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GMAD 3078-20
AEC RESEARCH AND
DEVELOPMENT REPORT

6

MCR System Parametric Studies (U)

Alternate Component Support Studies (U)

United States Atomic Energy Commission

Contract AT(30-1)-3078

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Alternate Component Support Studies (U)

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United States Atomic Energy Commission

Contract AT(30-1)-3078

R. S. Strzelecki
Program Manager

D. J. Mallon
Assistant Chief Engineer
Nuclear Systems

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ABSTRACT

A comprehensive parametric analysis of MCR-type mobile nuclear powerplants in the range of 1.5 to 15 Mw net electrical power output is presented in the four volumes of this report. The several phases of the study are reported as follows:

- Volume I—Parametric studies of the basic concept system
- Volume II—Parametric packaging studies of the basic concept system
- Volume III—Parametric studies of systems with alternate component concepts
- Volume IV—Support studies of other alternate component concepts

The independent parameters selected to define the basic concept system are reactor outlet temperature, liquid metal temperature rise in the reactor, maximum coolant velocity in the reactor, reactor vessel diameter, number of heat exchanger tubes per pass per transverse pitch, engine compression ratio, and turbine inlet temperature. The values of the independent parameters which produce maximum net electrical output with respect to plant weight do not change over the range of power output.

Parametric packaging studies show that systems with package weight limits of 35,000, 50,000, 65,000, and 100,000 lb are capable of producing 4.25, 6.7, 9.1, and 14.65 Mw net electrical output, respectively.

Studies of systems incorporating alternate component concepts show that three alternates offer increased system performance—increased reactor fuel burnup, adding intercooling and regeneration to the open Brayton cycle, and using helium or nitrogen in closed and pressurized Brayton cycles with intercooling and regeneration. Also, the use of a borated water shield is attractive from a cost viewpoint.

Support studies of other alternate component concepts show that these components offer possibilities for improvement of system performance or cost as follows: minimum ceramic fuel loading of Cb cermet, hollow pin fuel elements, two-zone fuel enrichment loadings, reflector thickness variation, LiH casting, boral in the shield, LiH as the innermost shield layer, shaped shielding, borated plastic shielding, replacement of shield B¹⁰ with natural B, self-contained primary loop pressurization, and modular plate primary heat exchangers.

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I. SUMMARY

At the time of the initiation of the MCR parametrics studies, a number of alternate component designs or concepts appeared to offer potential advantages but had not been investigated in sufficient detail to warrant inclusion in the parametric variations. These items were, therefore, studied individually for feasibility and for cost, size, weight, or system performance advantages. The studies, eighteen in number, but some involving multiple alternatives, are reported herein.

The following recommendations stem from these studies.

- Offering possibilities for improvement of performance, reduction in size or weight, or cost reduction, dependent on further study are:
 - Minimum ceramic fuel loading of Cb cermet
 - Hollow pin fuel elements
 - Boral in the shield
 - LiH as innermost shield layer
 - Shaped shielding
 - Borated plastic shielding
 - Replacement of shield B-10 with natural B
 - Self-contained-compressor primary loop pressurization
 - Modular plate primary heat exchangers
- Concepts offering advantages and recommended for further study if still pertinent when MCR concept selection is made are:
 - Two-zone fuel enrichment loadings
 - Reflector thickness variation
 - LiH casting
- Items recommended for no further study because extensive development or testing programs or extension of knowledge would be required for relatively small improvements are:
 - Plate fuel elements
 - Nucleate boiling without net vapor generation
- Not recommended for further consideration because of various deficiencies are:
 - Bellows compensation of primary loop expansion
 - Off-skid location of major power generation package components
 - Alternate methods of reactor control
 - Electromagnetic main coolant pumps
 - Rankine cycle or thermoelectric afterheat power conversion

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- Recommended for further consideration only if other alternatives are not available are:
 - Self-pressurization of the primary loop
 - Horizontal pressure vessel configurations

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II. INTRODUCTION

In FY 1964, the Allison Division of General Motors, under Contract AT(30-1)-3078, initiated a comprehensive parametric study of MCR-type mobile power units in the electrical power output range of 1.5 to 15 Mw_e. The objective of this study was to define the system and component characteristics and prepare a conceptual design of a mobile power unit optimized to best meet the design objectives for Energy Depot application. The system parametric studies are presented in four volumes as follows:

- Volume I—Basic Concept
- Volume II—Packaging Parametric Studies Basic Concepts
- Volume III—Alternate Component Concepts
- Volume IV—Alternate Component Support Studies

The study was scheduled to proceed from a parametric phase to a conceptual design of one or more plants optimized for the specified application. The following outline of the overall parametric study shows the four phases of effort:

- Phase I—System Parametric Studies
 - Basic Concept
 - Alternate Component Concepts
 - Alternate Component Support Studies
- Phase II—Packaging Parametric Studies
 - Basic Concepts
 - Alternate Concepts
- Phase III—Concept Studies
- Phase IV—Concept Selection

The effort completed as part of AEC Contract AT(30-1)-3078 includes Phase I and the Basic Concepts portion of Phase II.

PHASE I

A broad scoping parametric study was conducted on a basic plant concept derived from the 3-Mw_e MCR Preliminary Design.* This basic plant concept was defined by certain physical

*MCR Preliminary Design Report, Allison Division, GMC, GMAD 3078-8 (Allison EDR 3950), 31 July 1964.

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characteristics, such as a fast, liquid metal-cooled reactor; a primary loop with centrifugal pump and finned-tube, cross-counterflow heat exchanger; an open Brayton cycle engine; and a 60-cps electrical power alternator.

Comprehensive component parametric studies were conducted for each of the major components of the basic plant concept. Performance, weight, and cost of these components were determined for variations in system parameters, such as temperature, flow, radiation level, pressure drop, and power level. The range of system parameters selected provides system electrical power outputs of approximately 1.5 to 15 Mw_e.

The component parametric data were combined into a detailed system parametric study of the basic system. The performance, weight, and cost of the total system were determined for selected values of system parameters. The MCR System Integration Computer Program was developed to facilitate the system analyses.

As a part of Phase I effort, several alternate component concepts offering potential reduction in system weight or cost were investigated by substituting alternate component data for basic concept data in the MCR System Integration Computer Program. Separate preliminary studies of the originally proposed alternate component concepts indicated that the nine alternate components selected offered a potential improvement in system performance.

Alternate component support studies were also performed as part of the Phase I effort. The alternate component concepts considered during these support studies offered potential advantages to the system but did not warrant the data development necessary for incorporation of these alternates into the complete system model. The results of studies of eighteen alternate component concepts are reported in this volume. The alternate component concepts which were investigated are:

- Two zones of reactor fuel enrichment
- Minimum ceramic loading of cermet fuel
- Hollow fuel pins
- Nucleate boiling of reactor coolant
- Variations in reflector thickness
- Alternate methods of reactor control
- Horizontal reactor design
- Self-pressurization of the surge tank
- Forced convection afterheat removal
- Off-skid location of power generation package components

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- Electromagnetic primary pumps
- Modular plate primary heat exchanger
- Use of boral in the shield
- LiH as innermost shield material
- Use of shaped shielding
- Use of borated polystyrene and polyethylene in the shield
- Use of natural boron in shield to replace B-10
- Use of cast LiH to replace cold pressed LiH

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III. REACTOR

The alternate component support studies reports are grouped according to the part of the MCR system most affected by each: Reactor; Primary Loop; and Shielding. The following reactor studies are reported:

- Reactor Core Power Flattening by Zone Loading
- Low Loaded Cermets
- Alternate Fuel Element Configurations
- Reactor Coolant Bulk and Nucleate Boiling
- Reflector Thickness Variation
- Alternate Reactor Control Concepts

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Reactor Core Power Flattening by Zone Loading

Flattening the radial distribution pattern of power generation in the reactor core tends to equalize the operating conditions seen by each core element. This can be used to reduce the peak fuel temperatures and peak burnup in the core. Or, preferably, for an advanced MCR design a greater power level from a given core size can be obtained with the same peak fuel temperature and peak burnup. For a given burnup or fuel temperature limited core, the reactor power output will be increased by the ratio of the old to the new max/avg power ratio.

A study was undertaken to assess quantitatively the advantage which could be gained with one practical method of flattening the power—use of a two-zone fuel enrichment core loading. This technique can be employed if the comparable uniformly loaded core is not enrichment limited. Any potential advantage produced must be weighed against the increases in cost which would accrue in fabrication of fuel pins with two different enrichments. Procedures would have to avoid a pin or cluster finding its way into the wrong zone.

CONCLUSIONS AND RECOMMENDATIONS

Compared with MCR configuration uniformly loaded cores, the optimum power capability increases with two-zone core loadings are 14 and 21% for 16- and 24-in. dia cores, respectively. A similar comparison of control drum worths shows increases of 8 and 20%.

It is concluded that two-zone fuel enrichment loading offers attractive improvements in reactor performance and possible reduction in control drum complexity. It is therefore recommended that two-zone fuel enrichment be evaluated in detail for use in the MCR when the concept selection phase of the program has advanced to the point of a definitive selection of the reactor core. The influence of mechanical design aspects on the indicated performance gains should be assessed. Other power flattening techniques, such as use of a BeO reflector, should also be considered.

A study to determine extra cost in fabricating two kinds of fuel elements is recommended before the concept selection phase. The differing elements would have to be kept separate regardless of cost. Special handling techniques, different suppliers, and geometric differences of pins and elements, as well as fuel with two different enrichments, might be required to ensure no mixup. This should be considered in an evaluation of the cost.

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ANALYSIS

The following ground rules were adopted to provide a common basis for comparing the results of this study with data developed under Phase I Parametric Studies:

- Fuel material: 80 v/o UO_2 - 20 v/o Cb cermet
- Initial reactivity, $k = 1.03$
- Reflector thickness = $1/3$ of core diameter
- Standard core and reflector compositions
- The radial power pattern is determined by reactivity calculations employing the infinite cylinder model with the drums (poison) turned out

Figure 3-1 is a schematic diagram of a two-zone enrichment core which illustrates the nomenclature used herein.

There are many combinations of the parameter R_I/R_O with enrichment of the inner and outer zones that will provide an initial reactivity of 1.03. For each combination, there is a different radial power profile with its corresponding peak/average power. To minimize the number of computer problems required to find the combination of E_I , E_O , and R_I/R_O which provides the lowest peak/average power, the following model was assumed.

- The total loading (kg U^{235}) of the two-zoned core was the same as the single zone or uniformly enriched (Eu) base case.
- At any point r , the ratio of the power produced in the two-zoned systems to the power produced in the uniformly enriched system is

$$\frac{E_I}{E_u} \text{ or } \frac{E_O}{E_u}$$

depending on whether point r is in the inner or outer zone, respectively.

where: R = radius
 E = enrichment, % U^{235}
 I = inner zone
 O = outer zone

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Figure 3-1. Two zone enrichment core.

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Based on the first assumption, the following relationships apply:

$$\text{Total mass of } U^{235} = M_T = M_I + M_O$$

$$M_T \propto V_I E_I + V_O E_O$$

$$M_T \propto R_I^2 E_I + (R_O^2 - R_I^2) E_O$$

where

$$M = \text{mass of } U^{235}$$

$$V = \text{zone volume}$$

When the enrichment of the outer zone is raised from E_u to E_O the corresponding increase in loading in the outer zone must equal the decrease in loading in the inner zone. Thus,

$$(R_O^2 - R_I^2) (E_O - E_u) = R_I^2 (E_u - E_I) \quad (3-1)$$

where the uniform enrichment, E_u , is known.

Rearranging and simplifying terms,

$$\left[\frac{1-\beta}{\beta} \right] E_O + E_I = \frac{E_u}{\beta} \quad (3-2)$$

where $\beta \left(\frac{R_I}{R_O} \right)^2$

Based on the second assumption, the power at the center line of the two-zoned core relative to the power at the center line of the uniform enrichment core is equal to the ratio of the enrichment of the inner zone of the two-zone core to the enrichment of the uniform core.

The power in the outer region at a relative radius of R_I/R_O for the two-zoned system is $f(R_I/R_O) \frac{E_O}{E_u}$ where $f(R_I/R_O)$ is the ratio, for the uniform enrichment case, of the position R_I/R_O to the power in the center of the core.

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The minimum peak/average power ratio occurs when the power at the core center line is the same as the power at the interface of the inner and outer enrichment zones, or:

$$\frac{E_I}{E_u} = f(R_I/R_O) \frac{E_O}{E_u}$$

Therefore,

$$\frac{E_I}{E_O} = f(R_I/R_O) \quad \text{for minimum peak/average power} \quad (3-3)$$

If the uniform and the two-zoned cores have the same average power, then

$$\frac{(P/A)_{2Z}}{(P/A)_U} = \frac{(P/A)_{2 \text{ zone}}}{(P/A)_{\text{uniform enrichment}}} = \frac{(\text{peak power})_{2 \text{ zone}}}{(\text{peak power})_{\text{uniform enrichment}}}$$

$$(P/A)_{2Z} = (P/A)_U \frac{(\text{peak power})_{2 \text{ zone}}}{(\text{peak power})_{\text{uniform enrichment}}}$$

Taking peak power values at the center line:

$$(\text{peak power})_{\text{uniform enrichment}} = 1.000 \text{ (normalized)}$$

$$(\text{peak power})_{\text{two zone}} = 1.000 \left[\frac{E_I}{E_u} \right]$$

$$(P/A)_{2Z} = (P/A)_U \left[\frac{E_I}{E_u} \right] \quad (3-4)$$

When equations (3-2), (3-3), and (3-4) are combined, the minimum P/A can be found for each value of R_I/R_O , or:

$$(P/A)_{2Z} \text{ minimum} = \frac{f(R_I/R_O)(P/A)}{\beta f(R_I/R_O) + 1 - \beta} \quad (3-5)$$

Equation (3-5) was used to calculate the peak/average power ratios for 16- and 24-in. dia cores over the potential range of interest of the relative radius, R_I/R_O . The data are presented in Table 3-I. The $f(R_I/R_O)$ values were obtained from power profiles produced for the Phase I parametric studies.

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Table 3-I.
Predicted values of peak/average power ratio.

Relative radius (R_I/R_O)	<u>P/A for D=16 in.</u>	<u>P/A for D=24 in.</u>
1.00*	1.285*	1.393*
0.90	1.168	1.218
0.85	1.140	1.173
0.80	1.120	1.158
0.75	1.114	1.147
0.70	1.115	1.151
0.65	1.118	1.160
0.60	1.131	1.175

*Base case: single zone of uniform enrichment.

For both cores, the lowest peak-to-average value was predicted to occur at a relative radius of about 0.75. (This value is very close to the equal volume ratio of $R_I/R_O = 0.707$.) Utilizing equations (3-2) and (3-3), the inner and outer zone enrichments were predicted for a relative radius of 0.75.

The validity of these procedures and the results predicted thereby were tested by a series of reactivity calculations. UNC Code A-4 (DTF) was used in spherical geometry to compute the reactivity associated with the predicted enrichments. Only minor enrichment adjustments were required to achieve a $k=1.03$, as shown in Table 3-II. Infinite cylinder calculations were then run with the adjusted enrichments to determine the power distribution and peak/average values. Figures 3-2 and 3-3 display the single and two-zone power distributions for the 16- and 24-in. dia cores, respectively. The computed peak/average power ratios compare favorably with the predicted values, as shown in Table 3-II. The very close agreement between the analytically predicted data of Table 3-II and the machine calculations indicates that the equations derived provide a satisfactory procedure for determining these particular nuclear characteristics of two-zone cores.

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Table 3-II.

Comparison of predicted with calculated data at $R_I/R_O = 0.75$.

Core dia (in.)	Predicted			Calculated		
	$E_O\%$	$E_I\%$	P/A	$E_O\%$	$E_I\%$	P/A
16	83.2	61.6	1.114	83.2	62.3	1.123
24	49.23	33.03	1.147	49.23	34.03	1.147

The lower peak-to-average values of the two-zoned cores can be utilized in either of two different ways:

- If the total power of the two-zoned core is maintained the same as the uniformly enriched core, the peak fuel temperatures and burnup will be lowered.
- If the power produced by the central (limiting) fuel pin is the same for the two-zone as the uniform core, then the total power produced by the two-zone core relative to the uniform core is $\frac{(P/A)_U}{(P/A)_{2 \text{ zone}}}$.

Figure 3-2. Radial power distribution - 16-in. core.

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Figure 3-3. Radial power distribution - 24-in. core.

The second option is the preferred approach as it leads to improved reactor performance. Figures 3-2 and 3-3 plot the relative power of one- and two-zoned cores normalized to the same value (1.000) at the center line. The lower P/A value of the two-zoned core permits a 14% increase in power at a 16-in. core diameter and a 21% increase in power for the 24-in. core.

UNC Code A-4 was used in infinite cylinder geometry to determine control drum worth. The results are listed in Table 3-III, together with previously determined data for the uniformly enriched single-zone cores. The increase in control Δk amounts to 8 and 20% for the 16- and 24-in. dia cores, respectively.

Table 3-III.
Control drum worth.

Core dia (in.)	Δk (uniform enrichment)	Δk (two-zone enrichment)
16	0.0998	0.1082
24	0.0797	0.0959

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Low-Loaded Cermets for MCR Fuel

The use of columbium cermet fuels with UO_2 or PuO_2 loadings of 50 to 80 v/o in the MCR system was assessed. The reactors fueled with lower-loaded cermets were then compared with the 80 v/o UO_2 -20 v/o Cb-fueled reactors to determine whether increases in power density could be achieved. Lower-loaded cermets offer a higher thermal conductivity and the potential for higher fuel burnup and lower fission gas release rates.

CONCLUSIONS AND RECOMMENDATIONS

Lower-loaded cermets in conjunction with Pu^{239} or U^{233} fuels appear to offer significant advantages in the entire power range of interest to MCR. The key to establishing this fact is to obtain an accurate definition of the maximum allowable burnup and fission gas release in these cermet fuels as a function of loading. An experimental program would be required to definitively establish these fuel characteristics.

Based on this study, it is recommended that MCR systems, designed with cermets having the minimum ceramic loading to achieve criticality, receive further consideration. Use of Pu^{239} or U^{233} as the fissionable material further enhances the advantages of the low-loaded cermets. Studies must be made to determine the availability and fabricability of these fuel materials before a firm recommendation can be made for their use in MCR.

ANALYSIS

Investigation of the basic reactor parametric studies¹ using 80 v/o UO_2 -20 v/o Cb cermets discloses that the large diameter (>15 in.) and higher power (>15 Mw) cores are limited in power density by the limiting fuel burnup capability and fission gas release. The burnup limitation is reflected in the designs by the need to include more UO_2 (at a lower enrichment) in the core than is needed to meet criticality requirements. The gas release is reflected in increased fission gas space and clad thickness to meet the requirement to contain all the gas released within the pins.

The burnup limit of Cb matrix cermet fuels was not established; however, 70 v/o and 80 v/o UO_2 -Cb specimens performed excellently to burnups of 35,100 and 34,760 Mwd/tonne U, respectively.¹

*Superscripts identify references in Section VI.

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From the limited data available on high-loaded Cb-UO₂ cermet, it is not possible to predict accurately burnup limits as a function of oxide loading. However, it was assumed for this study that variable-loaded cermet operating in the same temperature environment could be irradiated to the same percent of heavy metal atoms (% HMA). Table 3-IV gives the burnup in Mwd/tonne U for UO₂ loadings from 50 to 80 v/o for cermet burnups of 2, 3, and 4% of the heavy metal atoms.

Table 3-IV.
UO₂ burnup for variable-loaded cermet.

<u>v/o UO₂</u>	<u>UO₂ burnup (Mwd/tonne U)</u>		
	<u>2% HMA</u>	<u>3% HMA</u>	<u>4% HMA</u>
80	28,700	43,000	57,300
70	36,200	54,300	72,400
60	46,500	69,800	93,100
50	60,800	91,200	121,600
40	82,300	123,400	164,600
30	117,600	176,400	235,200

Table 3-V shows that if the maximum allowable burnup for variable-loaded cermet can be expressed as a constant percent of the heavy metal atoms, more energy per unit volume of fuel can be extracted from low-loaded cermet in burnup limited cores.

Table 3-V.
Relative energy release from variable-loaded cermet fueled MCR cores.

<u>v/o UO₂</u>	<u>Relative total energy release</u>		
	<u>2% HMA</u>	<u>3% HMA</u>	<u>4% HMA</u>
80	1.00	1.50	2.00
70	1.10	1.66	2.21
60	1.22	1.82	2.43
50	1.32	1.98	2.65
40	1.43	2.15	2.87
30	1.54	2.30	3.07

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Further inspection of Table 3-V shows that for systems with the same cermet volume fraction, limited only by the allowable fuel burnup and in which the burnup limit is truly constant in percent of heavy metal atoms destroyed, the power density or core lifetime can be increased by as much as 50%. If the increased strength and lower operating temperatures of the lower-loaded cermets actually increase the allowable burnup to a higher percentage of HMA, even more significant power density or core lifetime increases are indicated.

The thermal conductivity is appreciably increased as the amount of matrix metal is increased in Cb-UO₂ cermets as shown in Figure 3-4.² This permits operation at a lower average fuel temperature. Keller *et al.*³ developed a creep failure model for correlating irradiation effects in dispersion fuels and showed that for low-loaded stainless steel cermets, failure was a sensitive function of the average fuel temperature.

Figure 3-4. Thermal conductivity versus fuel composition.

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Release of fission product gases from UO_2 and diffusion of these gases through metals are known to decrease at lower temperatures. Thus, the reduced operating temperature and the increased amount of matrix metal are expected to appreciably decrease the fission gas release rate in the lower-loaded cermet.

The obvious disadvantage of the use of lower ceramic loadings in cermet fuels is the reduced loading of fissionable material. This leads to difficulty in obtaining the required critical mass. The use of enriched Pu^{239} or U^{233} oxide in place of UO_2 significantly reduces the required critical loading. Typical critical loading requirements are given in Figure 3-5 for 93% enriched UO_2 (7% U^{238}) and 94% enriched PuO_2 (6% Pu^{240}) cermet as a function of core diameter. U^{233} data were not obtained, but U^{233} is expected to have smaller critical mass requirements than Pu.

Figure 3-5. The effect of core diameter on critical cermet loadings.

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Low-loaded cermet with high burnup have increased reactivity control requirements. An oxide burnup of 100,000 Mwd/tonne U represents destruction of approximately 10% of the fissile atoms. These large fuel burnup fractions require large excess reactivity in the initial loading and comparably large control drum worth capabilities.

Pu^{239} or U^{233} fuels cannot be considered without evaluation of the effects that use of these fuels have on the safety of the system. The smaller delayed neutron fractions for Pu^{239} and U^{233} shown in Table 3-VI cause a quantitative but not a qualitative difference in the dynamic response of systems using these fuels when compared with U^{235} -fueled systems.

Table 3-VI.
Fast fission delayed neutron fractions.⁴

<u>Nuclide</u>	<u>Delayed neutron fraction</u>
U^{233}	0.00268 ± 0.00016
U^{235}	0.00645 ± 0.00022
Pu^{239}	0.00204 ± 0.00011

Damaging nuclear accidents are not considered possible in the U^{235} -fueled MCR because there is no known source of uncontrollable reactivity insertion.* Scram certainty is provided for safe control of lower rate insertions or subprompt critical total insertion accidents. Prompt critical occurs at a smaller value of $\Delta k/k^{**}$ for Pu^{239} and U^{233} -fueled systems because of their smaller delayed neutron fractions. Careful design would be required to eliminate all sources of high rate reactivity insertions in excess of prompt critical for equivalent safety of U^{233} , U^{235} , and Pu^{239} -fueled reactors.

*Reactivity insertions in excess of prompt critical at insertion rates beyond the ability of the safety system to control.

**k is the multiplication factor of the system.

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Alternate Fuel Element Configurations

The 3-Mw_e MCR design⁵ when fueled with 80 v/o UO₂-20 v/o Cb cermet, contained 853 rod-type fuel elements, each 0.42 in. in diameter. This report presents the results of a study which examined the design and performance characteristics of cores loaded with two alternate fuel element configurations as well as performance of hollow fuel pins and fuel in plate form.

CONCLUSIONS AND RECOMMENDATIONS

The study indicates that there is no advantage in the use of hollow pins over solid pins fueled with an 80 v/o UO₂-20 v/o Cb cermet with respect to reductions in core diameter and/or overall core length. However, hollow pins might be attractive from a fabrication cost standpoint, because a significantly smaller number of hollow pins is needed as compared with the number of pins in a solid fuel pin design. It is recommended that a cost analysis be performed to evaluate this.

The conclusion relative to evaluation of fuel plates depends on the degree to which fission gases are contained within the fuel matrix. If fission gas release of the order of magnitude considered for the oxide fuel in the 3-Mw_e MCR preliminary design⁵ is assumed, it is concluded that there is no apparent advantage in the use of fuel plates over fuel pins. This is because an increased core diameter and/or overall length would be required. If, however, no fission gases were released, as might be possible with cermet fuel, fuel plates would provide a higher power than pins within the same core diameter. The magnitude of the experimental program necessary to ensure feasibility of fuel plates combined with the anticipated superior structural and burnup characteristics of fuel pins suggest that fuel plates be dropped from consideration for the initial MCR core loadings.

COMPARISON OF HOLLOW FUEL PINS WITH SOLID FUEL PINS

A comparison was made of the performance of hollow fuel pins (cooled internally as well as externally) with the 3-Mw_e MCR design solid fuel pin configuration. The comparison was made for three reactor powers—15, 30, and 60 Mw. Optimized core sizes were determined for both types of fuel pins based on the design criteria used for the 80 v/o UO₂-20 v/o Cb cermet fuel of the 3-Mw_e design. The fission gas void requirement was assumed to be containment of 15% of the volatile fission products generated in 5000 fph of operation.

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Alternate designs were investigated wherein the total length (fuel plus void space) of a hollow-pin core was made equal to the length of a solid-pin core, and a new core diameter for each of the hollow pin cases was then determined. In addition, total length requirements were determined when the hollow-pin core diameter was set equal to the solid-pin core diameter.

Analysis

Table 3-VII summarizes the results of the study. When the cladding is maintained constant at 0.015 in. (Case I), a small reduction in core diameter can be attained by utilizing hollow instead of solid fuel pins, and this improvement is more pronounced in the high power range. However, the fission gas void length and total length requirements for the hollow-pin cores are greater than for the solid-pin cores.

When the hollow-pin core is designed so that its total length is equal to that of a solid-pin core (Case II), the core diameter is greater for the hollow-pin cases in the 30- to 60-Mw range. At 15 Mw, the core diameter for a hollow-pin configuration is slightly smaller than for a solid-pin configuration. When the hollow-pin core has a diameter equal to that of a solid-pin core (Case III), its total core length is smaller than the solid-pin core at 15 Mw and greater in the 30- to 60-Mw range.

The number of pins in a hollow-pin core is significantly smaller than the comparable solid pin core in all cases.

Calculation Procedure

Core sizes optimized for minimum diameter were selected for both solid and hollow fuel pin configurations at 15, 30, and 60 Mw. The other operating parameters were the same as for the 3-Mw_e MCR design—300°F reactor coolant ΔT , 1500°F reactor coolant outlet temperature, and a core lifetime of 5000 fph. The spacing between pins was fixed at 0.030 in. The clad thickness for the hollow pins (both the outer and inner clad tubes) and solid pins was set at 0.015 in. The optimization procedure was based on the following limitations:

- Fuel enrichment $\leq 93\%$
- Coolant velocity ≤ 35 fps
- Nominal fuel temperature $\leq 2600^\circ\text{F}$
- Fuel burnup $\leq 35,000$ Mwd/tonne U

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Table 3-VII.

A comparison of the design characteristics of hollow versus solid fuel pins.

Maximum coolant velocity = 35 fps

Lifetime = 5000 fph

Reactor outlet = 1500°F

Reactor ΔT = 300°F

Fuel material: 80 v/o UO₂-20 v/o Cb

<u>Pin type</u>	<u>Reactor power (Mw)</u>	<u>Core diameter (in.)</u>	<u>Pin dia (in.)</u>	<u>Hole dia (D_H) (in.)</u>	<u>Number of pins</u>	<u>Clad thickness (in.)</u>	<u>Gas void length (in.)</u>	<u>Overall length (in.)</u>
Solid	15	14.9	0.42	-	853	0.015	2.3	17.2
Hollow (I)	15	14.4	0.79	0.24	238	0.015	4.6	19.0
Hollow (II)	15	14.8	0.81	0.24	238	0.024	2.8	17.2
Hollow (III)	15	14.9	0.87	0.24	238	0.027	2.4	16.8
Solid	30	18.7	0.29	-	3210	0.015	1.9	20.6
Hollow (I)	30	18.1	0.89	0.36	319	0.015	6.5	24.6
Hollow (II)	30	19.2	0.94	0.36	319	0.039	2.5	20.6
Hollow (III)	30	18.7	0.92	0.36	319	0.028	3.4	21.5
Solid	60	24.6	0.22	-	9640	0.015	1.7	26.3
Hollow (I)	60	23.5	1.07	0.54	372	0.015	10.4	33.9
Hollow (II)	60	25.5	1.15	0.54	372	0.055	2.8	26.3
Hollow (III)	60	24.6	1.11	0.54	372	0.034	4.6	28.1

Independent parameter

Hollow (I) - clad thick = 0.015 in.

Hollow (II) - total length = length of solid rods

Hollow (III) - core dia = dia of solid rods

Figure 3-6 is the unit hydraulic cell. When $\beta = 0$, the configuration is that of a solid pin. It was assumed that the coolant velocity in the hole is the same as that outside the pin. The flow in the hole and outside the pin was not matched to the heat transferred into these regions.

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Figure 3-6. Unit hydraulic cell schematic.

The analytical techniques used to determine the fission gas void length requirements for both the hollow pin and solid pin configurations were the same as those utilized in the 3-Mw_e MCR design,⁵ with the following additional simplified ground-rules.

- For the hollow pin, strength considerations were based on the hoop stress in the outer tube. The inner tube, which is subject to buckling loads as well as hoop stresses, was assumed to have the same wall thickness as required for the outer tube.
- A design temperature of 1715°F for the clad was used throughout the study, which permits an end-of-life allowable stress of 3675 psi.

To calculate the end-of-life internal gas pressure (P_g), the thin wall stress equation in the following form was used:

$$P_g = S_{\text{Allow}} \times \frac{2(t_c - t_{\text{corr}})}{D_p - t_{\text{corr}}} + P_{\text{Ext}} \quad (3-6)$$

where

- S_{Allow} = end of life allowable clad stress—3675 psi
- t_c = clad thickness—in.
- t_{corr} = corrosion allowance—0.0009 in.
- D_p = pin outer diameter—in.
- P_{Ext} = coolant pressure—60 psi

Applying the perfect gas law, the fission gas void length (L_v) is:

$$L_v = \frac{(FGR \times A) Q f_r f_{ph}}{\frac{\pi}{4} \left[(D_p - 2 t_c)^2 - D_H^2 \right] \left(\frac{36.05 P_g}{T_g} - 1 \right)} \quad (3-7)$$

where

- FGR = fission gas release fraction—0.15
- A = volume of fission gases generated—0.0668 $\frac{\text{in.}^3}{\text{Mw-hr}}$ at STP
- Q = reactor power, Mw
- f_r = maximum/average radial power factor—1.35
- f_{ph} = core lifetime—5000 hr
- D_H = outside diameter of inner clad tube—in.
- T_g = gas temperature—2120°R

To reduce the fission gas void length of the hollow fuel pin configurations (Case I in Table 3-VII), the thickness of the inner and outer clad tubes was increased as an independent parameter (Cases II and III). The number of fuel pins, the bare fuel dimensions, and the coolant flow area were held constant. Therefore, the fuel burnup, enrichment, coolant velocity, and temperature rise across the fuel were the same as in the optimized designs (Case I). Accordingly, as the clad thickness was increased, the reactor core diameter was increased to accommodate the additional clad material.

Discussion

The dashed lines in Figure 3-7 show how the length of the fuel element and core diameter for the hollow pin cases vary with the clad thickness of the inner and outer clad tubes. The horizontal solid lines represent lines of constant core diameter and total length, based on the results of the solid pin cases for a cladding thickness of 0.015 in. and reactor powers of 15, 30, and 60 Mw. The intersection of the solid and dashed core diameter lines represents the clad thickness which could be accommodated in a hollow-pin core of the same core diameter as a solid-pin core. From the dashed line curve of L_T , pin length can be found for that particular clad thickness. A similar procedure can be used to determine the clad thickness of a hollow-pin core which results in a pin length equal to that of a solid-pin core.

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Figure 3-7. Total length and core diameter versus clad thickness.

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The results of this analysis are reported in Table 3-VII. From the viewpoint of savings in core diameter and/or overall length, hollow fuel pins appear to be slightly more attractive than solid fuel pins in the low power range. However, in the 30- to 60-Mw range, solid fuel pins permit smaller core sizes than hollow pins.

A potential advantage of hollow pins over solid pins appears to be the reduced number of fuel pins required. It can be seen from Table 3-VII that at a power of 15 Mw, the solid fuel pin core requires 3.6 times as many pins as a hollow-pin core, while at 60 Mw the ratio is 26:1. The high power core has a significantly higher ratio of solid pins to hollow pins because the pin diameter in the solid pin cores is decreased from 0.42 in. to 0.22 in. as the power is increased from 15 to 60 Mw. This reduction in pin diameter, as the power is increased, is due to the assumption that the spacing between pins remains constant at 0.030 in.

From the results of the parametric studies, where the pin spacing was a dependent variable, it was found that the pin diameter of an optimized core did not vary appreciably with core size. If the 60-Mw case for solid pins is assumed to have the same pin diameter as the 15-Mw case (0.42 in.) it would require only 2645 pins, and the ratio of solid pins to hollow pins would then be 7:1 instead of 26:1. The same is true of the 30-Mw case where the ratio is 10:1 for a constant pin spacing of 0.030 in. and 4.8:1 if the pin diameter of this case were 0.42 in. (same as 15-Mw case).

COMPARISON OF PLATE-TYPE FUEL ELEMENTS WITH ROD-TYPE FUEL ELEMENTS

This subsection describes a comparison of the performance of fuel plates with the fuel pins of the 3-Mw_e MCR preliminary design. Of major interest was to determine if fuel plates would permit reductions in core size without violating the thermal, mechanical, and nuclear limitations imposed on the designs of the parametric studies.

In the pin configuration, the spacing between pins did not vary across the core. For the present study, the spacing between fuel plates was permitted to vary across the core so as to obtain the smallest possible total flow area without exceeding the maximum coolant velocity limitations.

The performance characteristics of cores using fuel plates were evaluated based on the ground rules set forth for the 80 v/o UO₂-20 v/o Cb cermet fuel of the 3-Mw_e preliminary design. Using selected cases from the Phase I parametric study, the performance of fuel plates was compared with the results for pins.

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Analysis

Table 3-VIII summarizes the results of this study and provides a comparison with optimized designs and performance characteristics obtained from the parametric studies. Case 1 is based on setting the allowable fuel temperature at 2500°F and the allowable burnup at 25,000 Mwd/tonne U. For a fuel plate having the same temperature and burnup limitations and operating at 16.6 Mw, the gas void space would have to be appreciably greater than for a fuel pin. Figures 3-8 and 3-9 show the model used to define the fuel plate in this study. For Case 2, 4000°F allowable fuel temperature and 40,000 Mwd/tonne U allowable burnup, the required gas void space, again, is appreciably greater for plates than for pins.

Table 3-VIII.
Performance characteristics of fuel plates and fuel pins.

Case	Allowable burnup (Mwd/tonne)	Allowable fuel temperature (°F)	Fuel element type	Fission	Reactor power (Mw)	Rod diameter or plate thickness (in.)	Clad thickness (in.)	Gas void space (in.)
				gas release (% of generated)				
1	25,000	2500	Pins	15	16.6	0.430	0.015	4
	25,000	2500	Plates	15	16.6	0.308	0.017	35
2	40,000	4000	Pins	15	19.4	0.400	0.030	4
	40,000	4000	Plates	15	19.4	0.427	0.035	19
3	40,000	4000	Pins	0	21.8	0.550	0.015	0
	40,000	4000	Plates	0	25.3	0.370	0.015	0

Independent fixed parameters

Core diameter = 16 in.

Lifetime = 5000 fph

Reactor outlet temperature = 1500°F

Reactor ΔT = 300°F

Maximum coolant velocity = 30 fps

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Figure 3-8. Core model for flat plate fuel elements.

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Figure 3-9. Cladding and fuel arrangement for a fuel plate.

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Case 3 has the same temperature and burnup limitation as Case 2 but is based on zero fission gas release. No gas void space is provided, and the clad thickness is set at 0.015 in. for both the rod and plate configurations. In this case, a higher power can be extracted from fuel plates than from pins, within a core diameter of 16 in.

Fuel Pin Analysis

The following were selected as independent fixed parameters for this study:

Core diameter = 16 in.
Lifetime = 5000 fph
Reactor outlet temperature = 1500°F
Reactor ΔT = 300°F
Maximum coolant velocity = 30 fps

From the parametric studies, the performance characteristics for fuel pins were obtained for the lower (Case 1) and upper (Case 2) limits of burnup and temperature. The limitations imposed on the cores in the parametric studies were as follows:

	<u>Case 1</u>	<u>Case 2</u>
Maximum burnup	$\leq 25,000$ Mwd/tonne	$\leq 40,000$ Mwd/tonne
Maximum fuel temperature	$\leq 2500^\circ\text{F}$	$\leq 4000^\circ\text{F}$
Fuel enrichment	$\leq 93\%$	$\leq 93\%$
Gas void space	≤ 4 in.	≤ 4 in.

For Cases 1 and 2, the fission gas release rate was 15% of the volatile fission products generated.

Case 3 was evaluated using the same ground rules as established in the parametric studies except that no fission gases were released. Accordingly, the cladding thickness was set at 0.015 in. and no void space was provided.

Fuel Plate Analysis

The model used in the analysis of the fuel plate cores is shown in Figure 3-3. The supporting core structure material, filler pieces, and regions of stagnant potassium account for a fraction of the cross-sectional area of the core. The balance of the core area contains the fuel elements

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and the coolant. The plates are schematically shown as being contained within square and rectangular cans. The cladding and fuel arrangement is shown in Figure 3-4. The space between plates forms the coolant passages. The spacing varies across the core to match the heat transferred from adjacent plates. In Figure 3-3, the plates in the central can are widely spaced (region of highest power), whereas the plates on the periphery of the core are spaced close together (region of lowest power). The fixed input parameters and reactor powers for each case were the same for both plates and pins, thereby establishing the coolant flow area and the total fuel plate cross-sectional area. For each permissible burnup, the quantity of fuel material was then determined. The thickness of the fuel was selected so that the fuel temperature was equal to the limiting value, and the corresponding fuel clad thickness was then determined. As shown in Figure 3-3, the fuel element clad was assumed to be composed of a series of finite plates, each plate having a width, b . The permissible internal gas pressures, p , were determined using the flat plate stress equation:

$$p = \frac{S_A}{0.75} \left(\frac{t_{cl}}{b} \right)^2 \quad (3-8)$$

where $S_A \equiv$ Allowable clad stress, psi.

The fission gas void volume and void length was then computed. The design limitations imposed on the plate elements are the same as for the pins, with the exception that the void length of the plates was not considered to be a limiting parameter.

In Case 3, the procedure was similar except that the clad thickness was treated as fixed input data and the reactor power was determined.

In all three cases, the volume fraction of supporting structural material and stagnant potassium was the same as that used for the pins in the parametric studies ($VF = 0.164$). The width of a fuel tab, b , was selected from mechanical considerations to be 0.25 in. for Cases 1 and 2, where a minimum width is desirable from a strength standpoint, and 1.0 in. for Case 3, where strength is not controlling.

DISCUSSION

The results shown in Table 3-VIII indicate that fuel plates are not as attractive as pins, using the ground rules established for the 3-Mw_e MCR design (Cases 1 and 2). The quantity of fission gases generated and released from the fuel must be stored in the fuel element under high internal pressures. However, because of the poor pressure-containing characteristics of plates,

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substantially more clad material would be needed in this type of element compared with the clad requirement for pins. Additional volume is available in the fuel plate cores—due to the reduced flow area required—but for Cases 1 and 2 this is not enough to accommodate the additional clad material needed. It is necessary to reduce the internal gas pressures in the plate cores, and this is achieved by increasing the volume provided for fission gas storage.

In pin elements, the fission gas storage space is at the top of the element. If in plate elements the storage space were also at the top, the height of this void space would be as reported in Table 3-VIII.

For Case 3, where no provisions are made for fission gas containment, the plate core can achieve a higher power than the pin core. This occurs because the flow area for a given power to maintain the coolant velocity below its limiting value is less for plates than for the pins. This is based on the design of varying the pitch between plates to match the heat load transferred from adjacent plates, and the average coolant velocity in the core approaches the maximum velocity. With pin elements, a variable spacing would be difficult to achieve because of their triangular arrangement within the hexagonal array of fuel cans. For the pins, the core is orificed to achieve an average velocity in the core equal to the maximum velocity divided by the radial maximum/average power ratio.

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Reactor Coolant Bulk and Nucleate Boiling

A study was performed to determine whether improvements in reactor power density or reactor package power-to-weight ratios would be made possible by allowing nucleate boiling (without net vapor generation) or bulk boiling in MCR systems. Nucleate boiling is attractive because it increases the coolant heat transfer coefficient, leading to lower clad and fuel temperatures. The potential advantage of bulk boiling is that more heat could be transferred to the coolant without increasing either the flow rate or reactor ΔT .

CONCLUSIONS AND RECOMMENDATIONS

Nucleate Boiling

Nucleate boiling may ultimately allow a reduction in system weight due to the lower system pressure, but a more thorough knowledge of safety considerations, flow stability, burnout heat flux, and condensate-type pumps is required before this advantage can be achieved. In view of the many unknowns, MCR systems should continue the conservative practice of setting system pressure high enough to prevent nucleate boiling.

Bulk Boiling

Boiling coolant removes less heat from the reactor than liquid per unit volume (at the core exit). This result leads to higher coolant velocities and larger pressure drops. These facts combined with higher coolant and fuel operating temperatures lead to the conclusion that power densities for a boiling potassium-cooled MCR reactor would be less than for liquid coolants. In addition, boiling potassium systems require larger flow channels and condensate-type pumps leading to increases in the primary system weight. Thus, no further consideration of bulk boiling in the MCR system is required.

ANALYSIS

Nucleate Boiling

Nucleate boiling with no net vapor generation gives only small reductions in fuel surface temperatures since the heat transfer coefficient is high for a liquid metal coolant containing no vapor. Acceptance of nucleate boiling allows a potential reduction in system pressure with the associated weight reduction of the pressurized components. (Nominal reductions of 1000 lb for

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a 10-psi reduction in system pressure were estimated for the 3-Mw_e MCR design plant.) However, any significant reduction in system pressure would require pumps operating with very low suction heads.* This would necessitate larger condensate-type pumps which would operate at low speeds to prevent cavitation. Also, flow stability problems are more severe because of the possibility of getting net vapor generation in the hot channel due to a power transient.^{6, 7} Finally, nucleate boiling requires operation nearer the burnout heat flux regime, and burnout conditions are not well defined for liquid metals.^{8, 9}

Bulk Boiling

A reactor performance study was made of the 3-Mw_e MCR design⁵ utilizing boiling potassium as the primary coolant. The same physical layout (flow areas, pipe sizes, etc) and power requirement (15 Mw) were considered. The analysis examined the consequences of lowering the system pressure so that the coolant would be saturated at its outlet temperature of 1500°F and boiling would occur when the flow rate was lower than the 3-Mw_e MCR design rate. The effect on pressure drop and pumping power is shown in Figure 3-10. The effect on core exit velocity is shown in Table 3-IX. At zero percent vapor quality, the conditions would be the same as in the 3-Mw_e MCR design in which coolant enters the core subcooled at 1200°F and exits as a liquid at 1500°F.

Table 3-IX.
Required core exit velocity as a function of quality for 15 Mw.

<u>Velocity</u> <u>(ft/sec)</u>	<u>Quality</u> <u>(%)</u>
35	0
1000	5
1300	15
1505	25
1680	50
1755	75
1790	100

*The pump suction pressure limit for the 3-Mw_e MCR design system⁵ is 46 psia.

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Figure 3-10. Pumping requirements for bulk boiling coolant concept using 3-Mw_e MCR design.

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When the coolant flow is less than that of the 3-Mw_e MCR design, the coolant temperature would reach 1500°F upstream of the core outlet. It would then start boiling and continue to boil throughout the duration of its residence in the channel. The quality of vapor at the exit depends on the coolant flow rate. For 100% quality at the core exit, the flow rate would be 0.0625 of the flow rate in the 3-Mw_e MCR design.

If the coolant were 100% liquid, a reduced flow rate would lower the pressure drop. However, as part of the liquid vaporizes, there is an appreciable reduction in coolant density which increases the pressure drop. As shown in Figure 3-10, the introduction of a small percent of vapor serves to markedly increase the core and system pressure drops. As the exit vapor quality increases, the ΔP 's and power requirements go through a maximum at about 10% quality and then tend to decrease until an exit quality of 100% is reached.

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Operation on the left side of the core ΔP curve would cause an unstable condition since a small increase in power would increase quality, leading to a large increase in pressure drop, an abrupt reduction in flow rate, and a shifting of the steady-state operation point to the right side of the curve.*

Considering pumping requirements, it would be attractive to operate at 100% vapor quality at the core exit. However, operation at higher than 50% quality in the hot channel may result in burnout. Therefore, steady-state operation should be limited to the region between about 20 and 50% exit vapor quality.

To operate the MCR with 50% exit vapor quality would mean that the system ΔP would be about 3.5 times as great as the preliminary design ΔP , but due to the reduced flow, the pumping requirements would be half as great. However, since the saturation pressure corresponding to 1500°F is only 25 psia, the pumps would be required to operate with very low suction heads.** This would necessitate a condensate-type pump which would operate at low speeds to prevent cavitation. Any savings in pumping power or system weight due to reduced system pressure would be offset by the need for bigger pumps required for lower speed operation.

In the preceding discussion, the effect of pressure variations along the coolant channels on the core pressure drop was neglected. If in the calculation of ΔP , the change in coolant pressure over the channel were considered, the ΔP would be somewhat lower. This occurs because of the higher densities associated with the higher pressures upstream of the core exit.

The effect of varying pressures along the channel was examined for the case of 50% vapor quality at the core exit. For this case, it was determined that the inlet pressure corresponding to 25 psia at the outlet is 100 psia. With the coolant entering at 1200°F and 100 psia, the coolant does not start to boil until the saturation temperature associated with the pressure in the channel is reached. For this 50% exit quality case, the flow rate is 0.125 of that for the preliminary design, and the ΔP in the subcooled region of the core can be considered to be negligible. The coolant is heated up to the saturation temperature (assuming no superheating) associated with 100 psia, which is 1850°F as shown in Figure 3-11. From this point on, the coolant continues to boil. As the coolant flows up the channel, its pressure is being reduced due to friction and expansion losses. As the pressure is lowered, the corresponding saturation temperature is lower, with the result that the coolant does not boil at a constant temperature

*References 6 and 7 provide some general background information on flow stability.

**The suction pressure limit for the 3-Mw_e MCR design pump is set at 46 psia.

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but "flashes" continuously as it flows up the channel. Furthermore, the temperature of the coolant at any point in the channel will correspond to the saturation condition. By the time the coolant has reached the channel outlet, its temperature will be at 1500°F, corresponding to a pressure of 25 psia. The coolant temperature along the length of the channel is shown schematically in Figure 3-12. The maximum coolant temperature in the channel is 1850°F compared with 1600°F in the 3-Mw_e MCR design. It should be noted that superheating will increase clad surface temperatures even further.

Figure 3-11.
Vapor pressure of potassium.

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Figure 3-12. Schematic temperature distribution for boiling reactor concept with 1500°F outlet and 50% exit vapor quality.

With the exit pressure at 25 psia for a 1500°F coolant outlet, the ΔP between the channel outlet and the suction side of the pump is limited to 10 psi and probably as little as 5 psi to achieve a pump that does not cavitate at these pressures and which is still of reasonable size.

For the case of 50% vapor quality, limiting the ΔP to 10 psi between the core outlet and the pump inlet would require reducing the flow resistance in this region by a factor of 10 compared with the 3-Mw_e MCR design plant. Accomplishing this requires increasing the flow areas in the reactor outlet plenum, outlet nozzle, piping, and heat exchanger by at least a factor of 2 to 3 times those in the 3-Mw_e MCR design.

The disadvantages of using bulk boiling compared with the all-liquid potassium coolant concept characterized by the FY 1964 3-Mw_e MCR design⁵ are summarized as follows.

- The pressure drop across the core would be much higher, thereby requiring thicker structural components whose design is influenced by the core ΔP .
- While the pumping power requirements could be reduced, there would most likely be an increase in pump size and weight due to the lower suction pressure associated with a boiling system. The saturation pressure corresponding to a reactor outlet temperature of 1500°F is only 25 psia.

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- The maximum coolant temperature would be about 200°F higher than the hot spot coolant temperature of 1660°F for an all-liquid coolant. This occurs because the coolant would not start to boil until it at least reached a saturation temperature associated with the pressure at any location along the length of the channel.
- The core end fittings, reactor outlet plenum and nozzle, external piping, and heat exchanger would have to be increased in size to allow for more flow area and to minimize the ΔP between the core outlet and the suction side of the pump; this serves to keep the pump suction pressure as high as possible.
- Excessively high coolant velocities are required at the core exit, leading to extreme erosion and corrosion problems in addition to the aforementioned large pressure drops.

Other reasons that make a boiling coolant concept unattractive are flow instability problems, higher temperature and/or lower pressures due to superheat required before boiling starts, and operation closer to the burnout region.

Any attempt to increase the reactor power density without increasing the reactor outlet temperature leads to aggravation of the pressure drop, hot spot temperatures, and exit velocity problems.

At operating outlet temperatures approaching 1700°F, the boiling coolant concept becomes more attractive. This is because the coolant density is higher and, therefore, the pressure loss is less than for the 1500°F case. A case of 50% exit quality and 1700°F outlet temperature was studied. The corresponding outlet pressure for 1700°F is 60 psi and the coolant density is double that of the 1500°F case. The core ΔP is 40 psi and the pumping power requirements are considerably less than for the 3-Mw_e design and half as much as for the 50% quality 1500°F case. Due to the flashing phenomenon previously described, the coolant would reach a temperature of about 1875°F somewhere upstream of the outlet and then flash down to 1700°F as the pressure is reduced due to frictional expansion losses. The flow areas between the core outlet and suction side of the pump would not have to be changed appreciably to operate the pumps at a suction pressure of greater than 20 psi. However, hot leg materials which will meet MCR performance requirements with a 1700°F outlet temperature in an oxidizing atmosphere are not available.

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Reflector Thickness Variation

The Phase I parametric study¹⁰ of the 3-Mw_e MCR basic reactor concept used the same general arrangement of core and reflector regions as the FY 1964 preliminary design.⁵ The reflector thickness-to-core diameter ratio of 1:3 of the 3-Mw_e design was maintained throughout the study. The present study was undertaken to determine whether more favorable systems result from a different choice of reflector thickness. Investigated were the influence of varying the reflector thickness on key reactor characteristics, such as control drum worth, reactor power, reactor cost, and reactor plus fixed shield weight.

CONCLUSIONS AND RECOMMENDATIONS

The procedure used for determining reflector thickness in the Phase I parametric studies is shown to have been satisfactory over the entire range of core sizes studies. Specifically:

- The effect of varying reflector thickness on reactor core performance, as measured by fuel enrichment requirements and power capability for a given core size, is very small.
- The effect of varying reflector thickness on control drum worth is appreciable, particularly for smaller core diameters. However, satisfactory control worths were achieved with the procedure reflector thicknesses.
- Reactor plus fixed shield weights attained with the Phase I reflector thicknesses are within a few hundred pounds of the minimum weights achievable by varying the reflector thickness, for cores up to 20 in. in diameter. For larger cores, procedure reflectors incur larger weight penalties, but these penalties are necessary to maintain the control worth.

In view of the results of this study, further consideration of reflector thickness variation is not warranted until a concept is selected for preliminary design. It is recommended that the control drum worth requirements for the selected concept be established by a detailed study at that time and the proper reflector thickness to yield optimum reactor performance and system weight be determined.

ANALYSIS

To provide a comparison with the results of the Phase I parametric studies, core diameters of 14, 16, 20, and 24 in. were selected for investigation in the present study. Irrespective of their size, all of the Phase I parametric reactors utilized ten control drums, as provided in the FY 1964 Preliminary Design.¹⁰ In the present study, however, the number of control drums was varied as an independent parameter to include 9, 10, 11, and 12 drums. For the ten-drum

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configuration of the Phase I study, the spacing between drums was a function of the core diameter, and these values were preserved in the present study. For a given core diameter, the pairing of the number of drums and the spacing between them uniquely determines the drum diameter and the reflector thickness. One additional configuration representing an exception from this procedure was studied—a 24-in. core surrounded by an 8-in. reflector with eleven instead of ten control drums.

In addition to the preceding geometrical assumptions, the following ground rules previously used in the Phase I basic concept analysis were observed:

- Fuel material: 80 v/o UO_2 -20 v/o Cb cermet
- Standard reflector composition
 - Fixed reflector thickness ratio, W/SS: 7/3
 - Control drum thickness ratio, $\text{BeO}/\text{W}/\text{B}_4\text{C}$: 3/4/3
- Standard core composition—volume fraction
 - Fuel: 0.60
 - Coolant: 0.25
 - Structure: 0.15
- Initial excess (hot) reactivity: 1.03
- Operating dose rate: 10 mrem/hr at 450 ft from center of core
- After-shutdown dose rate (removable shield separated): 500 mrem/hr at 10 ft from center of core, 12 hr after shutdown following 5000 fph of operation

Fuel Enrichment

Reactivity calculations to determine fuel enrichment requirements were performed with UNC Code A-4 (DTF) in spherical geometry. The results are plotted as a function of reflector thickness in Figure 3-13. The circled points are the data from the Phase I parametric studies in which the reflector thickness was made equal to one-third of the core diameter. The departure from this prescription has only a small influence on fuel enrichment requirements.

Control Drum Worth

Reactivity calculations to determine control Δk were performed with UNC Code A-4 in infinite cylinder geometry. Standard techniques were applied to account for axial leakage by the introduction of a fictitious absorber in the core. Control drum worth is plotted as a function of reflector thickness in Figure 3-14. In general, control worth increases significantly with increasing reflector thickness, particularly for the smaller core diameters. However, adequate control

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Δk is achievable with the procedure reflector thickness. The control Δk of the 14-in. dia core nearly doubles as the reflector thickness is increased from 3.2 to 5.3 in.; for the 24-in. core, control Δk increases only 40% as its reflector thickness is increased from 5.7 to 9.2 in. The filled-in circle represents the 24-in. core with eleven instead of ten drums; the control worth increased about 6%, somewhat less than the 10% physical increase in the number of drums.

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Figure 3-13. Fuel enrichment versus reflector thickness.

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Figure 3-14. The influence of reflector thickness and number of control drums on control drum worth.

Radial Maximum/Average Power Ratio

Local power generation rates were determined from the output of the infinite cylinder model reactivity calculations. The radial maximum/average power ratio was calculated and is plotted in Figure 3-15 as a function of reflector thickness. The radial power ratio is not strongly influenced by the reflector, varying about 5% for the 14-in. core and 3% for the 24-in. core.

Reactor Power and Vessel Size

One of the dependent parameters that served as a criterion for optimizing reactor power in the basic concept analysis was maximum allowable fuel burnup. Since maximum fuel burnup is directly proportional to the product of the radial max/avg power ratio and reactor power, an increase in the power ratio will cause a decrease in the reactor power while maintaining the fixed value of the allowable burnup. The following family of parameters was selected to determine the influence of reflector thickness (max/avg power ratio) on reactor power:

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- Reactor outlet temperature = 1500°F
- Reactor ΔT = 300°F
- Coolant velocity = 30 ft/sec
- Core lifetime = 5000 fph
- Fuel limitation = 25,000 Mwd/tonne and 2500°F

The results are presented in Figure 3-16 as pressure vessel ID versus reactor power in the same format that was used to display similar data (the circled points) for the Phase I parametric study. For a given core diameter, the influence of reflector thickness on reactor power is small since the power is affected by the change in radial max/avg power ratio which amounted to only a few percent. Pressure vessel diameter is directly related to changes in reflector thickness.

Figure 3-15. Radial peak/average power versus reflector thickness.

Figure 3-16. The influence of reflector thickness on pressure vessel ID and reactor power.

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Reactor Weight

Reactor weight calculations were performed for the range of parameters displayed in Figure 3-16; the results are presented as a function of reflector thickness in Figure 3-17. Control drum worth has also been plotted in Figure 3-17 to permit a convenient assessment of the trade-off between reactor weight and control Δk . For a 20-in. dia core, for example, as reflector thickness is increased from 5 to 7.3 in., control Δk increases from 0.07 to 0.10, but this is accompanied by a 5400-lb increase in reactor weight. The circled points represent the ten-drum configurations that have been similarly identified in Figures 3-1, 3-2, 3-3, and 3-4; the line usually drawn through these data was omitted here to avoid obscuring the other parameters.

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Figure 3-17. The influence of reflector thickness on reactor weight and control Δk .

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Reactor weight as a function of power is shown in Figure 3-18 for lines of constant reflector thickness. These data are presented in terms of the same coordinates as were used for the Phase I parametric study.

Reactor Cost

Reactor cost factor (including core costs) is plotted versus reactor power in Figure 3-19 for the parametric cases displayed in Figures 3-4 through 3-18.

Fixed Shield Weight

To determine the overall effect on the weight of Skid II (reactor, shutdown shield, and liquid metal system), fixed shields were determined for the range of core diameters and reflector thicknesses considered for the reactor performance calculations. The fixed shields were sized for an after-shutdown dose rate of 500 mrem/hr at 10 ft, 12 hr after shutdown following 5000 fph continuous operation, and an operating dose rate of 10 mrem/hr at 450 ft when enclosed with the removable shield.

Figure 3-18. Reactor weight versus reactor power.

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Figure 3-19. Reactor cost factor versus reactor power.

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The method used to perform these calculations utilized the results of the Phase I studies performed for a range of fixed shield lead thicknesses for each of several combinations of reflector thickness and core diameter. The effect of reflector thickness on each of the various after shutdown gamma ray sources and consequent dose rate from these sources was determined from a semianalytical expression developed to account for the effect of core diameter change. Constants for this expression were evaluated separately for each different reflector thickness and for each radial lead shield thickness using the Phase I results. The range of reflector thicknesses calculated in Phase I proved to be adequate for this study.

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The resulting fixed shield weights are presented in Figure 3-20 as a function of reflector thickness with core diameter as a parameter. The fixed shield weight includes the weight of the following components:

- A LiH-Pb-LiH top shield plug, containing the control drum drives
- A TiH_2 bottom shield plug
- A Pb radial shield
- A W-Pb-LiH pipe penetration shield segment
- Miscellaneous shield patches affixed to the shield structure

Figure 3-20. Total fixed shield weight as a function of reflector thickness.

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The radial Pb portion of the fixed shield weight decreases with increasing reflector thickness while the weight of the remaining portions, principally the top and bottom plugs, increases due to the increasing pressure vessel diameter. For small core diameters, the weight of the radial Pb fixed shield represents a large portion of the total fixed shield weight. The decrease in radial Pb fixed shield weight with increasing reflector size is the controlling change and the total fixed shield weight decreases. For larger core sizes, the weight of the radial lead shield becomes a smaller part of the total fixed shield weight. The decrease in radial fixed shield weight with increasing reflector thickness becomes less significant and the total fixed shield weight decreases more slowly than for the smaller core diameters. For the 24-in. core, the decrease in radial fixed shield weight is balanced by the increase in weight of the other portions of the fixed shield for a reflector thickness of about 8.0 in. For larger reflector thicknesses, the weight increases are controlling and the fixed shield weight increases.

Reactor Plus Fixed Shield Weight

The weight of the reactor and fixed shield as presented in Figures 3-17 and 3-20 are combined to give the reactor plus fixed shield weight curves presented in Figure 3-21. These combined weights are presented as a function of reflector thickness for the various core diameters considered. Control drum worth was also included to permit the convenient assessment of the trade-off between reactor plus fixed shield weight and control Δk .

The reactor plus fixed shield weight curves pass through a minimum since the reactor weight (i. e., weight of pressure vessel plus all components internal to the pressure vessel) increases with increasing reflector thickness while the fixed shield weight in general decreases. The circled points represent data from the Phase I parametric studies in which the reflector thickness was prescribed to be equal to one-third of the core diameter. The reactor plus fixed shield weights for the Phase I prescription reflector thicknesses are within a few hundred pounds of the minimum weight for core diameters of 14, 16, and 20 in. For a core diameter of 24 in., the prescription reflector thickness gives a reactor-plus-fixed-shield weight which exceeds the minimum by about 3700 lb. However, for this sized core this weight penalty must be accepted to maintain a reactivity control worth of 0.09 $\Delta k/k$.

It is interesting to note that the minimum reactor plus fixed shield weight appears to be achieved for all core diameters at a reflector thickness between 5.5 and 6 in.

The point indicated by a square in Figure 3-21 represents the weight for the 3-Mw_e MCR design which has a 15-in. dia core and a 5.0-in. reflector. The weight for this configuration, as determined from Figure 3-21, is 17,600 lb which compares favorably with the weight of 18,100 lb reported for the 3-Mw_e MCR design.

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Figure 3-21. Reactor plus fixed shield weight versus reflector thickness.

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Alternate Reactor Control Concepts

The 3-Mw_e MCR design reactor is controlled by ten independently and/or collectively rotating control drums located in the reflector.⁵ Four types of drive—nutating electric motor, twistable boot seal, hermetic drive housing, and harmonic drive seal—were considered for actuating rotation of the control drums of the 3-Mw_e-MCR design.⁵ Except for the nutating motor concept, these drives were tested in liquid metal by other organizations although the testing conditions were not the same for all drives. The motors for these drives are all located in the top shield plug above the reactor vessel. While a specific drive was not selected, the nutating motor drive was used to illustrate a typical drive installation.

During FY 1965, alternate reactor control methods were studied, along with alternate methods of actuating the reactor control elements. These studies were conducted in an effort to overcome the liquid metal vapor sealing problems, to overcome problems associated with the location of drives in the shield, and to prepare for controlling the larger reactors which may be needed to accommodate the higher power output levels being considered for the MCR. The alternate methods considered for controlling the reactor include rotary and axial movement of control elements, both within and external to the pressure vessel.

Gas and mechanical actuators were considered as alternate actuating devices for providing rotary or axial movement. In addition, flexible shafts and bevel gear drives were considered as a means of permitting an offset between the control drum axis and the control drum drive axis.

The studies reported herein are incomplete, but sufficient information was accumulated to permit recommendations concerning future design efforts.

CONCLUSIONS AND RECOMMENDATIONS

On the basis of incomplete studies, none of the control methods studied exhibited a significant superiority over the 3-Mw_e MCR design arrangement.

A rotary gas actuator shows potential for space saving in the top shield plug.

A translatory actuator is satisfactory, but it is undesirable because translatory motion requires additional voids in the top and bottom shield plugs. These voids cannot be patched conveniently in the axial direction because of the package height limitation of the power generation package.

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Preliminary studies indicate a possibility that either flexible shafts or bevel gear drives could permit locating control drum (or fuel element) actuators in a position that is remote from the reactor vessel. However, both would require adding shaft housings to the top of the reactor vessel, thereby complicating fabrication of the vessel.

Recommendations as a result of this work are:

- Concentrate future effort to the rotational movement of control drums.
- Eliminate the axial movement of control elements from future consideration for the MCR.
- Continue studies of the rotary gas valve to fully evaluate its potential for application to the MCR control system. The dynamics of such a system should be developed at least to a point where "Gas Pressures vs Radial Movement of the Drum" curves can be developed.
- Test the four control drum drives considered for the 3-Mw_e MCR design—nutating motor, twistable boot seal, hermetic drive housing, and harmonic drive seal—in a liquid metal and/or liquid metal vapor environment representative of MCR conditions.
- Continue studies of using flexible shaft couplings and angle drive gears for remote location of control actuators.

ANALYSIS

Alternate methods for controlling the reactor were investigated, along with alternate methods for driving the controls. However, the studies are incomplete for both areas.

Reactor Control Methods

The work reported herein represents the first stage of a supporting study that was to have evaluated potential alternates to the control drums used in the 3-Mw_e MCR design. A summary of the advantages and disadvantages of each alternate is presented in Table 3-X, and sketches of the various arrangements studied are shown in Figure 3-22. These include the rotary or translatable movement of control elements, located either inside or outside the reactor vessel.

It can be seen from Table 3-X that none of these methods offers significant advantages over the 3-Mw_e MCR design and most of them exhibit significant disadvantages.

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Table 3-X.
Summary of reactor control method variations.
 (as compared to 3-Mw_e design)

<u>Method</u>	<u>Advantages</u>	<u>Disadvantages</u>
1. Control elements located outside vessel	1. Simpler to remove gas generated in poison 2. Coolant sealing problem not as critical as for control elements in vessel 3. Easier access to control elements and drives 4. Control elements and drives need not be decoupled for refueling	1. More severe radiation environment for pressure vessel 2. Secondary containment and coolant system required for drums and reflector 3. Unrecoverable loss of heat produced in reflector 4. Increased weight (due to additional containment vessel and secondary coolant system components)
<u>All of the above advantages and disadvantages apply to cases 1a through 1d.</u>		
a. Rotary drum containing poison and reflector	1. Low temperature environment for drums and reflector	
b. Rotary drum containing void and reflector	1. No cost for poisons	1. Thicker operating shield required because of increased leakage 2. Decreased worth of control drums
c. Axial motion of control elements (with followers) containing poison	1. Simpler drive arrangement 2. Gravity assist for scram	1. Increased overall height of reactor 2. Perturbed core power pattern
d. Movement of reflector pieces to open leakage path	1. No cost for poison	1. Inefficient use of space. Location of shields at greater radius leads to increased weight
2. Control elements located inside vessel		
a. Axial motion of poison rods or blades with followers in core	1. Greater control worth (but not as much as in 2c and 2d) 2. Possible decrease in reflector thickness	1. Increased core diameter 2. Increased overall height of reactor 3. Perturbed core power pattern 4. Increased core mechanical complexity
b. Axial motion of poison rods or blades with followers in reflector	1. Greater control worth (but not as much as in 2a)	1. Increased overall height of reactor 2. Perturbed core power pattern
c. Rotary movement of fuel	1. Greater control worth	1. Coolant flow distribution and heat removal are problems 2. More shutdown shielding required 3. Increased core mechanical complexity
d. Axial movement of fuel	1. Greater control worth 2. Possible decrease in reflector thickness	1. Increased overall height of reactor 2. Perturbed core power pattern 3. Coolant flow distribution a problem 4. Increased core mechanical complexity

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Reactor Control Actuators

Gas and mechanical actuators (Figure 3-23) were briefly studied as a means for providing rotary and axial movement to control elements located within the core or within reflector regions. Dynamics of a control system using these actuators were not completely defined, nor were all necessary mechanical features determined, but the potential for applying such devices to the MCR was established.

The gas actuator requires a support system consisting of a high pressure reservoir, a low pressure reservoir, a gas compressor, valves, instrumentation, and piping, as shown in Figure 3-24. All of this equipment can be located outside the shield and does not affect the attenuation integrity of the shield.

There is a possibility that the gas actuator can be partially or completely supported by the cover gas system of the primary loop, particularly if that cover gas system is used which utilizes a gas compressor, a high pressure tank, and a low pressure tank. The operating characteristics of the control actuators would differ from the operating characteristics of the cover gas system in that the control actuators would operate more frequently and require a faster response than components supported by the cover gas system.

A rotary gas valve was considered as an actuating device for the control drum used in the 3-Mw_e MCR design. It is essentially a vane with differential pressures applied to each side of the vane to effect control of the control drum rotation. It could be designed to permit a significant reduction in the allotment of space needed in the top shield plug for the control drum drive. The rotary gas valve actuator for the control drum is the only actuating device among those studied which potentially offers a significant advantage over the nutating stepping motor used in the reference design.

A gas actuator system was also considered as a means to furnish axial movement to a control element. A double piston would be located above the reactor and connected to the control element with an axial shaft. A higher gas pressure is applied to the bottom of the piston for raising the control element, whereas the higher pressure would be applied to the top of the piston for lowering the control element. A 10.00-in. travel would require a piston housing that is about 14.00 in. long and about 3.00 in. in diameter. This system, while being the most desirable of the methods studied for providing translatory movement, is less desirable than the rotary gas valve system because it requires extensions to the top and bottom of the reactor vessel and excessive voids in both the top and bottom shield plugs. The forces involved for

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Figure 3-22. Schematic diagrams of reactor control methods.

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Figure 3-23. Reactor control methods – gas and mechanically actuated.

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Figure 3-24.
Gas pressurization system.

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axial movement of a control element located inside the pressure vessel, displacing liquid metal, were not determined, but these forces would have to be considered when the dynamics of the control system are defined.

The two types of mechanical actuators (Figure 3-23) considered for the axial movement of control elements were:

- Rack gear with a pinion gear drive
- Ball bearing screw drive

Both use an axial movement shaft which requires either that a canned motor be used or that an axial shaft seal be developed that is capable of containing liquid metal and liquid metal vapor within the reactor vessel. A double rubbing type of seal was considered for the shaft seal, with gas pressurization applied between the two sealing portions. The gas would be used to pressurize a chamber between the two contact seals, retaining liquid metal vapor within the reactor vessel and gas or lubricating oil within the gearbox. The mechanical drive would also require bearing housing(s) above the reactor vessel which would have an adverse effect on radiation and add complexity to the reactor vessel design and fabrication.

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Linkage Between Control and Control Actuator

The problem of sealing liquid metal and liquid metal vapor would be reduced and the higher radiation resulting from voids in the top shield plug, immediately above the reactor, could be avoided if actuators were located outside the shield or near the top of the package. The added need, in a horizontal reactor application, of avoiding direct contact of the sealing membrane of the 3-Mw_e design control drum drive with liquid metal at the reactor outlet temperature prompted the consideration of bevel gears which could operate in liquid metal and/or the use of flexible shafts. Both flexible shafts and nonflexible bevel gear drive shafts would require adding shaft housings to the top of the reactor vessel (rear of the horizontal reactor vessel). In addition, unless canned motors are used, seals would need to be developed for use between the shafts and the shaft housings.

A flexible shaft could transmit either rotational or translational motion from the actuator to the control device (drum or fuel element), thereby permitting the actuator to be located remote from the reactor vessel. Its deflection (torsional twist and/or elongation) as well as clearance between the shaft(s) and shaft housing(s) would require consideration when the operational dynamics of the control system are determined. In addition, some compensation may be needed because of creepage in the shafts.

The bevel gear drive, driven through a nonflexible shaft, would permit a closer control of the variation between actuator movement and control drum movement than would be permitted by a flexible shaft coupling. A gear drive, along with related bearings, would be a development item for a liquid metal application. However, the gears would be operating at low speed and with low loads. With cooling they could be operating at 300 to 500°F below the reactor inlet temperature. The principal problem is expected to be the retention of sufficient hardness for the gear teeth to avoid excess wear and deformation. The use of Coniflex bevel gears with a flame-sprayed tungsten coating has been suggested to provide a sufficiently hard surface.

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IV. PRIMARY LOOP

The following primary loop studies are reported in this section:

- Horizontal Reactor
- Alternate Forced Convection Afterheat Removal System
- Pressurization System
- Off-Skid Location of Power Generation Package Components
- Alternate Pump—Electromagnetic
- Alternate Heat Exchanger Concepts

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Horizontal Reactor

A vertically oriented reactor vessel was used in the 3-Mw_e MCR system.⁵ This concept is used as the reference design for the system discussed herein. The vertical reactor is preferred because it permits vertical loading of all reactor components. However, since package height limitations might become restrictive at high power levels, an investigation was made of several reactor design concepts, using a horizontally oriented pressure vessel, to estimate the influence of such designs on the reactor size, performance, and mechanical complexity and their effect on plant safety.

CONCLUSIONS AND RECOMMENDATIONS

The following conclusions were reached as a result of studying horizontally oriented reactor vessels.

- The horizontal reactor may permit the use of a one-piece removable shield.
- A lower package silhouette can be obtained for the same plant power rating.
- A simple parallel flow of coolant through the core and reflector can readily be achieved with a horizontal reactor, reducing the pumping power requirement.
- The overall power generation package is longer and heavier.
- The principle of natural convection for removing decay heat would be more complex to apply.
- The design of reactor internals is more complex.
- The external support for the reactor would be more complicated because it must be supported in at least two places and because it requires more complex shield penetrations.
- Control drum drives would probably be easier to maintain, but modifications to the drive mechanisms would be needed.
- Three methods of refueling the reactor were considered but each was found to present difficult design and operational problems.
- The removable shield package would be longer and heavier.
- In general, the disadvantages associated with a horizontal reactor far outweigh the advantages of the one-piece removable shield and the lower package silhouette.

It is recommended that:

- The horizontal reactor should be considered further only for those applications in which an upright reactor cannot be used.

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- Features developed in these studies of the horizontal reactor concept that have a potential for general application should be studied further. Among these are the flame-sprayed tungsten Coniflex bevel gears and the flexible shaft couplings for moving control drums or fuel elements.

ANALYSIS

A power generation package was studied with the reactor in a horizontal position—with the center line parallel with the skid floor. The position offers the possibility of a one-piece removable shield and a lower package silhouette at the shield. However, a number of serious disadvantages make such an arrangement generally impractical.

Package Arrangement

A package arrangement sketch, Figure 4-1, was made to determine the nature and magnitude of changes that are needed to adapt the 3-Mw_e MCR power generation package and removable shield package to accommodate a horizontal reactor. The reactor shown in Figure 4-1 is the 3-Mw_e MCR concept, modified to have the control drum drives extend from the liquid metal plenums on the opposite end of the reactor. The liquid metal nozzles are located so that the core will remain covered with liquid metal should a leak develop in the liquid metal piping external to the reactor vessel. The shield package is longer because essentially the same shield thickness must be provided around the horizontal reactor in the longitudinal direction that was provided around the upright reactor in the radial direction.

Four alternate piping and nozzle arrangements, shown in Figure 4-2, were considered for controlling the distribution of liquid metal. The pertinent data for these four concepts are compared in Table 4-I with the 3-Mw_e MCR reactor concept and to the modified reactor concept that is shown in Figure 4-1.

Supporting a horizontal reactor vessel could be more complicated than supporting an upright reactor because the horizontal vessel must be supported in at least two locations, as shown in Figure 4-3, and thermal expansion must be accommodated. The support structure may also complicate the shield design and fabrication. However, the relatively low cg of the reactor and the possibility of obtaining more optimum support spacing for a horizontal reactor may permit the design of a three-point support structure which may be an improvement over the upright reactor support.

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Figure 4-1. Cross skid heat exchanger with horizontal reactor - side elevation.

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NOTE: Arrows indicate direction of flow

Figure 4-2. Horizontal pressure vessel concepts.

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Refueling

The assembly and disassembly of the reactor internal subassemblies would be difficult while the reactor is in a horizontal position. The precision with which these subassemblies need to be positioned for assembly would require large, complex alignment fixtures.

As an alternate, the reactor could be removed from the package; however, this would require the cutting and rewelding of liquid metal lines. (Note: This approach to refueling is not necessarily undesirable.) Another approach would be to turn the entire package on end for refueling. This would require that either the forward portion of the skid be structurally capable of supporting the reactor in an eccentric position, the reactor be designed and fabricated to support the skid and other components during refueling, or a turning fixture be used for supporting the reactor and the skid (with other components) separately. In addition, stipulation that the package must be capable of being turned on end for refueling would place new requirements on the support structure for other package components.

A principal consideration in refueling the reactor is whether or not the liquid metal level can be maintained above the core during refueling. If the reactor is to be refueled in a horizontal position, the vessel head could not be removed until the after-shutdown heat generation had decayed to a point where all liquid metal could be drained from the reactor.

The maximum heating rate in a bared fuel pin in the central fuel element is conservatively estimated to be 310°F/hr at 200 days after shutdown and 202°F/day at 300 days after shutdown, following 5000 fph operation at 15 Mw_{th}.

If the reactor system temperature is 200°F during the drain operation, the times available for transferring a bared fuel element to a cooling cask, before exceeding the clad design point temperature of 1715°F, are:

- 5 hr at 200 days after shutdown
- 7.5 hr at 300 days after shutdown

The number and probable duration of operations required to remove the reference design core rule against this approach:

- Drain reactor coolant from the systems
- Cut vessel seal weld
- Remove vessel head
- Remove inner core subassembly and place in cooling cask
- Remove outer core subassembly and place in cooling cask

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Table 4-I.
Comparison of horizontal vessel concepts and 3-Mw_e MCR design.

Item	3-Mw _e MCR vertical vessel concept	Horizontal vessel concepts				
		Figure 4-1	Figure 4-2			
			I	II	III	IV
Maximum dimensions* of reactor plus shielding	101.375 in. long × 102.75 in. wide × 92.0 in. high	115.0 in. long × 102.75 in. wide × 80.0 in. high				
Internal flow pattern (through core and reflector)	Series		Parallel			Series
Pressure drop through vessel (psi)	52.8	52.8	45.0	42.0	44.0	49.0
Flow rate (lb/sec)	250		280			250
Control drum and drive	Uses vertical straight-line drum-drive arrangement with gas space provided at top for location of mechanism components.	<ol style="list-style-type: none">1. Use of horizontal straight-line drum-drive arrangement requires either develop- ment of an absolute liquid metal-to-gas dynamic seal, or gears or other mecha- nism which can operate submerged in liquid metal, or flexible shafts to connect the drum to an actuator located near the package roof. The flexible shafts would require adding shaft housings to the reactor vessel.2. Use of a right angle drive (bevel gears) requires the development of gears for operating in hot liquid metal. Shaft housings would need to be added to the reactor vessel.3. Drum bearings must be developed for supporting the drum weight and transporting shock loads.4. Using axial movement of fuel element(s) or control drum(s) requires the use of push-pull-type flexible shafts and the addition of shaft housings to the reactor vessel.				
Refueling	One seal weld (vessel)			One seal weld cut (vessel, inlet piping)	One seal weld cut (vessel)	
	Refuel without draining core—no tilting equipment needed.	<ol style="list-style-type: none">1. Refuel with core dry (must wait about two months to do this safely), or2. Refuel without draining core—package must be tilted 90 degrees.				
Internal seals (to prevent core bypass flow)	Ring seal installed during assembly. Vessel height would need to be increased 6.0 in. to eliminate this seal.	Seals are not required. Core bypass flow is prevented by welded members.				
Afterheat removal	<ol style="list-style-type: none">1. Natural convection readily attainable.2. Forced convection readily attainable since leak in piping will not drain core.	<ol style="list-style-type: none">1. Natural convection attainable but flow pattern is less pre- dictable than with upright reactor.2. Forced convection readily attainable.	<ol style="list-style-type: none">1. Natural convection is attainable but flow pattern is less predictable than with upright reactor.2. Forced convection not feasible if leak develops in liquid metal piping external to reactor vessel.			Same as for reactor of Figure 4-1.
Difference in elevation between midplanes of core and afterheat heat exchangers	18.0 in.	24.0 in.				
Safety considerations	Coolant pipe leak does not drain any portion of the core.			Coolant pipe leak can drain portion of the core, thereby causing a fuel element melt- down unless a secondary containment can be provided for the low pipe.		

*Exclusive of local shield patches to make up for voids in the shield caused by coolant piping.

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Figure 4-3. Reactor arrangement horizontal pressure vessel orientation.

Reactor Internal Design

The core, radial reflector, and method of control (rotating drums) were assumed to be the same for this study as those used in the 3-Mw_e MCR design reactor. However, changes were considered for the liquid metal manifolding (nozzles, piping, and flow pattern) the tungsten bowl used to protect the reactor vessel in the event of fuel meltdown, and control drum drive location.

The internal structure was not modified for this study, but it was recognized that some changes are probably needed. The fuel elements, reflectors, and control drums are supported axially in the upright reactor whereas they must be supported as beams in the horizontal reactor. This change could present a serious problem at the operating temperatures of the reactor. Deflections of these components could result in changes to the cross-sectional area of coolant flow passages, thereby affecting temperatures and thermal gradients throughout the reactor. In addition, added structure will be needed to resist transport shock loads which are higher in the vertical direction than they are in the lateral direction or in the fore and aft direction.

In the 3-Mw_e MCR design, a 0.25-in. thick tungsten bowl is provided beneath the core to protect the vessel from burning through in the event of a fuel meltdown. A similar device would need to be provided for the horizontal reactor. It could be a full or a partial cylinder and fit between the core and the reflector chamber. At least the lower portion would be constructed of tungsten; however, fabricating the protecting device as a complete cylinder would facilitate the machining and the mechanical assembly operations. A complete cylinder would permit concentric machining and concentric assembly of the reactor subassemblies and components, whereas a partial cylinder would require eccentric machining and assembly.

An advantage for all horizontal vessel concepts is the elimination of the need for installing ring seals during reactor assembly. Such seals are needed in the 3-Mw_e MCR design upright reactor to prevent unwanted bypass flow between the inlet and outlet liquid metal plenums. Without ring seals, the inlet and outlet nozzles would need to be stacked vertically to achieve the necessary flow control. This vertical stacking would increase the vessel height by 6 in. Excess liquid metal bypass flow can be prevented in a horizontal reactor by using rigid partitioning members which are permanently attached to the reactor vessel, particularly with a parallel flow arrangement as shown in Figure 4-3.

Flow and Thermal Hydraulics

The liquid metal nozzles and plenums shown in Figure 4-1, and in Concept IV of Figure 4-2 are arranged so that liquid metal flows through the radial reflector and then through the core in

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series. The nozzle concept shown in Figure 4-1 is the same as that used in the 3-Mw_e MCR design concept, with the reactor vessel rotated so that a liquid metal leak external to the reactor vessel will not drain liquid metal from the core. Concept IV of Figure 4-2, while providing series flow, has the disadvantage of potentially draining part of the core should a liquid metal leak develop in the reactor outlet piping. It also presents a more difficult shielding problem because shielding material cannot easily be located directly in front of the core.

Concepts I, II, and III of Figure 4-2 are arranged so that liquid metal flows through the core and the reflector in a parallel arrangement. With parallel flow, the coolant which exits from the reflector is lower in temperature than the coolant which exits from the core and the two streams are mixed at the outlet nozzle from the reactor.

A comparison of the data calculated for the series and parallel flow concepts is presented in Table 4-II. These data include the percentage of bypass flow (for the parallel patterns), core outlet temperature, and reflector outlet temperature. Three cases were considered: reflector outlet temperature held at 1250°F; core bypass flow held at 5% of the total coolant flow; and hot channel outlet temperature held at 1662°F. For the same reactor power generation, the flow rate must be increased by about 12% to simultaneously satisfy the 3-Mw_e MCR design conditions of a 1300°F vessel temperature and a 1662°F hot channel outlet temperature. However, this increase in liquid metal flow rate for a parallel flow arrangement is accomplished with less pumping power than is required for a series flow arrangement. This lower pumping power requirement results from the pressure drop through the core being 14 to 20% less for parallel flow cases than it is with the 3-Mw_e MCR design series flow case.

Afterheat Removal

The flow distribution of liquid metal within the reactor would be different for the horizontal reactor, and the manner and magnitude of the difference is not readily predictable. However, if the bottom shield thickness is the same as that used for the 3-Mw_e MCR design, the elevation between the reactor core center and the afterheat removal heat exchanger center line would be 24.00 in. for the horizontal reactor concepts as compared to the 18.00 in. available in the 3-Mw_e MCR design upright reactor. This would facilitate the use of natural convection for removing decay heat after shutdown.

In the event of a coolant pipe leak, a forced convection system could be used for the continued removal of decay heat with either the reactor shown in Figure 4-1 or Concept I of Figure 4-2. A leak in the liquid metal piping for these concepts would not drain the core. However, a leak in the reactor piping with Concepts II, III, and IV of Figure 4-2 would result in at least a

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portion of the core being drained. This, in turn, would result in a fuel meltdown unless the pipe leak could be controlled by a containment sleeve.

Table 4-II.

Comparison* of series versus parallel flow (15 Mw_t nominal power).

Type of flow	Condition	Fraction core bypass flow	Reactor outlet temperature (°F)	Core outlet temperature (°F)	Reflector outlet temperature (°F)
Series (FY 1964 PD conditions ^b)	Average hot channel	0.05	1500	1516	1209
		0.05	1500	1662	—
	Average hot channel	0.18	1500	1555	1250
		0.18	1515	1725	—
Parallel	Average hot channel	0.05	1500	1506	1380
		0.05	1515	1652	—
	Average hot channel	0.068	1500	1512	1332
		0.068	1515	1662	—

*At constant core flow rate of 250 lb/sec and a reactor ΔT of 300°F.

Note: Values in boxes denote parameter fixed for the calculation.

Control Drums and Drives

The control drums inside the reactor vessel and the control drum drives outside the reactor vessel were assumed to be the same as those used for the 3-Mw_e MCR design upright reactor. In a horizontal reactor, however, the drum radial bearings must accommodate higher drum support loads, including vertical transport shock loads, whereas they function primarily as guide bearings in the vertical reactor configuration.

Additionally, an in-line coaxial arrangement for the drum and drive would be less practical for a horizontal reactor if a gas environment for mechanical drive components is required. Assuming present state-of-art capability, it would not be possible to adequately seal the liquid metal from the gas chamber. Use of the 3-Mw_e design nutating motor drive in the horizontal

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reactor concept requires that the reactor side of the seal membrane be in a hot (approaching 1200°F) liquid metal environment. In the 3-Mw_e design, this seal is in a relatively cool (about 700°F) liquid metal vapor environment.

Alternate control methods and/or control drives considered for a horizontal reactor were: locate a nutating motor drive near the package roof and connect the drive member to the control drum through a flexible shaft; locate an actuator near the package roof and drive the control drum through bevel gears; and, change to an axial movement type of control, using either the control drum(s) or the fuel element(s), and operate through a push-pull type of actuator located near the package roof and connected to the control drum of fuel element with a flexible shaft. All of these require adding shaft housings to the reactor vessel and they require further study and analysis. Additional development would be required.

These methods all permit locating control drum actuators near the roof of the power generation package, minimizing voids in the shield. The bevel gears, if feasible, would require development effort; however, they would permit a more positive control of the variation between actuator movement and control drum movement.

The use of a control system involving the axial movement of either control drums or fuel elements requires that the reactor vessel length be increased to accommodate the control drum or fuel element when it is in the "out" position. The added length would result in further lengthening of the power generation package and further increasing its weight. An axial movement control is not generally recommended. However, using the axial movement of fuel elements to effect control of the reactor could result in a reduction to the reactor diameter, since the reflector thickness could be reduced.

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Alternate Forced Convection Afterheat Removal Systems

The afterheat removal subsystem dissipates heat generated in the reactor after normal shut-down or scram, so that:

- The reactor fuel and fuel cladding temperatures are maintained within design limits
- Thermal shock and temperatures are maintained within design limits

A natural convection afterheat removal system, with provision for forced convection coolant circulation by the primary loop pumps, is provided in the power generation package of the 3-Mw_e MCR design system.⁵ A supplementary off-skid forced convection system⁵ is provided for test operations to permit the removal of decay heat when the primary liquid metal loop, except for the reactor, is drained for maintenance.

The natural convection system requires that the afterheat removal system heat exchanger be located at an elevation above the reactor core which is sufficient to provide natural circulation. Thus, the location of this heat exchanger is limited even for normal operation and the rate of decay heat removal is dependent on the attitude of the power generation package. The primary loop pumps can be used to force liquid metal through the afterheat removal system heat exchanger when the power generation package is at an attitude that is unfavorable to natural convection. However, the primary loop pump must be operated at one-half its design speed to obtain the desired liquid metal flow for afterheat removal; therefore, the pump would be operating under a severe operational efficiency penalty, requiring 17.5 kw (92.6 kva) to pump the liquid metal. Less than 1.0 kw would be required for pumps which were designed for the correct flow.

An additional problem related to the natural convection system of the 3-Mw_e MCR design is the inability to remove decay heat when the primary loop, external to the reactor, is drained. Therefore, alternate forced convection systems, which could become a permanent subsystem with the power generation package, were considered in an effort to find a system that would permit the removal of decay heat at all design attitudes; permit the removal of decay heat from a drained MCR plant; and provide increased flexibility in the location of afterheat removal system components.

Two systems, employing Rankine cycle and thermoelectric conversion of the afterheat energy release, offered the potential of independently meeting the entire power requirements for the afterheat removal system and received limited attention during this study.

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The forced convection bypass afterheat removal system concept presented in Allison EDR 3710, 15 January 1964, was studied in detail. The effects of system rating, maximum temperature, liquid metal temperature drop, and air temperature rise on the weight and power requirements of this system are presented.

CONCLUSIONS AND RECOMMENDATIONS

Of the alternate afterheat removal concepts investigated, only the forced convection bypass system using electromagnetic pumps offers the degree of reliability desired for this vital system. It is recommended that further consideration be given to incorporating such a concept in the MCR powerplant design.

The Rankine cycle and thermoelectric afterheat conversion concepts pose development problems, added costs, and reduced reliability through increased complexity. Further study of these concepts is not recommended.

ANALYSIS

The forced convection afterheat removal system consists of a liquid metal-to-air heat exchanger, two air fans, an electromagnetic (EM) pump, piping, valves, controls, and instrumentation. Two liquid metal nozzles must be added to the reactor vessel, similar to those described in the 3-Mw_e MCR design report⁵ for the off-skid forced convection system. The 3-Mw_e design data for this system are presented in Table 4-III.

Parametric equations were developed to permit including this system in the overall MCR parametric system studies. The assumptions used in defining the equations are:

- The heat transfer area could be estimated by the overall heat transfer coefficient, $U = CG^{0.6}$, on the basis of analyses conducted during the MCR preliminary design studies.
- The mean temperature difference can be approximated by the arithmetic temperature difference.
- The ratio of heat transfer surface to matrix volume and the ratio of matrix dimensions are constants.
- An electromagnetic pump would be used.
- Piping is essentially the same as that used in the 3-Mw_e design.⁵
- Liquid metal pressure drop varies linearly with mass flow rate over the range of flow rates studied.
- Combined weight of piping and EM pump will be approximately optimum at the highest mass flow rate considered (57.6 lb/sec for 2250 kw at a liquid metal temperature drop of 200°F).

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Table 4-III.

Forced convection afterheat removal system 3-Mw_e MCR design data.

<u>Item</u>		
System rating—kw		225
Liquid metal maximum temperature—°F		1500
Liquid metal temperature drop-through heat exchangers—°F		300
Liquid metal flow rate—lb/sec		3.8
Liquid metal pressure drop—psi		4.2
Air inlet temperature—°F		59
Air outlet temperature—°F		400
Airflow rate—lb/sec		2.2
Air pressure drop—inches H ₂ O		6
System weight—lb		
Heat exchanger	74.1	
Piping	82.0	
Fans	35.8	
Electromagnetic pump	<u>100.0</u>	
Total—lb		292
Power requirements—hp		
Electromagnetic pump	0.5	
Fan	<u>2.8</u>	
Total—hp		3.3

The equations used in computing parametric data are:

Afterheat removal system weight:
(Ref = 3-Mw_e MCR design)

$$\frac{W_{HT}}{W_{HT_{Ref}}} = \left[\frac{Q}{Q_{Ref}} \right]^{0.667} \left[\frac{\Delta T_2}{\Delta T_{2_{Ref}}} \right] \left[\frac{\Delta T_{M_{Ref}}}{\Delta T_M} \right]^{1.667} \left[\frac{P_{AH}}{P_{AH_{Ref}}} \right] \frac{[2 SE + 1.4 P_{AH}]_{Ref}}{[2 SE + 1.4 P_{AH}]} \quad (4-1)$$

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$$\text{where } \left[\frac{P_{AH}}{P_{AH_{Ref}}} \right] \left[\frac{2 SE + 1.4 P_{AH}}{2 SE + 1.4 P_{AH}} \right]_{Ref} \quad (4-2)$$

is the term used to correct Equation (4-1) for temperatures and pressures that are different from those used in the reference (3-Mw_e MCR) design.

Fan power:

$$\frac{HP_a}{HP_{a_{Ref}}} = \left[\frac{Q}{Q_{Ref}} \right]^{2.333} \left[\frac{\Delta T_{a_{Ref}}}{\Delta T_a} \right]^4 \left[\frac{\rho_{a_{Ref}}}{\rho_a} \right]^2 \left[\frac{\Delta T_M}{\Delta T_{M_{Ref}}} \right]^{1.667} \quad (4-3)$$

where

$$\frac{\rho_{a_{Ref}}}{\rho_a} = \left[\frac{460 + T_{a_i} + \frac{\Delta T_a}{2}}{460 + T_{a_i} + \frac{\Delta T_a}{2}} \right]_{Ref}$$

Fan weight:

$$\frac{W_{Fans}}{W_{Fans_{Ref}}} = \left[\frac{HP_a}{HP_{a_{Ref}}} \right]^{0.45} \quad (4-4)$$

on the basis of an analysis of the literature.

Piping weight:

$$\frac{W_{Pipe-Tot}}{W_{Pipe-Tot_{Ref}}} = \frac{P_{AH}}{P_{AH_{Ref}}} \left[\frac{2 SE + 1.4 P_{AH}}{2 SE + 1.4 P_{AH}} \right]_{Ref} \frac{F - W_k}{F - W_{k_{Ref}}} \left[\frac{W_k}{W_{k_{Ref}}} \right]^{1.5} \quad (4-5)$$

$$\text{where } W_k = \frac{R_1 Q}{C_{p_k} \Delta T_k}$$

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Pump weight:

$$\frac{W_{\text{Pump}}}{W_{\text{PumpRef}}} = \left[\frac{W_k}{W_{k\text{Ref}}} \right]^{0.715} \left[\frac{A + B_P W_k}{A + B_P W_{k\text{Ref}}} \right]^{1.25} \quad (4-6)$$

Pump power:

$$\frac{HP_k}{HP_{k\text{Ref}}} = \frac{W_k}{W_{k\text{Ref}}} \frac{[A + B_P W_k]}{[A + B_P W_{k\text{Ref}}]} \quad (4-7)$$

The preceding equations were used to calculate data for the following conditions:

- Forced convection afterheat removal system rating of 120 to 2250 kw
- Air temperature rise of 300°F, 400°F, and 500°F
- Liquid metal temperature drop of 200°F, 300°F, and 400°F
- Maximum liquid metal temperature of 1300°F, 1500°F, and 1600°F

NOMENCLATURE

A	Constant
AHRS	Afterheat removal system
B _P	Constant
E	Joint efficiency (0.5)
F	Constant
HP _a	Fan motor power (hp)
HP _{aRef}	Fan motor power for 3-Mw _e MCR design (2.8 hp)
HP _k	Liquid metal pump power (hp)
HP _{kRef}	Liquid metal pump power for 3-Mw _e MCR design system (0.5 hp)
P _{AH}	System pressure (psig)
P _{AHRef}	System pressure for 3-Mw _e MCR design system (85 psig)
S	Allowable stress at temperature T _O - S = 830e-0.00497 (T _O -1600)
T _{ai}	Air inlet temperature (59°F)
T _O	Liquid metal maximum temperature (°F)
T _{ORef}	Liquid metal maximum temperature in 3-Mw _e MCR design system (1500°F)
ΔT _a	Air temperature rise (°F)

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ΔT_{aRef}	Air temperature rise in 3-Mw _e MCR design system (400°F)
ΔT_k	Liquid metal temperature drop (°F)
ΔT_{kRef}	Liquid metal temperature drop in the 3-Mw _e MCR design system (300°F)
ΔT_M	Arithmetic mean temperature difference in afterheat removal exchanger (°F)
ΔT_{MRef}	Arithmetic mean temperature difference in 3-Mw _e MCR design afterheat removal exchanger (1091°F)
U	Overall heat transfer coefficient (BTU/hr-ft ² -°F)
W _{Fans}	Weight of fans (lb)
W _{FansRef}	Weight of fans in 3-Mw _e MCR design systems (35.8 lb)
W _{HT}	Heat exchanger weight (lb)
W _{HTRef}	Weight of heat exchanger in 3-Mw _e MCR design (74.1 lb)
W _{Pipe-Tot}	Piping weight (lb)
W _{Pipe-TotRef}	Weight of piping in 3-Mw _e MCR design (82.0 lb)
W _{Pump}	EM pump weight (lb)
W _{PumpRef}	Weight of 3-Mw _e MCR design pump (lb)
W _a	Airflow rate (lb/sec)
W _k	Liquid metal flow rate (lb/sec)
W _{kRef}	Liquid metal flow rate in 3-Mw _e MCR design system (3.84 lb/sec)
ρ_a	Air density at $\left(T_{a_i} + \frac{T_a}{2}\right)$ °F, (lb/cu ft)
ρ_{aRef}	Air density at $\left(T_{a_i} + \frac{T_a}{2}\right)$ °F in 3-Mw _e MCR design (lb/cu ft)

The overall afterheat removal system weight is presented in Figure 4-4 for a maximum liquid metal temperature of 1300°F, Figure 4-5 for a maximum liquid metal temperature of 1500°F, and Figure 4-6 for a maximum liquid metal temperature of 1600°F. The graphical presentation of Equation (4-2), used for correcting the forced afterheat convection removal system weight for temperatures and pressures that are different from those used in the 3-Mw_e design, is shown in Figure 4-7.

These figures indicate that, for the ranges of parameters studied, the weight of the alternate forced convection afterheat removal system will:

- Decrease with an increase in air temperature rise through the heat exchanger (airflow is decreased to obtain same heat removal capacity)
- Decrease with an increase in the liquid metal temperature drop through the heat exchanger (liquid metal flow is decreased to retain same heat removal capacity)
- Increase as the maximum liquid metal temperature increases

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Figure 4-4. The effects of air and liquid metal temperature changes on afterheat removal system weight – maximum liquid metal temperature 1300°F.

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Figure 4-5. The effects of air and liquid metal temperature changes on afterheat removal system weight – maximum LM temperature 1500°F.

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Figure 4-6.
The effects of air and LM temperature changes on afterheat removal system weight — maximum LM temperature 1600°F .

Figure 4-7.
The effects of temperature and pressure on afterheat removal system weight.

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It should be noted that these trends are for the overall systems for the ranges of parameters studied. They may change for individual components and/or for different temperature ranges.

The power requirements of the forced convection afterheat system are presented in Figure 4-8 for a maximum liquid metal temperature of 1300°F, Figure 4-9 for a maximum liquid metal temperature of 1500°F, and Figure 4-10 for a maximum liquid metal temperature of 1600°F.

All of the functional advantages sought with a forced convection afterheat removal system are attainable with this alternate system which cools part of the primary loop liquid metal with air.

Secondary Liquid Metal Loop Rankine Cycle System

A secondary liquid metal loop, which operates at a lower system pressure than the primary loop, was investigated as a Rankine cycle system for removing decay heat. A schematic diagram is presented in Figure 4-11. The Rankine cycle afterheat removal system would consist of an evaporator, a turbine, a condenser, a liquid metal pump, an electric generator, valves, controls, instrumentation, and piping.

Figure 4-8. Effects of air and LM temperature changes on afterheat removal system power requirements - maximum LM temperature 1300°F.

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Figure 4-9. Effects of air and LM temperature changes on afterheat removal system power requirements – maximum LM temperature 1500°F.

Figure 4-10. Effects of air and LM temperature changes on afterheat removal system power requirements – maximum LM temperature 1600°F.

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Figure 4-11. Rankine cycle afterheat removal system.

The evaporator, located in the top liquid metal plenum of the reactor, is a liquid metal (primary loop) to liquid metal and liquid metal vapor (secondary loop) heat exchanger. On the primary loop side, the liquid metal would rise through the reactor core, give up heat as it passes across the evaporator, and return to the bottom of the core through the reflector. The natural convection flow would have been established by conditions of the normal MCR system operation since liquid metal in the reflector is at a lower temperature than it is in the core.

To prevent draining the evaporator if a leak develops outside the pressure vessel, some provision is needed to maintain the liquid metal in the reactor vessel at a level that is 2.0 in. higher than it is in the 3-Mw_e design. This provision could be supplied in the form of raising the liquid metal nozzles or piping by 2.0 in., locating a 2.0-in. high dam in the bottom of the nozzles or piping, or a combination of these and other provisions.

In the secondary loop, the liquid metal would enter the evaporator in a subcooled (20 to 40°F) condition, extract heat from the primary loop fluid, and exit to the turbine with a quality of about 91%. It would expand through the turbine which drives an electric generator. The vapor from the turbine is condensed and subcooled (50 to 70°F) in a liquid metal vapor-to-air heat exchanger and the condensate is returned to the evaporator by a liquid metal pump which obtains power from the electric generator driven by the turbine.

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The fluid flow rates, thermal transfer rates, and power rating of the various Rankine cycle system components are presented in Table 4-IV for the 3-Mw_e MCR design afterheat removal system. These data were generated from a preliminary design of a typical system in which reasonable to conservative assumptions were made for pressure drops, efficiencies, and heat exchanger effectivenesses.

Table 4-IV.
Rankine cycle afterheat removal system data for 3-Mw_e design.

<u>Item</u>	
Energy transferred in evaporator—BTU/hr	768,000
Flow at 1300°F—lb/hr	904
Energy extracted in turbine—BTU/hr	63,300
Energy removal in condenser—BTU/hr	704,700
Vapor condensed—lb/hr	828
Generator output—kw	6.5
Pumping power required—kw	0.05
Fan power required—kw	3.5
Excess power available—kw	2.95

Note: These data are for a typical Rankine cycle system, but it may or may not be the optimum system.

During normal operation of the MCR system, the evaporator is valved off and its vapor pressure allowed to rise until it is consistent with the liquid metal temperature at the reactor core outlet. This pressure would be less than the liquid metal pressure of the primary loop.

During shutdown of the reactor, or during scram, the evaporator valves are opened and the Rankine cycle afterheat removal system would function in its normal manner. For the study, the evaporator was assumed to operate at a boiling temperature of about 1300°F, thereby requiring a flow rate of about 904 lb/hr to remove the necessary maximum of 768,000 BTU/hr from the reactor core. It may be necessary to reduce the boiling temperature.

A 50% efficient turbine extracts 63,300 BTU/hr from the expanding vapor for a useful work output of 10.8 kw. The useful work drops to about 6.5 kw because of a 60% efficiency assumed for the electric generator, which is driven by the turbine.

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About 828 lb/hr of potassium vapor is liquefied in the condenser at 1250°F and subcooled to about 1180°F. The condensate pump is estimated to have a 30% efficiency. The condenser air fan was assumed to have an air temperature rise of about 800°F, a mechanical efficiency of 50%, and an electrical efficiency of 60%.

Although studies are incomplete, it is evident that more energy would be available from the use of decay heat than would be needed to operate the Rankine cycle afterheat removal system. This excess energy can be used to furnish power to other equipment and reduce the size and weight of the power generation package auxiliary power unit, as well as to reduce the fuel inventory needed for the auxiliary power unit.

A large portion of the power requirements that determine the capacity needed for the 3-Mw_e MCR design auxiliary power unit are attributed to the afterheat removal system. This is largely due to the need for operating one of the primary loop liquid metal pumps at one-half speed to furnish a forced convection capability to the afterheat removal system. Without the need to furnish power for the afterheat system, the power requirements of the auxiliary power unit are greatly reduced.

The principal disadvantages of a Rankine cycle afterheat removal system are the addition of a secondary liquid metal loop and the need for a larger number of components, particularly rotating components. These present added problems in maintenance of the plant and adversely affect its reliability. A secondary loop with its additional components would increase the initial plant cost and a secondary loop Rankine cycle system would require development.

It may be possible to convert the Rankine cycle system into a natural convection system, thereby eliminating the rotating components, but then the full use of decay heat as a source for electric energy would not be realized.

The potential of the Rankine cycle system was not completely determined and the components were not fully sized. Consequently, parametric equations were not included in the MCR system parametric studies.

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Pressurization Systems

The purposes of the 3-Mw_e MCR cover gas system are to:

- Develop and maintain a liquid metal pressure that is in excess of the pressure required to prevent coolant from boiling in the reactor and to prevent cavitation at the pump inlet under all operating conditions
- Provide primary coolant system overpressure protection
- Provide a means for controlling and measuring the liquid level

The cover gas system used for the 3-Mw_e MCR design⁵ was used as the reference design for the studies reported herein. It is a conventional system with a supply of argon gas, a surge tank, controls, and instrumentation. A liquid metal-cover gas interface is maintained in the surge tank which provides space for the volumetric expansion and contraction of liquid metal with temperature changes. The system is designed to maintain design pressures and to re-furnish gas that is lost during start-up and shutdown. About 12.5 scf of cover gas are vented to the surrounding atmosphere when the MCR plant is started and as the liquid metal expands into the surge tank. This gas is replaced from the cover gas system supply tanks at shutdown, when the MCR plant cools and liquid metal recedes from the surge tank. Gas is not lost during normal operation. The gas supply consists of a 238-ft³ NTP main tank and a 78-ft³ NTP back-up tank. These tanks are adequate for 1000 hr of operation. However, the requirement for replacing argon gas cylinders after every 1000 hr of operation is a logistic support requirement that could be avoided if a different type of cover gas system were used.

Alternate surge tank and cover gas systems were studied and compared with the 3-Mw_e MCR design. The systems studied were:

- Self-pressurized surge tank
- Bellows-type expansion compensator
- Compressor system with high pressure tank and low pressure tank

CONCLUSIONS AND RECOMMENDATIONS

Self-Pressurized Surge Tank

The self-pressurized surge tank system is larger than the 3-Mw_e MCR design and would require adding weight and length to the power generation package.

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The smallest self-pressurized surge tank that could be considered is 14 ft^3 , about 70% larger than the 3-Mw_e MCR design. A tank designed to perform all the functions of the 3-Mw_e design concept should be about 30 ft^3 in volume, or 275% larger than the 3-Mw_e design.

The self-pressurized surge tank is the most reliable of the pressurization systems studied as a result of its complete lack of moving parts and inherent simplicity.

Bellows-Type Expansion Compensator

The bellows-type expansion compensator is considerably more than twice as heavy and has about the same volume as the 3-Mw_e MCR surge tank and cover gas system.

The 105 to 165 convolutions needed for the bellows present a difficult and expensive fabricating problem as well as a problem of questionable reliability.

The bellows spring rate would vary with the ambient temperature, material composition, and fabricating variations. External adjustable compensating devices would be required to obtain and control the necessary bellows spring rate. A cover gas system is not required with a bellows-type expansion compensator.

High and Low Pressure System with Gas Compressor

A self-contained system (Figure 4-12) with a low pressure tank, a high pressure tank, and a gas compressor may be smaller and lighter than other systems studied. The greater complexity of such a system may, however, compromise its utility through lack of reliability.

Recommendations

The following recommendations are offered regarding pressurization systems for the MCR.

- Use the self-pressurization surge tank system for those MCR system applications in which the attainment of maximum reliability is sufficiently important to justify adding weight and volume to the power generation package.
- Discontinue consideration of the bellows-type expansion compensator.

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Figure 4-12. Schematic diagram – high and low pressure system with gas compressor.

- Perform additional studies on the concept which employs a high pressure tank, a low pressure tank, a gas compressor, and related valves, controls, instrumentation, and piping for the cover gas system. Such a system may be lighter and smaller than the 3-Mw_e design while not requiring the logistic support of replacing argon bottles. The study should be continued at least to a point where such a system can be compared quantitatively with the 3-Mw_e MCR surge tank and cover gas system.

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ANALYSIS

Two alternate pressurization systems were studied in detail and a third alternate system was briefly considered. The two systems studied in detail are the self-pressurization surge tank and bellows-type expansion compensator. The system studied briefly was one which uses a gas compressor with a high pressure gas tank and a low pressure gas tank. These systems are compared with the 3-Mw_e MCR surge tank and cover gas system in Table 4-V.

Table 4-V.
Comparison of pressurization methods.

Item	3-Mw _e MCR surge tank and cover gas system	Self-pressurized surge tanks		Bellows-type expansion compensator	Gas compressor system with high and low pressure tanks
		Minimum	Preferred		
Surge tank volume—ft ³	8.4	14.0	30.0	8.75	8.4
Surge tank weight—lb	231	380	810	1250	231
Auxiliaries weight—lb	370	—	—	—	320
Cost relative to 3-Mw _e MCR	—	same	higher	higher	higher
Reliability relative to 3-Mw _e MCR	—	higher	higher	lower	lower
Performance characteristics:					
a. Develops required pressure at full power average system temperature	x	x	x	x	x
b. Develops required pressure over full range of operating temperatures at power	x	x	x		x
c. Fulfills all pressure control requirements of 3-Mw _e MCR	x		x		x
Limitations	Requires logistic support	Requires major redesign of power generation package			x

*System and system components need to be defined. However, all components are conventional.

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Self-Pressurized Surge Tank

A self-pressurized surge tank is a tank in which argon gas is compressed or expanded above a liquid metal-cover gas interface as liquid metal in the primary loop expands or contracts with temperature changes in the primary loop. The tank is located so that, assuming an initial average primary temperature of 300°F, a liquid metal-cover gas interface exists at the liquid metal pipe inlet to the surge tank, as shown in Figure 4-13. The volume and initial gas pressure are selected so that the volumetric expansion of liquid metal in the primary loop, as a result of plant temperature change, will result in the cover gas applying the required operating pressure to the primary loop. Thus, the cover gas pressure increases to a specified value when liquid metal in the primary loop expands volumetrically in response to an increase in its average temperature (averaged over entire primary coolant system).

$T_{\text{argon}} = \text{Constant}$

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Figure 4-13. Schematic diagram – self-pressurized surge tank.

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The parameters used in defining the self-pressurized system are presented in Table 4-VI. They are the operational requirements used to define the 3-Mw_e design. These parameters are adequate to prevent boiling in the reactor core and to prevent pump cavitation. They also limit the primary loop pressures to levels below those which could overstress the components and piping of the primary loop.

Table 4-VI.

Primary system input parameters pertinent to self-pressurized surge tank study.

High pressure scram	79.6 psia
High pressure alarm	77.6 psia
Upper end of pressure control band	73.6 psia
Lower end of pressure control band	63.6 psia
Low pressure alarm	59.6 psia
Low pressure scram	57.6 psia
Minimum surge tank pressure (permitting operation of pumps at 300°F and 8000 rpm)	46 psia
Primary system average coolant temperature range consistent with ambient temperature and power level variations	1275-1425°F
Surge tank set pressure	68.6 psia
Average primary coolant temperature at rated power	1350°F

These preferred parameters furnish a capability for maintaining a surge tank pressure of 68.6 ± 9 psia over an average primary loop temperature range of 300 to 1425°F without consuming cover gas. An alternate performance criterion that may be acceptable is a capability for maintaining, without consuming cover gas, a surge tank pressure of 68.6 ± 9 psia over a 1275 to 1425°F range of average primary loop coolant temperature, and the 46.0 psia required for pump operation at 300°F (the system fill temperature).

It can be seen from Figure 4-14 that a minimum surge tank volume of 14 ft³ is required to meet the alternate performance criterion. Extrapolating the curves in Figure 4-14, a surge tank volume of about 30 ft³ is needed to duplicate all pressure control functions of the reference design. A comparison of the preferred and alternate surge tanks and criteria is presented in Table 4-VII. It should be noted, however, that neither the 14- nor the 30-ft³ system has taken into consideration the effects of creep in components of the primary loop. Total creep is estimated to approximate a 0.5-ft³ increase in volume for the primary loop, resulting in a system pressure reduction of 5.2 % if a 14.0-ft³ tank is used, or a reduction of 1.9% if a 30.0-ft³ tank is used.

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Table 4-VII.

Self-pressurized surge tank volume—required for a specified performance.

<u>Performance criteria</u>	<u>Surge tank volume required (ft³)</u>
<u>Preferred</u>	30*
a. Surge tank pressure of 68.8 ± 9 psia, over average primary coolant temperature range of 300-1425°F, and	
b. No consumption of cover gas	
<u>Alternate (possibly acceptable)</u>	14
a. Surge tank pressure of 68.6 ± 9 psia, over average primary coolant temperature range of 1275-1425°F, and	
b. No consumption of cover gas	

*Extrapolated from Figure 4-14.

Legend

P_{min}—minimum pressure for pump operation
P_{av}—average operating pressure (set point)
UCL—upper pressure control limit
LCL—lower pressure control
HPA—high pressure alarm
LPA—low pressure alarm
T_{av}—primary coolant full load average operating temperature
UL—upper limit average coolant temperature range
LL—lower limit average coolant temperature range

Figure 4-14. Effect of tank volume and average primary system temperature on self-pressurized surge tank pressure.

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In addition, the effects on the MCR plant control system, when a self-pressurized surge tank is used, were not completely evaluated. If the alternate (14.0 ft³) self-pressurized surge tank is used, the MCR plant control system must contain a provision for overriding the low pressure scram condition to permit operation of the control system during start-up. Provision of such interlocks in the MCR system for periods in which the plant is operating at a fairly substantial power level will necessitate including a negative pressure rate change scram during these periods of operation to account for loss of coolant protection prior to restoration of the normal low pressure scram system.

Bellows-Type Expansion Compensator

A bellows-type expansion compensator was considered to find out whether it is lighter or smaller than a conventional surge tank. It can be used only in a system that does not have a required liquid metal-cover gas interface—i. e., in systems which use either a canned or electromagnetic pump. Also, unless an emergency gas supply is included, the bellows expansion is limited to use in systems where there is no operational requirement to drain and refill the plant at intervals other than those required for refueling or overhaul. This bellows is a flat, disk-type compensator that is completely filled with liquid metal, expanding with the volumetric thermal expansion of liquid metal and applying pressure to the liquid metal in the primary loop as it is developed by the spring rate of the bellows.

The flat, disk-type bellows was selected for evaluation because it can be readily filled and drained, it is smaller than other types of bellows, and it is relatively easy to analyze. A schematic diagram of the compensator is shown in Figure 4-15, and the input parameters are presented in Table 4-VIII.

Table 4-VIII.
Input parameters for design of expansion compensator.

Material of construction	Type 316 SS
Operating pressure	68.6 psia
Operating temperature	700°F
Allowable stress at 700°F	17,000 psi*
Modulus of elasticity	25.5×10^6 psi
Number of cycles to failure	2300
Volumetric expansion of primary coolant (300°F to 1350°F)	4.31 ft ³

*From MCR structural design criteria.

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a) Initial condition

b) Expanded condition

Figure 4-15. Schematic diagram -
expansion compensator.4186-39
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The characteristics of a bellows-type expansion compensator are presented in Figures 4-16 and 4-17. The relationship between compensator weight, expanded volume, pressure (spring rate) developed, and the ratio of bellows width to inside radius are shown in Figure 4-16. The relationship between the number of convolutions, disk thickness, radius, and the ratio of bellows width to inside radius are shown in Figure 4-17. The characteristics of a bellows that will meet the input parameter requirements are summarized in Table 4-IX.

It can be seen from these figures that the compensator weight (excluding heads, supports, and preloading devices) is twice the 600-lb weight of the 3-Mw_e MCR design surge tank and cover gas system, the volume, excluding that required for supports and preloading devices, is about the same as that required for the 3-Mw_e MCR design surge tank; and the compensator, consisting of 105-165 convolutions, will be relatively difficult and expensive to fabricate, compared to the 3-Mw_e MCR design surge tank. Therefore, the expansion compensator will be heavier, at least as large, more expensive, and probably less reliable than the 3-Mw_e MCR surge tank and cover gas system.

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Figure 4-16.
Expansion compensator study -
flat disk type.

Figure 4-17.
Expansion compensator study -
flat disk type.

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Table 4-IX.
Characteristics* of a bellows-type expansion compensator.

<u>Item</u>	
Inside radius—ft	0.915
Ratio of bellows width to inside radius	0.15 to 0.20
Compensator weight—lb	1230 to 1270
Expanded volume—ft ³	8.5 to 9.0
Number of convolutions	105 to 165
Disk thickness—in.	0.105 to 0.125

*Determined for 3-Mw_e MCR design from the curves of
Figures 4-16 and 4-17, and consistent with the parameters
of Table 4-VIII.

In addition, an externally adjustable device would be needed to provide a controllable, uniform spring rate as environmental temperatures change. The spring rate of the bellows, by itself, will vary with the ambient temperature, material composition, physical dimension variations, and fabricating variations. Therefore, a compensating device must be provided for the bellows if a controlled spring rate—and thereby a controlled or predictable liquid metal pressure—is to be provided.

Gas Compressor System—with High Pressure Tank and Low Pressure Tank

A schematic diagram of a gas pressure stabilization system is shown in Figure 4-12. The system consists of a high pressure gas tank, a low pressure gas tank, a gas compressor, and related valves, vapor traps, piping, filter, controls, and instrumentation.

This system is judged to be lighter and smaller than either the 3-Mw_e design or the systems just discussed. It may be possible, using the compressor system, to avoid the venting and makeup of gas required by the 3-Mw_e design system and the bulk required by the other systems. However, the complete system must be more adequately defined from both hardware and operational viewpoints before an evaluation can be made.

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Gas is charged to the surge tank from the high pressure gas tank as required to maintain the desired liquid metal overpressure in the primary loop. As pressure rises with the increase of temperature in the primary loop and the accompanying expansion of liquid metal, the gas cover in the surge tank is partially reduced by venting to the low pressure tank. Thus, a high pressure gas source and the low pressure gas sink are used to maintain the desired pressure level in the primary loop. The compressor is used to raise the gas pressure from that of the low pressure tank to that of the high pressure tank, thereby completing the cycle.

Unidirectional valves are required at the locations shown in Figure 4-12. They would be either designed or arranged in the system so that required operation would not be jeopardized by solidification of liquid metal vapor on critical surfaces or parts. In addition, they would be independently operable.

Small quantities of liquid metal vapor can be tolerated throughout the cover gas system. Large amounts, however, would block passages, clog the filter, and/or harm the compressor. Therefore, vapor traps are provided at locations where the removal of liquid metal vapor can best be accomplished. Traps are located at the inlet and outlet of the surge tank—to isolate liquid metal vapor within the surge tank; at the inlet and outlet of other critical components (pumps, controls, etc)—to restrict liquid metal vapor in these areas to the piping system; and at the outlet of the low pressure gas tank—to provide backup protection for the compressor against leakage through other system traps.

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Off-Skid Location of Power Generation Package Components

Mobility requirements for mobile compact reactors (MCR) impose limits upon the size and weight of individual packages. It follows that the size and weight of the Power Generation Package (PGP) may limit the power rating of a plant unless the package can be broken down to smaller units for transportation.

The basic objective of this study is to investigate the feasibility of locating portions of the primary system and subsystems on other packages to determine whether significant PGP weight reduction can be realized. Major components or subsystems, or portions thereof, which might be considered are the primary heat exchangers, auxiliary afterheat removal subsystem, purification subsystem, drain tank subsystem, and surge tank and cover gas subsystem. Locating these major components or subsystems off-skid for transportation requires field assembly and disassembly of units involving the makeup and breakdown of liquid metal and gas lines.

CONCLUSIONS AND RECOMMENDATIONS

- Connectable off-skid systems for use in controlled maintenance depots is considered practical and desirable.
- The savings in weight and size for the 3-Mwe MCR PGP are small or negative for any of the alternates considered. In addition, each increases the size and weight of the overall plant.
- Preliminary tests indicate that lines up to 0.5 in. nominal pipe size can be separated repetitively with minimum loss of coolant and contamination in the laboratory. Extensive effort would be required to develop the valves, the couplings, and the capability of repetitively making and breaking these and larger size connections under MCR plant field conditions. It should be noted that failure of a valve to close and seal the system against liquid metal leakage, or air in-leakage, would require freezing, cutting, and seal welding of liquid metal lines in the field to decouple the plant for transport.
- Several hours would be required during plant assembly and disassembly to thaw the liquid metal in the off-skid equipment, if frozen, and make or break the connections to the PGP.
- During the makeup and breakdown operations, it is necessary to maintain temperatures in the connections and the off-skid equipment at approximately the same level as the on-skid liquid metal. Additional controls, interconnecting cables and system interlocks for the preheat system of the off-skid equipment, and additional preheat power would be required. This would add significant weight to the PGP or separate package.

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- Although there was no detailed evaluation of the radiation hazards involved, there can be little doubt that personnel involved in making and breaking the connections to the off-skid equipment would be exposed to a higher radiation dose per move than would be expected if off-skid connections were not required.
- It is recommended that no further consideration be given to alternate power generation package layouts requiring makeup and disassembly of liquid metal lines in the field.

ANALYSIS

The components and subsystems for which off-skid location were considered are:

- Primary heat exchangers
- Auxiliary afterheat removal subsystem
- Purification subsystem
- Drain tank subsystem
- Surge tank and cover gas system

Figure 4-18 shows a typical schematic for connecting off-skid systems to the MCR power generation package. Some variation of this typical layout is required for each of the systems studied; for example, locating the main primary heat exchangers off-skid would require four sets of liquid metal valves and spool pieces in lieu of the two sets shown.

- The connections at the off-skid system must be located at a level sufficiently below the connections on the PGP to provide complete draining of the spool pieces and valve bodies during disassembly.
- The sump must have sufficient capacity to accommodate the liquid metal from the spool pieces and valves as well as the expansion and contraction of liquid metal in the off-skid system. (A return line to the surge tank on the PGP could be used in lieu of the sump.)
- An auxiliary gas supply is required to pump liquid metal into or out of the spool pieces and valves and purge the spool pieces. A vacuum system would more efficiently remove air from the spool pieces during plant assembly and provide means for leak checking.
- Unless the liquid metal in the off-skid system can be retained at high temperature during transport by a self-contained heating system, heaters for the off-skid system and the spool pieces must be connected into the PGP preheat control when the plant is assembled.

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Figure 4-18. Schematic for connecting off-skid systems to the MCR power generation package.

Primary Heat Exchangers

The dry weight of the primary heat exchangers is approximately 4990 lb. These units occupy approximately 5 ft of one end of the PGP. Some of this reduction in PGP size and weight might be realized if the heat exchangers were located on the power conversion equipment package.

Implementation of this relocation requires the installation of two valves in each of the inlet and outlet 5-in. pipe connections for a total of four valves to be added to the PGP and four valves to the off-skid system. Valves for this service would require development; however, similar valves for liquid metal service occupy a space envelope approximately 2.5 ft X 2.5 ft X 6 ft high, and weigh approximately 1000 lb each. Pressure drop, at rated flow, would be approximately 5 psi per valve, or 20 psi per loop.

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On this basis, it is concluded that:

- The valves cannot be located on the PGP or power conversion equipment packages because of a height limitation.
- The four valves, exclusive of controls, insulation, shock and vibration supports, and additional liquid metal inventory, would reduce the weight savings of 4990 lb by 4000 lb to a net saving of less than 1000 lb.
- In addition, the pumps and pump power would have to be increased to accommodate the increased pressure drop in the system. All vessels and piping would require design review which would require a probable increase in weight due to the higher pressure required in the surge tank (more than 20 psi) to prevent pump cavitation. These additions, together with the valve controls, insulation, supports, and increased plant liquid metal inventory would considerably increase, rather than decrease, the PGP weight.
- A reduction of less than 2 ft in PGP length would be realized.
- It should be noted that the power conversion equipment package length would be increased by at least 8 ft and its weight increased by an estimated 10,000 to 12,000 lb by locating the heat exchangers on the PCEP.

Auxiliary Afterheat Removal System

A natural convection afterheat removal system is included in the 3-Mw_e MCR design rated at 1.5% of reactor power to remove reactor afterheat after shutdown and during transport. Increased heat removal capability exists when flow is maintained through the afterheat removal heat exchanger by operating either or both of the primary coolant pumps at low speed from auxiliary power supplies (APU).

To effect depot maintenance or to use natural potassium during NRTS operations at minimum radiation levels, an auxiliary afterheat removal system was recommended to operate in conjunction with the off-skid drain system. This afterheat removal system capability is required when the system, exclusive of the reactor, is drained for maintenance. Connections were provided in the reference design reactor to accommodate this system.

The auxiliary afterheat removal system and drain tank system are covered in Appendix A of GMAD 3078-8 (Allison EDR 3950), Volume III. If the need should arise for additional afterheat removal capability in the field, it is recommended that this additional capability be designed into the on-skid subsystem.

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Purification System

The 3-Mw_e MCR design purification system weighs approximately 294 lb and occupies a total space including piping and valves of approximately 3.5 ft³. With the present design of the PGP, space is available for the purification system components.

Liquid metal technology studies have demonstrated that the initial fill of liquid metal should be gettered liquid metal—i. e., the liquid metal should be as free of oxygen as possible. The plant should be out-gassed as completely as practical prior to fill. The purification system is designed to maintain system cleanliness under expected operating conditions for one core life with the gettering bed to be replaced at the time of each core change.

The complexity of locating the purification system off-skid is described in the discussion related to Figure 4-18. Since only a small weight advantage can be gained by locating this system off-skid, it is recommended that it be retained as a part of the PGP as described for the 3-Mw_e MCR plant.

Drain Tank Subsystem

An off-skid drain tank system for depot maintenance is provided in the 3-Mw_e design. This drain system would also be used during NRTS testing operations if natural potassium were used as the primary coolant to reduce the radiation levels outside the primary shield. This system and its operation with the off-skid auxiliary afterheat removal subsystem are more fully described in Reference 5. Use of such an off-skid system in a controlled maintenance area is considered practical and desirable.

Surge Tank and Cover Gas Subsystem

The surge tank, in the 3-Mw_e MCR design, is an 8-ft³ tank sized to accept the expansion volume from the primary coolant as system temperature rises from the freezing point of the coolant to the maximum temperature without overflow in any operating attitude. The gas volume above the coolant in the surge tank is large enough to accept coolant volume expansion and contraction over the power range of the reactor without pressurization or venting of the cover gas.

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Pressure must be maintained on the primary coolant system at a level high enough to prevent pump cavitation and bulk boiling of the coolant in the reactor under any condition of operation. The cover gas subsystem controls the pressure in the primary coolant system by controlling gas pressure in the surge tank. One 238-scf argon cylinder, with a 78-scf cylinder as backup, is sufficient for a 1000-hr normal duty cycle. The normal and backup cover gas systems are required during transport and must be included as on-skid equipment. Additional operating time between gas cylinder replacements can be easily accomplished by manifolding the on-skid cylinders to off-skid cylinders by conventional means. The existing system design incorporates the ability to exchange gas bottles in the field.

Some of the surge tank gas volume (about 2 ft³) could be located off-skid if this becomes necessary. Trapping and preventing vapor carryover to the off-skid tank volume would be an additional design problem leading to the requirement for additional equipment. Duplication of components is not considered to be justified.

Associated Considerations

- The feasibility of making and breaking liquid metal lines in 0.5-in. piping was demonstrated under ideal laboratory conditions. This has not been demonstrated for larger sizes nor under field conditions for any size piping. The laboratory tests were not conducted under conditions simulating those that would be required for handling radioactive equipment. Such would be the case under field conditions.
- Liquid metal leakage during makeup or disassembly of large joints could result in serious injury to the operator. Loss of coolant inventory through a large leak (10 lb or more) would make the plant inoperative until the inventory were replaced. The likelihood of significant coolant losses is much greater in large lines and spool pieces than it is from smaller sized pieces.
- Air in-leakage into the plant could result in catastrophic oxidation to the core.
- Valves have not been developed to the point that they can be considered reliable to the extent of having zero leakage under repetitive cycles of opening and closing over the temperature ranges involved in the MCR.
- The vacuum and purging equipment plus cleaning solutions required to clean the spool pieces and valves after each breaking of the system would add significantly to the size and weight of the overall plant. Complexity of the plant would be increased and reliability would be reduced.

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- Special training programs would be required for all operating personnel to acquaint them with the proper procedures for making and breaking liquid metal lines.
- Failure of any of the interconnecting valves to close completely would prevent disassembly of the off-skid component or subsystem from the PGP, thereby creating transportation problems. Small lines could be separated by freezing the lines with the valves closed and carefully cutting the spool piece. If the spool pieces were designed to be reusable, the exposed ends would have to be sealed as quickly as possible to prevent gross oxidation of the coolant. This procedure could be used on small lines in an emergency, whereas on larger lines, the more time-consuming conventional techniques of bagging, cutting, and welding in an inert atmosphere would be required.

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Alternate Pump—Electromagnetic

As an alternate to the 3-Mw_e MCR gas-filled pump, use of an electromagnetic (EM) pump was investigated for application to the MCR system.

The EM pump employs several design features which are desirable for a liquid metal system. There are no moving parts within the liquid metal circuit. This eliminates dynamic seals and associated problems. Since there is no free-surface gas-to-liquid interface in the EM pump, the unit provides installational flexibility—i. e., capability of being oriented in diverse attitudes.

For the purpose of these studies, the effect of the alternate primary pump(s) on the weight, cost, and net electrical output of the overall MCR system was evaluated in terms of pump weight, pump cost, and pump power requirements. For simplification, the pump study described herein was based on the alternate pump delivering the same coolant flow rate, at the same specific pump head as the 3-Mw_e pump and for an identical set of system parameters. For these conditions, the alternate pump weight, cost, and power requirements were determined and compared with those for the 3-Mw_e pump.

CONCLUSIONS AND RECOMMENDATIONS

The EM pump is considerably heavier and larger than the gas-filled pump. The power requirement exceeds that of the 3-Mw_e pump. Although the motor-driven EM pump costs are greater than those of the gas-filled unit, the turbine-driven EM pump costs are less. However, even for the turbine-driven EM pump, the excessive power requirements reflect a cost penalty to the overall plant.

It is recommended that this type of primary pump no longer be considered for use in the MCR.

The findings of the investigation of the effect on system weight and cost as a result of replacing the 3-Mw_e gas-filled pump with the alternate EM pump driven by an electrical motor or turbine drive are illustrated in Figures 4-19 and 4-20. For a given system net electrical output, the system including the electrically driven EM pump weighs approximately 5.4% more than the 3-Mw_e design. The turbine-driven EM pump weight exceeds the weight of the 3-Mw_e design by approximately 4.2%. The costs of the system incorporating the motor-driven and turbine-driven EM pumps exceed the 3-Mw_e design costs by 4% and 1.5%, respectively. Although a weight difference in pump components does exist, the major effect on system weight is a result of the increased pumping power required with the EM pump.

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Figure 4-19.
EM electric- and turbine-driven pump vs
gas-filled pump.

Figure 4-20.
EM electric- and turbine-driven pump vs
gas-filled pump.

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Comparison of the alternate pump power requirements with those of the 3-Mw_e pump is illustrated in Figure 4-21. The increased power required by the EM pump is incurred as a result of the extremely low pump efficiencies. The turbine pump requires less power than the motor-driven EM pump because it eliminates the inefficiencies of the electrical generator and motor. Figure 4-22 illustrates the comparison of alternate pump weights with the 3-Mw_e pump. For all flow rates investigated, the EM pumps weighed considerably more than the 3-Mw_e design. Cost comparisons between the alternate pumps and the 3-Mw_e pump are illustrated in Figure 4-23. The motor-driven EM pump is more expensive than the 3-Mw_e design, while the turbine-driven EM pump costs less.

Figure 4-21. Pump power comparison.

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Figure 4-22. Weight comparison.

ANALYSIS

The electromagnetic pump operates on the induction motor principle wherein a force is exerted on a conductor as a result of motion of a transverse magnetic field. The magnetic field motion is applied through the mechanical rotation of a helically wound rotor. Since the liquid metal is contained in a stainless steel annulus within the flux gap, moving parts are not exposed to liquid metal. This feature permits the capability of use of a variety of rotor drive methods. Further, since the flow rate can be controlled by the current of the field windings, variable

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Figure 4-23. Cost comparison.

flow can be achieved at constant drive speed. Consideration was given to the helical rotor EM pump driven by an electrical motor and by a turbine. The turbine drive was designed to use air bled from the liquid metal-to-air heat exchanger outlet.

The motor-driven EM pump study is based on use of commercially available 60-cps motors. Considerable weight savings but not in excess of those possible with the turbine drive, may be made by the use of reduction gears with 400-cycle drives; however, this combination is less attractive because of oil seal and auxiliary cooling requirements.

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Liquid metal velocities in the pump annulus were limited to 30 ft/sec, which is consistent with previous helical rotor pump operational experience. Some weight reduction might be possible by permitting an increase in this velocity; however, data would be required to ensure that cavitation and erosion would not be prohibitive.

The rotor winding current density was limited to 4000 amp/in.² to avoid the need for large, motor-driven cooling fans and possible design complications associated with the rotor cooling system.

The pump study is based on design of the annulus walls as self-supporting cylinders. Such a design is optimum for the smaller units; however, weight reductions may be possible in the larger pumps through the use of internal stiffening of the annulus walls.

To provide satisfactory flow coastdown characteristics, a shaft-mounted alternator was included to supply power to the field windings during the speed decay period.

EM Pump Model

The data used to develop the weight and cost of the alternate pump used in this component comparison were the result of a separate parametric study performed to preoptimize the alternate pump based on such parameters as flow rate, head, temperature, and design pressure. The basis for comparison of the EM pump with the gas-filled pump was that each pump would develop the same flow rate at the same pump head. The alternate pump parameters are developed as described in the following subsections.

EM Pump—Electrical Motor Drive

The primary variables employed as input data for the pump calculations are:

- Flow— $\dot{m}$ (lb/sec)
- System pressure drop— ΔP (psi)
- Pump inlet temperature— T (°F)
- Fluid density— ρ_{LM} (lb/ft³)

1. Compute pump capacity, gpm

$$\text{gpm} = 448.86 \frac{\dot{m}}{\rho_{LM}}$$

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2. Using Figure 4-24, determine pump efficiency, η_p

3. Compute shaft horsepower, Q_p (hp)

$$Q_p = \frac{Q \times \Delta P}{1714 \eta_p} \quad (\text{hp})$$

4. Using Figure 4-25, determine alternator power, Q_a (hp)

5. Compute rated drive power, Q_r (hp)

$$Q_r = Q_p + Q_a \quad (\text{hp})$$

6. Assume motor efficiency of 0.90 and compute motor power, Q_m (kw)

$$Q_m = \frac{0.746 Q_p}{0.90} = 0.50 Q_p \quad (\text{kw})$$

7. Assume power factor of 0.84 and compute apparent motor power, Q_{app} (kva)

$$Q_{app} = \frac{0.50 Q_p}{0.84} = 0.597 Q_p \quad (\text{kva})$$

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Figure 4-24. Pump efficiency.

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Figure 4-25. Alternator power versus shaft horsepower.

8. Using Figures 4-26 and 4-27, determine pump weight, W_{t_p} (lb)
9. Using Figure 4-28, determine alternator weight, W_{t_a} (lb)
Note step function in curve. This step is primarily due to a stepped speed change.
10. Using Figure 4-29, determine motor weight, W_{t_m} (lb)
Note step function in curve.
11. Using Figure 4-30, determine motor controller weight, W_c (lb)

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12. Compute total weight, W_{tT} (lb)

$$W_{tT} = W_{tp} + W_{ta} + W_{tm} + W_{tc} \text{ (lb)}$$

13. Using Figure 4-31, determine cost, C_p (2)

- a. For 2 units/year multiply by 2.476
- b. For 20 units/year multiply (a) by 0.85
- c. For 200 units/year multiply (a) by 0.70

EM Pump—Turbine Drive

The turbine drive was designed to operate by air bled from the liquid metal-to-air heat exchanger outlet. The drive converts the engine air into shaft horsepower as efficiently as the electrical drive. Therefore, the air turbine drive does not degrade the performance of the plant.

Items 1 through 5 are the same as given for the electrical motor drive.

6. Using Figure 4-32, determine pump weight, W_{tp} (lb)

7. Using Figure 4-33, determine alternator weight, W_{ta} (lb)

8. Using Figure 4-34, determine turbine drive weight, W_{tur} (lb)

9. Compute rated drive power, Q_r (hp)

$$Q_r = Q_p + Q_a \text{ (hp)}$$

Note: Gross engine power would be decreased by this amount.

10. Using Figures 4-26 and 4-27, determine cost of turbine drive, C_{tur} (2)

11. Using Figure 4-20, determine cost of pump, C_p (2)

12. Compute total cost of pump C_T (2)

$$C_T (2) = [C_{tur} (2) + C_p (2)] \cdot 2.476$$

For 20 units/year multiply C_T (2) by 0.85

For 200 units/year multiply C_T (2) by 0.70

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Figure 4-26. Electromagnetic pump weight.

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Figure 4-27. Electromagnetic pump weight.

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Figure 4-28. Alternator weight versus shaft horsepower.

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Figure 4-29. Motor weight versus motor horsepower.

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Figure 4-30. Controller weight versus motor horsepower.

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Figure 4-31. EM pump cost versus total weight.

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Figure 4-32. Total turbine drive weight versus shaft horsepower.

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Figure 4-33. Cost of air turbine drive.

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Figure 4-34. Pump cost.

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MCR Alternate Heat Exchanger Concepts

Investigations were conducted of alternate liquid metal-to-air heat exchanger concepts which might present a more efficient heat transfer surface than the tube-and-fin design used for the 3-Mw_e MCR (reference concept). Of primary importance in studying any advanced concept is the reduction in size, weight, cost, and pressure drop over that of the reference concept. Of attendant importance is the reliability of an advanced concept where operating experiences are virtually nil. This report briefly discusses the concept of a modular plate-type unit and its application as a liquid metal heat exchanger for the MCR

CONCLUSIONS AND RECOMMENDATIONS

The modular plate-fin concept has definite advantages over the tubular fin heat exchanger. Because of the compactness of the heat transfer surfaces, a smaller and lighter heat exchanger may be achieved for the same heat rating. When comparison is made with the 3-Mw_e MCR plant, reductions of up to 40% in weight may be realized. For the plate-type unit, air pressure drop through the core is slightly lower than that for the tube fin. Of more significance, liquid metal pressure drop in the plate-type unit is reduced by a factor of 10 when compared to the 3-Mw_e plant. Because of the plate-type unit's compactness, savings of as much as 50% in overall heat exchanger volume may be realized when compared with the design used in the 3-Mw_e plant.

Two problems that must be resolved are associated with a plate-type unit. One problem is that adequate manufacturing processes must be developed for bonding the fins to the plates. The primary problem inherent in plate-type units is the thermal expansion associated with high temperature operation. This problem is partially solved by the modular concept which allows differential thermal expansion.

A further study of this concept is recommended to determine its suitability for high temperature liquid metal operation. Of first order importance would be performance of reliability studies to ascertain the ability of the modular plate-type unit to satisfy the specified lifetime requirements. The desirability of conducting further development efforts will hinge on the outcome of these reliability studies.

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ANALYSIS

Figure 4-35 illustrates the concept proposed for a modular plate-type liquid metal-to-air heat exchanger. Analysis of the experiences of other researchers with plate-type heat exchangers has pointed out the serious problems associated with thermal expansions. The modular concept considered here alleviates this problem by allowing thermal expansion to occur within the core, between modules. The plate module is constructed of a flat tube with finned surfaces brazed or welded inside and on each outer surface. The ends of the flat tube are formed to receive the round expansion tube. A thin metal sheet may be brazed to one outer fin surface to complete the module assembly. To assemble a heat exchanger core, a number of these modules are stacked and joined together. The tubes joining the core surface to the header act as expansion loops to account for differential thermal expansions within the core. Table 4-X compares the present tubular fin heat exchanger concept designed for the 3-Mw_e MCR with the modular plate design.

Table 4-X.

Comparison of modular plate design with tubular fin design heat exchangers.

	<u>Tubular fin</u>	<u>Plate</u>
Shell OD—in.	41	32
Shell length—in.	54.75	42
Shell volume—in. ³	63,209	29,493
Total weight—lb	2495	1450
K-core ΔP —psi	5.61	0.5
Air-core ΔP —psi	2.697	2.3
Tube OD—in.	0.375	0.125 height (plate)
Tube wall—in.	0.035	0.020
Fin thickness—in.	0.008	0.008
Fin material	Type "A" Ni	Type "A" Ni
Tube material	316 SS	316 SS

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A vertical strip of ten small, square images showing the progression of a handwritten digit '4' from a noisy, pixelated state to a clean, clear state. The images are arranged vertically, with the most degraded image at the top and the most refined at the bottom.

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V. SHIELDING

This section reports on the following alternate component support studies:

- Use of Boral in the MCR Shield
- Interchange of Inner LiH and Lead Layers
- Air and Ground Scattering
- The Use of a Boronated Plastic Outer Neutron Shield
- The Use of Natural Boron in Place of Enriched Boron
- Cast LiH

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Use of Boral in the MCR Shield

A substantial fraction of the operating and shutdown gamma ray dose rate external to the MCR shield is produced as the result of neutron captures in the lead and stainless steel regions of the shield. One method for the reduction of gamma ray producing reactions is the inclusion of layers of boron or borated material at appropriate locations in the shield. Boron has a very high neutron capture cross section for the production of an alpha particle and a nonpenetrating gamma ray. In addition, the residual nuclei are not strongly radioactive. An obvious method for including boron in the shield is to use layers of boral—a mixture of boron carbide and aluminum clad in aluminum. Preliminary calculations were made to investigate the effects on the secondary gamma dose rate of including boral layers just outside and adjacent to the two lead shield regions of the radial shield.

CONCLUSIONS AND RECOMMENDATIONS

The preliminary results, which follow in greater detail, show that the boral has a significant effect in reducing the operating gamma ray dose rate outside the shield. For a shield using stainless steel cladding for the outer LiH, the dose rates at 450 ft are 12.3 mrem/hr without boral and 9.6 mrem/hr with boral. Thus, the boral affords a 22% reduction in gamma dose rate. If the outer SS can is not present, the boral reduces the gamma dose rate from 10.1 to 7.8 mrem/hr, or by 23%.

On the basis of these preliminary results, it is recommended that further studies be made to determine in greater detail the effects of boral on secondary gamma ray dose rates from and weights of MCR shields. In addition, calculations should also be made to determine the effects on the shutdown dose rate from shield activation and on nuclear heating, since neutron capture in boron releases only 2.8 mev compared to the 4.8 mev produced by a neutron capture in a lithium-6 nucleus. These studies should be made for a range of power levels and should be conducted after a detailed examination of the analysis of the shield critical experiments (Task 1119) to ensure that the secondary gamma ray source terms and ratios of inelastic gamma rays to those from neutron capture are accurate in all the MCR shield regions of interest.

It is also recommended that studies be made of the incorporation of boral into the borated water and borated plastic alternate shield systems since the boral should be even more effective in the less poisoned shields than in the LiH environment.

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Figure 5-1. Boral MCR shield configuration.

ANALYSIS

A calculation was performed to determine the effects on the secondary γ -dose rate of inserting layers of boral into the MCR "1070" shield.* A 0.25-in. boral sheet was inserted at the outside of each of the two Pb layers in the removable part of the shield. The configuration considered is given in Figure 5-1.

Effect of a Heterogeneous Arrangement of B_4C in Boral

Boral is a heterogeneous mixture of B_4C and Al sandwiched between Al layers. Experimental results for thermal neutron penetration through boral¹¹ were compared with calculations, which assumed a homogeneous B_4C -Al mixture. For 0.5-in. boral slabs, the experiments gave transmission factors higher than the calculated results. Subsequent calculations which attempted to take the "chunk" arrangement of B_4C into account gave fairly good agreement with the experiment.

The calculation of secondary gamma ray sources was done with UNC Code A-2 (SANE-2). Since this code handles homogeneous compositions of materials, it was necessary to adjust the boral composition to obtain transmission factors which would be comparable to those obtained experimentally. This was done by reducing the B_4C volume fraction in the mixture.

*Results of the UNC A-1, A-2 Monte Carlo Analysis for the 1070 Shield, United Nuclear Corporation, MCR-3146, 13 March 1965.

This shield is related to, but not identical with, the 3-Mw_e MCR design shield, as discussed in MCR-3146. Among other differences, the 3-Mw_e MCR shield employs aluminum instead of stainless steel canning for the outer LiH layer. The stainless steel canning was used here to determine the effect of boral in suppressing capture gamma rays from a stainless steel can if used.

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A typical B_4C volume fraction in unclad boral was given¹¹ as 0.40. Adding the Al cladding reduces this to 0.26. An "effective" volume fraction 25% lower (19.5%) was finally selected and used for these calculations.

The concentrations are then

$$\begin{aligned} B_4C &= 0.535 \times 10^{22} \text{ molecules/cc} \\ Al &= 4.85 \times 10^{22} \text{ atoms/cc} \end{aligned}$$

Input to UNC Code A-2

Except for the insertion of the two boral layers, the UNC A-2 input was identical with the previous SANE-2 problem used in the evaluation of the 3-Mw_e MCR design shield. The addition of the boral sheets served to increase the outer radius of the shield by 0.5 in.

Analysis of UNC A-2 (SANE) Output

It was assumed that the 0.25-in. boral layers do not affect the fast neutron fluxes in the shield. A comparison of the fast neutron doses at various positions in shields with and without boral proved this to be a valid assumption. This means that the gammas from inelastic scattering are unaffected and only the capture gammas need be compared.

Summing over the six energy groups used in the calculation gives the total dose from each shield layer.

Detailed Results

Table 5-I compares the external dose rates from each shield layer as a function of energy. The quantities ΣD_0 and ΣD_B represent the gamma dose rate (summed over all flux regions in a given shield layer) for the "no boral" and "boral" shields, respectively.

The total dose rate, from all layers, for the "no boral" shield is 12.3 mrem/hr while for the "boral" shield it is 9.6 mrem/hr. For systems such as the 3-Mw_e system which do not employ stainless steel cladding in the outer shield, the dose rate is reduced from 10.1 mrem/hr to 7.8 mrem/hr by the boral. From these results, it can be concluded that the addition of boral sheets to the shield results in a significant reduction in the secondary gamma dose.

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Table 5-I.
Gamma dose rates* (mrem/hr) at 450 ft.

Shield layer	Dose	Energy group (Mev)						Total	Total (no outer** SS)
		1	2	3	4	5	6		
Tungsten reflector	D _O	0	0	0.268	0.195	0.224	0.117	0.80	
	D _B	0	0	0.268	0.195	0.224	0.117	0.80	
Stainless steel reflector	D _O	0.060	0	0.031	0	0	0.048	0.14	
	D _B	0.060	0	0.031	0	0	0.048	0.14	
Pressure vessel	D _O	0	0	0.030	0	0.012	0.036	0.08	
	D _B	0	0	0.030	0	0.012	0.036	0.08	
Inner Pb	D _O	0	0.547	0.425	0.014	0.063	1.71	2.76	
	D _B	0	0.547	0.425	0.014	0.063	0.68	1.73	
Stainless steel cladding	D _O	0.069	0.601	0.411	0.448	0.254	1.72	3.50	
	D _B	0.063	0.461	0.321	0.335	0.186	1.23	2.60	
Inner LiH	D _O	0	0.522	0	0	0	0	0.52	
	D _B	0	0.460	0	0	0	0	0.46	
Outer Pb	D _O	0.663	0.418	0.238	0.110	0.039	0.377	1.85	
	D _B	0.663	0.418	0.238	0.110	0.039	0.302	1.77	
Stainless steel cladding	D _O	0.676	0.307	0.361	0.182	0.074	0.571	2.17	
	D _B	0.646	0.251	0.325	0.166	0.054	0.384	1.83	
Outer LiH	D _O	0	0.503	0	0	0	0	0.50	
	D _B	0	0.335	0	0	0	0	0.34	
Complete shield	D _O	1.47	2.90	1.76	0.95	0.67	4.58	12.3	10.1
	D _B	1.43	2.47	1.64	0.82	0.58	2.80	9.75	7.92

*Ignoring air and ground attenuation and scattering.

**Approximated by deleting contributions from outer stainless steel cladding and leaving all other contributions unchanged.

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It should be noted that two effects were neglected in these calculations. The first is the reduction in gamma dose due to the shielding afforded by the boral, and the second is the increase in dose due to neutron capture in the Al. However, these are second order effects which tend to cancel each other and should not affect the final conclusion.

In addition, no attempt was made to determine the effect of the boral on the activation rates in the fixed shield though it seems probable that the boral would reduce the shutdown dose rate and possibly allow a reduction in the weight of the fixed shield. No attempt was made to compute weight changes associated with these dose rates.

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Interchange of Inner LiH and Lead Layers

Early studies of the design of the radial fixed lead shielding assumed that the shutdown gamma ray dose rate originated only in the regions internal to the lead. This implied that a minimum weight fixed shield would result if the lead were placed immediately adjacent to the pressure vessel because the required thickness was determined solely by the source strength inside the vessel and was independent of the position of the lead. Subsequent investigation indicated that a significant fraction of the shutdown dose rate originated in the lead itself (impurity activation) and in the outer clad of the lead. In addition, it was clear that a more efficient operating shield would result from having the inner lead further from the core to reduce secondary gamma production in this and adjacent regions. A brief study was conducted to evaluate the ramifications of interchanging a 3-in. region of the inner LiH with the inner lead.

CONCLUSIONS AND RECOMMENDATIONS

Preliminary calculations indicate that the placement of a thin LiH layer between the pressure vessel and inner lead shield may lead to reductions in overall shield weight, cost, and size, as the result of reduced secondary gamma ray production and activation, increased self shielding, and an improvement in the fast neutron attenuation of the rearranged shield layers. Radiation damage and changes in shield nuclear heating need further investigation. If the results are favorable, more detailed calculations should be made for a range of power levels and mobility requirements to determine specific package changes that would result. The addition of boral layers should also be integrated into this study.

ANALYSIS

Figure 5-2 illustrates the two configurations studied. Each has the same total thickness of lead and LiH. For the alternate system, 3 in. of the inner LiH were shifted to between the vessel and inner lead. Considering the fixed lead shield and the 3-in. layer of the inner neutron shield as annular cylinders, the combined mass of the two regions when exchanged increases by approximately 20%.

Figure 5-3 presents the group-averaged neutron fluxes in the high energy range as calculated by DTK (one-dimensional transport code, S_4 approximation) for the two configurations studied. A definite reduction occurs in the fast fluxes in the alternate removable shield. Figures 5-4 and 5-5 present the relative total neutron dose rates in the two configurations studied.

Figure 5-2. Spherical geometry reactor/shell configuration.

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Figure 5-3. Group averaged flux versus radial distance from core center.

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Figure 5-4. Neutron dose rate responses versus radial distance from core center.

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Figure 5-5. Neutron dose rate responses versus radial distance from core center.

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Figure 5-6 presents Li^6 , H, and Pb capture rates in the two configurations studied. The Li^6 captures contribute most of the shield heating while the other captures produce secondary gamma rays. Note particularly the capture rate responses calculated in the fixed lead shield for the two configurations. The total number of lead captures in this region is reduced by about 28% utilizing the alternate configuration. The external gamma ray dose rate from lead captures would be decreased considerably more than 28% because a greater percentage of the captures would now occur in the inner portion of the lead which is more self-shielded than the outer layer.

Lithium-6 capture rates increase 19% in the split inner neutron shield of the alternate configurations. This is an indication of the increase in the heating rates in the LiH. However, 88.5% of the captures would now occur in the LiH layer adjacent to the pressure vessel. The localization of the resultant heating in this thin layer could simplify the shield cooling problem.

Gamma rays emitted by activated aluminum (used as cladding for the inner lead layer) give a significant fraction of the shutdown dose rate in the vicinity of the 3-Mw_e MCR system fixed shield assembly. For larger cores and higher power levels the importance of this source increases. This activation is entirely due to the (n,α) reaction produced by very high energy neutrons ($E > 8 \text{ Mev}$). Interposing the 3-in. layer of LiH between the pressure vessel and the lead reduces the activation of the outer aluminum cladding by a factor of five. There is also a reduction in activation of both lead impurities and the pressure vessel. These changes should make it possible to reduce the thickness of the inner lead.

During plant operation, as a result of the shift in neutron shielding material, the secondary gamma ray production rates in the stainless steel cladding of the inner LiH would be reduced, and there would be more gamma ray shielding between the sites of secondary gamma ray production and the outside of the shield. These factors should serve to increase the overall effectiveness of the operating shield and make it possible to reduce its thickness and weight.

Further reductions might also result from the incorporation of boral layers at appropriate locations within the shield. Consideration should also be given to the effects of the boral layers on the neutron instrumentation, shield temperature distribution, etc.

Weight Changes

No net increase in overall shield weight is expected as a result of placing a 3-in. layer of lithium hydride between the pressure vessel and fixed lead shield. The reactor plus fixed shield package could be 6 in. greater in diameter and about 1300-1400 lb heavier if no reduction

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Figure 5-6. Capture rate responses versus radial distance from core center.

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were made in the thickness of the inner lead layer. However, the removable shield package would be about 4 in. less in outside diameter because of its improved neutron shielding characteristics and would weigh over 1000 lb less. Therefore, the weight increase in the fixed lead shield as a result of its greater distance from the core could be compensated by removing a portion of the outer neutron shield. From Figure 5-5, it can be seen that about 2 in. can be removed from the alternate outer neutron shield to achieve the same neutron dose rate at the shield surface.

In practice, it should be possible to reduce the thickness of the two lead shields due to the decreased production of secondary gamma rays by neutron capture and inelastic scattering in the lead regions. In addition, the thickness of the inner lead region can be reduced because of the decreased activation of both the impurities in the lead (from resonance energy neutrons) and the aluminum cladding (neutrons above several Mev). Determination of the total amount of lead that can be removed as a result of the reduced secondary gamma ray production and activation will require further study.

The weight of lead removed should give the approximate overall weight saving; the shield outside diameter can be reduced by about 4 in., and the cost reduction should be approximately proportional to the weight of LiH removed.

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Air and Ground Scattering

Significant savings in shield weight, size, and cost may be achieved by providing for a variation in the radiation emission intensity over the surface of the MCR shield. Emission from surfaces that do not radiate directly to the control cab can be of greater intensity and, consequently, the shielding in these directions can be less than in the direction of the dose point. The extent to which the thickness of the shield can be reduced is limited by air and ground scattering of emitted radiation to the dose point.

Variation of the effective shield in different directions (shield shaping) can be incorporated directly into the design of the shield. Variation of the effective shield in certain directions can also be accomplished by the use of auxiliary shielding placed either near the reactor or control cab, perhaps employing natural materials indigenous to the reactor operating site. However, to effectively evaluate the application of shield shaping, either by design of the shield itself or through the application of expedient shielding, it is necessary to calculate the radiation exposure at the dose point resulting from air and ground scattering of radiation emitted from those surfaces of the shield having no direct path to the dose point.

CONCLUSIONS AND RECOMMENDATIONS

To provide a basis for calculating the dose rate from a shaped shield, an importance function, $I(d, \omega)$, defined as the air and ground scattered dose rate for a source particle emitted at angle θ to the vertical axis ($\omega = \cos^{-1}\theta$) and at a distance, d , from the source was determined for neutrons and for gamma rays. The geometry used was for reactor and dose points, each a few feet above an air-ground interface. Cylindrical symmetry about a vertical axis through the reactor source point was assumed.

A Monte-Carlo method employing UNC code A-15 (UNC-SAM) with the flux-at-a-point option was used to make calculations of the preliminary air-ground scattering importance function for five values of the emission angle θ from 0 to 180° and for distances of 100, 450, and 1000 ft. The importance function was calculated for both neutrons and gamma rays, using representative MCR shield neutron and gamma ray leakage spectra. No attempt was made to calculate the MCR dose field in the vertical plane to determine the actual air and ground contributions to the operating dose rate. These patterns should be calculated and combined with the importance functions prior to further investigations of shield shaping and expedient shielding, since the importance functions for neutrons are not strongly peaked in the direction toward the dose point.

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ANALYSIS

Background

A review of the literature pertaining to this subject was made to ascertain the need for further analysis.^{12,13,14,15} In summary, it was determined that solutions to the problem available in the literature were either not sufficiently general with respect to the MCR problem application or did not cover the required range for the variables considered. Much of the work is limited to distances of less than 100 ft. Consequently, the calculations described herein were performed to have a reasonably general basis for undertaking MCR studies related to expedient shielding and shield shaping.

Importance Function

The general form of the scattered dose rate may be expressed in terms of an importance function, $\bar{I}(\bar{d}, \bar{r}, \bar{\Omega})$, which represents the scattered dose rate per escaping particle. The importance function is expressed in terms of three reactor variables as follows.

$\bar{r}$ is the position vector of the point at which the particle escapes from the shield surface.

$\bar{\Omega}$ is the direction vector of the particle velocity.

$\bar{d}$ is the distance vector of the dose point from the point of particle escape.

This geometry is illustrated in Figure 5-7.

P = point of particle escape, defined by position vector $\bar{r}$

$\bar{\Omega}$ = direction vector of escaping particle velocity. If the particle velocity makes angles $\theta_x, \theta_y, \theta_z$ with each axis, then direction cosines of the direction vector are

$$\bar{\Omega}_x = \cos \theta_x$$

$$\bar{\Omega}_y = \cos \theta_y$$

$$\bar{\Omega}_z = \cos \theta_z$$

D = detector point, defined by distance vector $\bar{d}$ relative to P.

Figure 5-7. Geometry for the general case.

The unscattered dose, determined analytically, is added to the scattered dose obtained from the appropriate importance functions to yield the total dose at the detector.

Once the importance function is known for both neutrons and gamma rays, the dose at a distant point from a shield of any desired shape can be determined, provided the leakage radiation at the surface of the shield is known. By analytical or trial-and-error techniques, it is possible to determine an optimum shield shape to yield minimum shield weight for a given dose rate.

Geometrical Simplifications

Several features of the assumed MCR operating conditions simplify the form of the importance function:

- The separation distances between the MCR assembly and the operating cab (henceforth referred to as the source and the detector, respectively) are considered to have a possible range of 100 to 1000 ft. Since these separation distances are very large in comparison with the physical dimensions of the source and detector, the geometry is effectively a point-to-point configuration (see Figure 5-8).
- For this study, the exclusion area, under operating conditions, is considered to be centered about the source and is defined by a circle of exclusion of radius d . That is, there is no preferential source-detector direction along the ground and symmetry about the vertical axis may be assumed.

Figure 5-8. Simplified geometry.

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The results of the preceding simplifications are:

- The assumption of a point source removes the $\bar{r}$ dependency of the importance function.
- The azimuthal symmetry about the vertical Y axis makes it possible to replace the Ω (generalized directional cosine) dependency with a simpler ω dependency, where ω is defined as the cosine of the angle θ made by the emitted particle with the Y-axis (Figure 5-8).

In addition, the dependency on $\bar{d}$ (the vector displacement along the ground) is replaced by dependency on a simpler scalar displacement, d , the magnitude of the separation distance from source to detector. Under these assumptions, the importance function (scattered dose per source particle) becomes a function of d and ω only.

Method

The determination of the importance functions for both neutrons and gamma rays was performed with the UNC A-15 (UNC-SAM) Monte Carlo code, using the flux-at-a-point option.

The code was used to calculate the energy dependent fluxes at the detector point for a typical MCR leakage spectrum for a number of specific angles and detector distances. The importance function was then calculated by:

$$I(d, \omega) = \int_{\text{All } E} \phi(d, \omega, E) R(E) dE \dots \dots \quad (5-1)$$

where $R(E)$ = the response function to convert flux to dose and $\phi(d, \omega, E)$ is the flux at distance d , per unit energy, per source particle emitted at an angle $\cos^{-1} \omega$ to the vertical axis.

For the geometrical configuration under consideration, unreasonable variances would be associated with Monte Carlo "track length" calculations of flux since the solid angle subtended by the detector is vanishingly small. Hence, it was necessary to use the "flux-at-a-point" scheme.¹⁶

This option of the UNC A-15 code was adapted for a source specified as a conical element, centered about the Y-axis for any given value of ω (from -1.0 to +1.0), with a spread $\Delta\omega$ about ω (see Figure 5-8). The value of $\Delta\omega$ was taken as a very small number (0.0001), thus producing a conical shell source.

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Calculations and Results

Neutron and gamma ray importance functions were determined by means of calculations for the following five angular directions of the conical shell source:

ω	-1.0	-0.5	+0.15	+0.5	+1.0
θ	180°	120°	81.4°	60°	0°

and for detector distances of 100, 450, and 1000 ft.

Source and detector heights were 5 and 6 ft, respectively. The leakage spectra were those calculated for a typical MCR shield. The assumed dry ground and air compositions used in the calculations are given in References 17 and 18.

The calculated values of $I(d, \omega)$ were used to obtain plots of I vs ω with d as the parameter, and I vs d with ω as the parameter. The total computer running time on the CDC-1604-A was approximately 11 hr for an average standard deviation of less than 20%. The results are presented as Figures 5-9, 5-10, 5-11, and 5-12. Included on the plots, for all points, are the calculated standard deviations.

Evaluation

Several results are apparent from the importance function curves:

- When, as in the present study, the neutron importance function is averaged over all azimuthal angles, the ratio

$$I(\omega)_{\max}/I(\omega)_{\min}$$

is, for all displacement distances, less than a factor of 10. This result implies that if there are significant anisotropies in the MCR neutron shield, they will contribute to the operating dose rate by air and/or ground scattering.

- The presence of the dry ground reduces the neutron dose at the detector from that which would have been obtained had the configuration been one of an infinite air medium. This is seen by noting that over the entire range of considered source-detector displacements the neutron importance function for neutrons heading into the air ($\omega \neq 0$) is higher than the

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corresponding importance function for neutrons heading into the ground ($\omega < 0$). Specifically, from Figures 5-9 and 5-10:

$$\begin{aligned} I(\omega = +1.0) &> I(\omega = -1.0) \\ I(\omega = +0.5) &< I(\omega = -0.5) \end{aligned} \quad \text{for all values of } d.$$

- The angular dependency of the importance function is more pronounced for gamma rays than for neutrons. From Figures 5-11 and 5-12 it is seen that

$$I(\omega)_{\max} / I(\omega)_{\min}$$

for 100, 450, and 1000 ft is approximately 10, 20, and 150, respectively. Thus, the angular dependency becomes more pronounced with increasing source-detector separation distance.

It is also seen that for gamma rays, as in the case of neutrons, the presence of the ground reduces the dose from that which would have been obtained for an infinite air geometry.

- More calculations will be required to better define the curves of importance functions versus angle. The lines on the graphs are not final, and more work is being carried out at United Nuclear Corporation for subsequent publication.

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Figure 5-9. Neutron importance function - preliminary.

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Figure 5-10. Neutron importance function – preliminary.

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Figure 5-11. Gamma ray importance function – preliminary.

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Figure 5-12. Gamma ray importance function – preliminary.

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The Use of a Boronated Plastic Outer Neutron Shield

At the completion of the 3-Mw_e MCR design effort it was clear that a substantial fraction of the cost of the MCR was the direct result of using LiH in the outer neutron shield portion of the removable shield. Its use was dictated by the fact that it is the best neutron shield available per unit weight and that the initial design criteria for the MCR required that the removable shield be a minimum (or close to minimum) weight and minimum size system. Cost of the shield was not at that time a controlling factor, particularly if reducing the cost resulted in a larger, heavier system. Since that time, it appears that for other mobility constraints, weight and size limitations on the shield will be less restrictive while cost considerations will be of increased importance.

One approach to reducing the cost of the removable shield would be to use borated water in place of all the removable shield LiH. Such a system was evaluated parametrically, and the results are given in detail in GMAD 3078-20 (Allison EDR 4186, Volume III). The use of borated water reduces shield cost considerably but requires the addition of purification, boration, heating, and filling systems, and the lengthening of the shield skid. It also increases the possibility of a liquid metal-water reaction and vulnerability to small arms fire.

An alternate approach would be to retain LiH for the inner layer but replace the outer neutron shield layer (which represents the largest part of the LiH weight and cost) with a low-cost solid material which would not have to withstand the very high temperatures and radiation fluxes present at the inner layer. An obvious choice would be a hydrogenous plastic. A preliminary survey was conducted of potentially low cost, relatively radiation-resistant, good neutron shield materials into which boron could be incorporated. A brief study was made to determine the effects on dose rates and shield cost, weight, and size of replacing the outer LiH with borated plastic.

CONCLUSIONS AND RECOMMENDATIONS

A supplementary concept lithium hydride-lead-boronated polystyrene-boronated polyethylene removable shield was analytically investigated to determine a minimum weight configuration for the requirements of the 3-Mw_e MCR design. This brief analysis and shield synthesis resulted in a removable shield with a weight of 35,300 lb and an outside diameter of 115 in. For the 3-Mw_e design the comparable values are 27,400 lb and 102 in. (a compromise between minimum weight and minimum size). Radiation damage constraints on the polyethylene significantly increased the minimum weight achievable with this shield concept.

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A minimum weight removable shield configuration for a 20-in. dia core operated at 28.5 Mw and giving the same dose levels was also determined. The weight of this shield was determined to be 44,550 lb compared to 29,700 lb for a minimum weight lithium hydride shield and 45,000 lb for a borated water shield.

Preliminary estimates indicate that for the 3-Mw_e MCR system, replacing the outer LiH with a two layer boronated plastic shield would reduce the cost of the removable shield by approximately 70% despite probable penalties in shield weight and size. The substantial cost saving potential therefore justifies more detailed studies over the range of power levels of interest. Other advantages by comparison with borated water shields include the elimination of the need for a number of additional systems and greatly decreased vulnerability to small arms fire.

ANALYSIS

A nuclear analysis of a shield employing a LiH-lead-boronated hydrogenous plastic removable shield was performed with Codes UNC A-2 and A-3 (SANE and SAGE Monte Carlo neutron and gamma ray codes), primarily to obtain shield layer attenuation parameters for use in shield optimization.

Shield Configuration

The analysis was made for an assumed removable shield configuration surrounding the 3-Mw_e MCR design fixed shield configuration.

Thicknesses of the gamma shield and outer neutron shield layers for this calculation model were estimated so as to obtain a shield with approximately the same attenuation characteristics as the 3-Mw_e MCR design. The use of boronated plastic in the outer part of the removable shield does not affect the performance of the fixed shield.

The plastic shield region (all of which lies outside both the outer lead and LiH) consisted of an inner layer of boronated polystyrene (6.98 in. thick) with 0.125 in. of aluminum canning on the inside surface and 0.0625 in. of aluminum on the outside, and a boronated polyethylene layer 21.32 in. thick with 0.0625 in. of aluminum on its inner surface and 0.250 in. of aluminum on the outside surface. Both plastic regions were boronated with 5 w/o natural boron. For these calculations the plastic layers were preceded by 1.33 in. of lead and 8.4 in. of LiH.

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~~CONFIDENTIAL~~Results of Initial Analysis

The total gamma ray and neutron dose rates at 450 ft (in vacuum) are presented in Table 5-II for the assumed shield along with a breakdown of the secondary gamma ray dose rate contributions. The dose rates at 450 ft in air over an air-ground boundary geometry would be approximately 12.8 mrem/hr and 1.1 mrem/hr for gamma rays and neutrons, respectively, assuming an isotropic shield. For the 3-Mw_e MCR shield design, an additional operating dose rate contribution of 2 mrem/hr at the 450 ft dose point results from ground and air scattering of radiation emerging from the bottom shield plug, thus giving a total dose rate estimate for this nonoptimized system of 15.9 mrem/hr.

Table 5-II.

Neutron and gamma ray dose rates for the calculated model (Problem 2030).

	Gamma dose rate in vacuum at 450 ft (mrem/hr)	Neutron dose rate in vacuum at 450 ft (mrem/hr)
Core	0.105	0.82
Reflector I	0.04	—
Reflector II	2.45	—
Reflector III	0.16	—
Pressure vessel and support skirt	0.486	—
Inner lead	10.2	—
Stainless steel canning	6.40	—
Lithium hydride	3.05	—
Outer lead	0.498	—
Boronated polystyrene	0.854	—
Boronated polyethylene	0.135	—
	24.4	0.82
Total dose rate		25.20

Minimum Weight Removable Shields Containing Boronated Plastic

In the past, minimum weight removable shields were determined with the aid of the UNC A-26 (GOBLINS) shield optimization code. In addition to the dose rate requirements, minimum weight removable shields employing boronated hydrogenous plastic must also meet constraints

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of a maximum allowable energy deposition dose rate of 1×10^{12} ergs/gm for polystyrene and 3×10^{10} ergs/gm for polyethylene. Initial calculations with UNC A-26 indicated that this radiation damage constraint would have a controlling effect on the configuration of minimum weight plastic shields, and that UNC A-26 could not directly yield the optimized configurations. It was, therefore, necessary to employ a slower and less sophisticated mathematical method to obtain the minimum weight shields reported in the following pages.

Shield attenuation calculations for the various removable shields employing an outer region consisting of two plastic layers were performed with UNC Code A-28 (DWARFS) which employs the same simplified attenuation model as used in UNC A-26. This model employs two neutron groups—one for neutron dose rate, and one for secondary gamma ray production—and a single gamma ray group. The shield leakage and attenuation parameters for the removable shield were derived from the previously mentioned Monte Carlo calculations. To simplify the analysis a single set of parameters was derived to represent a sequence of boronated polystyrene and boronated polyethylene layers.

Weight Optimization Method

The procedure used to obtain a minimum-weight spherical three-layer shield satisfying the maximum allowable energy deposition dose constraints for the plastic layer was as follows.

Dose rates for shields over the range of the following layer thicknesses were calculated:

Lithium hydride	1 to 20 cm
Lead	1 to 10 cm
Homogenized plastic	60 to 100 cm

By plotting dose rate versus plastic thickness for each separate combination of LiH and Pb, many three-layer shield configurations were defined which gave the desired dose rate of 8 mrem/hr* from the radial shield.

The spherical model weight for these cases was then plotted versus LiH thickness with Pb thickness as the parameter. This plot is shown as Figure 5-13. The results indicate that a minimum weight shield is achieved with less than a 1-cm thick lead layer.

*10 mrem/hr from the entire shield.

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Figure 5-13. Shield weight versus LiH thickness.

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To incorporate the effect of limiting the energy deposition rates, calculations using a similar attenuation model were made to calculate energy deposition in the plastics from both neutrons and gamma rays. Many combinations of LiH and Pb were used to calculate energy deposition dose rate at the lead-plastic interface. The results are plotted in Figure 5-14. The energy deposition rate calculated by Monte Carlo methods was in substantial agreement with that calculated by the simplified model. By superimposing the LiH-Pb combinations for an energy deposition rate of 1×10^{12} ergs/gm from Figure 5-14 on Figure 5-13, the curve labeled 1×10^{12} ergs/gm in Figure 5-13 is obtained. The minimum weight point on this curve is for that shield which satisfies both the 8-mrem/hr dose rate requirement (total operating dose rate of 10 mrem/hr) and the limiting energy deposition dose rates of 1×10^{12} ergs/gm for the boronated polystyrene and 3×10^{10} ergs/gm for the boronated polyethylene.

Determination of the minimum weight shield for the 20-in. dia core, 28.5-Mw case was performed in a similar manner. For this case, the minimum weight shield which meets the limiting energy deposition dose requirement more closely approximates the minimum weight achievable without this requirement.

A minimum weight plastic removable shield configuration for the 3-Mw_e MCR design conditions of core diameter (15 in.), power (15 Mw), after-shutdown dose rate for the fixed shield (500 mrem/hr at 10 ft, 12 hr after shutdown), and operating dose rate (10 mrem/hr at 450 ft) is presented in Table 5-III. This shield meets the requirement of a maximum allowable energy deposition dose of 1×10^{12} ergs/gm in the boronated polyethylene. This shield is significantly heavier than could be achieved if this restriction were removed.

Table 5-III.

Minimum weight plastic outer shield for 3-Mw_e MCR design conditions.*

<u>Region</u>	<u>Thickness (in.)</u>	<u>Outer radius (in.)</u>	<u>Weight (lb)</u>
Fixed shield	—	18.89	—
Lithium hydride	6.97	25.86	1,800
Lead	1.97	27.87	7,190
Boronated polystyrene	6.98	34.81	4,810
Boronated polyethylene	22.67	57.48	21,500
			<u>35,300</u>

*Core diameter = 15.0 in.; power = 15 Mw; after-shutdown dose rate = 500 mrem/hr at 10 ft, 12 hr after shutdown; operating dose rate = 10 mrem/hr at 450 ft.

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Figure 5-14. Energy deposition dose versus LiH thickness.

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A minimum weight plastic removable shield configuration for a 20-in. dia core operated at 28.5 Mw is presented in Table 5-IV.

Table 5-IV.
Minimum weight plastic outer shield for 20-in. dia core operated at 28.5 Mw.*

<u>Region</u>	<u>Thickness (in.)</u>	<u>Outer radius (in.)</u>	<u>Weight (lb)</u>
Fixed shield	—	21.50	—
Lithium hydride	5.32	26.82	1,610
Lead	2.87	29.69	13,020
Boronated polystyrene	6.98	36.67	5,520
Boronated polyethylene	22.94	59.61	24,400
			<u>44,550</u>

*Same dose rates as 3-Mw_e MCR design.

Comparison with Other Removable Shields

Table 5-V presents a comparison of outside diameter and weight with other removable shield concepts for both the 3-Mw_e MCR design conditions and for a 20-in. dia core operated at 28.5 Mw.

Preliminary estimates of the cost of a LiH-Pb-boronated plastic removable shield (the 3-Mw_e MCR design) indicate that the plastic can be obtained for a few dollars/pound and that the cost of such a system would be approximately 70% less than the cost of a comparable LiH-Pb-LiH shield.

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Table 5-V.
Comparison of various removable shields.*

	Outside diameter	Weight**
<u>For 15-in. dia core operated at 15 Mw</u>	<u>(in.)</u>	<u>(lb)</u>
3-Mw _e MCR design (LiH)***	102.74	27,110
Minimum weight lithium hydride	106.48	23,600
Minimum weight with boronated plastic outer layers	114.96	35,300
<u>For 20-in. dia core operated at 28.5 Mw</u>		
Minimum weight lithium hydride	112.5	29,700
Minimum weight with boronated plastic outer layers	119.2	44,550

*For an operating dose rate of 10 mrem/hr at 450 ft and an after-shutdown dose rate of 500 mrem/hr at 10 ft, 12 hr after shutdown with removable shield removed (all shields include Pb gamma shielding).

**Weight includes shield material, structure, and canning. Weight does not include skid, shield cooling system equipment, purification equipment, or any other equipment external to the shield.

***Compromise between minimum weight and minimum size.

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~~CONFIDENTIAL~~Use of Natural Boron in Place of Enriched Boron

Since the cost of B^{10} in the MCR shield is high, a brief study was conducted to determine cost reduction measures that might be taken if the enriched B^{10} were replaced by less expensive natural B.

CONCLUSIONS AND RECOMMENDATIONS

Potential cost savings were evaluated using 1.0 w/o natural B in the internal boron stainless steel shielding of the core and radial reflector and 3 w/o natural B in the TiH_2 bottom shield plug. The natural B replaces nominal 1.0 w/o of B^{10} specified for these components in the 3-Mw_e MCR design. No significant increases in either the operating or after-shutdown dose rates is likely to result from these substitutions.

The combined effect of these substitutions would represent a saving of about \$28,000 in each MCR plant on a basis of one plant per year for five years when compared with the previous cost estimate for the 3-Mw_e MCR design. This previous estimate did not include the cost of the B^{10} which was assumed to be government furnished (GFE). Savings resulting from deleting this B^{10} , however, would amount to an additional \$40,000. These cost savings are summarized in Table 5-VI.

Table 5-VI.
Summary of cost savings by use of natural B for the 3-Mw_e MCR design.

<u>Component costs*</u>	<u>Core B/SS piece (\$)</u>	<u>Reflector B/SS piece (\$)</u>	<u>Bottom plug TiH₂ pieces (\$)</u>	<u>Total (\$)</u>
3-Mw _e cost**	52,000	24,000	38,730	114,730
Fabrication savings if natural B replaces B^{10}	17,250	10,250	260	27,760
Worth of B^{10}	15,100	8,740	16,700	40,540
Total 3-Mw _e MCR cost	67,100	32,740	55,430	155,270
Total 3-Mw _e savings if B^{10} is not used	32,350	18,990	16,960	68,300
New cost	34,750	13,750	38,470	86,970

*One plant/year for five years.

**Assumes B^{10} powder is GFE.

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In view of the significant cost reductions (approximately \$68,000 out of \$155,000) which would result from the use of natural B, it is recommended that further studies be made to confirm that this change would not alter the operating and shutdown dose rates. If these rates prove to be unimportant across the entire power level range of interest, it is recommended that this design change be incorporated. It is also recommended that a minimal effort be made to change the design of the plenum borated steel regions to make them reusable after core replacement.

ANALYSIS

Pertinent data used in this study from the original 3-Mw_e MCR cost estimate¹⁹ are listed in Table 5-VII for all components considered. An evaluation of the internal shield, fixed radial reflector, and bottom shield plug is presented in the following paragraphs.

Table 5-VII.
¹⁹
3-Mw_e cost estimates for borated components and
costs of contained boron.

<u>Component</u>	<u>B/SS core pieces</u>	<u>Fixed radial reflector pieces</u>	<u>Bottom shield TiH₂ pieces</u>
Primary material	304 SS + B	304 SS + B	TiH ₂ + B ₄ C
Boron content specified	1 w/o B	1 w/o B	1 w/o B ¹⁰
B ¹⁰ content of specified B—%	92	92	92
Weight of component—lb	950	550	1065
Weight of boron—lb	9.5	5.5	10.65
Unit cost of boron specified*—\$/lb	1590	1590	1590
Cost of B powder***—\$	15,100	8,750	16,700
Cost of component excluding B powder cost**—\$			
Material	22,000	13,000	10,900****
Labor	30,000	11,000	27,830
Total	52,000	24,000	38,730

*\$3.5/gm ORNL isotope price list (crystalline powder)

**Material and fabrication cost excluding material cost of B powder (one plant per year for five years)

***Assumed as government-furnished in PD cost estimate

****Includes conversion of 92% B¹⁰ to B₄C

Internal Shield

Internal boron stainless steel shielding used in the 3-Mw_e MCR above and below the core inside the pressure vessel contains 1 w/o B¹⁰. This shielding is designed to be replaced when a new core is added.

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Replacing the B^{10} with a lower enriched B while maintaining the same B^{10} content would require an increase in the total B w/o in the stainless steel. The addition of B to stainless steel is marked by an increase in tensile strength and a decrease in ductility. Neutron irradiation also causes further embrittlement.²⁰ The B content in type 304 SS is practically limited between 1 and 2 w/o. A 1 w/o B content is acceptable from a metallurgical and irradiation standpoint.

To maintain the original B^{10} content with natural B in place of B^{10} in the stainless steel would require approximately 5 w/o natural B. In view of the foregoing considerations, this is not considered feasible. It is possible, however, to consider the use of natural B in the 1 to 2 w/o range.

Substitution of 1 w/o natural B would reduce the B^{10} concentration to approximately 0.20 w/o. Although supporting calculations have not been made, indications are that the substitution of 1 w/o natural B for 1 w/o B^{10} may not lead to any significant dose rate penalties either during operation or after shutdown.

The cost reduction (see Table 5-VI) amounts to considerably more than the cost of the B^{10} since the cost of boron stainless steel is higher (\$24/lb vs \$5/lb) when B^{10} is involved. This is because special heats of material and closer control of losses are involved.

The calculated B^{10} burnup is about 0.2% B—ten per core life (5000 fph). Swelling does not appear to take place until about 0.7 to 1.0% burnup of B^{10} atoms—about five core lifetimes.

Fixed Radial Reflector

Boron stainless steel containing 1 w/o B^{10} is also used in the outer 1.5 in. of the stationary portions of the radial reflector. Replacement of these radial reflector pieces with boron stainless steel containing 1 w/o natural B is not expected to increase the dose rate during operation or after shutdown. Table 5-VI indicates the cost savings possible with this replacement.

Bottom Shield Plug

The bottom shield plug in the 3-Mw_e MCR design contains 1065 lb of TiH_2 with 1.0 w/o of contained B^{10} . Fully enriched boron is used to achieve this loading. Replacement of the B^{10} with natural B could result in significant reductions in the cost of B^{10} material required. There is no significant change in the cost of the TiH_2 or its fabrication.

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The original shield performance calculations for the bottom plug were made with 1 w/o B^{10} as a nominal concentration to provide minimum secondary gamma ray production and Ti activation within the TiH_2 bottom plug. No specific performance calculations for the bottom plug were made using natural B in place of the B^{10} . Qualitative review of the previous calculations made for the bottom plug indicate that no significant change in operating or shutdown dose rates would result if the concentration of B^{10} in the TiH_2 were reduced by a factor of two and if 3 to 4 w/o natural B_4C were used to achieve this B^{10} concentration instead of B^{10} .

The 3 w/o of natural B_4C is equivalent to 0.44 w/o B^{10} , and 4 w/o of natural B_4C is equivalent to 0.59 w/o B^{10} . These compositions do not pose any problems in manufacture. The cost savings involved in going from B^{10} to 3 w/o natural B_4C are summarized in Table 5-VIII. The cost comparison presented is for the 3-Mw_e MCR design with 1 w/o B^{10} fully enriched, 0.44 w/o B^{10} fully enriched, and 0.44 w/o B^{10} as natural B_4C .

Table 5-VIII.
Costs for bottom shield plug TiH_2 pieces.

	3-Mw _e MCR enriched B_4C 1.0 w/o B^{10} (\$)	Enriched B_4C 0.44 w/o B^{10} (\$)	3 w/o natural B_4C 0.44 w/o B^{10} (\$)	Savings for 3 w/o natural B_4C (\$)
Component cost per 3-Mw _e MCR design cost estimate*				
Material	10,900*	10,660	10,640**	260
Labor	27,830	27,830	27,830	—
Total	38,730	38,490	38,470	260
B^{10} powder	16,700	7,450	—	16,700
Total costs	55,430	45,940	38,470	16,960

*Includes \$535 for conversion of 92% B^{10} powder to B_4C but assumes B^{10} powder is GFE.

**Includes \$240 for natural B_4C at \$8.70/lb (Norton quote).

A reduction in the B content and the use of natural B in place of fully enriched material would have the added benefit of providing a more even distribution of heat generation in the shield.

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Cast Lithium Hydride

The use of cast LiH was considered as an alternate for cold pressed LiH for possible reduction of shield costs. The most expensive portion of the 3-Mw_e MCR shield is the LiH. Previous studies conducted at the General Electric Aircraft Nuclear Propulsion Department,²¹ and more recently at Atomics International,^{22,23} concluded that for the ANP and SNAP LiH shield programs, LiH is less expensive than the cold pressed material.

The nuclear and physical properties of the materials are very similar. Since the ANP and SNAP cast LiH shields were not designed in the same shape or for the same conditions as the MCR shield, the purpose of this study is to investigate the advantages of cast LiH as a substitute for cold pressed LiH.

CONCLUSIONS AND RECOMMENDATIONS

Cast LiH segments comparable in size and shape to those required for the MCR shield have not as yet been successfully fabricated. To assess the possibility of casting such segments, the experience obtained at General Electric and Atomics International is pertinent. A review of this experience implies that, despite certain technological problems, it is technically feasible to cast LiH components suitable for MCR application. Distortion of the container housing the LiH casting appears to be the biggest problem. To establish the feasibility of casting MCR components, a large development effort would be required.

The apparent cost advantage which Atomics International found for casting LiH was limited to components of relatively simple geometry. For the SNAP-10A shield, AI found that casting costs were less than half those of cold pressing. In the absence of actual cost information for parts comparable to those required for MCR, it is not possible to make a realistic estimate of the cost of a cast MCR shield. There is basis for speculating that if fixturing can be developed to prevent excessive distortion at a reasonable cost, casting will result in a lower cost than present estimates for a cold pressed MCR shield. The apparent flexibility of the casting process in accommodating internal structures could also lead to performance advantages.

In view of possible cost advantages associated with the use of alternate configurations using borated water or boronated plastic shields, the final shield design may not contain a large quantity of LiH. In this case, the potential savings from cast LiH may not justify the cost of a casting development program. It is, therefore, recommended that the possibilities of cast LiH be reevaluated after the final shield concept has been selected.

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ANALYSIS

Background

There are currently two methods for making large LiH components suitable for shielding applications. The cold pressing method which employs hydrostatic pressing of coarse LiH powder at 30,000 psi was used to make pieces roughly 4 ft in diameter by 4 ft in length. For high temperature application, the cold pressed pieces are sintered to 1100°F, then machined to the required geometry, and finally assembled in a sheet metal housing. Present equipment at the Oak Ridge National Laboratory's Y-12 installation operated by Union Carbide permits cold pressing of pieces 44 in. in diameter and 48 in. long. The maximum size sintered piece, however, is limited to a 24-in. dia and 48-in. length. The humidity control required during the sintering process results in high handling and machining costs. Large pieces have been so made with and without an internal stainless steel honeycomb and assemblies containing up to about 500 lb of LiH have been made and tested successfully for the ANP program.

The casting process starts with the same feed material as the cold pressing method. Consolidation occurs by melting the LiH inside a sheet metal container which is the housing for the finished part. The assembly is then zone cooled to prevent formation of large shrinkage voids produced by the 19% change in volume associated with solidification. This technique met with some success for large fixtured sections of the ANP program and has also been successful when applied to simple geometries, such as the SNAP-10A and -8 shields. The SNAP-10A shield requires a single 200-lb LiH casting while the SNAP-8 requires a 700-lb casting.

The following paragraphs present a review of the casting technology status, the Atomics International cost comparison for the SNAP-10A shield, and the basis on which an estimate for casting LiH segments for the MCR shield can be made.

Status of Casting Technology

At present, the only significant LiH casting program is being carried out at Atomics International. Their successful casting of a SNAP-10A shadow shield is reported in Reference 22. This 200-lb component uses an internal stainless steel honeycomb and is cast in a stainless steel can. The geometry is a domed, solid cylinder about 20 in. in diameter and 24 in. long. The casting procedures used were similar to those developed for the General Electric ANP program.²¹ This process involves melting an entire charge of crushed and vibratory compacted LiH in a stainless steel can, and then solidifying from one end by gradually raising the heat source (a furnace external to the casting) along the vertical axis of the charge. A reservoir

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on the top of the casting filled with molten LiH is used as a feeder for the solidifying casting. This reservoir is the last region to solidify. The current work at Atomics International, in addition to the SNAP-10A shield, includes castings of about 700 lb (40-in. dia X 24-in. length) for the SNAP-8 development program.²³

Since the process involves solidification of the molten LiH inward and upward and the LiH undergoes a large volume contraction from the molten state to the solid at room temperature, high stresses and a complex stress pattern in the LiH must be expected. Added to this is the combined effect of differential expansion of the LiH and stainless steel can and the adherence of the LiH to the can at the interface of these two materials. The net result is a rather complicated stress pattern which would be difficult to characterize.

Atomics International has demonstrated that rather vigorous thermal cycling of the SNAP-10A cast shield does not yield high stresses at the outside surface of the can. They have also shown that after one or two cycles the LiH no longer adheres to the can. This information, therefore, does not lessen apprehensions about the likelihood of serious distortions of the can during the casting operations for shapes more complex than those now being made. In fact, Atomics International has noticed some dimpling of the can after casting SNAP shields. It can be concluded, therefore, that distortion will be the major problem in any development effort aimed at proving the feasibility of casting MCR shield segments. The need for a complex fixturing arrangement is suggested by the development work continued in support of the ANP program.

Cost Comparison of Casting and Cold Pressing SNAP-10A LiH Shields

For a geometry similar to the SNAP-10A shield—a domed solid cylinder—it is estimated by Atomics International that casting requires half the labor needed to make a comparable cold pressed, sintered, and machined piece. Costs of honeycomb, canning material, LiH and inspection are comparable for both techniques. Atomics International attributes the higher cost of cold pressing to the lengthy sintering operation and the costs associated with handling and machining to keep the cold-pressed part "dry" and free from cracking.

In Reference 22, a cost comparison is made for a SNAP-10A shield made by cold pressing and by casting. This shield has a geometry amenable to casting and requires no special fixturing or reinforcement of the casting container. The casting costs, shown in Table 5-IX (total \$10,300), are compared with a cost of \$25,000 (not including the cost of a can) for a cold pressed SNAP-10A shield procured from Oak Ridge. The comparison is limited since different accounting procedures may have been used in arriving at the comparative costs and it is not known whether the can and honeycomb costs were omitted. Despite these defects, the comparison strongly suggests a substantial cost saving by the casting method.

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Table 5-IX.
Costs for casting SNAP-10A LiH Shield.²²

<u>Shop assembly, casting, etc</u>	<u>100 man-hours</u>	
	<u>500 man-hours</u>	
Labor	600 man-hours	\$ 7,000
LiH	220 lb at \$9.25/lb	2,000
Thermocouples and heaters		300
Reservoir and miscellaneous items		500
Utilities (electricity, gases, water)		500
		<u>\$10,300</u>

Cost Comparison for Casting and Cold Pressing LiH Components of the MCR Shield

Any attempt to estimate the costs associated with casting an MCR LiH shield is complicated by the following factors.

- Similar parts have not been made successfully by casting; therefore, current cost estimates are not available.
- Although it is clear that distortion would be a major problem, the cost of preventing such distortion (by fixturing) can only be estimated using "technical intuition."
- Since a substantial development program is required to establish the feasibility of casting such components, cost estimates must be based on the assumption that such a program will be successfully completed, resulting in a process similar to that used for simpler shapes.
- A common wall is utilized between the LiH and Pb shield components in the present design, based on loading pressed blocks into a can. A common wall would not be possible using cast LiH in the present design.

Table 5-X lists the LiH components for the MCR shield and compares the number of pieces required by both methods. The number of cold pressed pieces for each section came from an Oak Ridge estimate derived from Atomics International proposed capability and current design considerations.

Using the SNAP-10A cost comparison as a basis for comparing casting and cold pressing LiH component costs for the MCR program, an unsuccessful attempt was made to evaluate the considerations previously noted. If the assumption that the result of a casting development program would lead to a process similar to that used for SNAP-10A, it is possible to generate

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some formula based on the SNAP-10A comparison. This formula would include recognition of further cost reductions owing to the smaller number of pieces required for casting and certain increases in casting cycle time (and cost) for larger pieces. Estimates of this kind were made and do show casting of LiH components to be cheaper than cold pressing. If an attempt is made to determine the cost of fixturing and internal housing support, the disparities between the initial cost comparison (SNAP-10A) and the MCR shield become greater. This is especially true since, in the case of the ANP casting development, the external fixturing represented a substantial fraction of the total casting cost. If the fixturing can be repetitively used, this cost can be spread over a large number of castings. It becomes pure guesswork as to what total fixturing costs would be.

Table 5-X.
LiH shield components.

	<u>Removable shield</u>		<u>Fixed shield</u>	<u>Top plug</u>	<u>Conical shell</u>	<u>Patches in</u>
	<u>Inner</u>	<u>Outer</u>	<u>Primary piping segment</u>		<u>in radial shield</u>	<u>housing and skid</u>
Total weight—lb	1,615	9,232	435	181	44	272
Total volume—in. ³ *	60,000	342,000	16,200	6,700	1,600	10,000
Number of cold pressed pieces	24	26	8	3	2	4
Number of cast pieces**	3	8	8	2	2	4
Maximum service temperature—°F	544***	300	480***	478***	300	ambient

*Based on LiH density of 0.027 lb/in.³

**Based on maximum available size of 7 ft in diameter and 5 ft long for the proposed Atomics International installation, and on geometry and current design considerations.

***Most of these components would require a 900°F sinter after cold pressing to convert any LiOH present to Li₂O.

It appears that a cost estimate for casting the MCR shield LiH components would be at best only an educated guess and quite misleading. In addition, no estimate was made of what a development effort, of the same magnitude as required for casting, would do to the cost of the cold pressed shield.

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Comparison of Fabrication and Design Features for Cold Pressed and Cast LiH

One of the most promising features of casting is the apparent ability of cast LiH to fill its container with little clearance. This might result in eliminating the present arrangement of securing the LiH blocks, resulting in a substantial cost savings.

The capability of the casting process to incorporate different internal components gives added flexibility, while the cold pressing method is highly limited in this respect. By utilizing the casting method, cooling tubes could be located inside the LiH instead of at the surface, and the internal structures which permit the LiH to carry some load might be gainfully incorporated.

A desirable fabrication feature of cold pressing is the smaller loss associated with rework. A large casting which is found objectionable may require rerun through the entire melting and cooling cycle or could necessitate scrapping the entire assembly. There is less raw material waste associated with casting than with machining.

Program Required to Permit a Reliable Casting Cost Estimate

Despite certain probable technical and economic advantages of casting, it is clear that current technology would not permit fabrication of a prototype MCR shield by casting since a cold pressed shield can be fabricated using state-of-the-art methods. A major development effort would be required to put the casting method in the same category.

To provide a better basis for a cost estimate of a cast MCR shield, some casting study is required to demonstrate the degree of complexity of casting MCR shapes. Large size, but not necessarily full size, pieces would have to be made.

It would also be desirable to compare a cast component with a cold pressed and bolted component in a test involving shock loading, thermal cycling, and irradiation to establish any can damage under MCR conditions. This would provide a better estimate of the cost of housing fabrication.

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