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Fast Breeder Reactors

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EFFECT OF OPERATING TEMPERATURE
ON LMFBR CORE PERFORMANCE

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1.0 Introduction

The purpose of the study is to provide ERDA with an engineering evaluation of high and low temperature LMFBR core designs. The study was conducted by C-E supported by HEDL expertise in the areas of materials behavior, fuel performance and fabrication/fuel cycle cost.

The evaluation is based primarily on designs and analyses prepared by AI, GE and WARD during Phase I of the PLBR studies. Subsequent work by these contractors has been considered to the extent practical; however, it has been found that Phase II work is of limited usefulness because temperature decisions were made at the end of Phase I.

The PLBR contractors supplied extensive compilations of data and analysis in formal reports and letters. Additionally, each contractor was visited by a team of experts from C-E and HEDL for detailed discussions of the technical issues involved in the design and performance of LMFBR cores operated at different temperature levels. Pertinent data obtained from PLBR contractors is summarized in tables in Appendix I. After compilation, the Appendix I tables were transmitted to the contractors for review and correction; consequently, these data accurately describe contractor's designs for optimized cores for operation at high or low temperature. Although several designs were examined for each temperature, contractors agreed that the basic temperature dependent characteristics are displayed by these high and low temperature reference designs.

Special attention was focused on certain key issues to determine the nature and the magnitude of the effect of operating temperature. Independent calculations were performed in support of the study of a number of these issues. Following are the principal issues examined:

Design Issues

- . Fuel pin design.
- . Fuel assembly design.
- . Core design (zoning, fuel management, shim placement).
- . Upper internals design.
- . Vessel design
- . Refueling system design.

Performance Issues

- . Fuel pin performance (predicted life).
- . Fuel assembly performance (predicted life).
- . Clad and duct material performance (assumed radiation properties, strength, etc.).
- . Fuel coolant performance (hot channel factors, orificing efficiency, etc.).
- . Economic performance (assumed costs, economic models and parameters, etc.).

The design issues were addressed by C-E. Analyses of the performance issues were split between HEDL and C-E. HEDL performed parametric calculations of fuel life as a function of material property assumptions and clad temperature. HEDL also calculated material properties according to formulas used by each contractor and according to formulas currently recommended by HEDL. HEDL also provided an independent assessment of fabrication costs.

In addition to analysis of these key issues, normalized core designs were developed for high (950°F outlet) and low (875°F outlet) operating temperatures. The development of these designs helped to improve the depth of understanding of temperature effects and provides a consistent design vehicle for analysis of the issues outlined above. These designs are not intended to be new optimized designs, but rather are idealized designs which are intended to show the effects of temperature on a conventionally designed LMFBR using "1st core" steel with minimum design parameter changes associated with temperature change.

The data given in Appendix I is strictly quoted as supplied and verified by the PLBR vendors without any adjustment or normalization. However, in the body of the report, vendor data has been normalized and otherwise adjusted in certain instances where this was judged appropriate for clarity or consistency.

2. Summary and Conclusions

Each engineering organization has a significantly different philosophy and approach to core design; consequently, there are a number of non-temperature related differences among the various designs which are superimposed on parameters which are affected by the temperature for which each core is designed. It is difficult, therefore, to clearly identify those differences which are primarily due to differences in operating temperature. The majority of the prominent differences are not directly due to temperature, but are rather due to differences in design choices and constraints.

In Table 2.1, design differences which are mainly due to temperature reduction, are summarized. The most significant common parameter difference is the fuel lifetime which is associated with operating temperature decrease. The WARD design does not show an increased fuel life because, first of all, the high temperature design of WARD has a relatively long life (3 years) due to use of advanced cladding material; and secondly, because WARD put an upper limit on allowable burnup. This upper limit was established by consideration of certain factors which are independent of temperature; mainly the effect of plutonium migration at high burnup. This plutonium migration effect was analyzed independently, and it was concluded that no hard upper limit on burnup exists. Additional analysis done as part of this study indicates that there is no significant benefit to be gained in fuel cycle costs for fuel life greater than 4 years, and that there is a penalty in plutonium utilization at fuel life greater than 3 to 4 years. It was concluded, therefore, that lower operating temperature will probably allow longer fuel life even if advanced alloys are used in the core.

Other changes of design related to temperature level are not found to be common amongst the PLBR contractors. General Electric has determined by their optimization procedure that the low temperature design should have a decreased fuel pin diameter, an increased linear power, and an increased gas plenum length. Neither AI nor WARD find the same result; however, the depth of optimization by these contractors was less than that done by GE. The trends of cost as a function of fuel diameter were reviewed based on GE's published calculations.^(2.1) The indication is that fuel cycle cost is very insensitive to fuel pin diameter over the range used in this study. Consequently, it does not appear to be important to change fuel pin diameter

Table 2.1DESIGN DIFFERENCES DUE TO TEMPERATURE REDUCTION

	<u>AI</u>	<u>GE</u>	<u>WARD</u>	<u>Normalized Designs</u>
1 Fuel Pin	None	Decreased Diameter, Increased Gas Plenum, Increased Linear Power	Decreased Cladding Thickness	None
2. Fuel Assembly	Increased Gap	Increased Length, Gap, Decreased Duct Thickness	Decreased Duct Thickness	Decreased Gap
3. Fuel Life	+1.0 yr.	+0.2 yr.	None	+0.7 yr.
4. Orificing and Coolant Distribution	None	Increased Bypass Flow*	None	None***
5. Core Zoning and Fuel Management	None	None	None	None***
6. Reactor Vessel, Head and Internals	Increased Diameter	Decreased Diameter	Increased Diameter	Increased Diameter**

* Eliminated in later designs.

** Diameter increase required to equalize power of low and high temperature designs.

*** Should be studied.

when changing temperature. The WARD analysis assumed a decreased cladding thickness for the low temperature design; however, this assumption was made somewhat arbitrarily.

In general, other than fuel pin design changes, the fuel assembly design did not change to any great extent when the temperatures were reduced. There is general agreement that small adjustments of the inter-assembly gap should accompany a change of temperature because the fuel lifetime is changed and the rate of duct swelling and creep changes. Some change of the duct wall thickness is also generally felt to be appropriate. These changes have an effect on the plutonium utilization of the core; however, this effect is not large.

The other significant change which accompanies a change of operating temperature is the change of the reactor vessel, head, and internals diameter. This diameter change is connected with the changed diameter of the core, which is generally thought to increase as the temperature is reduced because of the larger core required to compensate for reduced thermal efficiency. In the GE design, the outside diameter of the core actually decreases slightly because of the compensating effect of an increase in fuel pin linear power and a decrease in fuel pin diameter. Both of these changes are developed from GE's optimization procedure, which is quite different from that used by other organizations.

Differences in design choices and design constraints used by various organizations in the study of the effect of operating temperatures are summarized in Table 2.2. These differences are important factors in determining the differences of design found in these studies. They also lead to differences in performance which are larger than those differences which are due to operating temperatures. Design choice and constraint differences which most affect the plutonium utilization figures and the fuel cycle costs are: (1) cladding thickness, (2) choice of duct and cladding material, (3) limits placed on duct-bundle-interaction, (4) limits placed on doubling time, (5) flow and orificing design choices, and (6) and the use of special design features, including ventilated ducts and half-wire spacers. Additionally, the GE approach to optimization in which linear power, pin diameter, plenum length, and several other design parameters are considered variable simultaneously, also has a significant effect on the resulting design.

Table 2.2

DIFFERENCES IN DESIGN CHOICES AND DESIGN CONSTRAINTS USED IN STUDY OF EFFECT OF OPERATING TEMPERATURES

	<u>GE</u>	<u>AI</u>	<u>W</u>	<u>Normalized Designs</u>	
Linear Power and Pin Diameter	Variable	Fixed	Fixed	Fixed	
Cladding Thickness - Allowed (mils)	>12	>15	>15	16	
- Actual (mils) (High/Low)	12/12	17/17	16/12	16/16	
Duct Design	Ventilated	Conventional	Conventional	Conventional	
Fission Gas Plenum	Top	Top	Split	Split	
Wire Wrap	Conv. St. Start	½ Wire Stag. Start	Conv. St. Start	Conv. St. Start	
Duct-Bundle-Interaction	Strict Limits	Approximate Limits	Approximate Limits	Conservative Limits	2.4
Doubling Time Required	<15 yr.	Low	Low	~20 yr.	
No. Core Enrichment Zones	2	2	3	3	
Clad & Duct Material	"1st Core"	"1st Core"	"N-Lot"	"1st Core"	
Bypass Flow and Orificing	4th Best	2nd Best	3rd Best	Best	
Burnup Limit	None	None	120 Mwd/kg	None	

In developing the normalized designs for this study, one of the principal underlying ideas was to eliminate most of the design choices and design restraint variations. Thus, the thin clad option was eliminated, the half-wire design was eliminated, temperature-related changes of clad thickness were not included, linear power and pin diameter changes were eliminated, ventilated ducts were eliminated, the high linear power of the AI design was eliminated, the 15 year doubling time constraint was relaxed, and the advanced cladding and duct material used by WARD was eliminated. Elimination of these options, features, and constraints from the normalized designs is not intended to imply that they are unnecessary or not good features. Rather, this normalizing and simplifying process is intended to clarify the effect of temperature by eliminating the effect of special features and artificial restraints. The normalized designs do not perform quite as well as PLBR designs because, to a certain extent, these special features were not incorporated.

The second major underlying approach used in developing the normalized designs was to attempt to keep these designs as conservative as seems necessary to accommodate the relatively poor performance of 1st Core steel, and to avoid development questions associated with special features. In particular, special attention was given to reducing the cladding and duct operating temperatures of normalized designs to as low a value as practical for a given mix-mean outlet temperature. It was assumed for the purposes of this study that one should use very efficient orificing and cooling strategies in order to minimize the difference between the mix-mean outlet temperature and the operating temperature of the core components. In other words, it was thought to be most reasonable to first employ efficient cooling design procedures, fuel management, and core enrichment zoning methods to minimize cladding temperature and duct temperature before actually making a reduction in overall temperature level by reducing the mix-mean outlet temperature. It was found that there was considerable difference among contractors in this important design area.

Performance Comparison of High and Low Temperature Designs

Table 2.3 summarizes the principal design features and the performance characteristics of the high temperature and low temperature designs developed by the three PLBR contractors and the normalized designs developed in this study. The designs presented here are selected from a larger number of designs developed by each of

Table 2.3

SUMMARY COMPARISON OF HIGH AND LOW TEMPERATURE DESIGNS

	AI		GE		WARD		Normalized Designs	
	High	Low	High	Low	High	Low	High	Low
Design Outlet Temperature ($^{\circ}\text{F}$)	930	875	950	875	950	900	950	875
Net Thermal Efficiency (%)	36.5	31.6	36.2	32.1	36.8	31.6	36.5	31.3
Power (Mwe)	944	944	900	900	938	923	893	766*
Fuel Pin O.D. (in.)	.300	.300	.277	.256	.310	.310	.310	.310
Cladding Thickness (in.)	.017	.017	.012	.012	.016	.012	.016	.016
Cladding and Duct Material	1st Core	1st Core	1st Core	1st Core	N-Lot	N-Lot	1st Core	1st Core
Fuel Life (cy)	2.1	3.1	2.2	2.4	3.0	3.0	2.0	2.7
System Inventory (kgHM)	5390	5830	4980	4900	5980	6830	6405	5804
Breeding Ratio	1.29	1.28	1.31	1.27	1.28	1.34	1.32	1.30
Doubling Time (yr.)**	18	17	14	14	23	18	22	21
Fuel Cycle Cost (mil/Kwhr)**	4.33	3.85	4.81	4.30	4.67	5.05	5.99	5.71

* Thermal power of normalized designs held constant.

** All values normalized to load factor of 80%.

the three contractors. For the purposes of this study, each contractor was asked to put forward a single high or low temperature design to contrast with their reference design. This was done in order to simplify the evaluation procedure. It was agreed that the important effects could be observed by looking at the difference between these single high and low temperature designs.

The combination of fuel cycle costs, capital costs, and unavailability costs were used for selection of either a high or low temperature design by each contractor. Economic factors were placed ahead of other considerations as is appropriate. In this study, attention is focused, first of all, on the fuel cycle costs, and then on those capital costs which are directly attributable to differences in core design.

AI and GE see an improvement in fuel cycle costs as the temperature is decreased. The WARD analysis shows an increase of fuel cycle costs with decreased temperature and the normalized designs show a small increase with decreased temperature. These cost differentials come about by a relatively complex interaction among several variables which change as temperature is changed. The main effects, however, are the change of thermal efficiency, the change of fuel lifetime, and in the case of GE, the change of fuel pin diameter and linear power. AI calculates an increased fuel lifetime sufficient to override the effect of decreased thermal efficiency. WARD, on the other hand, calculates no increase of fuel life; consequently, WARD's fuel cycle costs increase as operating temperatures are reduced. Calculations made using the normalized designs, indicate that these two factors are in fairly close balance and the net result is a very small change of fuel cycle cost.

GE's net result comes from the interaction of a larger number of parameters. The most interesting result found in analyzing GE's design is the fact that fuel lifetime does not increase a great deal as the operating temperature of the core is reduced. This comes about because of the relatively poor cooling strategy adopted by GE. Cladding and duct temperatures in the General Electric cores are substantially higher than those of the other designers, and furthermore, for the GE low temperature design, increased bypass flow makes the clad and duct temperatures extraordinarily high for a low temperature design. Nevertheless, some improvement

in overall performance is found by GE for their low temperature design; this appears to come from a combination of a small increase in fuel lifetime and other small improvements sufficient to overcome the efficiency decrease. GE's design is rather strongly influenced by their use of 12 mil cladding and their requirement to maintain the doubling time at exactly 15 years. (Table 2.3 shows a lower doubling time for GE because all doubling times have been normalized to a load factor of 80%.)

A general trend towards an increased system inventory is found for the low temperature designs with GE's designs being the slight exception. (The normalized design shown in Table 2.3 for low temperature has a lower absolute inventory than the high temperature design; however, note that the low temperature design is substantially reduced in net electric output.)

The trend in breeding ratio is not strong; there are off-setting effects associated with temperature reduction. There is the possibility of use of thinner clad, the reduction of the duct wall thickness, and reduction of the gap between assemblies. The extent to which the designer takes advantage of these possibilities depends on the importance placed on breeding ratio. Lengthening of fuel life tends to require thicker clad, duct wall, etc. Also, longer fuel life also means increased fission product concentration in the core. Little change of doubling time occurs because the specific inventory increase at lower temperatures is off-set by the effects of the larger core and longer fuel life.

The potential benefits of the low or high temperature design options are listed in Table 2.4. Although there appear to be benefits for the low or high temperature design in the areas listed, all of these benefits are judged to be rather small. The fuel lifetime benefit, as calculated by analysis, must be discounted to a certain extent because of the lack of experimental confirmation. Although calculations done to determine fuel life tend to show that a lower operating temperature will increase the life of the fuel, there is no definitive experimental data on fuel pins to substantiate this point. Fuel life is probably determined more by swelling and creep induced interaction between the fuel pin and the fuel assembly structure than by the fuel pin itself. The experimental bases for prediction of the temperature dependence of this effect

Table 2.4

EVALUATION OF BENEFITS

	HIGH TEMPERATURE (950°F)	LOW TEMPERATURE (875°F)
Unchallengeable Benefits	<p>Higher efficiency = lower core thermal power</p> <p>= fewer fission product wastes</p> <p>= less waste heat</p>	<p>Improved material properties</p> <p>= higher strength</p> <p>= lower duct swelling</p> <p>= lower creep rates</p>
Probable Benefits	<p>Lower burnup and fluence* = larger fuel/clad/duct performance data base</p> <p>Lower Pu inventory*</p> <p>Smaller reactor vessel, head and internals*</p>	<p>Longer fuel life*</p> <p>Improved design margins for upper internals</p> <p>Shorter fuel assembly*</p>
	<p>* The magnitude of these effects depends on design choices such as clad thickness and fuel pin diameter. These design choices have a stronger influence than temperature; consequently, trends may be reversed in specific cases.</p>	

is more firmly established; however, no final experimental confirmation is available.

In addition to those items discussed above, it is judged that the high temperature design will probably have a slightly smaller reactor vessel, head and internals because of a slightly smaller overall core diameter. It is also a significant benefit that the high temperature design has a lower burnup and fluence in that there is considerably more experimental and operating data on mixed oxide fuels at burnup below 100 Mwd/kg. This benefit to the high temperature design is a counter-balancing benefit of the longer fuel life associated with the low temperature design. In other words, the long fuel life carries with it some disadvantage because the experimental data base for high burnup fuel is somewhat thin.

Table 2.4 categorizes benefits as those which are unchallengeable and those which are probable. A considerable effort has been made to identify clearly unchallengeable benefits for the high temperature and low temperature options; it was found that there are very few of these. On the high temperature side, the high thermal efficiency will certainly lead to fewer fission products to be disposed of and less waste heat. The low temperature design certainly will have associated with it improved strength, swelling and creep properties for the materials used for cladding and ducts. These improved properties will be able to be utilized in either improving the life of the fuel or improving plutonium utilization, or some combination thereof.

Capital Costs

The incremental capital costs associated with the change from a high temperature design to a low temperature core design was calculated by the PLBR vendors and has been estimated independently in this study. These costs are shown as the top line in Table 2.5. In each case, the capital cost differential is only that differential which comes about because the core diameter has changed in going from a high temperature design to a low temperature design. With the exception of General Electric, all designers estimate that some increase of core diameter will occur. The cost differential associated with this diameter change, however, is highly variable. The numbers shown in Table 2.5 are the direct capital cost differentials quoted by the vendors and estimated by C-E. In several cases, the vendors included

Table 2.5COMPARISON OF CALCULATED COST ADVANTAGE OF LOW TEMPERATURE DESIGNS

	<u>AI</u>	<u>GE</u>	<u>W</u>	<u>Normalized Designs</u>
. Direct capital cost differential of vessel, head, internals, fuel storage				
- \$ Millions	-4.33	+0.75	-20	-4.5
- Mil/Kwh	-0.11	+0.02	- 0.50	-0.11
. Cost correction for surface area				
- Mil/Kwh	-	-0.53	-	-
. Fuel cycle cost differential				
- Mil/Kwh	+0.53	+0.51	- 0.35	+0.28
. Availability differential due to refueling				
- Mil/Kwh	+0.04	0	- 0.04	0
. Net differential cost				
- Mil/Kwh	+0.46	0.0	- 0.89	+0.17
. Net cost differential quoted by vendors	-0.69	+0.85	- 1.30	-

indirect costs as well as direct costs in these differentials. Because indirect costs include engineering and other factors, which are not closely connected with the physical size of these components, it was thought appropriate to eliminate these costs. For the normalized design, the differential cost was obtained by using a 0.6 power scaling law on the average value of cost of vessel head and internals quoted by AI and GE (\$45,000,000 total). Cost differentials due to building size change, change in numbers of control rods, and other such items, were not included because the relationship between these items and core size is speculative.

In addition to the vessel head and internals differential, GE indicated that there was a differential cost in the balance of plant which was required to maintain a constant net thermal efficiency when the core outlet temperature was reduced to values below 900°F. In order to put the GE costs on a basis comparable with other costs, this balance of plant differential has been converted to mil/kwh and inserted in this evaluation. This effect is not additive for the plant as a whole and should not, therefore, be treated as such. AI and WARD also indicated a cost differential due to unavailability associated with fuel handling. These cost factors are summed up together with the normalized fuel cycle costs calculated in this study to give a net differential cost for the three PLBR vendors and for the normalized designs. The re-evaluated differentials of Table 2.5 show trends different from those quoted by vendors in two out of three cases. Because these differentials represent the net summation of nearly equal positive and negative quantities, this result is not unexpected.

The general effect of the analysis done in this study is to reduce differentials associated with the temperature effect as compared to those values quoted by PLBR contractors. The independent estimate made using normalized designs also shows a very small differential cost which slightly favors the low temperature design. These cost differentials are so small that it is concluded that there is no real significant advantage in fuel cycle and core related capital costs for either design option.

As a qualitative explanation of the underlying reasons that the different contractors arrived at different conclusions for the cost trend, Figure 2.1 has been prepared. There are several effects illustrated here which are significant in explaining the different conclusions arrived at. First of all, each contractor studied a different temperature range: GE - 950°F to 875°F, AI - 930°F to 875°F, WARD - 950°F to 900°F.

The second, more important reason is the effect of design choices, material choices, and cost models. Increasing the efficiency of zoning, orificing and bypass cooling has the effect of shifting these curves from left to right. As previously mentioned, the GE design is relatively inefficient in reducing the duct and cladding temperature relative to the mix-mean outlet temperature. For designs which are more efficient in this respect, the net effect is to shift the cost vs. temperature curve to the right. A further effect, which also shifts the cost vs. temperature curve to the right is the assumption of improved material properties associated with advanced alloys. The final effect is the costs that are attached to the reactor vessel, head, internals and ex-vessel storage system. Both AI and WARD estimated these cost differentials to be rather high values, and this has the effect of shifting these curves to the right when these costs are added to fuel cycle costs. Thus, although General Electric is convinced that the costs of the fuel cycle and associated core costs always increase above an outlet temperature value of 875°F, other vendors are equally convinced of the opposite trend.

Materials Effects

Late in this study, a new correlation of thermal creep for 1st Core steel was developed and published by HEDL. This new correlation shows thermal creep values to be substantially higher in this material than previously thought. This information was available in time to make a partial evaluation of its effect on design. The indication is that the high temperature design developed in this study is only marginally acceptable. The high temperature designs of all PLBR vendors using 1st Core steel may also be marginal. Even GE's low temperature design may be marginal. This information of course, would not apply to the advanced alloy used in the WARD design since improved properties are assumed. However, since it was assumed that the N-lot material had thermal creep properties which were little different than 1st Core material, the

Figure 2.1

COMPARISON OF COST vs TEMPERATURE RELATIONSHIPS

significance of this new data is that the advanced alloy may require a substantial improvement of thermal creep as well as an improvement in swelling relative to first core material.

The Westinghouse approach in using the properties of N-lot material as defined in early 1976, has three significant effects so far as properties are concerned: (1) the stress rupture strength of the material is significantly improved, (2) the swelling of the material is significantly reduced at high temperature, (3) the thermal creep strength of the material is significantly better. The current analysis indicates that only two of these three are significant. The assumption of a relatively high stress rupture strength for the N-lot material used in the Westinghouse analysis was counter-balanced by a pessimistic assumption of the effects of decarburization. It is currently recommended by HEDL that no corrections be made to account for the effect of decarburization. WARD did make a substantial correction; consequently, these effects cancelled out. Furthermore, it is the current recommendation of HEDL that no net decrease of stress rupture strength need be applied to account for the effect of irradiation. This further decreases the significance of the assumed improvement of stress rupture of the N-lot material.

Depth of Analysis

It is clearly apparent that GE analyzed the effect of operating temperature on core performance in a great deal more depth than the other PLBR vendors, and, of course, in considerable more depth than was possible to do independently in this study. It is, therefore, proper to give greater weight to the conclusions arrived at by General Electric than by other vendors. The detailed optimization done in the course of GE's studies does help to clarify some trends; however, it is difficult to sort out those effects which are primarily due to temperature and those effects which are artifacts of the complex methods and assumptions used in the analysis.

One thing that is somewhat inconsistent in the GE approach is in the area of optimization of fuel management and cooling strategy. It would be expected, with the strong emphasis placed on the importance of low operating temperature, that some trade studies would have been conducted which would determine the payoff in efficient cooling of the core in terms of more sophisticated orificing schemes, more sophisticated fuel management and fuel zoning schemes, and by reduction of unheated bypass

flow. It would be logical to assume that if there are important penalties in fuel lifetime and other effects due to operating the core at high temperature, that some investigation and optimization should have been done to reduce these effects without reducing the outlet temperature of the reactor. It is particularly puzzling that on the one hand, there is strong emphasis put on the importance of reducing core operating temperatures, and on the other hand, the low temperature reference design of GE has an extraordinarily high bypass flow around the core, which results in core operating temperatures which are actually higher than some of the high temperature designs developed by other vendors. This choice is partially explained by GE in terms of the cost of pumping. There is certainly a trade-off between pumping costs and cost benefits gained from reducing the operating temperature of the core components by improved orificing and reduced bypass flow. Other improvements, however, do not affect pumping greatly. To the extent that there is a strong temperature dependence of core performance characteristics, a design would be expected which would have more optimum cooling and fuel management than the GE designs.

On the other end of the spectrum, WARD apparently investigated the relationship between operating temperature and core design performance in the least depth. Several internal inconsistencies were found in the WARD data. Nevertheless, if WARD's basic ideas are valid (use of thin cladding at low temperature, availability of advanced cladding material and no improvement of fuel life at lower temperatures) then WARD's projection of cost and plutonium utilization trends are correct.

One option which was considered by both GE and AI, which is particularly significant, is the option of using the half wire design feature in the fuel assembly to reduce the cladding temperature, and to reduce the differential in temperature between cladding and duct. GE rejected this option because it was thought not to improve duct-bundle interaction, and because GE designs were not limited by fuel pin temperature considerations. This conclusion merits re-examination in light of the new data on thermal creep of 1st Core steel. Furthermore, it is quite possible that the advantages of this design feature can be gained without any significant disadvantage if further development of the design is pursued. This is one method of reducing cladding temperatures and reducing duct-bundle temperature differentials for which there is essentially no penalty in terms of pumping power. This makes this design feature particularly attractive.

Conclusions

To recapitulate the main conclusions resulting from this study and discussed above, they are:

- (1) Independent calculation of core performance in which special features such as ventilated ducts, thin cladding, and advanced alloy have been eliminated, and in which variables of secondary importance are held constant, shows that a reduction of temperature from 950⁰F to 875⁰F produces off-setting effects which lead to a very small change of costs and plutonium utilization. From these calculations, and from the examination of calculations provided by the PLBR vendors, it can be concluded that the important net effects of a reactor outlet temperature change are not in the areas of fuel cycle costs, core reliability/availability, plutonium utilization or capital costs of the reactor vessel, head, internals, or fuel handling system.
- (2) The two principal changes in performance and design found when changing from a high temperature core to a low temperature core are: (a) an increase in the life of the fuel, and (b) a decrease in the net thermal efficiency of the system. These two effects approximately offset each other.
- (3) The predicted effect of operating temperature on fuel pin and fuel assembly life has not been conclusively verified by actual test data for operating experience. Phenix, which operates at an outlet temperature ~100⁰F above the "high" outlet temperature expected in these designs, provides substantial verification that minimum performance (2 years) is achievable with the high temperature option. There is very little prototypical operational data to verify projected improvements for lower temperature operation.
- (4) Conclusion (1) is valid only when considering LMFBR's using CW316SS fabricated to RDT-M3-28T and RDT-E6-20T ("1st Core Steel"). A limited investigation of a moderately advanced alloy ("N-Lot steel") indicates that the high temperature option may have a small but significant advantage if and when advanced alloys become available.
- (5) The high temperature design has an advantage with respect to reduced fission product waste because of its high thermal efficiency. The low temperature design has the advantage of better strength, creep and swelling of cladding and ducts, which leads to less steel and sodium in the core.
- (6) Differences in core design among the various designs studied which are attributable to operating temperature are less important than design differences

arising from the design philosophy of the vendors.

- (7) The relationship between outlet temperature and fuel cycle costs plus associated incremental capital costs can be shown to either increase or decrease relative to an 875⁰F reference point, depending upon assumptions made about fuel lifetime, cooling and fuel management efficiency, the cost of incremental size change of the vessel head and internals, the range of temperatures studied, and optimization methods.
- (8) New information on the thermal creep properties of 1st Core Steel indicate that all high temperature and some low temperature designs may be marginal if this material is used for cladding and ducts. This, together with the recognized high swelling characteristics of this alloy, means that some modest improvement of alloy properties or design change may be needed to support all high temperature designs and some low temperature designs. The normalized high temperature design developed in these studies is most conservative in this respect, and could be made acceptable with a modest increase in gas plenum size.
- (9) It is probable that AI and W-ARD overestimated the incremental effect of core size on the capital cost of the reactor vessel and associated hardware. AI and W-ARD estimate the cost of these contributions, to power costs of low temperature design, is of the order of +0.5 mil/kwh, whereas it is more probable that the contribution is of the order of +0.1 mil/kwh.

3.0 Temperature Related Design Differences

3.1 Fuel Pin

The fuel pin design parameters are a consequence of both a priori design choices and an optimization process. This section attempts to sketch, in a brief outline, a description of the PLBR fuel pin designs. Differences resulting from design choices as opposed to optimization processes will be mentioned.

Design choices refer to those parameters which are fixed prior to the core optimization and their selection is motivated by subjective decisions. Both the fuel smear density and cladding thickness were design choices in the PLBR cores. The smear densities were similar for all designs and allow a fuel clad gap currently thought sufficient to prevent excessive fuel-clad mechanical interaction. All designs could find support in the existing data base for these densities.

There is a marked difference in the clad thickness as the GE design has 12 mil clad and those of WARD and AI have 16 and 17 mil clad respectively. All designers appear to have allowed for the minimum clad thickness consistent with other constraints. All vendors make subjective judgments of defect allowances, and wear due to vibration and handling, but these allowances are similar. There is a difference in view point among the vendors in that WARD and AI propose designs in which fuel pin lifetime is the principal determining factor, ^{3.1.1, 3.1.2} whereas GE considers duct bundle interaction (DBI) generally more limiting. ^{3.1.3} There is also disagreement about the interpretation of the existing data base, as well as the controlling parameters in fuel pin lifetime. GE feels that end-of-life (EOL) cladding thickness/diameter ratio, burnup, and plenum to fuel volume ratio are more important than absolute cladding thickness. ^{3.1.4} As will be discussed in another section, the experimental data base does not yet support conclusions about fuel pin failure mechanisms, thus, the choice of clad thickness should be regarded as a selection based on opinion and judgment more than on hard data.

The pin outer diameter was fixed by the optimization process. WARD performed a general optimization which assumed, among other things, a three year

residence time. To achieve such a residence time required large diameter pins to drop the flux to manageable levels as well as advanced cladding. Later studies with a lower reactor outlet temperature showed greater fuel volume fractions resulting from less steel required at these temperatures. The low efficiency cycle introduced unacceptable fissile inventory penalties, and smaller pins requiring a lesser fissile inventory were limited by short burnups. On this basis, WARD proposed a 0.31" O.D. pin with 0.016" clad. Assigning a higher burnup to a lower temperature was the AI approach to the temperature question, but their results indicated only a modest gain in fuel cycle costs.

All cores optimized by GE possessed 12 mil clad as the COROPT optimization process removed steel to a design choice minimum. Under such conditions, small pin, high flux core designs were found to be economical. For such designs, residence times are not long. In the COROPT process, the plenum size was allowed to vary and in no design was the fuel pin the limiting factor. However, COROPT assigns only a small economic penalty for long plenum lengths.

3.1.5 As temperature increased, COROPT found larger pins necessary to decrease the flux and achieve economical burnups.

The plenum volume was fixed by WARD in their optimization and their design features a split plenum with 34 inches above the core and 19 inches below the core. This separation is conducive to long fuel pin lifetime as a sizeable fraction of the released fission gas exerts a lower pressure than would be the case were the plenum a single unit. The design and size was held constant for WARD high and low temperature cores, and its selection represents a design choice, i.e., its length was not important in their design optimization. The AI design provides for an upper plenum of 48 inches and this parameter was not varied during their optimization. The GE low temperature design features a very long (75 in.) plenum length because of a duct bundle interaction constraint. The high temperature design has a shorter plenum because it was limited by duct-duct interference (DDI).

The WARD high temperature design features a tight packing of fuel pins ($P/D = 1.15$) and the diameter of the fuel pins offsets the relatively small coolant area per pin. No significant advantage was seen in opening up the pin bundle at lower temperatures. The pressure drop through the active region of the core was 67 psi. The AI fuel pin P/D is somewhat larger at 1.20, but the wire wrap diameters of the outer rods in each subassembly are half as large as those of the interior rods. The "half wire" prevents overcooling of the outer pins and it allows a decrease of about 30° in clad temperatures.^{3.1.6} Such a design increases the duct temperature and dilation, but reduces the magnitude of DBI because the duct swells more. The design however has a somewhat less allowable DBI. Both GE high and low temperature designs have a relatively large P/D of 1.225 and 1.191 respectively, consequently pressure drop is less than other designs.

To provide a cleaner comparison between the effects of high and low temperature, two "normalized designs" were prepared in which only those parameters felt to be absolutely temperature dependent were varied. Fuel pin dimensions did not vary with reactor outlet temperature. For these designs, a pin outer diameter of 0.31", clad thickness of 0.016", and a P/D of 1.174 were selected. The smear density was 91% of theoretical. A split plenum of 34 and 19 inches in the upper and lower regions was initially chosen. A two year residence time was established for the high temperature core based on subassembly constraints. It should be noted that these cores are not completely optimized, but are presented to show the major effects of increasing reactor outlet temperature. A more detailed discussion of these cores is presented in Section 9.

Except for gas plenum differences due to pin life differences, the design of the fuel pin was not controlled by reactor outlet temperature. The differences appear to be more in the nature of design choices, e.g., half wire wrap, or optimization results, e.g., the long plenum in the GE low temperature design. Such differences are driven more by subjective judgement or optimization constraints than by a systematic response to a change in reactor outlet temperature. Detailed descriptions of all PLBR designs are presented in Appendix I.

References

- 3.1.1 PLBR Reactor Core Temperature Information Meeting, WARD/CE/HEDL, February 7-8, 1977, Madison, PA.
- 3.1.2 PLBR Reactor Core Temperature Information Meeting, Atomics International/CE/HEDL, February 14-15, 1977, Canoga Park, CA.
- 3.1.3 Letter, R. G. Sim to R. C. Noyes, "Outlet Temperature Issues," March 7, 1977.
- 3.1.4 Letter, R. G. Sim to R. C. Noyes, "Evaluation of GE and W-ARD PLBR Fuel Pin Performance," March 4, 1977.
- 3.1.5 PLBR Reactor Core Temperature Information Meeting, GE-FBRD/CE/HEDL, February 16-18, 1977, Sunnyvale, CA.
- 3.1.6 Table 3.2.1

3.2 Fuel Assembly

As in the previous section, characteristic subassembly parameters will be described in terms of a priori design choices, the influence of the optimization process and the effect of reactor outlet temperature. The effect of DBI and DDI mechanical constraints upon the design will also be discussed. The role of subassembly performance as a limiting factor in the PLBR designs will be mentioned in Section 7.3.

Design options available to the subassembly are somewhat more limited because the structural constraints are more clearly defined. Typical considerations in minimum thickness decisions include protection of the rod bundle in transport, core restraint and structural integrity considerations, proper coolant orificing, and accident prevention. Ventilated ducts, i.e., slots located in the subassembly wall above the core region, allowed GE low pressure drops in the core region. Consequently, GE designs were able to utilize thin duct walls due to a reduced creep contribution to the overall duct dilation. Although AI proposed a half-wire wrap design to lessen peak clad temperatures and reduce the magnitude of the DBI, other options were not pursued by the PLBR vendors.

Optimization of subassembly geometry was performed most thoroughly by GE, as they allowed the subassembly pressure drop to vary during the optimization. The low pressure drop allows GE to remove steel from the core without incurring excessive penalties due to the stress enhanced swelling component of duct dilation. AI and WARD had settled on an assembly ΔP based to a large extent on balance of plant considerations.^{3.2.1, 3.2.2} Thus, duct walls tend to be thicker for the other designs to restrain duct swelling.

The WARD lattice pitch increases with temperature as a thicker wall is needed to maintain the constant gap (0.360") assumed for these designs. The WARD assembly pressure drop is 95 psi, and it should be noted that the designs were done using the Revision 3 swelling correlation. The GE lattice pitch increases about 5% with temperature, but the increase reflects the greater pin diameter of the GE high temperature core. The gap decreased from 0.381" to 0.361" as temperature increased because the GE high temperature design has a lower fluence. At the low temperatures and pressures proposed by GE,

duct wall thickness considerations were secondary. The AI design displays the largest lattice pitch (6.95") partly as a result of the hotter duct walls resulting from the half wire design.

AI has incorporated the half wire design feature into their driver assembly design in order to reduce the effects of intra-assembly flow maldistribution. The main cause of assembly flow maldistribution is the large bypass flow area between the outer row of pins and the duct wall. This bypass flow area can be greatly reduced by shaping or shaving the wire diameter between the pin and the duct wall. AI reduced the wire wrap diameter in the edge pin to half of the interior pin wire diameter.

The effect of the half wire wrap design feature was estimated using the computer code ENERGY.^{3.2.3} The results are shown on Table 3.2.1. The interior subchannel flow maldistribution is the ratio of the expected coolant temperature rise to the temperature rise for a uniform flow per pin. As noted in Table 3.2.1 a substantial reduction in this maldistribution factor is achieved. This translated to an approximate 32°F reduction in the coolant temperature rise for the high flow high power assembly in the normalized design.

The other effect is an increase in the temperature of the edge subchannels since overcooling of the edge pins has been markedly reduced. Since these are adiabatic assembly calculations, the duct wall would be at the same temperature as the coolant in the edge subchannels. With the full wire wrap the average duct wall temperature at the core exit plane was approximately 110°F below the assembly mixed mean outlet temperature. With the half wire wrap the duct runs approximately 60°F hotter.

The porosity per ring appears, within the framework of current data, to be similar for all vendors.^{3.2.4} The bundle clearance, which is the difference between the duct inner dimension and the bundle outer diameter, is smallest for GE. Bundle clearances for the AI design were 0.048" and 0.046" for the high and low temperature designs, respectively. The duct bundle clearance for the WARD design remained constant at 0.049" for both temperatures.

The normalized high temperature subassembly was sized to give a balanced design consistent with a two year lifetime. A gap of 400 mils and a duct wall thickness of 115 mils was established by this procedure. The life of the low temperature case was determined by maintaining the same duct bundle interaction for both cores. The low temperature core thus has a 115 mil wall and a 360 mil gap. Consequently, the only effect of operating temperature is to change the gap from 360 mils to 400 mils.

Because the GE low temperature design has a much longer fission gas plenum than the GE high temperature design (75" vs 40"), the fuel assembly is 15" longer for the low temperature designs. Other vendors did not change fuel assembly length. Analysis of the normalized designs indicates that the low temperature design could have a considerably shorter fission gas plenum and consequently a shorter assembly..

In general, the trend with increasing temperature is for the hotter cores to have either bigger gaps or thicker duct walls. The former is required to accommodate increased steel swelling while the latter reduces the effect of irradiation creep. An exception to this rule is the GE low temperature core, which has a bigger gap than its corresponding high temperature counterpart. This core experiences a slightly greater swelling due to a higher fluence.

References

- 3.2.1 PLBR Reactor Core Temperature Information Meeting, Atomics International/CE/HEDL, February 14-15, 1977, Canoga Park, CA.
- 3.2.2 PLBR Reactor Core Temperature Information Meeting, GE-FBRD/CE/HEDL, February 7-8, 1977, Sunnyvale, CA.
- 3.2.3 E. U. Khan and W. M. Rohsenow, 'Manual for ENERGY I, II, III Computer Programs,' MIT, COO-2245-18TR, May 1975.
- 3.2.4 Hanford Presentation, LMFBR Driver Fuel Subassembly Program Review at RDD/HQ, May 27-28, 1976.

Table 3.2.1

EFFECT OF HALF WIRE WRAP

	<u>Full Wire</u>	<u>Half Wire</u>
Interior Subchannel Flow Maldistribution	1.136	1.045
Hot Channel Coolant Temperature Rise ($^{\circ}\text{F}$)	460	428
Ratio of Hot Channel Coolant Temperature Rise to assembly Mixed Mean Temp. Rise	1.442	1.342
Reduction in Hot Cladding Temp. ($^{\circ}\text{F}$)	-----	32
Difference Between Assembly Mixed Mean Outlet Temperature and Average Duct Wall Temperature ($^{\circ}\text{F}$)	110	50

3.3 Core and Core Cooling

This section discusses the general core configuration for the high and low temperature designs of the reactor vendors and the normalized designs. The overall reactor cooling strategy is discussed with respect to core temperatures in general and maximum midwall cladding temperatures in particular. Methods for reducing temperatures such as power flattening and orificing efficiency is included in the discussion. A detailed parameter listing of the various reactor vendor designs is found in Appendix I and a description of the normalized designs is found in Section 9.0.

3.3.1 Core Size

The plant sizes considered here are generally in the 900 MWe class. The core size depends on the plant net thermal efficiency, the energy deposited in the core, and the allowable linear pin power. The AI designs are the smallest cores in terms of the number of driver assemblies, 228 and 264 for the high and low temperature reactors. The small sizes are due to the low core power deposition fraction (.914) and the high average and maximum linear pin powers (10 kW/ft and 19* kW/ft). The increase in the number of driver assemblies from the high to low temperature designs is a direct result of the reduction in net thermal efficiency (36.5% to 31.6%). The physical size of the core increased due to a small increase in the lattice pitch as well as the larger number of driver assemblies. The change in the lattice pitch resulted from slight increases in the duct inside flat-to-flat and in the interduct gap. The duct size increased to accommodate the additional flow accompanying the decrease in the reactor vessel ΔT from 280°F to 270°F. Although the duct temperature reduces by ~55°F for the low temperature design, the increased core residence time (2.1 to 3.1 calendar years) resulted in a larger required interduct gap in order to prevent duct-duct interaction (DDI).

The Westinghouse core sizes increased solely due to the decrease in plant net

* 3σ , 15% overpower value

thermal efficiency (36.8 % to 31.8%). The core energy deposition factor (0.940) and average linear power (8.25 kw/ft) resulted in the higher number of required driver assemblies (276, high temperature, and 318, low temperature designs) as compared with AI. The driver assembly configuration and lattice pitch are identical in the two designs. Both assemblies are designed for reactors with 300°F reactor vessel ΔT . No credit for the ~50°F reduction in duct temperature was taken to reduce the duct wall thickness or the interduct gap in the low temperature design.

The GE cores show very little change in size with reactor outlet temperature. The thermal rating of the low temperature core increased with the decrease in thermal efficiency (36.2% to 32.1%); the core power deposition fraction (0.94) was virtually the same in both the high and low temperature designs. The number of driver assemblies for the low temperature design, however, increased only slightly (282 from 270) since the core wide average linear pin power is 11% higher than the high temperature design (8.9 kw/ft vs. 8.0 kW/ft). This small potential increase in core radius is further reduced by a 5% reduction in the lattice pitch, which is a direct result of a smaller fuel pin diameter. The fuel pin diameter effect is a result of the detailed multi-parameter optimization procedure of GE's COROPT code and, particularly, the 15-year maximum doubling time constraint imposed on the calculations.^{3.3.1}

The normalized designs and the design procedure are described in detail in section 9.0. The fuel pin and driver assemblies are the same for both the high and low temperature designs. The reactor vessel ΔT is 300°F in both cases and the outlet temperature reduces by 75°F from 950°F to 875°F from high to low. The required interduct gap decreases from high to low since the 75°F duct temperature reduction outweighs the increased residence time (2.0 to 2.7 calendar years). In order to illustrate the temperature effects clearly, the high and low temperature cores are identically configured,

and the net electrical output falls with the lower efficiency of the low temperature design. Thus, the core radius is slightly smaller for the low temperature design due only to the reduced interduct gap.

3.3.2 Overall Core Cooling

Before decisions concerning driver assembly orificing can be made, the gross reactor coolant allocation must be considered. Flow must be allocated to the control and radial shield assemblies to remove the low level of energy deposited there. There is direct leakage of flow between the reactor inlet and outlet plenums through the interduct gaps. This leakage can be kept to a minimum with appropriate design features. Some flow can be bypassed for cooling the vessel if the designer makes this choice. For the purposes of the discussion here, these various flows are collectively termed the By Pass flow.

The amount of flow allocated for the By Pass depends on the necessary cooling for control and radial shield assemblies. It also depends on the specifics of the reactor inlet region design with regard to leakage flows. Table 3.3.1 lists the By Pass flow fraction used by the several reactor vendors; it varies from an optimistic 2.5% to a very large 13.7%. The very high bypass represents a design approach wherein GE attempted to reduce duct wall thickness and primary sodium pumping power by reducing core pressure drop through reduced core flow.^{3.3.1} They have since dropped this design approach. A 3% By Pass flow was estimated for the normalized design. Based on the power distribution estimated by CRBRP^{3.3.2} and extrapolated to these large core sizes, a 0.5% power fraction could be expected for the control and radial shield assemblies. Employing an overcooling philosophy of half the reactor vessel ΔT for these regions, a 1% flow allocation for the control assemblies and for the radial shield assemblies was made. The 1% leakage employed in CRBRP^{3.3.2} was accepted for these calculations. Thus, a reasonably optimistic flow bypass of 3% was established.

TABLE 3.3.1OVERALL REACTOR COOLING STRATEGY

		<u>By Pass Flow* Fraction</u>	<u>Radial Blanket** Power to Flow Ratio</u>	<u>Temperature Difference Between Driver Outlet and Reactor Outlet</u>	<u>Driver Region Flow Fraction</u>
AI		0.025	0.59	14.9	0.924
GE	High	0.06	0.240	57.0	0.815
	Low	0.137	0.220	78.3	0.769
WARD		0.05	0.29	43.7	0.847
Normalized Design		0.03	0.50	19.8	0.91

* Bypass flow includes control assembly flows, radial shield assembly flows, leakage, and vessel bypass flow.

** Radial Blanket Power Fraction = 0.03 assumed for all cases.

The amount of flow assigned to the radial blanket is, in part, a matter of acceptable overcooling of the region. Since this region has a severe increase in power over life, flow allocation based on end of life temperature requirements leads to high overcooling during life. The extent of overcooling can be measured by the ratio of the radial blanket power fraction to the radial blanket flow fraction. The value of this parameter for the several reactor vendors is shown on Table 3.3.1. It should be noted the smaller the value of the ratio, the greater extent of the overcooling. For the normalized design, a coolant rise for the overall blanket region at middle of equilibrium cycle (MOEC) equal to half the reactor vessel ΔT was established as a reasonable target; the power fraction to flow fraction ratio is, therefore, 0.5.

The corresponding driver flow factors and driver region outlet temperature increase above the reactor vessel outlet temperatures are listed in Table 3.3.1. The values shown on this table are not the design values of the several vendors but represent the effect of their flow allocation philosophy on a given design with 3% power in the radial blankets. As the By Pass and radial blanket flows increase, the driver region flow fraction decreases, leading to increased driver region outlet temperature, and temperatures throughout the driver region are correspondingly higher.

The bypass flow and radial blanket flow allocation philosophy is also shown in Figure 3.3.1 in terms of the maximum midwall cladding temperature. The temperatures shown are for the normalized design at 950°F reactor vessel outlet temperature with the core orificed for equal end-of-life (EOL) midwall cladding temperature in each driver assembly. It is representative of the best orificing could achieve for a balanced core wide cladding temperature orificing philosophy. The point marked by the reactor vendor's identification reflects their flow allocation strategy. As can readily be seen, the best achievable midwall cladding temperature increases severely with By Pass flow and radial blanket overcooling. The design approach

Figure 3.3.1

EFFECT OF BYPASS FLOW ON CLADDING TEMPERATURE - HIGH TEMPERATURE NORMALIZED DESIGN

employed by GE in their low temperature design (i. e., very high bypass) is off-scale on this figure. The cladding temperatures for the low temperature normalized design are 75°F less than shown in the figure.

In order to judge properly the philosophy of high By Pass, the overall core and primary system implications must be examined. The benefits of high bypass, as noted above, is reduced core pressure drop. This leads to reduced pumping power which has an effect on the reactor optimization, depending on the economic weighing factor for pumping power. It also leads to thinner duct walls and reduced steel volume fraction. Thus, a nuclear performance improvement can be realized. The higher duct temperatures and cladding temperatures, in particular, are the serious drawback to this design approach. The assembly and fuel pin temperatures strongly affect core residence time in terms of cladding cumulative damage, cladding strain, and duct-bundle interaction. The use of high By Pass in the low temperature design seems to negate the low temperature benefits of the low temperature design. As noted in Appendix I, the maximum EOL midwall temperature of GE's high and low temperature designs are virtually the same. Again, it should be noted that this design approach has been abandoned by GE.

3.3.3 Driver Region Cooling

Coolant is allocated in the driver region in order to reduce spatial temperature variation due to the non-uniform power distribution. There are various means at the disposal of the designer to accomplish this, including methods for power flattening and high efficiency flow orificing. Increasing the number of enrichment zones will flatten radially the power generation profile. Power shifting in the core region of the reactor can be adjusted to some extent by the appropriate positioning of control assembly over the refueling cycle.

After power flattening methods have been employed, flow is then distributed radially in order to reduce temperatures in the high power driver assemblies. Generally, the driver region is divided into a number of zones within which all assemblies receive the same flow. There are various criteria for allocating flow, most of which are focused on reducing hot cladding temperatures. The intent is to improve fuel reliability for the design burnup - i.e., to reduce the fuel failure probability to an acceptable level. Ideally, all temperature-dependent, life-limiting fuel pin/fuel assembly phenomena should be investigated for each assembly or group of assemblies. The results of this investigation would establish maximum allowable temperatures for the assemblies. Flow would then be allocated so that these limits would not be exceeded. As a first approximation, the end-of-life cladding temperature could be minimized. This approach was used in the initial orificing of CRBR.^{3.3.2} Subsequent effort on the CRBR project involved re-allocating flow to the high-powered assemblies by allowing the low burnup pins to run slightly hotter.^{3.3.3}

Both AI^{3.3.4} and GE^{3.3.1} divided the driver region into a number of radial rings which contained assemblies of similar power level and power gradients. Flow was allocated to these rings (orifice zones) to limit the maximum cladding temperature in each zone. Other than use of two enrichment zones, no apparent power flattening method was employed to reduce the temperature level. AI did employ a half-wire wrap on the edge pins of the assembly. The improved assembly flow distribution resulted in a 30-35°F reduction in maximum cladding temperature. This has been discussed in some detail in Section 3.2 above.

WARD^{3.3.5} employed more enrichment zones to help flatten power. The large commercial target core used four enrichment zones and the prototype core employed three, whereas the other two vendors used the conventional two-zone core. WARD did employ some form of control rod patterning in order to flatten cycle power swings. The detailed orificing study used only 3-4

orifice zones, which was less than the 5 used by AI and the 7 used by GE. However, WARD did employ a more efficient orificing scheme by limiting each assembly to the cladding lifetime limiting temperatures or transient limiting temperatures. It was not clear from discussion with WARD that this detailed orificing scheme was employed in their initial optimization and trade-off studies.

The normalized designs employ a three enrichment zone core. The end-of-life (EOL) cladding midwall temperatures were equalized in each orifice zone as a first approximation to ensure fuel pin reliability. The effects of the number of orifice zones on EOL cladding temperature and peak-powered assembly outlet temperatures are shown on Figure 3.3.2. Large improvements are obtained for the first seven orifice zones; further improvements show smaller gains. Complete, assembly-by-assembly orificing was employed in order to illustrate achievable cladding temperatures with efficient orificing. For the high temperature normalized design, the maximum EOL cladding temperature at the 2σ confidence level is 1173°F . The low temperature design would be 75°F less at 1098°F .

It is not necessarily the case that complete and/or high efficiency orificing is the best route in all design approaches. As noted in Figure 3.3.2, the increase in the number of orifice zones is accompanied by a reduction in assembly outlet temperatures in the high power assembly. This is due, of course, to the increasing flow in this assembly, and it results in higher coolant velocity and assembly pressure drop. The higher pressure drop may require thicker duct walls, but certainly the primary coolant pumping power must increase. The increased pressure on the duct wall counteracts some of the duct dilation benefits obtained from reduced duct wall temperatures. The main benefits of the reduced temperature

Figure 3.3.2

EFFECT OF ORIFICING EFFICIENCY - HIGH TEMPERATURE NORMALIZED DESIGN

levels would be low fuel pin strain and damage for a given lifetime or a potential longer lifetime. The duct bundle interaction, which is dependent on temperatures but relatively insensitive to coolant pressure, would certainly be improved and perhaps allow longer core residence time. Appropriate redesign of the assembly could be instituted, which would reduce the coolant velocity, pressure loss, duct wall thickness, and inter-duct gap and thereby achieve some improvement in core nuclear performance.

The effect of reducing the core to two enrichment zones was briefly examined. This core, producing the same total reactor power as the three-zone core, operated at slightly higher peak linear pin power. Complete orificing of this core resulted in a maximum EOL midwall cladding temperature that is within 1°F of the complete orificing of the three-zone core. The flow in the high power assembly, however, is somewhat higher than the case of the corresponding assembly in the three-zone core. Thus, the steeper radial power profile of the two-zone core could be compensated with orificing but at the expense of high flow, high pressure drop effects in the core. Thus, more enrichment zones are still beneficial even with high efficiency orificing.

Unfortunately, there was no time or available information to determine more quantitatively the benefits of control rod motion to flatten power. However, it is expected that appropriate use of control could help relieve some of the coolant velocity and pressure drop effect concomitant with high efficiency orificing.

3.3.4 Discussions of Calculations

The main calculations performed in order illustrate the effects of the overall reactor cooling strategy, and orificing was the maximum midwall cladding temperature at the 2σ confidence level. All calculations use the thermal-hydraulic design conditions listed in Section 9.0.

A driver assembly with power and flow characteristics representative of the high power assembly in any of the reactor vendor's cores was established for comparative hot channel calculations. Each vendor's hot channel factors and methods were applied to this assembly to determine the hot midwall cladding temperature. The results of all calculations agreed to within 10°F of each other. Thus, no substantial temperature differences could be attributed to hot channel analysis. One method and set of factors were selected, for convenience, to be used in the study of the normalized designs.

All reactor vendors applied their hot channel factors to nominal temperatures, which included the effect of flow maldistribution and mixing. Computer code ENERGY was employed in the normalized design to determine the intra-assembly flow maldistribution effect, the location of the hot channel, and the effect of interchannel coolant mixing in the hot channel. These results were subsequently used in the hot cladding temperature calculation of the orificing study.

Based on the neutronic analysis of the two and three-zone normalized design, driver assembly power histories were obtained. The EOL powers of each assembly were used to determine cladding temperature as a function of assembly flow. These equations, along with the continuity equation, were solved to yield equal EOL cladding temperature in each zone. Driver assembly mass flow rates and outlet temperatures were then calculated.

Pressure drop calculations were performed following the determination of assembly flows, using conventional hydraulic calculation methods. Pin bundle friction losses were calculated using the Novendstern friction factor multiplier. Maximum design pressure losses were calculated by increasing all pressure losses by 20% except the bundle losses, which were increased by 14%, in accordance with the accuracy estimate of the Novendstern multiplier factor. The maximum pressure losses were used

to generate the duct wall pressure profile use in the duct dilation calculations discussed in Section 3.2.

3.3.5 Conclusions

The results of the investigation described above lead to several general conclusions and comments on the PLBR core study.

(1) The hot channel factors and calculation procedures employed by the several vendors have no impact on the high vs. low temperature decision made by each organization. Application of each vendor's method to a given assembly lead to similar results. Unusually high or low temperatures cannot be attributed to this aspect of the calculations.

(2) Most of the designs were inefficiently cooled since high flow bypass and significant radial blankets overcooling were employed; AI is an exception in this case. Significant reductions in the level of core temperature could be achieved with more careful flow allocation. The temperature dependence in the material properties, which lead to life-limiting conditions, make the reactor cooling strategy a very important consideration. This is particularly important for the high temperature designs and the economic evaluation of their efficacy relative to the low temperature designs.

(3) Most of the designs did not include design features which would improve the core temperatures. More enrichment zones and control assembly motion can be used to flatten power and reduce temperatures at lower assembly flow requirements. WARD is an exception here, but they employed only a few orifice zones. AI improved assembly flow distribution and temperatures with the half-wire diameter design feature.

(4) With regard to items (2) and (3), no vendor performed trade studies involving the effects of the overall reactor cooling strategy (i. e., By Pass cooling and radial blanket overcooling), improvements in power flattening, and efficient orificing. Improvements in temperature are accompanied by

higher flow which lead to higher pressure drops, thicker duct walls, changes in interduct gap and potentially longer core residence times. Power flattening techniques allow for some improvements with less high flow impact. The higher flow impact affects the economic evaluation through pumping power, core size and nuclear performance, and fuel cycle length.

References:

- 3.3.1 PLBR High/Low Outlet Temperature Evaluation Discussions, General Electric BRD Presentations, February 16-17, 1977.
- 3.3.2 CRBRP, Preliminary Safety Analysis Report, Chapter 4.4.
- 3.3.3 Carelli, M.D., Bach, C.W., Engel, F.C., and Spencer, D.R., "Predicted Preliminary Thermal-Hydraulic Performance of CRBRP Fuel and Blanket Assemblies," WARD-D-0054, Westinghouse Electric Corp., Advanced Reactors Division, January 1976.
- 3.3.4 PLBR High/Low Outlet Temperature Evaluation Discussions, Atomic International Presentations, February 14-15, 1977.
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3.4 Reactor Vessel, Head and Internals

The AI and W use of identical fuel pins and almost identical lattice spacings for their high and low designs results in a slight increase in core diameter for their reduced thermal efficiency low temperature designs. These increased core sizes are reflected in larger rotating plugs, core barrels, reactor vessels, and increases in containment building diameter. The AI and W addition of an extra row of fuel increases the core barrel and upper internals (UIS) diameter by 13" as shown in Table 3.4.1. The need to rotate the UIS in an offset position during fuel handling to a position tangent to its original position doubles the diameter impact on the rotating plugs. Applying equal diametral clearances, the reactor vessel diameter, therefore, is shown by both vendors to grow at essentially twice the core diameter growth rate. GE's approach in overall system optimization results in a slightly smaller low temperature core diameter, since the effect of the reduced pin diameter and smaller fuel assemblies offsets the extra row of fuel. A net core and vessel diameter decrease of 6" occurs in going from the GE Hi to the GE Lo design, with an equal decrease in the length and width of the rectilinear containment building. GE directly couples core, vessel, and containment size.

A common concern exists among the vendors for transient effects. Effective control of transients requires that reactor outlet plenum volumes must increase as core thermal power to maintain thermal transients within tolerable levels. This is one advantage of AI and W's increase in vessel size with the low temperature cores. The counteracting effect of enhanced structural properties at the lower temperature might not require an equivalent vessel increase for each core diameter increase.

A strong capital incentive exists for the vendors to contain or limit the effects of slight core diameter changes within a given vessel, rather than to pass them on in a direct scaleup manner through to the containment building I.D. without attenuation. However, the philosophy of applying equal margins and clearances in the high/low comparison for a preliminary study justifies the approach used.

Table 3.4.1

Vessel, Head, Internals Diameter Change

PARAMETER	AI		GE		W	
	Hi	Lo	Hi	Lo	Hi	Lo
Fuel - no. of rows	Ref.	+ 1	- 1	Ref.	Ref.	+ 1
ΔD Core Barrel, in.	Ref.	+13	+ 6	Ref.	Ref.	+13
ΔD Vessel, in.	Ref.	+29	+ 6	Ref.	Ref.	+32
ΔD Head, in.	Ref.	~+29	+6	Ref.	Ref.	~+32
ΔD UIS, in.	Ref.	+13	+6	Ref.	Ref.	+16
ΔD Containment, in. Bldg.	Ref.	+29	+ 6	Ref.	Ref.	+32

3.5 Fuel Handling System Impact

The impact of the high/low question on the Fuel Handling System is limited to changes in the physical size of the fuel assemblies and the effect of the number of assemblies which need to be transferred or held in storage and cooled in the EVST.

All vendors except GE (15 in. change) held assembly length constant between the high and low designs, with little change in the assembly outer configuration or weight. Hence, little change is required in the design of the IVIM and transfer tunnels. All vendors use an under the plug IVIM which delivers spent assemblies to an inclined tunnel transfer station similar in manner to the Phenix system. The Westinghouse system, as tabulated in Table 3.5.1, uses a tunnel which penetrates the vessel wall, whereas the AI and GE tunnels penetrate the stationary roof (head) structure. The W design uses two rotating plugs with a fixed offset handling arm, whereas AI and GE utilize three rotating plugs with straight pull handling machines. All equipment and capital costs are held constant between the high/low designs except for the W case. W increases the EVST diameter from 33.5' to 35' to handle the extra storage requirements. In doing so, however, W increases the EVST cost from \$31.5 M to \$38 M, for a \$6.5 M net fuel handling cost penalty for the low design. An apparent scale exponent of 4.2 leads to the conclusion that an expensive modification to the rack storage and tank geometry is anticipated to handle the slightly increased storage requirements.

W charged the low temperature design with an unavailability penalty of eight extra hours per year, since the low temperature design handles 15-20 more fuel, blanket, control and shield assemblies per year.

AI singled out an availability improvement creditable to the low temperature fuel handling system of 0.14% due to the reduced number of assemblies to be handled per year. Table 3.5.1, shown below, summarizes the refueling high/low variation.

Table 3.5.1

Fuel Handling System Impact

Parameter	AI	GE	W
Refueling Type	Under the plug - straight pull Inclined tube thru head	Under the plug - straight pull Inclined tube thru head	Under the plug - Fixed offset arm Inclined tube thru vessel well
No. of Rotating Plugs	3	3	2
Refueling Downtime, days Hi/Lo	21/20.5	~21/same	21/21.3
Fuel Handling Δ Cost, 10^6 \$	Base/same	Base/same	Base/+6.5

4.0 Fuel Cycle Costs, Core Related Capital Costs and Unavailability Costs

4.1 Fuel Cycle Costs

Fuel cycle costs quoted by the vendors ranged from 2.90 to 6.38 mils/kwh with AI and GE crediting the low temperature design with an advantage and W crediting the high temperature design with the lowest costs.

Page I-16 (App.I) lists the vendor assumptions and resultant fuel cycle costs as determined by AI (SOC-II with Stauffer, Palmer and Wyckoff correlations), GE (COROPT with S, P & W correlations) and WARD (S, P & W correlations).

WARD's detailed analysis using the Investment-Time Method were only performed for the high temperature case, hence the substitution of S, P & W calculations in their place.

Assumptions used by the vendors were not consistent with each other since the ERDA/EPRI guidelines were not issued until after Phase I. AI's costs were consistently less than W and GE, primarily due to an earlier starting date, higher load factor, lower reprocessing costs than GE, and lower fuel fabrication costs than W. AI credited its fuel cycle costs with a large lifetime inventory value gain due to anticipated Pu price escalation above and beyond the purchase price. All of these factors cause AI's resulting fuel cycle costs to end in the 3 mil/kwh range. However, the degree of documentation, internal consistency, and conformance to ERDA/EPRI guidelines was greatest with the AI fuel cycle costing. In face, most of the differences pointed out above are due to AI strictly following the guidelines, but GE and WARD using commercialization assumptions for analyses that were made before the guidelines were issued. The bottom line for AI is that a 0.36 mil/kwh fuel cycle advantage is predicted with the low temperature design, primarily due to longer residence (lower fabrication and reprocessing use charges) and increased fissile Pu production which offsets the increased carrying charges associated with the greater inventory.

GE results indicate a 0.77 mil/kwh advantage for the low temperature case. An increase in cycle length from 788 days to 868 days for the low temperature case for a 2% smaller inventory leads to lower use charges. More kilowatt hours can be sold from essentially equal cost cores. However, carrying charges

are almost identical, since inventories and electric power output are essentially equal.

Westinghouse reports higher fuel cycle costs with the lower temperature design. Both systems have a 3 year residence for driver exposure. However, the W low temperature design has a 14% greater system fissile inventory. More fuel must be fabricated and reprocessed ('use charges') and carried as a constant life inventory at 15.56% ('carrying charge'); yet, these costs must be written off over the same total amount of kilowatt-hours generated. Increased Pu generation slightly offsets these charges, but not enough. The result is a 0.80 mil/kWh higher fuel cycle cost penalty for the low temperature system according to WARD calculations.

To verify the reported trends and remove inconsistencies due to assumptions, C-E set up investment-time diagrams for all six cores on an annual basis. C-E then determined the single cycle, 0% inflation use charges and translators for carrying charges and a 16 year levelization period (further described in Appendix III - Fuel Cycle Cost Methods and Appendix IV - HEDL Fabrication Cost Approach). The resulting fuel cycle costs are given in Table 4.1.1. C-E used the HEDL formula for fabrication costs with a 100 MT/year plant at 70% capacity. Although the axial blanket cost contribution is neglected in the method, the use of a 70% capacity facility brought all three vendors' fabrication costs to within the \$50-65,000 range. ERDA/EPRI guidelines on reprocessing cost, Pu value, capital charge rates, capacity factor, startup year and levelization period were used. The resulting fuel cycle costs determined by C-E verify the reported trends. In comparison, the C-E normalized design fuel cycle costs are shown in Table 4.1.1. These results are slightly higher since a greater inventory is being carried relative to a reduced total amount of kilowatt-hours generated.

Table 4.1.1

CALCULATED FUEL CYCLE COSTS
(Levelized from 2000-2016 mills/kWh)

	AI		GE		W		Normalized	
	High	Low	High	Low	High	Low	High	Low
<u>USE COSTS⁽¹⁾</u>								
Fabrication	1.39	1.13	1.67	1.40	1.40	1.60	1.94	1.65
Reprocessing	1.48	1.34	1.63	1.29	1.60	1.89	1.91	1.68
Pu, Net Created	<u>-1.40</u>	<u>-1.61</u>	<u>-1.28</u>	<u>-1.19</u>	<u>-1.48</u>	<u>-2.14</u>	<u>-1.44</u>	<u>-1.56</u>
	1.47	0.86	2.02	1.50	1.52	1.35	2.41	1.77
<u>CARRYING CHARGE COSTS</u>								
Fabrication	0.58	0.43	0.72	0.69	0.53	0.60	0.80	0.94
Reprocessing	-.31	-.36	-.36	-.31	-.43	-.51	-0.39	-0.46
Pu, Net Created	0.29	0.44	0.31	0.33	0.40	0.58	0.30	0.43
Pu, Plant Constant	<u>2.29</u>	<u>2.48</u>	<u>2.12</u>	<u>2.09</u>	<u>2.65</u>	<u>3.00</u>	<u>2.71</u>	<u>3.27</u>
	2.85	2.99	2.79	2.80	3.15	3.67	3.42	4.18
Normalized Totals ⁽²⁾	<u>4.32</u>	<u>3.85</u>	<u>4.81</u>	<u>4.30</u>	<u>4.67</u>	<u>5.02</u>	<u>5.83</u>	<u>5.95</u>
(VENDOR QUOTED TOTALS)	(3.26)	(2.90)	(5.31)	(4.37)	(5.58)	(6.38)		

4.3

(1) No credit taken for lifetime inventory escalation.

(2) Costs include the effect of 6% inflation over the levelizing period (2000 - 2016). Inflation prior to 2000 is not included.

4.2 Core Related Capital and Unavailability Costs

The direct core effects of the high/low designs on capital costs are their impact on reactor internals, vessel, guard vessel, head refueling and containment size and cost. Primary pump and piping incremental cost changes are being separately addressed by another ERDA contractor.

The AI and WARD use of a similar core lattice spacing results in a larger low temperature core diameter due to the extra fuel row needed to maintain equal net electric power. As shown in Table 4.2.1, AI and WARD increased the core barrel, head and UIS diameters directly with the core. The need to provide diametral space for rotation of the UIS to an offset position doubles the diameter impact on the head, vessel and guard vessel. GE directly couples the core, vessel and containment diameters with equal increments for each.

AI uses a rough costing approach in SOC-II that generates relative costs of vessel and head using the tonnage of steel and length of welds. Wall thicknesses vary with seismic requirements. No improvements due to enhanced low temperature structural properties are assumed. Costs basically result as a function of overall weight. WARD performed detailed cost calculations, determining increased weight and weld requirements to obtain a net difference. No improvements to upper internals design or simplification of vessel liners and bypass cooling requirements are assumed.

GE operates the vessel entirely within the low temperature ASME Code ($T < 800^{\circ}\text{F}$), through utilization of bypass cooling. No credit was taken for this improvement in the Phase I comparison, however. COROPT contains a set of reference vessel and containment costs which are perturbed in the following manner (resulting in an approximate cost exponent of 1.5 on diameter):

$$\text{Vessel Cost} = \text{Reference Cost} \times \left(\frac{\text{Ref. Diam.}^2}{\text{New Diam.}^2} \right)^{\frac{1}{\left(\frac{\text{Ref. Diam.}}{\text{New Diam.}} + 1 \right)}}$$

The new building cost is obtained from the reference building cost with a 1.6 exponent on equivalent diameter, and a 1.0 exponent on height.

The quoted vessel, head, internals, and guard vessel costs for the three vendors are listed in Table 4.2.2. A preliminary C-E manufacturing estimate concurs with

GE's and AI's reported differential vessel and head cost range, each of which falls below a million dollars per foot change. WARD, however, reports an incremental cost due to a 2.67 foot size increase of \$3.25 million for the vessel and \$8.70 million for the head. These both appear subject to greater scrutiny. Table 4.2.2 shows a net vessel, head and internals penalty for the WARD low design of \$13.5 million. Addition of the effect of a larger ex-vessel storage tank (+\$6.5 million) and a 2.67 foot containment building increase (+\$.8 million) increase the WARD low temperature core-related cost penalty to \$20.8 million. GE assigned no vessel, refueling, or EVST cost penalty to the low temperature design even though the low temperature fuel assembly is 15 inches longer.

AI added a further penalty of \$3.7 million for an increased number of control rods for the low temperature design. Additionally, an extra \$2.3 million was added for the larger low temperature containment building. Thus, the AI low temperature design differential (core related) penalty is \$10.4 million.

Table 4.2.3 lists the above added core-related cost items quoted by the vendors to obtain the net high/low capital cost differential.

The net differential costs including containment, control rod and EVST effects, also given in Table 4.2.1, can in turn be related to the incremental core change. Dividing the incremental costs by the incremental core diameter provides a very interesting normalized cost sensitivity per inch of core O.D. change. The bottom line of Table 4.2.1 shows GE's \$0.12 million/inch core sensitivity vs. AI's \$0.84 million/inch, and WARD's \$1.5 million/inch value.

Incremental capital costs other than the direct costs of the vessel, head and internals, are judged to be too tenuously connected to core size to be included in this kind of analysis. Consequently, for the normalized design, an approximate \$4.5 million capital cost increase was estimated for the low temperature design based upon an exponential cost scale factor of 0.6 applied to the cost of the vessel, head and internals. These costs were estimated to be \$ 45 million by averaging AI and GE quotes. To determine the low temperature costs for constant mwt high and low designs, the ratio of electrical outputs (ratio of net efficiencies) was scaled by an assumed exponent:

$$\text{Cost}_{\text{low}} (\$/\text{Kwe}) = \text{Cost}_{\text{high}} (\$/\text{Kwe}) \times \left\{ \frac{\text{Mwe}_{\text{high}}}{\text{Mwe}_{\text{low}}} \right\}^{.6}$$

Using 893 Mwe for the high and 766 Mwe for the output of a low temperature core of the same thermal rating, the cost differential per Kwe was obtained. Then the total capital cost for the low temperature design was estimated using this value of \$/Kwe. The resulting penalty of approximately \$4.5 million almost exactly coincides with AI's estimate of \$4.4 million.

Unavailability increments attributed to differences between high and low temperature core designs are given in Table 2.5. These can be primarily attributed to differences in downtime for refueling. No credits or penalties were assumed by any vendor for unavailability changes due to phenomena such as sodium frosting or UIS striping damage. Also, all vendors kept equal capacity factors for their fuel cycle cost calculations with no temperature differences noted.

AI and WARD defined refueling outage differences between the high and low temperature designs. AI transfers fewer assemblies with the longer residence low temperature core during the annual refueling outage; thus resulting in a 0.14% availability gain (10 hours) factor, which only appears in the AI calculation of differential energy costs. There it results in a .04 mill/kwh low temperature design credit. WARD determined that its low temperature design requires an extra 8 hours refueling downtime per year, but did not penalize the energy cost or capitalize the result. GE's approach throughout the effort was to point out key areas of technical risk and minimize or contain them within acceptable bounds. No quantification of risks in terms of core-related incremental unavailability were determined, however.

TABLE 4.2.1

REFERENCE DESIGN PARAMETERS OF VESSEL, HEAD & INTERNALS

	AI	GE	W
VESSEL DIAMETER (FT.)	44.5 upper 23.2 lower	44	25
EFFECT OF LOWER TEMPERATURE ON CORE AND CORE BARREL DIAMETER (IN.)	+13	-6	+13
EFFECT OF LOWER TEMPERATURE ON UPPER INTERNAL STRUCTURE (IN.)	+13	-6	+16
EFFECT OF LOWER TEMPERATURE ON ROTATING PLUGS (IN.)	+13	-6	+16
EFFECT OF LOWER TEMPERATURE ON EVST DIAMETER (IN.)	NONE ASSUMED	NONE ASSUMED	+18
EFFECT OF LOWER TEMPERATURE ON CONTAINMENT DIAMETER (IN.)	+29	-6 (LENGTH, WIDTH)	+32
\$ EFFECT OF SIZE CHANGE TO LOWER TEMPERATURE (10^6 \$)	+10.4	-0.75	+20
\$ EFFECT OF SIZE CHANGE TO LOWER TEMPERATURE (10^6 \$/IN. CORE O.D.)	+.80	-.12	+1.5

Table 4.2.2

Vessel, Head, Internals and Guard Vessel Costs

Direct Costs, 10 ⁶ \$ (1975)	AI		GE*		<u>W</u>	
	Hi	Lo	Hi	Lo	Hi	Lo
Reactor Vessel	18.62	19.77	10.5	10.1	REF.**	+3.25
Head	21.59	23.83	14.8	14.5	REF.**	+8.70
Internals	11.94	12.98	3.0	2.9	REF.**	+0.77
Guard Vessel	8.11	8.13	1.4	1.5	REF.**	+0.40
Sum	60.30	64.70	29.7	29.0	REF.**	+13.1

Table 4.2.3

Added Vendor Core-Related Items

Direct Costs, 10 ⁶ \$ (1975)	AI		GE*		<u>W</u>	
	Hi	Lo	Hi	Lo	Hi	Lo
Reactor Vessel, Head, Internals & G.V.	REF	+4.4	+.7	REF	REF **	+13.1
Increased Control Rods	REF	+3.7	same	REF	REF	same
Increased Size EVST	REF	same	same	REF	REF	+6.5
Increased Size Containment	REF	+2.3	~0	REF	REF	+0.8
Net Core Related Diff.	REF	+10.4	+.7	REF	REF	+20.4

* Fifth of a kind preliminary estimates.

** W maintains as proprietary information its costs of reference design (5th of a kind).

5.0 Plutonium Utilization

5.1 Plutonium Inventory

Driver fissile material dominates the reactor system inventory. The most important factor in critical fissile loading is total fuel mass. Fissile loading increases directly with total fuel mass. Total mass is determined by 1) the number of pins, 2) the driver height, and 3) the pin I.D. and fuel density. For all reactor vendors (RVs) the number of fuel pins increased as coolant system temperatures dropped. For AI and WARD cores, the number of pins increased directly with core power. GE elected to increase the fuel peak linear power rating by 10%, resulting in major inventory savings relative to AI and WARD as temperature was varied. Driver heights were held constant. Only WARD and GE fuel pin I.D.s varied as system temperatures decreased (increased/decreased respectively). Assembly mechanical design plays a secondary role in fissile loading requirements. As the mass of steel or coolant associated with a fuel pin increases, fissile loading increases. Only WARD and GE steel masses varied significantly with falling coolant system temperatures (decreased/increased respectively). A small specific inventory (thermal) benefit is generally realized for increasing reactor size benefiting low temperature cores (the number of pins increases as temperatures are lowered). Because of larger core size, the critical enrichment would decrease about 0.8% for a low temperature core. (The effect of reactor size on the breeding ratio, however, is negligible within the range of thermal powers investigated by the RV's.)

The reactor system inventory includes fuel outside the core; in storage, in reprocessing, etc. Assuming that the reprocessing cycle is of fixed length, the fraction of system fuel outside the reactor decreases with in-core irradiation period. AI and GE propose that fuel life is extended at low temperature, reducing the out-of-core fuel storage fraction. WARD and AI calculate (total) system inventory increases with falling temperature. GE predicts very little change.

Normalized high and low temperature designs were investigated independently to assess inherent differences in reactor performance as temperature is varied (see Section 9). Pin design and core thermal power were fixed for this

analysis. The low temperature design has an enrichment somewhat less than that of the high temperature design. With reduced temperature, fuel life is extended from 2.0 to 2.7 years, reducing the out-of-core fuel storage fraction. Reactor specific inventory (electrical) increases about 8%.

The AI and C-E studies utilizing fixed pin designs indicate that assembly life can be extended significantly at lower temperatures. Reactor specific inventory (electrical) increases, however. GE modified its fuel design with decreasing temperature in such a way as to achieve minimum cost. A small increase in specific inventory was the result. WARD maintained the external dimensions of its fuel pin, and chose to fix fuel life as temperature decreased. By thinning the cladding, WARD drastically increased the system fuel loading as temperatures fell. System inventory increases generally occur with decreasing reactor temperature.

5.2 Breeding Ratio

The most important factor in achieving a large breeding ratio is maximizing the fuel material density in the core. Design changes which tend to decrease the breeding ratio include increasing the pin pitch to diameter ratio (P/D), increasing the duct wall thickness to duct pitch ratio, increasing the cladding thickness to diameter ratio (t/d), and decreasing the fuel smear density. Fuel smear densities were not varied by the RVs with temperature. AI and WARD fixed assembly pressure drop, pin O.D., and peak linear power resulting in essentially constant P/D ratios as system temperatures varied. GE varied assembly pressure drop, pin O.D., and peak linear power as temperature was reduced. By increasing core ΔP by 8%, the P/D ratio, which increased, was kept relatively low. Only WARD varied the duct wall thickness to duct pitch ratio significantly (decreased). WARD decreased cladding t/d and GE increased cladding t/d as coolant system temperatures dropped.

A second factor in breeding ratio determination is the driver fission product density. As fuel life is extended at low system operating temperatures, breeding suffers. A third important factor for breeding is the blanket configuration. This was maintained by the RVs as temperatures were varied.

5.3

The overall effect of decreasing system operating temperature on the breeding ratio varied among the RVs. AI calculated little change with temperature. GE found a 0.04 decrease and WARD found a 0.04 increase.

The effect of operating temperature selection on the breeding ratio is small for the normalized designs. At reduced operating temperatures, less allowance is made for duct dilation (interassembly spacing). The fission product density is increased somewhat with increased fuel residence. A small overall degradation in breeding is observed.

Due to the decreasing influence of irradiation damage on fuel designs at reduced temperatures, it is clear that if fuel life is fixed, structural requirements for fuel assemblies decrease with temperature. If pin O.D. and power are maintained, the breeding ratio will improve. As fuel life is extended, structural requirements increase and fission product buildup occurs for a low temperature core. C-E predicts a 0.7 year life extension and a small degradation in breeding. AI predicts a 1.0 year life extension and little change in breeding. GE balances core inventories and breeding characteristics for its cores to maintain equal doubling times for high and low temperature designs. The optimum cost GE low temperature core has a lower breeding ratio than the optimum cost high temperature core. The general effect of reduced operating temperatures is a reduction in assembly structural material resulting in some breeding improvement. However, increased fuel life (fission product buildup) tends to decrease the breeding ratio, and the net effect of reactor coolant temperature reduction is small.

5.3 Doubling Time

The inventory used for doubling time calculations is the beginning-of-equilibrium-cycle reactor system inventory. The plutonium production rate is proportional to the product of reactor power and the breeding ratio. Plutonium losses due to refabrication of fuel and Pu-241 decay are subtracted from plutonium production to evaluate net fissile production and doubling time. The effects of coolant temperatures on system inventories and breeding ratios were discussed in Sections 5.1 and 5.2 and are summarized in Table 2.3.

The variations in cycle length discussed in Section 5.1 are important with regard to system plutonium losses. As cycle length increases, reprocessing occurs at longer intervals, reducing fabrication losses. As system inventory decreases due to reduced ex-core inventory, fissile plutonium decay losses are reduced.

AI predicts a one-year doubling time improvement as coolant temperatures decrease from 950 to 875⁰F. The improvement in the out-of-pile fuel fraction with increasing fuel residence, and the increase in the plutonium production rate with increased core power more than offset the required increase in in-pile mass for the low temperature core.

GE maintained a 14 year doubling time (at 80% load factor) as temperatures decreased and reduced fuel cycle cost by modifying core design. The system inventory was relatively insensitive to operating temperature. The increase in core power offset the decrease in breeding ratio to sustain a high plutonium production rate as operating temperature was reduced.

WARD predicts a five-year doubling time benefit due to temperature reduction. The WARD doubling time improvement was dominated by a 0.04 breeding ratio increase which resulted largely from the change in fuel assembly structural content due primarily to reduction of cladding thickness.

The normalized core analysis predicts a one year doubling time improvement as coolant temperatures decrease from 950 to 875⁰F. The improvement in the out-of-pile fuel fraction with increasing fuel residence more than offsets the minor degradation in plutonium production for the low temperature core to insure a doubling time improvement.

Section 5.1 concluded that specific inventory (electrical) generally increases as reactor coolant temperatures are reduced. The constancy in the breeding ratio predicted in Section 5.2 and the increase in core thermal power as temperature is reduced combine to substantially increase the production rate of plutonium for a low temperature breeder. Doubling time falls with reactor coolant temperature.

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- 5.3 Private Communication E. Paxson (WARD) to R. J. Bergeron, "Contractor Summary of PLBR Design and Operating Characteristics", March 17, 1977.
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6.0 Risks, Reliability and Licensing

General Electric has suggested that the high temperature designs carry with them a substantial increase in risks. The risks to which these designs are thought to be exposed include: failure to meet economic and plutonium utilization objectives, the possibility of licensing delays, cost over-runs, and low operating availability. These risks are thought to be most strongly applicable to the permanent components of the system: the reactor vessel, the upper internals, the steam generators, and the turbine generators. However, it is also suggested that there may be significant risk associated with the core performance.

Since all components of the core are replaceable and have a short life relative to the life of the plant, the core is inherently less exposed to the sort of risks contemplated. Nevertheless, the risk of excessive fuel cycle costs and perhaps the risk of low availability need consideration. Should the fuel or fuel assemblies fail to operate for their expected lifetime, then fuel cycle costs would be increased and availability would be reduced. It would seem that this risk is very much associated with the promised life of the fuel. If the reactor vendor over-estimates the performance of his fuel, whether it operates at low or high temperature, then the risk of performance being less than anticipated is substantial. This would seem to be the most critical factor, not the design operating condition for the fuel. The current state-of-the-art is such that satisfactory fuel lifetime can be projected with very little risk. While it is true that, based on current means of analysis, projected fuel life is greater for the low temperature design than for the high temperature design, so long as the method of prediction is essentially the same, it would not appear that a significantly higher risk is associated with the high temperature fuel life prediction.

There is certainly no risk at all that completely unsatisfactory performance will result with either design. One needs only to look at the performance of the Phenix system to see that at least marginally satisfactory fuel behavior is currently achievable.

While, it may be argued that the properties of the cladding and duct material are less well understood for the higher temperatures than they are for the lower temperatures, the benefit to be gained from this better understanding at lower temperatures is either in the area of increased fuel lifetime, or in the area of improved plutonium utilization using thin clad for example. Increased

fuel lifetime means that, other things being equal, fuel burnup will be higher for the lower temperature fuel. Since experimental data at high burnup is less extensively available than data at low burnup, it can be argued that increased burnup should be associated with increased risk. The fuel irradiation data base becomes smaller and smaller as one extends a fuel burnup to higher and higher values. Consequently, it would appear that risks avoided by reduced temperature are, to a certain extent, balanced by increased risks due to a reduced data base in the experimental area.

If one takes advantage of better material properties at lower temperatures in order to gain improved plutonium utilization without extending fuel life by using thin clad and other design features of this kind, then it can be similarly argued that there are some increased risks associated with these design features. The data base on fuel with thin cladding is currently much smaller than the data base on irradiated fuel with more robust cladding. This situation can be expected to continue for the indefinite future.

It would appear that, at least so far as the core is concerned, one cannot avoid certain risks in the performance of the fuel and the core whatever approach is taken. Taking advantage of high temperature to get high efficiency may involve a risk associated with greater uncertainties of cladding and duct material properties; however, one can compensate for this uncertainty by demanding somewhat less of the performance of the fuel. This lesser performance, whether it be lifetime or in some combination of lifetime and plutonium utilization, is probably affordable because of the higher thermal efficiency of the high temperature design.

Conversely, because of its disadvantage with respect to thermal efficiency, the low temperature core must set a target of performance in terms of fuel life or plutonium utilization or a combination thereof, which is more demanding than the high temperature design. Because of a lesser data base for high burnup, thin clad designs, there is a certain risk associated with this performance level.

In any event, the prudent designer will not promise more than he can deliver within a reasonable level of risk, and so far as can be determined from the

current analysis, it does not appear that this risk would be any greater whether the temperature is at the higher or lower level.

If one grants that, despite the above arguments, the high temperature design carries with it a slight disadvantage of risk of failure to perform up to expectation, this design, nevertheless, would seem to have an advantage in its potential for improved performance. This means that even if a slightly greater risk is associated with this design, there is an associated advantage in that the potential for improved performance is greater. The normalized conservative designs developed as an independent reference in this study indicate that Core I steel can be utilized in a conservative design that would have an operating life of about 2 years. The low temperature normalized design developed on the same basis in a consistent manner would have a predicted life of 2.7 years. The analysis of temperature independent effects, presented in Section 7 below, indicated that fuel lifetime greater than 4 years would not benefit fuel cycle costs significantly, and would have a definite disadvantage so far as plutonium utilization is concerned. This would indicate that regardless of the temperature level, there is little to be gained beyond a life of 4 years for the fuel. Consequently, it would appear that a system starting with a relatively high thermal efficiency, but a relatively short life (2 years) has more room for improvement than a system starting from a life of 2.7 years and a lower thermal efficiency.

Of course, the improvement of fuel lifetime is not the only possibility so far as improvement of core performance is concerned. If better materials become available, then plutonium utilization can also be improved by improving the fuel fraction in the core and reducing the amount of structural material in the core. The potential in this area is probably not dependent on temperature. Nevertheless, the major economic advantage comes from improving fuel lifetime. Consequently, it would appear on balance that the high temperature system has a better potential for improvement. This greater potential for fuel cycle cost improvement may be balanced against any greater risks associated with higher temperatures. In any event, these balancing effects are judged to be relatively small in magnitude and should not be considered over-riding considerations. A major risk of a financial nature is not judged to exist in either case.

Licensing

The PLBR contractors were all requested to give their views as to the possibility that there might be some significant difference in the licenseability or the safety of these cores depending upon their design operating temperature. There was agreement universally that no significant basic difference exists between these two design approaches, and consequently, it was not thought to be very likely that there would be serious licensing delays or differences in licensing approach depending upon the design operating temperature.

Availability

So far as the question of availability is concerned, general agreement was not found. A non-specific qualitative disadvantage was assigned to the high temperature design by GE; however, other contractors did not see that this sort of penalty was justifiable. Small differences in availability were suggested due to differences in the time required for refueling the reactor system. Since the low temperature design has a larger number of fuel assemblies, it may have a larger number of fuel assemblies to refuel each year depending upon the life of these assemblies. One contractor calculated that this availability difference would favor the high temperature design. Analysis performed as part of this study indicates that this difference is quite small and may go either way. Certainly, if fuel life is eventually raised to the point where it is no longer temperature dependent, then there would be a small availability advantage for the high temperature design. This is a possibility that could result from advancement in material technology; however, it is not something that appears to be attainable with current technology or even modest extrapolations of current technology.

7.0 Fuel Life

7.1 Temperature Independent Effects

This study and the PLBR contractors studies of operating temperature have focused considerable attention on the effect of temperature on fuel pin and fuel assembly life. The reason for this is simply that there is a very substantial economic payoff associated with increased fuel life. This occurs because essentially fixed out-of-pile costs (fabrication, reprocessing and out-of-pile plutonium inventory charges) are spread over a larger block of net electric energy produced.

7.1.1 Fission Product Poisoning

Idealized calculations were made to quantify this effect in the absence of all temperature dependent effects. In these calculations, it was assumed that core geometry was completely independent of fuel life. The clad thickness, fission gas plenum length, duct thickness, gap between assemblies, fuel smear density and all other design parameters which are generally considered to be a function of fuel life and/or operating temperature were held constant. A core geometry similar to the normalized high temperature design was used. The purpose of the calculation was twofold:

1. To illustrate the strong influence of fuel life on fuel cycle cost and plutonium utilization in the absence of any temperature dependent effects, and
2. To examine the underlying effect of fission product poisoning on fuel cycle cost and breeding performance.

Results of these calculations are shown in Figure 7.1.1. Fuel cycle costs improve dramatically as the fuel life is increased from 1 to 2 years; improvement from 2 to 3 years is worth about 1 mil/kwh (at most) and improvement from 3 to 4 years is worth 0.5 mil/kwh (at most). Because of the way the calculations were done, these cost differentials represent upper limits; minimum design changes required to accomodate a longer life would reduce the benefit.

Note that even in the absence of design changes to accomodate longer life, plutonium utilization as measured by doubling time deteriorates beyond a fuel residence time of 3 years. Because of this deterioration of plutonium utilization and because little if anything is to be gained in fuel cycle costs, a practical upper limit of about 4 years residence time can be placed on fuel life, independent of operating temperatures.

FIGURE 7.1.1

EFFECT OF FISSION PRODUCTS ON COST AND DOUBLING TIME - CONSTANT CORE GEOMETRY

7.1.2 Plutonium Migration

The Westinghouse high temperature design using "N-Lot" cladding and duct material has a fuel life of 930 full power days (3 years at 85% load). This corresponds to a maximum burnup of 110 Mwd/kg. In its study of low temperature alternates, Westinghouse considered longer lived fuel which appeared permissible based on calculations of cladding CDF and duct dilation. This possibility of an extended fuel life was rejected at least partially because of a concern for the effects of plutonium migration at burnups in excess of ~110 Mwd/kg.

This concern is based primarily on the observations of D. C. Hata, et al (HEDL) on mixed oxide irradiated in EBR-II to a peak burnup of 12.5%. HEDL has provided a summary of this work which is reproduced in part in Appendix II. Review of this summary indicates that a firm burnup limit based on plutonium migration cannot be established at this time. At the most, this effect would appear to constitute a rather "soft" limit on fuel life.

A fairly large number of mixed oxide fuel pin irradiations have substantially exceeded 11-12% burnup without failure (see Section 7.6). Nevertheless, the data base is relatively thin for assurance of fuel integrity at very high burnup. Consequently, some additional risk must be associated with fuel operating to a very high peak burnup (~140-150 Mwd/kg).

7.2 Predicted Effect of Temperature on Fuel Pin Life

This section describes the contractors' methods and ground rules for establishing fuel pin lifetime. The effect of the high and low reactor outlet temperature on fuel pin lifetime is presented. Calculations showing the effect of varying the plenum size upon fuel pin lifetime are also discussed.

The WARD fuel pin for the high and low temperature designs had the same outer diameter, but the cladding was thinner for the low temperature case, i.e., 0.016" vs. 0.012". A proportionately greater plenum pressure was used for the assumed longer burnup of the low temperature design. The cladding temperatures were allowed to decrease through life from a peak BOL temperature of 1236°F for the high and 1186°F for the low. Fuel pin lifetime calculations were performed using the computer code FURFAN. Based on 0.4 value for the CDF (including steady state and upsets), a longer pin lifetime was calculated for the low temperature case (75% longer). However, slackening of the wire wrap as its growth in the axial direction exceeded that of the cladding was felt to limit fuel pin residence time to that of the high temperature case. WARD also felt that the effect of Pu migration also prevented an extended fuel pin lifetime. Thus, the WARD position regarding fuel pin lifetimes is that temperature independent phenomena limit fuel pin life to a three year residence. 7.2.1

Phase II results were presented by GE as representative of their position on the effect of temperature on fuel pin lifetime. 7.2.2 The actual calculations were performed as part of the COROPT optimization process. The fuel pin had the same clad thickness, but the longer plenum length associated with the low temperature core prevented the fuel pin from being the limiting factor in this design. The CDF was, by design, a limiting performance constraint in the high temperature core. Fuel pins were evaluated at constant EOL temperatures of 1239°F and 1230°F for high and low temperature designs. The GE position appears to be that the fuel pin lifetime is not a limiting factor in choosing a low temperature design over a high temperature design because it can be adjusted in the design process with little penalty.

AI used a 0.2% ductility limited strain as the fuel pin failure criteria and

supplemented this analysis with CDF calculations.^{7.2.3} Constant EOL temperatures of 1177°F and 1122°F characterized the high and low fuel pin. The plenum length was fixed for both designs. Their results indicated a low temperature design could achieve a longer peak burnup, e.g., 137 vs. 115 MWD/kg. The effect of the half wire wrap in reducing the peak (2σ) clad temperatures should be noted.

Fuel pin lifetime calculations of the normalized designs were conducted by HEDL^{7.2.4} using the computer code SIFAIL. A description of this pin design is found in Section 9.0 and is discussed in Section 3.1. The calculations included a range of cladding temperatures and lifetime cladding temperature histories which encompassed all temperatures expected by the several vendors. 100% fission gas release was employed in all calculations, and the split plenum was modeled as a top plenum with an effective volume which accounted for the lower temperature of the bottom plenum.

As will be discussed below in Section 7.6, a firm understanding of fuel pin behavior has yet to be reached. Currently ductile rupture is believed to be the mode of fuel pin failure. A limit of 0.2% on the thermal creep strain for steady state operation has been employed on both the FFTF and CRBR projects. The cumulative damage fraction (CDF) is currently gaining acceptance in fuel lifetime evaluation. A CDF of 1.0 is considered the point beyond which failure is expected to occur. A CDF of 0.5 for steady operation is a reasonable, conservative design limit. In order to determine if these two limiting conditions give rise to substantially different lifetimes, fuel pin lifetimes were determined by HEDL for both the CDF and strain limits.^{7.2.5,7.2.6} The calculations were performed using the LHRFDS properties and a cladding temperature constant over life. The results are shown in Figure 7.2.1; virtually the same lifetime is attainable with either design limit. Subsequent calculations employed the CDF limit in determining lifetime. This analysis employed the current thermal creep correlation for 1st core steel. The effect of the new correlation^{7.2.7} is discussed below.

The results of the HEDL parametric calculations^{7.2.5} on the normalized design fuel pin are shown in Figure 7.2.2. Fuel pin lifetime is presented as a function of beginning of life cladding temperature and cladding temperature change

Figure 7.2.1

COMPARISON OF FUEL PIN LIFE
PREDICTION BY CDF = 0.5 WITH
0.2% STRAIN LIMIT USING LHRFDS CORRELATIONS

Figure 7.2.2

EFFECT OF TEMPERATURE HISTORY ON PREDICTED FUEL PIN LIFE

(NORMALIZED PIN DESIGN)

(CDF = 0.5)

overlife. The LHRFDS properties were used in these particular calculations. Sensitivity of fuel pin lifetime to material properties is discussed in Section 7.5 below. As is readily seen fuel pin lifetime increases with decreasing cladding temperature. However, a relatively long life (>3 years) predicted for temperatures below 1200°F should be noted.

The cladding temperature history has a strong effect on lifetime if beginning of life temperature is the point of reference. However, the temperatures toward end of life, when fission gas pressure on the cladding becomes significant, are actually governing the lifetime. Figure 7.2.2 was replotted for lifetime as a function of end of life (EOL) cladding temperature; this is shown in Figure 7.2.3. This shows that the EOL cladding temperature is the predominating temperature effect and lifetime cladding temperature history plays a relatively minor role. This reinforces the decision to orifice the normalized designs for equal EOL cladding temperature throughout the core in order to maintain high fuel reliability.

The EOL cladding temperatures for the high and low temperature designs are 1173°F and 1098°F at the 2σ confidence level. Lifetimes in excess of 3 calendar years could be attained. As noted in Section 7.1 fuel lifetimes greater than 4 years show no significant benefit in fuel cycle cost and a plutonium utilization penalty for lifetimes greater than 3 to 4 years. Thus, it can be concluded that for reasonable fission gas plenum sizes, fuel lifetime is not severely limited by effects within the pin itself.

The long lifetimes of the normalized design fuel pins indicated that the fission gas plenums are probably oversized, at least for the low temperature design. Parametric calculations of the effect of gas plenum size were done where cladding temperature was held constant over life.^{7.2.6} The results indicate that a substantial reduction in plenum length could be realized. However, this was not done since these calculations employed the thermal creep correlations included in the LHRFDS set of properties. Recent out-of-pile thermal creep data^{7.2.7} indicates an order of magnitude greater strain for temperatures in the range under consideration.

Figure 7.2.3

EFFECT OF TEMPERATURE HISTORY ON PREDICTED FUEL PIN LIFE
(NORMALIZED PIN DESIGN)
(CDF = 0.5)

HEDL repeated the calculations of the normalized design fuel pin using this new HEDL thermal creep correlation.^{7.2.7} The results^{7.2.6} are presented in Figures 7.2.4 and 7.2.5. The first figure shows fuel pin lifetime as a function of EOL cladding midwall temperature for several fission gas plenum sizes; the reference normalized design with a 53" gas plenum length has a plenum volume to fuel volume ratio of 1.33. Fuel pin life for the current design limit on thermal creep strain of 0.2% is shown as well as a more optimistic design limit of 1.0%. The operating points of the normalized high and low temperature designs are also indicated in the figure. In Figure 7.2.5, the results are cross-plotted for thermal creep as a function of cladding temperature for several pin lifetimes. Only the reference normalized design fuel pin plenum volume to fuel volume is shown in this figure. These results indicated that the fuel pin for the high temperature design would be restricted to approximately 1.5 years unless the gas plenum was increased. However, if the 1.0% strain limit were imposed, the current configuration could sustain a 2.0 year core residence time. For the low temperature design, the 0.2% strain limit indicates a design lifetime in excess of 3.0 years. For the design core residence time of 2.7 years, a plenum length reduction could be achieved. The 1% strain limit, of course, would allow substantial increases in fuel lifetime and/or reduction in fission gas plenum size.

The new out-of-pile thermal creep data result in significant reductions in fuel lifetime. These data appear to be conservative with respect to recent in-pile test.^{7.2.8} Tests of pressurized tubes made of 1st Core and N-lot reference cladding material were conducted in EBR-II at representative temperatures. The total diametral change due to swelling, irradiation creep, and thermal creep was found to be less than the out-of-pile thermal creep under similar pressures and temperatures. This indicates a material hardening effect under the irradiation environment. Thus, it may be overly conservative to apply correlations based on out-of-pile tests to in-pile environmental conditions. The current understanding of the reference material does not include a clear connection between in-pile and out-of-pile data.

The 0.2% thermal creep strain limit used in the FFTF and CRBR projects are based on projection of in-pile data.^{7.2.9} Thus, it may be inappropriate to marry design

Figure 7.2.4

FUEL PIN LIFE BASED ON THERMAL CREEP STRAIN USING WIRE AND
STRAALSUND'S 3/14/77 CORRELATION FOR FIRST CORE STEEL
(P/F = PLENUM VOL./FUEL VOL., NORMALIZED DESIGN P/F = 1.33)

Figure 7.2.5

THERMAL CREEP STRAIN AS A FUNCTION OF TIME AND TEMPERATURE
(HEDL CALCULATIONS USING SIFAIL AND WIRE
AND STRAALSUND'S 3/14/77 THERMAL CREEP CORRELATION FOR
FIRST CORE STEEL ON 0.31" O'D' x 0.016" CLADDING THICKNESS)
(PLENUM VOL./FUEL VOL. = 1.33)

limits based on in-pile data with out-of-pile behavior characteristics, particularly in light of the improved in-pile behavior. A higher strain limit might be more appropriate when using the out-of-pile data. With the current status of the material properties, no definitive change in the fission gas plenum size could be recommended for the high temperature design. However, a reduction in plenum size for the low temperature design relative to the high temperature design could be effected.

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- 7.2.2 PLBR High/Low Temperature Evaluation Discussions, General Electric BRD Presentations, February 16-17, 1977.
- 7.2.3 PLBR High/Low Temperature Evaluation Discussions, Atomics International Presentations, February 14-15, 1977.
- 7.2.4 Letter, R. C. Noyes to W. E. Roake, FBR-77-17/21099, February 25, 1977.
- 7.2.5 "HEDL Support for the LMFBR Outlet Temperature Core Performance Study", Technical Information Transmittal, R. P. Omberg, HEDL to R. C. Noyes, C-E, March 15, 1977.
- 7.2.6 "CDF and Thermal Creep Calculations for PLBR Review Support", Technical Information Transmittal, R. P. Omberg, HEDL to R. C. Noyes, C-E, March 28, 1977.
- 7.2.7 Letter, G. L. Wire and J. L. Straalsund to RCD Task A and B Peer Groups, "Thermal Creep Behavior of 20% CW316SS", March 14, 1977.
- 7.2.8 Personal Communications, C. M. Cox, HEDL, April 5, 1977.
- 7.2.9 CRBRP, Preliminary Safety Analysis Report, Section 4.2.

7.3 Predicted Effect of Temperature on Fuel Assembly Life

The effect of outlet temperature upon duct bundle interaction (DBI) and the effect of DBI upon reactor design is complicated by several factors. Although severe DBI problems might be expected in long term irradiations, existing reactors, e.g., Phenix, have been able to operate unhampered. This fact suggests to some that despite the sensitivity of the various components of the duct bundle interaction to temperature and fluence, the interaction is less than the sum of its parts. Furthermore, the vendors did not always seek conditions allowing the worst duct bundle interaction calculation, and there was not agreement upon temperature and pressure characteristics of the bundle.

PLBR vendors display considerable differences regarding the size of the allowable DBI, the emphasis placed upon DBI, i.e., whether DBI has a limiting or non-limiting role in core design, and the calculational aspect of the problem. These differences are not surprising as the issue of DBI is a relatively new one. Present data point toward limits on fuel assembly performance due to DBI, but the location of these limits is still a matter of interpretation. Also open to question is the manner in which the DBI limits subassembly lifetime. All points of contention are involved in the PLBR vendors' treatment of DBI.

Concerning the size of the allowable DBI, the general trend is to permit a smaller interaction with larger diameter pins. This trend does not appear to be the result of either experiments or analyses, but rather the outcome of different approaches to the same problem. Allowable fuel rod cladding bending stresses were used to determine the maximum allowable interference for large diameter pins. This method, used by WARD and AI, produces a low estimate ($\sim$ one wire wrap diameter) of the allowable DBI^{7.3.1}. The stiff bundles designed by these vendors would not be easily displaced and a DBI-induced failure is thought to be mechanical in nature. A combination of experimental and analytical results were used by GE in establishing the allowable DBI for their smaller diameter pin bundles^{7.3.2}. These bundles tend to be more flexible and a relative displacement of pins within the bundle could be expected due to the DBI. Local hotspots could develop at the edge or the interior of the pin bundle before mechanical failure. The maximum allowable DBI tends to be somewhat larger as the smaller diameter pin bundles are more flexible. The procedure used by GE to determine allowable DBI relies

on empirical data describing rod-to-rod clearance as a function of bundle compression.

Given the allowable DBI, the next issue is its role in establishing the core design. Because mitigating factors such as irradiation induced stress relaxation are not considered in establishing the allowable DBI for larger diameter pins, the emphasis placed on these limits is not great. Because of anticipated spacer improvements, DBI is not a constraint for AI^{7.3.3}. Development of a local hotspot in a pin bundle is a more serious problem especially if the flow area around several pins is reduced. Based on hot spot analysis, GE considers the DBI criteria paramount and governing both the design and the subassembly lifetime.

The different vendors each use different calculational approaches as is noted in Appendix A. GE calculates the worst DBI for a given design based on a ring by ring approach in COROPT. The bundle is characterized by an average pin in the peak subassembly. WARD characterizes the bundle by a 2σ clad temperature and uses the maximum duct temperature rise to represent the duct. AI used an average bundle temperature in the peak subassembly and allowed the duct temperature to follow the reactor core temperature. All methods would produce different numerical estimates of calculated DBI for a given design. The larger issue, however, is the definition of the allowable and its use in reactor design, and these are independent of temperature.

According to the rationale described in Section 2, the normalized designs do not employ special design features nor do they depend on the development of an advanced alloy for their performance. Conservative constraints were placed on these designs for allowable DBI. The approach was felt to be both conservative and deterministic. The allowable DBI was calculated by GE methods^{7.6.6} based on interior rod-to-rod closure as a function of bundle compression (Figure 7.3.2). After allowing for bundle porosity, the allowable DBI was determined to be 150 mils. Typical results of DBI calculations for the peak subassembly and maximum hydraulic resistance are shown in Figure 7.3.1. The normalized high temperature design has a maximum DBI of 209 mils which is judged to be acceptable considering that no credit was taken for duct rounding. Also the available Phenix data indicate the allowable DBI may exceed the value calculated by GE methods by as much as a factor of two.

At a subassembly mixed mean outlet temperature of 980°F, this design has a 2.0 calendar year lifetime. Adopting the same allowable DBI for the low temperature core (subassembly mixed mean outlet temperature of 905°F) extended the life of this latter design to 2.7 calendar years. LHRFDS ("First Core") properties as shown in the figure display strong dependence of DBI upon temperature. Temperatures quoted above for the normalized designs lie at the low end of their respective ranges for subassembly outlet temperature. The curve thus also indicates the extent to which subassembly performance can be improved by efficient orificing.

Several general conclusions may be drawn from this survey of the PLBR vendors' approach to DBI. An allowable DBI is set independently of the temperature, and the influence of this permitted interaction upon the design is a matter of design judgment. If DBI is treated as a limiting design constraint, there are strong incentives to reduce temperatures because calculations indicate lifetime can be severely limited by DBI. The effect of high reactor outlet temperatures can be somewhat offset, and thereby reducing DBI, by efficient orificing.

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- 7.3.2 PLBR Reactor Core Temperature Information Meeting, GE-FBRD/CE/HEDL, February 16-18, 1977, Sunnyvale, CA.
- 7.3.3 PLBR Reactor Core Temperature Information Meeting, AI/CE/HEDL, February 14-15, 1977, Canoga Park, CA.

Figure 7.3.1
DUCT - BUNDLE INTERACTION

"FIRST CORE" STEEL

ϕt PEAK = 2.55×10^{23} (3.0 CY)

PRESSURE = 64/35 PSI

Figure 7.3.2

ALLOWABLE DUCT BUNDLE INTERACTION FOR NORMALIZED DESIGNS
271 - PIN BUNDLE, PIN O.D. = 0.31"

7.4 Predicted Effect of Temperature on Duct Dilation

The more limiting nature of duct dilation is evidenced by steps taken by PLBR vendors to design around it. Large gaps or slotted ducts were used to achieve compatibility of the core design with the requirement for only limited (~ 0) duct contact. Again there were some differences in the methods used by the vendors to characterize the worst duct.

Although the existence and calculation of duct dilation is not a matter of interpretation, there are off-setting effects of temperature and life. Swelling and creep increase with temperature, fluence, and pressure. Since the low temperature designs have longer fuel life, net duct dilation is little different between high and low temperature designs.

Among all PLBR vendors, the duct wall became slightly thicker or remained the same as temperature increased. The AI interduct gap is large, however, because of high duct temperature ($\frac{1}{2}$ wire design) and high coolant pressure. The AI interduct gap increases as temperature decreases partly due to the longer burnup assumed for lower temperature core, and partly due to a larger bundle dimension required for the lower temperature core^{7.4.1}. In this case, the pin outer diameter, the wall thickness, and pressure drop were held constant. WARD, on the other hand, kept the same pressure drop and pin size, but adjusted the wall thickness to maintain a constant gap as temperature changed.

Design features are also responsible for masking the temperature trends expected for duct dilation. Suppression of such trends is not surprising, since these features are introduced precisely for this reason. No vendor found it necessary to equip the high temperature design with some feature which was not also advantageous for the low temperature design and this similarity is itself an indication of the role of temperature in duct dilation. Thus, the GE ventilated duct design is employed for both high and

low temperature cores because it reduces the creep component of swelling in both instances^{7.4.2}. Similarly, the large interduct gaps employed by AI are used for both high and low temperature designs because they allow lower clad temperatures, via the half wire wrap, for both cores. WARD required no special features for either design in the area of duct dilation, because WARD used Revision 3 swelling correlation for both cores^{7.4.3}.

The gap for the high temperature normalized design was dictated by the maximum fluence permitted due to DBI considerations. No special design feature or advanced alloy behavior was assumed, although a very efficient orificing scheme was used (Section 3.3). Typical results are shown in Figure 7.4.1 for "First Core" material properties. These calculations employed a maximum hydraulic resistance and maximum calculated duct temperature rise. For a 2 year life and subassembly mixed mean outlet temperature of 980°F, a 400 mil gap is indicated. As with the DBI calculations, a strong temperature dependence is displayed. Consequently, there is a significant motivation to reduce temperature to avoid incurring neutronic penalties. The high temperature normalized design prohibits large temperature rises by efficient orificing. Given a 2.7 year lifetime, the same wall thickness, and assembly pressure drop as the high temperature core, the low temperature normalized design required a 360 mil gap. Duct wall temperatures for these designs are 790°F and 702°F at the location of maximum dilation. At these temperatures swelling makes a small contribution to duct dilation, and the lower temperature allowed a 10% reduction in interduct gap.

Duct dilation is a significant performance constraint in the PLBR design. Because of off-setting factors, no systematic response to increased reactor outlet temperatures was observed in PLBR design allowances for duct dilation. Other factors such as design options, optimization constraints, and considerations outside the reactor core played important roles in duct dilation. Based on consistent temperature considerations and an efficiently orificed core, a proper response to a lower outlet temperature appears to be a decrease in gap size.

7.22
Figure 7.4.1
DUCT DILATION

"FIRST CORE" STEEL
 ϕt PEAK = 2.55×10^{23} (3.0 CY)
PRESSURE = 64/35 PSI DUCT WALL = 115 MILS

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- 7.4.2 PLBR Reactor Core Temperature Information Meeting, GE-FBRD/CE/HEDL, February 16-18, 1977, Sunnyvale, CA.
- 7.4.3 PLBR Reactor Core Temperature Information Meeting, WARD/CE/HEDL, February 7-8, 1977, Madison, PA.

7.5 Effects of Materials Properties

In Phase I, the choice of material properties was left to the vendors, and Appendix A shows little agreement regarding material property correlations. In some cases, design or optimization constraints may override material property selection. For example, the GE stress rupture curve indicates a higher stress level is needed to produce failure for a given irradiation than the correlations employed by other vendors. But the effect of the optimization constraint in COROPT is to allow similar 2σ clad temperatures for both high and low temperature designs.^{7.5.1} Under these conditions, the role of the stress rupture correlation is secondary.

The WARD cores were designed under the assumption of a stainless steel possessing low swelling characteristics and a relatively high stress rupture strength.^{7.5.2} To discover the effect of such assumed properties, several calculations were performed on the normalized core design. In performing CDF calculations, WARD made a substantial adjustment to account for decarburization. The effect of these assumptions on CDF were calculated by HEDL,^{7.5.3} and are shown in Figure 7.5.1. For the normalized design conditions, i.e., constant clad temperature of 1177°F over life, an increase of 50% in fuel lifetime may be obtained if decarburization is not considered. Decarburization thus places the lifetime estimate below even the LHRFDS lifetime. The net effect then is that the assumption of high stress rupture strength is cancelled by the decarburization assumption. Moreover, the most recent information from HEDL indicates that the use of decarburization is unduly pessimistic, and that irradiation effects do not, in fact, degrade stainless steel stress rupture. WARD's assumption of improved material properties seems to have a negligible effect upon fuel pin lifetime prediction using CDF criteria.

Calculations were also performed for Revision 3 (N-Lot) materials in the DBI and duct dilation areas.

These calculations are shown in Figures 7.5.2 and 7.5.3, and the conditions quoted correspond to the normalized design conditions of Figures 7.3.1 and 7.4.1. By using the 209 mil allowable DBI to limit core lifetime, the effect of this improved material may be estimated. The 209 mil DBI limit does not

Figure 7.5.1

EFFECT OF MATERIAL PROPERTIES ON PREDICTED FUEL PIN LIFE
(NORMALIZED PIN DESIGN)
(CDF = 0.5)

Figure 7.5.2

DUCT DILATION

"N-LOT" STEEL

 ϕt PEAK = 2.55×10^{23} (3.0 CY)

PRESSURE = 64/35 PSI

DUCT WALL = 115 MILS

Figure 7.5.3

DUCT - BUNDLE INTERACTION

"N-LOT" STEEL

 ϕt PEAK = 2.55×10^{23} (3.0 CY)

PRESSURE = 64/35 PSI

occur until 2.42 years for the high temperature design and 2.95 years for the low temperature design. The high temperature design thus would have its life extended by 20%, while the low temperature design has its life extended by 10%. Clearly, there is a strong motivation for the development of a low swelling alloy for high temperature design. Duct dilation also decreases for these designs with the required gaps being 7.5% and 12.5% smaller for the high and low temperature cases, respectively. On the basis of subassembly performance, the use of advanced alloy material properties appears to favor the high temperature case. The advantage gained under this assumption is not entirely decisive as AI used the less favorable Revision 4 swelling correlation and still selected a high temperature design.^{7.5.4}

The effect of the recent HEDL correlation for thermal creep^{7.5.5} has been discussed in the fuel pin performance section (Section 7.2). In DBI calculations, thermal creep is generally a negligible contributor to overall bundle growth. The new correlation indicates a much larger contribution to overall bundle dilation would be expected. The effect of this correlation upon DBI calculations has not yet been evaluated.

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- 7.5.5 Letter, G. L. Wire and J. L. Straalsund to RCD Task A and B Peer Groups, "Thermal Creep Behavior of 20% CW316SS", March 14, 1977.

7.6 Operational and Test Data

The current state-of-the-art is such that prototype commercial fuel pin and fuel assembly design is based on performance predicted by analytical models using empirical data on material properties and verified primarily by tests of prototypical FTR and CRBR fuel pins. These fuel pins have relatively thick clad ($t/\bar{R} = 0.140$) and low smear density (85%). PLBR fuel is characterized by relatively thin clad ($t/\bar{R} = .081$ to 0.120) and higher smear density (88%-91%). The relatively small body of test data from EBR-II on this fuel design is summarized in Table 7.6.1 (7.6.1)

Table 7.6.1

Thin Cladding Irradiations

Experiment	No. of Pins	($t/\bar{R}$)	ρS (%T.D.)	T_c (ID) (°F)	Power (kw/ft)	Burnup (a/o)
WSA1	6	0.087	85-93	1100	6-8	6
WSA1	5	0.087	85-93	1000	6-7	12 ^(a)
WSA2	4	0.087	85-93	1050	8-10	8
WSA2	4	0.087	85-93	1000	8-10	17 ^(a)
WSA5	8	0.087	85-93	1100	13	10 ^(a)
P40	20	0.091	89-93	1150	13	8 ^(a)
P41	37	0.085	92	1120	14	2 ^(b)
P42	37	0.085	92	1120	14	2 ^(b)
F10A	4	0.083	88	1250	15	6 ^(a,b)

(a) continuing irradiation.

(b) anomalous failure observed.

Little unambiguous test or operational information is available which sheds direct light on the relationship between fuel life and fuel operating temperatures. Most tests and all reactor operations have been "success oriented", i.e., fuel failure has been avoided if possible. Consequently, postulated fuel failure mechanisms remain largely unconfirmed by actual experience.

Table 7.6 2 summarizes some typical successful test data for mixed oxide fuel operated in a test reactor (Rapsodie) at nominal clad temperatures representative of the predicted 2σ peak temperatures of a conservative high temperature design. Very high values of burnup have been achieved; however, the maximum fast fluence for these tests is slightly below the value of 1.7×10^{23} ($E > 0.1$ Mev) calculated for the normalized high temperature

design at 2 years residence time. Note also that peak cladding temperatures are typically quoted for beginning of life and that cladding temperatures decrease over life significantly in small test reactor irradiations.

Table 7.6.2

Results of Rapsodie High Temperature, High Burnup Irradiations (7.6.2)
Cladding Mid-Wall Temperature = 1150-1200°F

<u>Burnup Range (Atom %)</u>	<u>Approximate Fluence Range E > 0.1 Mev. x 10²³</u>	<u>No. of Pins* Irradiated</u>
11-12	0.95-1.05	37
12-13	1.02-1.12	264
13-14	1.12-1.21	250
14-15	1.21-1.30	169
15-16	1.30-1.39	150
16-17	1.39-1.47	<u>97</u>
		967

* Most pins are standard Rapsodie drivers - 0.26 OD x 0.0175 clad. Three pins failed in this group of 967.

From the point of view of flux and temperature, the most directly applicable experience is that obtained in the Phenix reactor where the peak flux has a value slightly higher than that typical of large commercial designs, and where the actual mix-mean outlet temperature is 1045°F. (7.6.3) Unfortunately, well documented information on Phenix irradiations is difficult to obtain. The highest burnup value actually published for Phenix fuel assemblies is 7.7 atom %. (7.6.4) This corresponds to a peak fluence of 1.6×10^{23} (E > 0.1 Mev.).

There is indirect evidence that "lead" fuel assemblies have reached 10 atom % burnup ($\phi t = 2.07 \times 10^{23}$ E > 0.1 Mev.). This estimate was made by W. B. Wolfe based on published plans for the operation of Phenix during 1976. (7.6.5) Operating temperatures for Phenix are as follows: (7.6.3)

	<u>Reactor Mixed Mean Outlet Temperature (°F)</u>	<u>Peak Clad Midwall Temperature with Hot Channel Factors (°F)</u>
Design	1040	1292
Actual Operation	1045	1274

The above cladding temperatures are for the beginning of life; it is estimated that peak cladding temperature would drop about 70°F during a two year life in

Phenix. This means that for a 2 year residence time, the end of life cladding temperature in Phenix is quite representative of 2 σ end-of-life cladding temperatures for conservative high temperature PLBR designs.

Being a wire wrapped mixed oxide fuel assembly of 217 pins with a 34 inch active core contained in a hexagonal duct, the Phenix fuel assembly is quite representative of PLBR designs. However, the cladding material is SA316 rather than CW316 and the alloy specification is not the same as "1st core" 316SS (RDT M-3-28T). As pointed out above, "1st core" material is significantly different than material fabricated to other specifications. Further, solution annealed material is known to behave differently under irradiation than cold worked material. Finally, the Phenix fuel pin has relatively low smear density (80%-85%) and thick cladding ($t/\bar{R} = 0.144$); thus, although operating conditions are representative, the fuel pin design is considerably different from PLBR designs. Nevertheless, its successful operation at relatively high temperatures, lends weight to the proposition that PLBR pins can operate successfully at these temperatures. The probability that the Phenix fuel assembly has operated successfully at high temperature and fluence is of equal or greater significance than the fuel pin success. This assembly experience is discussed further below.

The above discussed data is certainly not all inclusive with respect to irradiation experience with LMFBR mixed oxide fuel. However, it does include the bulk of the data put forward by PLBR contractors within the context of these discussions of core outlet temperature. The only significant additional data presented was an analysis of cladding failures in EBR-II experiments made by one contractor. The following results were obtained:

Table 7.6.3

EBR II - Experimental Fuel Pins* Cladding Types 304SS, SA316, CW316

<u>Cladding Temperature($^{\circ}$F)</u>	<u>No. Failures/No. Pins</u>
1050	$\frac{5}{737}$ (0.7%)
1050 - 1200	$\frac{12}{673}$ (1.8%)
1200	$\frac{5}{202}$ (2.5%)
=====	
All Temperatures	$\frac{22}{1612}$ (1.4%)

* As of September 1974, Encapsulated and Unencapsulated.

Ref: Handout GE-FBRD/C-E/HEDL Core Temperature Information Meeting Feb.16-18, 1977.

These data tend to suggest higher fuel pin failure rates as temperature is increased in the range of interest; however, more detailed examination of these experiments does not add weight to this hypothesis. For example, in a test assembly consisting of a number of identical pins, the failed pin tends to be randomly located and not correlated with the highest temperature location in the assembly. There is fairly general agreement among all contractors that there is little experimental evidence linking fuel pin failures and cladding operating temperature so long as this temperature is below about 1300°F.

Since experimental data is usually reported in terms of absolute peak temperature only, and since test reactor fuel pins are highly enriched, and consequently decrease in power and temperature during life, the above conclusion ought to be regarded with some caution in light of the trend indicated by Figure 7.2.2. A more conservative view, which is better supported by available data and theoretical considerations, is that fuel pin life is not affected by cladding operating temperature so long as the end-of-life temperature is not above about 1200°F.

The calculations performed as part of this study indicate that the swelling and creep of the cladding and duct and consequent interactions are probably more limiting to fuel life than phenomena affecting the fuel pin in isolation. This at least seems to be the case for "1st core" steel at PLBR operating temperatures and considering fuel life between 2 and 3 years. This is also the view strongly put forward by General Electric.

No direct experimental data was presented to support or dispute this view. Qualitative discussions were offered concerning "substantial bundle distortion" observed in EBR-II 61-pin experiments and reported informally by the French for Rapsodie and Phenix fuel assemblies. Fuel pin failures directly attributable to these distortions were not reported; however, out-of-pile experiments and analysis^(7.6.6) strongly indicate that bundle distortion will eventually lead to cladding hot spots. The early failures in the P41 and P42 experiments are thought to be due to such hot spots.

Since cladding and duct swelling and creep are directly related to fluence and since the fluence/burnup ratio is at least a factor of 2 higher in Phenix than in EBR-II, the duct-bundle-interaction occurring in Phenix fuel assemblies would be expected to be corresponding more severe. Peak duct-bundle-interaction predicted for Phenix assemblies is shown in Figure 7.6.1^(7.6.7) Calculations based on Ref. 7.6.6 give an allowable interaction of +105 mils for the Phenix assembly. The data indicates, therefore, that the G.E.'s theoretical limits on duct-bundle-interaction are quite conservative relative to Phenix operating experience.

References

- 7.6.1 Handout, GE-FBRD/C-E/HEDL Core Temperature Information Meeting, February 16-18, 1977.
- 7.6.2 F. Anselin, et al, "High Burnup Mixed Oxide Fuel Irradiation Experiments in Rapsodie", European Nuclear Conference, April 21-25, Paris. (1975)
- 7.6.3 F. Conte, "Un an D'Exploitation de Phenix", European Nuclear Conference, April 21-25, Paris. (1975)
- 7.6.4 CEA Annual Report for 1975.
- 7.6.5 J. Leclere, et al, "Superphenix Fuel Element Design", European Nuclear Conference, April 21-25, Paris. (1975)
- 7.6.6 GEAP-10028-57, Fifty-Seventh Quarterly Report, "Core Engineering", November-December 1975 and January 1976.
- 7.6.7 TC-240-11, Advanced Oxide Fuel Development Program Monthly Status Letter, November 1975.

Figure 7.6.1

PEAK DUCT - BUNDLE - INTERACTION PREDICTED FOR PHENIX FUEL ASSEMBLIES (SOURCE: TC-240-11, 11-75)

8.0 Materials Properties

Materials properties correlations used by the contractors are described in this section and include stress-free swelling, irradiation creep, stress-rupture, thermal creep, O.D. and I.D. clad wastage, and an allowance for clad manufacturing defects and wear. Correlations recommended by HEDL and those adopted for the LHRFDS study are also included for comparison. The HEDL recommendations represent HEDL's interpretation of the latest, up-to-date properties of 1st Core steel. Except for stress-rupture, the new "1st Core steel" thermal creep correlation, and WARD's swelling correlation, these recommendations are similar to the correlations originally chosen by the contractors. The LHRFDS recommendations are basically coincident with HEDL's except for thermal creep which is a very recent development. The term, "1st Core Steel", as used in this report, refers to CW361SS procured for FTR cores 1 through 4 according to RDT Standards, M 3-28T (cladding) and E 6-20T (ducts). The term "N-lot", refers to the design basis material used by WARD for temperature studies. This material has superior swelling, irradiation creep and stress-rupture properties compared to 1st Core steel. Recent data show that N-lot material has the same irradiation induced stress-free swelling rate as "1st Core steel", but at a given fluence, N-lot has less total swelling because its incubation period, τ , is slightly longer than that for 1st Core steel. The low swelling rate of "N-lot" (Revision 3) has been superseded by higher swelling rates of Revisions 4 and 5. Consequently, "N-lot" materials properties represent a target expected to be achieved by alloy development efforts (e.g., D-9).

The various stress-free swelling correlations are summarized in Table 8.1, and are compared in Figure 8.1, where stress-free volumetric swelling is plotted vs. temperature at a damage fluence of $2 \times 10^{23} \text{ n/cm}^2$. At temperatures above 900°F, the AI, GE, HEDL, and LHRFDS swelling correlations are quite close to each other. Swelling below 900°F is greater for Revision 4 (AI, GE) than for Revision 5 (HEDL, LHRFDS). This might be of significance to flow duct dilation and inter-duct spacing requirements. WARD's reasoning for their choice of the design basis material, "N-lot", is based in part upon the projection of successful development of an alloy to duplicate "N-lot" swelling (Revision 3), partly on considerations of very conservative design criteria justifying less conservative material properties, and also on the fact that "N-lot" is well characterized whereas data are rather sparse for "1st Core steel".

8.2

Irradiation creep correlations are summarized in Table 8.1 and differ from the HEDL recommendation of 6.3 for τ by only a rather small amount. No significant effects are anticipated in this area.

The stress-rupture correlations are also summarized in Table 8.1 and are plotted as stress-rupture times vs. temperature in Figure 8.2. The rupture times were calculated for a constant stress of 10,000 psi to allow comparison at typical times of interest. A wide range in life of one to two orders of magnitude is apparent. The HEDL and LHRFDS recommended correlations are close to each other and are intermediate to the low correlation (AI) and the high correlations (GE and WARD). All stress rupture correlations are based on out-of-pile data with corrections for the effect of irradiation with the exception of HEDL's current recommendation. Variations in stress rupture predictions occur mainly because of different interpretations of irradiation effects. Additionally, N-lot stress rupture data (unirradiated) is higher than "1st Core steel" data.

Thermal creep correlations, referenced in Table 8.1, are compared in Figure 8.3. Creep strains vs. temperature are shown at a constant stress level of 10,000 psi for 13,140 hours and span the range of thermal creep strains of interest. The HEDL "N-lot", LHRFDS, and WARD correlations show a weak temperature dependence, and creep strains in the neighborhood of 0.1%. The AI correlation shows a similar temperature dependence, but somewhat higher creep strains. Both the GE and HEDL's new "1st Core steel" correlations show a strong temperature dependence, but the new HEDL correlation is two orders of magnitude higher than the GE correlation and one to two orders of magnitude higher than the other correlations. The new HEDL correlation infers that "1st Core steel" may be more limited in application to PLBR designs than previously thought provided allowable thermal creep strain is limited to currently established values ($\sim 0.2\%$). The need for a better alloy is certainly increased by this new data.

Clad wastage correlations are referenced in Table 8.2 and compared in Table 8.3. Only minor differences are noted between the contractors, HEDL and LHRFDS correlations. Total wastage ranges from 2.4 to 4.1 (high temperature) and 1.9 to 3.22 (low temperature).

TABLE 8.1SUMMARY OF SWELLING, IRRADIATION CREEP AND STRESS RUPTURE CORRELATIONS

<u>Contractor</u>	<u>Stress-Free Swelling</u>	<u>Irradiation Creep</u>	<u>Stress Rupture</u>	<u>Thermal Creep</u>
AI	NSMH Rev. 4 ($\tau = 8$)	NSMH Rev. 3 ($\tau = 7$)	NSMH (N-lot with Brizes' correction for irradiation)	HEDL-TC-68
GE	NSMH Rev. 4 ($\tau = 7$)	NSMH Rev. 3 ($\tau = 7$)	GEAP-140321 (Irradiated N-lot nominal)	GEAP-14032-1
WARD	NSMH Rev. 3 ($\tau = f(T)$)	NSMH Rev. 3 ($\tau = 7$)	CRBRP-ARD-0115 (Irradiated N-lot design equations)	NSMH Rev. 1
HEDL Recommendation	NSMH Rev. 5 ($\tau = 7$)	NSMH Rev. 3 ($\tau = 6.3$)	NSMH Rev. 1 (1st Core Un-irradiated)	Letter, G.L. Wire and J.L. Straalsund to RCD Task A and B peer groups, thermal creep behavior of 20% CW316SS, March 14, 1977
LHRFDS (Large Heterogeneous Reference Fuel Design Study)	NSMH Rev. 5 ($\tau = 7$)	NSMH Rev. 3 ($\tau = 5.4$)	1st Core nominal with CRBRP-ARD-0115 type correction for irradiation	HEDL-TME 74-4

Figure 8.1

COMPARISON OF SWELLING CORRELATIONS

Figure 8.2

COMPARISON OF STRESS RUPTURE CORRELATIONS

 $(\sigma = 10 \text{ KSI})$

Figure 8.3

COMPARISON OF THERMAL CREEP CORRELATIONS

 $\sigma = 10 \text{ KSI}$, $t = 13140 \text{ HR.}$

TABLE 8.2SUMMARY OF TREATMENT OF DECARBURIZATION, Na CORROSION, AND CLAD I.D. WASTAGE

<u>Contractor</u>	<u>Decarburization</u>	<u>Na Corrosion (O.D.)</u>	<u>Clad I.D. Wastage</u>
AI	Included in Defect	WARD-NA-3045-23	ANS, 23 p. 159
GE	Included in Sodium Corrosion	WARD-NA-3045-23	TC-293, Rev. 2
WARD	CRBRP-ARD-0115	CRBRP-ARD-0115	CRBRP-ARD-0115
HEDL	None	CRBRP-ARD-0115	Proposed NSMH Correlation - W/FFTF C-760148
LHRFDS	None	CRBRP-ARD-0115	CRBRP-ARD-0115

TABLE 8.3COMPARISON OF WASTAGE CORRELATIONS

13,140 Hrs.
T = 1200°F (High)
T = 1145°F (Low)

<u>Contractor</u>	<u>I.D. (mils) (High/Low)</u>	<u>O.D. (mils) (High/Low)</u>	<u>Defects/Wear (mils)</u>	<u>Total (mils) (High/Low)</u>
AI	0.8/ 0.6	0.6/ 0.3	1.0	2.4/ 1.9
GE	1.5/ 0.92	0.6/ 0.3	2.0	4.1/ 3.22
WARD	1.8/ 1.5	0.6/ 0.3	2.0	4.4/ 3.8
HEDL	0.8/ 0.6	0.6/ 0.3	1.0	2.4/ 1.9
LHRFDS	1.8/ 1.5	0.6/ 0.3	1.0	3.4/ 2.8

9.0 Normalized High and Low Temperature Designs

During the course of this evaluation, high and low temperature designs were evolved to indicate clearly the effect of outlet temperature on core design and core costs. No attempt was made to attain an absolute optimum design. If further design changes would improve the performance or cost of the design but tended to obscure the main temperature effect, these changes would not be implemented. The design parameters were selected in order to give an overall conservative normalized design. The design and performance parameters are listed in Table 9.1.

A fixed core size was selected for both the high and low temperature designs in order to eliminate the cost implications of different diameter cores. The economic impact is seen in the different electrical output. The cores produce 2446 MWt for a peak linear pin power of 13.1 kW/ft and a maximum linear pin power at 15% overpower with 3σ hot channel factors of 17.0 kW/ft. The reactor vessel outlet temperatures were selected to span the entire range considered by the several reactor vendors: 950°F and 875°F for the thermal-hydraulic design conditions of the high and low temperature designs. A 300°F reactor vessel ΔT was used since it represented an implicit upper limit in the PLBR study based on anticipated transient effects on the primary system. Reduction of the reactor vessel ΔT would result in higher coolant flows and a small reduction in core temperatures for a given outlet temperature.

Figure 9.1, reproduced from the GE presentation at the PLBR High/Low Temperature Evaluation discussions,^{9.1} illustrates the variation of the net thermal efficiency with reactor vessel outlet temperature. An average of the GE and WARD net efficiencies (see Appendix I) was adopted for the high temperature normalized design. This point was plotted in the Figure and a line through this point parallel to the superheat cycle line was drawn. The low temperature efficiency was selected at the 875°F point on this line. The discontinuity at 900°F due to a switch from the superheat steam

TABLE 9.1

NORMALIZED DESIGNS
DESIGN AND PERFORMANCE PARAMETERS

	<u>HIGH</u>	<u>LOW</u>
1) Thermal-Hydraulic Design Conditions		
Power (MWe)	893	766
(MWt)	2446	2446
Reactor Vessel Outlet Temperature ($^{\circ}\text{F}$)	950	875
Reactor Vessel ΔT ($^{\circ}\text{F}$)	300	300
Net Thermal Efficiency (%)	36.5	31.3
2) Fuel Pin Design		
Cladding O.D. (in.)	0.310	0.310
Cladding Thickness (in.)	0.016	0.016
Cladding Material	1st Core	1st Core
Fuel Pellet O.D. (in.)	0.272	0.272
Smear Density (% TD)	91	91
Gas Plenum Length, Top/Bottom (in./in.)	34/19	34/19*
Pitch-to-Diameter Ratio	1.174	1.174
Wire Wrap Diameter (in.)	0.051	0.051
3) Driver Assembly Design		
Number of Pins/Assembly	271	271
Lattice Pitch (in.)	6.720	6.680
Interduct Gap (in.)	0.400	0.360
Duct Outside Flat-to-Flat (in.)	6.320	6.320
Duct Inside Flat-to-Flat (in.)	6.090	6.090
Duct Wall Thickness (in.)	0.115	0.115
Duct Material	1st Core	1st Core
Duct-Bundle Clearance (in.)	0.051	0.051
Porosity/Ring	0.005	0.005

* Analysis indicates some reduction of plenum size would be appropriate.

TABLE 9.1 (Continued)

	<u>HIGH</u>	<u>LOW</u>
4) Core Design		
Number of Driver Assemblies	276	276
Number of Radial Blanket Assemblies	216	216
Number of Control Assemblies	19	19
Number of Enrichment Zones	3	3
Core Height (in.)	48	48
Axial Blanket Thickness (in.)	14	14
5) Core Cooling Design		
By Pass Flow (%)	0.030	0.030
Radial Blanket Flow (%)	0.056	0.056
Orificing Scheme	high eff.	high eff.
6) Nuclear Performance		
Core Power Fraction (MOEC)	0.9492	0.9492
Axial Blanket Power Fraction (MOEC)	0.0225	0.0225
Radial Blanket Power Fraction (MOEC)	0.0283	0.0283
Peak Damage Fluence ($E > 0.1$ Mev) (10^{23} n/cm ²)	1.7	2.3
System Inventory (kg HM)	6405	5804
Breeding Ratio	1.32	1.30
Doubling Time (years)	21.5	20.7
Fuel Cycle Cost (Mill/kWh)	5.83	5.95
7) Thermal-Hydraulic Performance		
Driver Region Outlet Temperature, MOEC (°F)	969	894
Linear Pin Power (kW/ft)		
Nominal Peak	13.1	13.1
3 σ 15% Overpower	17.0	17.0
Maximum Midwall Cladding Temperature, 2 σ , EOL (°F)	1173	1098
Peak Power Assembly Mixed Mean Outlet Temperature, EOL (°F)	977	902
Peak Power Assembly Flow Rate 10 ⁶ lbm/h)	0.377	0.377
Driver Assembly ΔP (psi)	86	86
8) Fuel Assembly Performance		
Lifetime (calendar years)	2.0	2.7
Load Factor	0.8	0.8

Figure 9.1

NET EFFICIENCY vs TEMPERATURE

cycle to the saturated steam cycle was ignored in order to avoid any cost implication of this discontinuity. Electrical power produced by the high and low temperature systems are, therefore, 893 and 766 MW.

The 0.310" O.D. fuel pin was selected since it was the largest considered by the several vendors. This, combined with a two calendar year residence, would yield a relatively low peak damage fluence exposure. This reflects the conservative design philosophy established for the selection of the normalized design parameters. The relatively thick 16 mil cladding was selected to be conservative from the cladding wastage point of view. There was no obvious dominating temperature effect on pin diameter which would lead to a selection of different pin sizes for the two designs. Thus, this pin was used in both the high and low temperature cores.

The initial effort on the normalized designs employed fuel pin bundle with pitch-to-diameter ratios as tight as used by WARD. However, during the orificing study, it became clear that the multi-zoning and high efficiency orificing lead to high flows in the peak power assembly. In order to reduce the high assembly pressure drops, the pitch-to-diameter ratio was increased to 1.174. A 51 mil wire wrap diameter was selected so that the bundle would have a reasonable duct-bundle clearance and yet not have a porosity per ring which would cause concern from a bundle vibration point of view. These considerations were applicable to both the high and low temperature designs and no design difference was deemed necessary.

Figure 9.2 is a duct dilation map, duct wall thickness vs. interduct gap, for the high temperature design. The gap is required to avoid duct-duct interaction (DDI) for the two-year, 1.7×10^{23} n/cm² core residence. A wall thickness of 175 mils requires a 175 mil gap and leads to the minimum unit cell. In order to reduce the steel in the core, a steel for sodium tradeoff is made by

Figure 9.2

DUCT DILATION MAP - HIGH TEMPERATURE
NORMALIZED DESIGN

going down in wall thickness. A wall thickness of 115 mils which requires a 400 mil gap was selected. Further reduction in wall thickness would be accompanied by enormous gap requirements. Excessively large interduct gaps lead to concerns about thermal shocks at the duct load pad.

The fuel pin/fuel assembly residence time for the low temperature core was selected such that duct-bundle interaction (DBI) would be the same for the two cores. The DBI for the high temperature core was calculated to be 209 mils and was assumed to be acceptable. Figure 9.3 shows the duct dilation and the DBI of the fuel assembly in the low temperature core as a function of peak damage fluence. For the established allowable DBI, a peak damage fluence of $2.3 \times 10^{23} \text{ n/cm}^2$ at a core residence time of 2.7 calendar years was acceptable. The corresponding duct dilation curve indicated a required 360 mil interduct gap.

A discussion of the core cooling design is also presented in Section 3.3. The By Pass flow includes the control and radial shield assembly flows, inlet plenum to outlet plenum leakage through the interduct gaps, and vessel bypass. A total By Pass flow of 3% was estimated for the normalized designs. Table 9.2 lists the power splits for CRBR which has a 975 MWt rating. A small portion of the total power, 1.16%, is deposited outside the driver-radial blanket regions. Projecting this power deposition to larger size cores, a smaller percentage of power deposited outside the driver-radial blanket regions is expected. Imposing a cooling requirement on the control and radial shield that these regions will have coolant temperature rises at middle of equilibrium cycle (MOEC) equal half the reactor vessel ΔT leads to a total control and radial shield region flow of 2%. A direct leakage between interduct gap as 1% of the flow was estimated for CRBR.^(9.2) This was accepted for the normalized designs as a reasonable value. Reactor vessel bypass flow, if the design requires it, is included in the radial shield requirement.

Figure 9.3

DUCT DILATION AND DUCT BUNDLE INTERACTION - LOW TEMPERATURE NORMALIZED DESIGN

TABLE 9.2

CRBR POWER SPLITS*

	<u>% of Total Power</u>
Driver Region	91.1
Radial Blanket	7.74
Control Assemblies	0.76
Radial Shield	0.38
Other	0.02

* Section 4.4 - CRBRP, Preliminary Safety Analysis Report

The radial blanket region produces 2.8% of the total power at MOEC. In order to provide adequate cooling for individual blanket assemblies which experience a large lifetime increase in power, the blanket region must be overcooled. The coolant rise for the entire blanket region was established to be half the reactor vessel ΔT . Thus, 5.6% radial blanket flow was allocated. This is expected to provide sufficient cooling so that excessive blanket temperatures are not experienced at end of life (EOL). The resultant driver region flow (91.4%) and power (97.2%) lead to a driver outlet temperature of 969^oF and 894^oF for the high and low temperature cores.

The distribution of flow within the driver region was done to minimize the EOL midwall cladding temperature throughout the core. This criteria was selected since EOL conditions have a stronger bearing on fuel pin life than the maximum cladding temperatures which generally occur at beginning of life (BOL). A three enrichment zone core was selected in order to flatten the radial power distribution and help reduce the required flow in the high powered assembly. Complete, assembly-by-assembly orificing was selected in order to attain the maximum benefit in terms of temperature. This is discussed in greater detail in Section 3.3.

The nuclear performance parameter of Fuel Cycle Costs, Breeding Ratio, Doubling Time, and System Inventory shown on Table 9.1 are discussed, in detail, in Section 5.0.

References:

- 9.1 PLBR High/Low Outlet Temperature Evaluation Discussions
General Electric BRD presentations, February 16-17, 1977.
- 9.2 CRBRP, Preliminary Safety Analysis Report, Section 4.4.

SUMMARY OF PRINCIPAL CHARACTERISTICS

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
POWER (Mwe)	944	944	900	900	938	923
(Mwt)	2585	2990	2490	2750	2550	2925
DESIGN OUTLET TEMP. (°F)	930	875	950	875	950	900
NET THERMAL EFFICIENCY (%)	36.5	31.6	36.2	32.1	36.8	31.6
CORE HEIGHT (in.)	46	46	48	48	48	48
FUEL PIN OD (in.)	.300	.300	.277	.256	.310	.310
CLADDING AND DUCT MATERIAL	"1st Core"	"1st Core"	"1st Core"	"1st Core"	"N-Lot"	"N-Lot"
CLADDING THICKNESS (in.)	.017	.017	.012	.012	.016	.012
ASSEMBLY PRESSURE DROP (nominal) (psi)	100	100	48	52	82	82
PEAK CLADDING TEMP. (°F) (EOL)	1159	1104	1239	1230	1188	1138
FUEL LIFE (C.Y.)	2.1	3.1	2.2	2.4	3.0	3.0
LOAD FACTOR	0.8	0.8	0.7	0.7	0.85	0.85
INVENTORY (Core & Blanket kg _{fiss})	3443	4189	3260	3364	3843 ⁽¹⁾	4267 ⁽¹⁾
BREEDING RATIO	1.29	1.28	1.31	1.27	1.29	1.35
DOUBLING TIME	18	17	15	15	18	14
FUEL CYCLE COST (Mill/kwh)	4.25	3.97	6.10	5.30	5.87	6.52

(1) CORE ONLY

DESIGN AND EXPECTED OPERATING TEMPERATURES

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
<u>STRUCTURAL DESIGN TEMPERATURE (°F)</u>	950	895	965	890	950	N/A
<u>OUTLET TEMPERATURE BASIS ⁽¹⁾ FOR DESIGN AND ANALYSIS OF:</u>						
a) FUEL PIN	930	875	950	875	950	900
b) DUCT DILATION	930	875	950	875	950	900
c) DUCT-BUNDLE INTERACTION	930	875	950	875	950	900
<u>CORRESPONDING INLET TEMPERATURE FOR:</u>						
a) FUEL PIN	650	605	650 ⁽¹⁾	575 ⁽¹⁾	650	600
b) DUCT DILATION	650	605	650	575	650	600
c) DUCT-BUNDLE INTERACTION	650	605	650	575	650	600
<u>EXPECTED OR 'MOST PROBABLE' OPERATING TEMPERATURES</u>						
REACTOR VESSEL OUTLET TEMPERATURE (°F)	~910	~855	928	860	932	N/A
REACTOR VESSEL INLET TEMPERATURE (°F)	N/A	N/A	647	580	636.8	N/A

(1) All core design efforts done for 300°F temperature rise and either 950°F or 875°F reactor outlet.

CORE PERFORMANCE

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
(1) NET THERMAL EFFICIENCY, %	36.5	31.6	36.2	32.1	36.8	31.6
(2) BREEDING RATIO	1.29	1.28	1.31	1.27	1.29	1.35
(3) DOUBLING TIME (YR)	18	17	15	15	18	14
(4) FUEL CYCLE COST* (3) (MILLS/KWHR)	4.25	3.97	6.10	5.30	5.87	6.52
(5) DIRECT & INDIRECT INCREMENTAL CAPITAL COST OF VESSEL, EVST, INTER- NALS AND BUILDING DUE TO CORE SIZE CHANGE (MILLS/KWH)	Base	+1.05 ⁽¹⁾ + .74 ⁽²⁾	+0.27	0.0	0.0	+0.48
TOTAL COSTS (4) & (5)	4.25	5.02 ⁽¹⁾ 4.71 ⁽²⁾	6.37	5.30	5.87	7.00

*INCLUDING 6% INFLATION DURING 16 YR. LEVELIZING PERIOD PER S,P&W

(1) INCLUDES UNAVAILABILITY PENALTY

(2) INCLUDES CAPITAL COSTS ONLY

(3) EXCLUDING CREDIT FOR LIFETIME PU VALUE INCREASE

CORE DESIGN

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
<u>CORE SIZE</u>						
M _{we} (Net)	944	944	900	900	938	923
M _{wt}	2585	2990	2490	2758	2550	2925
<u>No. ASSEMBLIES</u>						
DRIVER	228	264	270	282	276	318
BLANKET	198	216	210	210	210	222
CONTROL	25	31	19	19	19	19
PLUTONIUM INVENTORY (core + blanket kg _{fiss})	3443	4189	3260(1)	3364(1)	3843(2)	4267(2)
No. ENRICHMENT ZONES	2	2	2	2	3	3
PEAK FLUENCE (E >.1 Mev) x 10 ²³ nvt	2.3	3.0	2.0	2.7	2.5(3)	2.5(3)
LINEAR POWER						
AVERAGE	10.0	10.0	8.0	8.9	8.25	8.25
NOMINAL PEAK	15.0	15.0	12.1	13.3	~12.4	~12.4
3σ 15% OVERPOWER	19.0	19.0	15.5	17.0	~17.0	~17.0

(1) BOEC

(2) EXCLUDING BLANKET PU

(3) DESIGN FLUENCE 2.9 x 10²³ nvt

ASSEMBLY DESIGN

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
LATTICE PITCH (in.)	6.95	7.008	6.091	5.814	6.74	6.65 ⁽¹⁾
DUCT OUTSIDE FLAT-TO-FLAT (in.)	6.354	6.372	5.708	5.433	6.36	6.30
DUCT INSIDE FLAT-TO-FLAT (in.)	6.034	6.052	5.526	5.259	6.00	6.00
DUCT WALL THICKNESS (in.)	.16	.16	.091	.087	.180	.150
INTER-DUCT GAP (in.)	.596	.636	.361	.381	.360	.330
ASSEMBLY ΔP (psi)	100	SAME	48	52	95	95
PIN BUNDLE FLAT-TO-FLAT (in.)	5.986	6.006	5.507	5.240	5.951	SAME
DUCT-BUNDLE CLEARANCE (in.)	.048	.046	.019	.019	.049	SAME
POROSITY/RING	.005	.005	.002	.002	.003	SAME
DUCT MATERIAL	20% CW	SAME	SAME	20% CW	20% CW	SAME
LENGTH (in.)	216	SAME	210	225	189	SAME

(1) CALCULATED FROM REVISED INTER-DUCT GAP VALUE SUPPLIED 3/30/77

FUEL PIN DESIGN

	AI		GE		WARD	
T _{outlet}	High	Low	High	Low	High	Low
PIN OD (in.)	.300	SAME	.277	.256	.31	SAME
CLAD THICKNESS (in.)	.017	SAME	SAME	.012	.016	.012
PELLET DIAMETER (in.)	.261	SAME	.2475	.2265	.272	.280
GAP (in.)	.005	SAME	0.0055	.0055	.006	.006
P/D	1.203	1.207	1.191	1.225	1.15	SAME
ρSMEAR (% TD)	88	SAME	SAME	88	91	SAME
CORE HEIGHT (in.)	46	SAME	SAME	48	48	SAME
PLENUM LENGTH (in.)	60	SAME	39.8	75	53 ⁽¹⁾	SAME
WIRE WRAP DIAMETER (in.)	.061 (I) .030 (O)	.062(I) .031(O)	.0518	.0565	.046	SAME
WIRE WRAP PITCH (in.)	12.	SAME	SAME	12.0	12.37	SAME
CLAD MATERIAL	20% CW	SAME	SAME	20% CW	20% CW	SAME
WRAP MATERIAL	20% CW	SAME	SAME	20% CW	20% CW	SAME

(1) 34" upper plenum/19" lower plenum

REFERENCE DESIGN PARAMETERS OF VESSEL, HEAD & INTERNALS

	AI	GE	W
VESSEL DIAM., FT.	44.5 UPPER 23.2 LOWER	44	26
VESSEL LENGTH, FT.	61.3	57.5	65
COLD LEG DOWNCOMERS	INTERNAL	INTERNAL	EXTERNAL
EFFECT OF LOWER TEMP. ON VESSEL DIAM.	+29" FOR LO TEMP.	-6" FOR LO TEMP.	+32" FOR LO TEMP.
UPPER INTERNALS STRUCTURE	CYLINDRICAL-BAFFLE INTERNALS UNDEFINED	CRBRP TYPE 180" D, 6" PLATE WITH CHIMNEYS	CYLINDRICAL BAFFLE WITH SHROUD TUBES
EFFECT OF LOWER TEMP. ON UIS	LARGER (13" ΔD) SAME MTLS.	SMALLER (6" ΔD) SAME MTLS.	LARGER (16" ΔD) SAME MTLS.
HEAD/ROOF CONSTRUCTION	CONICAL STEEL-PLATE SHELL STRUCTURE WITH STEEL PLATE SHIELDING	CONCRETE FILLED ROOF STRUCTURE CRBR PLUG/IVTM TECHNOLOGY	CRBR TYPE HEAD STEEL LAMINATED
ROTATING PLUGS	3	3	2
REFUELING TYPE	UNDER THE PLUG STRAIGHT PULL INCLINED TUBE THRU HEAD	UNDER THE PLUG WITH INERTED HOT CELL FOR TRANSFER STRAIGHT PULL INCLINED TUBE THRU HEAD	UNDER THE PLUG FIXED OFFSET ARM INCLINED TUBE THRU VESSEL WALL
EFFECT OF LOWER TEMP. ON REFUELING INCLUDING EVST SIZE	NONE	NONE	EVST INCREASED SIZE

CORE COOLING DESIGN - FLOW AND POWER SPLITS

	AI	GE		WARD
REACTOR POWER SPLIT TIME CORE DESIGNATION (TEMP.)	MOEC HIGH	HIGH	LOW	BOEC HIGH
CORE	0.914	.941(1)	.940	0.9437(2)
AXIAL BLANKET	0.032	.035	.028	0.0182
DRIVER (CORE + AXIAL BLKT)	0.946	.976	.968	0.9619
RADIAL BLANKET	0.054	.024	.032	0.0381
OTHER	0.0	0.0		0.0
REACTOR FLOW SPLIT				
DRIVER	0.883	.84(3)	.763	0.82
RADIAL BLANKET	0.092	.10	.100	0.13
OTHER	0.025	.06	.137	0.05
REACTOR REGION POWER-TO-FLOW RATIO				
DRIVER	1.07	1.161	1.282	1.17
RADIAL BLANKET	0.59	.240	.22	0.29
NUMBER OF DRIVER ORIFICE ZONES	5	7	7	3/4(5)

- (1) GE POWER SPLITS AS DETERMINED AND USED IN SCOPING STUDIES
- (2) WARD POWER SPLITS AS DETERMINED FOR 2325 MWT CORE
- (3) GE FLOW SPLITS AS OPTIMIZED IN SCOPING STUDIES
- (5) FINAL NUMBER NOT ESTABLISHED YET

PEAK CLADDING TEMPERATURES

THERMAL-HYDRAULIC DESIGN CONDITION	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
REACTOR VESSEL OUTLET TEMPERATURE (°F)	930	875	950	875	950	900
REACTOR VESSEL ΔT (°F)	280	270	300	300	300	300
HOT MIDWALL CLADDING TEMPERATURE, 2σ (°F) (INNER ZONE)						
BOL	1168	1113	1176	1160	1236	1186
EOL	1159	1104	1239	1230+	1188	1138
LIFETIME SWING	-9	-9	63	70	-48	-48
DIFFERENCE BETWEEN THE HOT MIDWALL CLADDING TEMPERATURE AND THE REACTOR VESSEL OUTLET TEMPERATURE (°F)	238	238	289	355+	286	286

+INCLUDES THE EFFECT OF HIGH FLOW BY-PASS

FUEL SUBASSEMBLY AND PIN LIFETIME

	AI		GE		WARD	
	HIGH	LOW	HIGH	LOW	HIGH	LOW
LIFETIME (calendar days)	775	1142	788	868	1095	1095
CAPACITY FACTOR	0.8	SAME	SAME	0.7	0.85	SAME
LIMITING PHENOMENA	DLS ⁽¹⁾	DLS	CDF ⁽²⁾ DBI ⁽³⁾ DDI ⁽⁴⁾	DBI DDI	CDF DDI	CDF DDI
CUMULATIVE DAMAGE FRACTION	.5 ⁽⁵⁾	.5 ⁽⁵⁾	.5	.223	.39	<.4
DUCTILITY LIMITED STRAIN (%)	.2	.2	1.05 ⁽⁶⁾ .40 ⁽⁷⁾	1.24 ⁽⁶⁾ .15 ⁽⁷⁾	.06	<.2
EOL DUCT BUNDLE INTERACTION (Interference) (mils)	~50	<50	134	142	60	< 60
EOL DUCT DILATION/BOL GAP - (mils/mils)	596 ⁽⁸⁾ /596	636/636	361/361	381/381	430/420(8)	430/385(8)

(1) DLS - DUCTILITY LIMITED STRAIN

(2) CDF - CUMULATIVE DAMAGE FRACTION

(3) DBI - DUCT-BUNDLE INTERACTION

(4) DDI - DUCT-DUCT INTERACTION

(5) CDF USED TO CONFIRM THE BURNUP INCREASE RESULTS
INDICATED BY USE OF THE DLS CRITERION

(6) TOTAL CLAD INELASTIC CREEP STRAIN (%)

(7) TOTAL CLAD THERMAL CREEP STRAIN (%)

(8) HOT GAP

FUEL PIN STEADY STATE LIFETIME DETERMINATION

	AI	GE	WARD
LIFETIME CRITERIA	STEADY STATE CDF ≤ 0.5 STEADY STATE THERMAL CREEP STRAINS $\leq 0.2\%$	STEADY STATE CDF ≤ 0.5 TOTAL INELASTIC STRAIN $\leq 2.0\%$	STEADY STATE + TRANSIENT CDF ≤ 1.0 STEADY STATE THERMAL CREEP STRAIN $\leq 0.2\%$
METHOD OF CALCULATION	GAS BOTTLE	GAS BOTTLE WITH CONFIRMATION USING THE LIFE CODE AND PROBABLISTIC CODES	GAS BOTTLE
OPERATING CONDITIONS	2σ , TEMPERATURES CONSTANT OVERLIFE	2σ , TIME VARYING TEMPERATURES VERIFICATION FROM LIFE AND PROBABLISTIC CODES	2σ , TIME VARYING TEMPERATURES W/ OPTIMIZED ORIFICING
GAS RELEASE MODEL	100% RELEASE	AS SPECIFIED IN THE COROPT USER'S MANUAL	100% RELEASE

FUEL PIN STEADY STATE LIFETIME DETERMINATION - (CONTINUED)

	AI	GE	WARD
MANUFACTURING DEFECTS AND INITIAL WEAR AT BOL SODIUM CORROSION	1 MIL WARD-NA-3045-23 0.45 MIL (EOL)	2 MILS •PHASE I: TC-293 REV. 1 •PHASE II: CRBRP-ARD-0115 ~1.6 mils (EOL)	2 MILS CRBRP-ARD-0115 1.25 (EOL)*
DECARBURIZATION	INCLUDED IN DEFECT ALLOWANCE	TREATED IN SODIUM CORROSION	CRBRP-ARD-0115 "EFFECTIVE" DECREASE 4.85 MILS
I.D. ATTACK	HEDL EQUATION ANS, 23 pg. 156 0.56 MILS (EOL)	PHASE I: TC-293 REV. 1 PHASE II: TC-293 REV. 2 ~2.0 MILS (EOL)	CRBRP-ARD-0115 ~1.7 MILS (EOL)*

*C-E ESTIMATE

ASSEMBLY LIFETIME - DUCT-BUNDLE INTERACTION

	AI	GE	WARD
CRITERIA	<ul style="list-style-type: none"> • MINIMUM EOL DUCT WALL TO PIN CLEARANCE $>0.05 \times \text{PIN O.D.}$, WITH DISPERSION • NO WIRE WRAP LIMIT SPECIFIED 	<ul style="list-style-type: none"> • MINIMUM EOL DUCT WALL TO PIN CLEARANCE $>0.025 \times \text{PIN O.D.}$, WITH DISPERSION • MINIMUM EOL PIN-TO-PIN CLEARANCE $>0.05 \times \text{PIN O.D.}$, WITH DISPERSION • NO WIRE WRAP LIMIT SPECIFIED 	<ul style="list-style-type: none"> • ONE WIRE WRAP DIAMETER INTERFERENCE ACROSS CORNERS, NO DISPERSION ASSUMED • WIRE WRAP TENSION AND STRAIN LIMIT
OPERATING CONDITIONS DUCT TEMPERATURES	SAME AS IN DUCT DILATION CALCULATION	SAME AS IN DUCT DILATION CALCULATION	SAME AS IN DUCT DILATION CALCULATION
BUNDLE TEMPERATURE	EQUAL TO NOMINAL MIDCYCLE CLADDING TEMPERATURE FOR THE PEAK PIN	NOMINAL LOCAL CLADDING TEMPERATURE AVERAGED OVER THE BUNDLE	EQUAL TO THE 0σ CLADDING TEMPERATURE - CIRCUMFERENTIAL AVERAGE, EXCLUDING WIRE WRAP HOT SPOT

ASSEMBLY LIFETIME - DUCT

	AI	GE	WARD
CRITERIA	<ul style="list-style-type: none"> • NO DUCT-DUCT CONTACT DUE TO DILATION AT EOL • DUCT STRESS LIMIT NOT SPECIFIED 	<ul style="list-style-type: none"> • NO DUCT-DUCT CONTACT DUE TO DILATION AT EOL • NOMINAL DUCT STRESS $\leq .55 \sigma_{ALL}^{(1)}$ 	<ul style="list-style-type: none"> • NO DUCT-DUCT CONTACT DUE TO DILATION AT EOL • PRIMARY DUCT STRESS AT INLET ≤ 21 ksi (650°F)
OPERATING CONDITION	<ul style="list-style-type: none"> • REACTOR VESSEL ΔT IMPOSED ON DUCT WALL ACROSS CORE • DUCT WALL PRESSURE PROFILE 74-48 psi 	<ul style="list-style-type: none"> • NOMINAL DUCT TEMPERATURES FOR PEAK SUBASSEMBLY • DUCT WALL PRESSURE PROFILE LINEAR OVER BUNDLE LENGTH (3) 	<ul style="list-style-type: none"> • NOMINAL DUCT(2) TEMPERATURES FOR PEAK ASSEMBLY • DUCT WALL PRESSURE PROFILE 53.4-28.4 psi BASED ON MAXIMUM HYDRAULIC RESISTANCE

(1) ALLOWABLE STRESS AS CALCULATED IN DRAFT RDT CRITERIA

(2) DESIGN ASSUMED A 390°F LINEAR INCREASE OVER ACTIVE CORE REGION

(3) MIDCORE DUCT TEMPERATURE AND PRESSURE: HIGH TEMP. DESIGN 780°F - 12.5 psi
LOW TEMP. DESIGN 719°F - 13.3 psi

MATERIAL PROPERTIES

	AI ⁽¹⁾	GE ⁽²⁾	WARD ⁽³⁾
SWELLING	NSMH-REV. 4	NSMH-REV. 4 ⁽⁴⁾	NSMH-REV. 3
IRRADIATION CREEP	NSMH-REV. 3	NSMH-REV. 3 ⁽⁴⁾	NSMH-2 (7)
THERMAL CREEP	HEDL-TC-68	GEAP-14032-1 ⁽⁴⁾	NSMH-REV. 0
CLADDING ID WASTAGE	ANS, 23 pg. 156	TC-293-REV. 2 ⁽⁴⁾	CRBRP-ARD-0115
DECARBURIZATION	NONE ⁽⁵⁾	NONE ⁽⁶⁾	CRBRP-ARD-0115
SODIUM CORROSION	WARD-NA-3045-23	CRBRP-ARD-0115 ⁽⁴⁾	CRBRP-ARD-0115

(1) AI USES CONSERVATIVE FIRST CORE STEEL PROPERTIES

(2) GE USES THE NOMINAL VALUE OF THE REPORTED EQUATIONS WITHOUT DISTINCTION BETWEEN FIRST CORE AND N-LOT PROPERTIES

(3) WARD USES NOMINAL N-LOT PROPERTIES

(4) PHASE I USED TC-293 REV. 1

(5) EFFECT INCLUDED IN WEAR ALLOWANCE

(6) EFFECT INCLUDED IN CONSERVATIVE TREATMENT OF SODIUM CORROSION

(7) AS MODIFIED BY REV. 3 SWELLING ENHANCED CREEP TERM

FUEL CYCLE COMPONENTS

(1976 \$)

	AI	GE	W
FUEL FAB. COST (\$/ASSY)	50,000	(1)	85,000 (less learning)
FUEL FAB. COST (\$/KG-HM)	500	(1)	748 (yr. 2000) 555 (yr. 2019)
AXIAL BLKT. COST (\$/KG-HM)	(2)	100 (levelized)	N/A
RADIAL BLKT. COST (\$/ASSY)	15,000	N/A	28,510
RADIAL BLKT. COST (\$/KG-HM)	70	150 (levelized)	53.4 → 50(1st yr.)(3)
FUEL REPROC., SHIP, STORE (\$/KG)	200	310 (levelized)	200
FAB./RECOV. TIME, MOS.	6/9	8/8	6/9
RECOV. LOSSES, %	0.5	0.5	0.5
DISCOUNT RATE, %	10.24%	10.24%	10.24%
INFLATION RATE, %	6%	6%	6%
CARRYING CHARGE (no-depr.), %	15.56	15.56	15.56
CARRYING CHARGE (with depr.), %	14.64	14.64	14.64
PLANT CAPACITY FACTOR, %	80	70	85
YEAR OF PLANT STARTUP	1991	2000	2000
INITIAL PLUTONIUM COST, \$/GM	21.0	18	24
16 YR. LEVELIZED PuCOST, \$/GM	28.9	24	24
FUEL CYCLE COSTS, HI TEMP.* (mills/kwh)	3.26	5.56	5.23
FUEL CYCLE COSTS, LO TEMP.* (mills/kwh)	2.9	4.79	5.79

*INCLUDING 6% INFLATION DURING 16 YR LEVELIZING PERIOD PER S,P&W

- (1) AS DEFINED BY GE/HEDL DERIVATION, LOW TEMPERATURE ASSEMBLY COST (INCLUDING AXIAL BLANKET) \$49.9K; FUEL COST \$591/KG. HIGH TEMPERATURE ASSEMBLY COST (INCLUDING AXIAL BLANKET) \$54.5; FUEL COST \$534/KG.
- (2) INCLUDED IN FUEL FAB. COST
- (3) DATA SUPPLIED 3/30/77 SUGGESTS \$141/KGM.

APPENDIX I

COMPARATIVE DATA
ON HIGH AND LOW TEMPERATURE DESIGNS

- Data Compiled by
CE and Reviewed by PLBR
Contractors for Accuracy 3/30/77

APPENDIX II

DISCUSSION OF PLUTONIUM MIGRATION IN

MIXED OXIDE FUEL

**Hanford Engineering
Development Laboratory**

II-1

P O BOX 1970 RICHLAND, WA 99352

7751106
March 15, 1977

E.B.R. MAR 18 1977

R. C. Noyes, Manager
C-E Power Systems
Combustion Engineering, Inc.
1000 Prospect Hill Road
Windsor, Connecticut 05095

- References: 1) Letter, R. C. Noyes to W. E. Roake, Concerning threshold effect, dated March 3, 1977.
- 2) D. C. Hata et al, "Plutonium Redistribution in the Outer Regions of Hypostoichiometric Mixed Oxide Fuel," HEDL-SA-1104, presented at the American Nuclear Society Meeting November 17, 1976, in Washington, D. C.

In response to your inquiry of Reference 1, HEDL is unaware of any "threshold effects" which limit fuel pin lifetime to the range of 100-120 MWd/kg. The specific questions you asked regarding plutonium migration were discussed by Mr. D. C. Hata at the ANS meeting in Washington, D. C., last November. A copy of his presentation, Reference 2, is attached.

Plutonium migrating from the outer regions of fuel operating at certain high power conditions may cause a thin annulus of plutonium-depleted UO_2 to form at or near the surface of the fuel column. The depleted annulus then tends to become an additional thermal resistance rather than heat source. One can calculate that some high burnup fuel that we have characterized may have experienced as much as an 8% reduction in power-to-achieve-incipient-melting compared with the case where it is assumed that no UO_2 rim forms. In a practical sense, the normal operating power of such a fuel pin would have been reduced in a reactor system by burnout in the driver fuel. Irradiation experience has not shown the plutonium relocation to be life-limiting. No confirming power-to-melt tests have been conducted on high burnup fuel.

If you have any additional questions on this matter please contact R. D. Leggett (509-942-3896 or FTS 444-3896) of my staff.

W. E. Roake, Manager
Fuels and Controls

pd

Attachment: HEDL-SA-1104

PLUTONIUM REDISTRIBUTION IN THE OUTER REGIONS OF HYPOSTOICHIOMETRIC
MIXED OXIDE FUEL

D. C. Hata, D. F. Washburn, R. A. Karnesky, and R. B. Baker

Paper to be presented in Washington, D. C., American Nuclear Society
Winter Meeting

November 15-19, 1976

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NOTE: Slides 2 through 11 have been omitted.

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PLUTONIUM REDISTRIBUTION IN THE OUTER REGIONS OF HYPOSTOICHIOMETRIC MIXED OXIDE FUEL

D. C. Hata, D. F. Washburn, R. A. Karnesky and R. B. Baker

Previous investigations of actinide redistribution have centered on the migration of plutonium to the central void which occurs during restructuring, at low burnups, in mixed-oxide fuel. Although this phenomenon was observed in our examination, it is not the subject of this paper and therefore will not be discussed. This afternoon, I will describe observations on a plutonium redistribution phenomenon that occurs in hypostoichiometric fuel at burnups ≥ 5.0 at% in fast reactors and present a preliminary analysis of its effect on fuel pin performance.

Unlike the low burnup migration which occurs primarily in the columnar grain region near the central void, the higher burnup redistribution phenomenon occurs in the equiaxed grain and outer regions of the fuel. Alpha autoradiography was used to qualitatively determine the extent and character of these redistribution phenomena. Subsequent electron microprobe analyses were used to quantitatively determine radial concentration profiles of plutonium, uranium, and selected fission products. To summarize the results, as shown in slide 1 the redistribution of plutonium resulted in peak concentrations in two different radial locations which were dependent on the burnup. At intermediate burnups, (~ 5 at%) (the upper part of the slide), plutonium was found in concentrations of $> 50\%$ on the very outside rim of the fuel and at high burnups, (~ 12.5 at%) (the lower part of the slide) plutonium in concentrations of $> 40\%$ was found in the equiaxed grain region. A calculated worst-case effect on fuel pin thermal performance was determined from the high burnup phenomenon. The analysis indicated the power to incipient fuel melting would drop $\sim 8\%$ and the fuel centerline temperature would correspondingly increase ~ 300 F° at a power rating of ~ 12 kw/ft due to plutonium migration.

The data and calculations presented here are based principally on the results of postirradiation examinations conducted on fuel pins from the PNL 5 subassembly. Fabrication details and operating parameters for

these pins are shown in slide 2. The PNL 5 subassembly was irradiated in EBR-II at a maximum pin power of 12 kw/ft and a maximum cladding inside surface temperature of 920°F. The fuel was composed of 25 wt% PuO_2 and 75 wt% fully enriched UO_2 with an initial O/M ratio of 1.96. The cladding was annealed 304 SS.

Fuel pins were destructively examined at burnups of 2.0, 5.0, 6.5, 12.5 at%. Fuel pin identification number, burnup and type of redistribution observed are tabulated in slide 3. As shown, the fuel pin examinations indicated a definite trend in the occurrence and character of Pu redistribution; no redistribution was observed at 2.0 at% burnup from the alpha autoradiographs. At ~ 5.0 at% burnup, higher Pu concentrations on the outer rim of the fuel began to occur as evident by half of the fuel pins examined exhibiting this redistribution. By 6.5 at% burnup, all pins exhibited Pu concentrations on the rim. However, slight Pu enrichment was found in the equiaxed grain region of one of the pins. At 12.5 at% burnup all pins examined exhibited Pu enrichments only in the equiaxed grain region.

Although alpha autoradiographs did not indicate plutonium redistribution in the outer regions of the fuel in fuel pins discharged at 2.0 at% burnup, as shown in slide 4, the electron microprobe results indicated that localized regions of high plutonium concentration existed on the fuel outside surface, in a second phase. This type of phase which was associated with Cs fuel reaction products is typified by its lack of structure and usually occurred in the fuel cladding gap, fuel grain boundaries and start-up cracks. This second fuel phase did not always contain high Pu at all burnups, but was always present when the redistribution phenomenon occurred.

At a burnup of ~ 5.0 at%, four fuel pins were destructively examined. In two of the pins examined, alpha autoradiographs, as shown in slide 5, indicated the presence of two layers on the fuel outer periphery, the outermost layer was high in Pu and the inner layer was depleted in Pu and high in uranium. Very little or no redistribution was observed in the other two fuel pins. Operating conditions and pin parameters did not indicate a reason for the difference in behavior, however, as seen in slide 6, the ceramography indicated an altered microstructure on the fuel rim in those fuel pins exhibiting plutonium redistribution. At high magnification, slide 7, the affected region exhibited numerous

voids, and wide affected grain boundaries containing an altered fuel phase. This type of microstructure was evident in all pins exhibiting Pu redistribution and was closely associated with those regions where Pu redistribution had occurred. The electron microprobe results indicated the high plutonium layer contained $\sim 50\%$ PuO_2 and corresponded to the altered fuel phase, Slide 8. The depleted layer contained $\sim 90\%$ UO_2 and occurred in the region of numerous voids and wide grain boundaries. Observations on other samples from the same pin suggested that this redistribution phenomenon was more pronounced in the upper part of the fuel column.

Three fuel pins were destructively examined at a burnup of 6.5 at%. Alpha autoradiographs indicated increased plutonium concentration on the outside rim of the fuel. In addition, as shown in slide 9, slight plutonium enrichment occurred in or near the equiaxed grain region in sections from one of the fuel pins. Associated with the higher plutonium concentration in the equiaxed grain region was higher beta-gamma activity. Microstructure and axial effects were consistent with lower burnup results.

At the highest burnup, 12.5 at%, all three fuel pins destructively examined showed high plutonium concentrations only in equiaxed grain regions. Consistent with results from the 6.5 at% burnup examination, a strong association between high beta-gamma activity and plutonium enrichments in the equiaxed grain region was noted on all sections, slide 10, when compared to the optical micrograph, a layer depleted in plutonium was evident, slide 11. Electron microprobe examination, as shown in slide 12, indicated that the $\text{PuO}_2/(\text{UO}_2 + \text{PuO}_2)$ ratio exceeded 45% in the equiaxed grain region. The outermost region was almost completely depleted of PuO_2 leaving essentially 100% UO_2 . Microstructural and axial effects were consistent with lower burnup results.

To evaluate the impact of this phenomenon on the thermal performance of fast reactor fuel, the hybrid computer version of SINTER heat transfer code was used to determine the effect of various radial power profiles on predicted fuel temperature. This was done by comparing the maximum fuel temperatures, as a function of the total power. In the case of a

fast flux, the radial power profile can be assumed to be the same as the radial distribution of fissile material. Since we wish to evaluate this effect for LMFBR fuel, plutonium was assumed to be the only fissile species, therefore two cases were considered: one, a uniform radial distribution of fissile material and two, the observed high burnup redistribution of plutonium. The high burnup distribution is expected to have the worst effect on fuel temperatures and power-to-melt since the UO_2 ring on the fuel surface will act to insulate the heat-generating fuel.

The impact of Pu migration to the equiaxed grain region is presented in slide 13. It can be seen that the observed redistribution, can result in up to an 8% decrease in the power to melt and about a 300°F increase in centerline temperatures at a power rating of ~ 12 kw/ft.

In conclusion; increasing burnup was found to strongly influence the radial location of the plutonium enriched regions. Significant segregation of plutonium and uranium was found at the high burnup (~ 12 a/o). In a fuel system of plutonium and natural uranium the high burnup phenomenon would result in up to an 8% decrease in the power-to-melt. However, this decrease would not limit the operating power level of a reactor since the high burnup fuel would have decreased its power generation by more than 8% through "burn-out" of fissile atoms. Study of this phenomenon is continuing with the examination of fuel pins from at least three subassemblies with a wide range of initial O/M's and burnups.

PLUTONIUM REDISTRIBUTION AT INTERMEDIATE (~5.0 AT %) AND HIGH (~12.5 AT. %) BURNUP

HEDL 7611-46.4

TYPICAL PLOT AT HIGH (12.5 at %) BURNUPS OF $\text{PuO}_2/\text{UO}_2 + \text{PuO}_2$ RATIO VERSUS RADIAL POSITION

11-8

CALCULATED EFFECT OF OBSERVED P_u DISTRIBUTION AT HIGH BURNUP ON POWER-TO-MELT, Q'_m

6-II

HEDL 7611-46.2

APPENDIX III

FUEL CYCLE COST METHODS

Fuel cycle costs quoted by the vendors ranged from 2.90 to 6.38 mills/kwh with AI and GE crediting the low temperature design with an advantage and W crediting the high temperature design. To verify the reported trends and remove inconsistencies due to assumptions C-E determined the single cycle, zero inflation "use" and "carrying" charges as itemized in Table III-1. The HEDL fuel fabrication cost formula (App. IV) was used to generate the listed fuel assembly costs. Using the Stauffer, Palmer and Wyckoff Method⁽¹⁾, and ERDA/EPRI guidelines (Table III-2), C-E determined the necessary translators (multipliers) to shift from single cycle, zero inflation fabrication, reprocessing and Pu credit use charges to 16 year levelized charges. These and resulting fuel cycle costs are presented for the AI, GE, W and C-E normalized designs as Tables III-2 to III-5 respectively.

Table III-1 lists the cost data gathered for determining use charges for fabrication, reprocessing and plutonium credit. ERDA/EPRI guidelines for performing PLBR fuel cycle costs were used by C-E (except where noted) and are attached as Table III-2.

Use cost (in mills/kwh) were determined by dividing charges per year or period by the number of kilowatt-hours generated over the same interval. Vendor data on the number, size and enrichment of fuel pins per assembly was used as input to the HEDL fuel fabrication cost formula (App. IV). Fuel assembly costs from \$49,500 to \$64,550 were determined. Insufficient blanket costing data led to the assumption of \$28,650/assembly (W quote) for AI, W and the normalized design. GE provided blanket fabrication cost data in terms of mass throughput per cycle. Fabrication costs estimated at \$150/KG-HM result in use charges similar to those of AI, W and C-E.

Fuel reprocessing use charges, as Table III-1 indicates, were determined using the EPRI guideline⁽¹⁾ figure of \$200/Kg-MO applied to the driver and axial blanket mixed oxide mass. Radial blanket reprocessing costs for the W, AI and normalized designs were taken to be \$46,350/assembly, (approximately equivalent to \$200/KGMO) whereas for GE \$200/KG-MO was applied to the total blanket mass

(1) T. R. Stauffer, R. S. Palmer, H. L. Wyckoff, "Breeder Reactor Economics," a paper prepared for Breeder Reactor Comp., July 1, 1975.

throughput per cycle. Total reprocessing costs were dominated by fuel (75% average) making the results less sensitive to changes in blanket reprocessing throughput.

The plutonium credit in Table III-1 is based upon selling the bred plutonium given in the Table at a \$24/gm levelized Pu sell price. The use charge (credit) simply averages the uninflated credit received over the kilowatt-hours sold during the same period.

Annual carrying charges for the entire system "lifetime" inventory are determined by the product of the carrying charge rate (non-depreciating) and the initial purchase cost (at \$18/gm) of the beginning of life inventory. Normalization is performed by dividing by the kilowatt-hours sold during the same period.

The zero inflation use charges generated are then multiplied by the appropriate translators⁽¹⁾ given in Tables III-3 to III-6 to obtain the 16 year levelized use and carrying charges for the three PLBR vendors and the C-E normalized design.

TABLE III-1
C-E Normalized Fuel Cycle Cost Analysis Components

	AI Lo	AI Hi	GE Lo	GE Hi	W Lo	W Hi	Normalized C-E Lo	Normalized C-E Hi
Cycle Length, days/yr	1142/3	775/2	868/2.4	788/2.1	1095/3	1095/3	12.7	2.0
Mwe, Net	938	938	900	900	923	938	766	893
Kwe-hours/year x 10 ⁻⁹ (80% C.F.)	6.57	6.57	6.31 ⁽¹⁾	6.31	6.47	6.58	5.37	6.26
F/A Cost/Assembly 1976\$	61,425	61,034	49,500	54,500	64,550	64,260	60,210	60,136
No. of Assemblies Replaced/Cycle	220	190	282	270	318	276	276	276
Fuel Cost/Cycle x 10 ⁻⁶ \$	13.5	11.6	13.96	14.72	20.5	17.7	16.6	16.6
B/A Cost/Assembly \$	28,650 ⁽²⁾	28,650 ⁽²⁾			28.650	28,650	28,650 ⁽²⁾	28,650 ⁽²⁾
No. of Assemblies Replaced/Cycle	141	86	(3)	(3)	117	108	72	72
Blanket Cost/Cycle x 10 ⁻⁶ \$	4.03	2.46	1.66	1.88	3.35	3.09	2.06	2.06
Total Fab. Cost/Cycle x 10 ⁻⁶ \$	17.5	14.1	15.62	16.60	23.85	20.8	18.66	18.66
Fab. Costs/Year or period, 10 ⁻⁶ \$	5.83	7.05	6.51	7.90	7.95	6.94	6.91	9.33
Fab. Costs - mills/kwh (1976\$)	.89	1.07	1.03	1.25	1.23	1.05	1.29	1.49
Rep. Cost - (Fuel/Blanket) \$/kgMO	200	200	200	200	200	200	200	200
Rep. Cost - (Fuel) \$/Assembly	41,500	41,500	31,400	37,000	48,450	45,880	40,800	40,800
Rep. Cost/Cycle - (Fuel) 10 ⁻⁶ (1976)\$	9.13	7.87	8.85	9.99	15.4	12.7	11.26	11.26
Rep. Cost (Blanket) \$/Assembly	46,350 ⁽²⁾	46,350 ⁽²⁾	(3)	(3)	46,350	46,350	46,350 ⁽²⁾	46,350 ⁽²⁾
Rep. Cost (Blanket), \$/Cycle x 10 ⁻⁶	6.53	3.98	2.5	2.8	5.56	5.14	3.33	3.33
Rep. Cost (Total) \$/Cycle x 10 ⁻⁶	15.66	11.85	11.35	12.79	20.96	17.84	14.59	14.59
Rep. Cost (Total) \$/Year x 10 ⁻⁶	5.22	5.92	4.73	6.09	6.99	5.95	5.40	7.29
Rep. Cost (Total) mills/kwh (1976)	.79	.90	.75	.96	1.08	.90	1.00	1.16
Fissile Gain/Year, KgPu	259.3	232.7	208/(12 mo)	213/(12 mo)	331	228	283	228
Pu Net Created, mills/kwhe	-0.95	-0.85	-0.69 ⁽⁴⁾	-0.75 ⁽⁴⁾	-1.23	-.83	-0.93	-0.87
System Fissile Inventory, KgPu	5827	5386	4900	4984	6834	5980	6405	5804
Carrying Charge on Sys.Inventory mills/kwh (0% Infl.)	1.03	0.96	0.87	0.88	1.24	1.06	1.26	1.19
Carrying Charge on Sys.Inventory mills/kwh (6% Infl.)	2.48	2.29	2.09	2.12	2.92	2.54	3.03	2.87

Table III- 1 (Continued)

- (1) Increased by 14.3% over GE estimate to use normalized capacity factors of 80% for all cases
- (2) Assumed equal to W quoted radial blanket costs
- (3) Hi temperature radial blanket - 12.53 MI'-HM/cycle; Lo-Temperature - 11.05 MT/cycle
- (4) GE data

TABLE III-2

Fuel Cycle Cost Components
(1975 \$)

	ERDA/EPRI Guidelines	C-E
FUEL FAB. COST (\$/KG-HM)	Not Defined	HEDL (App. IV)
RADIAL BLKT. COST (\$/KG-HM)	Not Defined	50
FUEL REPROC., SHIP, STORE (\$/KG/MO)	200	200
FAB./RECOV. TIME, MOS.	6/9	6/9*
RECOV. LOSSES, %	0.5	0
DISCOUNT RATE, %	10.24%	10.24%
INFLATION RATE, %	6%	6%
CARRYING CHARGE (no-depr.), %	15.56	15.56%
CARRYING CHARGE (with depr.), %	14.64	14.64%
PLANT CAPACITY FACTOR, %	80	80%
YEAR OF PLANT STARTUP	1991	2000
INITIAL PLUTONIUM COST, \$/GM	40% of U-235	18
16 YR. LEVELIZED PuCOST, \$/GM	40% of U-235 Levelized	24

* One year lead and lag time assumed for carrying charges

Table III - 3

AI FUEL CYCLE COSTS - S, P&W - 938 MWe - 80% CF

(Levelized from 2000-2016 - 76\$)

		ZERO INFLATION		6% INFLATION	
		Translators	mills/kWh	Translators	mills/kWh
Hi Temp. 2 yrs.	USE COSTS				
	FABRICATION	1.0	1.07	1.30	1.39
	REPROCESSING	1.0	0.90	1.65	1.48
	Pu, NET CREATED	1.0	-0.85	1.65	-1.40
			1.12		1.47
	CARRYING CHARGE COSTS				
	FABRICATION	.17	.18	.54	.58
	REPROCESSING	-.09	-.08	-.34	-.31
	Pu, NET CREATED	-.09	.08	-.34	.29
	Pu, PLANT CONSTANT LIFE INV.		0.96		2.29
			1.14		2.85
	TOTALS		2.26		4.32
Lo Temp. 3 yrs.	USE COSTS				
	FABRICATION	1.0	0.89	1.27	1.13
	REPROCESSING	1.0	0.79	1.70	1.34
	Pu, NET CREATED	1.0	-.95	1.70	-1.61
			0.73		0.86
	CARRYING CHARGE COSTS				
	FABRICATION	.20	.18	.48	.43
	REPROCESSING	-.13	-.10	-.46	-.36
	Pu, NET CREATED	-.13	.12	-.46	.44
	Pu, PLANT CONSTANT LIFE INV.		1.03		2.48
			1.23		2.99
	TOTALS		1.96		3.85

Table III - 4

GE FUEL CYCLE COSTS - S, P&W - 900 MWe 80% C.F.

(Levelized for 2000-2016 - 76\$)

	ZERO INFLATION		6% INFLATION	
	Translators	mills/kWh	Translators	mills/kWh
<u>Hi Temp.</u> 2.1 yrs.				
USE COSTS				
FABRICATION	1.0	1.25	1.34	1.67
REPROCESSING	1.0	0.96	1.70	1.63
Pu, NET CREATED	1.0	<u>-0.75</u>	1.70	<u>-1.28</u>
		1.46		2.02
CARRYING CHARGE COSTS				
FABRICATION	.18	0.22	0.58	.72
REPROCESSING	-.10	0.09	-.38	-.36
Pu, NET CREATED	-.10	0.08	-.38	.31
Pu, PLANT CONSTANT LIFE INV.		<u>0.88</u>		<u>2.41</u>
		1.09		2.79
TOTALS		<u>2.55</u>		<u>4.81</u>
<u>Lo Temp.</u> 2.4 yrs.				
USE COSTS				
FABRICATION	1.0	1.03	1.36	1.40
REPROCESSING	1.0	0.75	1.72	1.29
Pu, NET CREATED	1.0	<u>-0.69</u>	1.72	<u>-1.19</u>
		0.89		1.50
CARRYING CHARGE COSTS				
FABRICATION	.19	0.20	0.67	0.69
REPROCESSING	-.11	-.08	-0.42	-0.31
Pu, NET CREATED	-.11	.09	-0.42	0.33
Pu, PLANT CONSTANT LIFE INV.		<u>0.87</u>		<u>2.09</u>
		1.08		2.80
TOTALS		<u>1.97</u>		<u>4.30</u>

Table III - 5
W FUEL CYCLE COSTS - S, P&W - 900 MWe
(Levelized from 2000-2016 - 76\$)

		ZERO INFLATION		6% INFLATION	
		Translators	mills/kWh	Translators	mills/kWh
Hi Temp. 3 yr. cycle 80% C.F.	USE COSTS				
	FABRICATION	1.0	1.10	1.27	1.40
	REPROCESSING	1.0	0.94	1.70	1.60
	Pu, NET CREATED	1.0	-0.87	1.70	-1.48
			1.17		1.52
	CARRYING CHARGE COSTS				
	FABRICATION	0.20	.22	0.48	0.53
	REPROCESSING	-.13	-.12	-.46	0.43
	Pu, NET CREATED	-.13	.11	-.46	.40
	Pu, PLANT CONSTANT LIFE INV.		1.11		2.65
Lo Temp. 3 yr. cycle 80% C.F.	TOTALS		1.32 2.49		3.15 4.67
	USE COSTS				
	FABRICATION	1.0	1.26	1.27	1.60
	REPROCESSING	1.0	1.11	1.70	1.89
	Pu, NET CREATED	1.0	-1.26	1.70	-2.14
			1.11		1.35
	CARRYING CHARGE COSTS				
	FABRICATION	0.20	0.25	0.48	0.60
	REPROCESSING	-0.13	-0.14	-0.46	-0.51
	Pu, NET CREATED	-0.13	0.16	-0.46	0.58
	Pu, PLANT CONSTANT LIFE INV.		1.27		3.00
	TOTALS		1.54 2.65		3.67 5.02

Table III-6

C-E FUEL CYCLE COSTS - S, P&W - NORMALIZED DESIGNS

(Levelized from 2000-2016 - 75\$)

		ZERO INFLATION		6% INFLATION	
		Translators	mills/kWh	Translators	mills/kWh
<u>Hi Temp.</u> 2 yrs.	USE COSTS				
	FABRICATION	1.0	1.49	1.30	1.94
	REPROCESSING	1.0	1.16	1.65	1.91
	Pu, NET CREATED	1.0	-0.87	1.65	-1.44
			1.78		2.41
	CARRYING CHARGE COSTS				
	FABRICATION	.17	0.25	0.54	0.80
	REPROCESSING	-.09	-0.19	-0.34	-0.39
	Pu, NET CREATED	-.09	0.08	-0.34	0.30
	Pu, PLANT CONSTANT LIFE INV.		1.19		2.87
			1.33		3.58
	TOTALS		<u>3.11</u>		<u>5.99</u>
<u>Lo Temp.</u> 2.7 yrs.	USE COSTS				
	FABRICATION	1.0	1.29	1.28	1.65
	REPROCESSING	1.0	1.00	1.68	1.68
	Pu, NET CREATED	1.0	-0.93	1.68	-1.56
			1.36		1.77
	CARRYING CHARGE COSTS				
	FABRICATION	0.19	0.24	0.73	0.94
	REPROCESSING	-0.11	-0.11	-0.46	-0.46
	Pu, NET CREATED	-0.11	0.10	-0.46	0.43
	Pu, PLANT CONSTANT LIFE INV.		1.26		3.03
			1.49		3.94
	TOTALS		<u>2.85</u>		<u>5.71</u>

APPENDIX IV

HEDL RECOMMENDATION FOR ESTIMATION OF FABRICATION COSTS

LMFBR FUEL FABRICATION COSTS FOR USE IN THE LARGE
HETEROGENEOUS REFERENCE FUEL DESIGN STUDY

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The LMFBR fuel fabrication cost is a strong function of the reactor core design, the fabrication technology, and the fabrication plant size. Since all of these are continually evolving, it is necessary to develop fabrication cost relationships which are a function of the basic fuel design variables, the fabrication technology, and the size of the LMFBR industry. A study is currently underway at HEDL to develop such relationships, and some preliminary results from this study⁽¹⁾ are described in this report. Additional results should be available in approximately six months.⁽²⁾

The study performed at HEDL to date has consisted of an analysis of the individual components of the fabrication cost based upon: 1) the technology used to fabricate the FTR and CRBR fuel, 2) the technological improvements associated with a fuel fabrication plant utilizing the processes associated with the High Performance Fuels Laboratory (HPFL), and 3) the economies of scale associated with the transition to a mature LMFBR industry containing large fabrication plants with high throughput rates. The results have been put into a format useful to the LMFBR core designer. The results have also been used to estimate typical fabrication costs for different LMFBR core designs.

An expression which describes the fabrication cost as a function of the basic core design variables, and which was used successfully by General Electric in the Phase I PLBR work is

$$C = \alpha_1 \cdot \frac{R^*}{R} + \alpha_2 + \alpha_3 \cdot n_f + \alpha_4 \cdot m_f + \alpha_5 \cdot Em_f + \alpha_6 \cdot n_p + \alpha_7 \cdot m_s \quad (1)$$

where: C = LMFBFR core assembly fabrication cost (\$/assembly)
 R^* = fabrication plant design throughput (220 days/yr)
 (MT HM/yr)
 R = fabrication plant throughput (MT HM/yr), $R \leq R^*$
 n_f = number of fuel pins per assembly
 m_f = heavy metal mass in the active core region of an
 assembly (kg HM)
 Em_f = fissile Pu mass per assembly (kg Pu_f)
 n_p = number of fuel pellets per assembly
 m_s = steel mass per assembly (kg).

The definition of the coefficients α_i in Eq. (1) is as follows:

α_1 = annual plant and equipment capitalization cost = $a_1 C^*$
 α_2 = constant cost independent of fuel design = $a_2 C^*$
 α_3 = cost proportional to pins/assembly = $a_3 C^* / n_f^*$
 α_4 = cost proportional to heavy metal mass/assembly =
 $a_4 C^* / m_f^*$
 α_5 = cost proportional to fissile Pu mass/assembly =
 $a_5 C^* / Em_f^*$
 α_6 = cost proportional to fuel pellets/assembly = $a_6 C^* / n_p^*$
 α_7 = cost proportional to steel mass/assembly = $a_7 C^* / m_s^*$

where C^* is the cost of fabricating a reference LMFBFR fuel assembly characterized by starred (*) variables in a reference fabrication plant. The coefficients a_i represent the fractional contribution of each cost component to the total cost, while the coefficients α_i represent the absolute contribution of each cost component to the total cost. Both the a_i and the α_i will depend upon the fabrication plant size and the fabrication technology.

Table I shows the estimated relative values of the fractional fabrication cost components a_i as a function of fabrication plant size. The cost analysis of the first two fabrication plants (3 and 100 MT/yr) was based on current fabrication technology as applied to CRBR fuel. The cost analysis of the last three plants (15, 100, and 300 MT/yr) was based upon a technology of the HPFL type which would be available to the LMFBR industry after commercialization. A comparison of these components shows the annual plant fixed cost component to be the parameter changing most rapidly with fabrication plant size. As can be seen, this drops from 0.433 for a 3 MT/yr plant to 0.124 for a 300 MT/yr plant. The other components either remain relatively constant or contribute little to the total fabrication costs.

Table II shows the cost coefficients α_i which are required by Eq. (1). These cost coefficients were determined for a CRBR fuel assembly design as fabricated in each of the five plants described above. The cost coefficients--coupled with Eq. (1)--provide a means of calculating fuel fabrication costs for different LMFBR designs and for different stages of development of the LMFBR fabrication industry.

The data required to compute fabrication costs for the CRBR--and for a set of advanced core designs developed at HEDL--are shown in Table III. The fabrication costs calculated for these designs using Eq. (1) for various fabrication plant sizes are shown in Table IV. As can be seen, fabrication costs in \$/kg HM could be reduced by as much as a factor of eight as both core design and fabrication technology evolve.

The effect of fabrication technology and fabrication plant size on fabrication costs of a CRBR fuel assembly is shown in Figure 1.

References

1. D. K. Quigley, "LMFBR Fuel Fabrication Cost Study," Internal Memo, Westinghouse Hanford Company, December 3, 1976.
2. T. T. Claudson, Personal Communication.

Table I
Fractional Fabrication Contribution by Component⁽¹⁾

a_i Components	Fabrication Plant Size, R^* ⁽²⁾				
	Current Technology		HPFL Technology		
	3 MT/yr	100 MT/yr	15 MT/yr	100 MT/yr	300 MT/yr
Annual Cost	0.385 (0.433)	.426 (.475)	0.348 (0.394)	0.142 (0.168)	0.104 (0.124)
Constant Cost	0.131 (0.121)	.085 (.078)	0.136 (0.126)	0.166 (0.161)	0.176 (0.172)
Fuel Pin Cost	0.238 (0.219)	.205 (.188)	0.197 (0.183)	0.297 (0.288)	0.308 (0.301)
Heavy Metal Cost	0.130 (0.120)	.147 (.134)	0.183 (0.170)	0.216 (0.210)	0.229 (0.224)
Fissile Cost	0.030 (0.028)	.034 (.031)	0.030 (0.028)	0.063 (0.161)	0.068 (0.066)
Fuel Pellet Cost	0.048 (0.044)	.065 (.059)	0.015 (0.014)	0.023 (0.022)	0.014 (0.014)
Steel Cost	0.038 (0.035)	.038 (.035)	0.091 (0.085)	0.093 (0.090)	0.101 (0.099)

(1) Values in () include 8% interest during 5-year construction period of fabrication plant.

(2) Reference design.

Table II
Fabrication Cost Coefficients⁽¹⁾ for Eq. (1)

α_i Coefficients	Fabrication Plant Size, R* ⁽²⁾				
	Current Technology		HPFL Technology		
	3 MT/yr	100 MT/yr	15 MT/yr	100 MT/yr	300 MT/yr
Annual Cost	58135 (70948)	32674 (39875)	22968 (28030)	4544 (5545)	3016 (3681)
Constant Cost	19781	6520	8976	5312	5104
Fuel Pin Cost	165.61	72.46	59.92	43.80	41.16
Heavy Metal Cost	594.8	341.7	366	209.5	201.2
Fissile Cost	1192.1	686.3	521.1	530.5	518.9
Fuel Pellet Cost	0.2320	.1596	0.0317	0.0236	0.0130
Steel Cost	41.28	20.97	43.21	21.41	21.07

(1) Values in () include 8% interest during 5-year construction period of fabrication plant.

(2) Reference design.

Table III

Sample Data for LMFBR-Fuel Fabrication Cost Equation

		LMFBR Oxide Designs			
		<u>CRBR*</u>	<u>HEDL Small Pin Oxide Design</u>	<u>HEDL Large Pin Oxide Design</u>	<u>HEDL Advanced Large Pin Oxide Design</u>
n_f	- fuel pins/assy	217	271	271	271
m_f	- fuel mass/assy (kg HM)	33	45.5	77.5	77.4
Em_f	- fissile mass/assy (kg Pu_f)	3.80	5.55	9.27	8.86
n_p	- fuel pellets/assy (144 x n_f)	31248	39024	39024	39024
m_s	- steel mass/assy (kg)	139	119	201	199

*Reference design

Table IV
LMFBR Core Fabrication Costs
(\$/kg HM)⁽¹⁾

<u>Reactor Design</u>	<u>Fabrication Plant Size, R[*](2)</u>				
	<u>Current Technology</u>		<u>HPFL Technology</u>		
	<u>3 MT/yr</u>	<u>100 MT/yr</u>	<u>15 MT/yr</u>	<u>100 MT/yr</u>	<u>300 MT/yr</u>
CRBR	5,000	2,500	2,150	1,000	900
HEDL Small Pin Oxide Design	4,000	2,000	1,740	850	770
HEDL Large Pin Oxide Design	2,700	1,400	1,240	630	580
HEDL Advanced Large Pin Oxide Design	2,700	1,400	1,240	630	580

(1) 1976 dollars including 8% interest during 5-year construction period of fabrication plant.

(2) Fabrication plant is assumed to be operating at design throughput (220 days/yr).

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Figure 1. The Effect of Technology and Fabrication Plant Size on CRBR Fabrication Costs