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FINAL REPORT**

Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems

**Final Report
Sept. 15, 2002-Sept. 14, 2005**

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Abstract

This is the final report under award DE-FG07-02SF22608, project no. 2002-005, “Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems.” GFR cores having steady-state reload enrichments of 5 w/o U-235 were successfully developed and shown to satisfy neutronic, thermal hydraulic, materials and economic criteria. An innovative inverted (UC fuel outside coolant tube) vented fuel assembly design was developed to enable meeting project goals. The core was shown to be compatible with a conventional HTGR balance of plant (PCR/V, helium primary coolant, Rankine power conversion system). B&B cores were found to be economically competitive on a once-through fuel cycle, without requiring reprocessing or recycle, and utilize natural uranium three to four times more efficiently than LWRs. However, fuel can be blended with natural uranium and reused in B&B GFRs or LWRs after only minimal processing.

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Nuclear Energy Research Initiative

Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems

PI: Michael Driscoll, Massachusetts
Institute of Technology

Project Number: 02-005

Project Start Date: September 2002

Collaborators: Idaho National
Laboratory

Project End Date: September 2005

Executive Summary of Final Report

Research Objectives

The goal of this project was to develop a practical implementation of breed and burn operation in hard spectrum gas cooled fast reactors. In the present context, “breed and burn” (B&B) refers to cores in which steady-state reload fuel has a significantly lower enrichment than that required to sustain criticality, in the present instance 5 w/o U^{235} (startup core has 8 w/o avg., 10 w/o max.). Exceptional neutronic efficiency allows sufficient plutonium to build up to sustain burnup to very high values (e.g. ≥ 150 Mwd/kg). In the ideal case, recycling is not required (but neither is it precluded) to achieve natural uranium utilization that is significantly higher (factor >3) than current light water reactor (LWR) units. High utilization leads to competitive fuel cycle economics and the practicality of commercializing fast reactors without first deploying reprocessing and fuel recycling facilities.

Research Accomplishments

A successful version of the subject concept was devised. Table 0.1 summarizes the final design features. The high volume fraction fuel required for successful use of UC fuel (in lieu of the inordinately expensive alternative UN-15) motivated use of an innovative vented tube-in-duct fuel assembly design, and, to keep circulator power tolerable, a large coolant temperature rise across the core. This in turn dictated use of a conventional Rankine cycle. The result was that the balance of plant is of entirely conventional design – using proven British AGR and US/German HTGR technology. Thus virtually all of the ultimate project focus, as it evolved, was centered on core and fuel design and performance assessment.

The researchers also successfully transferred the shutdown cooling loop design developed by MIT under another contract (INERI, managed by ANL and CEA) for a helium-cooled cercer core, to the B&B application.

**Table 0.1. Reference Design Features of Preferred Final Version
of Breed and Burn GCFR Concept**

Core	Design	Comments:
Fuel:	UC	UO ₂ not viable neutronically
		Reaction of CO ₂ with UC precludes its use as coolant
Clad:	ODS	ODS may be able to resist creep adequately up to $\approx 750^{\circ}\text{C}$
Configuration:	Tube-in-Duct; Vented; Orificed	Lower fuel T than pin-type at increased fuel fraction; venting eliminates ΔP across clad
Coolant:	He @ 10 MPa, Indirect Cycle; Core $\Delta T_c = 380^{\circ}\text{C}$, Exit T $\approx 600^{\circ}\text{C}$	He is inert chemically, used in thermal HTGRs
Thermal-Hydraulics:	AXIAL Peaking Factor = 1.45, Radial Peaking Factor = 1.77, Power Density 130 W/cc Peak clad T = 716°C	Orificing reduces circulator power by factor of ≈ 2
Fuel Management:	5 w/o U-235 reloads Startup Core: 8 w/o avg., 10 w/o max.	6 radial zones; 1/6 th of core replaced each fueling
Burnup:	150 MWd/kg over 18 EFY	assembly $\rho > 0$ @ ≈ 20 MWd/kg core ρ peaks @ ≈ 80 MWd/kg
Plant		
Power Cycle:	Rankine 2400 MW _{th}	Allows $\approx 380^{\circ}\text{C}$ ΔT_c across core, which reduces coolant flow rate, hence circulator power
Reactor Vessel:	Prestressed Concrete Reactor Vessel (PCR/V)	PCR/V is proven in GCR and HTGR service (but at lower P)
Shutdown Cooling System: (combined shutdown & emergency)	<ul style="list-style-type: none"> • 4 x 50% capable forced convection loops • Water-boiler heat sink 	PRA-guided design supports this selection (basically same No. loops as GCFRs of the 1970's) Natural convection alone suffices if $P \geq 35$ atm (15 if CO ₂ injected)
Containment:	PWR type sized to keep post-LOCA pressure ≤ 5 atm	Combined with CO ₂ injection this permits decay heat removal solely by natural convection after ~ 24 hrs

With respect to the back end of the fuel cycle it was shown that, if otherwise acceptable, spent B&B GFR fuel could be re-used as B&B GFR reload fuel by blending it, after minimal, non-separative AIROX-type processing, with natural or depleted uranium; or used in a PWR after 50/50 blending with 4.4 w/o U-235 UO₂. Laboratory studies confirmed that it was advantageous to use CO₂ in place of air as the oxidizing medium, and powdered graphite as the

reductant. As LWR fuel, the previously accumulated fission product, Sm-149, acts as a self-provided burnable poison.

The principal need for future R&D in support of this concept is in the area of analytical and experimental qualification of fuel assembly design, and confirmation of clad and fuel materials endurance under core fluence and temperature conditions.

NERI Project Final Report

“Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems”

Project No. 2002-005

Period: Sept. 15, 2002- Sept. 14, 2005

Chapter 1

Foreword

1.1 Research Objectives

The goal of this project was to develop a practical implementation of breed and burn (B&B) operation in hard spectrum fast reactors. In the present context, “breed and burn” refers to cores for which reload fuel has a significantly lower enrichment than that required to sustain criticality: here ≤ 5 w/o U-235, but sufficient plutonium is created in situ to sustain core criticality until the fuel is driven to high burnup. In the ideal case, no recycle is required (but not precluded) to still achieve natural uranium utilization significantly higher than current light water reactor (LWR) units. Successful implementation of the B&B concept would permit breeder reactor deployment in advance of full-scope fuel reprocessing and recycle.

The general concept dates back to the late 1950’s; but, despite sporadic interest since, has not generated much top-level interest because of the unresolved challenges that are the subject of the present research: the need for a large, low-leakage core with minimal non-heavy-metal fuel diluents, plus high power density and burnup to accelerate and sustain fissile buildup such that one moves quickly to an equilibrium fuel cycle.

In contrast to the contemporary effort in Russia on a lead-cooled reactor of this type, the present project has centered on the use of gas-cooled fast reactor units (GFR) because their harder spectrum enhances B&B performance. This requires addressing attendant performance issues: most notably, the provision of highly reliable post-shutdown decay heat removal, with, preferably, a significant contribution by passive means.

Fuel integrity is another priority focus because of its high burnup, high fluence, and time-at-temperature. The unique environment and service conditions will require the application of advanced fuels and materials that have only recently become available. Consequently an important body of practical effort will be necessary to demonstrate fabrication and performance under near prototypic conditions.

Finally, we investigated fuel re-use employing non-separative reprocessing technology (e.g., AIROX-type successive oxidation-reduction) with two goals in mind: 1) the refabrication and re-insertion of fuel into a GFR (existing or new startup) to further improve fuel utilization, and 2) the recycling of the fuel into LWRs to improve overall economics and spoil the plutonium isotopic mix for potential diversion to weapons uses.

1.2 Administrative Aspects

This project was organized under four major tasks (each with two or more subtasks), with contributions apportioned among the collaborating organizations, namely:

<u>Originally</u>	<u>Subsequently</u>
MIT Nuclear Engineering Dept., Center for Advanced Nuclear Energy Systems (CANES)	MIT Nuclear Science and Engineering Dept., post Dec. 2004, and CANES
INEEL ANL-West	INL, post Feb. 2005
MIT	UNLV (Sub-award via MIT) post Jan. 2004

Principal Tasks were:

- Task A: Core Physics and Fuel Cycle
- Task B: Core Thermal Hydraulics
- Task C: Plant Design
- Task D: Fuel Design

While this task structure was appropriate for execution of the work plan, the strong elements of interrelation and feedback made it more appropriate to report final results in a somewhat modified format, as addressed next.

1.3 Organization of Final Reporting

The structure decided upon by the participants was to issue a final overall summary report (this report) prepared by the principal investigator: M.J. Driscoll, which drew upon, and synthesized, four major final topical reports:

1. P. Yarsky, M. J. Driscoll, P. Hejzlar, "Integrated Design of a Breed and Burn Gas-Cooled Fast Reactor Core," MIT-ANP-TR-107, Sept. 2005
2. K.D. Weaver, T. Marshall, James Parry, "Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems: Annual and Final Report," INL/EXT-05-00886, October, 2005
3. D. M. Wachs, M. K. Meyer, "Engineering and Physics Optimization of Breed and Burn Fast Reactor Systems: Annual and Final Report - Fuel Design," INL/EXT-05-XXXXX, December, 2005
4. D. Crawford, K. Czerwinski, "Kinetic Evaluation of Fission Element Removal by CARDIO," UNLV-RDCH-05-03, 2005

The present report contains chapters as follows: (2) Overview, (3) Core Neutronics, (4) Core Thermal Hydraulics, (5) Balance of Plant, (6) Fuel Processing, and finally (7) Conclusions and recommendations. A nomenclature compendium is appended.

Chapter 2

Overview

2.1 Introduction

The more narrowly focused chapters which follow are best understood in terms of the unique requirements imposed on a B&B reactor in general, and its GFR embodiment in particular. These fall into two categories: the core and the balance of plant. In turn two high level technical factors dictate virtually everything which follows:

CORE

Peak asymptotic reactivity following long irradiation of a fissile/fertile mix, here represented by Pu-239 and U-238 is just:

$$\beta = 1 - \frac{2 + \left(\frac{\delta_{28}}{1 + \alpha_{49}} \right)}{\eta_{49}(1 + \delta_{28})} \quad (2-1)$$

where δ_{28} is the fertile-to-fissile fission ratio, α_{49} the fissile capture to fission ratio, and η_{49} the fissile neutron yield per absorption. Since $\delta_{28}/(1 + \alpha_{49}) \ll 2$, the denominator dominates. It increases monotonically as the spectrum hardens: hence the emphasis on avoidance of core diluents in B&B cores, and a reason to prefer gas cooling.

BOP

The ratio of primary coolant circulator power to thermal power transported is inversely proportional to the coolant temperature rise (or equivalently mass flowrate) across the core to the power 2 or 3, depending on one's other assumptions and constraints. This ultimately forced us to abandon the originally postulated use of the supercritical CO₂ Brayton cycle in favor of a conventional Rankine cycle, which allows us to more than double core ΔT .

2.2 Breed and Burn Core Characteristics

The B&B reactor differs from other fast reactors mainly in its core design and fuel management strategy. As such it exploits a significantly different region of option space than conventional cores. Figure 2.1 compares some of the principal B&B core characteristics to those of a conventional GFR.

Fig. 2.1 Illustrative Option Space Map for Generic Breed & Burn vs. Conventional GFR Cores

Starting at “noon” and moving clockwise one finds:

- (1) Volume fraction fuel is much higher because of the need for exceptionally efficient neutronics, with low leakage and moderation. Fuel heavy metal density is further enhanced by using UC instead of UO_2
- (2) Coolant void reactivity is significant despite the low coolant volume fraction because of the large low-leakage core (hence spectrum-hardening effects dominate): again, large core size is the consequence of requiring excellent neutron economy
- (3) Specific power is low, to keep clad and fuel temperatures within tolerable limits despite the high fuel/low coolant configuration. This leads to long in-core residence times to achieve high burnup (e.g. ~ 150 MWd/kg): ~ 18 years for the B&B fuel vs ~ 3 years for a GCFR of 1970s vintage (because of their higher power density and lower burnup).
- (4) Doppler reactivity feedback is reduced because of the ultra-hard spectrum needed to enhance U-238 fission rate (among other benefits). This constrains the inherent prompt negative reactivity feedback needed in response to large break loss of coolant accidents.
- (5) Steady state reload enrichment is much lower. This is the essence of the B&B concept, from which all other core characteristics follow. It confers the benefits of improved natural uranium utilization compared to LWRs, with competitive fuel cycle economics, without the requirement for separative reprocessing (non-separative refabrication is beneficial, but not absolutely required).
- (6) The burnup reactivity swing is positive, in contrast to the loss typical of conventional designs. This means that control rods must be inserted as burnup progresses.
- (7) High fuel fraction requires low coolant fraction, which leads to higher pressure drop and circulator power consumption. This is partially offset by the use of vented, tube-in-duct fuel assemblies (no grid spacers or fission gas plena), and:
- (8) A large coolant temperature rise across the core, which reduces mass flow rate. It also favors a Rankine power conversion system over a highly recuperated Brayton cycle.

The parameters displayed on the axes of Fig. 2.1 are not independent of one another.

Thus it is also useful to display a flow chart showing the cause and effect linkage among the eight items of interest: see Fig. 2.2.

As shown, all consequences flow from the need for a reload enrichment which is significantly less than the maximum “asymptotic” fissile enrichment of about 10% reached at about 80 MWd/kg burnup. In our work a value of 5 w/o U-235 has been specified, in part because this is the upper limit currently licensed for use in LWR fuel. Other breed and burn investigators postulate enrichments as low as natural or depleted uranium. While this may be

neutronically possible, the consequences traced in Fig. 2.2 are all aggravated to the extent that practical feasibility is compromised. For example, fuel residence times required may exceed decades.

Figure 2.2 Cause and Effect Sequence for B&B Core Design Parameters

2.3 B&B Balance of Plant Characteristics

The breed and burn core requirements lead directly to most of the key decisions made with respect to balance of plant technology. Figure 2.3 traces through the major branch points.

The decision having the most significant impact is that to employ a conventional Rankine steam cycle. Experience with the British AGR units, and with Fort St. Vrain and THTR provides ample assurance of the feasibility of this approach and effectively eliminates the need for any further analysis or R&D. However, it also forgoes the potential plant capital cost savings of on the order of 15% predicted (but not yet demonstrated) for adopting a Brayton Power Conversion cycle. Busbar cost of electricity in mills/kWhr is further increased by the 2 to 4% lower efficiency of the Rankine cycle.

Similarly, the use of helium as the primary coolant confers many advantages, eliminates many concerns, and takes full advantage of Fort St. Vrain and THTR experience (see Table 2.1). However, helium is significantly inferior to CO₂ in the natural convection mode, and would be ineffective unless post-LOCA pressure could be kept above about 10 atm. This motivates the decision to do the utmost to avoid LOCA by using a leak-resistant reactor vessel, and by building in an active cooling system having ultra high reliability.

Fig. 2.3 Taxonomy of B & B Balance of Plant Design Decisions

Table 2.1 Summary of Relevant GCR Rankine Cycle Experience

	Reactor			
	AGR	Ft. St. Vrain	THTR	GA GCFR*
<u>Primary System</u>				
Coolant	CO ₂	He	He	He
Pressure, MPa	4.35	4.8	4.0	8.9
Core:				
outlet T, °C	635	775	750	550
inlet T, °C	335	405	250	323
<u>Reactor Vessel</u>	PCR V	PCR V	PCR V	PCR V
<u>Steam Plant</u>				
Pressure, MPa	16	16.5	18	8.0
Turbine inlet Temperature, °C	530	538	550	494
Efficiency, %	40	39	40	36

* designed in the 1970's, but not built

As noted in Table 2.1, only thermal GCRs have been constructed and operated. However, in the US a GCFR was designed in some detail in the 1970's, making extensive use of thermal GCR experience. Fig. 2.4 shows a cutaway of the PCR V for a 350 MWe demo unit; designs up to the 1200 MWe needed for B & B service are documented, and the GEN-IV GFR program is also investigating large units. Accordingly the balance-of-plant is not given further significant attention in this assessment of B & B feasibility. However, there is one modification, which would benefit all GFR concepts, which deserves further evaluation: The use of a prestressed cast iron vessel (PCIV) (2-1). Figure 2.5 shows a 3000 MWth VHTR design using this technology.

Fig. 2.4 350-MW(e) GCFR, Cutaway of PCR-V

Fig. 2.5 VHTR for Process Heat, Using PCIV (from Ref. 2-2)

2.4 Preview of Fuel Cycle Costs

Although this subject will be dealt with at a more sophisticated and design-specific level in Chapter 3, a preliminary discussion of some basics will help in the understanding of the unfamiliar economic aspects of B&B cores. The following treatment is approximate, but reasonably accurate, using generic values for a hypothetical B&B example.

Fuel cycle cost in mills per kilowatt hour levelized as of the start of irradiation is given by:

$$fcc = \left(\frac{C}{24\eta B_d} \right) \left(\frac{XT}{1 - e^{-XT}} \right) \text{mills/kWhre} \quad (2-2)$$

where:

C	=	Cost per kg of fuel as of the start of irradiation
η	=	Plant thermodynamic efficiency, here 0.40
B_d	=	Fuel discharge burnup, e.g. 150 MWd/kg
X	=	Discount rate, e.g. 0.1/yr; and
T	=	$B_d/0.365sp.L$, duration of fuel residence in core, yrs, in which
sp	=	Fuel specific power, 22 kW/kgHM, and
L	=	Plant capacity factor; assume 0.9

Hence $T = \frac{457}{sp} = 20.8 \text{ yrs}$ (2-3)

To obtain an estimate for C, one proceeds as follows; for the front end transactions per kilogram of fuel:

Natural U: $\frac{F}{P} = 2.43X - 0.73$ (2-4)

Separative Work: $\frac{S}{P} \cong 2.07X - 3.23$ (2-5)

(both at 0.3 w/o enrichment plant tails)
where X = weight % U-235

Take for the unit costs per kg, heavy metal (i.e. U)

50	\$/kg	for UNAT
110	\$/kg	for SWU
500	\$/kg	for Fabrication

which, ignoring the carrying charges between payment and start of irradiation, gives:

$$C = 349X + 108 \text{ \$/kg} \quad (2-6)$$

And finally for $X \approx 5$ w/o, the B&B steady state reload enrichment, one has

$$C \approx 1850 \text{ \$/kg}$$

This gives

$$fcc = 3.0 \text{ mills/kWhre}$$

to which one can add the 1.0 mill/kWhre US DOE waste disposal fee to obtain 4.0 mills/kWhre.

For a PWR at the same enrichment, $B_d=60$ MWd/kg, $\eta=0.33$ and $sp=34$ kW/kg, hence $T=5.4$ yrs, in which case (at the same fabrication cost) $fcc=5+1=6$ mills/kWhre, roughly what is experienced.

Thus, because of their high burnup, B&B cores can have a competitive fuel cycle cost.

It is also clear that their low specific power prevents achievement of an even lower fcc; the value of 22 kW/kg is approximately that of a BWR and much less than the 95 W/kg proposed for the 1970's era GA GCFR designs.

The reason for the large reduction in specific power is the current preference for a large "passive" natural convection contribution to shutdown cooling, especially post-LOCA. This also leads to extraordinarily long in-core residence durations for fuel assemblies: 20.8 years in the above example. This leads to an extremely large contribution by carrying charges (second term in brackets in the fcc equation). Perhaps creative financing can help here: The direct-only cost in the above example is 1.28 mills/kWhre: just over 40% of the total!

One other point worthy of note is the larger waste fee assessed against GFR (indeed any high burnup core, fast or thermal).

On a per kg initial heavy metal basis waste disposal costs, for a 1 mill/kWhre fee, becomes:

$$W=24\eta B_d \quad (2-7)$$

Hence for:

$$\begin{aligned} \text{GFR @ } 150 \text{ MWd/kg, } W &= 1440 \text{ \$/kg} \\ \text{PWR @ } 60 \text{ MWd/kg, } W &= 475 \text{ \$/kg} \end{aligned}$$

It is worth questioning whether charging on a per kWhre basis rather than per kg is a fair representation of actually incurred costs.

Finally, resource utilization is a useful metric for sustainability.

$$\frac{MWde}{kg U_{NAT}} = \eta B d / (F / P)$$

which in the above example gives:

$$PWR @ 1.73 MWde/kg U_{NAT}$$

and

$$GFR @ 5.25 Mwde/kg U_{NAT},$$

a factor of 3 larger.

2.5 Chapter Summary

The unique and very stringent constraints on B&B core neutronics have been reviewed, showing the consequences of the high fuel volume fraction and the need to avoid moderating diluents. The materials choices (both fuel and coolant) and thermal-hydraulic consequences lead to selection of a conventional HTGR-type Rankine power conversion system. This narrows the principal focus of this evaluation to tasks associated with core design and performance analysis. It was also shown that the relatively low core power density of modern GFRs (compared to first generation GCFRs and to LMRs) – which we felt constrained to adopt for the B&B version – does not lead to economically uncompetitive B&B fuel cycle costs.

2.6 Chapter References

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- (2-2) J. M. Ravets and J. M. Tobin, “The Very High Temperature Reactor for Process Heat,” *Trans. Am. Nucl. Soc.*, Vol. 22, Nov. 1975, p. 599

Chapter 3

Core Design and Reactor Physics of a B&B GFR

3.1 Introduction

A Breed and Burn Gas-cooled Fast Reactor (B&B GFR) was designed to have a once-through fuel cycle. B&B refers to the equilibrium operation of a reactor where low enriched uranium (5 a/o) reload assemblies are required, yet in-situ plutonium breeding sustains discharge burnup on the order of 150 MWD/kg_{HM} without the need for spent fuel reprocessing. Two reactor concepts were developed and evaluated against the Generation IV International Forum (GIF) goals of improved sustainability, economics, safety, and proliferation resistance. The first concept, or “demonstration” concept, is based on uranium nitride pin-type fuel in a tight pitch hexagonal lattice (~50 v/o fuel). The nitrogen is enriched to greater than 99% in the isotope ¹⁵N. The 2900 MW_{th} demonstration core uses vibrationally compacted (VIPAC) U¹⁵N fuel clad in MA 956 oxide dispersion strengthened stainless steel (ODS). The core is 200 cm in active height, and 350 cm in equivalent diameter. Coupled thermal hydraulic and neutronic analysis confirmed the feasibility of the B&B demonstration core, however, there were strong economic motivations to employ uranium carbide fuel. In the second, or “advanced,” concept an innovative tube-in-duct vented fuel assembly was designed to enable B&B operation with UC fuel. The fuel volume fraction was increased to ~60 v/o, the discharge burnup was slightly increased to 165 MWD/kg_{HM}, and the core diameter was increased to 400 cm. Both concepts build on extensive experience from previous gas cooled reactor designs and employ steam Rankine power conversion systems. Modeling of the B&B GFR confirmed its semi-passive safe operation under a host of postulated accident scenarios, as well as the economic advantages to B&B mode operation.

3.2 Background

Breed and Burn refers to a once-through-then-out fuel cycle where low enriched uranium fuel is irradiated until reaching a burnup of approximately 150 MWD/kg_{HM} without the need for any spent fuel reprocessing, recycle, or reuse. B&B is not a new concept. B&B was first mentioned in 1958 by Feinberg [3-1], studied in 1970 by Fischer, et al. [3-2] with collaborators at MIT [3-3], [3-4], and a lead cooled alloy version has been recently explored by Toshinsky, et al. in the 1990s [3-5]. Though research has been conducted that indicates that B&B may be possible with natural uranium, such proposed fuel cycles are complicated and require extremely long residency times and complex fuel shuffling sequences. Ryu and Sekimoto have recently described a pebble bed approach with online refueling, yet still requiring extra-long fuel residency times [3-6].

The current NERI funded research has focused on the design of a Gas-cooled Fast Reactor (GFR) operating in a Breed and Burn (B&B) fuel cycle mode. The B&B fuel

cycle meets the GenIV goals of sustainability, economics, and proliferation resistance by increasing fuel burnup without the need for spent fuel reprocessing, recycle, or reuse of any kind.

The neutronic requirements for B&B are strict and require an ultra-hard neutron spectrum. Therefore, the GFR is ideally suited for this fuel cycle. In the present work the B&B GFR concept evolved into two practical reactor designs, both of which build on extensive previous gas-cooled reactor design experience.

The first version is the “demonstration” concept using highly neutronically reactive $U^{15}N$ fuel in a hexagonal pin fuel array that is nearly 50 v/o fuel. The core is helium cooled, with an outlet temperature of 570 °C. The helium primary circuit is coupled to a steam Rankine power conversion system essentially identical to that for the British Advanced Gas-cooled Reactors. One advantage of the low coolant temperature compared to other GenIV GFR concepts is that it allows for the use of oxide dispersion strengthened stainless steels (ODS) in core. The fuel is manufactured using advanced vibration compaction techniques, clad in ODS, and vented in order to achieve the high burnup goal.

The second version, the “advanced” concept, builds on the experience of the demonstration concept to develop a B&B GFR without the need for expensive $U^{15}N$ fuel. In order to substitute the nitride fuel with carbide, significantly higher heavy metal loadings are required (60 v/o fuel for UC versus 50 v/o fuel for $U^{15}N$) which are not practically achievable with a conventional pin fuel array. Therefore, an innovative tube-in-duct assembly design was proposed to achieve B&B operation with the less neutronically reactive carbide fuel. The advanced core offers significantly reduced natural uranium requirements and lower equilibrium fuel cycle costs (5 mills/kWhre) compared with conventional light water reactors (7 mills/kWhre), as the burnup is tripled for the same reload enrichment.

The B&B GFR designs, though requiring active decay heat removal, are semi-self-regulating from a reactivity feedback standpoint and are designed to withstand all plausible accident scenarios, including loss of flow, loss of heat sink, and transient overpower, all without scram. Reactor pressure vessel blowdown (LOCA) was investigated and while the B&B GFR has a low positive coolant void reactivity (less than 1\$), the added reactivity during blowdown is compensated through other strong negative reactivity feedback mechanisms, thereby allowing for the safe operation of the B&B GFR.

3.3 Theory and Methods

In the B&B GFR fresh reload assemblies are enriched to 5% ^{235}U in U. In a fast reactor, this type of fuel is subcritical, and during normal operation will require reactivity sharing with other assemblies in order to produce power. However, during operation in a very hard neutron spectrum, a significant quantity of the ^{238}U transmutes to ^{239}Pu . The

plutonium in partially burnt assemblies is the source of excess reactivity to share with fresh assemblies. A wide range of fuel forms was examined to determine the fuel candidates with the highest B&B potential.

Figure 3.1 Comparison of Fuels' Reactivity History

MCODE, a MCNP-ORIGEN coupling program, was used to calculate the reactivity as a function of burnup for semi-infinite pin cell models. These models were used to compare a wide range of different fuel forms, many of which were proposed as part of the ANL Reduced Enrichment for Research and Test Reactors (RERTR) program [3-7]. The RERTR fuels are high heavy metal density fuels, and high heavy metal density is an indicator of good neutronic performance for a B&B reactor fuel. Figure 3.1 shows these histories for many ceramic fuels. The histories are roughly parallel indicating that most fuels will experience similar breeding gains and that the peak reactivity is the best metric for good neutronic performance. The buildup and then slow fall in reactivity during irradiation arises as fissile plutonium rapidly builds up to an asymptotic concentration and the fission product inventory is continuously increasing. U¹⁵N and UC were identified as the leading candidates for the B&B GFR.

By comparing the fuels at the beginning of irradiation it was found, as shown in Figure 3.2, that the reactivity is directly related to the heavy metal density of the fuel, and hence, that the reactivity of an assembly is inversely proportional to the fuel loading.

Figure 3.2 Relationship between Reactivity and Heavy Metal Density

Figure 3.3 shows the effects of fuel enrichment on the reactivity history using a UC test case. Since the enrichment at the BOC is only ~5% and the fuel reaches atomic burnup on the order of ~15%, the long-term, high burnup trends in reactivity will be dictated purely by the ^{238}U content and the spectrum. Also, while changing the enrichment from 5% to 10% may represent a 100% change in the ^{235}U content, it is only ~5% change in the ^{238}U content; in essence, the long-term trend in fuel reactivity will be nearly completely independent of the initial enrichment. This motivated the investigation of natural uranium fueled B&B concepts in the past.

Figure 3.3 Impact of Enrichment on High Burnup Reactivity

However, because the reactivity increases during irradiation, the reactivity-limited burnup is the minimum burnup (as opposed to maximum in conventional reactors) that the fuel must attain to sustain criticality once it is discharged and replaced with subcritical fresh fuel. The integral of the reactivity is what determines the limitations on discharge burnup and fuel residency, and as such, the current work focuses on using enriched (5 a/o) uranium fuel in order to reduce the reactivity limited burnup to the design goal of ~ 150 MWD/kg_{HM} burnup at discharge.

Based on the fuel comparison, two design concepts were pursued. The demonstration concept focuses on the U¹⁵N fuel while the advanced concept focuses on the less expensive, less neutronically reactive UC fuel.

3.4 Demonstration Core: Fuel and Core Design

There are four unique reactor physics aspects of the B&B GFR that dictate many features of the core design. First, the fresh assemblies are subcritical, and therefore, these assemblies depend on sharing reactivity with partially burnt assemblies. Second, because of clad endurance limits, the assemblies are discharged near the burnup where they attain peak reactivity, therefore, the other partially burnt assemblies, over any given cycle, must breed in sufficient excess reactivity to overcome the difference between the fresh and

discharged assemblies. The second aspect means that the reactivity swing (increase) over any given cycle will be large compared with conventional reactors. Third, the reactivity limited burnup is the minimum burnup that fuel must reach to sustain core criticality, and the fourth is that the reactivity is proportional to the inverse of the heavy metal density, and hence the heavy metal loading in the fuel. Therefore, a B&B GFR that enables reactivity-limited burnup of a reasonable level (such as the goal of 150 MWD/kg_{HM}) must utilize high heavy metal density fuels.

Vibration-compaction (VIPAC) fuel manufacturing was identified as a good approach; since the fuel must withstand high burnup, high fluence neutron irradiation. MA 956 ODS was selected as the cladding and structural material. ODS has an excellent blend of superior mechanical performance (based on its high melting temperature and creep resistance) and superior neutronic performance (given the low parasitic absorption cross section and very low helium production rates). Operating limits for the cladding peak temperatures under steady state and accident conditions (namely 750 °C and 1300 °C, respectively) were selected for the ODS based on its properties [3-8] and experience from the GA GCFR design process during the 1970s [3-9].

A tight-pitch hexagonal fuel pin array was designed to meet the neutronic requirements for the demonstration core, while meeting the steady state thermal hydraulic limits. With a primary system pressure of 10 MPa and a core thermal power of 2900 MW_{th}, the peak cladding temperature is 640 °C in steady state, and the helium outlet velocity is 120 m/sec. Figure 3.4 shows a schematic of the demonstration core fuel assembly.

Figure 3.4 Demonstration Core Fuel Assembly

The fuel assemblies are designed so that each has seven control rod locations. These rods form a cluster of control fingers that constitute the reactivity control devices for the core. There are control insertions at both the top and bottom of the core to

increase the system safety and reliability. The core design is summarized in Table 3.1. The core layout and batch shuffling sequence are shown in Figure 3.5.

Table 3.1 Demonstration Core Geometry

Fuel Form	U ¹⁵ N	
Fuel Pins per Assembly	162	
Pin Outer Diameter	0.82	cm
Clad Thickness	0.04	cm
Pin Pitch	1.00	cm
Assembly Flat-to-Flat	13.28	cm
Active Core Height	200	cm
Equivalent Core Diameter	350	cm
Fuel Volume Fraction	45.6	v/o
Structural Volume Fraction	12.8	v/o
Coolant Volume Fraction	41.6	v/o

The core model is 1/12th symmetric. The multi-batch reload sequence and control strategy was designed to optimize several competing goals. First, the scheme must promote reactivity sharing between partially burnt and fresh fuel assemblies in order to breed plutonium in low reactivity assemblies. Second, partially burnt assemblies are progressively moved towards the center of the core in order to sustain core criticality over multiple (six) cycles. Third, the power shape should remain fairly constant during irradiation to enable the use of fixed flow orificing to improve power conversion efficiency. Fourth, the radial power shape should be as flat as possible to maximize core power density. As a consequence of the current multi-batch reload scheme, the power history for the average assembly varies significantly between fuel reload and subsequent shuffling but is fairly constant during irradiation.

Figure 3.5 Demonstration Core Layout

In the current approach, the uncontrolled reactivity history for the core over multiple cycles is shown in Figure 3.6. The startup core is fueled with several batches

ranging in enrichment from 5 to 10 a/o ^{235}U in U. The startup core loading and refueling sequence are summarized in Table 3.2.

Table 3.2 Startup Core and Fuel Shuffle Sequence

Cycle Loading (a/o ^{235}U in U)			
Batch	BOC1	BOC2	BOCn
1	5	5	5
2	6	from 1	from 1
3	8	from 2	from 2
4	10	from 3	from 3
5	10	from 4	from 4
6	10	from 5	from 5

For this startup core configuration, and multi-batch reload sequence, the uncontrolled reactivity is plotted in Figure 3.6. The key features of the plot are that by the 8th cycle the core has reached its equilibrium cycle and that at the beginning of the 3rd cycle the core is slightly subcritical (with an eigenvalue of 0.997(5)). While increasing the enrichment in the 2nd batch would certainly increase the reactivity at this stage in life, the current startup configuration is meant to rapidly advance the core model to its equilibrium cycle. Furthermore, this small discrepancy is within the margin of uncertainty arising from conservative model simplicity.

Figure 3.6 Demonstration Core Uncontrolled Reactivity History

3.5 Safety Aspects of Demonstration Core

The Quasi-Static method, first developed for the design of the integral fast reactor (IFR) was used to evaluate the ability of the demonstration core to self-regulate during accident scenarios [3-10]. Specifically, loss of heat sink, loss of flow, and unexpected transient overpower (all without scram) were used to establish an envelope of accident conditions. Additionally, loss of primary pressure (LOCA) was considered. The Quasi-Static method uses the reactivity balance shown in Equation (3.1) to determine steady state core conditions after a perturbation.

$$0 = \Delta\rho_{ext} + \delta T_{in} C + \left(\frac{P}{F} - 1 \right) B + (P - 1) A \quad (3.1)$$

Where $\Delta\rho_{ext}$ is the change in reactivity from external sources (i.e. control rods),
P is the normalized power (at nominal power P is unity),
F is the normalized flow (at nominal flow rate F is unity), and
 δT_{in} is the change from nominal coolant inlet temperature.

The lumped reactivity feedback parameters are calculated based on individual reactivity feedback mechanisms as shown in Equation (3.2).

$$\begin{aligned}
 A &= (\alpha_D + \alpha_e) \Delta T_f \\
 B &= (\alpha_D + \alpha_e + \alpha_{Co} + 2\alpha_{RD} + 2\alpha_R) \frac{\Delta T_c}{2} \\
 C &= \alpha_D + \alpha_e + \alpha_{Co} + \alpha_R
 \end{aligned}
 \tag{3.2}$$

Where ΔT_f is the difference in temperature between the fuel and coolant [K],
 ΔT_c is the temperature rise across the core at steady state conditions [K],
 α_D is the Doppler coefficient [ϕ/K],
 α_e is the fuel thermal expansion reactivity coefficient [ϕ/K],
 α_{Co} is the coolant temperature coefficient [ϕ/K],
 α_{RD} is the control rod drive line coefficient [ϕ/K], and
 α_R is the radial thermal expansion coefficient (core flowering) [ϕ/K].

The steady state core conditions following the aforementioned accidents are related to the ratios of the lumped feedback parameters. Based on the limiting fuel temperatures and temperature data from a series of steady state thermal hydraulic evaluations, specific coefficient ratios were calculated and compared with their appropriate limits at various points in life. The results are tabulated in Table 3.3 and the B&B GFR is compared to two metal cooled reactors (the Actinide Burner Reactor and the Integral Fast Reactor).

Table 3.3 Quasi-Static Analysis of the Demonstration Core

		Demo. B&B GFR			ABR[3-11]		IFR[3-10]
		BOL	BOEC	EOEC	BOL	EOL	AVG
A	cents	-19	-25	-24	-10	-8	-31
B	cents	-22	-23	-28	-22	-19	-35
C	cents/K	-0.10	-0.11	-0.12	-0.27	-0.23	-0.25
A/B		0.84	1.09	0.85	0.47	0.41	0.88
C ΔT_c /B		1.70	1.90	1.70	1.14	1.11	1.10
$\Delta\rho_{TOP}$ /B		0.53	0.28	0.64	0.69	0	0
A/B limits		x < 1.14			< 1.25		< 1
C ΔT_c /B limits		x > 1			1 < x < 1.8		1 < x < 2
$\Delta\rho_{TOP}$ /B limits		x < 1.14			< 1.25		< 1
β		0.00882	0.00623	0.00544	0.0024	0.0023	0.0035
α_D	1e-3 c/K	-64	-91	-94	-55	-85	-120
α_e	1e-3 c/K	-17	-18	-9	-60	0	-90
α_{Co}	1e-3 c/K	1	1	2	30	40	180
α_{RD}	1e-3 c/K	0	0	0	-10	0	0
α_R	1e-3 c/K	-17	-6	-22	-180	-180	-220

While meeting the self-regulating criteria as applied here, the demonstration core requires active decay heat removal, and is therefore considered “semi-passive.” Additionally, it is worth noting that the design of the B&B GFR is less constrained than metal cooled reactors in that the reactor can be designed with strong negative reactivity feedback parameters without the risk of coolant freezing. The control rod driveline thermal expansion is always a negative reactivity feedback mechanism because the reactor pressure vessel is insulated. Hence, it was neglected in the current analyses to add conservatism.

Coupled thermal hydraulic and neutronic calculations indicate that the coolant void reactivity worth is a maximum at the EOEC, and is approximately 1\$. Pressure vessel blowdown, however, will take anywhere from one to several minutes; thereby allowing time for other negative reactivity feedback to compensate. However, the transient nature of rapid blowdown will have to be investigated further to fully assure the B&B GFR safety.

3.6 Economics

While the demonstration core offers much higher burnup for a similar level of enrichment compared to a conventional LWR, the uncertain, but likely high price for highly enriched nitrogen (HEN) motivated the evaluation of several fuel cycle options. Many of these options included possible end use options for the spent fuel. The base case is the once-through-then-out fuel cycle (OTTO), but AIROX treatment of the B&B GFR spent fuel, and subsequent reuse in a LWR was also considered. Additionally, recovery of the HEN in combination with either LWR reuse or direct GFR reuse was evaluated. The purpose of these calculations was to determine the breakeven price of HEN where the B&B GFR equilibrium fuel cycle costs would match those for today’s LWRs.

Table 3.4 Demonstration Core Cost of Generation and Alternative Fuel Cycles

Input Variables	Units	LWR Reference Value	Demo. B&B GFR OTTO	Demo. B&B GFR AIROX	AIROX with HEN Recovery	AIROX, HEN Recovery and Split
Discharge Burnup	MWD/kg _{HM}	50	150	150	150	150
Net Efficiency	%	33	40	40	40	40
Cycle Length	EFPY	1.5	2.5	2.5	2.5	2.5
Number of Batches		2.68	6	6	6	6
Capacity Factor		0.9	0.9	0.9	0.9	0.9
Enrichment	w/o	4.5	5	5	5	5
Fuel Cycle Cost	mills/kWh _{re}	6.72	6.72	6.72	6.72	6.72
Required Price of HEN	USD/gHEN	N/A	30.26	31.55	35.83	38.22

Current price estimates for HEN range between 100 USD/g to 1000 USD/g [3-12,3-13]. However, these calculations show that the demonstration core would only be

economical if the price of HEN were between 30 and 40 USD/g. The economic analysis clearly motivates examination of an advanced GFR concept that allows B&B mode operation without the requirement for HEN. Therefore, the UC fueled advanced concept was developed.

3.7 Advanced Core Fuel and Core Design

Since the reactivity is related to the heavy metal density, to substitute $U^{15}N$ with UC in the B&B GFR core, some design changes are necessary to compensate for the lower reactivity. The fuel assembly must have a much higher heavy metal loading to enable B&B operation. Additionally, the burnup is slightly increased (10%) to 165 MWD/kg_{HM} and the core size is somewhat increased (350 cm to 400 cm equivalent diameter). The combination of these design changes and the innovative tube-in-duct concept allow for B&B mode operation with UC fuel.

The tube-in-duct concept effectively inverts a pin-type fuel assembly. In the tube-in-duct (TID) assembly coolant flows through channels that are surrounded by fuel, instead of coolant flowing in between separate fuel pins. The TID assembly schematic is shown in Figure 3.7.

Vertical Cross Sectional View

Horizontal Cross Sectional View

Figure 3.7 Tube-in-Duct Fuel Assembly Schematic

Using the TID assembly, coupled thermal hydraulic and neutronic calculations show that volume fractions of nearly 60 v/o and a primary system pressure of 11.5 MPa or greater are required to sustain B&B operation. Table 3.5 summarizes the geometry for the advanced core concept.

Table 3.5 Advanced Core Geometry

Fuel Form	UC	
Channels per Assembly	91	
Channel Diameter	0.80	cm
Clad Thickness	0.04	cm
Channel Pitch	1.45	cm
Assembly Flat-to-Flat	14.8	cm
Active Core Height	200	cm
Equivalent Core Diameter	396	cm
Fuel Volume Fraction	54.5	v/o
Structural Volume Fraction	18.0	v/o
Coolant Volume Fraction	27.5	v/o

The layout of the advanced core is essentially identical to that of the demonstration core except for one distinct difference. Six assemblies from each batch are removed to make room for control rod assembly locations. The reference design for the advanced core makes use of 74 control rod assemblies, 37 inserted from above the core and 37 from below (see Figure 3.8). By removing these assemblies, the fuel volume fraction is approximately 55% instead of the 45% in the demonstration core.

Figure 3.8 Advanced Core Layout

The reactivity history for the uncontrolled core is shown in Figure 3.9. The results essentially mirror those for the demonstration core, thus verifying that B&B mode operation can be achieved with a UC fueled GFR. However, the combination of a lower thermal power (2400 MW_{th}), higher heavy metal loading, and slightly higher burnup results in a longer cycle length. The cycle length of the demonstration core is 2.5 EFPY and 4.7 EFPY for the advanced core.

Figure 3.9 Advanced Core Uncontrolled Reactivity History

3.8 Safety Aspects of Advanced Core

The Quasi-Static method was applied to the advanced core and it was found that control rod ejection at the EOEC posed a risk to the safe operation of the advanced core. The design solution is to adopt a distributed interassembly control scheme. While the demonstration core control scheme resembled that of a PWR, the advanced B&B GFR core adopted a BWR-like interassembly control approach. The control rod assembly (CRA) approach developed for the reference design concentrates the poison in a few high worth assemblies.

Table 3.6 Quasi-Static Analysis of the Advanced Core

		Adv. B&B GFR		
		BOL	BOEC	EOEC
A	cents	-35	-47	-40
B	cents	-36	-52	-50
C	cents/K	-0.17	-0.24	-0.22
A/B		0.99	0.90	0.79
CΔTc/B		1.82	1.75	1.66
Δρ _{TOP} /B		0.40	0.17	4.34
A/B limits		x < 1		
CΔTc/B limits		x > 1		
Δρ _{TOP} /B limits		x < 1		
β		0.00867	0.00638	0.00503
α _D	1e-3 c/K	-110	-116	-98
α _e	1e-3 c/K	-42	-53	-44
α _{Co}	1e-3 c/K	0.5	0.8	0.8
α _{RD}	1e-3 c/K	0	0	0
α _R	1e-3 c/K	-16	-34	-45

The cladding temperature is slightly higher (715 °C), therefore the limits for loss of heat sink (LOHS) and transient overpower (TOP) are reduced from 1.14 in the demonstration core to 1 for the advanced core. Despite the large reactivity worth associated with control rod ejection at the EOEC (which could be corrected with a control rod design change) the advanced core has consistently stronger negative reactivity feedback than the demonstration core, mainly due to a slightly softer neutron spectrum and the design of the assembly itself. The advanced core also has a lower void reactivity worth (~0.6\$ versus ~0.9\$ in the demonstration core). See Table 3.6.

3.9 Economics of the Advanced Core

The advanced B&B GFR once-through-then-out (OTTO) fuel cycle was compared to a conventional LWR, and the results are summarized in Table 3.7. The advanced core, in equilibrium, offers significant economic benefits compared to conventional LWR. Furthermore, the combination of higher burnup and higher power conversion efficiency reduce the need for uranium ore by a factor of 3.5, thus improving the sustainability of the OTTO fuel cycle relative to a LWR.

Table 3.7 Advanced Core Cost of Generation

Input Variables	Units	LWR Reference Value	Advanced B&B GFR OTTO
Discharge Burnup	MWD/kg _{HM}	50	165
Net Efficiency	%	33	40
Cycle Length	EFY	1.5	4.7
Number of Batches		2.68	6
Capacity Factor		0.9	0.9
Enrichment	w/o	4.5	5
Fuel Cycle Cost	mills/kWhre	6.72	5.11

3.10 Summary and Conclusions for Mainstream Effort

The demonstration and advanced concepts illustrate the feasibility of designing a practical B&B GFR. The advanced concept builds on the experience developed in the demonstration core design and illustrates the benefits of B&B mode operation. Table 3.8 briefly summarizes some of the unique attributes of the B&B GFR concept described in the current work as well as key findings related to core design and fuel management.

As a potential candidate core design for GEN IV GFRs, the advanced B&B GFR meets the GIF goal for improved sustainability by reducing the uranium ore requirement by a factor of 3.5 relative to today's LWRs. By achieving much higher fuel burnup for approximately the same level of enrichment, the B&B GFR also meets the goal for enhanced fuel cycle economics.

The B&B GFR cores described in the present work were successfully designed to have significant reactivity self-compensation, thus demonstrating that it is possible to design a B&B GFR system that will be safe and reliable. Lastly, the nature of the once-through-then-out fuel cycle makes the B&B GFR more proliferation resistant than conventional breeder reactors that typically require spent fuel reprocessing and separation of the transuranics.

In conclusion, the demonstration and advanced concepts prove that a B&B GFR system can be designed by building on an already extensive base of gas-cooled reactor experience and still achieve all of the goals set forth by the GIF.

Table 3.8 Summary of B&B GFR Core Attributes

Attribute	Manifestation	Ramification
Large , low leakage core needed	2400+ MWth rating results	Not suitable concept for small reactors
High heavy metal loading is required	High heavy metal density fuels (UC or U15N) are used	The fuels are not chemically compatible with CO ₂ , motivating the use of chemically inert He
	High fuel volume fraction and low coolant volume fraction	Only moderate power densities can be achieved, and the fuel residency time is long
Fresh and partially burnt assemblies must share reactivity	A 6 batch fuel management scheme is adopted. At startup the average enrichment is 8% and reload assemblies are 5% enriched	Burnup of ~15 a/o (fuel endurance, not reactivity, limited)
		With LWR recycle, a burnup of ~19 a/o
		With direct GFR reuse (split option), a burnup of ~24 a/o
Large core temperature rise needed to reduce core pressure drop	Best coupled to a Rankine power conversion system	The primary circulator power is modest, enabling efficient use of a secondary PCS
		Water ingress from the PCS is a reactivity concern near the BOL
Long term breeding	Fuel achieves peak reactivity only shortly before discharge	Large core reactivity swing (increase) over a cycle
		Incentives motivate fuel refabrication and reuse in the GFR core
	At long irradiation, the axial fissile content distribution is uniform	Enrichment zoning cannot reduce axial power peaking over entire fuel residency
	High plutonium content at discharge, approximately 80% of which is ²³⁹ Pu	Once-through spent fuel can be loaded into LWRs
Very high fertile fission fraction due to hard spectrum	β is large because of ²³⁸ U delayed fission neutrons	Void reactivity is limited to \$1
	Direct fertile fission is necessary given the strict neutron economy	Spectrum hardening increases achievable reactivity, allowing poison control
Mechanically robust, and neutronically transparent cladding is preferred	MA 956 ODS Stainless Steel is the preferred cladding material	ODS may adequately reduce creep at temperatures ~750°C
		ODS has no nickel, and therefore lower helium production than SS316
Fission products do not saturate	High concentration of ¹⁴⁹ Sm at discharge	Multipass spent fuel cannot be recycled into LWRs
		Core flooding at EOC results in a negative reactivity insertion

3.11 Future Possibilities

The major focus in the present work was on metal clad/coolant tube core designs. Hence core exit temperatures are limited to on the order of 650°C. This precludes use for hydrogen production by thermochemical processes, which require temperatures on the order of 850°C. This raises the question as to whether a ceramic B&B core is feasible. Accordingly this topic was a major focus of INL work in the final contract year (Ref. 3-14), and was also examined by Yarsky at MIT (Ref. 3-15) in the final stages of his thesis. INL investigated a UC-SiC cercer fueled core, while MIT replaced its steel tubing by SiC tubing. The key to neutronic performance is the volume ratio of SiC to UC, which was as follows:

	<u>SiC/UC</u>	
INL	2/1	
MIT	1/3	0.55 mm thickness: same as ODS)
	2/3	(double thickness)
INERI	1/1	ANL/CEA led design

Note that because heterogeneous self shielding is not particularly significant in hard spectrum GFRs, virtually the same neutronic results are to be expected for SiC as cladding or as the matrix in a cercer dispersion.

Table 3.9 gives the specifications for the 7% U-235 enriched reactor evaluated by INL (among others: see Ref. 3-14), and Fig. 3.10 shows its k vs burnup history. An overoptimistically generous linear extrapolation gives $k = 1$ at 80 MWd/kg: much longer than Yarsky's actual (and already excessive) 55 MWd/kg for his 5 w/o enriched double-thickness cladding case (3-15), and far too long to permit old fuel at $k > 1$ to support fresh reload fuel in a $\bar{k} \approx 1.0$ B&B core over the fuel's remaining burnup-limited in-core residence time: currently specified as 100 MWd/kg for SiC•UC cercer fuel in the ANL/CEA led GFR INERI program.

Table 3.9 7% U-235 Fueled Gas-Cooled Fast Reactor Specifications

Overall Core Specifications	
Power Density	100 W/cm ³
Height	1 m
Radius	1.24 m
Volume	4.83 m ³
Total Power	483 MW
Coolant Specifications	
Coolant	He
System Pressure	7 MPa
Coolant Volume Fraction	40%
Fuel Block Specifications	
Shape	Hexagonal prism
Height	10 cm
Dimension across flats	10 cm
Quantity	990
Fuel + Matrix Volume Fraction	60%
Fuel	UC
Matrix Material	SiC
Fuel to Matrix Ratio	50%
Fuel Constituents	
U-234	0.01%
U-235	6.92%
U-238	93.08%
Reflector Specifications	
Reflector Material	Zr ₃ Si ₂
Axial Thickness	1 m
Radial Thickness	1 m

Figure 3.10 Change in K_{eff} vs. Burn-up in the 7% U-235 Fueled Model

Figures 3.11 and 3.12 show Yarsky's reactivity ($\rho = (k-1)/k$) histories for SiC cladding. He concludes that only if SiC has the same thickness as its ODS alternative should one consider further evaluation of such a design.

Fig. 3.11 ODS and SiC Cladding Comparison at Same Thickness

Fig. 3.12 SiC at Double Thickness Reactivity History

To make a long story short, use of SiC for B&B applications in a GFR appears to be neutronically feasible only if a TID assembly design is employed having SiC coolant tube thickness on the order of 0.55 mm. Data is at present not available to confirm mechanical feasibility. However, since the TID assembly is vented, this is a low pressure stress application, and neither can this approach be ruled out.

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Chapter 4

Core Thermal Hydraulics and Materials

4.1 Introduction

The integrated physics and thermal design of two B&B cores has been described in Chapter 2 of this report, and in considerably more detail in Ref(4-1). Two aspects are singled out here for further attention: The necessity for and applicability of assembly flow orificing; and the ability of the core to endure thermal transients. Our goal, again, is to show that B&B cores fit within the performance envelope previously coped with in past fast reactor designs – both gas and liquid metal cooled.

4.2 Orificing

The B&B core is configured in six annular radial zones through which the fuel assemblies move as a group in each of six post-refueling/shuffling intervals. Figure 4.1 shows the group average assembly power history as a function of burnup, and Table 4.1 the peak-to-average power ratios.

Table 4.1 Impact of Control Clusters on Radial Power Peaking

Peaking Factor by Region	BOEC	EOEC (uncontrolled)	EOEC (clusters inserted)
1	0.25	0.20	0.28
2	0.47	0.40	0.50
3	1.21	1.45	1.38
4	1.74	2.05	1.77
5	1.32	1.13	1.19
6	1.01	0.77	0.88

Figure 4.1 Power History Comparison

The extremely large step changes in power are evident. The important points to note, however, are that:

- (a) for judicious control rod programming, the power during each cycle is relatively constant, which facilitates region-wise orificing
- (b) the largest power difference is actually lower than encountered in the first generation breeder reactors which employed depleted uranium blankets

Both in-assembly and in-grid-plate orificing was common in first generation fast reactors. For example, the GA-GCFR employed an adjustable orifice in each assembly, while the CRBR was to have four fixed zones in its blanket. (4-1) (4-2) For the B&B cores, fixed orificing in (five of the) six annular zones appears to be the most appropriate tactic in view of the small power change as cycle burnup progresses.

The use of orificing to, for example, equalize assembly coolant exit temperatures provides an obvious thermodynamic benefit: it raises mixed mean temperature to that of the hot channel, which, via the IHX, raises turbine inlet temperature and thereby cycle efficiency. An equally important benefit, which is less obvious, is the reduction in coolant circulator power consumption. For a GFR this is a particularly important

consideration (see also Chapter 5). This effect can be demonstrated by the simple model which follows.

In the absence of orificing the mass flow rate is the same in each assembly, hence circulator work is approximately

$$W_{NO} = N \cdot \frac{\hat{m}}{\rho} \cdot \Delta P \quad (4-1)$$

where N is the number of assemblies, $\hat{m}$ and ΔP the mass flow rate and pressure drop of the peak assembly.

With orificing pressure drop remains the same but off-peak flow resistance increases to reduce mass flow such that coolant temperature rise is the same as for the peak channel. One has:

$$W_{OR} = \sum_L^N \frac{m_i}{\rho} \Delta P \quad (4-2)$$

but

$$\frac{m_i}{\hat{m}} = \frac{q_i}{\hat{q}} \quad (4-3)$$

where q is assembly power.

Hence

$$\frac{W_{OR}}{W_{NO}} = \frac{1}{N} \sum_L^N \left(\frac{q_i}{\hat{q}} \right) = \frac{1}{f_r} \quad (4-4)$$

where f_r is the radial peak to average assembly power ratio.

Applying Eq(4-4) to the BOEC data of Table 4.1 gives $W_{OR}/W_{NO} = 0.57$, an appreciable savings.

It should be noted that other orificing strategies are possible, such as matching peak clad temperatures rather than core exit coolant temperatures. This alternative is worth further investigation because clad temperature is the dominant limit under steady state, transient and post-accident conditions in a GFR.

It is also important to recognize that the coolant flow distribution will differ under conditions far from the full power steady state, such as post-LOCA, during refueling shutdown, and especially under natural convection.

4.3 Thermal Transient Toleration

In advance of actual transient simulation using a code such as ATHENA, one can gain some valuable insights into how well a B & B core will fare.

4.3.1 Adiabatic Heatup Rate

Modern GFRs in general, and the B & B core version in particular, benefit from low power density, which was adopted to help foster passive decay heat removal. As shown in Table 4.2 this results in a low initial heatup rate in the limit of instantaneous loss of heat removal capability. The adiabatic heatup rate of the B & B GFR core is a factor of four slower than was the case of the GA-GCFR in 1980. This will considerably mitigate the severity of virtually all transient and accident scenarios which could threaten core integrity.

Table 4.2 Comparison of Former and Current GFR Core Adiabatic Heatup Rates

<u>Parameter</u>	<u>Design</u>	
	<u>GA-GCFR</u>	<u>B & B GFR</u>
Power Density kW/l	235	100
Specific Power kW/kgHM	95	16.3
kW/kg fuel	83.8	15.5
Fuel Heat Capacity kJ/kg°C	0.30 (UO ₂)	0.23 (UC)
Adiabatic Heatup Rate, °C/sec	279	67

4.3.2 Stored Energy

The tube-in-duct or inverted fuel cell has another advantage over the conventional pin-type configuration: a significantly lower average operating temperature – hence proportionally lower stored energy at the outset of a transient.

It is fairly straightforward to calculate the ratio of average and peak fuel temperatures for TID and PIN cells having equal volume fraction fuel, v_f , fuel power density and hydraulic diameter (4.3) (4.4). One finds for the ratio of average, ΔT , and peak, ΔT , fuel temperatures:

$$\frac{\overline{\Delta T}_{TID}}{\overline{\Delta T}_{PIN}} = \left(\frac{1-v_f}{v_f^2} \right) \left[-v_f - 2 - \frac{2}{v_f} \ln(1-v_f) \right] \quad (4-4)$$

$$\frac{\Delta^{\wedge} T_{TID}}{\Delta^{\wedge} T_{PIN}} = \left(\frac{1-v_f}{v_f^2} \right) \left[-\ln(1-v_f) - v_f \right] \quad (4-5)$$

Thus one has in the range of interest:

v_f	$\overline{\Delta T}_{TID} / \overline{\Delta T}_{PIN}$	$\Delta^{\wedge} T_{TID} / \Delta^{\wedge} T_{PIN}$
0.4	0.578	0.416
0.5	0.545	0.386
0.6	0.505	0.351
0.7	0.453	0.309

This shows that the TID assembly will have about half the stored energy at the outset of a transient for the same power density. This provides a significant margin before temperatures exceed limits in the subsequent transient.

Furthermore, at steady state the peak fuel temperature, relative to the temperature at the fuel's cooled surface, is about one-third that in the pincell. This helps maintain fuel structure and fission product retention, which should facilitate achieving high burnup.

Note that ratios of the above type would differ had different constraints been applied. In particular, coolant channel hydraulic diameter was kept the same, to preserve pressure drop and circulator power expenditure, as well as the heat transfer film ΔT_f of the gas. We also preserved in-fuel power density, hence specific power, kW/kg, which equalizes fuel cycle economics.

4.4 RELAP Transient Analyses

It is clear that one can design B&B cores that have acceptable steady-state full power thermal hydraulic performance, and, if fully active shutdown cooling using blowers is employed, any and all transient or accident scenarios can also be accommodated given sufficient circular power.

Both core and balance of plant designs evolved over the course of this project. It was only late in the game that a tube-in-duct (analogous to a hex-block) assembly was made the preferred reference design, and even later before reversion to use of a steam Rankine cycle PCS specified. This insured both technical feasibility and economic

mediocrity of a B&B GFR. It was only in the last few months that a future design using SiC cladding instead of metal, and a direct helium Brayton PCS was identified as a potentially more viable overall package. This would also make future B&B work more consistent and synergistic with the continuing mainstream INERI GFR effort led by ANL and CEA.

Accordingly INL set up and exercised a RELAP5-3D (formerly ATHENA) model of a double-ended guillotine pipe break for an all ceramic core housed in the reactor vessel layout shown in Fig. 4.2, having the specifications summarized in Table 4.3. (Ref 4-14)

Fig. 4.2 Prototypical Reactor Vessel Configuration for the Gas Fast Reactor

Table 4.3 Design Features for the Gas Fast Reactor Concept

Reactor Design Parameter	Conceptual Data
Power plant	600 MW _{th}
Net efficiency	48% (Helium, direct cycle)
Coolant pressure	9 MPa
Outlet coolant temperature	850 °C (Helium, direct cycle)
Inlet coolant temperature	490 °C (Helium, direct cycle)
Nominal flow and velocity	330 kg/s and 40 m/s
Core volume	10.9 m ³ (H/D ~1.7/2.9 m)
Core pressure drop	~ 0.04 MPa
Volume fraction (%) Fuel/Gas/SiC	50/40/10
Average power density	55 MW/m ³
Reference fuel compound	(U, Pu)C/SiC (50/50%) 17% Pu
Breeding/Burning performances	Self-Breeder
Maximum fuel temperature	1174 °C (normal operation) < 1650 °C (depressurization)
In core heavy nuclei inventory	30 tons
Fission rate (at %); Damage	~ 5 at%; 60 dpa
Fuel management	multi-recycling
Fuel residence time	3 x 829 efpd
Doppler effect (180 °C–1200 °C)	-1540 x 10 ⁻⁵
Delayed neutron fraction	356 x 10 ⁻⁵
Total He void effect	+230 x 10 ⁻⁵
Average burn-up rate at EOL	~ 5% FIMA
Primary vessel diameter	< 7 m

Figure 4.3 shows the results of the LOCA simulation, taking credit only for natural convection. Calculations are shown for two families of heat transfer correlations:

Dittus-Boelter and Gnielinski. Peak fuel temperature occurs at about 30 minutes and is less than the ceramic core maximum allowable transient limit of 1650°C: but only because the ambient helium-air ambient pressure was specified to be (the high) value of 1.2 MPa. This could only be assured if a proximate guard containment was employed. Parametric studies showed that for back pressure values of 2.0 and 3.0 the fuel temperature did not exceed steady state values.

Fig. 4.3 Comparison of LOCA Axial Fuel Temperatures for the Dittus-Boelter and Gnielinski correlations

These results confirm the need for active decay heat removal in the early stages of a severe LOCA, since addition of a high-pressure guard containment would add prohibitively to plant capital cost. A better alternative is to reduce LOCA frequency and break size by use of a PCRV (or PCIV) – as proposed by the GCFR design community in the 1970-1980 time frame. This also increases the probability of break isolation followed by reactor vessel repressurization. While special measures must be taken to keep the concrete at less than about 200°C, by insulation and cooling, the capability for doing so was demonstrated in the THTR.

Much more remains to be done along the lines of such severe transient analyses. The RELAP model developed for the runs summarized here (and others documented in Ref 4-14) can serve as the template for future work in which active shutdown cooling systems are added and sized to provide the required level of protection against core damage.

4.5 Core Materials

Our choice of fuel (UC) and cladding (ODS) is highly congruent with the mainstream GFR effort. Hence the B&B approach can “piggyback” on these existing efforts. Reference (4-5) discusses GFR materials state-of-the-art and needs. Reference (4-6) documents UC properties useful to the core designer.

4.5.1 Fuel

As noted in Chapter 3, UC fuel is our reference choice. It has for some time now been under test and evaluation for advanced fast reactor cores worldwide. Of particular relevance is its selection (as a UC•SiC cercer) for the ANL/CEA INERI GEN-IV GFR project (4-7).

The most germane recent development has been the full core test in the Indian fast breeder test reactor (FBTR) of UC fuel to a burnup of over 140 MWd/kg, without any fuel failures so far (4-8). Fuel discharged at 150 MWd/kg is undergoing post-irradiation examination. Power density exceeded our requirements, especially when one takes into account the TID vs pin temperature advantage noted in section 4.3.

Hence the irradiation properties of UC based fuels have been successfully demonstrated under nominal operating conditions. It is significant that the selected tube in duct fuel configuration keeps the fuel within the thermal regime where acceptable irradiation performance has been demonstrated. However, a number of macroscopic phenomena within individual fuel elements remain to be examined before the fuel performance can be definitively evaluated including densification following in-pile sintering, fission product transport, clad-fuel chemical interaction, etc. These phenomena are inherently a result of processes driven by microscopic behaviors accumulating into a macroscopic aggregate that may be unique to the specific fuel configuration. There is not enough fundamental information on the fuel in the proposed configuration to fully predict performance in the absence of additional irradiation tests but acceptable performance is anticipated. Experimental confirmation of in-pile behavior was unfortunately beyond the scope of this project.

4.5.2 Cladding (and Duct)

Here again our choice parallels that of many, if not most, contemporary fast reactor designers in the selection of oxide dispersion steel (ODS).

Accident scenarios drive fuel requirements for gas-cooled fast reactors. For example, in the case of a core ‘blowdown’ event, core pressure is reduced substantially and immediately followed by an increase in fuel and cladding temperature. This event is relatively short in duration. The principal consequences of the core blowdown on fuel behavior are threefold. Firstly, the balancing effect of core pressure on pin internal pressure is lost. During normal operation, coolant pressure of 7-10 MPa provides

restraint against outward fission gas driven cladding creep, resulting in low cladding stresses. During a depressurization event, the external pressure is lost, resulting in a sudden increase in stress. Secondly, the fuel cladding temperature increases, decreasing rupture strength and resulting in increased creep rates. Thirdly, the release rate of fission gas from the fuel may increase, further increasing pin internal pressure and stress on the cladding. These requirements combine to increase the requirements for cladding creep rupture resistance for fuel relative to the well-developed conventional LMFBR fuel. Presumably due to this issue, conceptual work in the 1970's on gas-cooled fast reactor fuel concentrated on vented fuel pin concepts to maintain reasonable levels of cladding strain during a 'blowdown' event. Pins were vented through a plenum and then through a 'scrubber' into the reactor coolant stream.

ODS alloys have been developed to improve high-temperature creep strength of ferritic steels. These materials are, for the most part, developmental alloys based on 9 - 20 wt.% Cr containing nano-particle dispersions of yttrium oxide. These particles markedly increase the high-temperature creep resistance of the ODS alloys relative to conventional low-swelling cladding steels. ODS alloys offer the additional advantage over refractory metals of robust resistance to oxidation in the case of air ingress and are also neutronically acceptable. Although still in the developmental stage, these alloys show promise as replacements for HT-9 steel that was the reference cladding material for high-burnup fuels near the end of the U.S. LMFBR cladding and duct development program. A partial listing of available irradiation data is provided in Table 4.4. Note that the JNC (Japan Nuclear Cycle Development Institute) has made recent advances in experiments and out-of-pile creep data indicate that these alloys should be usable in a GFR system up to a peak cladding temperature of 700°C during normal operation.

Table 4.4 Summary of ODS irradiation data

Alloy	Max. Dose (dpa)	Temp. Range (°C)	Summary Result	Reference
DT2203Y05	81	400-580	Negligible creep & swelling Severe embrittlement at $T_{irr} < 500^{\circ}\text{C}$, Ductile fracture $T_{irr} > 500^{\circ}\text{C}$	4-9
MA957	200 (8×10^{22} n/cm^2)	370-760	Increase in DBTT to 350°C at $T_{irr} < 410^{\circ}\text{C}$	4-10
JNC 9Cr-ODS 12Cr-ODS	20	373-572	No serious degradation in tensile strength, ductility, or Charpy impact energy	4-11
JNC-ODS	36 (6% MOX burnup)		Fueled MOX test in EBR-II, pressure resistance welded end plugs, no failures	4-12

Sufficient creep data is not currently available to develop cladding creep models for the various types of ODS clad fuel under irradiation. The Larson-Miller parameter can be used, however, to provide a tool for comparison using estimates of required creep rupture properties and available creep data; it is essentially a simple method for viewing temperature normalized creep rupture data. Stress is plotted over the Larson-Miller parameter, $LMP = T(C + \log(r_t))$, where C is a constant, and r_t is rupture time. Figure 4.4 shows Larson-Miller plots for some ODS alloys included in Table 4.4. Conventional Fe-9Cr and PWC-11 alloys are plotted for reference. Materials that plot toward the upper right hand corner are those that exhibit the best resistance to creep rupture. During normal operation, low cladding stresses are maintained by counter-acting pressures inside and outside of the cladding tube and result in reasonable requirements in terms of creep-rupture properties. In fact, cladding creep-down and subsequent ridging may be a more serious problem than creep-rupture. Creep-rupture requirements during steady-state operation could be met using current ODS steel alloys even if venting/pressure equalization were not employed. Thus it is the concern over the ability to preserve fuel integrity and hence coolable geometry during severe accidents that has led to our decision to emulate GCFRs of old in the use of vented fuel.

Fig. 4.4 Larson-Miller Plot of ODS Alloys and Envelope of B&B Cladding Requirements during Normal and Off-Normal Conditions

4.6 Summary and Conclusions

It has been shown that a breed and burn core is inherently associated with both beneficial and restrictive consequences of its stringent neutronic requirements. Specifically, a high fuel volume fraction is essential, which implies a small coolant volume fraction. This in turn leads to adoption of a tube-in-duct fuel assembly design, low power density and large coolant temperature rise across the core.

The upside of these responses to serious constraints is that a conventional steam Rankine cycle power conversion system can/must be employed, thereby greatly reducing the up-front R&D bill; and, on an even more positive note, transient tolerance is greatly improved by the low core power density and the low peak and average fuel temperature of the inverted TID fuel configuration compared to a conventional pin-type core.

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Chapter 5

Balance of Plant: Technology and Costs

5.1 Introduction

The original plan was to employ a supercritical CO₂ power conversion system (PCS) – indirect because of the incompatibility of high heavy metal density fuels such as UC or UN with CO₂. However, it eventually became clear that the small operating range (~150°C) of this type of PCS would lead to excessive power consumption by the primary coolant circulators – especially in view of the need to have high fuel/low coolant volume fraction in the core. Hence a decision was made to revert to use of a steam Rankine cycle power conversion system. This basically removed PCS design from the agenda because of the wide experience with the Rankine cycle in both the operation and design of gas cooled reactors. While no fast GCRs have been constructed to date, several designs were carried through to an advanced stage in the 1970's, and there is an almost directly transferable experience base in the form of thermal GCR operations. The most notable of these precursors is the British AGRs, but the first generation HTGRs – Fort St. Vrain, AVR and THTR – are also relevant because of their use of helium on the primary side.

5.2 Power Conversion System

Table 5.1 summarizes the Rankine PCS related characteristics of several GCRs which have been constructed and operated, while Table 5.2 does likewise for GFR concept studies carried out in the 1970 time frame.

Table 5.1 GCR Rankine Cycle Experience (5-1)

<u>Parameter</u>	<u>PLANT</u>		
	<u>British AGR</u> <u>(Typical of 14)</u>	<u>Fort St. Vrain</u>	<u>THTR</u>
Core outlet, T°C	638 (CO ₂)	775 (He)	750 (He)
Turbine inlet, T°C	538	539	530
Turbine inlet pressure, MPa	16.3	16.9	18
Gross efficiency, %	42.6	40.6	41.0
Net efficiency, %	39.4	39.2	39.5

Table 5.2 GCFR/GFR Design Studies

<u>Parameter</u>	<u>GA GCFR</u>	<u>Euro GBR-4</u>	<u>UK ETGBR</u>	<u>Russian BGR-300</u>
Core outlet, T°C	575	565	525	630
Turbine inlet, T°C	510	530	—	535
Turbine inlet pressure, MPa	12.4	11.2	—	17
Gross efficiency, %	40.3	—	40	—
Net efficiency, %	36.5	35	37	37
Ref	(5-2)	(5-3)	(5-4)	(5-5)

Conventional Rankine cycle efficiency can be related to the Carnot cycle value using the semi-empirical expression (5-6)

$$\eta_{Ra} = 0.6\eta_c + 0.04 \quad (5-1)$$

For a sink temperature of 300°K and turbine inlet temperature of 530°C, the predicted gross efficiency is 41.6%, in good agreement with the values cited in Tables 5.1 and 5.2.

Thus the value of net efficiency of 40% employed for the B&B reference reactor design is plausible in view of our specific adoption of the AGR BOP technology, and our use of helium coolant in place of CO₂, which should allow somewhat higher core exit temperatures if need be.

5.3 Primary Circulator Power Consumption

It is fairly straightforward to derive the following expression for the ratio of power expended to transport a given amount of thermal energy from a heat exchanger, or in this case, a reactor core (5-7).

$$\frac{W}{Q} = \frac{10^{-3} \text{Pr}^{2/3} Q^2}{(\rho^2 C p^3) A_f^2 \Delta T_f \Delta T_c^2} \quad (5-2)$$

where

W = circulator power expenditure

Q = thermal power

A_f = transverse flow area
 ΔT_f = channel average heat transfer film drop
 ΔT_c = coolant temperature rise in channel

For a given gas and core design, and $\Delta T_f \approx 1/2 \Delta T_c$, which minimizes maximum clad temperature, one has:

$$\frac{W}{Q} \sim \frac{1}{\Delta T_c^3} \quad (5-3)$$

Hence circulator power expenditure is very sensitive to coolant temperature rise.

For the S-CO₂ PCS, the efficiency-optimum temperature difference between recuperator outlet (IHX inlet) and turbine inlet (IHX outlet) on the secondary side is about 150°C. Hence the core ΔT_c is constrained to about the same value: core inlet (primary IHX outlet) must exceed the secondary inlet temperature to the IHX and core outlet is strongly constrained by materials capabilities.

On the other hand core ΔT_c values for the British AGR are about 340°C. Equation (5-3) implies that our GFR circulator power consumption would be a factor of approximately 12 higher if a S-CO₂ PCS were adopted: a clearly unsuitable penalty.

Circulator power as a percentage of gross electric power production was as follows for the GFR designs of Table 4.2:

GA-GCFR	10.7	%
GBR-4	10.5	%
ETGBR	8.3	%

The net loss in PCS efficiency is $(1-\eta)$ times these values because the circulators add thermal power to the primary coolant. Even so, the penalties are significant for these Rankine cycle plants. While the S-CO₂ cycle is several percent more efficient on a gross power basis, this is not nearly sufficient to make up for the much higher primary circulator power consumption its use would entail in the present instance.

5.4 Pressure Vessel

All of the older GFR designs, and all AGR units, have employed prestressed concrete reactor vessels (PCRV). However, the mainline GEN-IV GFR being evaluated by the INERI led by ANL and CEA specifies use of a steel vessel. Nevertheless, a PCRV appears the best choice for our B&B unit, in view of its use in all earlier large GCRs, and GCFR design projects. We have, however, evaluated use of a prestressed cast iron vessel (PCIV) as pioneered in Germany (5-8). This approach has several potential advantages, but has only been demonstrated in small-to-medium scale tests. Thus it is a worthwhile R&D topic, but not yet qualified for use in our reference design.

The most significant issue which may be raised is the pressure rating. Some comparisons of interest are as follows:

	<u>MPa</u>
B&B GFR	11
GA-GCFR	9
GBR-4	12
ETGBR (CO ₂)	4.35
BGR-300	16
AGR (CO ₂)	4.35
Ft. St. Vrain	4.8
THTR	3.9
GEN-IV (steel)	7

Thus, while on the high side, our use of a PCRV appears feasible based on expert opinion, despite the lack of in-the-field reactor experience above 5 MPa.

We need not expound on the many safety advantages attributable to PCRV use since the B&B GFR poses no unique safety challenges, and should benefit to the same extent as the other GFR designs developed using this technology. One interesting aspect is that all of these precursors incorporated a “core” or “debris” catcher at the bottom of the vessel cavity to provide added in-depth safety in the event of core meltdown.

5.5 Shutdown/Emergency Core Cooling System: SCS/ECS

5.5.1 General Aspects

It has been long and widely recognized that provision of shutdown cooling is the principal engineering challenge for gas-cooled fast reactors. While most other physical features are similar to their thermal spectrum HTGR counterparts, the GFR core power density (at full power, hence also in decay heat mode) is an order of magnitude larger. Hence this topic deserves special attention; accordingly the several GFR related research projects at MIT have all made design of a highly reliable shutdown cooling system a top priority task. Lessons learned include:

- (1) A hybrid active/passive set of cooling loops, of the generic type shown in Fig. 5.1, is to be preferred; completely passive natural convection/circulation is effective only if helium pressures can be maintained above about 15 atm
- (2) As shown in Table 5.3, four loops, each 50% capable, give very high assurance of functionality, if proper attention is paid to avoidance of common cause failures. Fig. 5.2 shows typical system failure probability as a function of individual loop failure probability.
- (3) Helium is vulnerable to flow starvation in the hottest channels, because its viscosity increase outweighs its buoyancy increase due to reduced density.
- (4) CO₂ is considerably superior to He in the natural convection mode (factor of 3-5), which motivates consideration of injection of CO₂ in extreme event scenarios.

Indirect cycle – Option IC3 (SCS/ECS in pods; ducts)

Key Features

- 4 x 50% loops; one loop shown
- Each has 4 blower/check valve assemblies
- Four blower motors, each 11.25 kW per loop
- Coaxial duct penetrates below IHX
- Uses external piping to gain thermal center height difference
- Thermal center height difference ~15m

Fig. 5.1 SCS Loop with HEATRIC™ HX

Table 5.3 Failure of Protective Function

$$F = \left[\frac{n!}{(n-m)!m!} \right] P_1^m (1-P_1)^{(n-m)}$$

where	F	=	failure probability of ensemble
	n	=	number of parallel systems
	m	=	number which must <u>fail</u> to cause failure of protection
hence	r	=	n – m + 1, number of parallel systems which must work
	P ₁	=	probability of failure of one system
	C	=	capability of each system, 100%/r

n	C%	m	r	F for P ₁ =	
				10 ⁻²	10 ⁻³
1	100	1	1	10 ⁻²	10 ⁻³
2	100	2	1	10 ⁻⁴	10 ⁻⁶
4	33	2	3	6 x 10 ⁻⁴	6 x 10 ⁻⁶
4*	50	3	2	4 x 10 ⁻⁶	4 x 10 ⁻⁹
3	50	2	2	3 x 10 ⁻⁴	3 x 10 ⁻⁶
3	100	3	1	10 ⁻⁶	10 ⁻⁹
4	100	4	1	10 ⁻⁸	10 ⁻¹²

*Recommended configuration

Fig. 5.2 Overall System Functional Failure as a Function of Single Loop Unreliability

The SCS/ECS loops in Fig. 5.2 are outside of the reactor vessel, which in the mainstream INERI GFR design is a conventional steel vessel. Protection against complete depressurization in the event of a LOCA (and/or a LOCA by SCS loop rupture) is provided by a surrounding proximate PCRV containment. In our B&B design the reactor vessel itself is a PCRV, which can house the SCS/ECS loop internally: see Figs. 2.4, 2.5, 5.3.

Fig. 5.3 Reference SCS/ECS for PRA Performance Assessment

In arriving at the rather straightforward design shown, a wide range (nine) of variations on the basic design were evaluated in an interactive process between the thermal-hydraulic and PRA groups. As documented in Ref. (5-9), the present version was found to have the best overall balance among assessment criteria.

Another line of attack on the reliability issue was an investigation of the use of fuel cells to supplement/supplant diesels for provision of emergency power in the event of station blackout. As noted in Refs. (5-10) and (5-11), this approach, while promising, is not ready for near-term adoption, but worth keeping an eye on. In particular, the

CITARO/NEBUS program is currently testing a fleet of buses having an integrated fuel cell power train, rated at 250 kW. This package could essentially be used almost as-is for our application. A second reason for deferral is the recent NRC acknowledgement of improved diesel-based emergency power system performance (5-12).

5.5.2 Decay Heat Removal (DHR) System Details

The main part of the hybrid active/passive DHR system, which represents the shutdown cooling system (SCS) loop with HEATRIC heat exchangers, adopted from our I-NERI project efforts (Ref. 4-7) is shown in Figure 5.1.

The shutdown cooling system (SCS) is designed with the goal of maximizing decay heat removal capacity by natural circulation of gas coolant under elevated backup pressure, with attention paid to economic considerations, in particular minimum size and design pressure of the containment. These considerations require the achievement of minimum pressure drop of the SCS loop and minimum possible dimensions, in particular loop height and SCS heat exchanger (SCS HX) size. Since large thermal center differences between the core and the SCS HX and larger HX size (thus small pressure drop) yield higher decay heat removal rates, a compromise in determining the SCS loop sizing is necessary. HEATRICTM compact heat exchangers, located about 11m above the core, were selected for the SCS HX due to their small size. In addition, very low core damage frequency (CDF) and high reliability is desirable, hence multiple SCS loops are necessary. A choice of 4x50% loops was made to satisfy the current single failure criterion requirement and allow for maintenance of one loop during operation. The safety-grade blowers maintain gas circulation after primary system depressurization until decay power is reduced sufficiently to make decay heat removal by natural circulation possible.

Heat exchanger channels are very short (0.4m) and are horizontal with fully counter-current flow. Hot fluid flow splits on the flow splitter into two streams – one proceeds into the chamber in front of the HX and the other the chamber behind the HX (not shown on the figure). From these chambers, hot flow proceeds horizontally into the HX, as indicated by arrows “LP-in” on the expanded view of the HEATRICTM HX. Inside the HX, the vertical plenum distributes the flow into a large number of horizontal channels where cooling occurs under fully counter-current flow. Cool flow is collected in the exit plena, from where it proceeds sideways through a number of channels as indicated by “LP Out” arrows. Then it turns downward into the blower chamber and proceeds via the blower and check valve into the cold duct, as indicated by blue arrows. There are 4 blowers and 4 check valves per loop (only 2 are shown on Figure 1). The HEATRICTM HX were sized to remove 2% decay power under natural circulation, but they can remove higher power levels under forced circulation during shutdown.

Component sizing was performed using the LOCA-COLA (Loss of Coolant Accident - Convection Loop Analysis) code for steady state analysis of generic gas convection cooling systems developed at MIT and a code for thermal performance

analysis of HEATRIC heat exchangers. Although HEATRIC™ HX with zigzag channels offer the largest power density, a version with straight channels was used for the SCS HX application because low pressure losses were more important than size for system performance. Table 5.4 gives the details of HEATRIC HX design, which was sized for a 2% decay power rating. The duct sizing was optimized so that the ducts have the minimum dimensions that can still maintain negligible pressure losses in the ducting. The optimum diameter was found to be 1.35m and 1.5m for the inner and outer duct.

LOCA-COLA calculations have shown that for the B&B core with TID fuel assemblies having radial and axial power peaking of 1.77 and 1.4, respectively, backup pressures of 35 bars and 15 bars would be needed for fully passive operation. Therefore, injection of CO₂ is a preferable approach to maintain the required containment pressure after LOCA at levels compatible with current containment design pressures. After 24 hours, decay heat is reduced to 0.5%. At this power level, decay heat can be removed by natural circulation at a containment backup pressure of 8 bars. During the first 24 hours, battery or fuel cell driven blowers will be used. Power requirement for these four blowers totals 45kW per 50% loop, thus 90kW are required to remove 3% decay heat through 2x50% loops. These power requirements are well within the range of fuel cells, and much less than the 2500kW diesels used for LWR emergency power.

Table 5.4 Decay heat removal heat exchanger design -

HX design	
OPERATING CONDITIONS	
48000	power (kW)
800	pressure - hot fluid - hot end (kPa)
500	pressure - cold fluid - cold end (kPa)
124.2807	hot side pressure drop (Pa)
-74.1285	cold side pressure drop (Pa)
92.4	mass flow rate on hot side (kg/s)
280.16	mass flow rate on cold side (kg/s)
3.03	cold mass flow rate over hot mass flow rate
101.14	temperature - hot fluid cold end (oC)
25.03	temperature - cold fluid cold end (oC)
581	temperature - hot fluid hot end (oC)
65.99	temperature - cold fluid hot end (oC)
1088.43	enthalpy - hot fluid - hot end (kJ/kg)
569.37	enthalpy - hot fluid - cold end (kJ/kg)
276.56	enthalpy - cold fluid - hot end (kJ/kg)
105.37	enthalpy - cold fluid - cold end (kJ/kg)
0.0467	cold side inlet velocity (m/s)
1.3411	hot side outlet velocity (m/s)
2516.6	Reynolds number - hot fluid cold end (-)
HX GEOMETRY	
14.97678	Total HX volume - all modules (m3)
7.48839	1 Module HX volume (m3)
22	Total HX height (m)
5.5	HX height for 1 module (4 submodules) (m)
0.6	HX width (m)
0.5673	HX length (m)
5	Channel diameter on hot side (mm)
5	Channel diameter on cold side (mm)
5.71	Channel pitch on hot side (mm)
5.71	Channel pitch on cold side (mm)
306116	Number of channels on hot side per 1 module
306116	Number of channels on cold side per 1 module
3.7	Plate thickness on hot side (mm)
3.7	Plate thickness on cold side (mm)
2972	Number of plates per module
2	Number of modules (2x50%)

5.6 Plant and Overall Cost Assessment

Cost Estimation

This task was considerably modified, but greatly simplified, by our mid-course switch to a Rankine Steam Cycle. This permitted referencing all except fuel cycle costs to a definitive HTGR cost analysis carried out by Gas Cooled Reactor Associates (GCRA) (with major contributions by GA, Bechtel and ABB/CE). Tables 5.5 and 5.6 summarize the results from Ref (5-13), which were prepared in accordance with standard costing procedures (Ref 5-14). Subsequent useful input was also available in the form of Ref (5-15).

For present purposes we need to adjust base case costs for our larger rating (2400 vs 450 MWth), slightly higher assumed thermodynamic efficiency (40 vs 38.5%) and year of reference (2003 vs 1992 \$). The year 2003 was selected to facilitate comparisons to the recent MIT and University of Chicago studies (5-16, 5-17).

Efficiency:

$$2391 \frac{\$}{\text{kWe}} \times \frac{38.5}{40} = 2300 \text{ \$/kWe}$$

Rating, using 0.7 scale factor:

$$2300 \times \left(\frac{450}{2400} \right)^{(1-0.7)} = 1392 \text{ \$/kWe}$$

2003 vs 1992\$, using 3%/yr escalation

$$1392 \times (1.03)^{11} = 1927 \text{ \$/kWe}$$

This compares to a base case overnight LWR cost of 2000 \$/kWe in Ref (5-16); the overnight cost of our B&B HTGR (see Table 1) can be estimated as:

$$1927 \times \frac{1450}{1657} = 1686 \text{ \$/kWe}$$

A considerable part of the reason for the advantage of the HTGR is its higher thermal efficiency (40 vs 33%). Were its efficiency only 33%, the HTGR overnight cost would be

$$1686 \times \frac{40}{33} = 2044 \text{ \$/kWe}$$

which considering the uncertainties inherent in cost estimation is essentially identical to that of the PWR.

Reference (5-17) cites studies having a spread of 1100 to 2300 \$/kWe in overnight costs, and carries out parametric studies at 1200, 1500 and 1800 \$/kWe. They note the adoption by the Energy Information Agency (EIA) of 1853 \$/kWe in their 2003 Annual Energy Outlook.

All things considered, a post-construction cost of about 1900 \$/kWe appears justifiable, with modest savings vs LWRs due to the higher thermal efficiency of the HTGR.

The unit capital cost in \$/kWe can be converted into mills/kWhre by multiplying by the factor

$$f = \frac{\phi}{8.766L}$$

where ϕ = annualized carrying charge factor (e.g. 0.11 yr⁻¹)

L = annual average plant capacity factor (e.g. 0.9)

Hence, for the values quoted $f = 0.0139$.

This is in good agreement with the value recoverable from Table 5.6, namely:

$$f = \frac{34.7}{2591} = 0.0134$$

Hence for the B&B host reactor, the capital cost is:

$$e_c = 0.0139 (1927) = 26.8 \text{ mills/kWhre}$$

The operating and maintenance cost contribution will be assumed to be the same as in Table 5.6: i.e. 9.5 mills/kWhre. The various correction factors for rating, efficiency and monetary inflation would, taken in concert, not change this term by more than the uncertainty involved in its estimation.

Reference 5-16 notes that recent non-fuel O&M costs for LWR nuclear plants have ranged between 12 and 18 mills/kWhre, with the best plants at 8 mills/kWhre, and some future projections as low as 5 mills/kWhre. Correcting 9.5 mills/kWhre for the lower LWR plant efficiency gives 11.5 mills/kWhre. Hence the value we have adopted is plausible.

The final contribution, fuel cycle cost, is unique to the B&B core, and has been discussed at some length by Yarsky (5-18). For his UC fueled “advanced” core a value

of 5.11 mills/kWhre is projected. A PWR calculated using the same cost model and economic parameter input would have a fuel cycle cost of 6.72 mills/kWhre. The B&B values are for steady state reloads. The startup core (first 6 batches) has an average enrichment of 8 w/o U-235 and average burnup of 92.5 MWd/kg (vs 150 MWd/kg at steady state). Thus startup core fuel cycle cost without any fuel re-use will be on the order of 13 mills/kWhre. Over the life of the reactor, then, the B&B core will have comparable economics to today's LWRs.

Total busbar costs are, therefore, on the order of 44 mills/kWhre, which is competitive with the advanced LWR concepts surveyed in Ref(5-17), but not outstanding from any perspective.

Discussion of Cost Projection

The preceding analysis glossed over a number of points which deserve mention:

- (1) We used costs for a thermal spectrum HTGR, whereas the B&B is a fast spectrum core. This is actually a reasonable and conservative approach since the total volume of a high power density (here 128 kW/l) GFR core is smaller than that of a low power density HTGR core (~8 kW/l), which provides more than enough volume for accommodation of the B&B core, if not prospects for savings on PCRV costs.
- (2) A B&B fuel fabrication cost of 275 \$/kg HM was assumed, the same as the OECD reference value for LWR fuel. This appears plausible for our pin-type core, whose assemblies are rather similar to the shrouded, hexagonal Russian VVER PWR fuel assemblies, with steel substituted for Zircaloy. However there is no manufacturing experience for the inverted tube-in-duct type assembly we have adopted for the preferred UC fueled version of the B&B core.
- (3) The mediocre economic performance of the B&B GFR is a direct result of its stringent neutronic requirements, which effectively prevent use of closed Brayton cycle power conversion systems. The need to exclude moderating diluents rules out cercer fuels, which restricts temperatures to values below that required for helium Brayton cycles. The need for high fuel volume fraction rules out use of a supercritical CO₂ Brayton cycle because its small temperature range (inlet vs outlet $\Delta T \sim 150^\circ\text{C}$) leads to high core coolant flow rates and excessive primary coolant circulator power in an indirect cycle (needed because CO₂ can oxidize UC and UN). Since most studies show a 10 to 15% busbar cost advantage of direct Brayton cycles, due mostly to their higher thermal efficiency, the Rankine B&B plant is at an inherent disadvantage.
- (4) On the positive side of the ledger, the use of proven steam cycle HTGR technology greatly reduces R&D and first-of-a-kind costs. (Reference (5-13) provides considerable detail on the added up-front costs of the helium Brayton cycle.) It also allows considerably shorter deployment scenarios. This aspect is synergistic with the B&B fuel cycle, which can be deployed without prior or concurrent deployment of fuel reprocessing and recycle facilities. A B&B fleet in existence can subsequently be converted to conventional breeder mode or to

speciality applications such as TRU and/or MA burning much faster than otherwise programmable: i.e. by replacing only the core. The bottom line is that the GFR B&B concept has acceptable economics, with busbar costs comparable to current or advanced LWRs, but inferior to the predicted costs of some advanced reactor/GEN-IV concepts.

Table 5.5 Costs of HTGR Reactor with Steam Cycle

Account No.	Account description	NI side	BOP side
<u>20</u>	<u>Land and land rights</u>	<u>0.00</u>	<u>2000.00</u>
<u>21</u>	<u>Structures and improvements</u>	<u>117150.16</u>	<u>32918.75</u>
<u>22</u>	<u>Reactor plant equipment</u>	<u>421929.79</u>	<u>1296.83</u>
<u>23</u>	<u>Turbine plant equipment</u>	<u>181.79</u>	<u>155481.46</u>
<u>24</u>	<u>Electric plant equipment</u>	<u>25150.33</u>	<u>26778.63</u>
<u>25</u>	<u>Miscellaneous plant equipment</u>	<u>16485.80</u>	<u>23680.71</u>
<u>26</u>	<u>Main cond. Heat reject system</u>	<u>0.00</u>	<u>30172.97</u>
	<u>Subtotal</u>	<u>580897.86</u>	<u>272329.35</u>
	<u>Total direct cost (1992 K\$)</u>	<u>853227.22</u>	
<u>91</u>	<u>Construction services</u>	<u>78610.10</u>	<u>38040.00</u>
<u>92</u>	<u>Engineering and home office services</u>	<u>55735.51</u>	<u>3970.00</u>
<u>93</u>	<u>Field supervision and field office services</u>	<u>36422.03</u>	<u>20425.00</u>
<u>94</u>	<u>Owner's cost</u>	<u>0.00</u>	<u>131978.31</u>
<u>95</u>	<u>Reactor manufacturer home office eng. & services</u>	<u>0.00</u>	<u>0.00</u>
	<u>Subtotal</u>	<u>170767.63</u>	<u>194413.31</u>
	<u>Total indirect cost (1992 K\$)</u>	<u>365180.94</u>	
	<u>Base construction cost (1992 K\$)</u>	<u>1218408.16</u>	
	<u>Total contingency (1992 K\$)</u>	<u>232000.00</u>	
	Contingency (1992 K\$)	185600.00	46400.00
	<u>Total overnight cost (1992 K\$)</u>	<u>1450408.16</u>	
	<u>Interest during construction (1992 K\$)</u>	<u>206620.00</u>	
	<u>TOTAL CAPITAL COST (1992 K\$)</u>	<u>1657028.16</u>	
	<u>TOTAL CAPITAL COST (1992 \$/kWe)</u>	<u>2391.09</u>	

Table 5.6 MHTGR Busbar Generating Costs ('92\$)
Replica Plants – 2013 Startup (from Ref. 5-13)

	STEAM CYCLE
REACTOR THERMAL POWER (MWt)	4x450
NET EFFICIENCY (%)	38.5%
NET ELECTRIC RATING (MWe)	693
CAPACITY FACTOR	81%
TOTAL CAPITAL COST (M\$)	1,795
UNIT CAPITAL COST (\$/kWe)	2,591
FIXED CHARGE RATE	9.49%
LEVELIZED CAPITAL COST (M\$/YR)	170
FIXED O&M COST (M\$/YR)	40.8
VARIABLE O&M COST (mills/kWh)	0.2
CONTROL ROD & REFLECTOR REPLACE (M\$/YR)	4.8
ANNUAL O&M COST (M\$/YR)	46.8
FUEL COST (\$/MBTU)	1.37
LEVEL FUEL CYCLE COST (M\$/YR)	59.8
DECOMMISSIONING COST (M\$)	194
LEVEL DECOMMISSIONING (M\$/YR)	5.2
REVENUE REQUIREMENT (M\$/YR)	282
BUSBAR COST (mills/kWh)	
CAPITAL	34.7
O & M	9.5
FUEL	12.2
DECOMM	1.1
TOTAL	57.5

5.7 Summary and Conclusions

As the B&B BOP plant design evolved, the trend was toward reversion to prior convention in virtually all aspects. This had two countervailing consequences:

- (1) It permits use of well-proven technology with a long history of experience in the field and with the licensing process
- (2) It rules out significant reductions in the cost of generated electricity.

The more specific consequence is that core design and performance has become virtually the only focus of breed and burn concept development.

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Chapter 6

Fuel Processing

6.1 Introduction

A principal advantage of the breed and burn concept is that fuel reprocessing (whether separative or non-separative) and recycle, is not required. This allows what amounts to replication of the LWR once-through fuel cycle. B&B reload fuel has the same enrichment as PWR reload fuel, but extracts three times the burnup, hence utilizes natural uranium ore more than three times as efficiently: more in view of the higher thermodynamic efficiency (e.g. 40 vs 33%). Fuel cycle costs in mills/kWhre will be about the same because higher fuel fabrication costs are likely and the long in-core residence time incurs higher carrying charges.

This allows early introduction of fast reactors without waiting for the prior perfection of less expensive and more proliferation resistant reprocessing methods. At a later date, should circumstances permit, the B&B cores could be replaced by more conventional breeder-with-recycle cores which would increase uranium resource utilization by more than a factor of fifty compared to LWRs. This ensures transition to an indefinitely sustainable energy generation scenario.

The main line of thought, therefore, was to put spent fuel into long term storage, and, if all else fails, then into geological final disposal. However, since the spent fuel contains on the order of 8 w/o fissile plutonium, it is an attractive candidate for re-use, either in the B&B GFR itself, or in LWRs. This can in principle be carried out using non-separative processing, combined with blending the recovered fuel with natural or depleted uranium (or perhaps even thorium) down to neutronic equivalence with the 5 w/o U-235 fuel normally used for LWR or B&B GFR reloads. Another attendant advantage is that the plutonium would be further spoiled as a weapons usable feedstock.

Accordingly, one important subtask of the subject B&B assessment project was to evaluate the feasibility of AIROX type processing for uranium carbide fuel using CO₂ as the oxidant, to produce as a final product either UO₂ for LWR re-use or UC for B&B GFR re-use. The sections which follow summarize our findings.

6.2 Chemical Processing

A review of the literature (6-1) (6-2) and confirmatory laboratory experiments showed that straightforward reduction of the oxide with carbon was the preferred approach:

In the MIT/UNLV lab experiments, the carbon was in the form of graphite.

This process can be used either to prepare fresh unirradiated reload fuel, or as a step in the sequence of successive oxidation-reduction reactions involved in AIROX-type fuel reprocessing.

A particularly pertinent current program is the Indian fast breeder work on carbide fueling (6-3). They employed carbothermic reduction of mixed uranium-plutonium oxide in high purity inert atmosphere glove boxes.

Oxidation of UC by CO₂ was evaluated to demonstrate a “CARDIO” equivalent to AIROX reprocessing. According to the literature one has:

Again, laboratory experiments gave results compatible with Eq(6-3).

The preceding results are applicable in the following respects:

- (1) Equation (6-3) shows how GFR spent fuel can be converted into UO₂ for recycle in LWRs
- (2) Equation (6-3) followed by Eq(6-1) shows how UC fuel can be re-constituted for recycle back into the GFR as UC
- (3) Equation (6-3) shows that CO₂ cannot be used as a coolant in UC fueled cores. Oxidation increases fuel volume by about one-third, which would destroy fuel integrity and threaten loss of coolable geometry.

In operations (2) above, the re-used U/Pu fuel which contains roughly 10 w/o fissile nuclides would be blended with fresh material (e.g. natural uranium) in a roughly 1:1 ratio to make fuel which is reactivity-equivalent to 5 w/o enriched fresh all-uranium fuel in the hard GFR neutron spectrum.

In operation (1), blending with fresh fuel of normal enrichment (e.g. 4.4 w/o) is required to reduce the otherwise excessive Sm-149 self-generated burnable poison to an appropriate level. Also, the AIROX/CARDIO processing is not credited with removal of a significant amount of fission products, hence residual fission products offset the high asymptotic fissile content of spent fuel when it is loaded into a thermal spectrum reactor.

This latter observation should be tempered by experimental evidence developed in the course of the current project that larger-than-expected removal of rare earths was

measured in simulated CARDIO processing tests (6-4) (6-5). This is worth further investigation, since it could form the basis for an advanced dry reprocessing method of general utility in the AFCI program.

6.3 Kinetic Evaluation of Fission Element Removal by CARDIO Processing

Previous results on the CARDIO process for the removal of fission products indicated removal of nearly 70 % of Eu, Gd, Nd, and Sm by oxidation under CO₂ at 1000 °C and 1600 °C. In addition, near complete removal for Ag, Cs, and Mo was observed in all cases. In order to model the removal of the fission elements and evaluate the fundamental reactions, kinetic experiments on the removal of the key fission elements from a uranium matrix were performed. The rate of removal was evaluated for Cs, Eu, Gd, Nd, Sm, Ag, and Rh separately, as a group, and in the presence of the U matrix. Bulk synthesis of the U matrix material was performed for carbide and nitride ceramics. Variations in the furnace conditions included temperature and CO₂ flow rate. Off gas samples were trapped and evaluated radiochemically or with ICP-MS.

The rate of fission element removal was found to fit:

$$[M]_t = [M]_{eq}(1-\exp(-kt)) \quad (6-4)$$

where $[M]_t$ is the solution metal ion concentration at time t , $[M]_{eq}$ the solution equilibrium concentration, and t the time of the concentration measurement. Volatile fission elements were removed regardless of the matrix composition. Removal of oxides of Eu, Sm, and Nd was found to occur in the presence of other fission elements, resulting in massive increases, by over three orders of magnitude, in the removal of lanthanides from the matrix. This behavior was attributed to the presence of Rh in the matrix. The data for all the fission elements is used to generate the rate constant term in Eq. (6-4). The evaluated removal rates for the fission elements are lower in the matrix, and lower still when UO₂ is added (Table 6.1). The data for the lanthanides does show a higher rate constant when each element is evaluated separately, but the removal fraction is so low that the results cannot be meaningfully compared. The kinetic data is used to evaluate activation energies for the removal of the fission elements from the different conditions (Table 6.2). The activation energies for the lanthanide single phases are not for the same species as the entire matrix based upon the difference in volatility concentration and therefore cannot be meaningfully compared to the matrix data. For the non-lanthanide elements a general decrease in activation energy in the matrix is observed. The difference in the activation energies for the fission elements is not greatly dependent upon the presence of U in the matrix. The volatility of Cs and Ag is not greatly impacted by the presence of other elements. The volatility of Rh is somewhat diminished in the presence of other elements. For the lanthanides, around 70% of the initial solid phase matrix is volatilized in the matrix, but only around 0.001% is removed when the lanthanides are in single phase. This corresponds with previous results and indicates Rh may be acting as a catalyst in forming volatile lanthanide species.

Table 6.1 Evaluated rate constants (min^{-1}) for the volatilization of Cs, Ag, Rh, Eu, Sm, and Nd in the presence of UO_2

Element	Temperature		
	750 °C	1000 °C	1250 °C
Cs	$7.84 \pm 0.96\text{E-}03$	$4.82 \pm 0.42\text{E-}02$	$1.72 \pm 0.59\text{E-}01$
Ag	$1.09 \pm 0.22\text{E-}02$	$1.74 \pm 0.62\text{E-}01$	$5.25 \pm 0.38\text{E-}01$
Rh	$9.22 \pm 0.40\text{E-}02$	$2.24 \pm 0.41\text{E-}01$	$4.94 \pm 0.54\text{E-}01$
Eu	$3.28 \pm 0.34\text{E-}02$	$1.11 \pm 0.66\text{E-}01$	$2.84 \pm 0.29\text{E-}01$
Sm	$3.85 \pm 0.54\text{E-}02$	$9.23 \pm 1.01\text{E-}02$	$2.15 \pm 0.29\text{E-}01$
Nd	$3.99 \pm 0.94\text{E-}02$	$7.85 \pm 1.95\text{E-}02$	$1.96 \pm 0.20\text{E-}01$

Table 6.2 Activation energies (kJ/mol) for the volatilization of Cs, Ag, Rh, Eu, Sm, and Nd in differing matrices

Element	In Matrix without U	In Matrix with U	Single Phase
Cs	26.8 ± 3.5	34.0 ± 2.8	51.1 ± 3.9
Ag	73.0 ± 10.3	64.5 ± 13.4	87.3 ± 8.3
Rh	37.7 ± 2.9	47.4 ± 4.1	43.6 ± 5.9
Eu	57.8 ± 15.2	50.2 ± 11.2	33.1 ± 6.5
Sm	47.9 ± 16.0	41.7 ± 8.9	45.2 ± 10.1
Nd	52.8 ± 12.5	43.9 ± 11.4	45.8 ± 9.0

6.4 Discussion, Conclusions and Recommendations

A sound basis has been established for a CARDIO alternative to AIROX fuel processing. This approach appears ideal for a B&B GFR because fuel lifetime is not reactivity limited. Fuel is removed for reconstitution only because data does not exist to support either clad or fuel endurance beyond the values (fluence ≤ 0.87 n/barn, burnup ≤ 165 MWd/kg) invoked to constrain the design presented in this report. Even if no fission products were removed, the re-fabricated fuel could sustain core reactivity to burnups approximately double (e.g. ~ 300 MWd/kg) the currently imposed once-through limit: longer if (as would be the case) blended with fresh uranium to create 5 w/o U-235 – equivalent reload fuel.

Since the laboratory work carried out under this project shows that, if the CARDIO approach were fine-tuned, significant fission product removal might be achieved, there are good prospects for attainment of benefits beyond those realized in the very conservative “no-removal” case. This is particularly true if fuel were to be blended-down for reuse in LWRs.

As this document is being written, serious consideration is reportedly being given in DOE to adoption of a (proliferation-resistant) separative reprocessing strategy coupled to future fast reactor deployment, motivated in large part by its benefits to high level waste disposal. (6-6) If this takes place, the incentives for the B&B concept, hence also CARDIO/AIROX processing, would be significantly reduced.

6.5 Chapter References

- 6-1 F. Gorle, W. Timmermans, "Preparation of Uranium Carbide: I, by Carbothermic Reduction of Oxide," *Ceramic Bulletin*, Vol. 53, No. 3, 234-236 (1974)
- 6-2 S. K. Mukerjee, J. V. Dehadraya, V. N. Vaidya, D. D. Sood, "Kinetics and Mechanism of $\text{UO}_2 + \text{C}$ Reaction for UC/UC₂ Preparation," *Journal of Nuclear Materials* 210, 107-114 (1994)
- 6-3 B. Raj, "The Core of Stage Two," *Nuclear Engineering International*, Vol. 50, No. 614, Sept. 2005
- 6-4 J. Plaue, K. R. Czerwinski, "Interim Report on Task 3: Fuel Material Interactions," MIT-GFR-011, March 2004
- 6-5 J. Plaue, K. R. Czerwinski, "Evaluation of Uranium Carbide and Sulfide Fuels for a Gas-Cooled Fast Reactor Utilizing Dry Reprocessing," MIT-GFR-007, Oct. 2003
- 6-6 S. Davis, "Administration Eyes Shift in Nuclear Waste Policy toward Reprocessing," *Energy Washington Week*, Vol. 2, No. 43, Oct. 26, 2005

Chapter 7

Recommendations for Future Work

There are no compelling reasons apparent for near term installation of a B&B core in a GFR. Hence there is time to further develop this concept. Attractive improvements could be made in several areas, as follows:

- (1) Increase core exit temperature to $\geq 700^{\circ}\text{C}$ to permit use of a direct helium Brayton cycle at an efficiency of $\sim 40\%$. This would require changing over to a ceramic tubing such as SiC, together with ceramic grid plates, in the TID assembly (7-1). This would result in a significant plant capital cost savings compared to an indirect Rankine cycle. He Brayton cycles optimize out with core ΔT values in the range $350\text{--}400^{\circ}\text{C}$: about the same as for the Rankine, and significantly larger than the S-CO₂ at $\sim 150^{\circ}\text{C}$. Thus the core pressure drop constraint would be alleviated.
- (2) Increase core power density, hence fuel specific power. Again ceramic tubing would permit this by allowing a larger gas film temperature rise between clad and coolant. This will improve fuel cycle economics, as evident from the discussion in Chapter 2.
- (3) Decrease reload fuel enrichment, which will decrease C in Eq 2.2 of Chapter 2. Early proponents of the B & B concept even contemplated starting with natural or depleted uranium. Even if feasible neutronically, this will increase peak to average assembly power and require a longer fuel residence time to breed the fuel up to a positive reactivity contribution, after which it can help sustain fresh reload fuel. Going this far is probably impractical, but reducing initial enrichment to 3 or 4% may be worthwhile. Another variation on this theme would be to reload fuel slightly enriched with LWR TRU.
- (4) Work should be done to find a way to make UC fuel compatible with CO₂, which would permit use of a direct supercritical CO₂ Brayton power conversion system. For example, Ref. (7-1) shows that CVD deposited SiC is only slowly oxidized by CO₂, even at $1200\text{--}1400^{\circ}\text{C}$. The S-CO₂ PCS is also less sensitive to high core pressure drop than indirect cycles or those employing perfect gas working fluids. Since CO₂ can dissociate to form CO and O₂ (which are more corrosive than CO₂) due to n and γ -induced radiolysis, in-pile testing will be required to determine whether this is a viable approach.
- (5) The single most important development effort needed for the B&B core conceptualized in this project is refinement of the vented tube-in-duct assembly design and its associated plant systems. Extensive work was done on vented pin-type fuel in the GCFR programs of the 1970's, and vented fuel

was actually deployed in the Dounreay (sodium-cooled) fast reactor and in the (helium cooled) Peach Bottom 1 HTGR. Transfer of this technology to TID assemblies appears fairly straightforward, but a comprehensive analysis is essential. It is probably a valid generalization that vented fuel is greatly to be preferred for high-pressure, high-temperature, high-burnup GFRs, B & B or otherwise, unless a block-type self-supporting fuel assembly (e.g. cermet, cermet or metmet) can be developed.

- (6) Further development of the CARDIO process for fuel re-generation would be beneficial, especially if the fuel is to be recycled into LWRs. Special attention should be given to removal of the lanthanides, which dominate residual absorption. Furthermore, it should be noted that the CARDIO process has applications far beyond the B&B application addressed in this project.
- (7) More needs to be done with respect to tracking clad and fuel temperatures (particularly the former) during severe thermal-hydraulic transients such as LOCA, ATWS, LOF, station blackout and the like. Since orificing and clad roughening (if it were to be employed) optimized for full power steady-state turbulent forced convection will not be comparably optimal for off-normal conditions, this is an area of special interest. The B&B core is particularly vulnerable because of its large radial power peaking factor (~ 1.8) compared to conventional GFR cores (at ~ 1.2).
- (8) Overall it must be emphasized that developments which benefit conventional GFRs will also benefit the B&B version. One area worth emphasis is even higher reliability shutdown cooling. Conversely, further development of the B&B approach will directly benefit conventional GFR designs. Likewise GFR B&B work has considerable synergism with development of a lead alloy cooled B&B LMR.

A final observation is the potential need to re-assess the top-level motivation for B&B core development, whether gas or lead alloy cooled. This stems from the reported recent DOE interest in prioritizing the introduction of reprocessing and fast reactor development to ameliorate high level waste disposal impediments (7-1). If aggressively pursued, this strategy would significantly reduce the incentive for further work on the B&B concept.

Chapter Reference

(7-1) S. Davis, "Administration Eyes Shift in Nuclear Waste Policy toward Reprocessing," *Energy Washington Week*, Vol. 2, No. 43, Oct. 26, 2005

Appendix

Nomenclature

AGR	Advanced gas-cooled reactor (as employed in Britain)
AIROX	Atomics International Reduction Oxidation process
ANL	Argonne National Laboratory
ATWS	Anticipated transient without scram
B&B	Breen and burn, shorthand for the core concept which is the subject of this report
BGR	Russian for GBR
BOC	Beginning of cycle
BOEC	Beginning of equilibrium cycle
BOP	Balance of plant
BWR	Boiling water reactor
CARDIO	Carbon dioxide oxidation, fuel processing method
CEA	Commissariat a l'Energie Atomique (France)
CRA	Control rod assembly
DHR	Decay heat removal system
DOE	Department of Energy (U.S.)
ECS	Emergency cooling system
EFPY	Effective full power years
EIA	Energy Information Agency
ETGBR	Existing technology gas-cooled breeder reactor
FIMA	Fissions per initial metal atom
GA	General Atomics, developer of GCFR designs in the 1970-1980 time frame
GBR	Gas-cooled breeder reactor
GCFR	Gas-cooled fast reactor (usually applied to pre-1980 versions)
GCRA	Gas-cooled reactor associates
GEN IV	Generation Four
GFR	Gas-cooled fast reactor of modern vintage
GIF	GEN IV International Forum
HEN	Highly enriched nitrogen (in N-15)
HTGR	High temperature gas-cooled reactor
HX	Heat exchanger
INERI	International Nuclear Energy Research Initiative
INL	Idaho National Laboratory (formerly INEL, INEEL)
IHX	Intermediate heat exchanger
JNC	Japan Nuclear Cycle Development Institute
LMFBR	Liquid metal (Na) cooled fast breeder reactor (1960-1999 time frame)
LMR	Liquid metal cooled reactor
LOCA	Loss of coolant accident
LOF	Loss of flow
LWR	Light water reactor: pressurized or boiling (i.e. PWR, BWR)
MIT	Massachusetts Institute of Technology

MWD	Megawatt day
NRC	Nuclear Regulatory Commission
O&M	Operating and maintenance
ODS	Oxide dispersion strengthened steel
OECD	Organization for Economic Cooperation and Development
PCIV	Prestressed cast iron reactor vessel
PCRv	Prestressed concrete reactor vessel
PCS	Power conversion system
PRA	Probabilistic risk assessment
PSA	Probabilistic safety assessment
PWR	Pressurized water reactor
RERTR	Reduced enrichment for research and test reactors, fuel development program
SCS	Shutdown cooling system
SWU	Separative work unit
THTR	Thorium high temperature reactor
TID	Tube in duct: fuel assembly using an inverted unit cell: fuel surrounding coolant tube
VHTR	Very high temperature reactor
VIPAC	Vibrationally compacted particle bed
VVER	Russian PWR

Task Flowchart

Milestone Status Table
As of Project Completion, 9/14/05

Task ID No.	Description	Planned Completion	Actual Completion	Comments
1.1	<ul style="list-style-type: none"> Vet Core Constituents & Downselect Study Leading Candidates 	9/30/03 9/30/04	9/30/03 9/30/04	UC and UN-15 are focus
1.3	<ul style="list-style-type: none"> AIROX Thermodynamics Fission Product Removal Experiments 	7/31/03 9/30/04	7/31/03 9/14/05	Lab Transferred to UNLV; Work Reprogrammed
1.4	<ul style="list-style-type: none"> AIROX Studies Select Ref. Mode 	9/30/03 9/30/04	9/30/03 9/30/04	GFR spent fuel is suitable PWR reload: as UO ₂ in LWR as UC in GFR
2.1	<ul style="list-style-type: none"> T-H Scoping Studies Downselect Coolant 	9/30/03 9/30/04	9/30/03 9/30/04	} Indirect, He Primary
2.2	<ul style="list-style-type: none"> Post-LOCA Cooling Evaluations Pick Best Approach 	9/30/03 9/30/04	9/30/03 9/30/04	} Active Loops With Passive Capability
3.1	<ul style="list-style-type: none"> Review Power Cycles Downselect Final Design 	6/30/04 6/30/05	6/30/04 12/31/04	} H ₂ O Rankine
3.2	<ul style="list-style-type: none"> Decay Heat Removal System Definition 	9/30/04	9/30/04	} Multiloop, Active
3.3	<ul style="list-style-type: none"> Plant and Overall Cost Assessment 	9/14/05	9/14/05	Using GCRA MHTGR Basis
4.1	<ul style="list-style-type: none"> Identify Materials Focus on Most Promising 	9/30/03 9/30/04	9/30/03 9/30/04	} UC, UN-15, ODS
4.2	<ul style="list-style-type: none"> Coordinate on Config. Choice Focus on Most Promising 	9/30/03 9/30/04	9/30/03 9/30/04	} Pin & Tube-in-Duct Vented

DRISCOLL NERI CONTRACT DE-FG07-02SF22608
MIT QUARTERLY FINANCIAL REPORT -- FINAL

Financial Performance:

Quarterly Expenditure Summary:

	03/04				04/05				05				
	Sept	Dec	Mar	June	Sept	Dec	Mar	June	Sept	Dec	Mar	June	Sept
Monthly Cost													
- Plan	25	73	72.97	73	73	73	73	72.97	72.97	72.97	73	73	73
- Actual	6.8	79.6	96.4	62.8	39	76.2	49.2	64.6	46.9	86.1	72.8	98.3	120.8
Cumulative													
- Plan	25	98	171	244	317	390	463	536	609	682	755	828	900
- Actual	6.8	86.4	182.8	245.6	284.6	361	410.2	474.9	521.8	607.9	680.7	779	900

Note: all numbers are in thousands of dollars.

Totals include subcontract to UNLV (K. Czerwinski) for total of 92,555

INL Effort (Excluding Former ANL-West)

	OCT	NOV	DEC	JAN	FEB	MAR	APR	MAY	JUN	JUL	AUG	SEP
Budget	16,274	23,313	19,103	29,417	14,521	21,057	32,556	16,233	27,485	24,770	20,719	7,580
Actuals	11,725	10,719	9,163	10,630	12,068	12,377	21,509	12,395	31,679	37,631	38,294	33,420

Cum												
Budget	16,274	39,587	58,690	88,107	102,628	123,685	156,241	172,474	199,959	224,729	245,448	253,028
Actuals	11,725	22,444	31,607	42,237	54,305	66,682	88,191	100,586	132,265	169,896	208,190	241,610

Breed and Burn Fast Reactor System - MFC Cumulative Expenditures

INL/BEA Final Year Spending Summary

Spending	Actual	Planned
Oct-04		
November		
December		
January		
February	\$2,966	\$0
March	\$4,317	\$9,323
April	\$4,049	\$20,242
May	\$2,247	\$10,093
June	\$9,743	\$15,425
July	\$20,977	\$15,401
August	(\$12,392)	\$12,882
September	\$3,489	\$4,714
Total FY 2005	\$35,396	\$88,080
Oct-05	\$31,341	\$0
Nov-05	\$0	\$0
Dec-05	\$19,780	\$0
Total FY 2006	\$51,121	\$0
Total	\$86,517	\$88,080

September through December actuals reflect obligations that were placed before the contract ended that are still pending.

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