lifetime. When fuel is discharged, the code obtains the nuclide concentrations and average burnup. The calculational methods used in obtaining a solution to the two-dimensional diffusion equation restrict the MOVE code to the treatment of a cylindrical reactor with azimuthal symmetry whose reflector can be represented by a reflector savings. Before using the MOVE code in these studies a number of independent calculations with the one-dimensional WANDA¹⁶ code were made to determine a suitable value of the reflector savings.

A number of different fuel-movement techniques can be selected by option in the FUEL-MOVE code. The fuel management program chosen in this study consisted of a discontinuous out-in irradiation, which, it was felt, would provide a favorable flattening of the radial power distribution. In this technique the reactor is divided into a number of equal-volume radial zones and is operated batchwise until it ceases to be critical with all control poison removed; then the fuel in the central zone is discharged. Fuel in the other zones is moved one zone inward, with an axial inversion if the move occurs at approximately mid-life. The inversion consists of restacking the two fuel assemblies, which results in the top and bottom elements exchanging positions in the channel. This method of inversion greatly improves the uniformity of exposure along the length of the fuel assemblies. Finally, new fuel is charged to the vacant outer zone.

¹⁶O. J. Marlowe, "WANDA - A One-Dimensional Few Group Diffusion Equation for the IBM-704," USAEC Report WAPD-TM-28, Add. 2, Westinghouse Bettis Atomic Power Laboratory, July 1959.

Several different poison management options are available in the FUEL-MOVE code. Due to the survey nature of these studies we have chosen the simplest, that of uniform poison removal. In this option the spatial distribution of poison is uniform over the reactor volume, and its magnitude is varied for reactivity control. It is recognized that this technique is somewhat unrealistic for a system of discrete control rods. However, the study serves to point out the basic advantages and disadvantages of the particular fuel management program. Once these basic characteristics are known, more detailed and realistic control techniques can be examined.

Fuel and poison management calculations were made for cases in which the reference GCR-3 core was divided into two, three, four, and five equal-volume radial zones; the results obtained are given in Tables 3.5-3.9. A cyclic-state condition is defined arbitrarily by the requirement that the exposures calculated for each mesh point of the problem are within 1% of the previous end-of-batch values. The time required for the reactor starting from an initial uniform loading of fresh fuel to arrive at a cyclic-state condition within 1% varies from 5.39 years for the two-zone core to 4.74 years for the five-zone core. The relatively short period of time required to approach a cyclic-state condition can be seen from the fact that in all of the cases studied the average exposure attained by the first core is nearly 90% of the final discharge exposure for cyclic state. The average exposure of fuel discharged at cyclic equilibrium increases from 16,729 Mwd per metric ton of uranium for the two-zone core to 20,279 Mwd per metric ton of uranium for the five-zone core, and the feed time interval decreases from 365 days to 177 days. These results reflect the increase in average exposure that can be expected as the refueling cycle approaches that of continuous feed, and are within about 5% of the exposures predicted by the results of the general survey (see Fig. 3.2). The uniformity of exposure in the discharged fuel is shown by the fact that the peak-to-average exposure ratio is about 1.10 for all cases.

Table 3.5. Lifetime Results for the Reference-Case GCR-3 Core Divided into Two Equal-Volume Radial Zones and with a Discontinuous Out-In Fuel Movement with an Axial Inversion of Zone Two Used Before Transfer

Feed Time (years)	Zonal Burnups (Mwd/MT of U)		k_{eff} After Refueling		$P_{\text{max}}/\bar{P}$	
			With Equilibrium Xe and Sm	Without Equilibrium Xe and Sm	Before Feed	After Feed
	Zone 1	Zone 2				
0.0000	0	0	1.174	1.235		2.2969
1.4300	13,635	10,257	1.119	1.162	1.2692	1.9354
2.3694	17,323	8,638	1.128	1.173	1.4897	2.1405
3.3885	16,714	8,942	1.127	1.171	1.4406	2.1040
4.3901	16,811	8,858	1.127	1.171	1.4491	2.1132
5.3895	16,729				1.4484	

Table 3.6. Lifetime Results for the Reference-Case GCR-3 Core Divided into Three Equal-Volume Radial Zones and with a Discontinuous Out-In Fuel Movement with an Axial Inversion of Zone Two Used Before Transfer

Feed Time (years)	Zonal Burnups (Mwd/MT of U)			k_{eff} After Refueling		$P_{max}/\bar{P}$	
	Zone 1	Zone 2	Zone 3	With Equilibrium	Without Equilibrium	Before	After
				Xe and Sm	Xe and Sm	Feed	Feed
0.0	0	0	0	1.174	1.235		2.2854
1.4331	14,010	12,457	9,448	1.079	1.118	1.2641	2.3490
2.0998	17,498	14,866	6,247	1.090	1.130	1.5312	1.7265
2.8333	19,560	12,896	7,041	1.090	1.130	1.4856	2.0149
3.5875	18,290	13,580	6,956	1.089	1.129	1.4692	1.8923
4.3266	18,694	13,433	6,924	1.090	1.129	1.4649	1.9097
5.0701	18,607	13,433	6,940	1.090	1.129	1.4605	1.9129
5.8130	18,606					1.4608	

Table 3.7. Lifetime Results for the Reference-Case GCR-3 Core Divided into Four Equal-Volume Radial Zones and with a Discontinuous Out-In Fuel Movement with an Axial Inversion of Zone Three Used Before Transfer

Feed Time (years)	Zonal Burnups (Mwd/MT of U)				k_{eff} After Refueling		$P_{max}/\bar{P}$	
					With Equilibrium Xe and Sm	Without Equilibrium Xe and Sm	Before Feed	After Feed
	Zone 1	Zone 2	Zone 3	Zone 4				
0.0	0	0	0	0	1.174	1.235		2.2971
1.4273	14,105	13,171	11,481	8,939	1.059	1.095	1.2719	1.9623
1.9217	16,116	15,707	13,475	4,840	1.077	1.114	1.6314	1.9972
2.5060	18,843	18,123	10,778	5,826	1.076	1.113	1.6297	2.0645
3.0862	20,928	15,780	11,661	5,780	1.076	1.114	1.6792	2.0323
3.6792	19,046	16,646	11,604	5,756	1.076	1.113	1.6443	2.0421
4.2647	19,713	16,538	11,575	5,763	1.076	1.113	1.6583	2.0412
4.8518	19,637						1.6565	

Table 3.8. Lifetime Results for the Reference-Case GCR-3 Core Divided into Five Equal-Volume Radial Zones and with a Discontinuous Out-In Fuel Movement with an Axial Inversion of Zone Three Used Before Transfer

Feed Time (years)	Zonal Burnups (Mwd/MT of U) Zone 1 Zone 2 Zone 3 Zone 4 Zone 5					k_{eff} After Refueling		$P_{max}/\bar{P}$	
						With Equilibrium Xe and Sm	Without Equilibrium Xe and Sm	Before Feed	After Feed
0.0	0	0	0	0	0	1.174	1.235		2.2597
1.4429	14,288	13,674	12,574	10,960	8,776	1.046	1.080	1.2533	1.8740
1.8403	16,117	15,926	14,334	12,361	3,841	1.060	1.095	1.5969	1.8245
2.3252	18,663	17,883	16,258	8,958	4,966	1.060	1.096	1.5335	1.8159
2.7999	20,144	19,564	13,272	9,991	4,955	1.061	1.097	1.5329	1.7856
3.2826	21,774	16,992	14,255	9,980	4,953	1.061	1.097	1.5772	1.7628
3.7715	19,574	17,925	14,228	9,959	4,920	1.061	1.096	1.5468	1.7729
4.2545	20,332	17,845	14,190	9,927	4,922	1.061	1.096	1.5532	1.7691
4.7397	20,279							1.5523	

Table 3.9. Summary of Discharge Exposures and Feed Intervals for Reference-Case Core of GCR-3

Number of Equal-Volume Zones	First-Core Average Exposure (Mwd/MT of U)	Discharge Exposure at Cyclic Equilibrium (Mwd/MT of U)		Feed Time Interval at Cyclic Equilibrium (days)	Estimated Refueling Operation Time (days)
		Average	Maximum		
2	15,479	16,729	18,758	365.3	14.38
3	17,023	18,606	20,739	270.9	11.33
4	17,498	19,637	21,349	214.4	9.79
5	18,197	20,279	21,959	177.1	8.83

The radial power distributions for both beginning- and end-of-cycle conditions are given in Figs. 3.10-3.13. The main coolant blower requirements stipulate the desirability of maintaining an overall radial peak-to-average power-density ratio not greater than about 1.2. This condition is approximately met by all four of the cases studied. In addition, however, since there are at present no means of on-power regulation of the channel orifice settings that determine the coolant flow per channel, it is desirable for the radial power distribution during a given cycle to vary no more than about 5%. With the present poison management program, this requirement is met only in the case of the three-zone core. However, it seems quite likely that more detailed studies of poison management can indicate methods for dealing with this problem.

The axial peak-to-average power ratio should be maintained at 1.5, or less, in order to avoid an excessive temperature peaking along the length of a fuel assembly. Figures 3.14-3.17 show the results obtained for both beginning- and end-of-cycle conditions; none of the cases studied meet the above requirement. Moreover, it is difficult to devise a satisfactory method of handling this problem, since the goals of achieving a uniform axial exposure and at the same time a low axial peak-to-average power ratio are not compatible. Further study is required to devise methods that will yield an overall acceptable power distribution in the GCR-3 core.

An inherent disadvantage of the out-in type of fuel movement is that multiple handling of each fuel element is required during its lifetime in the reactor. This may increase the probability of a mechanical failure occurring in the fuel assembly and hence may impose an upper limit on the number of radial zones that can be considered for fuel-shuffling purposes. On the other hand, decreasing the number of radial zones decreases the attainable fuel exposure and increases the excess reactivity which must be controlled by rod operation. In this study no attempt has yet been made to recommend a particular fuel movement program, or even to suggest which one of the four cases studied might be best suited to meet the often conflicting demands of actual reactor operation. However, the lifetimes predicted in the general survey were shown to be achievable with specific practical fueling procedures.

Fig. 3.10. GCR-3 Radial Power Distribution for Core with Two Equal-Volume Radial Zones.

Fig. 3.11. GCR-3 Radial Power Distribution for Core with Three Equal-Volume Radial Zones.

Fig. 3.12. GCR-3 Radial Power Distribution for Core with Four Equal-Volume Radial Zones.

Fig. 3.13. GCR-3 Radial Power Distribution for Core with Five Equal-Volume Radial Zones.

Fig. 3.14. GCR-3 Axial Peak-to-Average Power Ratio for a Two-Zone Core as a Function of the Radial Distance from the Center of the Reactor.

Fig. 3.15. GCR-3 Axial Peak-to-Average Power Ratio for a Three-Zone Core as a Function of the Radial Distance from the Center of the Reactor.

Fig. 3.16. GCR-3 Axial Peak-to-Average Power Ratio for a Four-Zone Core as a Function of the Radial Distance from the Center of the Reactor.

Fig. 3.17. GCR-3 Axial Peak-to-Average Power Ratio for a Five-Zone Core as a Function of the Radial Distance from the Center of the Reactor.

4. REACTOR VESSEL DESIGN

The conceptual designs and evaluations of a spherical and a cylindrical reactor vessel were prepared by Combustion Engineering, Inc.¹ The design of a vessel that could be economically fabricated in the field with present technology was assured with the aid from The Lummus Company, an affiliate of Combustion Engineering, Inc. Their contributions to the study are acknowledged. Specific requirements of the study included the following:

1. preparation of conceptual drawings of a spherical reactor vessel designed to satisfy ORNL requirements that it have an inside diameter of 52 ft 6 in., support a 1,900,000-lb core, and contain helium at 440 psig and 650°F,
2. preparation of conceptual drawings of a cylindrical reactor vessel designed to contain a reactor core of the same size as would be contained by the spherical vessel,
3. evaluation of feasible materials and determination of the optimum material for construction of the reactor vessels,
4. investigation of fabrication, shipping, and site erection limitations,
5. analysis of the designs to determine critical regions requiring additional investigations.

The design requirements were met by establishing criteria for material evaluation, structural sizing, and limitations on fabrication, shipping, and erection. The criteria established required sizing of the vessel components in accordance with Section VIII of the ASME Boiler and Pressure Vessel Code. Evaluation of materials was on the basis of physical properties, cost of material and fabrication, and effect of vessel cost on overall plant design. The basic premise used in establishing fabrication, shipping, and erection limitations was that the most economical construction results from the site work being restricted to a

¹G. E. St. Cin and T. M. Pierson, "Gas-Cooled Reactor Study," Report CE-1076, Combustion Engineering, Inc., 1962.

minimum. Therefore a fabrication procedure was proposed for constructing subassemblies in an area adjacent to the shop and shipping them to the erector by barge. Fabrication limitations were restrictions of weld sections to 5 1/2 in. and of subassembly weight to 300 tons. The design of the vessels required that fabrication tolerances be as liberal as possible. The tolerances established controlled the relative position of control rod nozzles to 1/16 in. of true position with relation to each other, restricted "run-out" of control rod extensions to 1/4 in. over their length, located nozzles other than control rod nozzles in the subassemblies within 1/4 in. or 0° 30' of true position, and required that vessel shell roundness and weld joint alignment comply with the ASME Code.

Combustion Engineering reached the following conclusions concerning the spherical and cylindrical design concepts: Technology in the United States today is adequate for construction of either the spherical or cylindrical vessel considered in this design with final assembly at the site. The optimum base material for construction of both design concepts is ASTM A212, grade B, carbon steel plate modified to satisfy A300 impact requirements. The 4-in.-thick spherical shell wall and the 5 3/8-in.-thick cylindrical shell wall do not exceed the thickness limitation of 5 1/2 in. on field welding of carbon steel. Maximum economics are realized by furnishing the erector with the largest possible subassemblies. Limitation of the shipping weight of any single piece to 300 tons requires four subassemblies for the spherical vessel and three subassemblies for the cylindrical vessel. The maximum subassembly weight for the spherical vessel is 232 tons, and for the cylindrical vessel is 267 tons. The spherical vessel concept allows greater freedom in pressure and/or diameter increases. Due to advantages in fabrication, handling, and shipping, plus reduction in field welding, the cylindrical vessel was recommended as the design for construction of a reactor vessel for the required parameters.

Vessel Design Parameters

The specifications provided for the design of a spherical vessel with an inside diameter of 52 ft 6 in. and a cylindrical reactor vessel with an

inside diameter of 34 ft 9 in., each subjected to an internal pressure of 440 psig. The coolant gas was specified as helium with an inlet temperature of 600°F and an outlet temperature of 1150°F; however, through the use of internal ducting and insulation, shell temperatures were limited so as to allow a design temperature of 650°F. Although design of the internal ducting and/or insulation and of the support grid for the 1,900,000-lb core was not included in Combustion Engineering's scope, provisions for the attachment of these structures are of necessity a part of the conceptual design. Other requirements were that the vessel be able to resist a horizontal acceleration (earthquake) of 0.05 g and the following openings be provided:

- four 60-in.-ID gas inlet nozzles
- four 66-in.-ID gas outlet nozzles
- seventy-three 8-in. control rod
nozzles on a 37.6-in. triangular
pitch
- one 14-in.-diam fuel inlet nozzle
- one 12-in.-diam fuel outlet nozzle
- two 30-in.-diam access nozzles

The reactor plant was specified to be located on a navigable stream in the United States.

Design Criteria

Since location of the plant site was not defined other than in the United States, local and/or state codes were not considered in the vessel design. However, design and fabrication procedures were established in accordance with Section VIII of the ASME Boiler and Pressure Vessel Code and applicable ASME Nuclear Code Cases 1270N, 1271N, and 1273N since adherence to these codes is mandatory for an operating license in virtually all states. Design of the vessel support systems was in accordance with the AISI Manual for Steel Construction as recommended by Nonmandatory Appendix G, Section VIII, ASME Boiler and Pressure Vessel Code.

Restrictions imposed on gas-cooled performance by practical considerations of the maximum-size vessel feasible for field erection required that cost of a material be evaluated on the basis of overall plant design in addition to the normal economic analysis of the vessel. Consequently, selection of the optimum material for a pressure vessel design was determined by the maximum-size vessel that could be constructed, the cost of material and fabrication of the vessel, and the advantages to be gained from the physical characteristics of the material.

In the past, field fabrication has been limited to the welding of thicknesses of 3 1/2 in. or less; however, investigations by The Lummus Company showed the feasibility of welding carbon steel up to 5 1/2 in. thick by automatic arc welding from both sides. Also, studies being conducted in the use of "inner shield guns," in which the flux forms a core for the filler wire, have shown excellent results for applications of this type. Consequently, it is felt that by using either of these methods, with carbon gouging² for backgrooving and a gamma source for radiography, welding of sections of carbon steel up to 5 1/2 in. is feasible.

In spite of the advances in welding technology, the difficulties and accompanying increase in cost inherent in field fabrication show the desirability of supplying the erector with the largest subassemblies possible. The size of section that can be handled by the erector is limited only by the crane capacity at the site. Restriction of the weight of the subassemblies to 200 tons provides an advantage to the erector since both unloading at the debarkation point and final erection can be accomplished with mobile cranes; however, economic advantages of the mobile cranes can easily be offset by the amount of welding required on vessels of the size included in this study. Therefore, the premise for subassembly size limitations used in this study is that the field erector can handle any section that can be shop-fabricated and shipped.

²Carbon gouging - "Removal of material by holding carbon electrode at very low angle to the base material and pushing it back and forth."
Ref: T. B. Jefferson, The Welding Encyclopedia, 13th Ed., Welding Engineer Publishing Co., Chicago, Ill., 1951.

These conclusions are at variance with experience in the United Kingdom,³ where it has become a practice to set up complete fabrication facilities at the site for construction of the large specialized components. Fabricators in the United States also may recommend doing more of the fabrication in the field, depending on their company experience. Consequently, although shop fabrication of the largest transportable subassemblies is assumed for this study, further investigation may show advantages for reducing the size of shipped pieces and doing more of the assembly at the site.

Specification of a plant site on a navigable stream allows shipping limitations to be determined from river barge capabilities. Limitations on barge shipment are dependent upon lock and overhead clearances, with weight a factor only in loading and unloading. Since a specific plant site had not been chosen, limitations for shipment were determined for clearances on the Tennessee and Mississippi Rivers between Watts Bar Lake, between Knoxville and Chattanooga, Tennessee, and New Orleans, Louisiana. Lock and bridge clearances on the route limit width to 60 ft 6 in. and elevation at normal pool levels to 57 ft.

The lifting capacity at the point of loading and unloading is assumed to be the same as at the site since the same type of equipment can be used at each point; then the maximum weight that can be shipped and erected is governed by the relative costs of equipment required versus the additional costs of field fabrication over shop fabrication. Since time did not allow a thorough investigation of these factors, the maximum weight that can be shipped and erected was arbitrarily set at 300 tons. This value was chosen because it imposes the need for three lifting installations but does not penalize the design with requirements for very elaborate facilities.

Limitations on barge shipment far exceed the capabilities of fabrication shops; however, even though fabrication methods are essentially the same as at the site, difficulties inherent in field construction can

³A. J. T. Clements, "Welding in a CO₂ Gas-Cooled Reactor Pressure Circuit," Brit. Welding J. 9(4):188-98, April 1962.

be minimized by fabrication of sections to shipping limitations in an area adjacent to the shops. The principal advantages in this method are the availability of experienced personnel at the shop, reduction in the number of qualified workers in the field, and possible economic advantages from utilizing facilities for vessels for other plant sites.

The design of the reactor vessels considered in this study is such that the need for restrictive tolerances is minimized. However, provisions of the ASME Code include requirements for vessel roundness and for alignment at weld joints. Also, although the design allows location of the core to fit the location of the control rod pattern, the position of the control rod nozzles relative to each other must be controlled. Although adjustment of connecting piping to fit the nozzles is more economical than requiring close tolerance on nozzle locations, the site work can be minimized by specification of loose tolerances in nozzle locations. Therefore, the following criteria for tolerances were established:

1. Tolerances on roundness and alignment of shell segments at weld joints shall satisfy requirements of paragraphs UG79-91, Section VIII, ASME Boiler and Pressure Vessel Code.
2. Control rod nozzles shall be located within $1/16$ in. of true position, as established from the center nozzle. Run-out of the control rod extensions is limited to $1/4$ in. over the length of the extension.
3. Nozzles located in the subassembly by coordinates shall be within $1/4$ in. of true position. Nozzles located in the subassembly by angular dimensions shall be within $0^{\circ} 30'$ of true position. No tolerances are required for nozzle locations after the vessel is assembled at the site.

Material Selection

Carbon steels have been universally accepted as the most suitable fabrication material for high-pressure gas containers in the United States. In gas-cooled reactor applications the preferred carbon steels have been ASTM A212, grade B, and A201, with the latter in a subordinate role, despite its better welding characteristics, because of its lower

strength. Numerous investigations have been carried on to evaluate these materials, the ORNL GCR-2 reactor vessel study⁴ being typical of the findings from such investigations. The conclusions of the GCR-2 study, which surveyed ASTM specifications A201 and A212B carbon steels, A203, A204, A225, A301 and A302 low-alloy steels, and T-1 quenched and tempered steels for such factors as weldability, brittle-fracture properties, neutron-irradiation damage, strain aging, vendor preference, and costs, were as follows:

1. The maximum thickness feasible for field welding in 1958 was 3 1/2 in.
2. Although thicker sections of carbon steel would be required than for some low-alloy steels, the weldability of the former offset this disadvantage in large vessel construction.
3. Notch toughness, low transition temperatures, and strain aging were no problem in low-alloy steels, and satisfactory performance with respect to these considerations in carbon steels could be obtained by fine-grain-forming practice as defined by ASTM specification A300.
4. The available information on radiation damage did not show any significant advantage for the low-alloy steels over low-carbon steels made to controlled specifications.
5. Field erectors would rather work with carbon steels than with low-alloy steels.
6. Although the required quantities of materials for low-alloy steels would be lower than for carbon steels, the fabrication costs per unit of weight overwhelmingly favored the carbon steels.

As a result of these conclusions A212B steel made to A300 specifications was selected as the optimum material for the GCR-2 vessel.

Since 1958 U.S. field fabricators, of which The Lummus Company is representative, have demonstrated the capability to weld steel sections up to 5 1/2 in. thick; this capability has been paralleled in the

⁴"The ORNL Gas-Cooled Reactor," USAEC Report ORNL-2500, Part II, Section 3.23, Oak Ridge National Laboratory, April 1, 1958.

United Kingdom, where the British Magnox reactor vessels have included spherical heads of comparable thickness (e.g., the Trawsfynydd reactor vessel) and of diameters greater than that of the GCR-3 vessel. There has been no appraisal of low-alloy, high-yield-strength, quenched-and-tempered steels since the time of the GCR-2 study. It was thought advisable to reconsider these latter materials in terms of more recent fabrication and metallurgical developments. U.S. Steel T-1, Great Lakes Steel N-A-XTRA 100, and Navy specification HY-80 steels are produced in plate thicknesses as great as 2 in. Figure 4.1 shows that for a steel such as HY-80 at GCR-3 conditions (pressure $\times$ radius = 140,000 lb/in.) the thickness is about 20% less than that of ASTM A212, grade B, carbon steel. Although HY-80 steel has been available in production quantities for only a relatively short time, it is reported to have better weldability and higher resistance to neutron-irradiation damage than USS T-1 steel and to be comparable in cost. A comparison of fabrication costs between HY-80 steel and A212B was therefore prepared.

A very optimistic viewpoint was taken toward field fabricability of HY-80 steel in spite of limited experience in this area, but its cost per unit of vessel surface area appears to be higher, by a factor of almost 2, than the cost of carbon steel. This higher cost results from the base cost being higher by a factor of at least 2 per unit of area (almost 3 on a weight basis), a reduction of 50% in the welding speed, a 20% increase in the forming capacity requirements because of the higher HY-80 strength at forming temperatures, and an approximately 40% decrease in the machining rate. This investigation shows that technological advancements in the use of the HY-80 material are still not sufficient to make it economically attractive at GCR-3 design conditions. No attempt was made in this study to capitalize on the higher temperature limits of the HY-80 material to eliminate internal insulation since the resulting cost improvement would not overcome the large cost difference. The conclusion was reached that ASTM A212, grade B, plate made to A300 requirements is still the most desirable material to be considered for this application. The technological status of carbon and low-alloy steel plate is discussed below.

The only major change from the conclusions drawn in the GCR-2 material evaluation program concerning the effect of advances in technology is that a thickness increase, from 3 1/2 in. to 5 1/2 in., in carbon steel is feasible for field welding. However, economic incentives for increasing reactor vessel size to the maximum practical for field construction necessitate a re-examination of costs as to the effect on overall plant design. Preliminary investigation shows that the greatest possibility of increasing the physical size of the vessel is to use the recently developed class of low-alloy, high-yield-strength, quenched and tempered steels. Commercial alloys of this class include T-1, N-A-XTRA 100, and HY-80. Only T-1 and HY-80 are presently produced in thicknesses greater than 2 in., and T-1, having been in production since 1952, offers an advantage in development of fabrication and welding procedures. Nonetheless, HY-80 was chosen, because of data obtained on neutron irradiation and results of investigations in welding thicknesses up to 4 in., as the material to be compared in cost with A212B carbon steel.

The results of the cost comparison between HY-80 and A212B are shown in Figs. 4.1 and 4.2. Even though extreme optimism was exercised in forecasting the ability to field-weld HY-80 plate in thicknesses up to 4 in., the maximum possible pressure-radius combination for a spherical vessel constructed of A212 steel is 193,000, whereas it is only 180,000 for HY-80. Figure 4.2 also shows that although use of HY-80 allows a reduction in required net section of a spherical vessel of approximately 25%, its base cost is roughly twice that of A212B steel. Consideration of fabrication costs is an additional reason for selecting A212B as the optimum material since limited data on HY-80 indicate its welding speed to be reduced approximately 50%, required forming capacity to be increased 20%, and rate of machining to be decreased roughly 40%.

Considerations of Brittle Fracture

Intensive investigations have been conducted over a period of several years by personnel of the Naval Research Laboratory on the problem of brittle fracture of vessels. As a result of this and related work, the concept of the nil-ductility transition temperature (NDT) for carbon and low-alloy steels was developed. This temperature is defined as that

Fig. 4.1. Material Comparison as to Pressure $\times$ Radius vs Thickness for Spherical Vessel. Thickness determined for design temperature of 650°C; allowable stress level for HY-80 assumed to be same as for USS T-1 per ASME Code Case 1208-4; weld joint factor = 1.0.

Fig. 4.2. Material Comparison as to Pressure $\times$ Radius vs Base Material Cost for Spherical Vessel. Based on vendor's price of \$0.087 and \$0.220 per pound for ASTM A212, grade B, steel and HY-80 steel, respectively.

temperature below which a steel loses all ability to deform in the presence of a sharp crack. It has been shown that below this temperature brittle fracture may be initiated by progressively larger flaws at stresses much lower than the design stress. However, there exists a lower stress limit, at about 5000 to 8000 psi, below which propagation of fracture is not possible, because the minimum amount of elastic strain energy release necessary for continued propagation of brittle fracture is not attained.

In addition to the concept of the NDT temperature, investigators at the Naval Research Laboratory have described a second temperature point, the fracture transition for elastic loading (FTE). This temperature is defined as that temperature below which a brittle fracture will propagate through elastically loaded regions. It has been determined to be 30 to 60°F above the NDT temperature for carbon and low-alloy steels, and is now arbitrarily set at $NDT + 60^{\circ}F$ for conservative design use. The recommended approach to fracture-safe design, as proposed by NRL and generally accepted by the industry, utilizes an analysis based on the presence of flaws that may grow to large size by fatigue or other environmental mechanisms. This criterion requires that full pressurization of the vessel be limited to temperatures above the FTE ($NDT + 60^{\circ}F$) temperature.⁵ An additional restriction required by this design basis is that at temperatures below the FTE temperature the maximum applied load, including internal pressure, shall be limited to 20% of the design value.⁶

Effects of Irradiation

The problems associated with radiation damage to the reactor vessel involve the above limitations on the operating procedures. It is well established that the NDT temperature of pressure vessel steels increases

⁵W. S. Pellini et al., "Analysis of Engineering and Basic Research Aspects of Neutron Embrittlement of Steels," paper presented at IAEA Symposium on Radiation Damage in Solids and Reactor Materials, Venice, Italy, May 7-11, 1962.

⁶"Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components (Pressurized, Water-Cooled Systems)," U.S. Department of Commerce, Technical Publication PB 151987 (Revised 1 December 1958).

significantly with irradiation, and comprehensive studies have shown that the increase in NDT temperature is probably independent of composition and is related directly to the integrated neutron dosage and the temperature of irradiation. Results of tests on these steels irradiated at low temperatures show substantial increases in the NDT temperature; as indicated in Fig. 4.3. The increase in the NDT temperature for integrated doses of approximately 10^{19} neutrons/cm² may be as much as 300°F.

However, at higher irradiation temperatures self-annealing effects become significant, as indicated in Fig. 4.4. The data for irradiations performed at 500 to 575°F show a shift, for a given dosage, of approximately 75 to 100°F less than for the low-temperature irradiations. The few data points at higher temperatures indicate that self-annealing effects are even more important. From these results it appears that self-annealing becomes significant at approximately 500°F and that successively higher temperatures of exposure result in a continuous increase in the self-annealing rate.

Postirradiation Annealing Studies

Recently, an attempt was also made to evaluate the effectiveness of postirradiation heat treatment for the recovery of fracture toughness. These studies have shown that recovery processes are quite complex; however, several conclusions derived from the available data may be summarized as follows:

1. Embrittlement developed by irradiation at low temperature (< 300°F) is annealed much more readily than that introduced by high-temperature irradiations (> 400°F).
2. The temperature levels required for the effective recovery of material irradiated at the higher temperatures associated with power reactor operations are so high that auxiliary heat sources would be necessary to accomplish effective annealing.
3. Extending the period of heat treatment from about 36 hr to 168 hr resulted in development of re-embrittlement in steels.

The practical application of annealing recovery must await information on the rate of embrittlement with dosage during second and

Fig. 4.3. Correlation of Transition Temperature Shifts with Total Neutron Exposure (> 1.0 Mev) of Materials Irradiated Under 200°F .

Fig. 4.4. Increase in the Nil-Ductility Transition Temperature Resulting from Irradiation at 500-575°F and 650-750°F as Compared with the < 450° Band.

subsequent irradiation cycles following annealing. This problem, plus the uncertainties in the effects and measurements of flux rate and neutron spectrum, emphasizes the need for an adequate surveillance program.

Effect on Design of GCR-3 Vessel

From a review of the available data it appears that irradiation embrittlement of the GCR-3 vessel will not be serious. Since the vessel will operate at 650°F, one is tempted to use the data from Fig. 4.4 for the 650 to 750°F irradiations. These data indicate that the increase in the NDT temperature would be insignificant for doses of approximately 5×10^{18} neutrons/cm², and would be no more than 30°F for a dose of 10^{19} neutrons/cm². If the initial NDT temperature of the ASTM A212, grade B, steel is 10°F, which is probable, then the final FTE temperature would be 10 + 30 + 60°F, or 100°F. This condition would not significantly complicate the operation of GCR-3.

On the other hand, the data for the higher temperatures were obtained from a single experiment on steel of a different composition; so a more conservative basis would be to use the data from irradiations at 500 to 575°F. In this case the increase in NDT temperature after an integrated neutron exposure of 5×10^{18} neutrons/cm² would be about 60°F, and the increase from an exposure of 10^{19} neutrons/cm² could be as much as 130°F. This latter case would result in a final FTE temperature of 200°F, and it would be necessary to maintain the vessel above that temperature when it was subjected to a stress greater than 5000 psi after it had been exposed to an integrated dose of 10^{18} neutrons/cm².

Reactor Vessel Design

The reactor vessel design is made complicated by the need for support of internals and of the vessel itself and for penetrations for coolant-gas flow, reactor control, and refueling operations. In addition, limitations of fabrication facilities and of field erection must be considered in all phases of the design. Calculations required by paragraphs UG27(d) and UG37-41 in Section VIII of the ASME Boiler

and Pressure Vessel Code for sizing of the vessel shells and nozzles were performed.⁷ The allowable stress value is 17,500 psi, and the joint factor for the fully radiographed and stress-relieved, full-penetration, double-butt welds used in the designs is 1.00. The ligament efficiency of 78.8% of the shell in the control rod penetration region is an approximation obtained by adapting provisions of paragraph UG53(d) for a cylinder to the spheres used in these designs.

Limitations on fabrication facilities are difficult to define due to variations in equipment. However, machining and handling problems are minimized as much as possible in the proposed design, thereby allowing modifications by the different fabricators to fit their specific facilities. Limitations on field erection are based on the premise, as discussed in "Design Criteria" in this chapter, that the most economical construction will result from minimizing fabrication to be performed at the site.

Spherical Vessel Design

The configuration of the 52 ft 6 in. inner diameter vessel design is shown in Fig. 4.5. The vessel shell is constructed of top- and bottom-cap segments and six shell courses, all formed of ASTM A212, grade B, carbon steel plate. The bottom cap and the four lower shell courses are 4 in. thick. The thickness of the top cap and of the two upper shell courses is increased to 5 1/4 in. to compensate for the reduction in shell efficiency in the control rod penetration region. Excessive shell deformations are avoided, and a base for core and vessel supports is provided by a 29-ft-diam support ring formed of A266, class II, forgings and located between the second- and third-shell courses from the bottom.

The vessel is designed to be fabricated in four sections, with each section to form the basis for a subassembly to be furnished the erector. The top dome section consists of the control rod penetration region and includes the transition from the 5 1/4-in. reinforced

⁷G. E. St. Cin and T. M. Pierson, "Gas-Cooled Reactor Study," Report CE-1076, Combustion Engineering, Inc., 1962.

section to the 4-in. shell section. The upper and lower shell courses of the top dome section are fabricated of 8 and 12 segments, respectively. The 29 ft 2 in. outer diameter of the top dome section allows machining of the openings for the 73 control rod nozzles and the fuel inlet nozzle on a Gray 7-in. heavy-duty boring, drilling, and milling machine; however, the proposed design provides for flame cutting of these openings if necessary. Also, this section is sized to allow radiography of welds and intermediate stress relief, if desired, with normal shop facilities.

The inlet nozzles are fabricated of 6-in.-thick A212B carbon steel plate and are normal to the vessel shell and 30° below the vessel equator. The size of the opening makes distribution of the required reinforcement into the nozzle not realistic, and therefore a 6-in.-thick, 92-in.-diam, A212B carbon steel reinforcement pad is inserted into the vessel shell at each of the nozzles. The outlet nozzles are fabricated of 7 3/8-in.-thick carbon steel plate and are located normal to the vessel shell and 45° above the vessel equator. The outlet nozzles extend inside the vessel 10 in., thereby providing a "bell" opening for the coolant and allowing the required reinforcement to be included in the nozzle.

All control rod nozzles are parallel to the vessel vertical axis and are constructed of A266, class II, carbon steel forgings. These nozzles are designed for the elliptical opening formed at the outermost penetration, thereby requiring a reinforced nozzle wall thickness of 2 1/2 in. The control rod drive mechanisms can be installed outside the radiation shield by means of A106B carbon steel pipe extensions on each nozzle.

The fuel inlet nozzle and the spent fuel discharge nozzle are fabricated of A266, class II, forgings. The 30-in. access nozzles are fabricated of 6-in.-thick carbon steel plate. Reinforcement of the elliptical hole formed by the "hillside" location of the access nozzles requires insertion of a 6-in.-thick pad at each nozzle. Fifteen-foot extensions closed with a welded membrane seal and a blind flange allow installation of remote tools and viewing equipment from outside the radiation shield.

In addition to the support of the vessel the support system design must provide for vessel expansion and a horizontal acceleration (earthquake) of 0.05 g. The magnitude of radial deflection (approx 7/8 in.) of the vessel shell at the support ring prevents rigid connection of the supports to the vessel shell. Consequently, the support system shown in Fig. 4.6 has been designed to give stability yet restrict the loads affecting the vessel shell to a minimum.

The primary members of the support system are sixteen 10-in. sched-160 carbon steel pipe columns equally spaced around the vessel shell and canted 10° from the vertical. The pipe columns are sized by rules specified in "Steel Construction Manual" of the American Institute of Steel Construction. Radial expansion of the vessel is allowed by providing circular rods on the brackets at the top and bottom of each pipe column. These circular rods nest in semicircular saddles on gussets attached to the support ring and to the base plate. This attachment reduces the bending loads in the columns and vessel shell to those resulting from the friction component acting on a moment arm equal to the radius of the rod. The horizontal component of the earthquake load is resisted by welding 1/2-in.-thick carbon steel shear web and 4-in. standard pipe between two pipe columns at 90° intervals around the vessel. Difficulties in machining the horizontal faces of the support ring are overcome by providing threaded connections between the pipe and the top bracket. These threaded connections allow adjustment of the column length at the site so as to provide equal load distribution among the columns.

In addition to providing an attachment point for the vessel support system, the 29-ft-diam support ring forms a base for support of the reactor core. However, critical tolerances on the elevation of the core support attachments plus difficulty in machining the large diameter ring require that the base for the core support grid be adjustable. This is accomplished by providing sixteen 2-in. screws with 4-in.-diam flat heads. By adjusting the elevation of the screw optically, the elevation of the core support grid base can be set after completion of vessel erection.

REVISED

PLAN VIEW ELEVATION VIEW "A-A" (C-C) VIEW "B-B" (C-C)

1. DIMENSIONS SHOWN ARE FOR STANDARD TEMP. OF 60°F.
 2. MATERIAL IS SA-212 B.
 3. DESIGN PRESSURE IS 440 PSI AND DESIGN TEMP. IS 650°F.
 4. ALL DIMENSIONS AND ANGLES ARE REFERENCE DIMENSIONS AND ARE NOT TO BE USED FOR FABRICATION. SEE FABRICATION DRAWINGS FOR DIMENSIONS AND TOLERANCES.

GENERAL NOTES

CONTRACT NO. 2662	
GCR 3 REACTOR STUDY	
SCALE 1/4" = 1'-0"	DATE 4-19-62
DRAWN BY PRICHARD	CHECKED BY
TRACED BY	APPROVED BY
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Fig. 4.6. Spherical Vessel Support System Assembly.

Supports for internal ducting and/or insulation consists of $3/8 \times 4$ -in.-long Nelson studs distributed over the inside of the area swept by outlet gas. These Nelson studs are spaced to accommodate 30-in. squares of insulation and/or ducting.

Cylindrical Vessel Design

The cylindrical vessel design, shown in Fig. 4.7, consists of a 34 ft 9 in. inner diameter cylindrical shell, $5 \frac{3}{8}$ in. thick and 27 ft 9 in. long, closed by a $3 \frac{3}{8}$ -in.-thick hemispherical head at the top and a $2 \frac{3}{4}$ -in.-thick hemispherical head at the bottom. This vessel is designed to be fabricated in three sections, each of which will form the basis of the subassembly to be furnished the erector. The top head section consists of the top hemispherical head and the upper shell course. The bottom head section includes the bottom head and the lower shell course. The third section is the intermediate shell course.

Except for adjustment of reinforcement for the changes in shell thickness, the cylindrical vessel nozzles are the same as for the spherical vessel. The configuration of the cylindrical vessel does not lend itself to installation of a support ring; so gussets are provided inside and outside the bottom head. Otherwise, the design of the support system is the same as for the spherical vessel.

Fabrication

As a result of the evaluation of materials discussed in "Materials Selection" in this chapter, ASTM A212, grade B, carbon steel plate, class II, forgings, and A106B seamless carbon steel pipe were selected as the optimum materials for construction of the vessels considered in this study. However, as was stipulated, use of carbon steel for nuclear vessel construction requires that ASTM specifications be supplemented with requirements for improved impact properties and for nondestructive tests needed to assure uniformity of quality throughout the material.

Normally, where elongation of the outside fibers is less than $3 \frac{1}{2}\%$, carbon steel plate is cold-formed; however, difficulties in cold forming the large sections required for these design concepts plus

Fig. 4.7. Cylindrical Reactor Vessel.

advantages in performing heat treatment in conjunction with the forming operation indicate the desirability of hot forming. Therefore, the description of the fabrication sequence is based on heat treating and hot forming of the plate sections by the fabricator.

Material Ordering

The desired impact properties can be obtained in the A212B carbon steel plate by specifying the additional requirements of A300. This supplemental specification requires average impact values of 15 ft-lb at -50°F as determined from specimens of the Charpy keyhole type, but more reliable results can be obtained by specifying impact values of 15 ft-lb at +10°F as determined from Charpy V-notch tests with type A specimens conforming to ASTM Designation E23-60T. It should be noted that even though the vessel fabricator performs the heat treatment the material-ordering data must include the process to be used and/or the design requirements, since acceptance of the material by the vessel fabricator is dependent upon plate-manufacturers' tests.

ASTM specifications for A266, class II, forgings and A106B pipe do not specify minimum impact properties or provide for heat treatment by the purchaser. Therefore, ordering of material to these specifications must include supplemental requirements for impact properties.

In addition to the appropriate ASTM specifications and heat treatment, the stock required for forming the carbon steel plate and for impact tests after heat treatment must be included in the material-ordering data. An additional thickness of approximately 1 in. and additional length and width equal to 2 1/2 times the plate thickness are required for forming. Material required for impact test specimens consists of a square piece of plate with sides equal to three times the plate thickness.

Requirements for nondestructive tests needed to ensure uniformity of quality throughout the material are most easily satisfied by specifying 100% ultrasonic testing of the material. Pulse-reflection equipment (Sperry U R reflectoscope or equivalent) is used, employing a single crystal and the longitudinal-beam technique at 2.25 Mc. Plate material is tested with a 1 1/8-in.-diam crystal; forgings are tested with a

1-in. square crystal, and drum or hollow forgings are tested with a 1-in. square crystal and the angle-beam technique. A suitable liquid couplant and a surface finish of 250- μ in. rms or better must be provided for all ultrasonic testing.

Ultrasonic testing is an extremely sensitive tool which reveals small inclusions, segregations, and inhomogeneities not harmful for service. Consequently, agreement between the material manufacturer and the fabricator on acceptance standards is often hard to obtain, and defects determined from ultrasonic tests must be substantiated by other methods of nondestructive testing, such as radiography, or by probing. Discontinuities in plate that exceed 1 1/8 in. in diameter and eliminate the end reflection shall be cause for rejection with defects. Forgings that result in a complete loss of back reflection and/or that produce indications exceeding in amplitude those from a calibration notch of 2 1/4 to 3% of the thickness shall be subject to rejection. Discontinuities in plates that reduce back reflection by 50% or more and in forgings that exceed 12 1/2% of the end reflection for individual indications or 5% of the end reflection for clusters require additional consideration and evaluation.

All surfaces should be inspected by magnetic-particle testing conforming to ASTM-E-109, with spacings greater than 8 in. allowed provided that the current is increased accordingly. Inspection of the plate surfaces should show no laps, fissures, cracks, or other linear defects, although defects that do not reduce the plate section below the minimum required may be removed and blended into the surrounding area.

Forming

Hot forming of the plate material requires heating the material to 1600°F and holding at that temperature for 4 hr. The rate of heating (°F/hr) is determined by dividing the forming temperature by the plate thickness. Forming of the long plate segments required for the intermediate shell sections will have to be performed in a series of operations; so in all probability the material will require reheating several times during the forming operation.

Heat Treatment

Heat treatment of the carbon steel plate can be performed in conjunction with the forming operation. After forming, the material is quenched in water to 1000°F and then tempered by heating to a minimum of 1150°F, holding at temperature for 1 hr per inch of thickness to allow thorough heating, and then furnace cooling to 600°F. Heat treatment of the forgings is accomplished in the same manner except that prior to the initial quench the forging must be heated to a temperature above the upper critical temperature and held for a time sufficient to ensure uniform temperature distribution.

At least two sets of Charpy V-notch type A specimens (three to a set) should be tested, and should be heat-treated as part of the material if possible; if not, the specimens are placed in the furnace with the material being treated. After heat treatment, the specimens are subjected to a simulated stress relief of 3 hr per inch of thickness (20 hr maximum) at 1150°F and then tested as prescribed above. The average impact values of the three Charpy V-notch specimens should be not less than 15 ft-lb at 10°F, with two specimens not less than 15 ft-lb and the third specimen at least 10 ft-lb.

Weld Preparations

The large diameter of the vessels considered in this study prevents machining of all shell weld preparations on the spherical vessel and the girth seams for the cylindrical vessel. In addition, the proposed sequence of fabrication requires welding of the nozzles into the shell segments prior to assembly of the shell subassemblies, thereby preventing realistic definition of tolerances in the final assembly. Therefore final location of pipe nozzles will be determined in the field, and the connecting piping will be adjusted to fit. Since provisions of the design allow alignment of the core with the control rod nozzles during installation, close-tolerance requirements apply only to those dimensions determining the position of the nozzles relative to each other. Therefore flame cutting of all weld preparations is specified even though final analysis of the design may show that the higher cost of machining the nozzle weld preparations and the cylindrical vessel long seams is offset

by the reduction in the needs for jigs and fixtures. In either case, it is felt that the tolerance requirements can be satisfied.

Restrictions on flame cutting of carbon steel are essentially the same as those for welding. For the thicknesses involved in this study, a minimum preheat of 250°F is required. Flame-cut edges must be thoroughly cleaned with wire brushes and all inclusions removed by chipping and grinding. The finished surfaces must be checked for laminations prior to welding.

Welding

All vessel shell welds and nozzle-to-shell welds are full-penetration welds through the vessel shell. Either the manual, shielded-metal-arc process with A233 carbon steel (class E7015) used as the filler material or the submerged-metal-arc process with a minimum preheat temperature of 250°F is prescribed for all welds. Backing rings are provided for all shop welds and are removed by back grooving (by carbon gouging). Field welds are designed with a land sufficiently wide to support the adjoining structure during erection. The first layer of weld, the back-grooved surface, and the finished surface of all welds are inspected by a magnetic-particle technique. Use of a joint factor of 1.0 in design of the vessel requires that all welds be radiographed in conformity to provisions of Section VIII of the ASME Boiler and Pressure Vessel Code and ASME Code Case 1273N.

Field Fabrication

Review of the vessel designs proposed in this study by The Lummus Company has assured feasibility of field erection. Fabrication in the field is minimized by furnishing the erector with four subassemblies for the spherical vessel or three subassemblies for the cylindrical vessel. The 232-ton maximum weight of the upper shell assembly for the spherical vessel and 267-ton maximum weight of the top head assembly of the cylindrical vessel are less than the limit (300 tons) assumed for this study. The fabrication concept followed in the designs provides for installation of the bottom dome assembly or the bottom head assembly for the cylindrical vessel on the vessel supports to form a base for field erection. The remaining sections are then stacked on this base and

welded with full-penetration welds. Back grooving is accomplished by carbon gouging, and a cobalt source is used for radiography. Preparations for welding pipes to the nozzles allows flame cutting for removal of the nozzle caps after hydrostatic testing. Machining at the site is limited to removal of the backing rings for the control rod nozzle extension by a portable boring bar. Final stress relief is accomplished after completion of all welding as prescribed in Section VIII of the ASME Code.

Proof Tests

Hydrostatic proof testing of the reactor vessel at $1\frac{1}{2}$ times the design pressure is required. In addition, use of helium as the coolant requires additional verification of the leak-tightness of the system.

Hydrostatic testing of the vessel is accomplished with a solution of 1 part Immuroil 1392, or equivalent, to 20 parts tap water. After the vessel is filled with water, the vents are closed, and the vessel is then pressurized to the required level. The vessel must maintain pressure for the time needed for an inspector to complete a visual inspection for leaks and gross deformations. Test temperature should be defined as at least 60°F above the nil-ductility temperature but should never exceed 200°F .

The vessel is leak-tested by being evacuated to at least $75\text{ }\mu\text{ Hg}$ absolute, with the pressure rise measured over a 4-hr period. Pressure in the vessel is continuously recorded, and acceptance is based on a pressure rise of $10\text{ }\mu/\text{hr}$, or less, when averaged over the 4-hr period. The vessel temperature must be recorded during the test so that pressure readings can be corrected for temperature variations.

Design Analysis

The scope of design analysis performed for this study was just enough for feasibility and cost of the two vessel concepts studied to be established. The criteria established for component sizing, while sufficient for conceptual design, did not include evaluation of the effects of operating conditions on vessel discontinuities. Also, the fabrication procedure chosen was based on the feasibility of the proposed method but did not include a complete economic evaluation of alternate

methods. Since complete evaluation of these problems was not made, certain problems required additional investigation, as given below.

Structural Problems

Design of a pressure vessel necessarily includes changes in section that, when subjected to operating loads, generate stresses significantly higher than the uniformly distributed stresses upon which the component sizing criteria were based. Although these stresses may be minimized by the use of generous fillets and tapered transition sections, the structural adequacy of the final design must be shown by a detailed evaluation of these regions.

Evaluation of the stresses in regions of discontinuities of shape requires determination of both internal and external moments and their effects on the deformation of the vessel. The applied moments may be determined directly by consideration of the load magnitude and the geometry at each point, but the internal moments are statically indeterminate. The magnitude of these statically indeterminate moments may be determined by considering that every point in a structure must have force equilibrium and continuity of deflections and rotations.

The procedure for calculating the statically indeterminate loads is to approximate the actual shell configuration as a series of structural elements, compute the displacements and rotations of each individual element under the applied loads, and then determine the forces and moments required to equate these deflections and rotations at the junctions of two elements.

The "Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components"⁸ is recommended for calculation and evaluation of stresses in the structure. In addition to presenting methods of calculation of stresses for both mechanical and thermal loads, the publication establishes criteria for evaluation of both steady-state and cyclic stresses.

⁸"Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components (Pressurized, Water-Cooled Systems)," U.S. Department of Commerce, Technical Publication PB 151987 (Revised 1 December 1958).

Regions of the spherical reactor vessel requiring detailed evaluation include the transition from the reinforced penetration area to the shell section, the shell-support ring junction, and the nozzles.

Regions of the cylindrical vessel requiring further evaluation include the junctions of the top and bottom heads with the cylindrical shell and the support attachments. The tapered transitions between the heads and the cylindrical shell minimize the effects of the discontinuity in this region. Consequently, other than consideration of nozzles, which is the same as for the spherical shell, structural problems requiring additional study are limited to evaluation of the effects of the support attachments.

In addition to structural discontinuities mentioned above, requirements of ASME Code Case 1273N limit the combined pressure membrane and steady-state thermal stresses in a nuclear vessel shell to 150% of the allowable stress value. Therefore the stresses generated by thermal gradients resulting from internal heat generation and the transition at the barrier between the inlet and outlet gas must be calculated for both designs.

Conclusions

Evaluation of the results of the vessel study and of the proposed design concepts resulted in the following conclusions:

1. Technology in the United States today is adequate for construction of a gas-cooled reactor vessel that satisfies the specified design parameters.
2. Fabrication of a vessel of the size considered in this study will require final assembly at the site.
3. The most economical construction of vessels of this type will result from furnishing the erector with the largest possible subassemblies.
4. The optimum base material for construction of a vessel of the size considered is ASTM A212, grade B, carbon steel modified to satisfy impact requirements of A300.
5. Field welding of carbon steel is feasible up to 5 1/2 in. thickness.

6. Tolerance requirements of Paragraph UG-79, Section VIII of the ASME Boiler and Pressure Vessel Code, pertaining to vessel roundness and alignment at joints can be satisfied with the proposed fabrication procedure.

7. Location of control rod nozzles can be maintained within $1/16$ in. of true position in the pattern.

8. Location of nozzles in the subassemblies, other than control rod nozzles, defined by coordinate dimensions can be maintained within $1/4$ in. of true position.

9. Nozzles located by angular dimensions can be maintained within $0^{\circ} 30'$ of true position.

10. Adjustment of piping to fit the final location of the nozzles is more economical than specification of more rigid tolerances or location of nozzles in the shell.

11. The maximum weight of any subassembly should be limited to 300 tons.

12. The vessel geometry may be either spherical or cylindrical.

13. The required shell thickness of the spherical vessel is 4 in., and that of the cylindrical vessel is $5 \frac{3}{8}$ in.

14. The feasibility of field welding of $5 \frac{1}{2}$ -in.-thick carbon steel allows the pressure-times-radius factor of 138,600 for the spherical vessel considered in the study to be increased to 193,000.

15. Advantages in fabrication, handling, and shipping, plus the ease with which head room can be increased, make it desirable to use the cylindrical concept for construction of the vessel considered.

16. An additional advantage of the cylindrical vessel concept is less welding at the site. Although the weld section for the cylindrical vessel is $5 \frac{3}{8}$ in. compared with 4 in. for the spherical vessel, the smaller circumference of the cylindrical vessel requires field depositing of only 32,000 lb of weld metal for the two girth seams, whereas approximately 43,000 lb of weld metal is required for the three girth seams of the spherical vessel.

17. An increase in handling capacity at the site from the 300-ton limit imposed by the design criteria to 356 tons would allow further reductions in site welding of the cylindrical vessel since the maximum section welded in the field could be reduced to a 3 3/8-in. thickness of the top head.

18. On the basis of conclusions 15 through 17 above, the cylindrical vessel design is recommended as the optimum geometry for construction of a vessel to satisfy requirements of this study. A comparison of the two concepts is given in Table 4.1.

Table 4.1. GCR-3 Reactor Vessel Comparison

Design conditions: 440 psig, 650°F

Material: ASTM A212, grade B,
carbon steel

Type of Vessel	Spherical	Cylindrical
Inside diameter, ft	52.5	34.75
Inside height, ft	52.5	62.5
Nominal wall thickness, in.	4	5 3/8
Top head thickness, in.	5 1/4	3 3/8
Bottom head thickness, in.	4	2 3/4
Total weight, tons	643	614
Cost, \$	3,750,000	3,550,000

5. FUEL-HANDLING SYSTEM

Design Concept

The GCR-3 fuel-handling system is composed of two separate parts operating simultaneously to replace spent fuel elements in the reactor with fresh fuel elements from the operating floor and to discard the spent elements into the storage pool. Part I of the system, the fuel-transfer machine (see Fig. 5.1), is designed to receive fresh fuel elements at the operating floor level, insert them in the fuel channels in the reactor core, exchange them with existing elements in the channels, discard the spent elements to the spent fuel discharge chute, exchange any fuel element in the reactor core with any other element in the core, insert, remove, and store itself, remove, replace, or store the upper biological shield plug, and remove and replace the lower biological shield plug. Part II of the system, the spent fuel equipment located below the reactor (see Fig. 5.2), removes the spent fuel elements from the spent fuel discharge chute, transfers them to the dry or wet storage area, and provides storage for the lower biological shield plug. It is estimated that 190 hr will be required to replace one-fourth of the fuel elements and relocate a second one-fourth. Estimates of the time required for refueling in two, three, four, and five equal-zone fuel management plans are given in Table 3.9.

Equipment Description

The fuel-transfer machine (in Fig. 5.1) is operated in a helium atmosphere at 75 psig and a maximum temperature of 300°F and by remote control through a 41-in.-ID nozzle in the top center of the reactor vessel above the reactor core. It will remove, relocate, or replace any 300-lb fuel element from any of the 1062 fuel channels within a 15-ft radius of the center line of the reactor core. Once this machine is located over a channel with two fuel elements in its magazine, it can complete the exchange of the elements in the magazine with the two in the channel without further radial or angular movement.

Fig. 5.1. Fuel-Transfer Machine.

Fig. 5.2. Spent Fuel Handling and Storage

The structural system of the machine is designed for a maximum deflection of the fuel magazine of 0.25 in. at a 15-ft radius with a total load of 3000 lb. The fuel magazine assembly consists of a cluster of four vertical aluminum pipes joined together by means of membrane plates, top and bottom. A section of steel mechanical tubing in the center of the cluster provides rigidity and permits horizontal rotation. This section also contains bearing housings for the hollow vertical steel shaft that connects the upper and lower housings of the assembly. A borescope inserted in the steel shaft, with proper lenses and operating in conjunction with a television camera, will give visual check on the orientation of the tube magazine in relation to the fuel channel in the core. An aluminum tube approximately 22 ft long located inside the inner shell of the post assembly guides the fresh fuel elements into the fuel magazine. The lower 6 ft of this tube is slotted to allow passage of the hoisting cable when the fuel machine is in an expanded position. The fuel elements are lowered into and raised from the reactor core by a pneumatically operated grapple attached to a wire cable from a drum hoist. All drives for the fuel machine are located on a shielded plate attached to the inner shell of the post.

The fuel-machine handling and storage can is used to feed fresh fuel elements into the fuel machine for placement in the reactor core; it also serves as a pressure seal during the refueling operation and as a fuel-machine storage container when the machine is not in use. An air lock in the lower 12 ft of the can is used for positioning fresh fuel elements to be lowered into the fuel machine by a cable and grapple. Pneumatically operated shear blocks support the fuel machine when in the storage container.

The shield-plug handling and storage can is used to remove, replace, and store the shield plug during operation of the fuel machine. It consists of a 48-in.-ID vertical steel shell approximately 20 ft in length, flanged at the open lower end, and closed at the upper end with 12 in. of shielding. A double-reel cable drum of 20-ton capacity in the upper end is used to raise and lower the shield plug.

The storage-can exchange gate valve is used whenever the plug is to be removed for use of either the fuel machine or some future piece of equipment and serves as a shielding and pressure seal during plug removal and replacement.

The fuel-transfer chain, which is used to transfer the spent fuel elements from beneath the discharge chute to the hoist chute, a distance of approximately 50 ft, consists of an endless chain operating on a horizontal track and actuated by a pneumatic cylinder with a 5-ft stroke and controlled from the operator's console on the fresh-fuel-handling machine. Attached to the outside of the chain are twenty 7-in.-ID steel rings equipped with supporting rollers 180° from the point of attachment to the chain. These rollers run on a track outside the chain track and concentric with it, thereby maintaining the rings in a horizontal position.

The plug storage can is made of stainless steel and is used to transfer the bottom biological shield plug from the spent fuel chute to the plug air lock in the canning machine area for storage during the refueling operation. This can is designed to seal against the valve at the bottom of the spent fuel chute for receiving the plug and to remain sealed during reactor operation, thereby fulfilling the secondary containment requirement. A special ring located in the fuel-transfer chain will be used to hold this can for transfer.

The spent fuel cans are used in conjunction with the fuel-transfer chain to transfer the fuel from the discharge chute to the spent fuel canning machine. They are composed of 18-gage aluminum of 7-in. OD and 10 ft 7 in. length, with the lower end closed. The upper 2 in. of the can is expanded to an 8-in. OD, which creates a shoulder to support the can in the 7-in.-ID hole in the rings of the transfer chain. The top edge of the 8-in. section of the can will be finished flat in a plane perpendicular to the center line of the can; this surface will be sealed against the unloading valve on the bottom of the discharge chute when receiving spent fuel elements. One can will be required for each fuel element assembly.

Two elevating jacks are required in this system: one is under the spent fuel discharge chute and will be operated from the control console

on the fuel-transfer-machine storage can; the other is under the canning room chute and will be operated from outside the canning machine enclosure. Each jack has a stroke of approximately 8 in. and is pneumatically operated. The jack under the discharge chute serves to seal the can to the valve, and will have a lock to hold it in the raised position during reactor operation. The jack under the canning room chute is used to raise the shoulder on the can clear of the transfer chain rings in order for the hoist grapple to lift the can at the shoulder.

A 1-ton transfer bridge crane is used to hoist the fuel element and its can, place it in the canning machine, place the canned fuel element in the chute to the storage pool, and transfer empty fuel cans from the transfer air lock to the rings in the fuel-transfer chain. This crane is remotely operated from outside the canning machine area. A television camera at the lower end of the canning room fuel chute assists the operator manipulating the hoist and grapple on the crane.

The canned-fuel storage chute will receive the canned fuel, which, under gravity, will slide down the chute to the storage pool. The bottom of the chute will be curved to receive the vertical elements and turn them horizontal and reduce their velocity from a maximum at the top of the chute to zero at the bottom. Since this chute will be connected to the storage pool, its water level will be the same as that in the pool.

A pendant-controlled, 2-ton, storage-pool bridge crane will be used to stack the canned-fuel elements in racks within the pool and to load elements into shielded carriers for removal. Racks in the storage pool will be designed to space stored elements in noncritical array.

The system control console will be attached to the fuel-machine storage container at operating floor level and will be electrically connected to machine-position-indicating devices in or on the machine or in the storage can. The console will have devices for indicating and controlling the following:

1. all positions of the fuel magazine,
2. load on fuel element cable hoist,
3. position of fuel element grapple,
4. load in fuel magazine tubes,

5. position of fuel-magazine tube gate valve,
6. two closed-circuit television screens,
7. position of fuel element support valve,
8. position of fuel-transfer chain,
9. position of elevating jacks.

Materials of Construction

The construction materials of the fuel-handling system and their ASTM specification number are given below:

Structural steel	A212
Steel pipe	A106
Flanges	A105
Forgings	A235
Threaded rods	A286
Shafting	A293
Gears	AISI-4140
Fuel magazine and guide tubes	AL-1100-0
Main rotational bearings	
Vertical thrust	Loose steel balls
Radial	Steel rollers

All other bearings below the drive plate in the post will be of sleeve type, with aluminum oxide (99% Al_2O_3) bushings.

6. CONTROL ROD DRIVES

The control-rod-drive design developed for the EGCR or, alternatively, a version comparable to that used in the Advanced Gas-Cooled Reactor (AGR) is applicable to the GCR-3 with relatively minor modifications and some simplifications in the basic design.

From studies of the reactor system arrangement a cable-supported control rod was shown to have the following advantages:

1. The amount of head room required in the reactor building is limited to that needed for the drive mechanism, with some minor additional space required for shielding and for storing the rod itself when withdrawn from the core.
2. The cable requires no bearings or alignment equipment between the drive and the control rod channel in the core.
3. A very simple shroud will protect the cable from any gas flow disturbances.

The requirements of the EGCR drives may be simplified for the GCR-3 in the following manner:

1. The limitations on physical dimensions will be relaxed by the drives being located in a blister or nozzle above the control rod channel. With more space provided for the drive mechanism, cable drums of larger dimensions, larger motors, and bulkier gearing will be permitted.
2. Cooling arrangements, although shown in Fig. 7.1 as similar to those in the EGCR, can be external to the primary coolant system, and temperatures can be better suited to the mechanism. Hydrocarbon lubricants can be tolerated to reduce frictional problems in the rotating components.
3. A low-frequency drive motor can be provided to eliminate the complex reduction gearing between the cable drum and the motor.

No attempt has been made to carry the design investigations beyond this basic evaluation of the design criteria. Verification of the EGCR arrangement will provide all the essential information needed for execution of the GCR-3 concept.

An important consideration in the arrangement of the rods and their drives is that the drive cables must be uncoupled from the rods so that the mechanism of the fuel charge machine will have free access to the fuel channels. A number of coupling arrangements were considered, including pneumatic and weight-actuated latches and a clevice-type connector which could be operated by the reactor fueling machine. A device that has already been developed by the British for its Magnox reactors is a ball latch mechanism actuated by differential pressure across a bellows. This feature has been found sufficiently reliable to be incorporated in the safety system of the British plants under construction and may be adaptable to the GCR-3 design. No specific arrangement was selected, but all of the above appear to be usable and inexpensive.

An important feature in assuring the reliability of cable-driven rods is a suitable shock absorber to prevent damage to the rods if they are accidentally dropped through failure of the cable or the rod attachment. Since the rods will operate in their own graphite channels, the shock absorber can be conveniently located at the bottom of the channel. A number of shock absorber devices have been developed for this application. A most attractive concept is that used in the Dragon design in which a predeformed tube was shown experimentally to deflect so as to absorb the rod's deceleration energy without damage to the rod itself. An alternate concept is a compressible metal cushion at the bottom of the control channel, which would permanently deform under the rod and would require replacement. Again, no detailed studies of this design feature were carried out, but a number of relatively simple arrangements are available, depending upon the ultimate needs of the rods.

The drive mechanism in the EGCR is shielded from neutron damage by means of a shield plug in the control rod nozzle below the mechanism; the control rod cable is threaded through the shield plug. The arrangement planned for the GCR-3 is similar but should be considerably simpler because the diameter of the nozzle is smaller and all major maintenance of the control rod drives will be performed with the reactor shut down and depressurized.

The GCR-3 core structure is arranged so that cooling of the control rod will be independent of that of the rod drive. A small quantity of coolant gas can be directed from the bottom of the reactor up through the control rod channel to provide rod cooling, which will eliminate the need for accurate control of the coolant flow for the rod drive mechanism. Cooling air external to the drive blister will probably provide sufficient cooling to keep the mechanism temperatures within the limits required by the bearings, electrical switches and windings, and mechanism lubricants, although a spare cooler within the drive blister could be provided.

7. GAS SYSTEMS

Primary Coolant Loops

The helium coolant for the GCR-3 is circulated in four primary loops as shown in the flow diagram, Fig. 7.1. Each loop contains one steam-turbine-driven gas circulator, one steam generator, and reactor inlet and outlet block valves, and is designed to remove 500 Mw of heat during normal operation. On two loops at the reactor side of the block valves is a shutdown cooler loop that consists of an atmospheric-pressure steam-generating heat exchanger or of a cooler with inlet and outlet block valves and designed to remove half of the decay heat starting 15 sec after reactor shutdown.

Helium passes through the reactor vessel top plenum at the rate of 9,100,000 lb/hr at 422 psia and 1150°F; 2,275,000 lb/hr flows through the respective primary loop hot legs and enters the shell side of the steam generators. Leaving the steam generator, the helium is at 414.7 psia and 550°F at the suction of the gas circulators. The helium enters the cold-leg piping at 439 psia and 577°F. The reactor vessel receives the gas through four nozzles from which the flow is upward through the lower plenum and the core support structure prior to passing into the core and to the reactor top plenum. The inlet and outlet block valves provide isolation of loops during normal startup and shutdown and when a gas circulator is out of service. The type of major maintenance that would require personnel to enter the gas circuit is not intended when other loops are in service and the reactor is at power. A bypass line is provided in each primary loop so that each gas circulator can be operated independently of the others during startup and shutdown.

A side stream of helium (5720 lb/hr) is taken from the primary loops at the four outlet lines from the reactor for chemical purification in the helium purification system. When it is necessary to reduce pressure in the primary system or to remove gas for any other reason, the gas is pumped into containers for storage. Oil-buffered running seals on each circulator contribute 1.6 lb of helium per second per circulator to the

recovery system. This flow is passed through an oil-removal system and returned to the coolant system at the control rod nozzles on the top of the reactor vessel to provide purge and cooling of the drive mechanisms.

Metal-to-metal seats are employed in the block valves, and absolutely leak-tight shutoff is not expected to be achieved. However, the valves should be leak-tight enough to permit continued operation of the reactor on the remaining loops when any one or two loops have to be shut down. Stem leakage from all valves is controlled and recovered at low-pressure leakoff chambers.

Seventeen thousand pounds [1.5×10^6 ft³ (STP)] of Grade A helium is required to fill the primary coolant system.

Shutdown and Emergency Cooling System

Description

When the reactor is shut down the core may be cooled in one of several ways:

1. By normal operation of one primary loop, generating steam in the steam generator at whatever pressure and temperature is possible and using the steam to drive the blower turbine and a feed pump turbine, with the excess dumped into the main condenser and the deaerating heater. The gas pressure can be decreased to compensate for the reduced power generation rate. At 1 psig (15.7 psia) one loop operating at normal volume flow rate can remove the heat generated 3 hr after shutdown with a temperature rise across the core of only 382°F.

2. By operation of one loop, with steam from the house boilers used to drive the blower and one boiler feed pump and with the feed pump used as a circulator to pump water through the steam generators, flash tanks, and main condensers with or without any of it being converted to steam.

3. By natural convection with the low-pressure evaporative coolers provided. This method is dependent only on an adequate supply of distilled water being fed into the cooler to keep up with the evaporation rate at about 3 hr after shutdown. Head tanks on the roof of the heater

bay connected to the coolers will be kept filled with 50,000 gal of water to provide for the first 3 to 5 hr of decay heat; then battery-driven pumps will be used to pump distilled water from the main storage tank and the deaerator at a rate sufficient to keep up with the heat generation. Steam generated in the cooler can be condensed in the main condenser or vented to atmosphere.

The steam required for single-loop operation with the gas circulator operating at 3000 rpm and 633 lb/sec is estimated to be 90,000 lb/hr. The flow of steam required for the boiler feed pump turbine for 25% capacity is 50,000 lb/hr. Since condensate deaeration is also required during this period, the remaining steam supply is available for this function. This is adequate for over 300,000 lb/hr feedwater flow when the requirements are 150,000 lb/hr and 100% cold-feed-water makeup.

Analysis

The results of preliminary estimates of the effectiveness of natural circulation for removal of the afterheat in GCR-3 are shown in Fig. 7.2 as percent of design power removable by natural-circulation flow vs core outlet temperature at several system pressure levels. The height of the emergency heat exchanger above the core center line was assumed to be 80 ft, and the core inlet temperature was held constant at 400°F. The value used for the design system pressure drop was 24 psia. It can be seen in Fig. 7.2 that at the design pressure level the afterheat generated at 30 sec after reactor shutdown may be removed with a core outlet temperature of less than 1000°F. The afterheat generated after one day may be removed at an outlet gas temperature of 1150°F by a pressure level of only 33 psia.

The derivation of the equations used in preparing Fig. 7.2 is summarized below:

$$\%Q = \frac{W_2 c_p \delta t_2}{W_1 c_p \delta t_1} \times 100, \quad (1)$$

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Fig. 7.2. GCR-3 Natural-Circulation Cooling.

where Q = heat removed, W = flow rate, δt = core temperature rise, and subscripts 1 and 2 refer to design value and natural-circulation value, respectively. It is assumed that

$$W_2 = W_1 \left(\frac{\Delta P_2 \rho_2}{\Delta P_1 \rho_1} \right)^{1/2} \quad (2)$$

or, for same gas, that

$$W_2 = W_1 \left(\frac{\Delta P_2}{\Delta P_1} \frac{P_2 T_1}{P_1 T_2} \right)^{1/2}, \quad (3)$$

$$\Delta P_2 = H(\rho_c - \rho_h), \quad (4)$$

where H represents the height of heat sink above heat source, subscript c refers to the cold leg, and subscript h refers to the hot leg; or

$$\Delta P = \frac{MP_2}{R} H \left(\frac{1}{T_c} - \frac{1}{T_h} \right). \quad (5)$$

Substituting Eqs. (2) or (3) and (4) or (5) into Eq. (1) gives

$$\%Q = P_2 \frac{\delta t_2}{\delta t_1} \left[\frac{MHT_1}{R\Delta P_1 P_1} \frac{\frac{1}{T_c} - \frac{1}{T_c + \delta t_2}}{\frac{1}{T_c} + \frac{\delta t_2}{2}} \right]^{1/2} \times 100. \quad (6)$$

It should be noted that Eq. (2) assumes the system pressure drop to be due to frictional effects only and the friction factors to be equal for the two conditions. Since in the actual cooler loops the total flow area is only half that of the four main loops and the length is approximately 50% greater, this assumption introduces an error of approximately 15% in the heat-removal capacity of the system as given by Fig. 7.2.

Values on the curves should be reduced by 15%.

Helium Purification Systems

Chemical Purification System

A chemical purification system is required to remove from the circulating helium the impurities that could remove graphite from the core, deposit carbon in the cool portions of the system, or corrode the materials of construction. Materials that may be objectionable from this standpoint include O_2 , CO , CO_2 , H_2 , and H_2O .

A significant quantity of contaminants [approximately 16.67 cc (STP) per 100 g of graphite] will be released from the graphite at start-up as a result of air coming in contact with the graphite during shutdown. In normal operation the principal source of impurities will be water in-leakage from the heat exchanger, which will cause graphite removal from the core by the reactions

Deposition of soot in the cool portions of the system will occur by

The impurity concentrations and/or in-leakage rates are limited by graphite burnout, soot deposition, and corrosion. It is accepted practice to design on the basis of 1% graphite burnout during the useful life of the reactor. For the present case, the burnout permitted is $10,310/1500 = 6.873$ lb/day, or 0.573 lb-mole/day based on 1,031,000 lb of graphite in the core (excluding the reflector) and a useful life of five years (1500 days). All H_2O entering the system is presumed to remove graphite from the core by reaction (1), and circulating CO_2 presumed to remove graphite by reaction (3). The water-graphite reaction is presumed to occur instantaneously. The CO_2 -graphite reaction proceeds at a rate of 1.25×10^{-5} lb-mole day⁻¹ ft⁻² (atm of CO_2)⁻¹

at the average core temperature, based on extrapolation of high-temperature data.¹ Based on the reported rate data, soot deposition occurs at a rate of 4.09×10^{-4} lb-mole day⁻¹ ft⁻² (atm of CO)⁻¹. The design corrosion allowances were based on the following impurity content:

<u>Component</u>	<u>ppm by vol</u>
CO ₂	650
CO	450
H ₂ O	10
H ₂	50

In analyzing the chemical purification system, it was assumed that the water-graphite reaction is instantaneous and produces CO and that the efficiencies of the gas cleanup system components are independent of the composition of the gas entering a given component. Then, from material-balance considerations alone, a system of equations was derived for describing the impurity concentration in the helium:

$$X_{H_2} = \frac{I}{L + PE_{A(H_2O)} E_o(H_2)} , \quad (5)$$

$$X_{CO} = \frac{R_{CO_2} X_{CO_2} + I}{PE_{o(CO)} + 2R_{CO} + L} , \quad (6)$$

$$\frac{X_{CO}}{X_{CO_2}} = \frac{R_{CO_2} + L + PE_{A(CO)_2}}{R_{CO} + PE_{o(CO)} [1 - E_A(CO_2)]} , \quad (7)$$

$$G = R_{CO_2} X_{CO_2} + I , \quad (8)$$

¹J. E. Antill and K. A. Peakall, "Thermal Reactions of Gaseous Impurities in Flowing Argon Leading to Carbon Transport," pp 267-90 in Proceedings of the US/UK Meeting on the Compatibility Problems of Gas-Cooled Reactors held at Oak Ridge National Laboratory, February 24-26, 1960, USAEC Report TID-7597, Book 1, March 3, 1961.

where

X = mole fraction,

I = water in-leakage, lb-mole/day,

L = helium leakage, lb-mole/day

= 2.2,

P = flow to gas purification system, lb-mole/day

= 34,000,

R_{CO_2} = rate of reaction $CO_2 + C \rightarrow 2CO$ per unit mole fraction of CO_2 , lb-mole day⁻¹ (mole fraction of CO_2)⁻¹

= 51.2,

R_{CO} = rate of reaction $2CO \rightarrow CO_2 + C$, lb-mole of C day⁻¹ (mole fraction of CO)⁻¹

= 5362.8,

$E_o(CO)$ = efficiency of oxidizer for reaction $CO + CuO \rightarrow CO_2 + Cu$

= 0.5 (design),

$E_o(H_2)$ = efficiency of oxidizer for reaction $H_2 + CuO \rightarrow H_2O + Cu$

= 0.75 (assumed),

$E_A(CO_2)$ = efficiency of adsorber for removing CO_2

= 0.5 (assumed),

$E_A(H_2O)$ = efficiency of adsorber for removing H_2O

= 0.9 (design),

G = graphite burnout, lb-mole/day

= 0.573.

Solving the equations we obtain

$$X_{CO_2} = 1.66 \times 10^{-5},$$

$$X_{CO} = 2.06 \times 10^{-5},$$

$$X_{H_2} = 2.48 \times 10^{-5},$$

$$I = 0.57215 \text{ lb-mole/day},$$

$$\text{Soot deposition} = 0.1105 \text{ lb-mole/day}.$$

For the postulated system the impurity levels will, obviously, be very low, even at the maximum in-leakage rate. The equations also show that the circulating hydrogen concentration will be a measure of the water in-leakage rate. Furthermore, the H_2 , CO, and CO_2 concentrations will maintain a fixed ratio to each other, this ratio being dependent on the relative efficiency of the purification system for handling given components (see Fig. 7.3).

Equations for the non-steady-state behavior of the system have also been written and solved. The solutions show that if the gas evolved from the graphite on startup is presumed to enter the coolant system instantaneously, the concentrations of impurities will be reduced to their steady-state values in approximately one day (see Fig. 7.4).

The recommended purification system consists of two parallel CuO beds for oxidizing CO and H_2 , a cooler, and two parallel molecular-sieve beds for adsorbing H_2O and CO_2 . The CuO beds are 2 ft in diameter by 3.4 ft in length and contain 1363 lb of CuO each. The adsorber beds are 3 1/2 ft in diameter by 14 ft in length and contain 5920 lb of molecular-sieve material each; one set of beds would normally operate over a 24-hr period while the second is being regenerated with hot air. The cooler is designed to cool 5720 lb of helium per hour from 1100°F to 100°F. Auxiliary equipment includes a regenerating air blower, regenerating air pre-heaters, and purified helium compressor.

The design basis is a bed that will oxidize 50% of the CO reaching the bed to CO_2 , with the efficiency for H_2 oxidation not specified but which should be higher than for CO. The adsorber bed is to be 90% efficient in removing H_2O and somewhat less efficient in removing CO_2 . At the design flow, very low impurity levels can be easily maintained.

The purification system processes all the helium needed for control rod nozzle cooling. Design data for this system are given in Table 7.1.

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Fig. 7.3. Effect of Water In-Leakage Rate on Graphite Burnup Rate and on Levels of Gas Impurities.

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Fig. 7.4. Concentration of Impurities vs Time.

Table 7.1. Design Data for Chemical Purification System

Gas Flow Rates, lb-moles/hr	
Helium side stream	1430
Air for regeneration of CuO bed	58
Air for purging adsorber bed	58
Pressure Drops, psi	
Helium control valve, assumed	12
Air control valve, assumed	3
Total pressure drop	
In helium stream	23.8
In oxidizer regeneration air stream	4.56
In adsorber purging air stream	4.36
Steady-State Concentrations of Impurities	
Assumed efficiencies of equipment, %	
Oxidizer on CO	50
Oxidizer on H ₂	75
Adsorber on CO ₂	50
Adsorber on H ₂ O	90
Maximum permissible water leak rate, lb/day	12.65
Steady-state concentrations, ppm	
H ₂	30.4
CO	49.4
CO ₂	25.0
Graphite burnup rate per 1000 days, %	1
Equipment specifications and operating conditions	
Two CuO bed oxidizers	
Weight of CuO needed, lb/day	1363
Tower diameter, ft	2
Tower height excluding clearance, ft	3.4
Semicycle time, hr	12
Pressure drop, helium stream, psi	8.11
Pressure drop, air for regeneration, psi	3.4

Table 7.1. (continued)

One heat exchanger (four-pass shell-and-tube; gas flowing inside tubes)

Total number of tubes	136
Length of each tube, ft	10.6
Outside diameter of each tube, in.	1.0
Tube gage	No. 12 BWG
Arrangement of tubes	1.25-in. triangular pitch
Total outside cooling area, ft ²	378
Inlet gas temperature, °F	1150
Outlet gas temperature, °F	100
Assumed inlet water temperature, °F	75
Water flow rate, lb/hr	99,400
Assumed gas fouling coefficient, Btu hr ⁻¹ ft ⁻² (°F) ⁻¹	300
Assumed water fouling coefficient, Btu hr ⁻¹ ft ⁻² (°F) ⁻¹	1000
Overall heat transfer coefficient, Btu hr ⁻¹ ft ⁻² (°F) ⁻¹	74.5
Inside pressure drop (outside area), psi	2.21
Outside pressure drop, psi	1.8

Two adsorbers

Weight of molecular sieve needed per adsorber, lb	5920
Type of molecular sieve	Linde Co. 5A
Diameter of molecular sieve, in.	1/8
Height of pellets, in.	1/8
Height of tower, ft	14.3
Semicycle time, hr	12
Pressure drop of helium stream, psi	1.47
Pressure drop of air stream for purging, psi	0.64

123-kw strip heaters

Assumed heating loss after insulating, %	5
Diameter of sched-40 pipe, in.	4
Pipe length, ft	48.8

Table 7.1. (continued)

Air outlet temperature, °F	1000
Overall heat transfer coefficient based on inside area, $\text{Btu hr}^{-1} \text{ft}^{-2} (\text{°F})^{-1}$	14
Pressure drop, psi	0.25
54-kw strip heater	
Assumed heat losses, %	5
Diameter of sched-40 pipe, in.	4
Pipe length, ft	14.1
Overall heat transfer coefficient based on inside area, $\text{Btu hr}^{-1} \text{ft}^{-2} (\text{°F})^{-1}$	14
Air outlet temperature, °F	500
Pressure drop, psi	0.05

Oil-Removal System

Some degree of oil contamination is expected to occur from the oil used as the buffering fluid in the gas circulators. The design consideration is that the proposed system reduce oil concentration to approximately 0.2 ppm by volume as methane. The helium flows into a mechanical separator at the rate of 1.6 lb/sec, together with 4.5 gal/min of DTE-797 lubrication oil from each gas circulator, making the total flow rate for helium containing oil 23,040 lb/hr. It was found that if a perfect mechanical separation was effected, the concentration of the oil left in the helium stream as a result of its vapor pressure would amount to only 0.077 ppm by volume at 200°F; with a molecular weight of 350 assumed for the oil, this corresponds to a concentration of 6.7 ppm by weight. Hence it is not necessary to design equipment to remove the oil vapor from the helium stream. A 99.997% efficient mechanical cleanup system to remove entrained oil droplets is necessary, however, to reduce the concentration of oil physically dispersed in the helium to 0.1 ppm (molar) or 8.8 ppm (weight), which is approximately the amount present due to saturation at 200°F. The purified gas is returned to the primary coolant system. The

cleanup system (Fig. 7.1) consists of a cooler, knockout drum, compressor, demisters, adsorbers, and filters. Development work will be required to demonstrate the capability of such a design.

The radioactivity level of the main coolant gas is not expected to be high enough to necessitate shielding of the chemical purification system. If the fuel cladding should fail, the gas will be pumped through absolute filters and charcoal adsorbers for removal of fission products before being compressed into storage tanks. (This system has not been designed.)

Transfer and Storage System

The transfer and storage system (Fig. 7.1) is designed to pressurize the reactor or its auxiliaries and recover excess helium, transfer it to the storage area, and unload it (only helium free of radioactivity is considered for this system). A study of various system pressure conditions indicated a pressure of 565 psia to be desirable.

The equipment consists of precoolers, compressors, intercoolers, aftercoolers, piping, valves, and storage containers. A single-tier rack of 13 pipe containers serves as the storage facility, with 5 arranged for storage of 1,500,000 ft³ (STP) of recovered helium and the remainder arranged for storage of the gas as-received but not limited to this service. Each pipe tube connects through valves to a common header, is 330 ft long, and consists of 24-in. sched-40, ASTM A106 material. All-welded fabrication with radiographic inspection is used. The maximum operating temperature considered is 150°F.

Gas is transferred from the primary coolant system to the storage system after being cooled to 110°F. Forty percent of the coolant gas is transferred through a bypass around the compressor plant by allowing the pressures in the two systems to become equal. The equilibrium is 240 psia. After the pressure is equalized, the bypass is closed and the remainder of the gas is pumped from the reactor to the storage containers by the compressors. Gas is returned to the reactor by equalizing the pressure prior to operating the compressors. A constant pressure of 20 psia is

maintained by pressure-reducing valves at the compressor suction to simplify equipment control. The equipment is sized to permit the primary system to be vented and recharged in 6 hr, with 2 hr allowed for equalizing the pressure between the systems and 4 hr allowed for the compressor operation.

Gas Venting System (Fig. 7.1)

Normal Venting

The normal discharge of reactor helium coolant gas and any other coolant and purge gas is routed through piping and particulate- and iodine-removal filters to the off-gas stack. Suitable interlock and control valves prevent transfer of gas between systems and provide for control of the venting rate to limit plant and public exposures to activity release. The stack effluent is continuously monitored, and flow is automatically stopped when the effluent reaches a dangerous level of radioactivity.

The largest flow from the reactor coolant system would occur if the gas were so contaminated that it could not be stored during shutdown periods but had to be manually vented. Manual venting of auxiliary service systems such as the service machine, miscellaneous vessels, and purging operations, however, constitutes a very minor flow. During evacuation the vacuum pump discharges air and other gases through a knockout drum and then to the filter system.

Reactor containment manual venting is also routed and regulated in a manner similar to that described for the other systems, although it is not illustrated on Fig. 7.1.

Pressure-Relief Venting

Any gas discharged from pressure-relieving devices is routed through a separate filter system for particulate and iodine removal. The main coolant safety valves constitute the source for the maximum

possible discharge flow, which may be a steam-helium mixture in the case of failure of an internal feedwater line in the steam generators. Simultaneous discharge is minimized by staggering the relief-pressure setting.

Shutoff valves located ahead of the safety valves permit in-service repair; however, it also necessitates duplication to assure safe operating conditions. Rupture disks provide a means to reduce helium leakage. The low-pressure leakoff-recovery system receives the flow from the pipe section between the relief devices.

Containment Cooling System

The containment vessel will be filled with nitrogen gas to prevent reaction of oxygen with the graphite in the event that the primary coolant system ruptures. In order to prevent damage to structure materials such as concrete and certain operating equipment, the temperature inside the containment vessel must not exceed a maximum of 150°F. The system proposed for this purpose is as follows: Nitrogen gas at atmospheric pressure flows by natural convection down the inlet-line pipe tunnel in each main coolant loop, circulates around and through the reactor vessel zone containment, and flows out the hot-leg tunnels. Water-cooled heat exchangers located in the main steam generator containment zone receive the heated nitrogen gas prior to its entering the cold-leg tunnel. A nitrogen makeup plant will supply gas to this system. Large volume storage is not anticipated. During normal operation the containment zones are isolated from the outside air by maintaining a nitrogen pressure slightly above atmospheric at all times.

8. STEAM GENERATORS

As part of the advanced gas-cooled reactor power plant study, Combustion Engineering¹ undertook the conceptual design of steam generators for the GCR-3. Consideration was given to the continuous-tube once-through type of steam generator with a reheater and to the controlled-circulation type. Also, in the once-through type, two different arrangements were compared: a vertical helium-containing cylinder and a horizontal cylinder. Four steam generators, each of 500-Mw(t) capacity, will be required in the power plant. The operating conditions for one of the once-through type of units are shown in Table 8.1. The dimensions and weights of the vertical and horizontal units are compared in Table 8.2, and the heat-transfer and flow characteristics are compared in Table 8.3.

The scope of the study was limited to determining the general arrangement and size of the units, the fabrication feasibility, and their costs. An estimated 4% of the engineering effort required to design and manufacture one of these units was expended on this study; therefore the design details have been left in the conceptual stage, and only a few analytical investigations have been attempted.

Basis for Selection of Continuous-Tube Once-Through Steam Generator

A continuous-tube once-through steam generator, in which water enters a tube in a subcooled state and exits superheated, eliminates the need for separate heat-exchanger tube bundles for preheating, evaporating, and superheating. The resulting simplified mechanical design does not require steam-water separation equipment or circulating pumps and reduces the complexity of controls compared with the common practices of steam generation. The incentive for using this type of unit is the low cost. The controlled-circulation designs that are used in fossil-fuel plants that operate between 1400 and 2800 psia steam pressures have a long history of

¹L. D. Fraley, "Steam Generator Study, 750e MW Advanced Gas Cooled Reactor Power Plant," USAEC Report CE-1077, Combustion Engineering, Inc., June 1962.

Table 8.1. Operating Conditions for One Steam Generator

Heat transferred, Btu/hr	1706×10^6
Gas conditions	
Flow rate, lb/hr	2.275×10^6
Temperature, °F	
Inlet	1150
Outlet	550
Pressure, psia	
Inlet	420
Outlet	415
Steam conditions	
Boiler	
Flow rate, lb/hr	1.452×10^6
Temperature, °F	
Inlet	450
Outlet	950
Pressure, psia	
Inlet	3032
Outlet	2490
Heat transfer, Btu/hr	1.443×10^9
Reheater	
Flow rate, lb/hr	1.036×10^6
Temperature, °F	
Inlet	540
Outlet	1000
Pressure, psia	
Inlet	437
Outlet	407
Heat transfer, Btu/hr	0.263×10^9

Table 8.2. Comparison of Dimensions and Weights of Vertical and Horizontal Steam Generators

	<u>Vertical Unit</u>	<u>Horizontal Unit</u>
Length (overhead)	89 ft 3 in.	79 ft 9 in.
Shell, outside diameter	18 ft 6 in.	16 ft 11 in.
Shell, wall thickness	2 7/8 in.	2 5/8 in.
Dry weight	662.6 tons	565.5 tons
Helium inlet nozzle, inside diameter (excludes internal insulation)	5 ft 6 in.	6 ft 6 in.
Helium outlet nozzle, inside diameter	6 ft	6 ft
Feedwater nozzle, ^a outside diameter	10 3/4 in.	10 3/4 in.
Superheater nozzle, ^a outside diameter	14 in.	14 in.
Reheater inlet nozzle, ^a outside diameter	30 in.	30 in.
Reheater outlet nozzle, ^a outside diameter	24 in.	24 in.
Economizer header, outside diameter	22 in.	30 in.
Superheater header, outside diameter	22 in.	30 in.
Reheater inlet and outlet headers, outside diameter	18 in.	30 in.
Economizer tubes		
Number	1470	1094
Outside diameter	5/8 in.	3/4 in.
Wall thickness	0.151 in.	0.210 in.
Length	256.2 ft	225.9 ft

Table 8.2. (continued)

	<u>Vertical Unit</u>	<u>Horizontal Unit</u>
Evaporator tubes		
Number	1470	1094
Outside diameter	5/8 in.	3/4 in.
Wall thickness	0.130 in.	0.130 in.
Length	117.4 ft	87.0 ft
Superheater tubes		
Number	1470	1094
Outside diameter	5/8 in.	3/4 in.
Wall thickness	0.130 in.	0.210 in.
Length	66.4 ft	56.9 ft
Reheater tubes		
Number	903	1290
Outside diameter	1 in.	1 in.
Wall thickness	0.095 in.	0.095 in.
Length	40.5 ft	118 ft

^aSize of connecting piping obtained by welding tees to shell nozzles.

Table 8.3. Steam Generator Heat Transfer and Flow Characteristics

	<u>Vertical Unit</u>	<u>Horizontal Unit</u>
Total heat transfer, Btu/hr		
Economizer	489.3×10^6	490.1×10^6
Evaporator	436.8×10^6	424.0×10^6
Superheater	515.6×10^6	528.4×10^6
Reheater	265.3×10^6	264.3×10^6
Log mean temperature difference, °F		
Economizer	64.1	79.5
Evaporator	96.6	132.9
Superheater	147.4	221.3
Reheater	296.6	98.2
Average overall heat-transfer coefficient, $\text{Btu hr}^{-1} \text{ft}^{-2} (\text{°F})^{-1}$		
Economizer	120.2	132.8
Evaporator	170.5	169.1
Superheater	205.0	204.0
Reheater	115.4	92.8
Heat-transfer area, ft^2		
Steam generator	101,500	77,000
Reheater	8900	33,400
Pinch point ΔT , ^a °F	38	62
Friction losses, psi		
Helium side	4.24	5.65
Water side	460	569

^a Minimum temperature difference between helium and water at any location.

dependable operation but are complicated because the steam generation process must be mated to the combustion process with its high, and sometimes unpredictable, heat fluxes and slag-containing gas flows. The GCR steam generators have predictable gas temperatures and gas-flow paths and thus permit a simpler treatment of the steaming process. Although the once-through approach tends to treat the generator as a shell-and-tube single-phase heat exchanger, the considerations of large property changes of the water were the major factors in determining the geometry of the design. The property changes influence the heat transfer, the flow in the tubes, and the solubility of the solids.

Heat Transfer.—In order to predict the heat transfer, the generator must still be considered as three different heating surfaces: preheater, evaporator, and superheater. The variation in specific heat alters sharply the temperature-transferred-heat relationship of the water, particularly in the boiler. This, coupled with the different heat-transfer coefficients of the water in these surfaces, forces a subdivision for calculation purposes. The flow rate in the tube must be high enough to produce acceptable preheater and superheater heat-transfer coefficients but not so high as to excessively raise the boiling temperature because of superheater pressure loss. At high pressures an increase in superheater pressure loss can increase the log mtd in the evaporator by a shift of the apparent pinch point into the upper region of the economizer. When considering a single gas pass with multiple water passes, the superheater should have at least two passes in order for the integrated temperature difference to approach the counterflow case.

Flow in Tubes.—The large variation in specific volume with heat addition, plus the highly turbulent condition that occurs when the two phases are mixed, might cause the flow rate through certain tubes to vary widely from the flow through other, identical tubes. Such a condition is referred to as "flow instability" and occurs when the flow rate is a multivalued function of the pressure difference across the manifolded tubes. A tube with a longer than average subcooled section but a higher flow rate can have the same pressure drop as the average tube. Similarly, a tube with a longer than average superheater but a lower flow rate can have the same pressure drop as the average tube.

It becomes necessary to know the pressure loss vs flow rate characteristics of the individual circuits. Counterflow generators do not generally have multivalued flows, but the characteristic curve may have a point of inflection or an area of decreased slope. It is best not to design close to this, so that the pressure loss will be a strong function of the flow rate. To maintain flow stability, the slope on a $\log \Delta P$ vs $\log W$ plot should be at least 0.8.

An important point is that the flow rate is dependent on the specific volume, which varies greatly with heat addition. Not only must the tubes receive equal heat but also the heat flux must be distributed along all the tubes in the same manner. This means that major flow restrictions (bends, butt welds, etc.) should occur in regions of about the same fluid density in the various circuits.

Solubility of Solids.—The high degree of solubility of solids in the feedwater compared with the zero solubility in the exit steam will cause the solids entering the generator to be deposited in the tubes, vaporized, or mechanically entrained in the steam. Satisfactory operation of supercritical once-through units, without trouble from deposits or entrainment, has been obtained over a number of years by using only volatile materials for feedwater treatment and limiting the water to the following impurities:

Total solids	0.25 ppm
pH	9.0 to 9.5 (by N_2H_4 and NH_3)
Total iron	0.01 ppm
Total copper	0.01 ppm
Silica	0.02 ppm
Dissolved oxygen	0.007 ppm

Annually washing the tubes with acid and flushing them during startup will prevent a long-term buildup of deposits.

Tube Spacing in Horizontal and Vertical Units

Besides having a different plant layout, the major difference between a vertical and a horizontal steam generator with gas connections at the top and bottom is the tube ligament, that is, distance between adjacent tubes. In the vertical unit the gas-flow path is narrow and

long, and the tubes must be widely spaced in order for the specified gas pressure loss not to be exceeded. In the horizontal unit the gas-flow path is wide and short, which permits a close spacing for achieving a high gas velocity and good heat transfer.

From parametric studies it was shown that as the ligament is made larger the width of the tube bundle decreases and the depth increases. The smaller-ligament tube bundles can be made to fit into a cylinder in which the gas flows across the diameter, and the large-ligament bundle to fit into a cylinder in which the gas flows along the axis. The amount of tubing required, that is, the average tube length for a constant number of tubes, is 10 to 15% less for the smaller ligaments. It is interesting to note that the amount of tubing required goes through a maximum as the ligament is increased. This occurs when the width of the bundle is small and a high gas velocity can be maintained without the tubes having to be close together. In general, the cylinder such units fit into is excessively long. Uniform flow distribution of gas is much easier to attain to and from the vertical tube bundle, since its width is smaller, than to and from the horizontal tube bundle.

Helium Containment Shell Diameter

Shop fabrication facilities at Combustion Engineering limited the diameter of the helium container. The heat-treatment furnace can accommodate a shell about 17 ft in diameter, with the gas nozzles located as on the horizontal unit. The vertical unit has the nozzles in the heads, and a shell diameter of about 18 ft 6 in. can be accommodated. Alternate methods of heat treatment (such as by induction-heating or with a treatment furnace) were not considered for this study.

Tube Size

Time did not permit a study of the influence of tube diameters on the cost of the steam generators. Small-diameter tubes have the lowest material cost and result in a more compact and lighter unit; large-diameter tubes require less labor because the number of tubes is less. Therefore there is a diameter for which the total cost is minimum. Small-diameter tubes were selected for the units in order to take advantage of

the compact arrangement and thus keep the size of the units within the range of shop fabrication facilities.

The minimum diameter that can be selected is set by the inside diameter. About 0.3 in. was considered the smallest inside diameter that could be used without excessive pressure drops occurring in the bends or butt welds. The pressure loss varies with the fifth power of the inside diameter. Tolerances on the tube wall thickness could cause a small inside diameter to vary excessively. In order for the flow resistance of the circuits to be equal, the tubes were specified with a ± 0.004 -in. tolerance on the inside diameter; variation in the wall thickness will result in outside-diameter variations. The tolerance on the wall thickness was limited to $\pm 6\%$ so that the outside-diameter variations would not hinder tube bending or complicate the tube supports.

The reheater tubes could not be small in diameter because of the steam-pressure-loss limitation and were therefore specified in the standard manner with a ± 0.004 -in. tolerance on the outside diameter and a $\pm 10\%$ wall-thickness tolerance.

Tube Material Selection

The selection of tube material depends on temperature and pressure. Water and gas side corrosion, strength, fabrication, and cost are also considerations. The possibility of introducing halogens through contaminated feedwater to the steam generator makes it desirable to use ferritic materials, which are not susceptible to stress corrosion in a region of alternate wetting and drying. Two designs for the steam generator were investigated: one with 2 1/4% Cr-1% Mo steel used for all tubing and the other with material changes as the temperature permitted. For the superheater, 2 1/4% Cr-1% Mo steel was used, and, as the temperature decreased, 1% Cr-1/2% Mo steel and ASTM A192 steel were selected for the evaporator and economizer. The cooling effect of the steam made it possible for ferritics to be used in regions where the gas temperatures were above the useful design temperature of the material.

In the reheater the introduction of halogens is not a problem. The low specified steam pressure drop prevents the steam from developing a high enough coefficient to cool the tubes; therefore, the gas inlet

temperature is used as the design temperature. Stainless steel is required for this condition.

Corrosion Allowance

ORNL tests of 1000-hr duration with 2 1/4% Cr-1% Mo steel at 1100°F in an impure helium atmosphere produced a corrosion rate of 4 1/2 mils/year.² Similar Combustion Engineering tests, but in an atmosphere of dry air, produced the same results. Linear extrapolation of the 1000-hr test to 20 years results in a 1/16-in. corrosion allowance being added to the tube walls. If the corrosion is of such a nature that the corrosion products can be swept away, as indicated by the ORNL test, then the extrapolation is probably correct. If, however, the corrosion products tightly adhere to the surface and retard further corrosion, the extrapolation will grossly overestimate the corrosion. Combustion Engineering experience, using 2 1/4% Cr-1% Mo steel in hot flue gas, indicates that a tightly adherent film forms and is only infrequently broken off. Since the process in helium is not completely understood, 1/16 in. has been added to all low-alloy and carbon steel members in contact with the gas.

Maintenance

The most important maintenance problem in the GCR-3 generators concerns repair of a possible leak in the water circuit. Although quality construction and 100% inspection will produce a leak-free unit, over the lifetime of the unit some leaks may develop. It is possible to plug the tubes from inside the generator headers on both designs. The flow of steam or water is directed through one branch of a tee placed on the end of each header, and a removable cap is welded to the other branch of the tee for access for repairs. Headers that are at least 24 in. in inside diameter are large enough for a man to enter. The smaller feedwater and superheater headers can be increased in size to make them more easily accessible, or special tools can be used to reach in from the ends for leak testing and tube plugging.

²Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. March 31, 1961," ORNL-3102, p. 213.

With normal operation, cleaning problems will not exist since helium and water in passing through the exchanger also pass through highly efficient cleaning systems. Annual acid washing of the tube side of the steam generator would eliminate a long-term buildup of solids.

Tube Welds

Socket Welds.—The geometry of socket welds of tubes of A213, grade T22, steel to headers of A387, grade D, steel conforms to detail "y" of Fig. UW-16.1 of Section VIII of the ASME Boiler and Pressure Vessel Code, with the welding to be by the manual shielded-metal-arc process. Either the magnetic-particle or dye-penetrant technique of inspection can be used. This type of weld has been used in the production of power boilers for many years with excellent results.

Bimetallic Butt Welds.—Butt welds of A213, grade TP 304, steel to tubes of A213T22 steel in 1-in. ID and 0.124-in. wall thickness, as proposed for the vertical unit reheater, can be accomplished by using an inert-gas rod of Racolloy 304 on all passes. The joint geometry prior to welding would be essentially as shown in detail (a) of Fig. 8.1. Inspection would be by dye-penetrant or x-ray (when space permits) technique.

Induction Butt Welds.—Butt welds between tubes of both similar and dissimilar material in 5/8- and 3/4-in. diameter and 0.13- to 0.21-in.-wall thickness can be accomplished with the induction pressure welding process developed by Combustion Engineering. Welds between ferritic materials have been made in large quantities by this process and have given excellent service. The process utilizes induction heat applied through a coil surrounding the joint. Axial pressure is applied to accomplish an upsetting action at the time when the metal is at a temperature just below its melting temperature. The entire operation takes place in a protective atmosphere. The tube ends are machined, with a small bevel, toward the outside just prior to welding so as to maintain cleanliness. The weld results in a bead of upset metal that can be controlled to 1/32 to 3/64 in. on the inside and 3/64 to 1/16 in. on the outside of the tube.

The resultant weld is of a high quality and is tested in production by periodic removal of a weld (such as 1 of every 20) for face and root

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(a) BIMETALLIC BUTT WELD

(b) STAINLESS STEEL TUBE TO HEADER WELD

Fig. 8.1. Welding Details for GCR-3 Steam Generator Tubes.

bend tests of the ASME Boiler and Pressure Vessel Code Section IX type. Destructive bend tests, together with an ultrasonic test which detects areas of an incomplete degree of bonding, result in welds of the highest quality. Radiography is not applicable since no porosity or slag is present and the welding faces are pressed together so tightly that a weld with an incomplete degree of bond is not detectable.

Stainless Steel Tube to Stainless Steel-Clad Header.—The proposed reheater design for the horizontal unit would involve a tube-to-header weld similar to the tube-to-tube sheet welds that have been developed for the pressurized-water-reactor type of steam generators. The tube would be expanded into the header and manually fillet-welded to the stainless steel cladding on the inside of the header (Fig. 8.1b). The weld geometry, except for the use of cladding, is identical to that for the tube joint described in paragraph UG-43 (g) (5) of Section VIII of the ASME Boiler and Pressure Vessel Code. The dye-penetrant inspection method would be used.

Reheater

The reheat cycle requires an additional heat exchanger in the gas loop with the steam generator. The temperature conditions are such that it may be either in parallel or in series in the gas stream with the steam generator. Considerable manipulation of surface size was necessary to fit both the generator and reheater into the same cylinder. It was found easier to fit the reheater in series in the vertical unit and in parallel in the horizontal unit.

Steam pressure loss determines to a considerable extent the size of the reheater. The steam heat-transfer resistance is about half the total heat-transfer resistance of the reheater, which results in high operating temperatures of the tubes and a large surface to be combined with the number of tubes required for a low-pressure loss. The effect of the pressure loss on the reheater size is greater in a gas-cooled plant than in a fossil-fuel plant, where the steam heat-transfer resistance is low compared with the total heat-transfer resistance.

The tube wall thickness is determined by the helium pressure and temperature in order to prevent the tube from collapsing during startup

or other conditions when there is external pressure but no internal pressure. During normal operation the steam pressure is about 30 psi greater than the gas pressure.

Control

A possible control scheme for a base load plant would be to vary the reactor outlet temperature to maintain a constant superheat temperature. The reheat temperature would be adjusted to 1000°F at design load by a preset baffle that permits gas to bypass the reheater surface. At other loads the reheat temperature would be controlled by feedwater injection. Feedwater flow to the steam generator would be set by the load, and the outlet pressure would be held constant by a regulating valve in the steam line. Startup would be accomplished by circulating 25% water flow through the steam generator pressure-control valve and bypass valves around the turbine to the condenser. The reactor would then bring the outlet steam temperature up to the operating value and the control would be switched to the operating scheme.

Comparison of Vertical and Horizontal Steam Generators

Description

Both the vertical steam generator (Figs. 8.2 and 8.3) and the horizontal steam generator (Figs. 8.4 and 8.5) are a shell-and-tube type of heat exchanger, with the helium coolant gas on the shell side and the steam on the tube side. The temperature-heat transfer relationship for the vertical design is shown in Fig. 8.6, and for the horizontal design in Fig. 8.7. The shell, heads, economizer inlet header, and shell nozzles are constructed of carbon steel. The superheater outlet header, reheater inlet and outlet headers, and economizer, evaporator, and superheater tubes are constructed of 2 1/4% Cr-1% Mo steel, and the reheater tubes of A213TP304 stainless steel. The tubes in the economizer, evaporator, and superheater are supported by scalloped bars attached to beams supported by the shell. All headers are also supported by the shell.

Fig. 8.2. Vertical Steam Generator.

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Fig. 8.3. Vertical Steam Generator.

Fig. 8.4. Horizontal Steam Generator - General Arrangement.

Fig. 8.5. Details of Horizontal Steam Generator.

Fig. 8.6. Temperature-Heat Transfer Relationship for Vertical Steam Generator.

Fig. 8.7. Temperature-Heat Transfer Relationship for Horizontal Steam Generator.

Vertical Design. - In the vertical design the reheater section is located in the uppermost portion of the shell. The tubes are arranged in a four-pass serpentine design for gas cross-flow. The economizer, evaporator, and superheater sections (once-through design) are arranged similarly. Each circuit has 35 passes. A stainless steel baffle encloses the tube bundles to channel the coolant gas across the tubes. Two rectangular bypass ducts are located in the area of the reheater and contain manually preset dampers to permit adjustment of gas flow across the reheater for steam temperature control. A layer of Thermoflex RF-800 insulation attached to the inside of the shell and a layer of Thermobestos on the outside will keep the shell temperature to about 600°F. Provisions are made for attachment of external insulation in the field. The heat loss is less than 0.1% of the total heat transferred.

Low-pressure-drop requirements in the reheater and welding costs dictated the design of a reheater circuit with a minimum of butt welds. The spacing of tubes in the bundle (1-in.-OD tubes on 1 1/8-in. pitch) dictated the use of a large header in order to provide space for welding the tubes to the header. As a result, it proved to be advantageous to expand the tubes into the header tube holes and seal-weld from the inside. Cladding the inside of the reheater headers will make it unnecessary to provide for further stress relief after the tubes are installed.

Horizontal Design. - In the horizontal design, the reheater inlet and outlet headers are clad on the inside with weld-deposited stainless steel. The reheater tubes, 1 in. in outside diameter, are supported by short 1 1/8-in.-OD sleeves, which are slipped over the tubes and locked together in such a manner as to hold the tubes in proper alignment and to transmit the weight of the tubes to the support beams. The sleeves are prevented from sliding axially along the tubes by use of a certain number of flared sleeves that engage other unflared sleeves and are locked to the scalloped bars attached to the support beams. Tie rods extending from the top and bottom beams and passing through the spaces between the tube passes provide clamping action and lateral support.

The reheater and steam generator sections are located side by side in the shell, with baffling arranged to provide parallel entrance of hot gas into the two sections. A removable baffle panel is located in one portion of the gas passage to the reheater in order to provide a degree of manually preset adjustment of steam temperature. Initial startup of the unit determines proper baffle setting. No provision is made for adjustment of the baffle during operating periods. The reheater contains seven passes of 1290 tubes per pass arranged for gas cross-flow. The economizer, evaporator, and superheater sections of the steam generator are in series (once-through design) and together contain 13 passes of 1094 tubes arranged for gas cross-flow.

A layer of Thermoflex RF-800 insulation is attached to the inside surface of the shell by means of 24-gage casing studded to the shell, and provisions are made for attachment of a layer of Thermobestos on the outside to maintain the design shell temperature of 600°F. About half the gas inlet side of the shell will be subjected to high-temperature gas. The insulation has been sized for this condition and continued around the shell. It is expected that conduction circumferentially through the shell will keep the outlet half of the shell at about 600°F. A 16-in.-ID manway located in each head provides access into the shell. The manway closure consists of a welded seal plate held in place by a bolted cover plate.

The vessel is supported on two saddles, which in turn are supported on rollers. The shell will be held in a fixed position at or near the gas nozzles, which will allow the expansion and contraction of the shell to be taken up at each saddle in nearly equal amounts.

Pressure-drop limitations, cost, temperature, and other considerations resulted in the selection of stainless steel for the reheater tubes. Space limitations dictated the use of the smallest header to which the given number of tubes could be welded by a reliable procedure. The type of tube-to-header weld selected, a conventional socket weld as used in power boilers, contains a built-in crevice. This crevice, while of no concern in carbon or low-alloy steels, is to be avoided in stainless steels because of the possibility of halogen stress corrosion at the

crevice. Therefore 2 1/4% Cr-1% Mo steel nipples were selected for socket welding to the 2 1/4% Cr-1% Mo steel headers and for butt welding to the stainless steel reheater tubes.

Fabrication, Assembly, and Erection (Both Designs)

The steam generator pressure-containing parts will be of all-welded construction and will be fabricated and inspected in accordance with Section VIII of the ASME Boiler and Pressure Vessel Code and all applicable nuclear code cases. The cylindrical portion of the shell will be fabricated in two parts with the nozzles in place and then furnace-stress-relieved at 1150°F. The two shell cylinders will then be welded together and the girth weld locally stress-relieved at 1150°F. The heads will be fabricated with the nozzles in place and furnace-stress-relieved. The headers will be fabricated from plate or pipe. After tube nipples, heads, and other attachments are welded to the headers, carbon steel headers will be furnace-stress-relieved at 1150°F. The 2 1/4% Cr-1% Mo steel headers will be fabricated according to appropriate preheat and interstage heat treatments, with a final furnace stress relief at 1350°F.

After the tubes have been bent, the butt joints will be welded. The stainless steel reheater tube bends will be stress-relieved at 1900°F prior to welding to header nipples. Tubes of less than 1/8-in.-wall thickness, which are to be welded by a shielded-metal-arc process, will have their ends thickened by upsetting prior to butt welding. The tube circuits will be assembled in fixtures outside the vessel shell by being welded to the headers and the support bars being attached as assembly progresses. After all other attachments, such as baffles and baffle supports, have been attached as far as is practical, the reheater and steam generator assemblies will be rolled into the shell on temporary tracks or on an overhead beam located in the shell. After tube bundle supports have been attached, the headers will be welded to the shell nozzles and locally stress-relieved. All internal baffles and insulation except in the head girth seam region will be installed. The head-to-shell girth seams will then be welded and locally stress-relieved. The remaining insulation, baffling, and duct work will be installed, and final inspection and testing will be performed.

The completed unit will be moved to the river dock on railroad cars or special trailer and pushed or pulled directly onto a properly braced river barge without being unloaded from its vehicle. At the power plant site the horizontal steam generator will be removed from the barge and loaded onto a temporary railroad track; the track will be laid on a rolled-dirt ramp from the barge to the steam generator foundation. Then the steam generator will be winched and blocked up the track to a position over the foundation and lowered into place. Additional equipment and labor will be required to raise the vertical unit to the upright position.

The external shell insulation and external supports for both units will be installed at the site.

Metallurgy (Both Designs)

Material selection was based primarily upon obtaining the highest strength at the operating temperature as modified by such economic considerations as initial cost and ease of fabrication. In general, carbon steel was selected for operating temperatures up to 650°F, and 2 1/4% Cr-1% Mo steel for temperatures above 1000°F or where high corrosion resistance was desired, such as for reheater tube supports and for all baffles.

Welding and Inspection (Both Designs)

Material inspection will be per ASME Boiler and Pressure Vessel Code, Section II and VIII and applicable nuclear code cases, plus that specified below; all welding will be of the general types specified:

Welding

Shell, heads, nozzles, header girth, and long seams	Automatic submerged arc and/or manual shielded arc; preheat and postheat
Tube-to-header socket welds	Shielded metal arc; preheat and postheat
Weld deposited cladding (horizontal reheater only)	Automatic submerged arc; preheat and postheat
Tube butt welds	Pressure induction for similar metals; inert gas for dissimilar metals
Stainless steel plate	Inert-gas consumable-electrode or shielded metal arc

Inspection

Material

Plate	Charpy V-notch impact testing
Forgings	Charpy V-notch impact testing ultrasonic and magnetic- particle inspection
Tubes (all)	Ultrasonic and dye-penetrant inspection
Pipe (headers)	Magnetic-particle and ultrasonic inspection

Welds

Shell, heads, and header seams	Radiographic inspection
Nozzles-to-shell	Radiographic and ultrasonic (where required) inspection
Tube socket welds	Magnetic-particle or dye- penetrant inspection
Tube butt welds	Ultrasonic or x-ray inspection
Reheater tube-to-header seal weld (horizontal unit only)	Dye-penetrant inspection
Tube and header assemblies	Hydrostatic testing
Completed shell	Pneumatic testing

Hot-Tube Studies (Both Designs)

Some of the tubes in the steam generator will have a less-than-average steam flow rate, which results in a higher average metal temperature in the tubes. With the steam flow rate and the terminal temperatures in the steam generator held constant, the tube-side pressure drop becomes a function of the average flow conditions. This pressure drop will be the same between the terminal ends of any tube in the bundle, whether it contains the optimum, average, or worst possible flow conditions. This will cause a variation in the flow rate through these tubes, with the minimum flow occurring in the tube containing the greatest flow resistance.

Tubes containing average and severe flow conditions can be compared by determining their relative equivalent lengths and their inside diameters. Since the pressure drop through the tubes will be the same, the ratio of their flow rates can be easily calculated.

The assumed conditions which will produce minimum flow are (1) an inside diameter 0.004 in. smaller than the average inside diameter; (2) maximum length of tube in the bundle, and (3) a reduction of 0.125 in. in the inside diameter at the butt weld joining the tube sections. The assumed conditions which will produce average flow are (1) average inside diameter, (2) average tube length in the bundle, and (3) negligible reduction in the flow area at the butt welds.

The sequence and physical characteristics of the bends are such that they do not contribute to the steam and water flow unbalance in the steam generator. This is because all bends are made on the same center-line radius and each tube contains the same total angle of bend at approximately the same location. The feedwater will be maintained sufficiently pure to prevent formation of scale, thereby removing any possibility of flow unbalance due to the deposit of solids. From these assumptions it was found that the flow through the tube with the worst possible flow conditions would result in a flow reduction of about 10%. A more conservative figure of 15% was used to determine the superheater outlet temperature in the tube with flow reduction. The average wall temperature at the superheater outlet in this tube was found to be 1030°F in the vertical unit and 1087°F in the horizontal unit.

Figure 8.8 shows that for the tube with 15% flow reduction the minimum time to rupture for the vertical unit would be approximately 20 years, and for the horizontal unit would be 18 years. The tube with average flow conditions would have a minimum life of approximately 23 years in the vertical unit and 21 years in the horizontal unit.

It is highly improbable that the combination of conditions assumed to produce minimum flow could ever occur in a single tube through the entire length of the steam generator. It is also true that only the top few rows of the superheater could contain a tube with a wall temperature higher than that of a tube with average flow since the temperature of both the gas and the steam decreases as the gas moves down the depth of the bundle.

The effect of load transients is also an important consideration in the steam generator design and could have a major effect on the performance limitations of the steam generator. Figure 8.8 shows that when the

Fig. 8.8. Superheater Tube Life vs Temperature.

superheater tube wall is operating at 1150°F the time to rupture is still over six years, so that short-time temperature transients in the range of 1 to 5 min which cause the tube wall to rise to 1150°F would have an insignificant effect on the steam generator failure characteristics. The units should therefore have considerable flexibility in terms of short-time deviations between the steam and gas side heat-transfer conditions.

Cost Estimates

Cost estimates for both designs were made on two bases. The first estimate was based on using only 2 1/4% Cr-1% Mo steel for all steam generator tubing. The alternate price was based on using A192 carbon steel tubing in the economizer, 1% Cr-1/2% Mo in the evaporator section, and 2 1/4% Cr-1% Mo in the superheater section. Comparative costs of the two units with the two alternate tubing materials are given in Table 8.4. Tubing for the reheater was type 304 stainless steel in all four cases. The steam generator tubing material has a greater effect on cost in the vertical unit than in the horizontal unit because the proportion of surface in the steam generating part of the unit is greater for the vertical unit. In spite of the larger amount of stainless steel tubing used in the horizontal unit, it is the cheaper design.

The estimated cost of moving the four horizontal steam generators from the barge to the site and erecting them is \$450,000. The corresponding estimate for the vertical units would be something in excess of this due to the necessity of putting them in an upright position.

Problems Encountered in the Designs

Stability

A preliminary analysis indicates that both units will be stable, that is, the slope on the log ΔP vs log W diagram is greater than 0.8. However, a more detailed analysis will be required to check this result, particularly at low loads. Should the analysis indicate the unit to be unstable, the addition of intermediate mixing headers about two-thirds through the water heating zone and about one-third through the superheating zone can eliminate the problem.

Table 8.4. Steam Generator Cost Data

	Horizontal		Vertical	
	2 1/4% Cr-1% Mo Tubing	A192, 1% Cr-1/2% Mo, and 2 1/4% Cr-1% Mo Tubing	2 1/2% Cr-1% Mo Tubing	A192, 1% Cr-1/2% Mo, and 2 1/4% Cr-1% Mo Tubing
Cost of four units	\$14,400,000	\$13,750,000	\$16,100,000	\$14,960,000
per kw(e)	18.90	18.00	21.10	19.60
per ft ²	32.60	31.10	36.50	33.90
per lb	3.18	3.04	3.04	2.82

Tube Supports

The major problem encountered in the mechanical design of the units was the support of the tubes, particularly in the vertical unit. In the vertical unit the tubes are widely spaced, but the platens are long and flexible and expand considerably. In the horizontal unit the platens are easier to handle, but the tubes are close together. A compromise would be to have a cubical-shaped bundle, fitting in a sphere or large cylinder, with the tubes farther apart than in the horizontal units and the platens shorter than in the vertical unit. However, this would create problems with shop fabrication of the helium container. Further study of the proposed supports would produce more acceptable designs without increasing their cost.

Corrosion

The addition of 1/16 in. to the tube walls for corrosion results in an estimated 5 to 10% cost increase and a bulkier unit. In the horizontal unit, in which the tubes are only 1/4 in. apart, a 1/8-in. corrosion would increase the gas-flow area 50% and thus decrease the gas heat-transfer coefficient about 22%. Long-term corrosion data should be evaluated to determine whether the low-alloy steels can be used in helium without losing 4 1/2 mils/year continuously. The alternate materials to the low alloys are stainless steel and nickel-chrome-iron alloys, but stainless steel is objectionable from a stress corrosion consideration and nickel-chrome-iron alloys are costly.

Reheater

The fitting of the reheater heat exchanger and the steam generation heat exchanger into the same gas pressure container prevents making an optimum selection for either surface. In addition, the scheme for controlling these two interacting exchangers is complicated and not very flexible. From the exchanger viewpoint a separate gas loop for reheating would be desirable. This would give the designer more flexibility both in choosing a concept and in determining the size of the reheater. The control scheme could be made positive and operative over a large load range.

Problems Not Investigated

The following problems were not investigated but need further study before the final design is completed:

1. stability and control during part-load operation,
2. transients that occur during "turbine trip" and "hot restart,"
3. analysis of steady-state temperatures and transients,
4. gas-flow distribution to and from tube bundle,
5. analysis of vessel support,
6. analysis of tube, header, and baffle supports relative to temperature distribution and differential expansion,
7. analysis of flow-induced and plant-induced vibration.

9. MAIN COOLANT BLOWERS AND DRIVES

General Description

An axial-flow blower driven by a 30,000-hp steam turbine is used to circulate helium in each of the four main coolant circuits. Each blower is coupled to its turbine by a jackshaft approximately 20 ft in length supported on four bearings. System design-point data for the blowers are as follows:

Suction pressure	400 psig
Suction temperature	550°F
Pressure rise	24.3 psi
Mass flow	2.28×10^6 lb/hr
Volume flow	248,000 ft ³ /min
Gas	Helium
Speed	3570 rpm
Speed range	2200 to 3600 rpm
Adiabatic efficiency at 3570 rpm	85%

The preliminary design study for the blowers and drives was carried out by the Steam Division of the Westinghouse Electric Corporation at Lester, Pennsylvania. Most of the effort was devoted to a study of the 4-blower design, but some preliminary design work was also carried out for two other blowers and drives: one twice the mass flow shown above per blower (two blowers and two steam generators per reactor) and one half the mass flow per blower (eight blowers per reactor).

Figure 9.1 is an outline drawing of a blower mounted on its bedplate, together with the auxiliary equipment required for the shaft seals. The blower consists of three major subassemblies: the bedplate, the blower housing, and the blower element. An exploded view of the three subassemblies is shown in Fig. 9.2, and a cross-sectional view of the blower is shown in Fig. 9.3. The total weight of the blower is 171,200 lb, with the bedplate assembly weighing 44,200 lb, the blower housing 40,700 lb, and the blower assembly 86,300 lb.

Fig. 9.1. Gas Circulator and Accessories on Bedplate.

Fig. 9.2. Principal Subassemblies of Gas Circulator.

Fig. 9.3. Cross Sections of Gas Circulator.

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The 60-in.-diam inlet and exhaust ducts are welded to the blower housing, which also carries the main blower support. The main support is of the trunnion type to permit alignment of the fixed part of the casing with the rotating assembly, the jack shaft, and the drive turbine. The drive end of the blower is supported by a vertical plate to allow for the slight amount of axial movement that would result from expansion of the blower casing. The blower housing becomes an integral part of the main coolant piping at installation and does not have to be disturbed for service of the blower. The housing and the blower element are joined by a vertical flange, seal-welded to eliminate leakage.

The blower element, which can be removed as a unit by breaking the one main vertical flange, is made up of an outer casing, a horizontally split inner casing, and the rotor. The outer casing contains the inlet bearing and seal housing and provides support for the inner casing, which is grooved to retain the stator vane diaphragms. The bearing housing at the blower discharge is supported from the inner casing by means of struts. The rotor is made up by shrinking disks on a forged spindle.

The bedplate provides a rigid support for the blower, and could also serve as a convenient mounting for the accessory components. The low-pressure reservoir that supplies oil for the thrust bearings is built into the back end of the bedplate. The high-pressure reservoir, the lube and seal pumps, filters, and coolers are integrally mounted on the bedplate. The resulting assembly could, therefore, be shipped with most of the piping installed to reduce field erection costs and time. Further study will be required to determine whether maintenance considerations will make it advantageous to use this arrangement for the auxiliary equipment or whether it should be installed elsewhere.

The use of an axial blower discharge duct was adopted primarily to reduce the number of elbows in the system coolant piping, but it has several advantageous effects on the design of the blower. The high-pressure shaft seal elements at the discharge end are eliminated. The axial distance between the bearings is reduced, which facilitates the problem of providing suitable critical speeds for the blower rotor.

The overall length of the large-diameter section of the blower casing is reduced, because it is not necessary to provide a plenum to turn the discharge flow through a 90° angle. One consideration must be balanced against these three advantages: with the blower bearing at the discharge end located inside the process helium stream, the blower has to be disassembled for replacing or otherwise maintaining the bearing. This disadvantage is believed to be a very minor one, because a journal bearing can be made to be extremely reliable, particularly if normal precautions are taken to provide a supply of clean, filtered lubricant.

Aerodynamic Design of Blower

To facilitate parallel operation of the blowers and to provide a margin for any possible increase in coolant system resistance, ORNL specified that the pressure rise at which blower surge would occur not be less than 125% of the normal system design pressure rise. To accomplish this purpose, Westinghouse designed the blowers to produce a pressure rise of 27.5 psi at a speed of 3720 rpm. This represents 112% of the normal pressure rise at a speed of 3570 rpm, and the balance of the required margin is made up by the design allowance normally used in a conventional axial-flow blower design. The expected performance of a single blower is shown in Fig. 9.4.

Each blower has seven blading stages of constant diameter. A free-vortex radial equilibrium was used in the design with a symmetric velocity triangle at the mean blade height. The selection of this configuration was based on research and development testing by Westinghouse on blading designed for air and for helium. A number of studies were made of the effects of various flow coefficients, hub-to-tip ratios, and number of stages. It was determined that the most important criterion in the selection of the final design was a rotational speed that would permit the use of blading and other elements in both the blower and the steam turbine drive that could make use of available designs and tooling to keep overall manufacturing costs to a minimum. A comparison of blower

Fig. 9.4. Calculated Performance of Helium Blower.

designs having five, six, and seven stages showed that the overall efficiency increased slightly with the number of stages, while the design shaft speed decreased from 4784 rpm down to the finally selected value of 3720 rpm.

Mechanical Design of Blower

The major factors in the mechanical design of the blower have been reported by Westinghouse,¹ and are summarized here. Blowers with blades having two different chord values were studied for their ability to withstand a transient surge of pressure, as might happen in the case of a coolant piping rupture. It was found that a blower equipped with rotor blades having a 4.27-in. chord might withstand a pressure surge of 196 psi, while blades having a 6.17-in. chord might withstand a surge substantially in excess of 400 psi. These pressures are based on static loading of the blades and not on an oscillatory motion. The oscillatory motion that would be imposed on the blading during such a circumstance might decrease the maximum pressure that the blades could withstand. The final selection of the blade chord should await further studies of the overall coolant system criteria for abnormal and emergency operation and studies of the pressure distribution between the individual stages.

Each blower is provided with a dummy piston vented to the blower discharge to eliminate the thrust load of some 27,000 lb that occurs at the design point.

Studies of the critical speeds of the blower shaft indicated that it would not be possible to operate below the first critical speed. Since it is not considered good practice to operate a blower at or within plus or minus 10% of any critical speed, operation of the selected configuration must be above the first critical speed and below the second critical speed. For the design with the 6.17-in. blade chord a

¹V. P. Buscemi et al., "Gas-Cooled Reactor Steam Cycle Evaluation," USAEC Report WCAP 1970, Westinghouse Electric Corporation, May 1, 1962.

configuration was selected that gave first critical speeds of 1880 rpm (based on rigid bearings) and 1600 rpm (including the effect of the oil-film spring rate in the bearings). The second critical speeds of all designs studied were above 5200 rpm. Conservatively, it appears feasible to operate the blower over the speed range between 2200 and 3600 rpm (from 60 to 101% of the normal design speed). The blower and turbine could both be operated at speeds up to 3720 rpm from the mechanical design standpoints. The maximum horsepower output of the drive turbine would, however, be exceeded at this speed when operating on the normal system resistance line.

Operating Characteristics

Since the mass flow at constant blower inlet conditions is essentially directly proportional to the blower speed (for a constant system resistance), it will be possible to vary the mass flow over the range from 60 to 101% of the design value by varying the speed of the blower drive turbine. Operation at lower values of mass flow can be accomplished in one of three ways: by decreasing the system pressure level, by bypassing some of the flow from the blower, or by shutting down one or more of the parallel cooling circuits.

One major consideration in operating blowers in parallel coolant circuits is the difference in operating speed that can be tolerated between the blowers without encountering instabilities (pressure and flow surges). Figure 9.4 shows the surge line for a single blower; the operation of the blower at any point to the left of this line will be unstable, and may be expected to lead to damage to the blower or to other parts of the coolant system. Figure 9.5 shows the resistance line along which any one of the four blowers will operate if running at a speed slower than that of the other three blowers. The intersection of this resistance line with the surge line indicates that the single blower would become unstable if its speed were less than about 76% of that of the other three blowers.

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Fig. 9.5. Calculated Performance of Four-Loop Helium Blower System.

This curve was obtained by assuming the pressure loss through the reactor at design flow as being two-thirds of the total system pressure loss, and the loss through the steam generator and piping as being one-third. It was further assumed that the flow through the reactor is the sum of the flows through the blowers, with flow through the steam generator and piping equal to the individual blower flow for each loop. Also, resistances were assumed to be proportional to the flow squared. The system losses were, therefore, determined as follows:

$$\Delta P = 16.2 \left[\frac{(n - 1) G_D + G}{n G_D} \right]^2 + 8.1 \left(\frac{G}{G_D} \right)^2 ,$$

where

ΔP = the pressure rise, psi, required of the incoming blower,

n = the number of blowers or loops of the design,

G_D = the design flow of the blower,

G = the flow through the incoming blower.

Individual blowers will normally be started with the valves in the bypass line opened and with the valves at the inlet and discharge of each individual coolant circuit closed (see Fig. 7.1). The speed of the blower will be set so that the flow it produces will be appropriate for parallel operation before being valved into the main reactor circuit. Individual blowers may also be shut down by following this procedure in reverse.

Analyses of the operating characteristics of the blowers under abnormal conditions were not made. Recommended studies are discussed in "Development Required for Blowers and Shaft Seals."

Blower Lubrication and Sealing System

The general design of the blower lubrication and sealing system is based on development work performed by Westinghouse for the Maritime Gas-Cooled Reactor. A full-scale seal and lubrication test rig for that project was built and tested at the Westinghouse Lester plant. The system consists, basically, of an oil-lubricated bearing and seal

assembly, which serves as the principal pressure-containing unit, and of a two-element labyrinth gas seal, which makes use of helium as a buffer gas to prevent the lubricating oil from entering the helium coolant system and the radioactive helium from entering the lubrication and sealing system.

The general arrangement of a typical system for an individual blower is shown in Fig. 9.6. Oil is pumped from the helium oil reservoir (item 1) to the bearings and the oil-seal rings. The flow through the bearing is maintained at about 87 gpm to provide adequate cooling for the shaft, bearings, and seal parts. The pressure of the oil entering the bearings is maintained at a few pounds per square inch below the blower discharge pressure.

A portion of the oil flowing from the helium oil reservoir is introduced into the space between the two seal rings nearest and to the left of the journal bearing at the coupling end of the blower shaft. The pressure of this supply is maintained at about 20 psi above that of the journal-bearing supply so that approximately 1.5 to 3 gpm leaks into the journal-bearing area across the inner oil-seal ring. The zone between the middle and outer seal rings (those nearest the thrust bearing and blower coupling) is maintained at a pressure of about 50 psig during normal operation. The middle seal ring is consequently the main pressure breakdown point in the sealing system. The flow across this ring is approximately 2.5 to 1.2 gpm, depending on the system pressure level.

Figure 9.7 shows one of the two pivoted-pad type of journal bearings and the two seal rings located in grooves adjacent to it (two additional rings are used at the coupling end of the blower, as described above and subsequently). The use of pivoted-pad bearings permits the use of relatively small bearing clearances, which facilitates the design of the shaft seal elements. Each seal ring is about $3/4$ in. square in cross section and is made up of two pieces fastened together with machine screws, as shown in Fig. 9.7.

The outer seal ring (nearest the thrust bearing) serves to limit the leakage of oil containing dissolved helium into the thrust-bearing lubrication system to approximately 0.3 gpm.

ITEM	COMPONENT IDENTIFICATION
1	HELIUM OIL RESERVOIR
2	MAIN OIL PUMP
3	H.P. OIL PUMP RELIEF VALVE
4	OIL FILTER
5	OIL COOLER
6	ATMOSPHERIC OIL RESERVOIR
7	BEARING SUPPLY REGULATOR
8	L.P. BEARING HEADER RELIEF VALVE
9	BEARING DRAIN ORIFICE
10	AUXILIARY OIL PUMP
11	OIL SEPARATOR
12	OIL SEPARATOR LEVEL CONTROL VALVE
13	HELIUM RECOVERY COMPRESSOR
14	SCAVENGE GAS REGULATOR
15	SCAVENGE GAS DRAIN COOLER
16	SCAVENGE GAS CLEANUP FILTER
17	SCAVENGE BLOWER
18	CONDENSATE DRAINER-OIL
19	SEAL LEAKAGE RELIEF VALVE
20	END SEAL GRAVITY TANK
21	END SEAL REGULATOR
22	SEAL OIL PUMP
23	BEARING DRAIN REGULATOR
24	TRANSFER PUMP
25	OIL RESERVOIR LEVEL CONTROL VALVE
26	END SEAL SUPPLY ORIFICE
27	DECONTAMINATION SYSTEM & BLOWER

Fig. 9.6. Schematic Diagram of Blower Lubrication and Seal System.

Fig. 9.7. Pivoted-Pad Type of Journal Bearing.

Gas for the helium-buffered portions of the shaft seal is obtained from the blower discharge, and passes through the helium purification system. It is then fed to the center of a two-element labyrinth, with part of it flowing toward the blower rotor and the balance toward the drain for the oil-seal ring nearest this labyrinth. The mixture of oil and helium is conducted from this drain to an oil separator (item 11 on Fig. 9.6), where the oil is separated and then returned to the helium oil reservoir. The helium is returned to the coolant system by a small compressor after it has passed through an oil cleanup system.

The oil lubrication system for the thrust bearing is conventional in all respects but one: it is necessary to conduct the small amount of oil that leaks into the thrust-bearing area back to the helium oil reservoir through the line marked "helium oil reservoir refill."

The seal and bearing arrangement at the discharge end of each blower is similar to that previously described except that two of the four oil-seal rings can be eliminated since the shaft does not penetrate the blower casing.

A third labyrinth element is placed in series with the two previously described, at each end of the blower. The space between the second and third element is connected to the suction end of the blower to assist in providing a pressure differential to induce flow through the center labyrinth element.

A number of provisions are incorporated into the system to ensure continued operation of the oil-lubricated portions of the seal after the failure of one or more items of equipment. As shown in Fig. 9.6, an auxiliary oil pump (item 10) is installed in parallel with the main pump (item 2), and would be fed from an independent power supply. In the event that both pumps become inoperative, the end seal gravity tank can maintain the required flow of high-pressure sealant across the two seal rings to the left of the drive-end journal bearing for a period of 30 min. A third pump (seal oil pump, item 22) can be operated intermittently to refill the end seal gravity tank.

A static seal for use during blower shutdown is provided at the drive end of the main blower (omitted from Fig. 9.6 for the sake of simplicity, but shown in Fig. 9.8). Two elastomer O-rings are installed in annular recesses at either side of the middle seal ring at the drive end. The inside diameter of the O-rings is somewhat larger than that of the shaft during normal operation, so that they do not interfere with the flow of the oil sealant. To actuate the static seal, oil pressure is applied through the two lines that supply the annuli surrounding the O-rings, through the connection marked "actuating pressure"; the latter are forced against the shaft and also seated against the edges of the annuli. Westinghouse reports that an effective seal is thus formed, as long as a film of oil is maintained between the shaft and the static seal rings. The static seal is released by applying oil pressure to the line marked "release pressure" in Fig. 9.8.

Blower-Turbine Jackshaft

The 20-ft-long jackshaft connecting each blower to its drive turbine passes through the containment vessel. The length of the jackshaft is primarily determined by the requirement of an axial space of 14 ft required for the blower element to be withdrawn from the blower casing. Preliminary design data for the jackshaft are as follows:

Nominal shaft diameter	7 in.
Number of bearings	4
Bearing diameter	6 in.
Bearing length	3 in.

The bearings will be of the shoe (pivoted-pad) type and will be spaced on center lines of approximately 5 ft 4 in. Koppers No. 7 Fast-type flexible couplings will be used at each end of the jackshaft. Four bearings are required to obtain suitable critical speeds for the shaft. Preliminary calculations have indicated that the lateral critical speed will be approximately 7500 rpm and that the basic torsional frequency will be about 510 vibrations per minute. The lateral critical speed is

Fig. 9.8. Static Sealing System for GCR-3 Blower.

well above the maximum operating speed of the blower and therefore should cause no difficulties. Further investigation of the torsional vibration frequency spectrum would be required to assure that undesirable torsional resonances are avoided in the rotors and blading of the blower and drive turbine.

A shaft seal will be required at the point at which the jackshaft passes through the containment vessel. No detailed study has been made of this problem, but it seems highly probable that any one of several known configurations could be used without undue difficulty or cost. The jackshaft will probably be split into two 10-ft sections, bolted together at a center flange, to facilitate disassembly.

Blower Drive Turbine

Each blower will be driven by a multistage single-flow steam turbine rated 31,000 hp at the maximum operating speed of 3720 rpm. Inlet steam will be taken from the exhaust of the high-pressure element of the main generator turbine, and the blower drive turbine will exhaust to the main condenser. The same turbine design will be used for each of the four steam cycles discussed in this study, except for steam cycle 4-A, in which case nonautomatic extraction openings will be provided in the turbine design.

The pressure of the steam entering the blower drive turbine will be 445 psia at normal operating conditions, and the total temperature will vary from 540 to 699°F, depending on which steam cycle is selected.

Each turbine will be provided with a hydraulically operated stop valve, an oil relay type of main speed governor, a diaphragm-operated pneumatic speed changer, and a hand-operated speed changer. A separate emergency overspeed governor will also be provided. The lubricating oil and the control oil supplies will be separate and will be provided with both main and auxiliary pumps.

Maintenance of Blowers, Drives, and Auxiliary Equipment

Since the blower drive turbines are located outside the containment shell, they can be maintained by normal power plant practices. When major maintenance is performed on the internal parts of the main blowers or shaft seals, the pressure in the reactor coolant system must be reduced to atmospheric pressure, and access to the blower will be gained by removing an access plug forming part of the containment shell (see Fig. 1.3). Prior to this operation it will be necessary to dismantle the jackshaft that normally couples the blower to the drive turbine.

If the servicing operation involves any of the bearing or seal elements at the coupling end of the blower, these elements can probably be designed to permit their removal and replacement without the blower element having to be removed from the casing (some modification of the design shown in this report will be necessary to permit this). If the bearing or shaft seal components at the discharge end of the blower require servicing or if the blower element is to be inspected or serviced, it will be necessary to remove the latter from the casing. This can be accomplished, without disassembling the turbine, by grinding out the seal weld at the main blower casing flange and unbolting the flange. Then the 42-ton element can be moved on a dolly (or suspended from an overhead rail) straight out of the casing for about 14 ft, until the end of the discharge cone clears the blower casing, and then moved in a roughly circular arc so that it passes between the outside of the containment vessel and the drive turbine.

Comments on Blower and Turbine Drive

Further developmental or investigative efforts are either required or desired for several aspects of the blower and turbine drive, as discussed at the end of this section. There are also a number of areas in which little or no development should be required. The drive turbine,

for example, is a standard item of equipment in the size range specified. The aerodynamic design of the blower blade path is based on an extensive background of technology developed principally for aircraft gas turbine compressors and is used in a variety of industrial applications.

The type of seal described in this report appears to incorporate a number of advances over those to be used in the EGCR and other first-generation gas-cooled reactors. First of all, the present seal configuration permits a substantial reduction in the span of the shaft between the bearings, since the latter are essentially located inboard of the liquid-buffered seal elements, while in other seal designs all the seal elements are located between the rotor and the bearings. The present seal, therefore, makes it possible to reduce the overall length of the rotating assembly and eases the problem of obtaining desirable critical speeds. Second, the present design also eliminates the need of a separate cooling system for the seal area and thus does away with many design and fabrication complexities. Third, the use of elastomeric O-rings as a part of the normal running seal configuration is eliminated.

It has been pointed out previously that the use of an axial discharge connection permits the elimination of one high-pressure seal element for each blower.

Alternate Blower Arrangements

Preliminary design information for blowers half the size and twice the size of those given in the main portion of the report is given in Table 9.1. These units would be designed for the same suction pressure (400 psig), suction temperature (550°F), and pressure rise (24.3 psi) as those previously described. Corresponding design data for the latter units are included in the table for purposes of comparison.

In determining the minimum paralleling speed for the last blower, it is assumed that all the other blowers in the system are operating at normal speed (defined as that speed which would produce 100% of the design flow with all blowers operating at the same speed).

Table 9.1. Preliminary Design Information
for Different Size Blowers

	Small	Medium	Large
Number of blowers for one reactor	8	4	2
Mass flow, lb/hr	1.14×10^6	2.28×10^6	5.56×10^6
Volume flow, ft ³ /min	124,000	248,000	496,000
Normal speed, rpm	5700	3570	3270
Maximum speed, rpm	5890	3600	3370
Maximum power input, hp	15,500	31,000	62,000
Number of blower stages	6	7	5
Blade hub diameter, in.	25.42	37.36	48.57
Hub-tip ratio of blades	0.60	0.60	0.60
Minimum paralleling speed for last blower, % of design speed	63	76	80
Cost estimates (per unit)			
Blower and seal system	\$855,000	\$1,640,000	\$3,680,000
Seal and seal system	115,000	210,000	462,000
Steam turbine	454,000	700,000	1,280,000
Jackshaft	37,500	50,000	75,000

The relatively short period of time allowed for the Westinghouse design study did not permit them to calculate such items as the critical speed of the blower rotor or to complete an initial design for either the small or the large blower. The cost estimates for these two sizes must therefore be considered as being very preliminary.

The 62,000-hp steam turbines used to drive the large blowers will be of the tandem-compound double-flow type. The turbines for the remaining two sizes will be of the single-case double-flow type. Turbine weights are estimated as follows:

	<u>Small</u>	<u>Large</u>
Turbine, hp	15,500	62,000
Total weight, lb	52,000	330,000
Rotor weight, lb	6900	25,000

Development Required for Blowers and Shaft Seals

A number of variables in the design of any reactor coolant system must be taken into account before the blowers, shaft seals, seal system, blower drives, and speed-control system can be completely specified. These variables include the method selected for obtaining variable mass flow rates, the response time of the blower control system, the inertias of the system, and the operation of the coolant system under abnormal and emergency conditions. The design of the rotating equipment and the associated components and systems can be undertaken after these and other variables have been established for all conditions of reactor operation, but it must be done in conjunction with a certain amount of development work to determine the most feasible and economical approach to meet the coolant system operational requirements.

The general design of the blower shaft seal and lubrication system is based on development work performed by Westinghouse for the Maritime Gas-Cooled Reactor. If the data from the tests being conducted by Westinghouse on their full-scale seal and lubrication test rig can be made available to the general gas-cooled reactor program, they should provide some of the information required to size the various components and to predict the operating characteristics of the seals and bearings for most operating conditions.

The Maritime Gas-Cooled Reactor uses a single coolant circuit and the GCR-3 may have either two, four, or eight circuits in parallel. It will therefore be necessary to perform studies to determine the best arrangement of the shaft seal auxiliaries for a multiblower reactor and to decide whether a single system should be used for all the blowers, whether separate systems should be employed (one for each blower), or whether some alternate arrangement can be used. It will also be necessary to determine whether the shaft seal system auxiliaries can be combined with other auxiliary systems used for similar purposes in the reactor plant. These studies will revolve around three primary considerations: system operability, reliability, and overall economy.

It will be necessary to evaluate the maximum rate at which it will be permissible to release helium to the containment vessel from the atmospheric oil reservoir and the rate at which oil can be introduced into the reactor coolant system from the shaft seals and the oil-removal system. The transfer of radioactivity from the helium coolant to the oil lubricant and sealant must also be studied. The results of these studies will permit a final determination of the optimum auxiliary circuit arrangement and will make it possible to size the various auxiliary compressors. The studies should also indicate whether it will be possible to either simplify or eliminate the equipment used to remove oil from the helium returned to the reactor coolant system by the seal system.

Preliminary studies indicate that the blowers which supply the labyrinth seal will be relatively large (3.2 lb/sec per main blower at a pressure rise of about 5.0 psi, corresponding to about 40.0 hp). This rate of flow is required by the relatively large clearances associated with the labyrinth seal elements, and studies and tests of means for reducing these clearances without sacrificing reliability are indicated. The study may also reveal other (possibly simpler) means for preventing radioactivity from entering the oil-buffered areas of the seal. In the proposed seal a flow of clean helium to the double-labyrinth arrangement prevents such contamination. Developmental tests of hermetically sealed

blowers for in-reactor test and reactor auxiliary use are now in progress at ORNL, and data from this program should be of value in selecting the auxiliary blowers for the GCR-3 shaft seal system.

The proposed shaft seal system and its associated instrumentation are complex and represent a considerable proportion of the cost of each blower. It would be very desirable to obtain a less complex and less expensive system if at all possible. Some progress toward this goal should be achievable by studying the GCR-3 shaft seal requirements in the light of the data now being obtained at the University of Tennessee from the evaluation of all types of shaft seals that can be used for gas-cooled reactor applications. Such studies will be made a part of the Gas-Cooled Reactor development program. ²⁻⁶

The suitability and reliability of the static seal proposed by Westinghouse as an integral part of the proposed shaft seal system have not been ascertained. The proposed development work would include the study and evaluation of this static seal and would permit the specifying of a suitable static seal in the event that the one proposed did not perform as expected.

Blower-to-Turbine Jackshaft Seal

The shaft seal for the jackshaft at the point at which it penetrates the containment vessel must provide a barrier between the volume inside the vessel and the atmosphere in the event of a reactor coolant

²Frederick Arnold and William K. Stair, "The Labyrinth Seal - Theory and Design," USAEC Report TID-15986, March 1962.

³William H. Carden, "The Mechanical Face Seal," USAEC Report TID-15988, March 1962.

⁴William K. Stair, "A Research Program on Dynamic Seals," Rotating Machinery for Gas-Cooled Reactor Application, Proceedings of Meeting at Oak Ridge National Laboratory, April 2-4, 1962, USAEC Report TID-7631, pp. 85-103, July 1962.

⁵William K. Stair, "The Visco Seal - A Survey," USAEC Report TID-15987, March 1962.

⁶William K. Stair, "The Bearing Seal - A Design Evaluation," USAEC Report TID-16358, April 1962.

system rupture. The jackshaft may be rotating when the rupture occurs, but the shaft seal must be capable of providing a high degree of long-term reliability when the jackshaft is stationary. The seal should be as simple as possible and should be independent of the need for the continued operation of auxiliary systems.

Initial studies indicate that a Visco seal should be capable of fulfilling the requirements for the jackshaft application. The theory and arrangement of this type of seal are shown in a recent report issued by the University of Tennessee.⁵ An attractive feature of the Visco seal configuration is that the elements can be arranged to give automatic application of the static seal upon blower shutdown without the necessity of a source of external energization. A substantial amount of development work must be carried out before such a seal could be used with any degree of confidence for the GCR-3 application. Alternate types of seals will therefore be considered in the initial studies so that the total expenditure for development, construction, and operation can be minimized.

Operational Studies for Blowers

One important consideration in evaluating the operation of the blowers and associated systems under various abnormal and emergency conditions and during startup and shutdown is the deviations in speeds that can be permitted for the blowers of a multiblower reactor without encountering instabilities in the reactor coolant system and possible surging of one or more of the blowers. Another important consideration is related to the response of the blowers under abnormal operating conditions, such as might be caused by the introduction of steam into the helium coolant system from a leak in a steam generator, and the means for preventing possible damage to the blowers. The operating variables in each new reactor system make it necessary to conduct studies to evaluate these phenomena. A considerable amount of related information will be obtainable from the precritical and critical operation of the EGCR.

There are numerous methods by which the coolant mass flow rate can be controlled in the reactor. The response characteristics of the GCR-3 blower systems and controls must be analyzed to obtain information for development of suitable control methods.

10. PRIMARY COOLANT PIPING

General Design Considerations

The layout of the GCR-3 primary piping and the selection of materials have been premised upon the requirements of Section VIII of the ASME Boiler and Pressure Vessel Code with the nuclear code cases and upon requirements of the ASA B-31 Code for Pressure Piping, with due consideration being given to the present state of technology of the various components and to the important economic factors.

Neither the size nor the pressure presents any undue difficulties to the various components; however, the high design temperature of 1200°F of the hot piping leg was felt to impose difficult technical and economic problems. Rather than burden the piping with a 1200°F design condition, it was decided to internally insulate the hot piping and thereby maintain a maximum temperature of 650°F at the pipe wall so that carbon steel could be used through the hot portion as well as the cold portion of the main piping system. The feasibility of internally insulating against high temperatures has been demonstrated by Battelle Memorial Institute's testing of reflective insulation for the EGCR.¹ Similar types of insulation could be utilized in the GCR-3 without further development; however, it is felt that significant savings can be realized through the use of ceramic fiber insulations such as Thermoflex or Fiberfrax. Although the ceramic fiber insulations have not been used in the precise conditions imposed by the GCR-3, sufficient development work has been done to justify their selection for the GCR-3.^{2,3}

¹Herbert R. Hazard et al., "Experimental Evaluation of Reflective Insulation for the Experimental Gas-Cooled Reactor," USAEC Report BMI-X-165, Battelle Memorial Institute, December 21, 1960.

²T. D. McLay, "Thermal Insulation Designs for Gas-Cooled Reactors," USAEC Report APEX-641, General Electric Nuclear Materials and Propulsion Operations, August 1961.

³J. J. Thigpen, "Experimental Analysis of the Mechanism of Heat Transfer Through Fibrous Insulating Materials," University of Texas Doctoral Dissertation, June 1959.

The hot piping for the GCR-3 is therefore envisioned to consist of an inner insulation layer, a piping shell of A212B carbon steel, and an external layer of calcium silicate insulation. The inner insulation layer would be structurally supported at the inner face by overlapping sheets of stainless steel, which in turn would receive structural support by means of welding studs fastened to the carbon steel plate. The overlapping sheets would provide protection not only against fretting of the ceramic fiber insulation but also against collapse of the insulation layer as the result of a depressurization accident.

The external insulation would consist of a 1/2-in.-thick layer of calcium silicate on the hot section and a 3 1/2-in.-thick layer on the cold section.

Valves

A survey of vendors revealed that there is limited experience with the fabrication of 60- to 96-in.-diam valves for nuclear service; so it was decided to specify primary coolant valves that would permit (1) each loop to be isolated following mechanical failure of loop components so that operation of the other three loops could continue and (2) control of compressor bypass and loop flow for startup of gas circulators. The investigation reported here was limited to the first functional requirement.

Although from the technical standpoint it appears that gate, cone, or butterfly valves can be designed for GCR-3 service, butterfly valves appear to offer the cheapest solution to the GCR-3 valve problems.

No attempt has been made to assess the valve problems associated with an operation simulation, a hazards analysis of malfunction, or failure of loop components. However, judging from the previous experience of valve procurement on the EGCR and from the scope of problems associated with valves of the GCR-3 requirement, considerable development work would appear to be required.

Expansion Joints

Size of the piping loops precluded any practical method of accommodating thermal expansion, except through means of bellows expansion joints. The existing technology of bellows-joints fabrication appears to be developed well enough for the GCR-3 application.

Notwithstanding the generally acceptable level of competency for fabrication of bellows of this size and application, the probability of bellows failure is sufficiently high for it to appear mandatory that a joint of each size and manufacturing process be proven by a full-scale cyclic test at design conditions.

Only joints of hinged or gimbal construction have been considered for the GCR-3 loops in order to avoid problems associated with absorption of large moments and forces incurred with anchored and guided types of construction. An examination of the movements associated with hinged and gimbal joints for the GCR-3 indicates that large sizes, 60 through 96 in., may be accommodated with the existing layout; greater clearance, however, is needed between joints and shielding concrete with the larger sizes and slight reorientation of joints in the larger sizes would permit reducing the number of bellows convolutions.

Economics

Based upon current data from industry and from an extrapolation of EGCR costs, the costs associated with the main coolant piping of the GCR-3 concept do not appear to be excessive. Figure 10.1 presents the estimated direct construction cost for the GCR-3 main coolant piping, exclusive of the emergency shutdown loop piping, over a size range from 60 to 96 in.

An attempt has been made to assess the effect of the following parameters upon the GCR-3 piping cost: flow diameter, hot-piping internal insulation thickness, power cost, and interest rate on capital. These factors were permitted to vary over a considerable range in order to obtain the effect upon optimum flow diameter and upon the incremental savings. A flow diameter of 60 in. and an internal insulation thickness of 3 in. were chosen as a reference design upon which to base the effect of the variation of the parameters. The incremental savings or loss was determined as follows:

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Fig. 10.1. Direct Cost of GCR-3 Piping.

$$\begin{aligned}
 \text{Net savings or loss} = & \text{incremental pumping power savings} \\
 & + \text{incremental circulator cost savings} \\
 & + \text{incremental heat savings} \\
 & + \text{incremental heat-sink cost savings} \\
 & - \text{incremental piping cost}
 \end{aligned}$$

Three dimensional plots of net savings vs insulation thickness and flow diameter were made for a widely varying range of power costs at each of two interest rates on capital, 6 and 14.5%. These plots indicated that under no circumstance can internal insulation greater than 3 in. be justified economically. However, a minimum of 3-in. internal insulation is required to maintain the carbon steel piping at or below the design temperature of 650°F. Only a slight shifting of the optimum flow diameter from about 72 in. to about 84 in. was observed throughout the range of values used for the various parameters. Figures 10.2 and 10.3 present plots for which the power cost is close to the estimated plant potentiality for both REA (6%) and investor-owned (14.5%) interest rates. It is interesting to note that the optimum flow diameter in both instances is very close to 78 in. The net savings for these plots is expressed as a present-worth value, considering pumping power and heat loss as uniform annual costs for which present-worth values were determined for the appropriate interest rate by using uniform annual payment present-worth factors. Although the potential savings are small as a percentage of the initial capital investment, the potential savings are large enough, approximately a half million dollars present worth, to justify a careful scrutiny of the component costs in the design and construction of a reactor of the GCR-3 type and size.

Fig. 10.2. Curve of Cost Savings vs Flow Diameter and Internal Insulation Thickness (Based on Investor-Owned Interest Rate of 14.5%).

Fig. 10.3. Curve of Cost Savings vs Flow Diameter and Internal Insulation Thickness (Based on REA Interest Rate of 6%).

11. BIOLOGICAL SHIELDING REQUIREMENTS

The recommended maximum permissible exposure to radiation is 100 mrem/week, or 2.5 mrem/hr based on a 40-hr work week. In the design of the biological shielding for the GCR-3 the criterion set was that the dose rate not exceed 2.5 mrem/hr during normal operation at any point on the shield exterior located in an unlimited-access area. This criterion inherently includes allowance for significant underestimation of the hot-spot dose without the general area exceeding the recommended 2.5 mrem/hr.

The main shield over the top of the reactor was specified as consisting of 12 ft of ordinary concrete. Since only 10.5 ft of a monolithic concrete shield was required to reduce the dose rate to 2.5 mrem/hr, the extra 1.5 ft was added to allow for penetrations such as instrument tubes, control rods, etc. The hottest spots will undoubtedly occur at the control rods. A detailed design of the control rod system is necessary for estimating the dose rates at these points. On the basis of present concepts, however, no extreme shielding problems should occur.

The lateral shield may be of earth fill since the reactor is below grade level. The only concrete required is for structural purposes. If future plans call for subterranean rooms about the reactor, additional concrete for shielding will be required about the reactor room and primary coolant lines.

Shielding is required about the equipment because of the neutrons and gamma photons that stream down the primary coolant lines from the reactor. The contribution to the total dose rate by corrosion-product deposition and neutron activation will be small in comparison with that from the above sources.

Economic and structural considerations have specified the containment vessel wall to be reinforced 2-ft-thick concrete. During normal operation at 2000 Mw the maximum dose rate at the exterior of the wall will occur over the vertical leg of the primary coolant outlet pipe, and was estimated to be 3 mrem/hr. This

slightly exceeds the 2.5-mrem/hr maximum for an unlimited access area. However, the hot-spot dose occurs on the top of the containment vessel, which, because of its construction, must be a limited-access area, and therefore, because of geometric attenuation, the dose in the surrounding grade-level area will not be significantly raised.

The GCR-3 is designed as a clean system, and under normal operation no fission products should appear in the coolant stream. However, it is credible that on some occasions fuel elements will leak some fission products. Also, as the result of an unusual operating occurrence, some fuel elements or portions thereof could be destroyed. On the assumption that the fission products contained in one fuel element (each fuel channel contains 14 fuel elements) are distributed uniformly throughout the approximately 125 ft of coolant line between the reactor and the steam generator, the maximum dose rate at any point on the containment-wall exterior 1 hr after the incident would be 1.1 rem/hr and would drop to 220 mrem/hr in 24 hr. If all the fission products collected in the steam generator, the maximum dose rate would be 1.4 rem/hr after 1 hr and 240 mrem/hr after 24 hr. The foregoing dose rates will change in proportion to the number of fuel elements destroyed. The addition of 1 ft of stacked concrete block shielding around the steam generators and blowers would reduce the dose rates to acceptable levels at ground level.

12. STEAM AND FEEDWATER SYSTEM

Steam cycles for the GCR-3 plant were investigated by Westinghouse Electric Corporation¹ under contract with the AEC. Conditions specified for developing heat balances to determine the effect of inlet pressure as well as reheat were (1) 1450 psig, 1000°F, no reheat; (2) 1800 psig, 1000°F, no reheat; (3) 1800 psig, 950°F, 1000°F reheat; (4) 2400 psig, 950°F, 1000°F reheat.

Each of the four basic cycles incorporated nonextraction condensing turbine drives for the helium blowers and the boiler feed pumps. An alternate to the fourth cycle which utilized extraction turbines for the blower drives was also studied. The study included complete heat balances for the four cycles and the alternate, complete bills of material for the steam cycle equipment, general conditions, classifications, and engineering data of the turbine-generator units and the heat-transfer equipment. Estimates of equipment cost were prepared for all four cycles and the alternate. Cost data for cycle 4, which was used as a basis for the overall GCR-3 plant study, are included in Chap. 14 of this report. Dimensional data on all equipment included in the Westinghouse study were provided for use in the plant design and layouts. For all cycles investigated the exhaust pressure assumed was 1 1/2 in. of Hg absolute. The nominal heat rating of the plant was 2000 Mw and the steam flow for each cycle was determined by the respective steam conditions and the same final feedwater temperature of 450°F. Due to the variation in cycle efficiency, the net station output is different for each heat balance. Reheat cycles for a conventional plant operating at 2400 psig would probably utilize a higher feedwater temperature than 450°F, but in this application the higher feedwater temperature would require raising the steam generator exit-

¹V. P. Buscemi et al., "Gas Cooled Reactor Steam Cycle Evaluation," USAEC Report WCAP 1970, Westinghouse Electric Corporation, May 1, 1962.

gas temperature above 550°F. It was felt that the higher gas temperature at the suction of the blowers would require an excessive amount of pumping power and would result in too high a temperature at the inlet to the reactor. The optimum value for this temperature has not been determined. It may be either lower or higher than this assumed number.

Heat balance diagrams for the four basic cycles are given in Figs. 12.1-12.4 and given in Fig. 12.5 for the alternate cycle.

A comparative summary of the results is given in Table 12. 1. Although the minimum heat rate, and consequently the highest overall efficiency, was attained for the alternate to cycle 4, in which all extraction steam for three high-temperature feedwater heaters was taken from the blower drive turbines, that cycle was not selected as a basis for the plant layout because of the increased amount of piping required and the larger number of heaters required per stage of feedwater heating. Also, the minimum cost per kilowatt for the equipment included in the study corresponded to that for cycle 4.

Since the helium blowers and boiler feed pumps are steam-turbine-driven and since these turbines are included in the heat balances for the system, the power outputs shown are net power output for the plant. Also, since compressor work adds approximately 92 Mw of heat to the gas, the total heat input required from the reactor to provide a total heat supply of 2000 Mw for the system is only 1908 Mw; so the net thermal efficiency of the plant as a whole is slightly higher than the thermal efficiency based on the heat balance diagram for the steam system alone.

The 2400-psi, 950°F/1000°F turbine-generator selected for this study is a cross-compound unit, as illustrated in Fig. 12.6. It will have a double-flow high-pressure and a double-flow intermediate-pressure element on a 3600-rpm shaft. The low-pressure unit will consist of two identical double-flow, low-pressure elements with 44-in. last-stage blades operating at 1800 rpm. The unit is described more completely in Table 13.1.

Fig. 12.1. Heat Balance Diagram for Cycle No. 1. A (ABS) = absolute; B.F. = boiler feed; DC = drain cooler; F = °F; FTT = total temperature, °F; GC = gland condenser; GLD. STM = gland steam; GV = governor valve; h = enthalpy condensate, Btu/lb; H = enthalpy steam, Btu/hr; HTR = heater; I&A = air ejector inter- and after cooler; IV = intercept valve; PF = power factor; RSV = reheat shutoff valve; STM = steam; TV = throttle valve.

Fig. 12.2. Heat Balance Diagram for Cycle No. 2.

Fig. 12.3. Heat Balance Diagram for Cycle No. 3.

Fig. 12.4. Heat Balance Diagram for Cycle No. 4.

Fig. 12.5. Heat Balance Diagram for Alternate Cycle, No. 4A.

Table 12.1. Steam Cycle Results^a

	Steam Cycle Number				
	1	2	3	4	4A ^b
Throttle pressure, psia	1465	1815	1815	2415	2415
Throttle temperature, °F	1000	1000	950	950	950
Reheat temperature, °F			1000	1000	1000
Generator output, Mw(e)	718.1	729.4	750.0	763.0	766.0
Heat rate, Btu/kwhr	9501	9351	9094	8937	8913
Cycle efficiency, %	35.9	36.5	37.5	38.2	38.3
Plant efficiency = Mw(e)/1908	37.6	38.2	39.3	40.0	40.1
Cost of steam power cycle equipment and blower drives					
Turbogenerator	\$18,080	\$18,070	\$18,570	\$16,400	\$16,460
Heaters and condensers	2,976	3,558	3,594	3,594	3,381
Pumps and drive turbines	<u>1,145</u>	<u>1,333</u>	<u>1,239</u>	<u>1,568</u>	<u>1,568</u>
	\$22,201	\$22,961	\$23,403	\$21,562	\$21,409
Blower drive turbines	<u>2,800</u>	<u>2,800</u>	<u>2,800</u>	<u>2,800</u>	<u>3,312</u>
Total	\$25,001	\$25,761	\$26,203	\$24,362	\$24,721
Cost per net kw(e)	\$34.82	\$35.32	\$34.94	\$31.93	\$32.27

^aHeat input to all cycles: 1908 Mw from the reactor; 92 Mw from the blowers.

^bCycle 4A employs extraction from blower turbines for feedwater heating instead of extraction from main turbine.

Fig. 12.6. Proposed Westinghouse GCR-3 Turbine.

Guaranteed gross capability of the unit will be 763 Mw, with exhaust at 1.5 in. Hg absolute. Maximum expected capability will be 763 Mw at 3.5 in. Hg absolute. The turbine net heat rate will be 8937 Btu/kwhr.

The generators have a combined rating of 788.8 Mw at 45-lb hydrogen pressure and 869.7 Mw at 60-lb hydrogen pressure. The load at the guarantee point will be divided 55% on the high-pressure machine and 45% on the low-pressure machine. General specifications for each generator will be 24,000 v, 3-phase, 85% power factor, 60 cps, and a short-circuit ratio of 0.64 at 45-lb hydrogen pressure. Both rotors will be hydrogen-cooled and the stators will be water-cooled by a closed-circulation system. The overall length for the high-speed machine is 93 ft 1 in. and for the low-speed unit is 120 ft 7 in. Maximum turbine width is 31 ft 11 in. for the low-pressure turbines.

Turbine-generators of capabilities in the range of this study have not been built, but units having capacities in excess of 1000 Mw are on order for 2400-psi, 1000°F/1000°F design.² The turbine selected for this study exceeds by about 150 Mw the present largest unit in operation.³

At the present state of development of low-pressure exhaust blades, the cross-compound design is required for units above 500-Mw capability. This design has been proved by industry to be highly reliable. Extensive service has been accumulated on units having up to 500-Mw ratings.⁴

The selection of a superheat temperature of 950°F is expected to effect a significant saving in materials both in the high-pressure turbine and in the steam generator superheater. However,

²"New Record Set for World's Largest Turbine," Combustion 33(4): 50 (October 1961).

³"Steam-Electric Plant Construction Cost and Annual Expenses," U. S. Government Printing Office Document, F.P.C. S-149, 13th Annual Supplement, Federal Power Commission, 1960.

⁴Philip Sporn, "Tanners Creek 600 Mw Design," Electrical World 156(18): 60-63 (October 1961).

a detailed economic study has not been prepared. It is expected that material saving results because allowable stresses are increased considerably. The 1800-rpm turbine blading design has had very favorable experience in industry.⁴

The steam cycle study was based on using a deaerating main condenser instead of a deaerating feedwater heater. Therefore in the heat balance diagrams (Figs. 12.1 through 12.3) all feedwater heaters are shown as the closed type. In the principal piping system (Fig. 12.7) a deaerating heater is used for the No. 3 feedwater heater with a full-flow polishing demineralizer added. This provision ensures the feedwater purity required for the once-through steam generators, and the heat balance for the deaerating heater will be the same as that for the closed heater. Makeup for water losses is supplied by a separate demineralizer, which is also used as a source of water for the two shutdown coolers which generate and exhaust steam at atmospheric pressure when the power system is shut down. Makeup is 100% for the coolers (see Chap. 7).

The turbine main condenser serves as a heat sink for the main steam generator during startup, shutdown, or any time it is desired to avoid water loss. Relief of steam overpressure is made through safety valves directly to the atmosphere.

Fig. 12.7. Power Plant - Flow Diagram, Principal Piping Systems.

13. PLANT DESIGN

The design of the entire plant outside the containment vessels was performed by the Tennessee Valley Authority. The structural design and cost estimates for the containment and shield were also done by TVA. A summary of site and plant data is given in Table 13.1 and the general plant arrangement is shown in Figs. 13.1-13.8.

Service building requirements and annual operating costs were determined by the TVA EGCR Operating Department. Table 13.2 shows the organization chart assumed for the GCR-3 operation. The following assumptions were made in establishing the service requirements:

1. The essential functions are organized in a manner consistent with conventional station practice.
2. The reactor is operated strictly as a power producer on base load.
3. The plant has reached an equilibrium or steady-state condition, the break-in or startup phase has been completed, and the major technical and mechanical operating difficulties have been eliminated.
4. Day-shift maintenance coverage is provided; supplemental personnel will be provided from outside the station for major overhaul when required.
5. The trades and labor assignments are based upon AFL practice.
6. The standard work week is 40 hr, and the staff size is large enough to provide coverage for vacations and sickness.
7. Services such as film badge processing, portable instrument calibration, and radiobiological analysis are performed under contract.
8. The annual replacement cost for helium assumes 1 1/3 charges per year at \$37.50 per 1000 ft³ (STP) and is included in the enclosed cost.

Design of the service building provides the following space requirements:

Machine shop, includes welding, jig room, and office	6,750 ft ²
Steamfitter shop	1,600
Boiler shop, jig room included	1,000
Paint shop, includes spray booth	1,000

13.2

Electric shop, includes jig room	2,280 ft ²
Main instrument shop	1,750
Instrument shop near control room	100
Instrument shop near reactor	100
Toolroom	700
Upper storeroom	9,200
Lower storeroom	38,400
Storeroom office	660
Health-physics field office	480
Fuel storage vault, adjacent to charge face	2,600
Fuel-handling room	400
Laundry	400
Plant Staff offices	9,000
Assembly, first-aid, lunch, toilet, and locker rooms	<u>6,000</u>
	82,420 ft ²

The estimated annual operating costs, excluding all fuel charges, are the following:

Salaries (160 employees)	\$1,264,000
Operating supplies and expense	176,000
Maintenance materials and supplies	<u>600,000</u>
	\$2,040,000

The design basis for the containment vessel for the steam generators is the following:

Configuration	Arch with fixed supports at heavy-base slab
Structure	Concrete shell lined with 3/8-in.-thick steel plate all-welded and air-tight
Loading conditions	
No. 1	Dead load of shell plus snow load on part of top surface
No. 2	Bursting pressure, 60 psi
Shell concrete	2 ft thick, increased to 5 ft at supports

Shell reinforcement	
Transverse	2 circular cages, each with No. 11 bars at 6 in. on center
Longitudinal	2 layers of No. 9 bars spaced 12 in. on center
Allowable stresses	
Loading condition No. 1	Normal
Loading condition No. 2	27,000 psi in both reinforcing and steel plates, which are assumed to act together in resisting the bursting pressure

Table 13.1. Summary of Site and Plant Data

Tentative location: on right shore of Chicamauga Lake on Tennessee River mile 528.3±, approximately 1.6 miles downstream from Watts Bar Dam; known as the Yellow Creek site

GENERAL DATA

Access	
Highway	0.5 mile to be constructed from Highway 68
Railroad	0.75 mile to be constructed from existing TVA access railroad (8.1 miles from Southern Railway near Spring City) to Watts Bar Steam Plant
Water	On 9-ft navigable channel connected with Inland Waterway System
River Stages	
Maximum elevation	718.0 ft
Minimum elevation	675.0 ft
Construction Quantities	
Excavation (unclassified)	1,013,200 yd ³
Reinforced concrete	73,600 yd ³
Backfill	81,100 yd ³

Table 13.1 (continued)

POWERHOUSE, SERVICE BAY, AND ADMINISTRATION WING

Structure

Foundation

Material	Shale rock
Allowable bearing pressure	
Average	10 tons/ft ² over large areas
Maximum	20 tons/ft ² under heavy loads

Type of construction

Substructure	Reinforced concrete
Superstructure	Structural steel

Principal dimensions

Turbogenerator bay	234 × 92 ft with center line of turbines 24 ft above grade
Heater bay	32 × 300 × 70 ft above grade
Reactor bay	260 × 56 × 95 ft above grade
Steam generator containments	200-ft × 68-ft diameter with center line 36 ft above grade
Service bay	314 × 102 × 26 ft above grade

Architecture

Reactor bay

Exterior

Walls	Precast concrete
Windows	Aluminum with heat-absorbing glass
Roof	Built up
Exposed steel	Frames and fasciās

Interior

Walls	Glazed tile wainscot with exposed precast panels above
Floors	Quarry tile
Ceiling	Exposed structure

Steam generator container

Exterior

Interior

Walls	Reinforced concrete
Floor	Steel shell, painted
	Quarry tile and steel grating

Heater bay

Exterior

Walls	Precast concrete
Windows	Aluminum with heat-absorbing glass
Frame	Structural steel, painted
Roofs	Built up
Floors	Concrete and steel grating

Table 13.1. (continued)

Interior	
Control room	
Walls	Plastered
Floors	Vinyl asbestos tile
Ceilings	Acoustical tile
Other rooms	
Walls	Glazed tile
Floors	Concrete
Ceilings	Exposed structure
Service bay and administration wing	
Exterior	
Walls	Precast concrete panels
Windows	Aluminum with heat-absorbing glass
Exposed steel	Frames, fascias, and mullions, painted
Roof	Built up on insulated steel deck
Entrance	Aluminum-framed doors with heat-absorbing glass
Interior	
Lobby	
Walls	Wood paneled
Floor	Terrazzo
Ceiling	Acoustical tile
Stairway	Precast terrazzo treads, wood hand-rail, and steel railing
Lockers, toilets, and showers	
Walls	Glazed tile
Floors	Ceramic tile
Ceilings	Plaster
Laboratories, health unit, and lunchrooms	
Walls	Glazed tile
Floors	Vinyl asbestos
Ceilings	Acoustical tile
Corridors	
Walls	Glazed tile
Floors	First floor, colored cement; second floor, vinyl tile
Ceilings	Acoustical tile
Offices	
Walls	Plaster
Floors	Vinyl tile
Ceilings	Acoustical tile
Assembly and conference rooms	
Walls	Wood-paneled, with chalk and cork boards
Floors	Vinyl asbestos
Ceilings	Acoustical tile
Service bay (shops, etc.)	
Walls	Glazed tile
Floors	Colored metallic hardened concrete
Ceilings	Exposed structure

Table 13.1. (continued)

Turbogenerator Unit

Turbine

Type and speed

Cross-compound, quadruple flow,
3600-1800 rpm (parallel shafts)

Throttle pressure

2400 psig

Throttle temperature

950°F

Reheat temperature

1000°F

Extraction points

6

Back pressure

1.5 in. Hg abs

Turbine heat rate (at guaranteed maximum capability and 1.5-in. Hg exhaust pressure)

8937 Btu/kwhr

Generators

Ratings

High-pressure shaft

3,600-rpm, 512,000-kva, 0.85-power factor, 435,200-kw, 60-cycle, 3-phase, 24,000-v hydrogen inner-cooled generator

Low-pressure shaft

1,800-rpm, 416,000-kva, 0.85-power factor, 353,600-kw, 60-cycle, 3-phase, 24,000-v hydrogen inner-cooled generator

Maximum capability

High-pressure shaft

563,200 kva, 478,720 kw

Low-pressure shaft

457,600 kva, 388,960 kw

Cooling

Hydrogen, 45 psig at rated capacity;
60 psig at maximum capacity

Condensers (two)

Type

Single-pass, twin-bank, rectangular, deaerating, radial-flow surface condenser with divided waterboxes

Surface area

167,700 ft²

Steam condensed

1,434,504 lb/hr

Back pressure

1.5 in. Hg abs

Cooling water temperature

66°F

Cooling water flow

215,000 gpm

Tube outside diameter

7/8 in.

Tube length

34 ft

Tube material

Admiralty metal

Condensate pumps (three)

Type

Vertical, multistage, pit

Rated capacity

4823 gpm

Rated head

300 ft

Motor

450 hp, 1180 rpm

Condensate booster pumps (three)

Type

Centrifugal

Rated capacity

4823 gpm

Rated head

360 ft

Motor

250 hp

Table 13.1. (continued)

Feedwater Equipment

Feedwater heaters (two each)

Type

Closed, horizontal, two pass,
integral drain coolingShell design pressures and
temperatures

Heater No. 1	500 psig and 500°F
Heater No. 2	250 psig and 425°F
Heater No. 3	Deaerator (one only)
Heater No. 4	75 psig and 400°F
Heater No. 5	50 psig and 350°F
Heater No. 6	50 psig and 300°F

Tube design pressures and
temperatures

Heater No. 1	3400 psig and 500°F
Heater No. 2	3400 psig and 435°F
Heater No. 3	Deaerator
Heater No. 4	300 psig and 285°F
Heater No. 5	300 psig and 285°F
Heater No. 6	300 psig and 285°F

Boiler feedwater pumps (two each)

Type

Horizontal, centrifugal, 4 stages

Rated capacity, each

2,899,040 lb/hr

Rated head

3140 psi

Boiler feedwater pump turbines

Maximum power	14,500 hp
Design power	13,200 hp
Design speed	5200 rpm
Design speed range	1.5/1
Initial pressure	445 psia
Initial temperature	540°F
Exhaust pressure	1.5 in. Hg
Steam flow	107,077 lb/hr

Boiler feedwater pump turbine
condensers (two)

Type

Single-pass, twin-bank, rectangular
deaerating, radial-flow surface
condenser

Surface area	20,100 ft ²
Steam condensed	107,077 lb/hr
Back pressure	1.5 in. Hg abs
Cooling water temperature	66°F
Cooling water flow	14,500 gpm
Tube outside diameter	3/4 in.
Tube length	22 ft
Tube material	Admiralty metal

Table 13.1. (continued)

Boiler feedwater pump turbine condensate pumps (four)	
Type	Vertical, multistage, pit
Rated capacity	430 gpm
Rated head	40 ft
Motor	60 hp, 1780 rpm
Boiler feedwater booster pumps (three each)	
Type	Centrifugal
Rated capacity, each	2,899,040 lb/hr
Rated head	350 ft
Motor	400 hp
Internal water treatment	
O ₂ scavenger and pH control pumps	4 duplex-plunger type, 2 for the O ₂ scavenger and 2 for the pH control agent, 4 gph at 400-psi rated capacity; system complete with storage tanks and piping
Demineralizers	
Condensate polishing mixed bed system with prefilters, post filters, and external regeneration, capacity	9650 gpm
Makeup unit 2-bed system with oxygen removal unit, capacity	100 gpm
Gas Blower Equipment	
Gas blowers (four)	
Type	Axial-flow, 7-stage helium circulating blowers
Capacity	248,000 ft ³ /min
Suction pressure	400 psig
Pressure rise	24.3 psi
Helium temperature	550°F
Gas blower turbines (four)	
Type	Multistage single-flow, mechanical drive, condensing steam turbines
Horsepower	31,000
Speed	3600 rpm
Throttle pressure	445 psia
Throttle temperature	540°F
Steam flow	248,892 lb/hr
Exhaust pressure	1.5 in. Hg abs
Design speed range	5/1

Table 13.1. (continued)

Gas blower turbine condensers (four)	
Type	Single-pass, twin-bank, rectangular, radial-flow surface condenser with divided waterboxes
Surface area	24,000 ft ²
Steam condensed	248,892 lb/hr
Back pressure	1.5 in. Hg abs
Cooling water temperature	66°F
Cooling water flow	31,600 gpm
Tube outside diameter	3/4 in.
Tube length	24 ft
Tube material	Admiralty metal
Gas blower turbine condensate pumps (eight)	
Type	Vertical, multistage, pit
Rated capacity	500 gpm
Rated head	60 ft
Motor	100 hp, 1780 rpm
Miscellaneous Mechanical Equipment	
Compressed air facilities	
Compressors	One 610-ft ³ /min and three 250-ft ³ /min compressors, Y-type, 2-stage, at 100-psig discharge, V-belt driven, with aftercoolers
Receivers	Two 266-ft ³ vertical steel tank receivers, one for station service and one for control air
Oil purification facilities	
Lubricating oil	Two oil transfer pumps, one for dirty oil and one for clean oil, 100 gpm at 80-psi total developed head; one stationary centrifuge- type purifier; dirty and clean oil storage tanks with total storage capacity of 36,000 gal
Insulating oil	One oil transfer pump 100 gpm at 70-psi total developed head; one stationary centrifuge-type purifier; dirty and clean oil storage tanks with total storage capacity of 42,500 gal
Elevators	
Operator	One in heater bay, 2000-lb capacity at 200 ft/min
Cranes	
Turbine bay	125-ton gantry type
Reactor bay	100-ton overhead type
Service bay	10-ton overhead (machine shop)

Table 13.1. (continued)

House boilers (two)

Type	Packaged unit
Capacity	100,000 lb/hr
Operating pressure	650 psig
Operating temperature	550°F

House boiler feedwater pumps (three)

Type	Horizontal centrifugal
Capacity	200 gpm
Head	700 psig
Motor	100 hp, 3600 rpm

Miscellaneous pumps

Fuel oil transfer	One 200 gpm at 25 psig
Fuel oil supply	One 50 gpm at 200 psig
Raw water cooling	Six 6000 gpm at 120 ft
Vacuum priming	Two 130 ft ³ /min at 20 in. Hg
Condenser vacuum	Three 1800 ft ³ /min at 15 in. Hg
Distilled water	One 3500 gpm at 400 ft
Raw water service	Two 200 gpm at 325 ft
Station sump	Two 1000 gpm at 65 ft
Condensate drain tank	One 175 gpm at 80 ft
Fire protection	Two 1250 gpm at 350 ft

Piping systems

Complete with pipe, valves,
fittings, accessories,
hangers, and insulation

Main steam, extraction steam,
boiler feedwater, condensate,
heater drains, heater vents,
turbine vents, raw water cooling,
raw water service, service air,
control air, fire protection,
lubricating oil, insulating oil,
gland seal, vacuum priming,
plumbing, chemical feed, circu-
lating water, and drainage

Mechanical control equipment

Principal features

Centralized control for turbo-
generator unit and accessory
steam plant equipment from the
unit control room, including
boiler feedwater controls, flow-
metering equipment, temperature
recorders, conductivity and pH
recorders, thermometers, pressure,
flow, and level recorders, annun-
ciators, electrical switches,
ammeters, megawatt meters, indi-
cating lights, etc.

Table 13.1. (continued)

Heating, ventilating, and air conditioning

Air-conditioning units

Control room	Three at 15 hp = 45
Service bay and offices	One at 20 hp = 20
Service bay and offices	One at 15 hp = 15
Service bay and offices	Six at 10 hp = 60
Service bay and offices	Two at 7 1/2 hp = 15
Service bay and offices	One at 5 hp = 5
	<u>160 hp</u>

Supply and exhaust fans

Battery room	Two for 10,000 ft ³ /min
Electrical board room	Six for 30,000 ft ³ /min
Service bay and offices	Fifteen for 140,000 ft ³ /min
	<u>180,000 ft³/min</u>

Heating boiler

One required 150 hp

Blast heating coils

14 for 1,500,000 Btu

Unit heaters

12 for 720,000 Btu

Electric heaters

9 for 24 kw

Principal Electrical Equipment

Generator bus

Welded aluminum construction, manufacturer's isolated-phase type, 65°C rise over 40°C ambient, with closed forced-air-cooling system with heat exchanger, and potential transformer and surge protective equipment; high-pressure generator bus 14,500 amp; low-pressure generator bus 12,000 amp

Station service transformers

Unit station service

Two 22.5-4.16 kv, 10,000-12,500 kva, OA/FA, 3-phase

Common station service

Two 161-4.16 kv, 12,000-15,000 kva, OA/FA, 3-phase

Station service buses

Unit station service

Weatherproof, enclosed, 4.16 kv, 2000 amp, 3-phase

Common station service

Weatherproof, enclosed, 4.16 kv, 2000 amp, 3-phase

Auxiliary board transformers

Eight 4160-480 v, 1000 kva, 3-phase

Five 4160-480 v, 750 kva, 3-phase

Two 4160-480 v, 500 kva, 3-phase

Lighting board transformers

Three 480-240/120 v, 250 kva, 1-phase

Two 480-240/120 v, 100 kva, 1-phase

One 480-240/120 v, 50 kva, 1-phase

Table 13.1. (continued)

Powerhouse, 4.16-kv unit and common auxiliary switchboards	
Breaker ratings	1200- and 2000-amp, 250,000-kva interrupting capacity
Powerhouse, reactor building, and miscellaneous 480-v main auxiliary boards	
Breaker rating, incoming feeder and tie	1600-amp, 50,000-kva interrupting class
Feeder breakers for large motors or selective tripping	600-amp, 25,000 kva interrupting class
Other feeder breakers	225-amp, 15,000-kva interrupting class
Powerhouse, reactor building, and miscellaneous 480-v control centers and lighting boards	
Breaker ratings	Moulded case type, 100- and 225-amp, 15,000-kva interrupting capacity
Batteries	Two main control batteries, 250 v, 664 amp-hr at 8-hr rate to 1.75 v per cell; one annunciator battery, 125 v, 240 amp-hr at 8-hr rate to 1.75 v per cell; one telephone battery, 46 v, 160 amp-hr at 8-hr rate to 1.75 v per cell
Battery chargers	Three main control battery chargers, silicon rectifier type, input 480 v a-c, output 100 amp, 260-280 v d-c; one annunciator battery charger, rectifier type, input 480 v a-c, output 129 v d-c, 5 kw, 40 amp; one telephone battery charger, rectifier type, input 120 v, single-phase, output 12 amp, 48 v
Emergency M-G set	Two 25 kw, 120 v a-c, 40 hp, 250 v d-c
Electrical control switchboard	Bench-type panels with mimic bus, control switches, and indicating lights for generators, transformers, and outgoing lines (main transformers and switchyard equipment not included in estimate); vertical panels for instruments, relays, temperature recorders, load control, and d-c distribution

Table 13.1. (continued)

Emergency diesel generators	Two 200-kw, 480-v, 3-phase, 60-cycle generators, driven by diesel engines, complete with controls for automatic startup
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WATER SUPPLY

Pumps	Six circulators at 102,600 gpm each at 35-ft head, 1250 hp each; three boosters at 52,000 gpm each at 20-ft head, 400 hp each; three screen wash pumps at 1050 gpm each at 240-ft head, 100 hp each
Tunnels	Main tunnel 14 ft square; auxiliary to boiler feed pump turbine 7 ft square; auxiliary to gas blower turbine 4.5 ft square
Butterfly valves	Condenser circulating water pumps; main, 66 in.; auxiliary, 42 in.
Condensers	Main inlet 84 in., main outlet 72 in., boiler feed pump turbine condenser 24-in. inlet and outlet, gas blower turbine condenser 30-in. inlet and outlet
Pumping station design elevations	Deck elevation, 720.0 ft; mechanical equipment and electrical equipment floor elevation 707.0; valve room floor elevation 679.5; pump pit elevation 653.0; center-line discharge elevation 683.25; top of tunnel 686.0; invert of 14-ft-square tunnel 672.0
Water-Treatment Plant	
Primary treatment	Flocculation, settling, and filtration; capacity, 150 gpm

Table 13.1. (continued)

<u>YARD</u>	
Roadways and parking areas	Stabilized crushed stone base with asphaltic concrete surface
Sidewalks	Concrete 4 in. thick
Concrete curbs	At main entrance to service bay and in parking area
Fence	Protective type around entire plant
Piping systems	Raw water fire protection and service including hydrants, compressed-air service, building drain lines to storm sewers, storm drainage system, and sewage drain lines
Sewage disposal plant	Extended aeration, system capacity, 16,000 gal/day

CONSTRUCTION PLANT BUILDINGS

Buildings (Incomplete list, special buildings only)

Type	Prefabricated metal with concrete floors including interior partitions and building services (exclusive of equipment)
Pipe fabricating shop	50 by 100 by 15 ft high (insulated)
Instrument shop	20 by 20 by 12 ft high (insulated)
Graphite inspection and storage	50 by 100 by 12 ft high
Fuel element inspection and storage	20 by 20 by 12 ft high
Radiograph inspection	20 by 60 by 15 ft high
Pipe and equipment cleaning area	100 by 200 by 30 ft high

Fig. 13.1. General Plan.

Fig. 13.2. Equipment Plan at 700-ft Elevation.

Fig. 13.4. Equipment Plans at 740-, 760-, 775-, and 790-ft Elevations.

Fig. 13.5. Transverse Section A-A of Equipment Plans.

Fig. 13.6. Longitudinal Sections B-B and C-C of Equipment Plans.

Table 13.2. Operating Organization Chart

14. GCR-3 COST ANALYSIS

One of the major objectives of the GCR-3 study is to establish a basis for predicting the power costs attainable from this reactor system. Studies in 1958 by Kaiser Engineers¹ and ORNL² of smaller reactor systems [250 and 225 Mw(e) net] showed that a range between 12 and 13 mills/kwhr could be anticipated based on 1958 technology. It was recognized then that the cost evaluation would be very sensitive to the

1. total capacity of the power plant,
2. thermal efficiency of the system,
3. power density in the core,
4. engineering of certain key components of the nuclear system, including reactor pressure vessel, steam generators, and graphite-moderator structure,
5. utilization of the most modern steam turbine equipment for the application,
6. fuel fabrication costs and fuel lifetime,
7. method of fuel loading, with the additional capital costs being balanced against potential improvements in fuel lifetime.

The GCR-3 design, in its provisions for higher electrical outputs, higher power densities in the core, and much more advanced steam conditions, includes some of the engineering insight reflected in the EGCR from the 1958 studies. The GCR-3 further considers the favorable developments in fuel technology of EGCR-type systems and thus takes into account longer potential fuel lifetimes and the low fabrication costs

¹Kaiser Engineers and Nuclear Products, Division of ACF Industries, Inc., "Gas-Cooled Power Reactor Feasibility Study, Optimum Partially Enriched Uranium Nuclear Power Plant," USAEC Report IDO-2024 (Rev. 1), April 1, 1958.

²"The ORNL Gas-Cooled Reactor," USAEC Report ORNL-2500, Part 4, Section 11, Oak Ridge National Laboratory, April 1, 1958.

of the EGCR fuel. Finally, the GCR-3 design makes allowances for the very costly development in some engineering features of the EGCR while retaining portions of the EGCR concept that appear to be economically attractive. The power generation costs, based on AEC current rules³ for estimating, are given in Table 14.1.

Capital Costs

The capital cost summary for the reactor system is given in Table 14.2, and a detailed cost breakdown is presented in Table 14.3. A serious effort was made to develop reliable cost data for those items on which the evaluation of capital investment was crucially dependent. The method by which major cost features of the nuclear portion of the plant were established for the GCR-3 is as follows:

The Reactor Vessel. - Combustion Engineering made a thorough engineering study of the vessel and then put the design through their normal cost estimating process. The cost estimate therefore reflects the manufacturing costs that are to be expected on firm bid proposals.

Steam Generators. - The cost estimate for the steam generators was prepared by Combustion Engineering by the same process as above, and reflects the price to be expected from a firm bid proposal.

Compressors and Steam Turbine Drives. - The cost of this equipment was developed in studies by Westinghouse under subcontract with ORNL. Advantage was taken of the information that Westinghouse had developed through its own experience on gas turbines and compressors, together with that obtained in the development program on gas turbines for the Maritime Gas-Cooled Reactor Program.

Graphite Core Structure. - The cost estimate for fabrication of the graphite core structure was obtained directly from National Carbon Company, supplier of the EGCR graphite, and represents approximately \$2.50 per pound of finished graphite material, \$0.50 a pound higher

³Kaiser Engineers, "Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025, March 15, 1962.

Table 14.1. Power Generation Costs

	Capital Cost	Rate (%)	Annual Cost	Unit Cost (mills/kwhr) ^a
Fixed Charges				
Depreciating Capital				
REA	\$150,039,000	6.0	\$ 9,002,340	1.715
Municipal	156,500,000	7.7	12,050,500	2.295
Investor-owned	162,961,000	14.5	23,629,345	4.501
Nondepreciating Capital				
Land and land rights	500,000			
Working capital	3,170,570			
REA	3,670,570	3.2	117,458	0.022
Municipal	3,670,570	5.5	201,881	0.038
Investor-owned	3,670,570	13.0	477,174	0.091
Annual nuclear liability insurance			320,000	0.061
Total Fixed Charges				
REA				1.798
Municipal				2.394
Investor-owned				4.653
Operating Costs				
Operating and maintenance			2,040,000	0.389
Fuel cost				1.460
Total power generation costs				
REA				3.647
Municipal				4.243
Investor-owned				6.502

^aBased on total power output of 5.25×10^9 kwhr/year.

Table 14.2. Summary of Capital Cost for GCR-3

Account Number		Total Cost
Direct Construction Cost		
21	Structures and improvements	\$ 12,968,000
22	Reactor plant equipment	65,643,000
23	Turbine-generator units	25,374,000
24	Accessory electric equipment	4,532,000
25	Miscellaneous power plant equipment ^c	1,250,000
	Total	\$109,767,000
Indirect Construction Costs		
	General and administrative (5.8% ^a)	6,366,000
	Subtotal	\$116,133,000
	Miscellaneous construction costs (0.6% ^b)	700,000
	Subtotal	\$116,833,000
	Engineering, design, and inspection	
	Architectural and engineering services (7.0% ^b)	8,178,000
	Subtotal	\$125,011,000
	Nuclear engineering ^b	4,300,000
	Subtotal	\$129,311,000
	Startup costs	714,000
	Subtotal	\$130,025,000
20	Land and land rights	500,000
	Subtotal	\$130,525,000
	Contingency (10%)	13,053,000
	Subtotal	\$143,578,000
	Interest cost during construction	
	REA, 4.5%	\$ 6,461,000
	Municipal, 9.0%	\$ 12,922,000
	Investor-owned, 13.5%	\$ 19,383,000
	Total capital cost	
	REA	\$150,039,000
	Municipal	\$156,500,000
	Investor-owned	\$162,961,000
	Unit capacity cost [per kw(e)]	
	REA	\$200.1
	Municipal	\$208.6
	Investor-owned	\$217.2

^aValue taken from Fig. A-1 and applied to total direct construction cost.^bValue taken from Fig. A-1 and applied to preceding subtotal.

Table 14.3. GCR-3 Detailed Cost Estimate

Cost Code	Description and Materials	Total
20	Land and Land Rights	
201	Land and privilege acquisition	
202	Relocation of roads and railroads	
203	Relocation of telephone and power lines	
204	Site and area surveys	
		<u>\$ 500,000</u>
21	Structures and Improvements	
211	Ground Improvements	
.1	Access roads	
.2	General yard improvements	
.3	Railroads	\$ 931,000
.4	Intake and discharge channels	<u>250,000</u>
		<u>\$ 1,181,000</u>
212	Buildings	
212A	Power House — Building	
.11	Excavation	
.12	Backfill	\$ 323,000
.13	Foundation treatment	125,000
.14	Dewatering	100,000
.31-.33	Substructure	1,150,000
.34	Waterproofing — joints, stops	34,000
.4	Superstructure	<u>4,600,000</u>
		<u>\$ 6,332,000</u>
212B	Spent Fuel Handling Structure	\$ 300,000
212C	Helium Storage — Slab and Fence	\$ 110,000
212D	Reactor Stack and Blower House	\$ 100,000
212E	House Boiler Stack	\$ 65,000
212F	Water Intake and Discharge Structures	\$ 715,000
219	Reactor Container Structures	
.3	Biological shield and blower foundations	\$ 948,000
.41	Concrete containment vessels	2,217,000
.42	Biological shield hatch	100,000
.62-.64	Heating and cooling systems	500,000
.68	Inert gas system	<u>400,000</u>
		<u>\$ 4,165,000</u>
	Total for Account 21	<u>\$12,968,000</u>

Table 14.3 (Continued)

Cost Code	Description and Materials	Total
22	Reactor Plant Equipment	
221	.1 Reactor Vessel	
	.11-.12 Vessel fabricated and erected	\$ 4,000,000
	.13 Insulation — interior	214,000
	.14 Insulation — exterior	20,000
	.15 Core support — fabrication	240,000
	.16 Core support — erection	40,000
	.17 Core lateral support	400,000
		<u>\$ 4,914,000</u>
	.2 Reactor Controls	
	.21 Control rods	\$ 355,000
	.22 Drive blisters	426,000
	.23 Drive mechanisms	3,190,000
	.24 Shield plugs	142,000
	.25 Heat dams	53,000
		<u>\$ 4,166,000</u>
	.6 Graphite Core	
	.61 Graphite structure	\$ 3,700,000
	.62 Core gas seals and orifice assembly	530,000
	.63 Core erection	200,000
		<u>\$ 4,430,000</u>
	.7 Dummy Fuel Assemblies	
	.71 Bottom dummy assemblies	\$ 213,000
	Total for Item 221	\$13,723,000
222	Heat Transfer System	
	.1 Primary Coolant System	
	.11 Blowers and auxiliaries (erected)	\$10,736,000
	.12 Main coolant piping (erected)	2,720,000
		<u>\$13,456,000</u>
	.2 Emergency Coolant System	
	.22 Coolant piping (erected)	\$ 1,100,000
	.23 Heat exchanger	2,475,000
	.3 Steam Generators	\$ 3,575,000
	.31 Steam generators	\$14,400,000
	.32 Steam generators (erection)	450,000
		<u>\$14,850,000</u>

Table 14.3 (Continued)

Cost Code	Description and Materials	Total
22	Reactor Plant Equipment (continued)	
222	Heat Transfer System (continued)	
	.4 Coolant Charging and Discharging	
	.41 Helium transfer system	\$ 430,000
	.42 Evacuation system	200,000
	.43 Vent gas system	300,000
		<u>\$ 930,000</u>
	.5 Coolant Purification and Recovery Systems	
	.51 Low pressure leak-off recovery system	\$ 175,000
	.52 Chemical purification system	150,000
	.53 Oil removal system	125,000
		<u>\$ 450,000</u>
	.6 Coolant Storage Facility	
	.61 Pipe storage farm	\$ 570,000
	Total for Item 222	\$33,831,000
223	Fuel Handling and Storage Facilities	
	.1 Cranes and Hoisting Equipment	\$ 450,000
	.2 Fuel Assembly and Disassembly Equipment	
	.21 Fuel Charge and discharge machine	3,000,000
	.22 Fuel charge and discharge locks and shield	150,000
	.23 Fuel discharge chute, lock	100,000
	.24 Spent fuel carrousel	150,000
	.25 Spent fuel dry storage	50,000
	.26 Spent fuel dry storage cooling equipment	75,000
	.27 Wet fuel storage racks, inspection and dismantling facilities, and cooling	300,000
	.3 Control Rod Assembly and Disassembly Equipment	
	.31 Spent control assembly equipment	100,000
	.4 Spent Fuel and Rod Shipping Casks	
	.41 Spent fuel shipping casks	200,000
	.42 Spent rod shipping casks	25,000
		<u>\$ 4,600,000</u>
225	Radioactive Waste Treatment and Disposal	\$ 400,000
226	Instrumentation and Control	\$ 4,250,000

Table 14.3 (Continued)

Cost Code	Description and Materials	Total
22	Reactor Plant Equipment (continued)	
227	Feedwater Supply and Treatment Systems	
	.1-.2 Raw water treatment and supply system	\$ 1,710,000
	.3 Feedwater equipment	<u>1,918,000</u>
		\$ 3,628,000
228	Steam Condensate Feedwater Piping	\$ 4,911,000
229	Miscellaneous Reactor Plant Equipment	\$ 300,000
	Total for Account 22	\$65,643,000
23	Turbine-Generator Units	
231	Turbine-Generators	
	.1 Foundation	\$ 330,000
	.2 Turbine-generators	<u>18,468,000</u>
		\$18,798,000
232	Circulating Water System	
	.1 Equipment	\$ 2,246,000
	.2 Circulating lines	<u>770,000</u>
		\$3,016,000
233	Condensers	\$ 3,041,000
234	Central Lubricating System	\$ 109,000
235	Turbine Plant Boards Instruments and Controls	\$ 250,000
236	Turbine Plant Piping	\$ 100,000
237	Auxiliary Equipment for Generators	\$ 50,000
238	Other Turbine Plant Equipment	\$ 10,000
	Total for Account 23	\$25,374,000
24	Accessory Electric Equipment	
241	Switchgear	\$ 615,000
242	Switchboards	\$ 1,225,000
243	Protective Equipment	\$ 116,000
244	Electrical Structures	\$ 230,000
245	Conduit	\$ 817,000
246	Power and Control Wiring	\$ 748,000
247	Station and Service Equipment	\$ 781,000
	Total for Account 24	\$ 4,532,000

Table 14.3 (Continued)

Cost Code	Description and Materials	Total
25	Miscellaneous Power Plant Equipment	
251	Cranes and Hoisting Equipment	\$ 401,000
252	Compressed Air System	150,000
253	Other Miscellaneous Equipment	699,000
	Total for Account 25	\$ 1,250,000
	Total Direct Construction Cost	\$110,267,000

than the EGCR cost. This value is used despite the less severe dimensional requirements expected for the GCR-3 design so as to assure ample contingency for unforeseen engineering developments.

Reactor Controls. - Even though unit costs experienced on the EGCR are thought to be high for the control rods and drives because of the developmental nature of these components, they were used in the GCR-3 estimate. Engineering experience and a simplification of the EGCR drive concept made possible by the larger working space provided in the GCR-3 design should make the manufacturing cost of the GCR-3 control rod drives somewhat less than that of the EGCR. This cost estimate therefore is thought to be extremely conservative.

Fuel-Handling Facilities. - The current costs of the EGCR were used as a working basis for estimating the cost of the GCR-3 fuel-handling facilities, even though the GCR-3 machine is not required to store irradiated fuel, operate at the reactor operating pressure of 400 psig, or transport heavy shielding together with the fuel from the reactor to the storage point. Allowance was also made for the related components that must be provided to make the GCR-3 machine a workable device.

Instrumentation and Controls. - In the area of instrumentation and controls the GCR-3 design departed somewhat from the EGCR concept, partly to avoid some of the cost penalties associated with such equipment as the burst-slug-detection system, the PTM temperature monitors, and other miscellaneous in-core instrumentation but mainly to simplify the core physical structure through elimination of some of this instrumentation. The EGCR instrumentation and control costs are used as the basis for the GCR-3 estimate. This basis is believed to be a valid one because the instrumentation requirements are very insensitive to the power level. Any increase in complexity due to the increased power level should be more than offset by the simplifications mentioned.

Steam Power Equipment. - Since one of the goals of the GCR-3 design was to show that conventional steam power equipment of modern design could be used in a gas-cooled reactor application, it was possible to use steam power equipment costs equivalent to those for fossil-fuel plants. The estimates for the specific operating equipment were made by

Westinghouse Electric Corporation and are known to be 15 to 25% higher than the current quoted prices for this equipment when purchased in open competition. They are therefore quite conservative. The piping runs were estimated from the Tennessee Valley Authority's cost experience and were examined in sufficient detail to assure their equality with comparable fossil-fuel installations.

Structures and Improvements. - The cost of structures and improvements, including buildings, yard facilities, reactor-containment structure, and concrete shielding, was estimated by TVA on the basis of their construction cost experience on similar structures. Since EGCR experience has shown that estimates on this type of construction are reliable for gas-cooled reactor systems, these costs are believed to be well-founded.

The limitation, if any, of this cost summary stems from the inability to account for all items of equipment without completely engineering the plant. However, it is believed that experience on the EGCR provided sufficient guidance for the cost allowance for the miscellaneous features to be at least fairly accurate.

The conclusion drawn from this cost estimate is that the direct cost of the 750-Mw(e) GCR-3 plant will not exceed \$109,767,000 (exclusive of land costs), or \$146.4 per kilowatt (electrical). As was pointed out, this cost estimate is thought to be conservative. The following areas seem to be amenable to cost reduction through technological advancement, design simplification, or broader cost competition:

Pressure Vessel. - The estimated fabrication cost of the reactor vessel is approximately \$2.25 per pound, exclusive of the internals. Carbon steel of the same thickness has been fabricated at two-thirds this cost in nonnuclear applications, and many new fabrication techniques for cutting costs are in use. These include the new Electroslag welding technique being used by the British on nuclear vessels, field application of submerged arc welding, and advancements in radiography and ultrasonic inspection techniques that reduce labor costs for reactor vessel applications.

Control Rods and Drives. - The cost basis used here is that obtained for each unit in the EGCR, even though experience in the United Kingdom and in the United States in water reactor systems indicates that the value used here can be reduced by approximately one-third by design simplifications.

Steam Generators. - The design presented here includes conservative additions of 25% excess surface and an extraordinarily conservative 1/16-in. corrosion allowance for the superheater tubing; therefore refinement of the design will bring about significant cost reductions.

Graphite Core. - It appears likely that the unit cost of \$2.50 a pound developed by National Carbon Company, which is about 25% higher than that for the very complex EGCR design, can be reduced to at least \$1.50 per pound once the design has been given a complete manufacturing study for methods of cost reduction.

Control System. - The EGCR control system cost, which was used as the estimating basis, includes much experimental instrumentation, many complicated coincident and redundant safety devices, and a large number of temperature-sensing points for informational purposes. Since the GCR-3 design is predicated on the reactor performance being established prior to construction and a plan for public safety to take the burden off the reactor controls, the control system is expected to be greatly simplified in comparison, and the costs should thus be reduced substantially.

Conventional Steam Equipment. - The catalog prices were used for the steam turbines, condensers, and other equipment designed by Westinghouse and would naturally be expected to be 15 to 25% less when the items are priced competitively.

Fuel Handling. - Since fuel-handling-machine costs are those developed for the very expensive EGCR on-load scheme, cost reductions for the simplified shutdown scheme should be realized.

These considerations led to a second estimate of the cost of the GCR-3 and to a somewhat more optimistic basis than that used initially. The revised (optimistic) cost is compared in Table 14.4 with the base estimate given in Table 14.3 and shows a direct cost for a 750-Mw(e)

Table 14.4. Conservative and Optimistic Direct Costs as a Function of Size

Account Number	Item	750-Mw(e) GCR-3		500-Mw(e) GCR-3		1000-Mw(e) GCR-3			Reason for Optimism	Size Effect	
		Conservative	Optimistic	% Change	Conservative	Optimistic	% Change	Conservative			Optimistic
20	Land and Land Rights	\$ 500,000	\$ 500,000		\$ 500,000	\$ 500,000		\$ 500,000	\$ 500,000	None	
21	Structures and Improvements										
	Ground improvements	1,181,000	1,181,000		1,181,000	1,181,000		1,181,000	1,181,000	Insignificant	
	Power house	6,332,000	6,332,000		6,332,000	6,332,000		6,332,000	6,332,000	Insignificant on building size because of reactor size change	
	Spent fuel handling	300,000	300,000		300,000	300,000		300,000	300,000	Independent of power level	
	Helium storage	110,000	110,000		110,000	110,000		110,000	110,000	Independent of power level	
	Reactor stack and blower house	100,000	100,000		100,000	100,000		100,000	100,000	Independent of power level	
	Boiler house stack	65,000	65,000		65,000	65,000		65,000	65,000	Independent of power level	
	Water intake and discharge structures	715,000	715,000		715,000	715,000		715,000	715,000	Independent of power level	
	Reactor shield and blower foundations	948,000	948,000	-8	870,000	870,000	+4	995,000	995,000	Changes with active core volume	
	Containment vessel	2,217,000	2,217,000	-33	1,470,000	1,470,000	+33	2,950,000	2,950,000	Directly proportional to power level	
	Heating and cooling system	500,000	500,000		500,000	500,000		500,000	500,000	Independent of power level	
	Biological shield hatch	100,000	100,000		100,000	100,000		100,000	100,000	Independent of power level	
	Inert gas system	400,000	400,000		400,000	400,000		400,000	400,000	Independent of power level	
	Subtotal	\$12,968,000	\$12,968,000		\$12,143,000	\$12,143,000		\$13,748,000	\$13,748,000		
22	Reactor Plant Equipment										
	Reactor vessel and internals	\$ 4,914,000	\$ 3,000,000	-29	\$ 3,480,000	\$ 2,130,000	+14	\$ 5,930,000	\$ 3,420,000	Improved fabrication methods will reduce costs 40%	$\text{GCR-3 cost} \left[\frac{\text{new vessel diameter}}{\text{GCR-3 vessel diameter}} \right]^3$
	Control rods and drives	4,166,000	1,460,000	-33	2,775,000	978,000	+33	5,520,000	1,942,000	EGCR cost of \$60,000 each will fall to \$20,000 each	Proportional to power level
	Graphite core structure	3,700,000	2,450,000	-30	2,618,000	1,715,000	+20	4,440,000	2,940,000	Estimate of \$2.50 is 25% over EGCR; should fall to \$1.50 per lb	$\text{Varies as } [\sqrt{\text{power level ratio}} \times \text{GCR-3 core diameter} + \text{reflector thickness}]^2$
	Core seals and orifices	530,000	530,000	-33	345,000	345,000	+33	705,000	705,000		Proportional to power level
	Core erection	200,000	200,000	-30	140,000	140,000	+20	240,000	240,000		Proportional to graphite weight
	Dummy fuel assemblies	213,000	213,000	-33	143,000	143,000	+33	285,000	285,000		Proportional to power level
	Primary blowers, drives, and auxiliaries	10,736,000	8,500,000	-33	7,193,000	5,695,000	+33	14,278,000	11,305,000	Repetitive design and competition will cut cost 15%	Proportional to power level
	Primary coolant piping	2,720,000	2,720,000	-33	1,822,000	1,822,000	+33	3,617,000	3,617,000		Proportional to power level
	Emergency shutdown cooling	3,575,000	2,230,000	-33	2,395,000	1,494,000	+33	4,755,000	2,966,000	Detailed design will show cost reduction in heat exchanger	Proportional to power level
	Steam generators	14,850,000	11,200,000	-33	9,950,000	7,504,000	+33	19,750,000	14,896,000	Elimination of 25% over design and corrosion allowance	Proportional to power level

Table 14.4 (Continued)

Account Number	Item	750-Mw(e) GCR-3		% Change	500-Mw(e) GCR-3		% Change	1000-Mw(e) GCR-3		Reason for Optimism	Size Effect
		Conservative	Optimistic		Conservative	Optimistic		Conservative	Optimistic		
22	Reactor Plant Equipment (continued)										
	Coolant transfer system	\$ 930,000	\$ 930,000		\$ 930,000	\$ 930,000		\$ 930,000	\$ 930,000		Independent of power level
	Coolant purification and recovery	450,000	450,000		450,000	450,000		450,000	450,000		Independent of power level
	Coolant storage	570,000	570,000		570,000	570,000		570,000	570,000		Independent of power level
	Fuel handling and control rod maintenance	4,600,000	2,600,000		4,600,000	2,600,000		4,600,000	2,600,000	Reduction of \$2,000,000 seen through elimination of on-load features, lower design pressure, and in vessel fuel transport	Independent of power level
	Radioactive waste disposal	400,000	400,000		400,000	400,000		400,000	400,000		Independent of power level
	Instruments and controls	4,250,000	2,000,000		4,250,000	2,000,000		4,250,000	2,000,000	EGCR used as basis, and costs will be cut by elimination of experimental requirements	Independent of power level
	Water treatment and supply	3,628,000	3,628,000	-21	2,866,000	2,866,000	+19	4,317,000	4,317,000		Varies as [power level] ^{0.6}
	Feedwater and condensate system	4,911,000	4,911,000	-21	3,880,000	3,880,000	+19	5,844,000	5,844,000		Varies as [power level] ^{0.6}
	Miscellaneous power plant equipment	300,000	300,000		300,000	300,000		300,000	300,000		Independent of power level
	Subtotal	\$65,643,000	\$48,292,000		\$49,107,000	\$35,962,000		\$81,181,000	\$59,727,000		
23	Turbine-Generator Plant										
	Turbine-generators	\$18,798,000	\$16,100,000	-25.7	\$13,967,000	\$11,962,000	+17	\$21,994,000	\$18,837,000	Prices used in conservative estimate are catalog, and competitive bidding has been consistently 15 to 25% under these values; a 15% reduction is claimed on equipment only	Varies with unit capacity
	Circulating water system	3,016,000	2,670,000	-21	2,388,000	2,114,000	+19	3,589,000	3,177,000		Varies as [power level] ^{0.6}
	Condenser	3,041,000	2,585,000		2,400,000	2,040,000		3,619,000	3,076,000		Varies as [power level] ^{0.6}
	Central lube system	109,000	109,000		109,000	109,000		109,000	109,000		Independent of capacity
	Turbine plant boards	250,000	250,000		250,000	250,000		250,000	250,000		Independent of capacity
	Turbine plant piping	100,000	100,000		100,000	100,000		100,000	100,000		Independent of capacity
	Auxiliary equipment	50,000	50,000		50,000	50,000		50,000	50,000		Independent of capacity
	Other equipment	10,000	10,000		10,000	10,000		10,000	10,000		Independent of capacity
	Subtotal	\$25,374,000	\$21,774,000		\$19,274,000	\$16,635,000		\$29,721,000	\$25,609,000		

Table 14.4 (Continued)

Account Number	Item	750-Mw(e) GCR-3		500-Mw(e) GCR-3			1000-Mw(e) GCR-3		Reason for Optimism	Size Effect
		Conservative	Optimistic	% Change	Conservative	Optimistic	% Change	Conservative	Optimistic	
24	Accessory Electrical Equipment									
	Switchgear	\$ 615,000	\$ 615,000		\$ 615,000	\$ 615,000		\$ 615,000	\$ 615,000	Independent of capacity
	Switchboards	1,225,000	1,225,000		1,225,000	1,225,000		1,225,000	1,225,000	Independent of capacity
	Protective equipment	116,000	116,000		116,000	116,000		116,000	116,000	Independent of capacity
	Electrical structures	230,000	230,000		230,000	230,000		230,000	230,000	Independent of capacity
	Conduit	817,000	817,000		817,000	817,000		817,000	817,000	Independent of capacity
	Power and control wiring	748,000	748,000		748,000	748,000		748,000	748,000	Independent of capacity
	Station and service equipment	781,000	781,000	-21	620,000	620,000	+19	935,000	935,000	Varies as [power level] ^{0.6}
	Subtotal	\$ 4,532,000	\$ 4,532,000		\$ 4,371,000	\$ 4,371,000		\$ 4,686,000	\$ 4,686,000	
25	Miscellaneous Equipment	\$ 1,250,000	\$ 1,250,000		\$ 1,250,000	\$ 1,250,000		\$ 1,250,000	\$ 1,250,000	Independent of power level
	Total direct cost (exclusive of land costs)	\$109,767,000	\$88,816,000		\$86,145,000	\$70,361,000		\$130,586,000	\$105,020,000	

plant of \$88,816,000 (exclusive of land costs), or \$118.4 per kilowatt (electrical). This latter value is thought to be more representative of the long-range potential of the system and should be used in appraising its merit.

Top Charges

The direct cost does not, of course, represent the final cost to the user, and Table 14.2 presents the top-charge additives for engineering, overhead expense, miscellaneous, contingencies, startup, and use of construction money as recommended in TID-7025.³ This report, prepared for AEC by Kaiser Engineers, covers cost evaluation methods for plants costing up to \$60,000,000. The top charges for the larger and higher-capital-cost GCR-3 have been obtained by conservative extensions of the recommended top-charge curves as shown in Fig. A.1 of the appendix. Since the basis for these charges is somewhat arbitrary, it is difficult to establish whether they are meaningful in evaluating the actual cost of a GCR-3 type of plant. Probably the allowances for overhead and engineering are appropriate to a plant being constructed for the first time, but subsequent installations might be expected to follow the normal pattern for conventional steam power plants.

The top-charge allowances used by the Pacific Gas and Electric Company in estimating the cost of its 313-Mw(e) (net), boiling-water, nuclear power plant at Bodega Bay may be more appropriate for evaluating the potential of a power system. Table 14.5 compares the capital cost of the conservative and optimistic bases. Almost 20% reduction in total cost is realized if the Bodega Bay basis is used in the estimate.

Size Effects

Although the GCR-3 is based on the specific design of a 750-Mw(e) reactor plant, the unit costs of the equipment can be used to determine whether increasing or decreasing the capacity will affect the capital investment.

Table 14.5. Total Costs, Including Top Charges, as a Function of Size

	750-Mw(e) GCR-3		500-Mw(e) GCR-3		1000-Mw(e) GCR-3	
	Conservative	Optimistic	Conservative	Optimistic	Conservative	Optimistic
Indirect Multiplication Factors						
General and Administrative	1.058	1.065	1.065	1.071	1.054	1.058
Miscellaneous construction charges	1.006	1.008	1.008	1.008	1.007	1.006
Architect-engineer services	1.070	1.083	1.083	1.096	1.062	1.070
Nuclear engineering	1.034	1.043	1.043	1.053	1.027	1.034
Product	1.1775	1.2125	1.2125	1.2455	1.1576	1.1775
Total indirect costs	\$ 19,484,000	\$ 18,873,000	\$ 18,306,000	\$ 17,302,000	\$ 20,580,000	\$ 18,641,000
Total direct cost	109,767,000	88,316,000	86,145,000	70,361,000	130,586,000	105,020,000
Land and land rights	500,000	500,000	500,000	500,000	500,000	500,000
Total direct and indirect cost	\$129,751,000	\$108,189,000	\$104,951,000	\$ 88,163,000	\$151,666,000	\$124,161,000
10% contingency	12,975,000	10,819,000	10,495,000	8,816,000	15,166,000	12,416,000
Startup cost plus 10% contingency	785,000	785,000	524,000	524,000	1,044,000	1,044,000
Total direct plus indirect plus startup plus contingency	\$143,511,000	\$119,793,000	\$115,970,000	\$ 97,503,000	\$167,876,000	\$137,611,000
Interest on cost at 13.5%	\$ 19,374,000	\$ 16,172,000	\$ 15,656,000	\$ 13,162,000	\$ 22,663,000	\$ 18,577,000
Grand Total	\$162,885,000	\$135,965,000	\$131,626,000	\$110,665,000	\$190,539,000	\$156,188,000
Ratio of top charges to direct cost	0.477	0.525	0.522	0.570	0.455	0.482
Total cost – Bodega Bay basis, using ratio of 0.223	\$134,857,000	\$109,232,000	\$105,966,000	\$ 86,633,000	\$160,317,000	\$129,050,000
Cost per kw(e) – AEC basis	\$218.2	\$181.3	\$263.2	\$221.2	\$190.5	\$156.2
Cost per kw(e) – Bodega Bay basis	\$180.1	\$145.6	\$211.9	\$173.3	\$160.3	\$129.4
Capital charge, mills/kwhr						
0.9 plant factor, AEC basis	4.0	3.3	4.8	4.1	3.5	2.9
0.8 plant factor, AEC basis	4.5	3.7	5.4	4.6	3.9	3.2
0.9 plant factor, Bodega Bay basis	3.1	2.5	3.6	3.0	2.7	2.2
0.8 plant factor, Bodega Bay basis	3.5	2.8	4.1	3.3	3.1	2.5

Size effects are determined to some extent by the engineering approach used in changing the plant capacity. Thus for the purpose of analysis it is necessary to make some assumptions regarding the means used to vary the plant capacity. A number of possible techniques include changing the core size, the coolant pressure level, the lattice pitch, the gas temperatures, and so on. For the purpose of this study the size effects were examined by assuming that the power level would be altered by changing the number of fuel channels and thus the core size. The costs for 500- and 1000-Mw(e) plants are compared with that for the 750-Mw(e) GCR-3 in Table 14.5. Both conservative and optimistic cost bases are used, as well as AEC and Bodega Bay top charges.

The analysis indicates that changes in power level even at this extended GCR-3 range have a significant influence on capital cost and, as shown in Fig. 1.6, that lowering the capacity from 750 Mw(e) to 500 Mw(e) would increase the capital investment per unit of capacity about 20%. Increasing the capacity to 1000 Mw(e), the size now of interest in fossil-fuel systems, would result in a capital cost reduction of about 12.5%. This analysis reemphasizes the important power cost reductions attainable through the use of large-capacity gas-cooled reactor systems. It is interesting to note that the savings through increasing plant capacity can be only partially assigned to reduction in the unit cost of the nuclear plant features, since steam turbines and related equipment also become less costly with increasing capacity.

Fuel-Cycle Costs

Section 3 of this report discusses the effects of fuel lifetime, fuel reprocessing, fuel fabrication, and plutonium credit on the fuel-cycle cost for the GCR-3. As shown, if there are no metallurgical limitations on the fuel lifetime, the fuel-cycle costs can drop below 1.5 mills/kwhr (see Fig. 3.8). For the purpose of this study an average design lifetime of 20,000 Mwd/MT was assumed and, as shown by Table 14.6, a fuel-cycle cost of \$270.54 per kilogram of uranium is anticipated, which gives a unit fuel cost of 1.46 mills/kwhr of net

Table 14.6. GCR-3 Fuel Cost Summary^a

A: Fuel Processed

B: No Fuel Processing

	Processing		Shipping		Use Charge ^b				Uranium Loss or Consumption				Plutonium Loss or Production for		Total Costs			
					A		B		A		B		A		A		B	
	A	B	A	B	1961	1962	1961	1962	1961	1962	1961	1962	1961	1962	1961	1962	1961	1962
Fabrication																		
Transportation to conversion site (and fabricator)			\$ 1.68	\$ 1.68	\$ 0.82	\$ 0.74	\$ 0.82	\$ 0.74							\$ 2.50	\$ 2.42	\$ 2.50	\$ 2.42
Conversion and fabrication	\$52.00	\$52.00			6.73	6.07	6.73	6.07	\$ 5.62	\$ 5.09	\$ 5.62	\$ 5.09			64.35	63.16	64.35	63.16
Transportation to reactor and recycle			1.65	1.65	0.73	0.73	0.73	0.73							2.38	2.38	2.38	2.38
Subtotal	\$52.00	\$52.00	\$ 3.33	\$ 3.33	\$ 8.28	\$ 7.54	\$ 8.28	\$ 7.54	\$ 5.62	\$ 5.09	\$ 5.62	\$ 5.09			\$ 69.23	\$ 67.96	\$ 69.23	\$ 67.96
Reactor																		
Preirradiation inventory					\$ 3.15	\$ 2.84	\$ 3.15	\$ 2.84							\$ 3.15	\$ 2.84	\$ 3.15	\$ 2.84
Irradiation					22.15	20.20	22.15	20.20	\$194.50	\$173.50	\$281.20	\$254.30	(\$78.70) ^c	(\$52.48)	137.95	141.22	303.35	274.50
Decay					1.35	1.26	1.35	1.26							1.35	1.26	1.35	1.26
Subtotal					\$26.65	\$24.30	\$26.65	\$24.30	\$194.50	\$173.50	\$281.20	\$254.30	(\$78.70)	(\$52.48)	\$142.45	\$145.32	\$307.85	\$278.60
Chemical Processing																		
Transportation to process site			\$15.52		\$ 0.23	\$ 0.21									\$ 15.75	\$ 15.73		
Separation	\$22.55				0.59	0.55			\$ 0.87	\$ 0.81			\$ 0.79	\$ 0.52	24.80	24.43		
Uranium conversion	5.37				0.30	0.28			0.26	0.24					5.93	5.89		
Plutonium conversion	9.73												0.78	0.52	10.51	10.25		
Transportation to receiving point			0.96												0.96	0.96		
Subtotal	\$37.65		\$16.48		\$ 1.12	\$ 1.04			\$ 1.13	\$ 1.05			\$ 1.57	\$ 1.04	\$ 57.95	\$ 57.26		
Totals	\$89.65	\$52.00	\$19.81	\$3.33	\$36.05	\$32.88	\$34.93	\$31.84	\$201.25	\$179.64	\$286.82	\$259.39	(\$77.13)	(\$51.44)	\$269.63	\$270.54	\$377.08	\$346.56

^aAll costs are based on kilograms of uranium charged into the reactor.^bAEC uranium cost schedule changed in 1962.^cValues in parentheses are credits for net plutonium production.

electric power generated. The detailed computation of the fuel cycle costs is given in Table A.1 of the appendix. The uncertainties in the estimate are the behavior of the fuel under long-time irradiation and the cost of applying surface roughening to the fuel cladding. Neither uncertainty requires major extensions of the fuel technology. For example, the improved flux flattening attainable in the large gas-cooled reactor core means that the required peak fuel lifetime is substantially lower for a given average lifetime than it would be in the water reactor systems. In a like manner the cost of the surface roughening, even if it caused a substantial increase in the cost of tubing used for the cladding, would enter into the total fabrication cost of the fuel in a relatively minor way. The tubing costs in the EGCR fuel were less than 7% of the total fabrication cost, which of itself is not a dominant factor in the GCR-3 fuel cycle cost estimate (less than 0.3 mill/kwhr out of a total of 1.46 mills/kwhr).

The GCR-3 fuel cycle is subject to the same uncertainty with fuel reprocessing costs and plutonium credit values that is found in the water-cooled reactor systems, since the fuels have similar physical characteristics. An examination of the cost of a throwaway cycle, in which the spent uranium and plutonium are not reprocessed for recovery, shows that even though it has a significant effect on the fuel-cycle cost the effect is not drastic. Table 14.6 shows that if no credit is claimed for spent uranium or plutonium and if the reprocessing costs are eliminated, the fuel-cycle costs would rise from \$270.54 to \$346.56 per kilogram of uranium, based on the AEC's 1962 cost schedule (16.9 ¢/million Btu's to 21.6 ¢/million Btu's), increasing the unit fuel cost from 1.46 to 1.87 mills/kwhr. The fact that loss of the plutonium and uranium credits does not drastically alter the fuel-cycle cost is a comforting situation.

The impending change in ownership policy for nuclear fuels also requires some consideration in the fuel-cycle evaluation. If the AEC's 4.57% use charge were replaced by a 10% capital charge rate for the uranium inventory in the GCR-3, the fuel-cycle cost would rise from 1.46 mills/kwhr to 1.66 mills/kwhr.

Plant Operating Charges

The operating costs of the plant other than for fuel were prepared by TVA's operating staff assigned to the EGCR, and represent an educated guess reflecting TVA's knowledge of its large fossil-fuel plants now under construction combined with their planning work for operation of the EGCR. In terms of manpower for the amount of operating equipment, it checks out reasonably well with the practice on Magnox reactor systems being built in the United Kingdom. It is naturally subject to verification by actual experience. Again, a reasonable margin of error could be tolerated without drastically affecting the predicted power costs.

Relative Economic Position of the GCR-3 in Power Reactor Technology

Although the purpose of the GCR-3 study was to establish whether the system has a reasonable economic potential as a central station power plant, some indication of its competitive position can be seen by comparing it with the recently released figures on the Pacific Gas and Electric Company's Bodega Bay 313-Mw(e) boiling-water nuclear power plant.⁴ The anticipated power costs for this plant are compared with those for the GCR-3 in Table 14.7 and in Table 1.3 and Fig. 1.6.

A comparison of the GCR-3 with other reactor systems is virtually impossible at this stage of power reactor development, because no studies of other large-sized reactor systems have been prepared in sufficient detail. In addition, only the water-cooled reactor systems can present a developed fuel concept for use in analysis. Thus comparisons must be made by predicting successful results for the research and development work still under way. A fair comparison of the GCR-3 concept would require the assumption of advancements in technology,

⁴"Summary of Estimated Capital and Annual Costs and Cost of Energy for Bodega Bay Nuclear and Conventional Fossil Fuel Plants," Atomic Energy Clearing House 8(12):4-8 (March 19, 1962).

Table 14.7. Cost Comparison - GCR-3 and Bodega Bay

	313-Mw(e) Bodega Bay		750-Mw(e) GCR-3			
	Total Investment	Unit Capacity Cost [per kw(e)]	Total Investment	Unit Capacity Cost [per kw(e)]	Total Investment	Unit Capacity Cost [per kw(e)]
		Conservative		Optimistic		
Direct Costs						
Land and land rights	\$ 150,000	\$ 0.5	\$ 500,000	\$ 0.7	\$ 500,000	\$ 0.7
Structures and improvements	7,830,000	25.0	12,968,000	17.3	12,968,000	17.3
Reactor plant	23,429,000	74.8	65,643,000	87.5	48,292,000	64.4
Turbine-generator	13,277,000	42.4	25,374,000	33.8	21,774,000	29.0
Accessory electrical equipment	1,920,000	6.1	4,532,000	6.0	4,532,000	6.0
Miscellaneous power plant equipment	1,505,000	4.8	1,250,000	1.7	1,250,000	1.7
Subtotal	\$48,111,000	\$153.2	\$110,267,000	\$147.0	\$ 89,316,000	\$119.1
Top Charges						
Construction — plant, warehouses, etc.	\$ 1,217,000					
Engineering, superintendence, and accounting	2,823,000					
Overhead construction	6,675,000					
Subtotal	\$10,715,000	\$ 34.2	\$ 24,590,000 ^a	\$ 32.8	\$ 19,917,000 ^a	\$ 26.6
Total plant investment	\$58,826,000	\$187.4	\$134,857,000	\$179.8	\$109,233,000	\$145.7
	Annual Cost	Unit Power Cost (mills/kwhr)	Annual Cost	Unit Power Cost (mills/kwhr)	Annual Cost	Unit Power Cost (mills/kwhr)
Energy Costs						
Fixed charges at 13.2% and 0.9 plant factor	\$ 7,765,000	3.2	\$ 17,801,000	3.01	\$ 14,419,000	2.44
Fuel cost at 15,000 Mwd/T	4,772,000	2.0	9,458,000	1.60 ^b	9,458,000	1.60
Other operating costs, including insurance	1,580,000	0.7	2,790,000	0.48	2,790,000	0.47
Subtotal	\$14,117,000	5.9	\$ 30,049,000	5.09	\$ 26,667,000	4.51
Transmission cost	\$ 752,000	0.31		0.31		0.31
Total power cost		6.2		5.40		4.82
Net thermal efficiency	31%		40%		40%	
Net output, including 3% transmission losses	2,394,000 kwhr			5,736,000 kwhr		

^aBased on 0.223 ratio of top charges to direct costs for Bodega Bay.^bGCR-3 is designed on the basis of a 20,000-Mwd/T fuel lifetime at a predicted fuel cycle cost of 1.44 mills/kwhr, but these costs are corrected to 15,000 Mwd to make the estimate comparable to the Bodega Bay design.

such as

1. further reduction in the fuel-cladding thickness for improved neutron economy,
2. extensions of the high-temperature limits by supplanting the fuel cladding with a refractory alloy,
3. replacement of the helium coolant with hydrogen for improved thermodynamic efficiency and lower capital cost,
4. improved equipment design.

Since these areas are unexplored, their benefits cannot be properly evaluated. An optimistic attitude would undoubtedly indicate startling improvements in power costs, but at the present stage they can only be presented as further evidence of the promise of the GCR-3 design.

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15. SAFETY EVALUATION

The problems of safety in gas-cooled reactor systems of the GCR-3 type are determined by the reactor's capability for removing afterheat following a depressurization accident and by the release of activity from the UO_2 fuel. Experimental work¹ has established that the UO_2 activity-retention properties are excellent at operating temperatures below 1600°C , the release being confined to gaseous fission products that leave the fuel body through diffusion-controlled mechanisms. Thus it can be expected that the fuel cladding will contain as free gas only a small percentage of the gaseous activity generated by the fuel over its lifetime. When the fuel body temperature exceeds 1600°C , the diffusion mechanisms no longer appear to be controlling and gross activity release can be expected. The GCR-3 design has capitalized on this ability to assure a means of holding the gaseous activity within the fuel body by maintaining the bulk of the oxide below the critical temperature.

Normally, the high temperatures in the UO_2 develop because of the poor thermal conductivity of the oxide. For the GCR-3 design the oxide thickness has been set at a value which will assure that very little of the oxide exceeds the maximum temperature at the highest power density in the core. When the reactor is shut down, the energy release rate from the fuel becomes very low and the temperature gradient across the fuel body levels out. For this reason the oxide temperature quickly falls to a low-temperature level suited to good fission-product retention when the reactor becomes subcritical. The initial problem in activity release is then confined to that resulting from the gaseous products previously released and contained within the fuel cladding.

If conditions following the accident are such that the UO_2 temperature is allowed to rise, then the problem of gross-activity release may recur. Mechanisms by which such an event could develop are, first, if

¹J. L. Scott, "Analysis of ORNL Data on the Release of Fission Gases from UO_2 ," USAEC Report ORNL CF-60-8-15, Oak Ridge National Laboratory, September 21, 1960.

because no afterheat-removal capability exists in the core, the internal temperatures rise until the critical oxide temperature is exceeded and, second, if the temperature level inside the core is caused to rise excessively by the appearance of a supplemental heat-generation source. In the EGCR and other comparable gas-cooled reactor systems this supplemental heat source problem has required the evaluation of accidents resulting from the potential presence of oxygen in the core following a depressurization accident, since oxygen could react with graphite exothermically to raise the heat-removal demands on the reactor system. The design approach to most gas-cooled reactor systems is either to hold the temperature within the core at some level at which the graphite oxidation rate will be low so that very little additional energy over that from afterheat must be removed or to provide sufficient excess cooling to override the heat generated by the graphite-oxidation process. In both cases a very reliable heat-removal circuit is required, and, if the rate of graphite oxidation is assumed to be high, the capacity of this system may be substantial.

In the GCR-3 design, as in the Philadelphia Electric Company Peach Bottom plant, the decision was reached to provide an inert gas atmosphere around the primary coolant circuitry. An arrangement of this sort guarantees that as long as the secondary-containment system is not breached there will be no means by which oxygen can reach the graphite moderator, and graphite oxidation as a source of energy can be ignored. The design must then concentrate on assuring the afterheat-removal capability of the reactor system and the integrity of the secondary containment.

If the secondary-containment system is designed for sufficiently high pressure to hold the gaseous fluids that may be released in a reactor accident and if the containment is sufficiently tight to hold the rate of activity release within the bounds considered safe for the public, then the required safety practices currently accepted for nuclear power technology are satisfied. The initial pressure increases in the system potentially arise from the release of the high-temperature coolant in the reactor and the release of the steam in the steam generator. In the GCR-3 design the pressure level from these two pressure sources is fixed

by the system's inventory so that they cannot exceed the containment-shell pressure design limit of 60 psi except by an unlimited temperature rise of the gaseous contents. An upper temperature limit is assured by providing a reliable natural-convection atmospheric boiler which is independent of the primary steam circuit and the primary coolant circuit. The effectiveness of the natural-convection cooling is determined only by its position with respect to the high-temperature heat source of the reactor and by the containment-shell pressure. It has been shown in Chapter 7 that natural convection will remove the afterheat of the reactor system after one day if the containment-shell pressure is as high as 40 psi. Since the containment shell has a design pressure of 60 psi, it is apparent that the natural-convection cooling system will assure the long-time integrity of the containment shell with respect to pressure. The nature of the containment-shell design assures that the leak-tight membrane will not exceed its maximum temperature limits.

The short-time temperature rise immediately following the accident is effectively controlled by the large heat capacity of the graphite-moderator structure, which acts as a heat sink. The afterheat-removal system therefore guarantees the public safety.

A secondary hazard problem that arises in reactor systems of this type is the question of hydrogen generation and potential hydrogen explosions which may breach the containment system. Hydrogen is generated by the producer-gas reaction $\text{H}_2\text{O} + \text{C} \rightarrow \text{H}_2 + \text{CO}$ as a result of leakage of moisture from some operating source, for example, the steam generator, into the primary coolant circuit. Although this reaction is endothermic and temperature-sensitive, it could occur if the graphite temperature were allowed to rise to above 1200°F and if significant quantities of moisture were present. The rates of hydrogen generation are very low at these lower temperature levels, but they could lead to a slow buildup of the hydrogen concentration within the coolant circuitry; thus if the coolant circuit is mixed with air, a hydrogen explosion or detonation can potentially open the containment system. The approach in the EGCR was to assure that any hydrogen generated could be maintained at a concentration below its explosive limits. In the GCR-3 design the presence of the

inert atmosphere in the containment shell eliminates any concern about the hydrogen-generation problem. It would, in fact, permit the system to operate on a hydrogen cooling circuit without creating an explosion or detonation hazard.

For the purpose of evaluating the accident limits of the GCR-3 design the following investigations have been undertaken:

1. an examination of the mechanisms for heat removal within the containment system and the effect on the ultimate containment pressure,
2. the demands on the containment system tightness resulting from the release of the gaseous activity held by fuel cladding as a function of the fuel lifetime for two values of UO_2 thermal conductivity [1.0 and $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$].
3. the potential radiation dose at the site boundary as a function of the activity release and the containment leak-tightness.

These studies have shown that the natural-convection cooling system is necessary but adequate for heat removal from the very large power system and that a containment leakage rate of 0.5% per day would be satisfactory.

Afterheat Removal

The afterheat removal from the GCR-3 is investigated for three emergency conditions. For the first emergency condition (Case 1) it is assumed that the afterheat is removed through several resistances, including conduction through the reactor vessel and steam generator insulation and the gas mixture inside the containment vessel to the surface of the concrete. The concrete is maintained at 150°F by the circulation of cooling water. The temperature difference required to move the afterheat from the core to the internal surface of the reactor vessel and the steam generators was neglected on the basis that the block valves in the primary gas loop are open and that, under this condition, natural circulation would tend to equalize the temperature on the inside of the reactor vessel and the steam generators.

The second emergency condition (Case 2) considers heat removal by natural convection from the core to the emergency coolers when the system

is depressurized. All main blowers are assumed to be inoperable. Flow of cooling water is maintained to the emergency coolers, but not through the steam generators. Failure of both emergency coolers is not considered credible since two emergency coolers are provided and they are well separated physically. Flow of cooling water is maintained by a head tank. In the analysis which follows, both emergency coolers are considered to be operating. The cooling available with only one emergency cooler is not grossly different from that calculated herein.

The third emergency condition (Case 3) considers afterheat removal by natural circulation from the core to the containment when the system is depressurized. For this case a very large rupture in the highest point in the system is assumed. Circulation is by natural convection and complete mixing in the containment gas is assumed.

Discussion and Calculations

Case 1. - For Case 1, heat is removed from the core only by conduction through the reactor vessel and steam generator walls. The temperature reached by the gas in the containment shell will be such that the heat transferred across the primary vessel walls will equal the heat transferred from that gas to the containment shell.

The heat losses from the exterior of the reactor vessel and steam generators to the containment shell are controlled by a natural-convection gas film and radiation from the reactor vessel and steam generators to the containment shell as shown in Fig. 15.1. The heat loss is calculated as follows:

$$q_{\text{containment}} = q_{\text{convection}} + q_{\text{radiation}} \quad (1)$$

The concrete is assumed to be at 150°F, and the resistance across the shell wall itself is negligible.

The heat transferred across the natural-convection gas film is

$$q = hA (t_{\text{containment}} - t_{\text{concrete}}) , \quad (2)$$

where

$$h \approx 0.3 \Delta t^{0.25} \quad (3)$$

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Fig. 15.1. Heat Transferred from Containment Shell to Concrete vs Containment Gas Temperature.

The surface area of the containment shell is obtained as follows. The containment around the steam generators can be closely approximated by two cylinders with radii of 33 ft and length of 135 ft and with hemispherical ends. The reactor vessel is surrounded by an approximately 8-ft-long cylinder with a radius of 27 ft with hemispherical ends. The total containment surface area is, therefore, approximately 97,000 ft², which is to be substituted in Eq. (2).

The radiation heat loss is

$$q_{1,2} = 1730A_1 F_{1,2} \left[(0.001t_1)^4 - (0.001t_2)^4 \right], \quad (4)$$

where nomenclature is given at the end of this section and subscript 1 in this case refers to the outer surface of the reactor vessel and subscript 2 refers to the part of the containment shell surrounding the reactor vessel (8-ft-long cylinder with a 31-ft radius with hemispherical ends). The radiation losses from the steam generators are neglected.

Since one surface completely surrounds the other, the overall interchange factor F is given by

$$F_{1,2} = \frac{1}{\frac{1}{e_1} + \frac{A_1}{A_2} \left(\frac{1}{e_2} - 1 \right)} \quad (5)$$

The emissivity of all surfaces is assumed to be 0.8.

For the purpose of calculating the radiation heat loss, the temperature of the outer surface of the reactor vessel is assumed to be at the bulk containment temperature, since the major temperature drop from the core to the containment is through the insulation.

The primary-system insulation resistance is assumed to be such that 360 Btu ft⁻² hr⁻¹ will be lost when the reactor gas outlet temperature is 1200°F and the containment temperature is 150°F. The heat-transfer area consists of the reactor vessel (a sphere 54 ft in diameter) and the outer surface of the four steam generators, which is approximated by four cylinders 63 ft long and 17 ft in diameter with hemispherical ends. The total area of the primary system is 25,460 ft². The heat transferred at the reference condition is, therefore, 360(25,460) = 9.16 × 10⁶ Btu/hr.

Since the heat transfer is by conduction through the insulation (i),

$$q_i = \frac{\Delta t_i q_{\text{reference}}}{\Delta t_{\text{reference}}} \quad (6)$$

The heat transferred from the primary system to the containment gas is plotted in Fig. 15.2 as a function of the containment bulk temperature for various core temperatures; the heat transferred from the containment gas to the containment shell is also plotted. The heat removed from the system is found at the intersection of the family of curves for heat transferred from the core to the containment gas with the curve for the heat loss from containment gas to the containment.

A plot of average core temperature vs time is constructed on an incremental time basis for blower failures occurring at shutdown and 1, 10, and 30 days after shutdown. The plot of core temperature vs time is constructed by subtracting the heat removed from the heat generated; then the remainder is divided by the heat capacity of the core:

$$\int_{\theta_1}^{\theta_2} q_{\text{generated}} d\theta - \int_{\theta_1}^{\theta_2} q_{\text{removed}} d\theta = nC_p \Delta t \quad (7)$$

In arriving at the core heat capacity, only the graphite is considered. The heat capacity of the gas and the fuel is small relative to that of the graphite. The heat capacity of the reactor vessel is the same order of magnitude as that of the graphite but is neglected because, due to the internal insulation, it is at a temperature lower than that of the core.

The initial average graphite temperatures are assumed to be 400°F for all times of blower failure after shutdown except when immediate failure occurs, when an initial temperature of 850°F is assumed. The heat generated is obtained from the integrated afterheat based on ORNL preferred curves for afterheat generation. The heat removed is obtained for each core temperature from Fig. 15.2.

The results are shown in Figs. 15.3 and 15.4.

Case 2. - Cases 2 and 3 involve natural-circulation cooling. The heat removed by natural circulation is dependent on the flow rate. The flow rate of gas through the core is assumed to be proportional to the

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Fig. 15.2. Heat Loss from Containment and Heat Transferred from Core to Containment vs Containment Gas Temperature.

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Fig. 15.3. Heat Loss from Containment and Heat Transferred from Core to Containment vs Containment Gas Temperature.

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Fig. 15.4. Average Temperature of Graphite vs Time After Blower Failure.

square root of pressure drop:

$$W \sim \sqrt{\Delta P} . \quad (8)$$

Therefore

$$W_{\text{nat conv}} = W_{(\text{design})} \left(\frac{\Delta P_{\text{nat conv}}}{\Delta P_{\text{design}}} \right)^{1/2} . \quad (9)$$

The pressure differential, which is the driving force for flow, is dependent on the difference in densities of the hot and cold legs and the height of the legs, which is taken to be 80 ft:

$$\Delta P_{\text{nat conv}} = (\rho_{\text{cold leg}} - \rho_{\text{hot leg}}) H . \quad (10)$$

The density of a particular leg is

$$\rho_{\text{leg}} = \rho_{\text{av}} \frac{t_{\text{av}}}{t_{\text{leg}}} . \quad (11)$$

Therefore

$$\Delta P = \rho_{\text{av}} H t_{\text{av}} \left(\frac{1}{t_{\text{cold}}} - \frac{1}{t_{\text{hot}}} \right) . \quad (12)$$

Substitution of Eq. (12) into Eq. (9) yields

$$W = \left[\frac{\rho_{\text{av}} H t_{\text{av}}}{\Delta P_{\text{design}}} \left(\frac{1}{t_{\text{hot}}} - \frac{1}{t_{\text{cold}}} \right) \right]^{1/2} W_{\text{design}} . \quad (13)$$

The average density is evaluated in the following manner. A total containment volume of 1,231,000 ft³ and a primary system volume of 149,000 ft³ are obtained. The net containment volume is, therefore, 1,082,000 ft³. Assuming a perfect gas at 150°F and atmospheric pressure, 2430 lb-moles of N₂ are contained in the containment volume. The helium inventory is 4250 lb-moles, and the average molecular weight of the mixture is 12.72. The average density is equal to

$$\begin{aligned} \rho_{\text{av}} &= \frac{nM_{\text{av}}}{V_{\text{total containment}}} \\ &= 0.069 \text{ lb/ft}^3 . \end{aligned} \quad (14)$$

For Case 2, it is assumed that the energy is removed by the emergency coolers and that the coolers are sized to remove all the energy that can be transferred by natural circulation. Thus

$$q_{\text{removed}} = WC_{\text{av}} (t_{\text{core outlet}} - t_{\text{cooler exit}}) . \quad (15)$$

The average specific heat is

$$C_{\text{av}} = \frac{n_{\text{He}} M_{\text{He}} C_{\text{He}} + n_{\text{N}_2} M_{\text{N}_2} C_{\text{N}_2}}{n_{\text{He}} M_{\text{He}} + n_{\text{N}_2} M_{\text{N}_2}} ,$$

$$= 0.462 \text{ Btu lb}^{-1} (\text{°F})^{-1} . \quad (16)$$

The gas leaving the coolers, as well as the average gas temperature in the system, is assumed to be at 300°F.

A plot of heat removed vs outlet core gas temperature is made (Fig. 15.5) by using Eqs. (13) and (15). A plot of outlet gas temperature vs time is then made by using Eq. (7). The heat removed for a particular gas temperature is obtained from Fig. 15.5, and the heat generated is obtained from the integrated² afterheat curve, Fig. 15.6. It is assumed that the outlet gas temperature rose or dropped the same number of degrees as the average graphite temperature during a time interval (see Fig. 15.7).

Case 3. — Case 3 represents the maximum possible cooling that could be achieved by natural circulation from the core without the emergency coolers. As has been stated, the heat removal depends on the flow rate, which, in turn, depends on the pressure differential developed. The pressure differential depends on the difference of densities of the hot and cold legs and the height of the legs. This height is the difference between the elevation of the rupture and the elevation of the center of the core and is thus a maximum when a rupture occurs in the uppermost part of the system. If a rupture occurs at or below the elevation of the

²From Fig. 1.3 in "GCR Quar. Prog. Rep. June 30, 1961," USAEC Report ORNL-3166, Oak Ridge National Laboratory.

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Fig. 15.5. Heat Removed by Emergency Coolers vs Outlet Gas Temperature.

Fig. 15.6. Integrated Afterheat. (Integrated from Curve 3 of Fig. 1.3 in "GCR Quar. Prog. Rep. June 30, 1961," ORNL-3166.)

Fig. 15.7. Outlet Core Gas Temperature vs Time After Shutdown - Main Blower Failure Immediately After Shutdown.

center of the core, little or no natural circulation exists and therefore the only mechanism for heat removal is by conduction, as in Case 1.

Since the heat capacity of the gas is small compared with that of the graphite, the containment gas temperature should rapidly reach a value whereby the heat lost from the containment to the concrete is equal to the heat transferred by circulation from the primary system to the containment volume. If there is circulation from the core to the containment, the gas temperature at the inlet to the reactor core should equal the containment gas temperature. Uniform mixing of the containment gas is assumed.

The heat transferred by circulation to the containment volume from the reactor core is

$$q_{\text{convection}} = WC_{\text{av}} (t_{\text{core outlet}} - t_{\text{containment}}), \quad (17)$$

$$q = \left[\frac{\rho_{\text{av}} H t_{\text{av}}}{\Delta P_{\text{design}}} \left(\frac{1}{t_{\text{core outlet}}} - \frac{1}{t_{\text{containment}}} \right) \right]^{\frac{1}{2}} (t_{\text{core out}} - t_{\text{containment}}). \quad (18)$$

$$t_{\text{av}} = \frac{V_{\text{primary system}} \frac{(t_{\text{core in}} - t_{\text{core out}})}{2} + V_{\text{net contain.}} t_{\text{contain.}}}{V_{\text{total}}}. \quad (19)$$

By using Eqs. (18) and (19), the heat removed for various outlet core temperatures is plotted in Fig. 15.3 as a function of the bulk containment gas temperature. For a particular gas core outlet temperature, the heat transferred from the system is the intersection of the curve for heat losses to the containment with the curve for heat loss from the containment. A plot of outlet core temperature vs time is constructed (Figs. 15.8 and 15.9) by using Eq. (7) for blower failures immediately after and one day after shutdown and assuming that the changes in the outlet gas temperature are equal to the change of the average graphite and core temperature during a particular increment of time.

Fig. 15.8. Outlet Core Gas Temperature vs Time After Shutdown.

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Fig. 15.9. Outlet Core Gas Temperature vs Time After Blower Failure.

Nomenclature for Afterheat-Removal Calculations

A = area, ft^2

C = specific heat, $\text{Btu lb}^{-1}(\text{°F})^{-1}$

C_p = molar heat capacity, $\text{Btu (lb-mole)}^{-1}(\text{°F})^{-1}$

F = overall interchange factor for radiation including geometry and emissivity factors, dimensionless

h = heat-transfer coefficient, $\text{Btu ft}^{-2} \text{ hr}^{-1}(\text{°F})^{-1}$

H = height, ft

M = molecular weight, lb/lb-mole

n = number of moles

P = pressure, lb/ft^2

q = rate of heat flow, Btu/hr

t = temperature, °F

W = flow rate, lb/hr

V = volume, ft^3

e = emissivity, dimensionless

θ = time, hr

ρ = density, lb/ft^3

Conclusions

In the event of a depressurization accident the temperature change in the core will depend upon the exact nature of the failure, as well as the time of failure in relation to shutting down the reactor. Three cases were investigated without attempting to define the accident that would cause such conditions. Rather, the conditions define a method of heat removal which will exist to some extent for any accident.

Following a depressurization accident, the worst condition is to have no forced or natural convection. If heat removal is by conduction through the vessel walls only, the graphite temperature will increase rather rapidly immediately following an accident. It may be seen in Fig. 15.4 that the graphite temperature increases at an initial rate of about 150°F/hr . Although this temperature rise is not acceptable if allowed to continue, over 2 hr will be required for 1200°F to be exceeded. If the accident occurs after the reactor is shut down, the

rate of temperature rise is not so high and the time to reach a temperature that would be unacceptable is much longer. Therefore it may be concluded that some other method of removing heat must be obtained since conduction is not adequate. Only after the reactor has been shut down for about 30 days (Fig. 15.10) does heat removal by conduction appear to be adequate.

Following a depressurization accident, heat may be removed from the core by natural convection. Numerous heat sinks are available, including the main steam generators, emergency coolers, and the containment itself. Inasmuch as it is not possible to conclude that steam generators will always be available following all accidents, it was postulated that the only heat sink would be the containment or the emergency coolers. Heat removal by natural convection to the emergency coolers appears to be adequate for all times of failure. Figure 15.7 indicates that a slight increase in outlet gas temperature will occur but that within a few hours the graphite temperature will begin to decrease rapidly.

If the only heat sink is the containment, it may be seen in Figs. 15.8 and 15.9 that the outlet gas temperature will increase about 75°F if the accident occurs immediately following shutdown but will not increase at all if the reactor has been shut down for one day. As in the previous case, graphite temperatures will eventually be brought down as heat is adequately removed.

It therefore appears that for all credible accidents afterheat from the GCR-3 can be removed by one or more methods. Although additional studies will be required to evaluate the adequacy of the design for all credible accidents, initial studies indicate that the utilization of the systems provided will result in at least three lines of defense for removal of afterheat following an accident.

Containment System Tightness

The activity analysis is based on the study of the release of iodine and the exposure dose to the thyroid. The activity release of I^{131} from the core, with the thermal conductivity for UO_2 assumed to be $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$, is approximately 3.2×10^5 curies. The use of

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Fig. 15.10. Average Temperature of Graphite vs Time After Shut-down.

a lower value of thermal conductivity results in an increase in the activity release since the temperature of the UO_2 increases significantly. If the thermal conductivity of UO_2 is assumed to be 1.0 (as was done for the EGCR analysis), the activity release would be increased by a factor of about 3.5. The release of I^{131} is not significantly affected by changes in burnup ranging from 10,000 to 40,000 Mwd/MT, since I^{131} has a sufficiently short half-life for saturation to be reached in about 10,000 Mwd/MT for the fuel programming proposed. The release of fission products could be altered from the values predicted if higher values of peak-to-average power resulted from fuel programming.

The leakage rates that could be considered as acceptable to limit the exposure dose to the public depend upon the location of the reactor, the exclusion area, and the low-population-zone distance associated with the site. Assuming the most adverse meteorological conditions, a leakage rate of about 0.5% per day would be required if the exclusion distance from the reactor is 250 meters and the low-population zone distance 5000 meters. If the thermal conductivity of UO_2 is assumed to be $1.0 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$, the release is increased such that the above distances would need to be 515 and 13,500 meters for the 0.5% per day leakage. The exclusion distances appear to be reasonable for most sites, but the low-population-zone distance would probably restrict the site location if the evaluation is based on the lower value of thermal conductivity. However, it should be pointed out that there are numerous methods for reducing this distance by limiting the release of activity following an accident. Therefore it appears that a leakage rate of 0.5% per day would be satisfactory even for pessimistic assumptions used in the analysis.

One of the dominant factors influencing the number of fuel element failures during an accident is the pressure buildup inside the fuel element that causes the element to fail by bursting at elevated temperatures. Several factors are important in limiting this pressure differential across the cladding for the GCR-3. The use of a large, free volume inside the fuel element not only results in a larger volume to store the released gases but also reduces the temperature drop in the gap since the thermal conductivity of the gas mixture is not reduced rapidly.

The length of the rods is extremely important in limiting pressure buildup since the rods necessarily extend to portions of the core where temperatures are not high.

An investigation of the pressure buildup for a thermal conductivity of $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$ and a burnup of 40,000 Mwd/MT indicates that the total pressure inside a fuel rod is about 99 psia. The fractional release of stable noble gases to obtain this pressure is about 3.7%. This is the maximum release in the channel with the highest power density and therefore overestimates the total pressure buildup since the fractional release will decrease in zones of lower average power density. Since only 24 psia of the 99 psia is due to noble gases, the increase in pressure due to a greater fractional release would not significantly affect the result. A lower value of thermal conductivity would increase the partial pressure due to additional noble gas release, but this effect would be small compared with the increase due to higher gas temperature in the central hole.

Following the depressurization of the primary system, the containment and primary system pressures equalize at about 40 psia. Therefore, the maximum possible initial pressure differential across the fuel element cladding is only 59 psi. Since it takes time to depressurize the primary system and since at some reduced primary-system pressure a scram will occur, the pressure inside the fuel element is decreasing before the pressure inside the containment reaches 40 psia. Therefore it is unlikely that the pressure inside the fuel element will approach the 59 psi. The probability of failure of fuel elements is decreased as the pressure differential across the cladding decreases. It should be pointed out, however, that due to the difference in the diameter and thickness of the cladding in the EGCR and the GCR-3, a pressure of 59 psi in the GCR-3 fuel rods causes the same stress in the cladding as 104 psi does in the EGCR rods.

The activity release of 3.2×10^5 curies of I^{131} includes only the activity that is released from the UO_2 by diffusion during operation. At the time of an accident this value could be increased if UO_2 temperatures were allowed to increase appreciably. If UO_2 temperature increased during an accident, a higher value of the diffusion coefficient (D)

would have to be used in estimating the activity release. However, since the release depends upon the product of D and time, a comparison of Dt for normal and accident conditions indicates that for all credible situations the additional release during the accident is negligible. If the UO_2 temperature reaches 1600°C , the release will not be by diffusion alone but could also be due to grain growth. It is therefore necessary to evaluate the fraction of the fuel that would reach 1600°C .

If the thermal conductivity of UO_2 is $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (^\circ\text{F})^{-1}$, the highest UO_2 temperature during normal operation is only 2607°F . If a lower value of thermal conductivity is assumed in the analysis, the UO_2 temperature is increased to about 3063°F during normal operation. In the latter case, some grain growth could occur and lead to a higher release. In order to determine whether a significant increase in activity release could result from grain growth, it is necessary to examine the temperature transients in the fuel during the accident. It is not expected that such transients will lead to significant additional release. Only if the time to scram is long and the heat removal is cut off rapidly will the UO_2 temperature increase significantly. Neither is expected for the GCR-3.

Analytical Method

Equations Used in Analysis. — Evaluation of the quantity of radioactive fission products that diffuse from the fuel and are available for release upon rupture of the fuel cladding was based on the model proposed by Eichenberg et al.³ This model involves the assumption that a porous body may be described in terms of a small equivalent sphere, chosen so that the surface-to-volume ratio is the same as that of the fuel pellet. The release by diffusion from the equivalent sphere is calculated. Gas-phase diffusion from the boundary of the equivalent sphere to the surface of the fuel pellet is considered to be instantaneous, and it is assumed that the material is available for release after it has diffused from the equivalent sphere.

³J. D. Eichenberg et al., "Effects of Irradiation on Bulk UO_2 ," USAEC Report WAPD-183, Westinghouse Bettis Atomic Power Laboratory.

Temperatures at the various points within the fuel were calculated from the cladding temperature, heat generation rates, and pellet geometry. Since temperature varies radially and axially, each fuel rod was subdivided into ten equal lengths and into five equal-volume shells. The effective temperature was then calculated by the method of ref 4:

$$T_m = \frac{2T_A + T_S}{3}, \quad (1)$$

where T_A and T_S represent the shell inner and outer surface temperatures, respectively, and are in themselves a mean average of the temperatures at each end of the vertical subdivision.

The temperature drop across the gas gap between the cladding and the UO_2 pellet is determined from the following equation:⁵

$$\frac{\phi}{A} = \frac{(T_b - T_c)}{\delta} k_m + 0.1714 \epsilon \left[\left(\frac{T_b + 460}{100} \right)^4 - \left(\frac{T_c + 460}{100} \right)^4 \right], \quad (2)$$

where

T_c = surface temperature of cladding, °F,

T_b = surface temperature of UO_2 , °F,

k_m = thermal conductivity of gas mixture in gap, $Btu\ hr^{-1}\ ft^{-1}(\circ F)^{-1}$,

ϕ = thermal flux, $Btu\ hr^{-1}\ ft^{-1}$,

A = surface area, ft^2/ft ,

δ = gap, ft,

ϵ = emissivity of UO_2 .

The value for T_c was obtained from Fig. 15.11 for the 21 positions in the channel which are the boundary points for the arbitrary rod subdivisions. A mean temperature for the segment was assumed. All calculated temperatures were assumed to be a mean for that subdivision.

⁴W. B. Cottrell et al., "Fission Product Release from UO_2 ," USAEC Report ORNL-2935, Oak Ridge National Laboratory, September 1960.

⁵W. L. McCabe and J. C. Smith, Unit Operations of Chemical Engineering, p. 521, McGraw-Hill, New York, 1956.

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Fig. 15.11. Coolant and Fuel Element Clad Temperature.

It is assumed that the power generation per unit volume is constant for each axial rod subdivision provided the thermal conductivity is not a function of position or temperature. It can be shown that the temperature distribution in a cored fuel pellet is given by

$$k (T_r - T_b) = - \frac{\phi}{2\pi} \left[\frac{(r^2 - b^2)}{2(b^2 - a^2)} + \frac{a^2}{b^2 - a^2} \ln \frac{b}{r} \right], \quad (3)$$

where

T_b = temperature of outer surface of UO_2 pellet, $^{\circ}F$,

T_r = temperature at r , $^{\circ}F$,

k = thermal conductivity of UO_2 , $Btu\ hr^{-1}\ ft^{-1}(^{\circ}F)^{-1}$,

ϕ = heat generation, $Btu\ hr^{-1}\ ft^{-1}$,

b = outside radius of pellet, ft ,

a = inside radius of pellet, ft ,

r = distance from center of pellet, ft .

The general diffusion equation for the release of fission products, not including burnup, is

$$\frac{\partial C}{\partial t} = D \left(\frac{\partial^2 C}{\partial r^2} + \frac{2}{r} \frac{\partial C}{\partial r} \right) + B - \lambda C, \quad (4)$$

where

C = concentration of the fission product at radius r at time t ,

D = diffusion coefficient,

B = generation rate of the fission product,

λ = decay constant.

The solution of the general diffusion equation is³

$$N(t) = 3B \left\{ \left[\left(\frac{D'}{\lambda} \right)^{1/2} \coth \left(\frac{\lambda}{D'} \right)^{1/2} - \frac{D'}{\lambda} \right] \frac{1 - e^{-\lambda t}}{\lambda} - \frac{2}{\pi^2} e^{-\lambda t} \sum_{n=1}^{\infty} \frac{1 - \exp(-n^2 \pi^2 D' t)}{n^2 (n^2 \pi^2 D' + \lambda)} \right\}, \quad (5)$$

where

$N(t)$ = number of atoms escaping, per unit of volume, from the fuel,

n = number of nuclei,

$D' = D/a^2$,

a = radius of an equivalent sphere.

If

$$R = \left\{ \left[\left(\frac{D'}{\lambda} \right)^{1/2} \coth \left(\frac{\lambda}{D'} \right)^{1/2} - \frac{D'}{\lambda} \right] \frac{1 - e^{-\lambda t}}{\lambda} - \frac{2}{\pi^2} e^{-\lambda t} \sum_{n=1}^{\infty} \frac{1 - e^{-n^2 \pi^2 D' t}}{n^2 (n^2 \pi^2 D' + \lambda)} \right\}, \quad (6)$$

then $N(t)$ can be expressed as

$$N(t) = 3 \text{ BR}. \quad (7)$$

Based on the technique developed by Beck⁶ of BMI, the following definitions are made:

$$G = \lambda R, \quad (8)$$

$$\mu = \frac{\lambda}{D'}, \quad (9)$$

$$X = D' t. \quad (10)$$

Then

$$G = \left(\frac{1}{\sqrt{\mu}} \coth \sqrt{\mu} - \frac{1}{\mu} \right) (1 - e^{-\mu X}) - \frac{2}{\pi^2} e^{\mu X} \sum_{n=1}^{\infty} \frac{1 - e^{-n^2 \pi^2 X}}{n^2 (n^2 \pi^2 + \mu)}, \quad (11)$$

and the activity release becomes

$$A = \lambda N(t) = 3 \text{ BAR} = 3 \text{ BG}. \quad (12)$$

⁶S. D. Beck, "The Diffusion of Radioactive Fission Products from Porous Fuel Elements," USAEC Report BMI-1433, Battelle Memorial Institute, April 1960.

Numerical values of G for wide ranges of μ and X have been tabulated and reported.⁶ Interpolations of these values based on calculated values of μ and X were used to determine the values of G in the calculations. To determine μ and X , values of $D'(T_m)$ were calculated from

$$D'(T_m) = D'(1400^\circ\text{C}) \exp \frac{Q}{R} \left[\frac{1}{1673} - \frac{1}{T_m} \right], \quad (13)$$

where

Q = activation energy, kcal/mole,

R = gas constant, kcal mole⁻¹(°K)⁻¹,

T_m = effective temperature, °K.

The generation rate for nuclide formation may be expressed as

$$B = 3.1 \times 10^{10} \gamma P, \quad (14)$$

where γ = chain fission yield and P = power, w. Substituting Eq. (14) in Eq. (12) and converting to curies gives

$$\begin{aligned} A &= \frac{3 (3.1)(10^{10}) \gamma PG}{(3.7)(10^{10})} \\ &= 2.51 \gamma PG \text{ curies/subdivision.} \end{aligned} \quad (15)$$

Equation (15) was applied to each isotope of interest and for each subdivision.

Three areas in the reactor were studied: 4a, the zone with the maximum average power, and 2b, the zone with the maximum power, which were completely evaluated, and zone 1d, which was estimated based on the results of the other evaluations. The results of zone 2b were used to calculate the total iodine release (see Fig. 15.12 for the location of these zones).

The internal dose in rem due to an exposure from a radioactive cloud for a time t is:

$$E (\text{rem}) = \int_0^t \chi(t) \text{ DBR } dt, \quad (16)$$

where

$$\chi = \left[\frac{\chi}{Q} \right] A(t) \delta,$$

Q = activity released, curies/sec,

χ = concentration of activity at point of interest, $\mu\text{c/cc}$,

A = activity in containment, curies,

δ = leakage rate, sec^{-1} ,

BR = breathing rate, cc/sec,

D = conversion factor, rem/ μc ,

t = exposure time, sec.

Therefore

$$E(\text{rem}) = \int_0^t \left(\frac{\chi}{Q} \right) A(t) \delta \text{DBR} dt.$$

If meteorological conditions remain constant and if the leakage rate is constant, Eq. (16) becomes

$$E(\text{rem}) = \left(\frac{\chi}{Q} \right) \delta \text{DBR} \int_0^t A(t) dt. \quad (17)$$

If no allowance is made for the removal of activity from the containment gas due to filters or deposition on the interior surfaces of the containment vessel, $A(t) = A_0 e^{-\lambda t}$. Therefore

$$E(\text{rem}) = \left(\frac{\chi}{Q} \right) \delta \text{DBR} \int_0^t A_0 e^{-\lambda t} dt = \left(\frac{\chi}{Q} \right) \delta \text{DBR} A_0 \frac{1 - e^{-\lambda t}}{\lambda}. \quad (18)$$

The value of χ/Q depends upon the meteorological conditions at the site and may be determined by

$$\frac{\chi}{Q} = \frac{1}{\pi \sigma_y \sigma_z \bar{u}} \exp \left[-1/2 \left(\frac{y^2}{\sigma_y^2} + \frac{h^2}{\sigma_z^2} \right) \right]. \quad (19)$$

The criteria applied in determining an acceptable leakage rate from the GCR-3 are based on meeting the exposure dose values suggested by the

AEC.⁷ Accordingly, three distances or zones are associated with the site: the exclusion area, the low-population zone, and the population-center distance. A definition of these distances or zones is quoted below from ref 7:

"'Exclusion area' means that area surrounding the reactor, in which the reactor licensee has the authority to determine all activities including exclusion or removal of personnel and property from the area. This area may be traversed by a highway, railroad, or waterway, provided these are not so close to the facility as to interfere with normal operations of the facility and provided appropriate and effective arrangements are made to control traffic on the highway, railroad, or waterway, in case of emergency, to protect the public health and safety. Residence within the exclusion area shall normally be prohibited. In any event, residents shall be subject to ready removal in case of necessity. Activities unrelated to operation of the reactor may be permitted in an exclusion area under appropriate limitations, provided that no significant hazards to the public health and safety will result.

"'Low population zone' means the area immediately surrounding the exclusion area which contains residents, the total number and density of which are such that there is a reasonable probability that appropriate protective measures could be taken in their behalf in the event of a serious accident. These guides do not specify a permissible population density or total population within this zone because the situation may vary from case to case. Whether a specific number of people can, for example, be evacuated from a specific area, or instructed to take shelter, on a timely basis will depend on many factors such as location, number and size of highways, scope and extent of advance planning, and actual distribution of residents within the area.

"'Population center distance' means the distance from the reactor to the nearest boundary of a densely populated center containing more than about 25,000 residents."

⁷Atomic Energy Commission, Title 10, Part 100, "Guide for Site Criteria for Power and Testing Reactors."

The three distances or zones are determined as follows:

1. "An exclusion area of such size that an individual located at any point on its boundary for two hours immediately following onset of the postulated fission product release would not receive a total radiation dose to the whole body in excess of 25 rem or a total radiation dose in excess of 300 rem to the thyroid from iodine exposure.
2. "A low population zone of such size that an individual located at any point on its outer boundary who is exposed to the radioactive cloud resulting from the postulated fission product release (during the entire period of its passage) would not receive a total radiation dose to the whole body in excess of 25 rem or a total radiation dose in excess of 300 rem to the thyroid from iodine exposure.
3. "A population center distance of at least 1 and 1/3 times the distance from the reactor to the outer boundary of the low population zone. In applying this guide, due consideration should be given to the population distribution within the population center."

Pressure Buildup in Fuel Elements. - The pressure inside the fuel elements during operation is due to the release of stable xenon and krypton and to the helium that is introduced into the fuel elements during fabrication. The atoms of xenon or krypton produced in a fuel rod are given by the expression

$$N_p = 3 \times 10^{10} \gamma P t, \quad (20)$$

where P is the power in watts, t is time in seconds, and γ is the total fission yield of stable atoms of xenon or krypton. The fraction of those fission products produced in the fuel that escapes by diffusion can be expressed as

$$f = 1 - \frac{6}{90D't} + \frac{6}{\pi^4 D't} \sum_{n=1}^{\infty} \frac{1}{n^4} \exp(-n^2 \pi^2 D't) . \quad (21)$$

By subdividing the fuel into segments and shells, as previously described, the release in a small subdivision is obtained. The total release is then obtained by summing up the releases from all the

subdivisions. The number of atoms of xenon and krypton released is the product of Eqs. (20) and (21):

$$N_R = N_p f . \quad (22)$$

The volume of a noble gas at standard conditions (0°C and 14.7 psia) is given by

$$\begin{aligned} V_i &= \frac{N_{pi} f_i}{6.02 \times 10^{23}} 22,414 \\ &= 3.72 \times 10^{-20} N_{pi} f_i . \end{aligned} \quad (23)$$

The volume for storing gas inside the fuel element is 645 cc. Therefore the partial pressure inside the fuel element due to xenon and krypton is as follows:

$$P_i = \frac{14.7 V_i T_{av}}{(645) 492} \quad (24)$$

where T_{av} = average temperature of gas inside fuel element, °R. Combining Eqs. (20-24) yields

$$P_{Xe} = 1.135 \times 10^{-14} t T_{av} P_f , \quad (25)$$

$$P_{Kr} = 2.06 \times 10^{-15} t T_{av} P_f . \quad (26)$$

The partial pressure due to helium is

$$P_{He} = \frac{14.7}{492} T_{av} = 2.83 \times 10^{-2} T_{av} . \quad (27)$$

Therefore the total pressure in a fuel rod is

$$P = \left(1.34 \times 10^{-14} P_f t + 2.83 \times 10^{-2} \right) T_{av} . \quad (28)$$

If the value of P in Eqs. (25) and (26) is the total power generation in a rod, the value of f is an effective fractional release for the rod and is obtained from the following expression:

$$f_{\text{eff}} = \sum P_s f_s / P_{\text{total}}, \quad (29)$$

where P_s = power generation in a subdivision and f_s = fractional release in the subdivision.

Evaluation of Parameters. - In order to solve the equations given above it is necessary to evaluate a number of parameters, most of which are not firmly established during the initial phases of studying a reactor design. Therefore it has been necessary either to estimate a conservative value based on experience with similar analysis or to study a range of values. The following parameters influence the activity release and exposure dose calculations:

1. fuel cycle and burnup,
2. power distribution in core,
3. cladding temperature,
4. contact resistance between UO_2 and claddings,
5. thermal conductivity of UO_2 ,
6. fission-product-retention properties of UO_2 ,
7. meteorological conditions.

The values used in the study are described below.

1. The fuel cycle used in the analysis is based on moving fuel in four steps from the outer zone of the reactor toward the center of the core. For the purposes of the study the core has been divided into four equal-volume zones. The residence time in each zone is one-fourth of that required to reach a total average burnup of the desired magnitude. The positions of the top and bottom fuel rods are interchanged when the rods are moved from zone 3 to zone 2. Activity release is calculated based on burnups of 10,000, 20,000, and 40,000 Mwd/MT. Pressure buildup in the fuel is based on 40,000 Mwd/MT only.

2. The power distribution in the core is based on ORNL physics studies for the equilibrium core with the fuel cycle described previously. The peak-to-average power in the radial direction is approximately 1.19, and the overall peak-to-average power is about 1.66. The power distribution is shown in Figs. 15.12 and 15.13. Calculations of activity release and pressure buildup were made for fuel rods in zones 2b and 4a

Fig. 15.12. Radial Power Distribution.

Fig. 15.13. Axial Power Distribution.

(see Fig. 15.12 for location). Zone 2b has the highest power density, and zone 4a has the greatest power generation. Therefore both locations were of interest.

3. The cladding temperature used in the study was obtained from G. Samuels of ORNL. The temperature of the coolant and the fuel cladding along a channel is shown in Fig. 15.11.

4. During fabrication of the fuel elements, it is necessary to size the cladding so that the UO_2 pellets may be inserted into the rods. This results in an initial gap between the UO_2 and the cladding. During operation the UO_2 and cladding expand, and the expansion tends to close the gap. In a like manner, in those regions of the core where the cladding temperature is high, the system pressure of 400 psi will tend to collapse the cladding on the UO_2 and effectively eliminate any real gas gap. In order to present a conservative analysis, it has been assumed that a contact resistance exists between the UO_2 and the cladding. For the purposes of analysis, this resistance is assumed to be a 1-mil gas gap. The temperature drop across the gap will depend upon the thermal conductivity of the gas in the gap. The gas in the gap will initially be helium but will become a mixture of helium, xenon, and krypton following power generation in the fuel. Since the thermal conductivity of xenon and krypton is substantially less than that of helium, the temperature drop across the gap will increase with burnup. The analysis is based on the assumption that the gap contains a mixture of helium and noble gases such that the thermal conductivity of the mixture is one-fourth that of helium. Although the relative amount of helium and noble gases has not been determined, the extremely large free volume inside the fuel rod would indicate the assumption to be conservative. (The void-to-fuel ratio inside the GCR-3 fuel rods is four times as great as for the EGCR fuel rods.)

5. The estimation of UO_2 temperature is of great importance since the release of fission products by diffusion increases rapidly with increasing temperature. The temperature in the UO_2 depends upon the thermal conductivity value of the UO_2 . The many factors influencing the thermal conductivity of UO_2 are described elsewhere.⁴ The data described⁴ indicated, at the time, that a conservative value for analysis would be

1.0 Btu hr⁻¹ ft⁻¹(°F)⁻¹. This value was used in the analyses for the EGCR. More recent data obtained at ORNL⁸ indicate that a conservative value for the thermal conductivity of UO₂ is 1.5 Btu hr⁻¹ ft⁻¹(°F)⁻¹. Although the thermal conductivity varies with temperature, a constant value was used since the temperature dependence is not great in the range where greatest accuracy is needed for the calculations. The use of a constant value of thermal conductivity results in estimating higher fuel temperature in those portions of a channel where cladding temperature and power generation are low. In order to compare the results of this study with similar studies performed for the EGCR and to evaluate the effect of assuming a lower value of thermal conductivity, the analysis has been performed for both values of thermal conductivity [1.0 and 1.5 Btu hr⁻¹ ft⁻¹(°F)⁻¹].

6. The physical properties of the UO₂ material influence the ability of the fuel material to retain fission products. This is reflected in the value of D' for the fuel material. The many factors influencing D' have been described.⁴ It is assumed in this study that the D' for the GCR-3 fuel will be the same as the value used in EGCR studies (D' at 1400°C = 10⁻¹⁰ sec⁻¹). The study of activity release is restricted to a study of the release of radioactive iodine. Therefore it is assumed that the release of iodine is at the same rate as the release of xenon and krypton.

7. In order to assess the hazards of a reactor, the most adverse meteorological conditions are assumed. In order to determine the most adverse conditions that are realistic, it is necessary to have records of wind speed and information for estimating values of σ_y and σ_z [Eq. (19)]. Since this information is not available for the GCR-3 site, the conditions used in the study are based on the worst conditions possible, that is, moderately stable conditions with a wind speed of 2 meters/sec (see curve F, Figs. 15.14 and 15.15, and Table 15.1). The effect of varying wind speed was investigated for wind speeds between 1 and 3 meters/sec.

⁸Oak Ridge National Laboratory, "GCR Quar. Prog. Rep. March 31, 1962," USAEC Report ORNL-3302, p. 252.

Fig. 15.14. (a) Horizontal and (b) Vertical Dispersion Coefficients as a Function of Distance from Source. (From S. D. Beck, "The Diffusion of Radioactive Fission Products Porous Fuel Elements," USAEC Report BMI-1433, Battelle Memorial Institute, April 1960.)

Fig. 15.15. Downwind Ground Level Concentration as a Function of Wind Velocity and Distance from Release Point for a Ground Level Release. (See Fig. 15.14 for definition of A-F. Taken from S. D. Beck, "The Diffusion of Radioactive Fission Products Porous Fuel Elements," USAEC Report BMI-1433, Battelle Memorial Institute, April 1960.)

Table 15.1. Meteorological Categories^a

A: Extremely unstable conditions D: Neutral conditions (applicable
 B: Moderately unstable conditions to heavy overcast, day or
 C: Slightly unstable conditions night)
 E: Slightly stable conditions
 F: Moderately stable conditions

Surface Wind Speed (meter/sec)	Daytime Insolation			Thin Overcast or $\geq 4/8$ Cloudiness ^b	
	Strong	Moderate	Slight	$\leq 3/8$ Cloudiness	
< 2	A	A-B	B		
2	A-B	B	C	E	F
4	B	B-C	C	D	E
6	C	C-D	D	D	D
> 6	C	D	D	D	D

^aInformation from W. F. Hilsmeir and F. A. Gifford, Jr., "Graphs for Estimating Atmospheric Dispersion," U. S. Weather Bureau Station, Oak Ridge, Tennessee.

^bThe degree of cloudiness is defined as that fraction of the sky above the local apparent horizon which is covered by clouds.
 Ref: Manual of Surface Observations (WBAN), Circular N(7th ed.), paragraph 1210, U. S. Government Printing Office, Washington (July 1960).

Discussion of Results

Previous hazards studies have shown that the isotopes contributing the most significant exposure dose during a maximum credible accident (MCA) are the isotopes of iodine. Therefore the study of activity release has been confined to a study of the release of radioactive iodine.

Activity Release from a Failed Fuel Element

The release of radioactive iodine from fuel elements in various positions of the core is shown in Table 15.2. It may be seen that the release is dependent upon power density and the assumed value of thermal conductivity of UO_2 . Although the power density along a channel is symmetrical about the center line of the core, the activity release in those fuel elements in the top portion of the core is greater than for the elements in the bottom half of the core. This results from the higher cladding temperatures in the top part of the core.

The activity release for various burnups, ranging from 10,000 to 40,000 Mwd/MT, was investigated and was found to be independent of burnup for burnups exceeding the 10,000 Mwd/MT. This condition exists because the activities studied are all relatively short-lived, the longest being I^{131} with a half-life of approximately 8 days. Equilibrium is reached in approximately six half-lives. Therefore only for such fission products as Sr^{90} will the effect of burnup be appreciable.

In order to determine the effect of the thermal conductivity of UO_2 on the activity release, the release of iodine was computed in zone 2b, with a thermal conductivity of $1.0 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$ assumed. The use of the lower value of thermal conductivity results in higher UO_2 temperatures and a greater activity release. The release of iodine in zone 2b increases by a factor of about 3.5 for the lower thermal conductivity.

Activity Release from the Core

The activity release from the core at the time of a major accident would depend upon the number of failed fuel elements and the activity release per failed element. Since the number of fuel element failures cannot be estimated from available information, it is assumed that all the fuel elements in the core fail. The total activity release from the

Table 15.2. Activity Release from a Failed Fuel Element

Zone	Position in Channel	Activity Release (curies per rod)					Average Power Generation (Btu hr ⁻¹ ft ⁻¹)
		I ¹³¹	I ¹³²	I ¹³³	I ¹³⁴	I ¹³⁵	
4a ^a	Top	39.8	7.7	29.2	7.2	15.8	4.93 × 10 ⁴
	Bottom	$\frac{22.5}{62.3}$	$\frac{4.3}{12.0}$	$\frac{16.5}{45.7}$	$\frac{4.1}{11.3}$	$\frac{8.9}{24.7}$	
2b ^a	Top	26.0	4.4	19.4	4.6	9.9	4.34 × 10 ⁴
	Bottom	$\frac{17.2}{43.2}$	$\frac{3.0}{7.4}$	$\frac{12.8}{32.2}$	$\frac{3.0}{7.6}$	$\frac{6.5}{16.4}$	
1d ^a	Top	9.0	1.8	7.2	1.8	3.8	3.26 × 10 ⁴
	Bottom	$\frac{5.9}{14.9}$	$\frac{0.9}{2.7}$	$\frac{3.8}{11.0}$	$\frac{0.9}{2.7}$	$\frac{2.0}{5.8}$	
2b ^b	Top	90	17	67	16	35	4.34 × 10 ⁴
	Bottom	$\frac{60}{150}$	$\frac{10}{27}$	$\frac{44}{111}$	$\frac{11}{27}$	$\frac{24}{59}$	
Average for core	Top	23.5	4.0	17.5	4.2	9	4.16 × 10 ⁴
	Bottom	$\frac{15.5}{39}$	$\frac{2.6}{6.6}$	$\frac{11.5}{29}$	$\frac{2.7}{6.9}$	$\frac{6}{15}$	
Total release (based on zone 2b)	Top	1.93 × 10 ⁵	3.27 × 10 ⁴	1.44 × 10 ⁵	3.42 × 10 ⁴	7.36 × 10 ⁴	
	Bottom	$\frac{1.28 \times 10^5}{3.21 \times 10^5}$	$\frac{2.23 \times 10^4}{5.5 \times 10^4}$	$\frac{0.95 \times 10^5}{2.39 \times 10^5}$	$\frac{2.23 \times 10^4}{5.65 \times 10^4}$	$\frac{4.83 \times 10^4}{14.19 \times 10^4}$	
		Total curies of iodine released:					8.13 × 10 ⁵

^aUO₂ thermal conductivity = 1.5 Btu hr⁻¹ ft⁻¹ (°F)⁻¹.^bUO₂ thermal conductivity = 1.0 Btu hr⁻¹ ft⁻¹ (°F)⁻¹.

core has been estimated based on the activity release from fuel elements in zone 2b. An activity release of I^{131} of about 3.2×10^5 curies (Table 15.2) would be increased by a factor of about 3.5 if the thermal conductivity of UO_2 were assumed to be $1.0 \text{ Btu hr}^{-1} \text{ ft}^{-1} (^\circ\text{F})^{-1}$. However, the release would be significantly reduced if all the fuel elements did not fail at the time of the accident.

Leakage Rates

The leakage rates that could be considered as acceptable to limit the exposure dose to the public depend upon the location of the reactor and the exclusion area and low-population zone associated with the site. Exclusion area and low-population zone distances as a function of wind speed and leak rate are shown in Figs. 15.16–15.19. If a leak rate of 0.5% per day (which is within present construction capabilities) at ground level and the most adverse meteorological conditions are assumed, it is seen that for a thermal conductivity of $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (^\circ\text{F})^{-1}$, the exclusion distance is 250 meters and the low-population zone distance is 5000 meters. If the thermal conductivity of UO_2 is assumed to be 1.0, the release is increased such that the above distances would need to be 515 and 13,500 meters.

The results indicated in Figs. 15.16–15.19 are conservative in that they are based on a ground-level release and the most unfavorable meteorological condition (curve F, Fig. 15.15). It is unlikely that either condition would occur over the entire time of release following an MCA. No allowance has been made for deposition or collection of fission products in the containment-shell filtration prior to release or for the pressure decay in the containment shell. Allowances for either deposition or filtration would be sufficient to reduce the dose rates by at least 50%.

By definition, it is possible to evacuate the occupants of the low-population zone following an MCA. The relationship between exposure dose in the low-population zone and exposure time is shown in Fig. 15.20.

The probability of the 300-rem value being reached is very small since extremely adverse weather conditions could not exist for a period of time long enough to result in an exposure level of this magnitude.

Fig. 15.16. Effect of Containment Leakage Rate and Wind Velocity on Required Exclusion Area. UO_2 thermal conductivity, $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1}$.

Fig. 15.17. Effect of Containment Leakage Rate and Wind Velocity on Required Exclusion Area. UO_2 thermal conductivity, $1.0 \text{ Btu hr}^{-1} \text{ ft}^{-1}$.

Fig. 15.18. Effect of Containment Leakage Rate and Wind Velocity on Required Low-Population Zone Distance. UO_2 thermal conductivity, $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1}$.

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Fig. 15.19. Effect of Containment Leakage Rate and Wind Velocity on Required Low-Population Zone Distance. UO_2 thermal conductivity, $1.0 \text{ Btu hr}^{-1} \text{ ft}^{-1}$.

Fig. 15.20. Exposure Dose at Low-Population Zone Distance for Various Times Following MCA.

Pressure Buildup in Fuel Elements

The pressure buildup inside the fuel elements depends upon the fuel geometry, the fractional release of stable noble gases, and the temperature of the gases contained within the fuel element. Since most of the gases are contained in the volume in the central part of the fuel rod, the UO_2 central temperature will influence the pressure inside the fuel rod. Therefore both thermal conductivity of UO_2 and power distribution will affect the pressure buildup. In a like manner, both factors influence the fractional release of stable fission products. In addition, the burnup or the exposure time will affect the pressure buildup, since the number of atoms of xenon or krypton escaping from the fuel will be directly proportional to time for a given fractional release.

The pressure buildup in a fuel rod will vary each time the fuel rod is moved in the reactor. Although the highest average power density in a channel occurs in zone 4, the pressure buildup in the fuel in this zone will not be as great as in other zones because the fuel is fresh and only a small partial pressure will have built up due to noble-gas release. When the fuel is moved from zone 4 to zone 3, there will be additional noble-gas release, and, at the same time, the power generation and gas temperature will be almost as great as in zone 4. In a like manner, as the fuel is subsequently moved to zones 1 and 2, additional noble-gas release will result in an increased partial pressure. However, when the fuel is in zone 1, the power density decreases such that the fractional release is appreciably reduced and, at the same time, the average gas temperature is decreased.

In order to determine the pressure buildup in the fuel rods, the fractional release of noble gases in zone 2b was estimated. The release in zones 1, 3, and 4 was assumed to be the same as in zone 2b. Although the release would probably be slightly higher in zones 3 and 4, the fractional release in zone 1 would be appreciably less, so that this assumption does not lead to a significant change in the result. In order to calculate the pressure, the UO_2 central temperature along the rod in zone 2b was used. The results are listed below for a burnup of 40,000 Mwd/MT and for top and bottom channel positions:

	Pressure (psia)	
	Top	Bottom
Helium	72.6	65.5
Xenon	22.0	13.2
Krypton	<u>3.9</u>	<u>2.4</u>
	98.5	81.1

The effective fractional release for the rod in the top of the channel is 3.7% and is 2.47% in the bottom of the channel. The effective temperature for computing pressure in the rod in the top and bottom positions is 2106 and 1853°F, respectively. These effective temperatures are quite low since the rods extend to portions of the core where the power density is quite low. For example, the rod in the top of a channel has UO₂ central temperatures that vary between 1415 and 2607°F. However, approximately half the central temperatures are below the average temperature.

The pressure buildup for burnups less than 40,000 Mwd/MT will not be changed appreciably since the partial pressure due to helium is so dominant. A change in burnup will alter the fractional release and the exposure time. The fractional release for small releases may be expressed by

$$f = 4 \left(\frac{D't}{\pi} \right)^{1/2}.$$

Therefore the partial pressure due to noble-gas release for burnups less than 40,000 Mwd/MT can be expressed as

$$p_1 = p_2 \left(\frac{B_2}{B_1} \right)^{-3/2},$$

where

p_1 = partial pressure due to xenon or krypton at a burnup of B_1 Mwd/MT,

p_2 = partial pressure due to xenon or krypton at a burnup of 40,000 Mwd/MT,

B_2 = 40,000 Mwd/MT.

From this approximation the following internal fuel element pressure results:

	Pressure (psia)		
	At 10,000 Mwd/MT	At 20,000 Mwd/MT	At 40,000 Mwd/MT
Helium	72.6	72.6	72.6
Xenon	2.8	7.8	22.0
Krypton	0.5	1.4	3.9
	<u>75.9</u>	<u>81.8</u>	<u>98.5</u>

The values of internal pressure given above were, in both cases, based on a thermal conductivity of UO_2 of $1.5 \text{ Btu hr}^{-1} \text{ ft}^{-1} (\text{°F})^{-1}$. If the thermal conductivity of UO_2 were 1.0, the effect would be to increase the fractional release and the UO_2 central temperature and thus the temperature of the gases stored in the fuel. No estimate was made of the increase in the fractional release, but the increase in the average UO_2 central temperature would be about 300°F . This would increase the partial pressure due to helium by about 19%, or from 72.6 psia to about 86 psia.

APPENDIX A

GCR-3 Fuel Cost Calculations

In the entry "Charges Incurred During Fabrication" in Table A.1 conversion and fabrication are considered to be performed by the same contractor; shipping and use charges during these operations are not broken down separately; costs due to recycled scrap are assumed to be included in the conversion and fabrication costs; and it is assumed that processing costs associated with the recycling (rework) are included in the unit charges. In all instances, except where noted otherwise, the charges were based on 1 kg of uranium charged into the reactor.

Table A.1. Data for Establishing Fuel Costs

Charges Incurred During Fabrication

Processing Charges

Conversion and fabrication

Quantity	1.00 kg of U
Unit charges	\$52.00
Conversion and fabrication	\$52.00

Shipping Charges

Transit to conversion site and
to fabricator

Quantity	1.00 (1 + 0.01 + 0.01 + 0.10) = 1.12 kg of U
Unit charge	\$1.50
Shipping	$1.12 \times 1.50 = \$1.68$

Transit, fuel to reactor and
recycle scrap to AEC

Quantity	1.10 kg of U
Unit charge	\$1.50
Shipping	$1.10 \times 1.50 = \$1.65$

Table A.1. (continued)

Use Charges

Transit to conversion site and
to fabricatorQuantity $1.0 (1.0 + 0.01 + 0.01 + 0.10)$
= 1.12 kg of U fed

Time 20 days

Unit price

1961 basis^a \$281.201962 basis^b \$254.30

Use charge

1961 basis^a $1.12 \times 0.0475 \times 281.20 \times \frac{20}{365}$
= \$0.8181962 basis^b $1.12 \times 0.0475 \times 254.30 \times \frac{20}{365}$
= \$0.74

Conversion and fabrication

Quantity (average) $1.0 + 0.02 + 0.10$
= 1.12 kg of UTime $\frac{21.8}{4.0 \text{ MT/30 days}}$
= 164 days

Unit price

1961 basis^a \$281.201962 basis^b \$254.30

Use charge

1961 basis^a $1.12 \times 0.0475 \times 281.20 \times \frac{164}{365}$
= \$6.731962 basis^b $1.12 \times 0.0475 \times 254.30 \times \frac{164}{365}$
= \$6.07Transit to reactor and recycle
scrap transit

Quantity 1.10 kg of U

Time 20 days

Table A.1. (continued)

Unit price	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Use charge	
1961 basis ^a	$1.10 \times 0.0475 \times 281.20 \times \frac{20}{365}$ = \$0.805
1962 basis ^b	$1.10 \times 0.0475 \times 254.30 \times \frac{20}{365}$ = \$0.728
Uranium losses	
Conversion and fabrication losses	
Quantity lost	$1.00 \times (0.01 + 0.01)$ = 0.02 kg of U
Unit price	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Loss	
1961 basis ^a	$0.02 \times 281.20 = \$5.62$
1962 basis ^b	$0.02 \times 254.30 = \$5.09$
Charges Incurred at Reactor	
Processing charges	None
Shipping charges	None
Use charges	
Delivery of charging batch prior to charging	
Quantity	1.00 kg of U
Time	30 days
Unit price	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Use charge	
1961 basis ^a	$1.00 \times 0.0475 \times 281.20 \times \frac{30}{365}$ = \$1.10
1962 basis ^b	$1.00 \times 0.0475 \times 254.30 \times \frac{30}{365}$ = \$0.99

Table A.1. (continued)

Spare fuel inventory

Quantity	4.0 MT of U total or $\frac{4.0}{87.20}$ = 0.05 kg of U
Time	1121 days = average residence of fuel in reactor ^c
Unit price	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Use charge	
1961 basis ^a	$0.05 \times 0.0475 \times 281.20 \times \frac{1121}{365}$ = \$2.05
1962 basis ^b	$0.05 \times 0.0475 \times 254.30 \times \frac{1121}{365}$ = \$1.85

Irradiation inventory

Quantity	1.00 kg of U
Time	$\frac{19,637 \text{ Mwd/MT of U}}{0.80 \times 1908 \text{ Mw(t)}/87.2 \text{ MT of U}}$ = 1121 days
Unit price (average)	
1961 basis ^a	$1/2 \times (281.20 + \frac{21.8}{87.2} \times 89.44)$ = \$151.80
1962 basis ^b	$1/2 \times (254.30 + \frac{21.8}{87.2} \times 89.44)$ = \$138.33
Use charge	
1961 basis ^a	$1.00 \times 0.0475 \times 151.80 \times \frac{1121}{365}$ = \$22.15
1962 basis ^b	$1.0 \times 0.0475 \times 138.22 \times \frac{1121}{365}$ = \$20.20

Decay storage

Quantity	0.97 kg of U
Time	120 days
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26

Table A.1. (continued)

Use charge	
1961 basis ^a	$0.97 \times 0.0475 \times 89.44 \times \frac{120}{365}$
	= \$1.35
1962 basis ^b	$0.97 \times 0.0475 \times 83.26 \times \frac{120}{365}$
	= \$1.26
U ²³⁵ Consumption	
Uranium consumed	
Initial quantity	1.00 kg
Unit price	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Initial value	
1961 basis ^a	\$281.20
1962 basis ^b	\$254.30
Discharge quantity	0.97 kg of U
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Discharge value	
1961 basis ^a	0.97×89.44
	= \$86.70
1962 basis ^b	0.97×83.26
	= \$80.80
Depletion charge	
1961 basis ^a	$281.20 - 86.70$
	= \$194.50
1962 basis ^b	$254.30 - 80.80$
	= \$173.50
Plutonium Consumption or Production	
Plutonium credit	
Quantity	$6.76 \text{ g of Pu} \times 0.97 \text{ kg of U}$
	= 6.56 g of Pu

Table A.1. (continued)

Unit price (per g)	
1961 basis ^a	\$12
1962 basis ^b	\$8
Credit	
1961 basis ^a	6.56×12 = \$78.70
1962 basis ^b	6.56×8 = \$52.48

Charges Incurred in Chemical Processing

Processing charges

Separation

Quantity	0.97 kg of U
Process rate	1.0 MT of U per day
Batch size	21.8 MT of U
Unit charge	\$17.00, plus turnaround
Turnaround	8 days
Separation	$0.97 \times 17.0 \times \left[1 + \left(\frac{8}{21.8} \right) \right]$ = $0.97 \times 17.0 \times 1.367$ = \$22.55

Conversion, uranium

Quantity	$0.97 \times 0.99 = 0.96$ kg
Unit charge	\$5.60
Conversion	0.96×5.60 = \$5.37

Conversion, plutonium

Quantity	$0.97 \times 6.76 \times 0.99$ = 6.48 g
Unit charge (per g)	\$1.50
Conversion	6.48×1.50 = \$9.73

Table A.1. (continued)

Shipping Charges

Transit to processing site

Quantity	0.97 kg of U
Unit charge	\$16
Shipping	16×0.97 = \$15.52

Transit of U and Pu to AEC
receiving (supply) point

Quantity	$0.97 \times 0.99 \times 0.997$ = 0.958 kg of U
Unit charge	\$1
Shipping	0.958×1.00 = \$0.96

Use Charges

Transit to processing site

Quantity	0.97 kg of U
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Time	20 days
Use charge	
1961 basis ^a	$0.97 \times 0.0475 \times 89.44 \times \frac{20}{365}$ = \$0.23
1962 basis ^b	$0.97 \times 0.0475 \times 83.26 \times \frac{20}{365}$ = \$0.21

Chemical separation

Quantity	0.97 kg of U
Process rate	1.0 MT of U per day
Batch size	21.8 MT of U
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Time	$\frac{21.8}{1} + 30$ = 52 days/batch

Table A.1. (continued)

Use charge	
1961 basis ^a	$0.97 \times 0.0475 \times 89.44 \times \frac{52}{365}$ = \$0.59
1962 basis ^b	$0.97 \times 0.0475 \times 83.26 \times \frac{52}{365}$ = \$0.55
Conversion	
Quantity	0.97×0.99 = 0.96 kg of U
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Time	$0.99 \times \frac{21.8}{1} + 5$ = 27 days
Use charge	
1961 basis ^a	$0.96 \times 0.0475 \times 89.44 \times \frac{27}{365}$ = \$0.30
1962 basis ^b	$0.96 \times 0.0475 \times 83.26 \times \frac{27}{365}$ = \$0.28
Transit to AEC receiving point	
Use charge	Included in separation and conversion use charges
U ²³⁵ Losses	
Separation loss, uranium	
Quantity	0.01×0.97 = 0.0097 kg of U
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Loss	
1961 basis ^a	89.44×0.0097 = \$0.87
1962 basis ^b	83.26×0.0097 = \$0.81

Table A.1. (continued)

Conversion loss, uranium

Quantity	$0.003 \times 0.99 \times 0.97$ $= 0.0029 \text{ kg}$
Unit price	
1961 basis ^a	\$89.44
1962 basis ^b	\$83.26
Loss	
1961 basis ^a	89.44×0.0029 $= \$0.26$
1962 basis ^b	83.26×0.0029 $= \$0.24$

Plutonium Losses

Separation loss, plutonium

Quantity	$0.01 \times 6.76 \times 0.97$ $= 0.0655 \text{ g}$
Unit price (per g)	
1961 basis ^a	\$12
1962 basis ^b	\$8
Loss	
1961 basis ^a	12×0.0655 $= \$0.79$
1962 basis ^b	8×0.0655 $= \$0.524$

Conversion loss, plutonium

Quantity	$0.01 \times 0.99 \times 6.76 \times 0.97$ $= 0.0648 \text{ g}$
Unit price (per g)	
1961 basis ^a	\$12
1962 basis ^b	\$8
Loss	
1961 basis ^a	12×0.0648 $= \$0.78$
1962 basis ^b	8×0.0648 $= \$0.518$

Table A.1. (continued)

Unit fuel cost - fuel processed

1961 basis ^a	\$269.63
1962 basis ^b	\$270.54
Average fuel exposure	19,637 Mwd/MT of U
Based on overall plant efficiency of 39.4%	
Net yield	$(19,637 \times 10^{-3})$ Mwd/kg of U $\times$ $(0.394 \times 24 \times 10^3)$ kwhr/Mwd $= 18.57 \times 10^4$ kwhr

Fuel cost

1961 basis ^a	$\frac{(\$269.63/\text{kg of U}) \times 10^3 \text{ (mills/\$)}}{18.57 \times 10^4 \text{ kwhr}}$ $= 1.45 \text{ mills/kwhr}$
1962 basis ^b	$\frac{(\$270.54/\text{kg of U}) \times 10^3 \text{ (mills/\$)}}{18.57 \times 10^4 \text{ kwhr}}$ $= 1.46 \text{ mills/kwhr}$

Unit fuel cost - fuel not processed

1961 basis ^a	\$377.08
1962 basis ^b	\$346.56
Average fuel exposure	19,637 Mwd/MT of U
Based on overall plant efficiency of 39.4%	
Net yield	$(19,637 \times 10^{-3})$ Mwd/kg of U $\times$ $(0.394 \times 24 \times 10^3)$ kwhr/Mwd $= 18.57 \times 10^4$ kwhr

Fuel cost

1961 basis ^a	$\frac{(\$377.08/\text{kg of U}) \times 10^3 \text{ (mills/\$)}}{18.57 \times 10^4 \text{ kwhr}}$ $= 2.03 \text{ mills/kwhr}$
1962 basis ^b	$\frac{(\$346.56/\text{kg of U}) \times 10^3 \text{ (mills/\$)}}{18.57 \times 10^4 \text{ kwhr}}$ $= 1.87 \text{ mills/kwhr}$

^aFrom "AEC Schedule of Base Fuel Charges for July 1, 1961."^bFrom "AEC Schedule of Base Fuel Charges for July 1, 1962."^cSee "Irradiation inventory."

Table A.2. GCR-3 Fuel Cost Parameters

Design Parameters	
Fuel composition	UO ₂ , 1.0-in.-OD pellets
Cladding material	Stainless steel
Fuel enrichment when charged to reactor, %	3.0
Fuel enrichment when discharged from reactor, %	1.38
Average fuel exposure, Mwd/MT of U	19,637
Plutonium concentration in discharged fuel, g per kg of U discharged	6.76
Rated gross power level, Mw(t)	1908
Rated net power level, Mw(e)	750
Reactor fuel loading, initial, MT of U	87.2
Total fuel discharged per initial fuel loading, MT of U	21.8
Description of fuel management program	Replacing 1/4 of core every 7 months and out-in shuffling
Operating Parameters, Set by Industry	
Predicted plant operating factor, %	80
Shipping time, AEC to fabricator, days	20
Shipping time, fabricator to reactor, days	20
Shipping time, recycle scrap to AEC, days	20
Shipping time, reactor to chemical processing site, days	20
Conversion and fabrication plant throughput rate, MT of U per month	4.0
Time interval between delivery of fuel batch to reactor site and charging to reactor, days	30

Table A.2. (continued)

Spare fuel maintained on hand at all times, exclusive of discrete charging batches (average), MT of U	4.0
Batch size charged to reactor per refueling, MT of U	21.8
Batch size discharged from reactor per refueling, MT of U	21.8
Number of discharge batches accumulated for chemical processing campaign	1
Irrecoverable losses during conversion of UF_6 to UO_2 , % ^a	1.0
Irrecoverable losses during fabrication, % ^a	1.0
Conversion and fabrication, recycle to AEC, % ^a	10.0
Operating Parameters, Set by AEC	
Minimum decay cooling period for irradiated fuel, days	120
Irrecoverable loss during chemical separation, % ^b	
Uranium	1.0
Plutonium	1.0
Irrecoverable loss during conversion, % ^c	
Uranium	0.3
Plutonium	1.0
Chemical separation plant processing rate, MT of U per day	d
Economic Parameters, Set by Industry	
Conversion and fabrication processing cost (excluding shipping, use charges, and losses), ^e \$ per kg of U	52
Shipping charge, \$ per kg of U shipped	
AEC to fabricator	1.50
Fabricator to reactor	1.50
Reactor to chemical processing site	16

Table A.2. (continued)

Economic Parameters, Set by AEC

Use charge rate, %/year	4.75
Uranium price at enrichment prior to irradiation (as UF_6) ^f , \$ per kg of U	
1961 cost ^g	281.20
1962 cost ^h	254.30
Uranium price at discharge enrichment (as UF_6) ^f , \$ per kg of U	
1961 cost ^g	89.44
1962 cost ^h	83.26
Conversion charge, UNH^i to UF_6 , \$ per kg of U fed to conversion	5.60
Conversion charge, Pu nitrate to Pu metal, \$ per g of Pu fed to conversion	1.50
Pu price (credit) ^j , \$ per g	
1961 cost ^g	12
1962 cost ^h	8
Shipping charge, chemical process site to AEC receiving plants (U and Pu) ^k , \$ per kg of U shipped	1.0
Separations plant daily charge ^l , \$	17,000

^aPercentage of finished product from fabrication.

^bPercentage of weight fed to separation.

^cPercentage of weight fed to conversion.

^dSee Table 460-1 in "Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025 (Vol. 4), March 15, 1962.

^eEGCR costs.

^fSee Tables 440-1 and 440-3 in "Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025 (Vol. 4), March 15, 1962.

^gAEC Schedule of Base Fuel Charges for July 1, 1961.

^hAEC Schedule of Base Fuel Charges for July 1, 1962.

ⁱUranium nitrate hexahydrate.

^jDelivered to AEC as metal.

^kIncluded charge for shipping Pu.

^lEscalated, see Fig. 460-1 in "Guide to Nuclear Power Cost Evaluation," USAEC Report TID-7025 (Vol. 4), March 15, 1962.

Fig. A.1. Indirect Construction Cost Curves.

Internal Distribution

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