GA-10298
Amendment 9

MASTER

**GAS-COOLED FAST BREEDER REACTOR
PRELIMINARY SAFETY INFORMATION DOCUMENT
AMENDMENT 9**

GCFR FUEL CLADDING PC-5 (FAULTED) TEMPERATURE LIMIT

**Prepared under
Contract DE-AT03-76SF71023
for the San Francisco Operations Office
Department of Energy**

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GCFR FUEL CLADDING PC-5 (FAULTED) TEMPERATURE LIMIT

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1. INTRODUCTION AND OBJECTIVE

1.1. INTRODUCTION

The "Gas-Cooled Fast Breeder Reactor Preliminary Safety Information Document (PSID)," Ref. 1-1, provided a basis for a preapplication safety evaluation of the gas-cooled fast breeder reactor (GCFR) demonstration plant by the U.S. Atomic Energy Commission (USAEC), Directorate of Licensing (DOL) and the Advisory Committee on Reactor Safeguards (ACRS). During the period from 1971 through 1974, a series of technical exchanges took place on various aspects of the PSID with the USAEC, DOL and the ACRS. The purpose of these exchanges was to acquaint the USAEC [now the U.S. Nuclear Regulatory Commission (NRC)] with the safety-related design bases and design features of the GCFR and to obtain assurance about their suitability and adequacy for eventual plant licensing. The exchanges took the form of questions following meetings or reviews of the General Atomic Company (GA) submittals. The preapplication review resulted in a "Preapplication Safety Evaluation of the Gas-Cooled Fast Breeder Reactor" (Ref. 1-2). Responses to safety concerns contained in the preliminary safety evaluation have been issued in a series of supplements and amendments to the PSID. The purpose of this amendment is to address the NRC concerns about the cladding faulted condition temperature limit which were cited in Section 4.4.1 of the preliminary safety evaluation.

For safety evaluations, the spectrum of credible plant operating conditions is identified in accordance with the anticipated frequencies of occurrence (Ref. 1-3). These are divided into five categories, Plant Conditions 1 through 5. The safety objectives in relating design requirements to each plant condition are the following:

1. The most probable occurrences shall be accommodated by the largest design margin and yield the least radiological risk to the public.

2. Those extreme situations having the potential for the greatest risk to the public shall be those least likely to occur.

The approximate relationship of these plant conditions to existing NRC and other classification systems is shown in Table 1-1. Plant Condition 5 (PC-5) is commonly referred to as the "faulted condition." It corresponds to an expected best estimate frequency of occurrence for the initiating event per reactor year of 10^{-4} to 10^{-6} . "PC-5 shall not preclude achieving and maintaining a safe shutdown and cooldown of the reactor and shall be accommodated without loss of safety function to safety systems required to mitigate the consequences of the event and without exceeding the reactor safety limits" (Ref. 1-3).

The clad faulted temperature limit selected for the design basis depressurization accident (DBDA) in the PSID was 1370°C (2500°F), near the stainless steel melting temperature. The response of the Division of Reactor Licensing was that the melting temperature was unacceptable as the faulted temperature limit, and they predicted that the acceptable limit would come in line with the 1200°C (2200°F) limit being used for light-water reactors (LWRs) with zirconium alloy cladding. Since the GCFR clad oxidation and ballooning conditions are much less severe than in an LWR, 1290°C (2350°F) was selected and presented to the ACRS. The ACRS neither accepted nor rejected 1290°C (2350°F), but they implied that a verification program would be required to substantiate the cladding limit for the GCFR faulted event (Ref. 1-10).

10CFR50.46 (Ref. 1-4) specifies the 1200°C (2200°F) temperature limit for zirconium alloy cladding in LWRs. This limit was based on metal-water reactions, rupture strength, and thermal margins. Similar criteria were used to establish a 1260°C (2300°F) limit for stainless steel cladding in LWRs. This amendment reviews the physical parameters affecting the integrity of the GCFR fuel and blanket assemblies, resulting in selection of a 1300°C (2370°F) faulted condition temperature limit for GCFRs with stainless steel cladding. The verification test programs are discussed. The features of

TABLE 1-1
COMPARISON OF EVENT CLASSIFICATION SYSTEMS^(a)

Event Frequency Range (per year)	ANS-50 ^(b) Policy 2.4 (Ref. 1-3)	Existing Terminology					
		NRC			ANS ^(b)		
		10CFR (Ref. 1-4)	RG 1.48 (Ref. 1-5)	RG 1.70 Rev. 3 (Ref. 1-6)	N18.2 (Ref. 1-7)	N212 (Ref. 1-8)	N213 (Ref. 1-9)
Planned Operations	PC-1	Normal	Normal	Normal	Condition I	Normal PPC ^(c)	Plant Condition A
10 ⁻¹	PC-2	Anticipated operational occurrences	Upset	Moderate frequency	Condition II	Frequent PPC ^(c)	Plant Condition B
	PC-3	-----	-----	Infrequent incidents	Condition III		-----
10 ⁻²						Infrequent PPC ^(c)	Plant Condition C
10 ⁻³	PC-4		Emergency			-----	-----
10 ⁻⁴		Accidents	-----				
10 ⁻⁵	PC-5			Limiting faults	Condition IV	Limiting PPC ^(c)	Plant Condition D
10 ⁻⁶			Faulted				
	Not considered						

(a) Boundaries shown by dashed lines are inferred.

(b) American Nuclear Society.

(c) PPC = plant process condition.

the evaluation models for the cooling system/core thermal-hydraulic analyses will be discussed in PSID Amendment 10, "GCFR Residual Heat Removal System," which will be submitted to the NRC for preapplication review later in 1980.

1.2. AMENDMENT OBJECTIVE

The objective of this amendment is to obtain NRC concurrence that the verification test programs are adequate to verify a GCFR fuel and blanket cladding temperature limit of 1300°C (2370°F) for PC-5 (faulted condition) events. NRC approval of this temperature limit, conditioned by successful completion of the supporting verification testing program described herein, is requested.

REFERENCES

- 1-1. "Gas-Cooled Fast Breeder Reactor Preliminary Safety Information Document (PSID)," General Atomic Report GA-10298, February 15, 1971.
- 1-2. "Preapplication Safety Evaluation of the Gas Cooled Fast Breeder Reactor," U.S. Atomic Energy Commission, Directorate of Licensing Report, August 1, 1974.
- 1-3. "Plant Design Conditions for Nuclear Power Generating Stations," American Nuclear Society ANS-50, Policy 2.4, June 1978.
- 1-4. "General Design Criteria for Nuclear Power Plants" in Code of Federal Regulations, Title 10, Part 50.46, Appendix A, U.S. Government Printing Office, Washington, D. C., 1977.
- 1-5. "Design Limits and Loading Combinations for Seismic Category I Fluid System Components," Nuclear Regulatory Commission Regulatory Guide RG 1.48, May 1973.
- 1-6. "Standard Format and Content of Safety Analysis Reports for Nuclear Power Plants," Nuclear Regulatory Commission Regulatory Guide RG 1.70, Revision 3, November 1978.
- 1-7. "Nuclear Safety Criteria for the Design of Stationary Pressurized Water Reactor Plants," American National Standards Institute ANS N18.2, American Nuclear Society, 1973.

- 1-8. "Nuclear Safety Criteria for the Design of Stationary Boiling Water Reactor Plants," American National Standards Institute ANS N212, American Nuclear Society, 1974.
- 1-9. "Nuclear Safety Criteria for the Design of Stationary Gas Cooled Reactor Plants," American National Standards Institute ANS N213, American Nuclear Society, 1974.
- 1-10. Stratton, W. R. (chairman, Advisory Committee of Reactor Safeguards), "Conceptual Design for Prototype Gas-Cooled Fast Breeder Reactor GCFBR," correspondence to Dixy Lee Ray (chairman, U.S. Atomic Energy Commission), November 8, 1974.

2. GCFR DESIGN AND LIMITING FAULTED EVENTS

Section 2 is presented as background information only. NRC review of Section 2 is not requested.

2.1. DESIGN DESCRIPTION

There have been many design developments and improvements to the GCFR demonstration plant since the last submittal to the NRC on the GCFR PSID. The most notable of these is the change of the helium coolant flow direction through the core from downflow to upflow cooling. The principal consideration motivating the change to upflow cooling was the enhancement of coolant natural circulation capability for residual heat removal (RHR). The change in flow direction has had a significant effect on the nuclear steam supply system (NSSS) general arrangement, such as the core support concept, relative location of components, and shielding configuration.

The RHR characteristics of the revised configuration will be the subject of PSID Amendment 10. Amendment 10 will also include up-to-date detail descriptions of the NSSS, the cooling systems, the reactor core, and all other associated systems and components. For the purposes of the amendment contained herein, general arrangement information is provided on the nuclear island, and a summary description of the NSSS is given. Detail design information is provided on the reactor core illustrative of a GCFR demonstration plant upflow core design in the 300- to 400-MW(e) range. The final conceptual design of the GCFR demonstration plant upflow core design, to be included in Amendment 10, is expected to be essentially the same as the design described herein. Some design parameters may be slightly different than given here, but no changes are envisioned that would have an impact on the generic faulted temperature limit considerations or on the associated verification program plans.

2.1.1. Nuclear Island General Arrangement

The general arrangement of the GCFR nuclear island is shown in Fig. 2-1. The nuclear island is housed in a reactor containment building, a reactor service building, a control and diesel building, and a turbine-generator building. The reactor containment building contains a prestressed concrete reactor vessel (PCRv) enclosing the NSSS. A fuel storage pool is located in the fuel service building adjacent to the reactor containment.

2.1.2. Nuclear Steam Supply System (NSSS)

The reactor and its associated reactor coolant circuit components are shown in Fig. 2-2, and typical system parameters are given in Table 2-1. The entire primary coolant system is contained within the PCRv, which is a multicavity pressure vessel reinforced with steel rods and prestressed by a system of longitudinal tendons and circumferential wire wrappings. The reactor core is located in the central cavity; peripheral cavities surrounding the central cavity contain heat exchanger and helium circulation equipment. The peripheral cavities are interconnected with the central cavity by cross ducts. All PCRv interior surfaces are lined with leak-tight steel liners which contain the primary coolant. These, in turn, are lined with thermal barriers to protect the PCRv from the high temperatures of the helium coolant. The limited quantity of heat that passes through the thermal barrier is removed by a liner cooling water system embedded in the PCRv.

The 750°C (1380°F) clad temperature for normal operation (shown in Table 2-1) occurs after completion of a fuel upgrade strategy. The plant will operate with a maximum clad temperature of 700°C (1290°F) for the first five years. In addition to the first three regular annual refuelings, fuel assemblies will be removed at 6-month intervals for inspection. Next, selected assemblies will be performance tested at 750°C. Satisfactory performance will permit raising the maximum clad temperature to 725°C (1337°F) during the fifth and sixth years and finally to 750°C in the seventh year.

Fig. 2-1. GCFR demonstration plant, general arrangement, section view

Fig. 2-2. GCFR demonstration plant NSSS

TABLE 2-1
GCFR DEMONSTRATION PLANT TYPICAL SYSTEM PARAMETERS

Reactor thermal power	1150 MW(t)
Gross electrical power	370 MW(e)
PCRv operating pressure	10 MPa (1450 psia)
Core pressure drop	180 kPa (26 psi)
Core temperature	
Inlet	300°C (575°F)
Outlet	550°C (1020°F)
Maximum clad temperature ^(a)	750°C (1380°F)
Coolant flow rate	890 kg/s (1962 lb/s)

(a) Normal operation after completion of the fuel up-grade strategy. Initial operating temperature is 700°C (1290°F).

The reactor coolant system consists of three main loops, each with an independent steam generator, a horizontally mounted electric-motor-driven circulator, and a gravity closing isolation valve. Three core auxiliary cooling loops are also provided, each having a vertically mounted electrically driven circulator, heat exchanger, and gravity opening isolation valve.

A schematic of the main loop cooling system (MLCS) is shown in Fig. 2-3. During normal power operation, the high-pressure helium is pumped through the reactor core by the main helium circulators located at the bottom of the steam generator cavities. The helium coolant flows upward through the core and then across through the upper cross ducts to the steam generator cavities. It flows downward through helical-tube bundle steam generators and continues down to the circulator inlets.

Post-shutdown heat removal from the core is normally provided by the MLCS. In addition, two safety class systems are provided for long-term RHR. These systems are the shutdown cooling system (SCS) and the core auxiliary cooling system (CACS). Each of the systems is seismic category I and is independent from the other.

Each loop of the SCS consists of a main cooling loop steam generator, a pony motor on the main helium circulator, a water-to-air cooling system for rejecting heat to the atmosphere, and a feedwater pump.

Each loop of the CACS consists of a circulator driven by an electric motor, a shutoff valve, and a helium-to-water heat exchanger, all contained in a cavity inside the PCRV. A cooling-water-supply system external to the reactor containment provides pressurized cooling water to the core auxiliary heat exchanger (CAHE) and rejects the heat to an external heat sink. Diesel generators ensure an electrical supply to the auxiliary circulator motor and associated pumps and fans. Operation of the CACS is shown schematically in Fig. 2-4. This system is automatically activated following reactor trip when neither the MLCS nor the SCS are capable of providing adequate core cooling.

Fig. 2-3. MLCS operating conditions

Fig. 2-4. GCFR CACS

As a backup to the three forced-convection RHR systems, the CACS design of the GCFR demonstration plant incorporates a natural circulation capability to allow for an inherently passive cooling of the core after shutdown. Component elevations to assure circulation capability are shown schematically in Fig. 2-5.

2.1.3. Reactor Core Cavity Arrangement

The general arrangement of the reactor core cavity is shown in Fig. 2-6. The active core is located in the lower portion of the cavity, supported by a bottom-mounted grid plate in the lower inlet coolant plenum. The grid plate is supported by a skirt attached to a thermal sleeve anchored to the PCRV cavity wall. The grid plate and support skirt are designed to be fabricated from Type 316 stainless steel.

The core cavity closure is provided by a removable plug located at the top of the cavity. The control and shutdown drive mechanisms are located in penetrations provided in the closure plug. Control-rod-guide structures and instrument trees are provided between the closure plug and the top of the reactor core.

Upper plenum, radial, and lower plenum shielding is provided in the core cavity to protect the reactor internals and the PCRV liner from excessive irradiation. The upper and lower plenum shielding has flow through passages for the helium coolant to enter the lower plenum and exit the upper plenum.

2.1.4. Reactor Core

The general configuration of the reactor core is illustrated in Fig. 2-7, and major design parameters are given in Table 2-2. The active core region consists of 169 hexagonal assemblies; 150 of these are fuel assemblies, 15 are control assemblies, and 4 are secondary shutdown assemblies.

Fig. 2-5. Containment building

Fig. 2-6. GCFR upflow configuration with flowthrough shielding

Fig. 2-7. GCFR upflow core, demonstration plant, general configuration

TABLE 2-2
GCFR CORE DESIGN CHARACTERISTICS

No. of fuel assemblies	150
No. of control assemblies	15
No. of shutdown assemblies	4
No. of radial blanket assemblies	162
No. of reflector/shield assemblies	138
Total No. of core assemblies	469
Assembly cross section	Hexagonal
Assembly length	5000 mm
Assembly across flats o.d.	194.0 mm
Interassembly gap	7.0 mm
Assembly pitch	201.0 mm
Load pad gap	0.8 mm
No. of fuel rods/fuel assembly	265
Total No. of fuel rods/core	39750
Active fuel length	1200 mm
Upper axial blanket length	600 mm
Lower axial blanket length	600 mm
No. of blanket rods/radial blanket assembly	61
No. of blanket rods/core	9882
Radial blanket length	2400 mm
No. of control pins/control assembly	55
No. of control pins/core	825
Control rod length	1300 mm
Control material	B ₄ C
Control material length	1200 mm
No. of shutdown pins/shutdown assembly	55
No. of shutdown pins/core	220
Shutdown rod length	1300 mm
Shutdown material	B ₄ C
Shutdown material length	1200 mm

The central core region is surrounded by a radial blanket region of fertile material. The radial blanket consists of 162 hexagonal blanket assemblies arranged in three concentric rows around the active core. Fertile blanket material is also included in the fuel assemblies, above and below the active core (upper and lower axial blankets). The radial blanket is surrounded by 138 hexagonal reflector/shield assemblies arranged in two concentric rows.

The core assemblies are axially supported at their lower ends by the bottom-mounted grid support plate. The assemblies are supported laterally by a core restraint mounted on the grid plate, which consists of a cylindrical support barrel and core formers surrounding the core. The core restraint is designed to provide lateral support at two elevations: one at the top of the active core and the other at the top of the core assemblies. This core restraint system has been adopted from the technology developed in the liquid metal fast breeder reactor (LMFBR) program and is designed to minimize reactivity insertion due to core distortions.

The reflector/shield assemblies located outboard of the radial blanket are included to protect the core restraint. Shielding is provided within the core assemblies below the lower axial blanket to protect the core support grid plate. Shielding is also included within the core assemblies above the upper axial blanket region to protect the instrument trees and control rod guide structures located in the core exit plenum.

The control and shutdown rod assemblies are located on the centers of regions that consist of a central control (or shutdown) assembly surrounded by six fuel assemblies. Removal and replacement of assemblies during refueling is accomplished through the penetration in the closure plug provided for the drive mechanisms. Each seven-assembly region is serviced by one of the penetrations. In the regions outboard of the control and shutdown assembly locations, additional penetrations are provided in the closure plug to service the blanket and reflector/shield assemblies.

2.1.4.1. Fuel Assembly. The GCFR fuel assembly design is similar to designs employed in LMFBR programs, particularly the fuel rod design. The fuel rod design employs the same materials and has similar geometry and operating conditions as those being developed in the international LMFBR programs. The unique characteristics incorporated in the GCFR fuel assembly design include the following:

1. Roughened fuel rod cladding to enhance heat transfer to the helium coolant.
2. Pressure-equalized and vented fuel rods, which essentially eliminate any pressure-induced stresses on the cladding from either the primary coolant system pressure or from fission gases generated within the rods.
3. A large fuel rod pitch-to-diameter ratio relative to that commonly employed in the LMFBR designs.

The design of the fuel assembly is shown in Fig. 2-8, and design configuration parameters are given in Table 2-3. The fuel assembly is hexagonally shaped, 194 mm across flats, and 5 m long. A cylindrical inlet nozzle, 172 mm diameter by ~675 mm long, is located at the bottom end. The inlet nozzle fits into a mating hole in the core support grid plate. The assembly rests on the conical surface which forms the transition between the cylindrical and hexagonal shapes.

Contained within the assembly is a 2870-mm-long fuel bundle consisting of 265 fuel rods. The fuel rods are 8 mm in diameter and are located on an 11-mm triangular pitch. The rods are fastened to a grid manifold at their lower ends and are laterally spaced by 12 spacer grids positioned at selected axial locations along the length of the rods. The spacer grids are retained in position by means of six hanger rods. The hanger rods are structural members containing no fuel material. A hexagonal flow duct is provided around the rod bundle for channeling the coolant flow through the rod bundle.

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Fig. 2-8. Fuel assembly, general arrangement

TABLE 2-3
FUEL ASSEMBLY CONFIGURATION PARAMETERS

Fuel Assembly

No. of fuel assemblies/core	150
Assembly length	5000 mm
Active core height	1200 mm
No. of fuel rods/assembly	265
No. of hanger rods	6
Fuel rod spacer type	Spacer grid
No. of spacer grids	12
Flow duct material	Type 316 stainless steel
Flow duct across flats	
I.d.	186.0 mm
O.d.	194.0 mm
Flow duct wall thickness	4.0 mm
Fuel rod pitch	11.1 mm
Rod-to-rod gap	3.1 mm
Rod-to-duct gap	2.48 mm 80% of rod-to-rod gap

Fuel Rod

Length	2870 mm
O.d.	8.00 mm
Root o.d.	7.74 mm
Av volumetric diameter in rough section	7.815 mm
I.d.	6.98 mm
Clad root thickness	0.38 mm
Clad material	Type 316 stainless steel
Fuel and axial blanket pellet o.d.	6.84 mm

TABLE 2-3 (Continued)

Fuel Rod (Continued)

Fuel and axial blanket pellet-to-clad diametral gap	0.14 mm
Fuel column length	1200 mm
Fuel material	(U,Pu)O ₂
Fuel material planar smear density	85.5% of theoretical
Axial blanket column length (each)	600 mm
Axial blanket material	Depleted UO ₂
Axial blanket material planar smear density	90.0% of theoretical

Roughness Geometry

Rib shape	Trapezoidal
Rib height	0.13 mm
Rib width	0.45 mm
Rib pitch	1.56 mm
Length of roughening	1220 mm

Grid plate shielding is provided beneath the rod bundle between the grid manifold and the grid plate to protect the grid plate from excessive irradiation. Additional grid plate shielding is provided within the fuel assembly inlet nozzle adjacent to the grid plate upper surface. Exit end shielding is provided above the rod bundle to prevent excessive irradiation of the instrument trees and control rod guide structures located in the reactor cavity upper plenum.

An exit end nozzle is provided on the top end of the assembly. The exit nozzle is used for handling the assembly and contains a fixed-area replaceable orifice. The orifice is designed to be changed at refueling outages.

Each fuel rod, Fig. 2-9, consists of a cladding tube, a fuel stack, upper and lower axial blankets, a holddown spring, a fission product trap, and end plugs. The cladding tube is roughened with trapezoidally shaped ribs over the fuel region to enhance heat transfer to the helium coolant. The fuel region consists of a 1200-mm-long stack of mixed oxide (U,Pu)O₂ cylindrical pellets with a fissile plutonium enrichment of ~20%. The upper and lower axial blankets are located above and below the fuel region. Each axial blanket consists of a 600-mm-long stack of depleted UO₂ cylindrical pellets.

A pellet holddown spring is located above the upper axial blanket stack to maintain proper positioning of the pellet stacks during handling and shipping prior to irradiation. Venting of the fuel rods is accomplished by means of a special lower end plug. The lower end plug contains an axial vent hole for passage of the fission gases out of the rod and is threaded for attaching and sealing the rod to the grid manifold. A charcoal fission product trap is assembled as an integral part of the lower end plug. The charcoal trap retains volatile fission products and delays to permit radioactive decay. Retaining screens are provided at the lower end of the trap to prevent charcoal particles or fuel fragments from passing into the end-plug vent hole.

Fig. 2-9. GCFR upflow core, fuel rod assembly

The rods are screwed into mating holes in the grid manifold where a metal-to-metal seal is made. The grid manifold structure is cross-drilled through the grid ligaments such that there are at least two flow paths from each rod. Redundant flow paths are also provided from the grid manifold to an assembly charcoal trap located in the assembly inlet nozzle below the grid plate shielding for further delay and decay. From the assembly fission product trap, the fission gases pass to two vent connections located at the transition between the hexagonal flow duct and the cylindrical inlet nozzle. The fission gases are swept from the vent connection to the reactor helium purification system for separation and storage.

A schematic of the pressure equalization system (PES) is shown in Fig. 2-10. A suction hole is provided at the PES vent connection such that a small quantity of coolant at core exit conditions sweeps the fission gases diffusing out of the fuel assembly on to the fission gas separator in the purification system. The driving head for the sweep gas is the pressure drop between the core exit and the circulator inlet. As the result of the suction hole being at reactor core exit pressure conditions, the pressure within the fuel rods is maintained at core exit conditions. The only pressure differential acting on the cladding is the core pressure drop of ~ 180 kPa (26 psi). In addition, any cladding breaches will result in coolant flow into the rod, which will limit the release of radioactivity to the coolant.

2.1.4.2. Radial Blanket Assembly. The general arrangement of the radial blanket assembly is shown in Fig. 2-11, and design configuration parameters are given in Table 2-4. The blanket assembly design is essentially the same as the fuel assembly, except that the blanket rod bundle consists of a smaller number of larger rods. Other than the rod bundle, the radial blanket assembly is designed to use the same major components as the fuel assembly. These major components include the inlet nozzle, grid plate shielding, assembly charcoal trap, exit shielding, flow duct, and exit nozzle. The fixed-area replaceable orifice would be specific to the blanket assembly and would contain mechanical discrimination characteristics to prevent a blanket orifice from being assembled on a fuel assembly.

Fig. 2-10. Schematic diagram of GCFR PES

Fig. 2-11. Blanket assembly, general arrangement

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TABLE 2-4
RADIAL BLANKET ASSEMBLY CONFIGURATION PARAMETERS

Radial Blanket Assembly

No. of radial blanket assemblies/core	162
No. of blanket rods/assembly	61
Assembly length	5000 mm
Blanket rod spacer type	Wire wrap
Flow duct material	Type 316 stainless steel
Flow duct across flats	
I.d.	186.0 mm
O.d.	194.0 mm
Duct wall thickness	4.0 mm
Blanket rod pitch	23.25 mm

Blanket Rod

Length	2880 mm
O.d.	21.70 mm
I.d.	20.70 mm
Clad material	Type 316 stainless steel
Wire wrap diameter	1.45 mm
Wire wrap pitch	300.0 mm
Blanket pellet o.d.	20.55 mm
Blanket column length	2400 mm
Blanket material	Depleted UO ₂
Blanket material smear density	90.0% of theoretical

The blanket rod bundle consists of 61 rods, 21.7 mm o.d. by 2880 mm long. Each rod consists of a cladding tube, a stack of depleted UO_2 cylindrical pellets, a pellet holddown spring at the top of the pellet stack, and end plugs. The blanket rods are pressure equalized and vented the same as the fuel rods except that, due to the low fission product yield, a fission product trap is not included in each blanket rod. An assembly trap is, however, included in the inlet nozzle region.

The blanket rods are arranged on a much smaller pitch-to-diameter ratio than the fuel rods. As a result, a wire-wrap spacer system, rather than spacer grids, as used in the fuel assembly design, is used to maintain the rod array.

2.1.4.3. Control and Shutdown Assemblies. The design of the control assembly is shown in Fig. 2-12. The shutdown assembly design is planned to be similar to the control assembly, except for poison material worth. The design configuration parameters of the control and shutdown assemblies are given in Table 2-5.

The control and shutdown assembly design has the same external configuration as the fuel and blanket assembly design, and many of the major components are the same or are slight modifications. The flow duct and inlet nozzle structures are the same as used in the fuel and blanket assemblies, and the grid plate shielding and exit nozzle design are slight modifications.

The control rod consists of a bundle of control pins contained within a guide duct having upper and lower end fittings. The composite of the guide duct, control pins, and end fittings is designed to transverse up and down in the flow duct, but it always remains wholly contained within the flow duct.

The control pin bundle consists of 55 pins, 16.8 mm o.d. by 1300 mm long. Each pin consists of a cladding tube, a stack of B_4C poison pellets,

Fig. 2-12. Control assembly general arrangement

TABLE 2-5
CONTROL AND SHUTDOWN ASSEMBLY CONFIGURATION PARAMETERS

Control and Shutdown Assemblies

No. of control and shutdown assemblies/core	19
Assembly length	5000 mm
Poison height	1200 mm
No. of hanger rods/assembly	6
Control/shutdown pin spacer type	Spacer grid
No. of spacer grids	5
Flow duct material	Type 316 stainless steel
Guide duct material	Type 316 stainless steel
Flow duct across flats	
I.d.	18.60 mm
O.d.	194.0 mm
Flow duct wall thickness	4.0 mm
Guide duct across flats	
I.d.	168.0 mm
O.d.	176.0 mm
Guide duct wall thickness	4.0 mm
Control/shutdown pin pitch	20.9 mm
Pin-to-pin gap	4.1 mm
Pin-to-channel gap	3.2 mm
	78% of rod-to-rod gap

Control and Shutdown Pins

No. per control/shutdown rod	55
Length	1300 mm
O.d.	16.8 mm
I.d.	15.8 mm
Clad thickness	0.5 mm
Pellet o.d.	15.5 mm

a pellet holddown spring, and end plugs. The control pins are attached to the upper end fitting and are laterally spaced by five spacer grids. Six tie rods are provided in the bundle hexagonal corners to maintain the axial spacing of the spacer grids. The control pins are vented directly to the helium coolant.

A lower end fitting is provided on the lower end of the guide duct to prevent any of the control pins from moving out of the poison cluster should a failure occur in the poison pin cladding or in the pin attachment to the upper end fitting. A backup stop/absorber is provided in the lower portion of the assembly beneath the poison cluster to prevent the cluster from moving below the active core zone should a failure occur anywhere in the withdrawal shafting in the region between the poison cluster and the primary stop, which comes to rest in the exit nozzle.

2.1.4.4. Reflector/Shield Assembly. The reflector/shield design configuration parameters are given in Table 2-6. The reflector/shield has the same external configuration as the balance of the core assemblies and is essentially identical to the radial blanket assembly, except that the reflector/shield assembly has a smaller number of larger rods and does not have a fission product trap in the inlet nozzle or grid-plate shielding.

The reflector/shield rod bundle consists of 19 rods, 40.1 mm o.d. by 3180 mm long. Each rod consists of a cladding tube, a boronated graphite column, and end plugs. The rods are attached to a grid at their lower end and are laterally spaced by wire wraps. The reflector/shield rods are vented directly to the helium coolant.

2.2. DESCRIPTION OF UNDERCOOLING TRANSIENTS

Several types of PC-5 events have been reviewed to determine which type results in the highest cladding temperature. For the GCFR, the undercooling events are limiting. Within this subset of undercooling events, the DBDA results in the highest clad temperature. The DBDA is described below. A

TABLE 2-6
REFLECTOR/SHIELD ASSEMBLY CONFIGURATION PARAMETERS

Reflector/Shield Assembly

No. of reflector/shield assemblies	162
Assembly length	5000 mm
No. of shielding rods/assembly	19
Shielding rod spacer type	Wire wrap
Flow duct across flats	
I.d.	186.0 mm
O.d.	194.0 mm
Duct wall thickness	4.0 mm
Shielding rod pitch	41.35 mm

Shielding Rod

Length	3180 mm
O.d.	40.1 mm
I.d.	39.1 mm
Clad material	Type 316 stainless steel
Wire-wrap diameter	1.20 mm
Wire-wrap pitch	600 mm
Shielding material	Boronated graphite
Shielding pellet o.d.	39.0 mm

complete loss of all forced helium circulation (LOFC) is also described to show the depth of protection provided by the GCFR natural circulation cooling capability. An LOFC is beyond PC-5 (probability less than 10^{-6} /yr) and, therefore, is not a design basis event.

The clad temperature limit has been selected based on the worst undercooling event, the DBDA. For undercooling events, keeping the clad temperature below 1300°C (2370°F) fully satisfies safety criteria for the reactor core. For overpower events, the 1300°C limit is still valid, but a clad temperature limit alone may not be sufficient. Additional limits, such as a fuel temperature limit, might have to be satisfied.

2.2.1. Design Basis Depressurization Accident (DBDA)

The DBDA in the GCFR is based on a failure of the primary coolant system boundary, resulting in a blowdown of helium to the containment. Under DBDA conditions, the reactor coolant pressure decreases exponentially with time and becomes equal to the containment pressure in ~1.5 to 3 min. Soon after initiation of a DBDA, a reactor trip is actuated. The plant protection system identifies this condition, and the CACS is initiated to perform the RHR function. The off-site power (or power from emergency diesel generators if necessary) is used to bring the CACS equipment into a standby condition. A depressurization accident is detected by a signal resulting from the primary coolant pressure decrease, and auxiliary circulators are accelerated to their full design speed.

At the end of depressurization blowdown, air ingress is postulated from the containment atmosphere into the primary coolant loop through the leak passage by means of thermally induced inhalation, natural circulation, and molecular diffusion. CACS startup is assumed to be delayed until the end of depressurization; this is conservative, because less air inhalation occurs when the CACS starts earlier and the reactor pressure is higher. Air ingress makes the coolant a better heat-transfer and heat-transport medium for a given volumetric flow. However, since the CACS motor has both

maximum torque and maximum speed limitations, air ingress has a small effect on the overall core cooling capability with the CACS operating.

The fuel cladding reaches its maximum temperature ~3 to 5 min after initiation of the DBDA and remains above normal operating temperature for several minutes. The blanket cladding reaches its maximum temperature after ~20 to 25 min. Air in-leakage after the DBDA is sufficient to support some oxidation of the cladding.

2.2.2. Loss of Forced Circulation (LOFC)

LOFC is beyond PC-5 (faulted), and therefore, is not a design basis event. It is discussed here to show the depth of protection in the GCFR design.

Three independent means of forced circulation cooling, which are listed in order of intended use, are available to the reactor core: (1) MLCS, (2) SCS, and (3) CACS. A brief description of the cooling systems was provided in Section 2.1.2.

By the design intent of the GCFR demonstration plant, the RHR requirements are fully satisfied with the above three forced circulation systems. LOFC is considered to be incredible. Nevertheless, the plant is designed for natural circulation cooling sufficient to keep fuel and radial blanket cladding temperatures below the PC-5 temperature limit.

The PCRV is arranged so that the elevation difference between the core and the CAHE assures adequate natural circulation. The loop isolation valves of the MLCS and the CACS are gravity actuated. During normal operation, the pressure head of the main helium circulator keeps the main loop isolation valves open and the auxiliary loop isolation valves closed. At the end of main helium circulator coastdown, the main loop isolation valves are closed, and the auxiliary loop isolation valves are opened by gravity. Following the change of the valve state, the core can be cooled using the auxiliary loops under either forced or natural circulation modes.

During a LOFC, natural circulation flow maintains the fuel and blanket hot-spot cladding temperatures well below those that occur during a DBDA. The DBDA is, consequently, the most limiting undercooling event. Therefore, the conditions that occur during a DBDA are those that have been primarily utilized for selection of the PC-5 (faulted) limit and establishment of the verification programs.

3. SELECTION OF PC-5 TEMPERATURE LIMIT

In the analysis of transients or postulated accidents for nuclear reactors, the difference between the maximum cladding temperature reached during the transient and the temperature threshold for fuel assembly damage is an important measure for the safety margin provided by the engineered safety systems. Conservative assumptions incorporated into the evaluation models for the simulation of the system response to various initiating events tend to overestimate the cladding temperatures in the core: conservatisms are included in the calculation of the fission and decay heat generation rates, the power distribution, heat transfer correlations, coolant flow and flow distribution, material properties, the simulation of the initiating event, and the accident progression. Also, 3σ hot-spot factors are applied to the calculations to provide a high degree of confidence that the temperature limit is not exceeded.

To obtain a clear assessment of the safety margins available in a reactor design, a realistic value for the PC-5 (faulted) temperature limit of the fuel assembly should be used. As long as this temperature limit is not exceeded, the core remains within design cooling geometry. Some cladding breach is permissible as long as 10CFR100 limits (Ref. 3-1) are not exceeded. The purpose for designing to a PC-5 temperature limit is to assure sufficient clad strength to keep the fuel assembly within design geometry for accident conditions. Thus, clad melting and accident propagation are precluded.

3.1. EXPERIENCE FROM THERMAL AND FAST REACTORS

Design considerations and limiting parameters for several other reactor systems with stainless-steel-clad fuel have been reviewed. Some LWRs, as well as thermal gas-cooled reactors, utilize stainless steel as structural

and cladding material. All fast breeder reactors (FBRs) are designed with stainless steel tubes as cladding for the fuel rods. A summary of the phenomena that limit the maximum temperature of the fuel cladding in these reactors and the rationale for the selection of PC-5 limits is given in the following sections.

3.1.1. Light-Water Reactor (LWR)

The limiting conditions for the fuel rods in water-cooled power reactors occur during a large loss-of-coolant accident (LOCA). Because of the high saturation pressure, the coolant is ejected rapidly, and the core is uncovered within about 10 s. During the subsequent core heatup, the cladding is exposed to steam at high temperature. Depending on the transient conditions and the cladding material, the exothermic metal-steam reactions can add substantially to heat generation rates in the core, and thus, raise the maximum temperature of the cladding. Quenching of the pressurized fuel rods in the reflood phase causes additional stresses in the cladding.

The temperature limit selected for zirconium alloy cladding following the design basis accident (large LOCA) is 1204°C (2200°F) (Ref. 3-2). Absorption of oxygen in the zirconium alloy cladding (maximum solubility ~30%) and the consequent embrittlement are the limiting phenomena. The very large heat of reaction between steam and zirconium could start an autocatalytic reaction at high temperatures.

For LWR fuel assemblies with stainless steel cladding, metal-water reaction, and oxidation embrittlement are not the limiting phenomena, since the solubility of oxygen in iron or nickel is very small compared to that of zirconium. The main concerns during a LOCA are the cladding strength and a partial flow blockage due to ballooning of the cladding tube under internal overpressure. Although the maximum cladding temperature during a large LOCA is closer to the melting point of stainless steel than that of zirconium alloy, the strength of stainless steel in this temperature range is

substantially higher than the strength of zirconium alloy, and there is less potential for autocatalytic reactions. The potential for rupture of stainless-steel cladding tubes at elevated temperatures is, therefore, much smaller than that of zirconium alloy tubes. Due to the small rupture strain of stainless steel, the resulting flow blockage after rupture of stainless steel cladding is less than 40% of the free-flow area.

Based on an empirical correlation between cladding rupture temperature and internal overpressure of the cladding tube, a cladding temperature limit of 1260°C (2300°F) has been recommended for LWRs using stainless-steel cladding (Ref. 3-3).

3.1.2. Advanced Gas-Cooled Reactor (AGR) and Gas Breeder Reactor 4 (GBR-4)

Stainless steel with a relatively high nickel and chromium content (25% nickel, 20% chromium) is used as cladding material in the British Advanced Gas-Cooled Reactor (AGR). The fuel is not vented, resulting in a fission gas overpressure after prolonged operation. At full reactor coolant system pressure, however, the fission gas pressure is too low to cause substantial stresses in the cladding wall. An "ultimate" clad temperature limit of 1350°C (2462°F) has been selected if the clad is breached (Ref. 3-4). This is about 40°C (72°F) below the melting point of the cladding material. The limit is based on a need for sufficient strength to retain the basic core geometry.

The safety limit selected for the Type 316 L stainless-steel-clad Gas Breeder Reactor 4 (GBR-4) fuel is 1300°C (2372°F) for short-term hot spots during low flow transients. The design incorporates vented fuel pins and low oxidizing coolant. Therefore, the 1300°C limit was selected to assure nominal strength prior to clad melting (Ref. 3-5).

3.1.3. Liquid Metal Cooled Fast Breeder Reactor (LMFBR)

A critical threshold in LMFBR safety analysis is the saturation temperature of the coolant. If local temperatures exceed the saturation

point, sodium boiling can occur. Depending on the location of sodium void formation, the effect of neutron leakage or a hardened neutron spectrum can cause a negative or positive change of the reactivity. Simultaneously, the heat removal from the fuel rods is reduced. Sodium boiling by itself does not affect the integrity of the fuel assembly.

A fuel damage limit for extremely unlikely events (i.e., PC-5 events), is defined in the preliminary safety analysis report (PSAR) of the Clinch River Breeder Reactor (CRBR). Below this temperature limit, the geometry of the core remains coolable, preventing fuel damage propagation in the core. This implies that the flow resistance in the coolant channels does not increase substantially as a consequence of the core heatup. Little distortion of the fuel rods and no relocation of cladding material are expected below the melting point of stainless steel. Based on these considerations, a temperature of 1370°C (2500°F) has been selected as the limit for the fuel assembly following extremely unlikely events in CRBR, although the effective limit is lower due to sodium boiling (Ref. 3-6).

3.1.4. Summary of PC-5 Temperature Limits

See Table 3-1.

3.2. GCFR FAILURE CRITERION

An essential requirement for GCFR design undercooling transients is to maintain the basic configuration of the reactor core and thus preserve the capability for heat removal from the core. A gross change of the core geometry could reduce or redistribute the coolant flow, resulting in a degradation of the cooling capability and damage propagation between adjacent assemblies.

Effects on the core assembly, such as melting or significant relocation of core material, are therefore considered unacceptable. Geometry changes that preserve the relative arrangement and orientation of the core components

TABLE 3-1
SUMMARY OF PC-5 TEMPERATURE LIMITS

Reactor	Cladding Material	Faulted Temperature Limit [°C (°F)]	Ref.
LWR	Zirconium alloy	1200 (2200)	3-2
LWR	Stainless steel	1260 (2300)	3-3
AGR	Stainless steel	1350 (2460)	3-4
GBR-4	Stainless steel	1300 (2370)	3-5
CRBR	Stainless steel	1370 (2500)	3-6

are acceptable for PC-5 events as long as the coolant flow path is not obstructed or the heat removal capability is not significantly reduced. Thus, the design cooling geometry is maintained, and accident propagation is precluded. Nonpropagating cladding breach is acceptable: the ultimate acceptance criterion is keeping radioactivity release to the environment within 10CFR100 (Ref. 3-1) guidelines.

3.3. CLADDING MATERIAL PROPERTIES

3.3.1. Chemical Composition

Type 316 austenitic steel is a variation of the standard 18/8 chromium-nickel stainless steel to which 2% to 3% molybdenum has been added for improved corrosion resistance. The composition range of Type 316 stainless steel from several commercial vendors is given in Table 3-2. Property data on other types of stainless steel are included in this amendment to show approximate properties of Type 316. Similarly, most of the discussion in this amendment would be valid for other stainless steels similar to Type 316. (Use of such stainless steels is under consideration for advanced design GCFR cores as well as LMFBRs.)

3.3.2. Melting of Cladding Material

Undercooling of an assembly causes a heatup of the fuel and stainless-steel cladding tubes. If insufficient heat is removed over a prolonged time, the melting range of stainless steel could be reached. At the solidus temperature, no liquid metal has yet formed. The cladding temperature has to rise further while absorbing the heat of fusion. With increasing temperature, the fraction of molten material grows. At the liquidus point, the stainless steel is completely liquid. Somewhere in the melting range between solidus and liquidus temperature, the cladding material would be expected to begin relocating. Liquid metal steel would form and move downward on the fuel rod. Because of the axial temperature gradient in the core, a relocation of liquid steel would bring it into contact with the

TABLE 3-2
 CHEMICAL COMPOSITION OF TYPE 316 STAINLESS STEEL
 (REF. 3-7)

Element	(%)
Chromium	16.40 - 17.40
Nickel	13.10 - 13.80
Molybdenum	2.25 - 2.65
Manganese	1.32 - 1.64
Carbon	0.038 - 0.057
Phosphorus	0.001 - 0.013
Silicon	0.440 - 0.570
Sulphur	0.006 - 0.013
Nitrogen	0.005 - 0.024
Copper	0.010 - 0.090
Boron	<0.001 - 0.009
Iron	Balance

cladding at lower temperature, increasing the viscosity of the molten material and slowing down the axial movement. At this location, where marginal movement of liquid steel is possible, molten cladding would tend to accumulate, moving slowly downward during the core heatup. Accumulation of material between the fuel rods could result in blockage of the coolant channel and propagation of melting in the radial direction. Therefore, melting of the stainless-steel cladding in the active core or blanket is clearly a concern for the GCFR PC-5 event.

Ternary phase diagrams for iron-chromium-nickel (excluding effects of other elements) are shown on Figs. 3-1 and 3-2 (Ref. 3-8). For the composition of Type 316, the solidus temperature shown is 1415°C (2580°F), and the liquidus temperature is 1440°C (2620°F). Figure 3-3 (Ref. 3-8) is an isothermal diagram for 1400°C (2552°F). For LMFBR safety analysis, the middle of the melting range has been used as the melting point of stainless steel (Ref. 3-9). The average of solidus and liquidus temperature of stainless steel is given as 1430°C (2600°F). Although a considerable fraction of the heat of fusion has to be added to the stainless steel before relocation of cladding starts, the lower limit of the melting range (solidus) will be used in this evaluation to provide an additional safety margin against cladding relocation. The heat of fusion for Type 316 stainless steel is about 3610 cal/g-mol (6500 Btu/lb-mol) (Ref. 3-9).

In addition to iron, chromium, and nickel, all stainless steels contain numerous other elements (see Table 3-2), which lower the melting point slightly from that shown on the idealized ternary diagrams. Melting of stainless steel is generally regarded to start at 1400°C (2550°F) (Refs. 3-9 and 3-10). However, there are variations of about 30°C (50°F) among references for the melting temperature of Type 316 (due in part to the range in chemical composition shown in Table 3-2). The CRBR PSAR (Ref. 3-6) uses a conservative value of 1370°C (2500°F) to assure no clad melting. Reference 3-11 states a melting range as 1370° to 1400°C (2500° to 2550°F).

Fig. 3-1. Solidus lines in ternary diagram iron-chromium-nickel (Ref. 3-8)

Fig. 3-2. Liquidus lines in ternary diagram iron-chromium-nickel (Ref. 3-8)

Fig. 3-3. Isotherms for 1400°C (2552°F) in the ternary diagram iron-chromium-nickel (Ref. 3-8)

3.3.3. Strength at High Temperature

In most reactors, the limiting stress on the cladding during core heatup or LOCAs is caused by internal overpressure in sealed fuel rods. The PES used in the GCFR prevents a development of pressure differentials between the fuel rod and the helium coolant by vent connections between fuel rod and the helium of the primary system. Without internal overpressure, strength requirements are minimal for GCFR fuel cladding. Of course, the cladding must retain sufficient strength and resistance to creep to support its weight and to retain its basic rod bundle geometry for the time period of the DBDA.

3.3.3.1. Unirradiated High Temperature Strength. Extensive experimental investigations have been conducted to determine the tensile strength, ductility, and other mechanical properties of cladding tubes manufactured from Type 316 stainless steel. However, most of the available data are for temperatures up to 1000°C (1830°F).

Table 3-3 contains values of the ultimate tensile strength of prototypical cladding tubes with 20% cold work from several lots of Type 316 stainless steel for temperatures up to 870°C (1600°F) (Ref. 3-7). Very high strength is retained up to 870°C. The data in Table 3-4 reflect the influence of the strain rate on the tensile strength of 20% cold-worked stainless steel (Ref. 3-12). In Table 3-5, tensile strength data of Type 316 and 309 annealed stainless steels in the temperature range from 260° to 1300°C (500° to 2400°F) are given (Refs. 3-11 and 3-13). The data indicate strength of 7 to 14 MPa (1000 to 2000 psi) at 1300°C (2370°F).

The behavior of unirradiated stainless steel cladding under simulated accident conditions has been investigated by C. W. Hunter and R. L. Fish (Ref. 3-14). Depending on the hoop stress and the transient heatup rate, cladding tubes failed by rupturing between 1100° and 1300°C (2000° and 2400°F). The results of these tests (Table 3-6) show that stainless-steel

TABLE 3-3
TENSILE STRENGTH OF FAST REACTOR FUEL CLADDING MATERIAL (UNIRRADIATED) FROM ROOM
TEMPERATURE TO 870°C (1600°F) (20% COLD-WORKED TYPE 316 STAINLESS STEEL) (REF. 3-7)

Temperature [°C (°F)]	Tensile Strength [MPa (ksi)]									
	Tube Lot								Av	95% Confidence Limits
	N-1	N-2	N-3	O-1	R	T	U	BB		
Room Temp	828.2 (120.2)	811.6 (117.8)	811.6 (117.8)	864.7 (125.5)	790.3 (114.7)	822.6 (119.4)	846.1 (122.8)	769.6 (111.7)	--	--
	821.9 (119.3)	801.3 (116.3)	818.5 (118.8)	873.0 (126.7)	787.5 (114.3)	804.8 (116.8)	835.8 (121.3)	788.2 (114.4)	816.5 (118.5)	764.4-868.8 (110.9-126.1)
	813.7 (118.1)	811.6 (117.8)	810.3 (117.6)	--	--	--	--	--	--	--
430 (800)	684.2 (99.3)	671.7 (97.5)	675.2 (98.0)	692.4 (100.5)	654.6 (95.0)	669.7 (97.2)	689.0 (100.0)	645.6 (93.7)	--	--
	682.1 (99.0)	660.1 (95.8)	681.4 (98.9)	683.5 (99.2)	653.9 (94.9)	664.9 (96.5)	--	639.4 (92.8)	668.3 (97.0)	633.9-702.1 (92.0-101.9)
	676.6 (98.2)	663.5 (96.3)	638.7 (92.7)	--	--	--	--	--	--	--
540 (1000)	624.9 (90.7)	629.1 (91.3)	649.7 (94.3)	642.8 (93.3)	613.9 (89.1)	623.5 (90.5)	640.8 (93.0)	600.8 (87.2)	--	--
	634.6 (92.1)	622.2 (90.3)	604.9 (87.8)	639.4 (92.8)	613.2 (89.0)	627.7 (91.1)	641.5 (93.1)	601.5 (87.3)	627.7 (91.1)	597.4-657.3 (86.7-95.4)
	638.7 (92.7)	633.2 (91.9)	639.4 (92.8)	--	--	--	--	--	--	--
650 (1200)	436.8 (63.4)	450.6 (65.4)	472.0 (68.5)	479.5 (69.6)	463.7 (67.3)	438.2 (63.6)	456.1 (66.2)	417.5 (60.6)	--	--
	439.6 (63.8)	427.9 (62.1)	481.6 (69.9)	477.5 (69.3)	447.9 (65.0)	432.7 (62.8)	438.2 (63.6)	423.7 (61.5)	449.9 (65.3)	410.0-490.6 (59.5-71.2)
	449.2 (65.2)	447.2 (64.9)	474.0 (68.8)	--	--	--	--	--	--	--
760 (1400)	258.4 (37.5)	272.8 (39.6)	245.3 (35.6)	264.6 (38.4)	242.5 (35.2)	247.4 (35.9)	252.2 (36.6)	243.9 (35.4)	--	--
	257.7 (37.4)	262.5 (38.1)	250.1 (36.3)	264.6 (38.4)	243.2 (35.3)	242.5 (35.2)	251.5 (36.5)	244.6 (35.5)	252.2 (36.6)	228.1-277.0 (33.1-40.2)
	257.7 (37.4)	270.0 (39.2)	223.2 (32.4)	--	--	--	--	--	--	--
870 (1600)	131.6 (19.1)	146.1 (21.2)	137.8 (20.0)	141.9 (20.6)	130.9 (19.0)	137.8 (20.0)	134.4 (19.5)	135.0 (19.6)	--	--
	133.7 (19.4)	145.4 (21.1)	137.8 (20.0)	141.2 (20.5)	130.2 (18.9)	118.5 (17.2)	127.5 (18.5)	135.0 (19.6)	135.7 (19.7)	122.0-149.5 (17.7-21.7)
	136.4 (19.8)	144.0 (20.9)	129.5 (18.8)	--	--	--	--	--	--	--

TABLE 3-4
INFLUENCE OF STRAIN RATE ON TENSILE TEST DATA OF STAINLESS-STEEL TUBES
(UNIRRADIATED) BETWEEN 370° AND 870°C (700° AND 1600°F) (TYPE 316 STAINLESS STEEL) (REF. 3-12)

Test Temperature [°C (°F)]	Uniaxial Ultimate Tensile Strength [MPa (ksi)]	Equivalent Hoop Stress [MPa (ksi)]	Modulus Modified Hoop Stress [MPa (ksi)]	Strain Rate (min ⁻¹)	Total Elongation (%)	Test Time (h)
370 (700)	710 (103.0)	788 (114.3)	608 (88.3)	1.82 x 10 ⁻³	9.7	0.888
427 (800)	696 (101.0)	772 (112.1)	612 (88.8)	1.82 x 10 ⁻³	8.6	0.788
482 (900)	682 (99.0)	757 (109.9)	615 (89.3)	1.82 x 10 ⁻³	9.3	0.852
538 (1000)	638 (92.6)	708 (102.8)	598 (86.8)	1.82 x 10 ⁻³	8.6	0.788
593 (1100)	553 (80.2)	553 (80.2)	486 (70.5)	1.82 x 10 ⁻³	9.8	0.897
649 (1200)	404 (58.6)	404 (58.6)	371 (53.8)	1.82 x 10 ⁻³	16.6	1.520
704 (1300)	285 (41.3)	285 (41.3)	274 (39.7)	1.82 x 10 ⁻³	22.4	2.051
760 (1400)	227 (32.9)	227 (32.9)	227 (32.9)	1.82 x 10 ⁻³	24.1	2.207
427 (800)	670 (97.3)	744 (108.0)	590 (85.6)	1 x 10 ⁻²	11.7	0.195
538 (1000)	630 (91.5)	700 (101.6)	591 (85.8)	1 x 10 ⁻²	11.6	0.193
649 (1200)	453 (65.8)	453 (65.8)	453 (60.9)	1 x 10 ⁻²	22.2	0.370
760 (1400)	256 (37.1)	256 (37.1)	256 (37.1)	1 x 10 ⁻²	41.4	0.690
870 (1600)	137 (19.9)	137 (19.9)	150 (21.8)	1 x 10 ⁻²	40.8	0.680

TABLE 3-5
TENSILE STRENGTH OF ANNEALED STAINLESS STEEL
(UNIRRADIATED) FROM 260° TO 1320°C (500° TO 2400°F)

Temperature [°C (°F)]	Ultimate Tensile Strength [MPa (ksi)]		
	Type 316 Stainless Steel (Ref. 3-11)	Type 316 Stainless Steel (Ref. 3-12)	Type 309 Stainless Steel (Ref. 3-13)
260 (500)	506 (73.5)	506 (73.5)	531 (77.0)
370 (700)	500 (72.5)	500 (72.5)	510 (74.0)
480 (900)	489 (71.0)	482 (70.0)	475 (69.0)
600 (1100)	458 (66.5)	434 (63.0)	420 (61.0)
700 (1300)	317 (46.0)	320 (46.5)	310 (45.0)
820 (1500)	189 (27.5)	186 (27.0)	176 (25.5)
930 (1700)	103 (15.0)	127 (18.5)	103 (15.0)
1040 (1900)	55 (8.0)		59 (8.5)
1150 (2100)	21 (3.0)		31 (4.5)
1260 (2300)	13 (1.9)		17 (2.5)
1320 (2400)			14 (2.0)

TABLE 3-6
RESULTS FROM THERMAL-TRANSIENT TESTS OF UNIRRADIATED 20% COLD-WORKED TYPE 316
STAINLESS-STEEL CLADDING TUBES (REF. 3-14)

Specimen No.	Hoop Stress [MPa (psi)]	Initial Test Temp [°C (°F)]	Transient Heat Rate [°C/s (°F/s)]	Diametral ^(a) Strain (%)	Total Hoop ^(b) Strain (%)	Termination or Failure Temp [°C (°F)]
4	22.3 (3240)	599 (1110)	5.6 (10)	2.5	NA	1204 (2200)
5	37.2 (5400)	599 (1110)	5.6 (10)	13.0	55	1154 (2110)
6	37.2 (5400)	599 (1110)	110 (200)	9.0	28	1221 (2230)
7	22.3 (3240)	599 (1110)	110 (200)	4.6	8.7	1288 (2350)
8	22.3 (3240)	599 (1110)	110 (200)	3.4	NA	1238 (2260)
9	24.8 (3600)	732 (1350)	5.6 (10)	3.3	NA	1232 (2250)
10	24.8 (3600)	732 (1350)	110 (200)	2.3	NA	1243 (2270)
11	44.7 (6480)	732 (1350)	5.6 (10)	5.5	61	1160 (2120)
12	44.7 (6480)	732 (1350)	110 (200)	19.9	38	1238 (2260)
13	22.3 (3240)	599 (1110)	110 (200)	1.3	NA	1121 (2050)
14	22.3 (3240)	599 (1110)	5.6 (10)	1.5	NA	1121 (2050)
15	37.2 (5400)	599 (1110)	5.6 (10)	6.6	NA	1121 (2050)
16	37.2 (5400)	599 (1110)	110 (200)	2.1	NA	1121 (2050)
17	22.3 (3240)	599 (1110)	5.6 (10)	7.0	NA	1232 (2250)
18	24.8 (3600)	732 (1350)	5.6 (10)	1.0	NA	1127 (2060)
19	24.8 (3600)	732 (1350)	110 (200)	0.8	NA	1110 (2030)
20	44.7 (6480)	732 (1350)	5.6 (10)	11.0	NA	1127 (2060)
21	44.7 (6480)	732 (1350)	110 (200)	3.5	NA	1110 (2030)

TABLE 3-6 (Continued)

Specimen No.	Hoop Stress [MPa (psi)]	Initial Test Temp [°C (°F)]	Transient Heat Rate [°C/s (°F/s)]	Diametral ^(a) Strain (%)	Total Hoop ^(b) Strain (%)	Termination or Failure Temp [°C (°F)]
23	44.7 (6480)	732 (1350)	5.6 (10)	1.2	NA	1038 (1900)
29	8.9 (1300)	732 (1350)	5.6 (10)	0.3	0.7	1288 (2350)
30	24.8 (3240)	732 (1350)	5.6 (10)	2.4	21.6	1260 (2300)
33	8.9 (1300)	732 (1350)	110 (200)	0.2	0.3	1327 (2420)

(a) Diametral strain is adjacent to the failure point.

(b) Total strain is at the failure point.

cladding retains some residual strength up to the highest failure temperatures. Data on unirradiated cladding in Figs. 3-4 and 3-5 (Refs. 3-14 and 3-15) also show retention of some strength up to near the melting point.

Additional high temperature data will be obtained from tests described in Section 4.1.1. These tests are expected to show retention of sufficient tensile strength at the 1300°C (2400°F) limit.

3.3.3.2. Irradiation Effects. Tests on irradiated specimens similar to those run on unirradiated samples are also reported in Refs. 3-14 and 3-16. All cladding specimens were 20% cold-worked Type 316 stainless steel and were cut from Experimental Breeder Reactor II (EBR-II) irradiated fuel pins (burnups of up to 59 MWd/kg, fluences of up to 5.8×10^{26} n/m²). The poorest performance was exhibited by specimens that had been adjacent to the fuel. The transient heating rates used in the tests were 5.6°C/s (10°F/s) and 110°C/s (200°F/s), which are representative of the range of interest for the Fast Test Reactor (FTR).

The irradiated cladding exhibited reduced ductility and lower failure temperatures compared to unirradiated specimens, and this effect was greater for the 5.6°C/s (10°F/s) tests. Failure strains decreased with increasing fluence and reached a saturation value. Reduced ductility adjacent to fuel was ascribed to segregation of impurities on the grain boundaries of the cladding and to enhanced embrittlement due to helium atoms implanted by neutron collisions. The large decrease in failure temperatures and low failure strains were associated with intergranular fracture of the irradiated cladding. Intergranular fracture occurs above ~540° to 650°C (1000° to 1200°F). The conditions during irradiation are correlated with test conditions and test results in Table 3-7. The influence of the fast neutron irradiation ($E > 0.1$ MeV) on the failure strain of Type 316 stainless steel is illustrated in Fig. 3-6 and shows a strong reduction of the strain at failure caused by the irradiation.

Fig. 3-4. Effect of irradiation on failure temperature of 20% cold-worked Type 316 stainless-steel cladding during high-temperature transients (Ref. 3-14)

Fig. 3-5. Failure strength and strain of irradiated Type 316 stainless-steel cladding at high temperatures (Ref. 3-15)

TABLE 3-7
STRESS AND STRAIN DATA FROM THERMAL-TRANSIENT TESTS OF IRRADIATED 20% COLD-WORKED TYPE 316
STAINLESS-STEEL CLADDING TUBES (REF. 3-14)

Irradiation Conditions					Transient Test Conditions			
Specimen No.	Temp [°C (°F)]	Fluence, n/cm ² (E > 0.1 MeV)	Hoop Stress [MPa (psi)]	Initial Test Temp [°C (°F)]	Transient Heat Rate [°C/s (°F/s)]	Diametral ^(a) Strain (%)	Total Hoop ^(b) Strain (%)	Failure Temp [°C (°F)]
E12	766 (1410)	9.0 x 10 ²¹	44.7 (6480)	732 (1350)	5.6 (10)	1.9	2.3	1138 (2080)
E14	766 (1410)	9.0 x 10 ²¹	44.7 (6480)	732 (1350)	110 (200)	0.4	-	1210 (2210)
E11	771 (1420)	9.0 x 10 ²¹	24.8 (3600)	732 (1350)	5.6 (10)	0.8	1.3	1204 (2200)
E13	771 (1420)	9.0 x 10 ²¹	24.8 (3600)	732 (1350)	110 (200)	1.0	0.9	1249 (2280)
C62	565 (1050)	7.4 x 10 ²¹	37.2 (5400)	599 (1110)	5.6 (10)	1.4	2.4	1110 (2030)
C46	565 (1050)	7.4 x 10 ²¹	37.2 (5400)	599 (1110)	110 (200)	1.2	1.8	1188 (2170)
C51	571 (1060)	7.4 x 10 ²¹	22.3 (3240)	599 (1110)	5.6 (10)	1.2	1.1	1199 (2190)
C47	565 (1050)	7.4 x 10 ²¹	22.3 (3240)	599 (1110)	110 (200)	0.7	1.3	1210 (2210)
E16	766 (1410)	9.0 x 10 ²¹	8.9 (1300)	599 (1350)	5.6 (10)	1.0	1.3	1304 (2380)

(a) Diametral strain is adjacent to the failure point.

(b) Total strain is at the failure point.

Fig. 3-6. Effect of irradiation on the failure strain of 20% cold-worked Type 316 stainless-steel cladding during high-temperature transients (Ref. 3-14)

The effect of fast neutrons on the maximum hoop stress and the failure temperature at different heating rates is shown in Fig. 3-4. Higher test temperatures, as well as higher irradiation temperatures, tend to reduce the effect of irradiation. Similar to unirradiated stainless steel, the stress at failure decreases with increasing failure temperature. There appears to be no effect of the neutron fluence, however, on the strength of Type 316 stainless steel at very high temperatures between 1260° and 1370°C (2300° and 2500°F) (Fig. 3-5, Ref. 3-15).

3.4. PHENOMENA POTENTIALLY AFFECTING PC-5 TEMPERATURE LIMIT SELECTION

3.4.1. Ballooning of Cladding Tubing

A radial expansion of the cladding under an internal overpressure would reduce the coolant flow area in the fuel assembly and the effectiveness of heat removal from the fuel. Since the fuel rods of the GCFR are vented and kept at the same pressure as the surrounding helium even during a depressurization accident, cladding tube ballooning cannot occur. During a DBDA, the pressurized helium inside the rods exits into the PES. The flow passages are sufficient to release the small volume of helium within the rod such that the rod internal pressure follows the reactor pressure down with negligible differential pressure across the cladding. Therefore, the internal pressure stress on the cladding is negligible. With regard to the potential of the vent passages becoming blocked by fission products, there is a development program in progress, as discussed in Section 4.1.3, which has an objective to determine the system characteristics necessary to prevent fission product plugging of vent passages. This program will demonstrate a sufficiently low probability for plugging to eliminate systematic plugging of fuel rods from further consideration. However, it could be postulated that an isolated rod in the core is somehow blocked by fission products or was not properly vented during manufacturing. The consequences of these two situations are discussed below.

3.4.1.1. Rod Blocked After Reactor is Pressurized. Postulating fission product plugs early in core life is incredible because insufficient fission products will have been produced. By the time that significant fission products are produced in fuel rods, the cladding will have lost a significant portion of its ductility due to fast neutron fluence. Therefore, if a fuel rod in mid-to-late life failed to depressurize, the rod would breach (by a pin hole or crack) with only slight diametrical expansion and no loss of design cooling geometry around the blocked rod. The maximum diametrical strain at failure from internal pressure is only ~1% (3×10^{22} n/cm², Fig. 3-5).

Radial blanket rods have lower fast neutron exposure, and their cladding retains more ductility (therefore more potential for ballooning). Blanket rods have pitch/diameter ratios close to 1.0 and can withstand less ballooning before losing design cooling geometry. Offsetting the above, blanket rods would be much less likely than fuel rods to plug from fission products. Fission products accumulate very slowly in the blanket from the initial load of U-238 and fissions of bred fissionable material. Also, larger pellet-clad gaps and plenums permit easy depressurization. Even if design cooling geometry were lost around an isolated ballooned blanket rod, there is little potential in the blanket assemblies to lead to significant accident consequences. This conclusion is subject to experimental verification in the local flow blockage tests discussed in Section 4.1.3.

3.4.1.2. Rod Not Vented Due to Manufacturing Defect. Due to plutonium content, fuel rods will be fabricated in a sealed condition. The rods will be vented during fuel assembly construction. A manufacturing technique that precludes assembly without rod venting will be used, and strict quality assurance procedures will be followed to verify that each rod is properly vented. Radial blanket rods do not need the same seal precautions (since they contain no plutonium), and proper venting will be part of the rod manufacturing and quality assurance. However, it is recognized that loading an isolated nonvented fuel or blanket rod could be postulated.

If a DBDA were to occur early in core life with a nonvented rod in the core, the pressure of the gas inside the rod would tend to increase in proportion to the accident temperature. For example, if the rod temperature were to increase from 300 K room temperature when sealed to 1500 K during a DBDA, the rod internal pressure would be 5 atm abs in the absence of gas volume changes. At the same time, reactor pressure (external pressure on the sealed rod) would drop to ~5 atm (containment building equilibrium pressure). There should be no time when there is any internal overpressure in the rod. (Typical temperatures and pressures for the above scenario will be included in PSID Amendment 10.) It is expected that there will be no ballooning, due to the absence of any internal overpressure. This conclusion is subject to analytical and experimental verification by the programs discussed in Section 4.1.3.

Another postulated condition might be a DBDA late in core life with a nonvented rod in the core. When the reactor is pressurized and brought to operating temperature, a rod that was somehow sealed before core loading begins slow creep collapse of its cladding. Cladding integrity is likely to be breached due to either excessive strains caused by the collapse or strains resulting from excessive mechanical interaction between the collapsed cladding and the fuel. If cladding integrity is maintained, fission gas pressure will begin to build up in the rod. Loss of fuel cladding ductility occurs before a significant buildup of fission gas pressure. If a DBDA occurred late in life, a fuel rod sealed before core loading, which still retained its integrity, would breach as a consequence of the DBDA without significant diametrical expansion. In a radial blanket rod, fission gas pressure buildup would be too slow to prevent creep collapse of the cladding. Also, it appears that creep collapse would occur before loss of ductility. A DBDA would likely cause a collapsed fuel or blanket rod to breach quickly, without ballooning, at the location of the collapse damage.

3.4.1.3. Summary of Ballooning Conditions. It could be postulated that fission products block an isolated rod, preventing rod depressurization during a DBDA. However, such a block is incredible during early life before significant fission products have formed. If a rod were to block during a DBDA late in life, the clad would breach without ballooning, due to radiation-induced loss of clad ductility. Fuel clad loses its ductility early in life before production of significant fission products. Radial blanket clad retains its ductility longer, but fission product buildup is very slow, making fission product plugs extremely unlikely. There may be a midlife gray area; tests are planned which are expected to confirm that the assembly rod spacing is sufficient to permit slight rod ballooning prior to nonductile clad breach. Also, local blockage tests are expected to confirm that damage will not propagate due to a ballooned rod.

It could also be postulated that the core contains a rod that somehow was not vented during manufacturing. Such a rod would begin to creep-collapse, leading to a clad breach (providing a vent through the breach). Even if a DBDA occurred while a rod was still sealed, the slight internal pressure from gas thermal expansion would be offset by the increase in containment building pressure. Therefore, there should be no pressure stress on the cladding.

As the result of the above considerations, the conclusion is reached that an isolated plugged rod does not influence the selection of the PC-5 temperature limit. Certain performance characteristics of isolated plugged rods are, however, subject to experimental verification.

3.4.2. Dead-Weight Effects

In the present design of the GCFR fuel and blanket assemblies, the rods are supported axially at their lower ends, which are also the coolant inlet end. In the case of the fuel assemblies, the rods are supported laterally periodically along their length by means of spacer grids; the

blanket rods are laterally supported by wire wraps. For these configurations, the only significant dead-weight effect upon the cladding is the weight of the cladding (and wire wrap on blanket rods). The weight is maximum at the coolant inlet end where the strength is very high.

The region where dead-weight effects might be important during a DBDA would be near or above the core midplane where the peak temperatures occur. At this location, the cladding dead-weight force would be due to the weight of the cladding above the point of maximum temperature. The only other dead-weight effect that might be considered is some amount of fuel or axial blanket-pellet weight that might be frictionally bound on the cladding above the point of maximum cladding temperature. For the pellet weight to be transferred to the cladding, there would have to be a gap below the bound pellet. Then highest downward load on the cladding would be if all pellets above the bound pellet were bonded together. Therefore, the stress due to dead weight will be limited to several MPa (several hundred psi). Tests described in Section 4.2.1 will evaluate clad creep under dead-weight loading for time periods representative of the DBDA.

3.4.3. Seismic-Induced Forces

The DBDA PC-5 condition is caused by a failure of the reactor coolant pressure boundary, which could be postulated to occur simultaneously with a safe shutdown earthquake (SSE), also a PC-5 condition. The maximum cladding temperature occurs a considerable time after the start of the DBDA: about 4 min for fuel cladding and 20 to 30 min for blanket cladding. (The DBDA time history will be discussed in PSID Amendment 10.) Therefore, it is very unlikely that the SSE forces would be applied to the core at the same time as the maximum DBDA clad temperature. The DBDA transient temperature ramp will be compared to the likely duration of the SSE, and the core will be designed for a conservative combination of clad temperature and seismic force. Very high temperature tensile strength tests, discussed in Section 4.1.1, will be performed to verify the capability of the fuel and blanket cladding for accommodating dead weight as well as seismic forces without

the loss of design cooling geometry. For the purposes of selecting the PC-5 temperature limit, it has been assumed that the cladding retains sufficient strength at the limit to assure design cooling geometry in the assembly. The effects of rod displacement (if any) due to seismic forces will be analyzed in the same manner as rod bowing.

3.4.4. Rod Bowing

Asymmetrical temperature gradients cause fuel rods to bow, resulting in local changes of the rod pitch and the corresponding coolant flow channels. A common theoretical expectation is that rod bow due to thermal gradients is in the direction to increase the temperature gradients, causing further rod distortion and degradation of heat transfer. In water, liquid-metal, and gas systems, extensive experience has shown that an autocatalytic effect does not develop when the bows are maintained at a small fraction of the rod-to-rod gap. Stable equilibrium positions are established that are a combined function of the thermal-hydraulic and structural characteristics.

Structural rod bow tests, out-of-pile heated rod experiments, and in-pile tests, as described in Section 4, are planned to demonstrate that the GCFR design parameters satisfy the requirements for maintenance of a stable geometry. In-pile tests, as well as out-of-pile tests, are planned to ensure that the correct structural characteristics of the rods are represented, but more importantly, that the correct deformations as the result of in-pile creep and swelling are properly represented. These programs for demonstrating the system stability during simulated PC-5 conditions result in rod bowing not being a limiting parameter for selection of the clad temperature limit.

3.4.5. Rod-Spacer Interaction Forces

During the heatup period of a DBDA, the temperature distributions within the core assemblies are such that most components at a given axial

location are not widely different in temperature. The cladding average temperature at a given axial location will, however, always be slightly hotter than the assembly structural components (the spacer grids, grid supports, and flow duct). There is, therefore, the potential for some differential thermal expansions to occur. The most significant differential expansions are those which occur in the axial direction and are normally accommodated by motion of the rods through the grids. These motions would occur predominately during the early phases of a DBDA. The initial shutdown period would not result in effects significantly different than those in a normal shutdown. During the subsequent heatup period, the differential expansions should become negligible as the maximum temperatures are approached, since the differential temperatures between the rods and structural components would tend to approach constant values. However, there is the potential for having to accommodate some differential expansions between the fuel rods and grids at the peak temperature during a DBDA.

The differential expansion is accommodated by slippage between the rods and grids. At the very high temperatures during a DBDA, there could be a tendency for the rods to self-weld or otherwise seize in the grids. Should this happen, the differential expansions would have to be accommodated by component deformations. Tests discussed in Section 4.2.2 are designed to demonstrate that fuel rods can slide through the grid spacers (or in the alternative that the deformations are acceptable). Rod-spacer interaction phenomena are also an integral part of the tests mentioned above, with regard to rod bow considerations, for demonstrating stability of the geometry. Therefore, for purposes of selecting the PC-5 temperature limit, it is assumed that rod-spacer interaction forces are negligible.

3.4.6. Fuel-Clad Mechanical Interaction (FCMI)

The GCFR fuel rod is designed to have an initial nominal pellet-to-clad gap of 0.14 mm. The gap is sized to permit easy pellet loading, but is sufficiently small to prevent an excessive pellet-to-clad temperature drop.

When brought up to power, the gap reduces to less than 50 μm at the peak power position due to differential thermal expansion. During operation, the gap tends to close farther as the fuel swells, due to fission product production. Analyses using the LIFE-3 computer code (Ref. 3-17) indicate that, in the GCFR rod design, the pellet-to-clad diametrical gap never completely closes during steady-state lifetime operation. The presence of the relatively high primary system pressure within the rod suppresses the gaseous fission product component of fuel swelling.

After an initial incubation period corresponding to a burnup of ~ 4 at. % and a fluence of $\sim 5 \times 10^{22} \text{ n/cm}^2$, the pellet-to-clad gap begins to open due to fluence-induced metal swelling of the cladding. As a consequence, very little fuel-clad mechanical interaction (FCMI) occurs in the GCFR rod design. The only expected source of any significant FCMI is during over-power transients, should any occur, at the time in life before metal swelling initiates and the pellet-to-clad gap is a minimum.

The DBDA and LOFC are both undercooling events at reactor shutdown conditions. During these events, the average fuel temperature decreases and the cladding temperature increases. Typical analysis results indicate that, during a DBDA when the cladding temperature reaches a maximum, there is less than 50°C (90°F) temperature difference between the clad midwall temperature and the pellet centerline temperature. Since the average pellet temperature reduces while the cladding temperature increases, the pellet-to-clad gap increases during the limiting faulted events. Consequently, FCMI does not affect selection of the cladding PC-5 temperature limit. This conclusion is based upon analysis results using models that require further development for the particular conditions present in the GCFR rod design during the DBDA. Therefore, a verification test program is planned (see Section 4.4) to demonstrate performance with irradiated rods.

3.4.7. Fuel-Clad Chemical Interaction (FCCI)

Fuel-clad chemical interaction (FCCI) has been observed in most rods irradiated at temperatures above 600°C (1112°F) in the LMFBR program. FCCI was also observed in GCFR irradiation experiment F-1 (X094). The extent of cladding attack in the F-1 experiment was well within the limits of expected attack obtained from the large LMFBR data base, which can be represented by the following equation (Ref. 3-18):

$$D = 0.4521 (B + K) (O/M - 1.942)(T - 728)$$

for $B > 0$,
 $O/M > 1.942$, and
 $T > 728 \text{ K}$,

where D = depth of cladding attack (microns),

B = local fuel burnup (at. %),

K = constant (a function of the confidence level for maximum depth of interaction. For $K = 0$, the equation gives the median value of the attack. $K = 12.23$ gives the maximum attack at the 95% confidence level),

O/M = initial fuel oxygen/metal ratio, and

T = local time-averaged cladding inner surface temperature (K).

For the reference GCFR fuel, with $O/M = 1.96$ at the maximum i.d. cladding surface temperature of 770°C (1418°F), the above equation yields a median attack depth of 26 microns and a maximum attack depth of 57 microns at 10.2 at. % burnup (100 MWd/kg). Using the nominal attack depth, this represents a loss of wall thickness of only 7%. Since the GCFR cladding has no primary stress loadings at any time in its life, the effect of FCCI on the strength and the PC-5 temperature limit is expected to be negligible. The time at the temperature limit is too short to produce fuel-clad chemical effects. This conclusion is subject to the results of the verification test program with irradiated rods discussed in Section 4.

3.4.8. Formation of Eutectics

In a core design using structural materials of different compositions, diffusion at high temperatures could lead to the formation of eutectic alloys. Because of lowered strength and melting temperature of eutectics, a potential for eutectic alloy formation would pose structural problems. Only stainless steel is utilized in the GCFR core assemblies. Therefore, formation of eutectic alloys is not considered possible.

3.4.9. Coolant-Clad Chemical Reactions

Because of the inert helium coolant, no significant chemical reaction takes place in the GCFR that jeopardizes the integrity of the fuel assembly. However, in the event of a large leak in the primary system (DBDA), air ingress would slowly introduce oxygen into the reactor coolant system. Even under the most conservative assumptions concerning air ingress into the GCFR coolant system, the oxygen concentration in the mixture would be significantly less than in air. The low oxygen concentration has two effects: (1) it reduces the oxygen available in the coolant for reaction with the stainless steel cladding, and (2) it affects the oxidation rate (Ref. 3-19).

Oxide formation on stainless steel in air at temperatures between 1100° and 1375°C (2000° and 2500°F) has been investigated by J. T. Bittel, et al. (Ref. 3-20). The parabolic rate constant in dry air is extremely low up to ~1370°C (2500°F). Above 1370°C, the rate constant in air increases by three orders of magnitude. Melting of wustite (FeO) at the interphase between oxide and metal at 1375°C (2507°F) is assumed to cause the sudden change observed in the parabolic rate constant. Therefore, the PC-5 temperature limit should be less than 1375°C. Results of these oxidation experiments are contained in Table 3-8.

A study of the high-temperature oxidation of chromium-nickel Type 302 and 309 steels in dry and wet air at 980° to 1100°C (1800° to 2000°F) (Ref. 3-21) confirms the high resistance of stainless steels against oxidizing

TABLE 3-8
RESULTS OF OXIDATION OF TYPE 304L STAINLESS STEEL AT
1100° TO 1375°C (2012° TO 2507°F) IN AIR (a) (REF. 3-20)

Temperature [°C (°F)]	Rate Constants (mm/ $\sqrt{h}$)
1100 (2012)	0.0012
1200 (2192)	0.00054
1225 (2237)	0.00083
1350 (2462)	0.0065
1360 (2480)	0.0065
1375 (2507)	0.35
1375 (2507)	0.38

(a) Dry air (-66°C dew point).

atmospheres. Since the oxidation of Type 302 and 304 stainless steel at 898°C (1650°F) is similar to that of Type 316 (Refs. 3-10 and 3-11), a slow rate of oxidation of Type 316 cladding material is expected at higher temperature. In a helium-air mixture, oxidation of stainless steel during the DBDA time period at temperatures between 980° and 1300°C (1800° and 2370°F) is not expected to pose serious structural problems. Preliminary experiments conducted at GA on the oxidation behavior of ribbed GCFR cladding tubes manufactured of Type 316 stainless steel appear to confirm this conclusion. Additional tests are planned to verify the oxidation characteristics described in Section 4.1.2.

3.5. SELECTION OF PC-5 TEMPERATURE LIMIT

The previous sections discussed the important clad material properties and the phenomena that are potentially determinative of the PC-5 (faulted) temperature limit. These phenomena were the types of forces on the cladding and the chemical reactions that can significantly affect the material properties. It was shown that some of these properties and phenomena have little or no effect on selection of the limit (in some cases to be confirmed by test programs described in Section 4). Three effects appear to determine temperature limit: (1) clad melting, (2) strength (including irradiation effects), and (3) oxidation due to air ingress. The selected temperature limit of 1300°C (2370°F) provides adequate margin to assure that the core retains design cooling geometry during a DBDA.

The most important factor for retaining design cooling geometry is to assure no clad melting. The 1300°C (2370°F) limit is 70°C (126°F) below the melting point given in the most conservative reference (Ref. 3-11). The heat of fusion has to be added at temperatures above the solidus temperature before actual cladding relocation can occur, providing additional margin. Even if there were minor relocation of cladding material at the hot spot, this is not expected to seriously impair heat transfer in the fuel assembly or cause subsequent damage propagation in the core. A temperature limit of 1300°C (2370°F) to prevent melting of stainless steel is, therefore, considered conservative.

All of the forces that can act on the cladding at the time of maximum temperature following a DBDA are very small. Nevertheless, the cladding must retain some strength to assure design cooling geometry. Strength as used here means tensile strength and resistance to creep. Important parts of the test program are designed to obtain strength data and evaluate the effects of the small forces that act on the cladding. The amount of margin for strength cannot be determined until the test programs are completed, but sufficient strength margin is expected at 1300°C (2370°F).

Clad oxidation due to air ingress following a DBDA would be very small up to 1375°C (2507°F), the melting point of FeO. Therefore, the 1300°C (2370°F) limit has a margin for oxidation effects of 75°C (135°F). This conclusion will be confirmed by the oxidation test program.

The 1300°C (2370°F) limit is believed to be conservative with sufficient margin in the material properties for the phenomena identified in this amendment. The limit is subject to successful completion of the test programs described in Section 4.

3.6. APPLICATION OF TEMPERATURE LIMIT AND HOT-SPOT FACTORS

The 1300°C (2370°F) temperature limit has been selected under the conservative assumption that all of the cladding in the upper part of the fuel region is at 1300°C. In fact, the limit is used for accident design purposes as a 3σ hot spot; 1300°C would be three standard deviations (3σ , corresponding to the 99.86 percentile) above the mean temperature, assuming a normal temperature distribution. The above approach is conservative, because only a small percentage of the rods might have 3σ hot spots, and these small areas would occur only in the core outlet region.

The thermal-hydraulic analysis of the GCFR core with subchannel codes predicts nominal temperatures, without accounting for nuclear, engineering, and manufacturing uncertainties. To assure that the maximum temperatures do not exceed the limits, uncertainties affecting the temperatures are

accounted for by hot-spots factors. Hot-spot factors are determined for the uncertainties in each of the many parameters that affect cladding temperatures. The uncertainties in each of the design and performance parameters are based on experiments conducted under the GCFR program and on experience available from other fuel development programs, specifically the LMFBR. The assumption is made that all deviations from nominal design values follow normal distributions for which standard deviations can be determined. Using this assumption, the individual hot-spot factors are combined into overall hot-spot factors corresponding to 2 and 3 standard deviations above the mean (2σ and 3σ levels). (Statistical methods will be discussed in PSID Amendment 10.)

Following the procedure used in the design of the CRBR, 2σ hot-spot factors are being used for all normal operating condition calculations, and 3σ hot-spot factors are being used for PC-5 transient condition calculations (Ref. 3-6). The 3σ level was chosen for the CRBR to provide a high assurance that sodium boiling would not occur (one of the PC-5 acceptance criteria for CRBR is no sodium boiling). No similar concern exists for the GCFR core design where the 1300°C (2370°F) PC-5 limit is at least 70°C (126°F) below an acceptance criterion of no clad melting. Therefore, a 2σ level corresponding to the 97.72 percentile would be acceptable for the 1300°C PC-5 limit. Nevertheless, 3σ hot-spot temperatures are being evaluated against the 1300°C limit, to be consistent with the CRBR precedent. Application of the 3σ hot-spot factor to the calculated cladding temperature of the hottest rod following a DBDA results in an increase of about 200°C (360°F); a 2σ hot-spot factor increases the temperature by about 150°C (270°F). (Calculated temperatures will be included in PSID Amendment 10.)

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4. VERIFICATION TEST PROGRAMS

The experimental test programs that have been identified for verification of the PC-5 (faulted) temperature limit and associated analytical methods include the following:

1. Material and chemical property tests at temperatures approaching the cladding melting temperature.
2. Laboratory tests of cladding structural response characteristics at temperature when subjected to the loadings during a DBDA.
3. Electrically heated rod bundle tests to obtain composite bundle effects during simulated DBDA conditions and postulated flow blockage.
4. In-pile tests of irradiated rod bundles subjected to simulated DBDA conditions for final verification.

Descriptions of each of these programs are given in the following sections and summarized in Table 4-1.

4.1. MATERIAL AND CHEMICAL PROPERTIES TESTING

The behavior of fuel rod cladding during a design basis transient is of importance for prediction of rod performance during the transient. The cladding material property test programs are of three general types:

1. Standard property tests on Type 316 stainless steel.
2. Oxidation behavior and associated loss of metal and strength.
3. Fission product distribution and plateout.

TABLE 4-1
VERIFICATION TEST PROGRAMS

Properties and Phenomena	Material Properties and Chemistry			Structural Tests				Electrically Heated Rod Bundles				In-Pile Tests
	Tensile and Creep Tests	Oxidation	Fission Product Plateout	Dead Weight-Stability	Rod Spacer Interaction	Rod Bow Relaxation	Ballooning Strength	Heater Rod Development	Depressurized Accident Condition Test (DACT)	Core Flow Test Loop (CFTL)	Local Flow Blockage	
Cladding Material Properties												
Melting								○	●		○	
Strength at high temperature	○			○		○	○					
Irradiation effects			○				○					●
Phenomena												
Blockage/ballooning			○				○			●	●	○
Dead weight	○			○	○	○	○	○	●	●	●	●
Seismic forces	○			○	○		○		●	●	●	
Rod bowing					○	○		○	●	●	●	●
Rod-spacer interaction		○			○	○		○	●	●	○	●
Fuel-clad mechanical interaction (FCMI)			○									●
Fuel-clad chemical interactions (FCCI)			○									●
Coolant-clad chemical reactions (oxidation)		○							○			

- Contributory information.
 ○ Separate effects data.
 ● Partially representative bundle performance.
 ● Fully representative bundle performance.

The following sections discuss each item above in greater detail and outline the planned test programs.

4.1.1. Standard Property Tests

Standard property tests on Type 316 stainless steel are needed to round out the mechanical properties data base. Such tests are especially needed for temperatures greater than 1000°C (1830°F); 1000°C is above the recrystallation temperature and just a few degrees below the minimum solution heat treating temperature. For this reason, the tests run at temperatures above 1000°C need not be run on cold-worked material.

For tensile tests beyond 1000°C (1830°F), the test loads will be applied at rates comparable to those expected in the reactor. Because of the high temperatures and short times involved in these tests and their effect on the measured properties, the tests cannot be run in strict accordance with American Society for Testing Materials (ASTM) recommendations.

Creep and stress relaxation are related properties: creep is stress controlled, and stress relaxation is strain controlled. Under the adverse conditions of these high temperatures, both tests will be run to attempt to assess the relative worth of the tests. At temperatures above 1000°C (1830°F), the stress relaxation rates and the creep rates under any significant load will be high. Because the tests will be relatively short-time tests, the heat-up times and soak times generally used to stabilize conditions before testing will be a large portion of the time the specimen will be at temperature. This comparatively long time can have a significant effect on the test and must be kept as short as possible. Some experimentation will be necessary to learn to run these tests under dynamic conditions and to obtain usable data.

Optical and electron microscopy will be conducted on the material in all conditions tested. This will give some insight into the aging reactions taking place during operation and during the accident, the recrystallation times and temperatures, and the failure mechanism. The scanning electron microscope can be used for fracture surface observation and can also be used for phase identification. The observed structural changes will be correlated with changes in mechanical properties.

4.1.2. Oxidation Behavior of Ribbed Cladding (Type 316 Stainless Steel)

Some cladding oxidation will occur should air ingress during a DBDA. Preliminary tests performed at GA have indicated that oxidation is not a design limiting problem. The preliminary tests were performed in a pure air environment, but the actual environment with air ingress during a DBDA would be an air-helium mixture. Oxidation behavior is also dependent on the initial oxide layer or pretreatments. Based on these considerations, the following test program is planned.

A significant number of fuel and blanket cladding specimens will be oxidized under conditions up to the PC-5 temperature and simulating the DBDA (i.e., heating rates, environment, test duration, design hydrogen/water ratio, and possibly high velocity coolant). Prototypic heating rates (which are slower than those used in the preliminary tests) will be simulated. The slower heating rate may be beneficial, since it increases the possibility of forming a tenacious oxide layer before the specimen reaches high temperature. The effect of high velocity coolant may be significant, because mass transfer effects can be expected. These mass transfer effects lower oxygen concentration in the boundary layers, but replenish the oxygen more effectively; the net effect is unclear. Irradiation-induced transmutations may alter oxidation behavior; therefore, oxidation tests on irradiated cladding material will be considered. Such tests would have to be conducted in a shielded glove box.

4.1.3. Fission Product Plateout

The potential for plateout of condensible fission products in the GCFR PES is being investigated in a research and development (R&D) program which includes both laboratory and in-pile testing. A primary objective of the program is to demonstrate that rods will depressurize properly during normal operation and a DBDA. As discussed in Section 3.4.1., even if an isolated rod were to somehow plug, the cladding breach would be nonductile with no clad ballooning.

The laboratory program is focused on plateout and plugging in the core assemblies which have lives of 3 to 4 yr. If volatile fission products can be retained by the subassemblies by the charcoal traps, it is not possible for condensible fission products (resulting from decay of precursor noble fission gases downstream of the subassemblies in the PES) to plug the passages during the 30-yr plant life.

Laboratory investigations of plateout and plugging potential in the core assemblies are being conducted at Argonne National Laboratory (ANL) and GA. ANL is responsible for the fuel rod chemistry R&D, and GA is responsible for developing the subassembly and rod fission-product-trapping technology.

At ANL, fuel rod chemistry involves investigation of the nature of the transport processes and the chemical reactions between gaseous and volatile fission products, fuel and blanket pellets, and cladding constituents that can lead to constriction or blockage of the passages needed for gas flow in venting and pressure equalization of the rods. Most of this technology is common to the LMFBR and the GCFR, since fission products generated in the fuel must be vented to the rod plenum in sealed LMFBR rods or to the rod traps in GCFR. Also much of the technology being developed on the LMFBR FCCI is applicable.

Irradiation of LMFBR and GCFR fuel rods in both fast and thermal reactor fluxes has shown that the principal potential for blockage arises from the formation of cesium uranate. While the uranate has only been observed at the fuel-blanket interface in short-rod irradiations, it is postulated that it will also occur in long rods and under conditions that will exist in FBRs. The lower density of the uranate relative to the fuel and blanket pellets results in local swelling of the pellets and constriction of the gas passages. ANL is investigating the phase diagrams of the reactants, the thermodynamics and kinetics of the reactions, reactions controlling the oxidation potential, and ways to accommodate, control, or inhibit the reactions. The GCFR aspects focus on the conditions in breached rods where inleaking helium coolant provides a significant source of moisture and hydrogen as coolant impurities. Also being investigated are potential advantages of controlling the fuel rod chemistry by controlling the oxidation potential (i.e., the H_2O to H_2 ratio) of the GCFR coolant.

At GA, the fission-product-trapping R&D is studying activated charcoal as the primary material, with graphite as a backup. The GCFR program extends the considerable basic technology developed for the high-temperature gas-cooled reactor (HTGR). While initially it was thought that iodine would be the principal fission product to be trapped in the fuel rod and subassembly traps, irradiation of GCFR fuel rods shows cesium to be the principal material transported beyond the blanket region, accompanied by traces of iodine. The irradiation program has also shown significant shrinkage of charcoal in fast reactor neutron fluxes. Graphite is known to be more resistant to radiation damage.

Thus, the R&D program is directed to the technology of cesium trapping and to neutron radiation damage to charcoal and graphite. The chemisorption of cesium is being measured on unirradiated charcoal and graphite and for these materials irradiated in thermal, cadmium-filtered, fast neutron fluxes as a function of burnup (or radiation dose).

The sorption and desorption of cesium on bed materials prior to, concurrent with, and after shrinkage will be investigated. Trap designs which will maintain the distribution of bed material as shrinkage occurs, to preclude trap bypassing and streaming and to assure its containment in the trap volume, will be developed. Cesium transport, sorption, and desorption in leaking fuel rods are considered to be critically important and are currently being investigated. The effects of moisture, hydrogen, and steam on trap properties and performance will also be measured.

The irradiation part of the plateout and plugging program is carried out as part of the integral irradiations of GCFR fuels. The irradiation program is described in detail in Ref. 4-1.

The multirod EBR-II irradiations, F-1 and F-5, are sources of irradiated charcoal and graphite materials for ex-pile measurements. Samples of irradiated trapping materials both exposed to volatile and gaseous fission products and isolated from the latter during irradiation are available. Materials loaded with fission products prior to, concurrent with, and after shrinkage of charcoal (due to irradiation) will be provided. These materials will then be further tested in the laboratory program described above.

The F-5 irradiation contains fuel rods in which special blanket pellets adjacent to the fuel are being tested as a means of accommodating Cs_2UO_4 formation at the fuel-blanket interface.

A series of sweep-gas irradiation experiments is being carried out (Refs. 4-2 and 4-3). Capsules GB-9 and GB-10 were irradiated in the Oak Ridge Research Reactor (ORR). The GB-11 capsules (three) will be irradiated in a cadmium-filtered flux in the Br-2 reactor. These capsules simulate PES operation under controlled conditions. The GB capsule series also provides information on short and long rods for evaluation of Cs_2UO_4 formation. They operate under both nonleaking and simulated leaking

conditions so that volatile fission products (particularly cesium and iodine) can be measured under both diffusive and convective transport conditions in the rods. Testing is also done with or without controlled levels of moisture and H_2 impurities in the sweep gas to evaluate the extent and kind of chemical reactions that will occur in GCFR fuel rods under real conditions. The control of helium flow in the fuel rods permits flow through and bypass of the traps to measure their effectiveness and the consequent plateout in the lines downstream of the traps. Measurement of the flow conductance of the fuel rods, which is a direct measure of pressure-equalization capability, is conducted as a function of burnup. Finally, the capsules will provide trap materials exposed to thermal and cadmium-filtered neutron fluxes and fission products, especially cesium, that have been transported to the trap under normal and leaking rod conditions. These trap materials will be analyzed and tested in the laboratory program described above.

A series of 12-rod, manifolded, vented and pressure-equalized GCFR fuel bundles are being irradiated in the helium loop in the BR-2 reactor at Mol, Belgium (HELM 2, 3, 2A, 4, and 5) (Ref. 4-4). These fuel bundles simulate all the features of GCFR fuel subassemblies, except for the number of rods, effects of fast neutron fluence, and total rod length. However, the rods are long enough to simulate the effect of rod length on the location of Cs_2UO_4 formation. Operation with deliberately defective rods will be conducted in the HELM-2A experiment. H_2 and moisture impurities at controlled levels will be present in the coolant, and chemical reactions with fuel, cladding, fission products (cesium, Cs_2UO_4 , etc.), and trapping materials will be observed. Local temperature effects (e.g., at rod spacer locations), if any, on cesium accumulation along the fuel rods will be tested. Trap materials exposed to cadmium-filtered flux and fission products from normal and leaking rods will be provided for analyses and testing in the laboratory program described above.

Finally, a number of irradiations of GCFR rods and bundles in the FTR, not yet well defined, will contribute to the plateout and plugging R&D program. Additional tests will be incorporated into these irradiations to add to the plateout- and plugging-prevention data base.

4.2. LABORATORY TESTS OF STRUCTURAL CHARACTERISTICS

Laboratory tests of structural characteristics will be performed as a supplement to the basic material properties tests described in Section 4.1 to verify or demonstrate structural performance characteristics of the fuel and blanket assembly components at very high temperatures. These tests are design dependent and could change as the design of the assemblies evolves. Consequently, the tests described in the following sections should be regarded as representative of the tests that will be performed to demonstrate the structural performance characteristics at very high temperatures.

4.2.1. Dead-Weight Effects

To demonstrate the stability of the rods to accommodate dead-weight effects, tests will be performed to measure cladding deformation as a function of temperature and unsupported length when subjected to a loading which simulates the dead weight. For the present design configuration, sections of fuel and blanket cladding will be loaded in compression by a dead weight. The section of fuel cladding will be supported laterally at points which correspond to the span between two adjacent spacer grids. The unsupported span will be heated to a temperature somewhat less than melting, held at temperature for a period of time corresponding to the time at temperature during a DBDA and the deformations measured. The test will be repeated at sequentially higher temperatures up to slightly less than melting, such that a temperature range is spanned from below the PC-5 temperature limit to above the limit. The entire test series will be repeated for at least two additional spacer spans.

The test results will be the time-dependent deformations as a function of spacer span and temperature for a given dead weight. Variation of loading (i.e., dead weight) will be performed to cover expected as well as maximum projected dead-weight effects.

4.2.2. Elevated Temperature Rod-Spacer Interactions

At the very high temperatures during a DBDA, there may be a tendency for the fuel rods to self-weld or otherwise seize in the grids. Should this happen, the differential expansions would have to be accommodated by component deformations. Two types of tests will be performed to assess the capability of accommodation of differential axial expansions up to 1300°C (2372°F). The first of these is the measurement of sliding friction at very high temperatures as a function of light lateral forces [$\leq 5\text{N}$ (1 lb)] and short dwell times. The tests are expected to show that rods remain free to move in the grid spacers.

The second type of test is to determine the capability for accommodating differential expansions with simulated seized conditions. Here, the differential expansion corresponding to the differential temperature between the rods and the structural supports will be imposed upon a rod sample with integral grid cells and the deformation characteristics measured for assessing maintenance of design cooling geometry.

4.2.3. Rod Bow Relaxation Tests

During normal operation, temperature gradients are developed across the peripheral rods (edge rods) in a rod bundle. The gradients are caused by differences in the coolant flow channels on the bundle side versus the duct side of the edge rods, as well as gamma heating in the duct walls. The gradients cause the rods to bow. The rod support system (grid spacers or wire wrap), included to maintain the rod array, suppress the resultant rod bow and introduce bending stresses in the

cladding. Long time-at-temperature causes relaxation of the bending stresses, such that rods removed from bundles exhibit permanent reverse curvatures at room temperature. In addition to the deformations caused by the temperature gradients, metal swelling of the cladding material at high fluence causes additional rod bow. This additional bow is because the metal swelling is temperature- and fluence-dependent. Consequently, when there are gradients across rods, differential swelling takes place even though the fluence is essentially constant across a single rod.

The thermal gradients and differential metal swelling cause bending stresses to develop in the rods, but the stresses are maintained at low levels by the creep mechanism. Should the rods be subjected to very high temperature, more complete relaxation of the stresses would occur. Analytical studies indicate that the relaxation at the very high temperatures does not result in any change in the rod bow deformations. However, the creep (and relaxation) data are, at present, quite meager at the very high temperatures. Additionally, the analytical methods are not well established for evaluation of the very fast creep rates that occur at the very high temperatures. Therefore, simple laboratory tests will be performed to evaluate the effect of very high temperatures and resultant creep on fuel and blanket rod bow deformations. In these tests, prebowed and stressed rods will be subjected to representative peak DBDA temperatures, and the relaxation will be observed to assess maintenance of design cooling geometry.

4.2.4. Deformation Behavior of Cladding

Deformation behavior of cladding (both irradiated and unirradiated) at high temperatures has been the subject of extensive investigation by the LMFBR program under the fuel clad transient test (FCTT) program (Ref. 4-5). In this program, deformation tests were performed by subjecting prepressurized cladding samples to temperature ramps. The

failure temperatures and clad failure strains were measured. Temperature ramps of 5.6°C/s (10°F/s) and 110°C/s (200°F/s) were used in these tests which represent anticipated LMFBR transient conditions. Since these temperature ramps are rapid, there is no significant change in the gas pressure inside the cladding during the test. Thus, these tests represent a constant load (stress biaxiality of 2), and hence, continuously varying strain rates. The results are published in the Nuclear Systems Handbook (Ref. 4-6).

The problem in obtaining cladding behavior data at high temperatures is because major changes in microstructure take place at a rate faster than the time required to establish test conditions and perform the test. Polygonization, recrystallation, and grain growth are the important phenomena leading to microstructural changes. (Polygonization is a subgrain restructuring phenomena which occurs during annealing, whereby edge dislocation change their arrangement to a vertical grouping.) Microstructural changes have the effect of reducing the strain at failure, thus reducing deformations during a design basis transient. Therefore, ideal tests would simulate the expected heating and strain rates.

The loading conditions and heating rates during the GCFR DBDA are different from those used in the LMFBR tests. Although the GCFR DBDA conditions are expected to be similar to the 5.6°C/s (10°F/s) transients, additional tests simulating GCFR conditions are planned. These tests will require slightly lower loading (gas pressure) and heating rates to simulate the GCFR DBDA conditions and need to be performed on ribbed cladding. These tests will extend the LMFBR data base to the GCFR PC-5 temperature regime. Most of the tests will be on unirradiated, ribbed cladding to verify use of the LMFBR data base. Unirradiated cladding represents the worst case for potential ballooning. Clad ballooning will be measured and used to design local flow blockage tests in

electrically heated rod bundles. Some tests will be run on irradiated, ribbed cladding to verify use of the LMFBR data base.

4.3. ELECTRICALLY HEATED ROD BUNDLE TESTING

4.3.1. Lower Power Safety Experiment (LPSE)

A joint experimental program is underway with participation by Los Alamos Scientific Laboratory (LASL) and GA. This program has been designated the Low Power Safety Experiment (LPSE) and is described in a program management plan (Ref. 4-7).

This program includes the depressurized accident condition tests (DACT) as part of the GCFR development. The object of the program is to experimentally verify the performance of the GCFR core assemblies under simulated accident conditions of low flow and low pressure which lead to clad temperatures that approach, equal, and exceed the design faulted limit. In these out-of-pile tests, electrically heated rods will model nuclear heating in a helium environment. The range of testing will include steady-state operation that demonstrates the design at progressively higher clad temperatures through the range of normal operation (PC-1) and accident (PC-2 through PC-5) limits. Extensive data will be collected to qualify the accident prediction computer codes.

The test objectives are the following:

1. Verify and validate the accuracy of predictive analytical models and methods.
2. Determine adequacy of the hot-spot engineering factors under DBDA conditions for both the edge and the interior fuel rods.

3. Obtain test data to confirm the use of heat transfer and friction factor data for both the edge and interior subchannels under DBDA conditions.
4. Demonstrate integrity of the fuel assembly or evaluate the degree of the fuel rod deformation under DBDA conditions.

The other part of the LPSE program is composed of the steel melting and relocation test (SMART), which is studying conditions and resultant phenomena beyond the PC-5 condition. The DACT and SMART program elements are further defined in their respective "Objectives, Criteria, and Requirements" documents (Refs. 4-8 and 4-9). The two program elements are performed in the same experimental facility at LASL, and much of the test development applies to both programs.

4.3.1.1. Heater Rod Development. LASL has the responsibility for developing the technology to perform the LPSE tests, for designing and fabricating test equipment and fixtures, and for planning, operating, and examining the individual tests. The first phase of the program involves developing heater rods which will simulate GCFR fuel rods in all characteristics judged to have an influence on the early phases of accident sequences. Successful development these heater rods is an essential part of the overall program and constitutes a major portion of the LPSE program during the first 2 yr. The LASL responsibility for the design, fabrication, and assembly of test fixtures encompasses all hardware and software systems necessary to conduct the LPSE tests. Specifications for the prototypic core components will be provided by GA.

The chronology of experimental progress is listed in Table 4-2, and the planned evolution of the core modules is illustrated in Fig. 4-1. The emphasis to date has been on heater rod development and operation of the full-length subgroup fixture (FLS). FLS-1 and FLS-2 have demonstrated that the test equipment can operate properly in the melting

TABLE 4-2
CHRONOLOGY OF EVENTS LEADING INTO THE SMART PROGRAM

<u>Event</u>	<u>Date</u>	<u>Significance</u>
GCFR Safety Program Review Committee meeting	May 1976	First demonstrated capability to melt fuel rod cladding by Los Alamos Scientific Laboratory (LASL)
GCFR core disruptive test program funded	July 1976	
254-mm (10-in.) fixture	October 1976	Demonstrated feasibility of graphite- alumina-steel rod design
7-rod test bundle	February - June 1977	Demonstrated feasibility of contoured rods in a parallel configuration. Achieved steel melting.
37-rod FLS-1	June 1978	First test of full length parallel rod array. Demonstrated importance of natural convection (pressure effect). Extensive clad melting.
37-rod FLS-2	July 1979	Demonstrated extended rod life with a modified full-length parallel rod array. Extensive clad and duct melting.
High bay completion	September 1979	Enhanced testing capability to handle full scale subassembly at operating pressure
37-rod FLS-3	March 1980	High temperature operation [1000 to 1350°C (1832° to 2462°F)] and associated bundle distortion
First SMART test	June 1980	First full-scale (subassembly) accident simulation at operating pressure

temperature range. FLS-3 is the first test in the series that is related to actual GCFR DBDA conditions; this test is expected to show that the rod bundle retains its basic design cooling geometry to the 1300°C (2372°F) PC-5 limit and beyond. This expectation is based on FLS-1 and FLS-2 post-test examination in which the bundle configuration was retained in the ceramic insulators in the collection of solidified clad melt.

4.3.1.2. Depressurization Accident Condition Test (DACT). The DACT tests at LASL will consist of a series of steady-state tests performed with test bundles at low pressure (<2 atm), low flows (<6%), and low powers (<6%), which simulate closely the quasisteady-state conditions expected at the time of peak fuel cladding temperature following postulated accidents. The fuel axial power profiles will be simulated with electrically heated rods having a graphite conductor embedded in ceramic insulation. The rib roughening provided for the fuel rod surface to enhance heat transfer will be simulated. The test bundle will be preheated to reach a specified clad temperature profile. Helium will be supplied from storage tanks.

To simulate heat transfer during the DBDA with air ingress, a helium-nitrogen mixture will be used as coolant. The gas will flow through the test section and be discharged in a controlled manner. The power, gas flow rate, inlet pressure, and temperature will be controlled and measured. A substantial number of thermocouples will be installed to obtain a statistical quantity of data. In addition to the cladding temperature measurement, the coolant channel outlet temperature, duct wall temperatures, and the flow pressure drops will be measured.

Tests will be conducted on full-size, 271-pin, fuel-rod bundles and 19-pin fuel-rod bundles to provide scaling data for in-pile tests. Limit testing will be performed at increasing temperatures. Confidence will be improved in the analysis methodology and to predict clad

temperature and rod movement that might affect design cooling geometry. Post-test examinations will be conducted to evaluate the overall assembly integrity during accident conditions. Detailed metallographic examinations of the cladding will be performed. It is expected that the cladding will maintain its integrity beyond the faulted temperature limit.

4.3.2. Core Flow Test Loop (CFTL)

For the GCFR, the most sophisticated out-of-pile test loop is being built at Oak Ridge National Laboratory (ORNL) to test fuel, blanket, and control assembly models of the GCFR core assemblies over the complete range of GCFR conditions. The core flow test loop (CFTL) principal parameters are presented in Table 4-3. These are envelope parameters that establish the potential test ranges. Test conditions within these ranges will be specified that simulate the predicted GCFR normal and possible accident conditions. The test program consists of (1) three fuel assembly models of 37, 61, and 91 rods, (2) two control assembly models, and (3) one full-size blanket model. The three fuel models were selected to span the size difference between the 19-rod in-pile bundles and the full-size DACT and flow-test models. Possible additional model testing will be evaluated as part of the assessment of the initial test results. The objective of the tests are twofold: (1) to demonstrate the satisfactory performance of the design and (2) to verify the GCFR steady-state and transient methods that are employed to predict model performance. For all tests, the principal input parameters will be power and helium flow, with constant time for steady state and varying time for transients. Loop pressure will be reduced during the simulation of the depressurization accidents. The thermal and structural response will be measured for each test and compared with prediction. The thermal and structural response of the physical test models will be studied to demonstrate the designs and to confirm or correct the analysis. Deviations from normal

TABLE 4-3
OPERATING REQUIREMENTS OF THE CFTL

Coolant	Helium
Design pressure	11.8 MPa (1715 psia)
Maximum operating pressure	10.3 MPa (1500 psia)
Minimum operating pressure	0.1 MPa (14.5 psia)
Test bundle data	
Test bundle maximum pressure	278 kPa (40.4 psi)
Inlet temperature	
Nominal	320°C (610°F)
Maximum	350°C (660°F)
Outlet temperature	
Nominal	550°C (1020°F)
Minimum	260°C (500°F)
Maximum ^(a)	593°C (1100°F)
Maximum total helium flow	3.2 kg/s (7 lb/s)
Helium flow per rod	
Core maximum	30.4 g/s (242 lb/hr)
Blanket maximum	27 g/s (215 lb/hr)
Maximum transverse fuel power gradient ^(b)	4%/cm (10%/in.)
Minimum transverse power gradient	0%/cm (0%/in.)
Maximum simulated fuel rod length	3065 mm (121 in.)
Maximum power per fuel rod	38 kW
Power per blanket rod	45 kW

(a) At full power and pressure (will be higher for DBDA tests)

(b) Rod power is controllable by rows to model transverse power gradients.

bundle geometry will be predicted from measurements of cladding temperatures, rod and bundle deformation, and examination of rod surfaces.

In combination with DACT, part of the CFTL test program will be directed toward demonstrating the clad faulted temperature limits by operating at the limit and higher to show margin. The upper limit for CFTL testing will be melting of the ORNL heater element (nichrome or Kanthal^{*}) that was developed for LMFBR and water reactor safety testing. The use of DACT heaters for low power/high temperature is possible, since these have operated to substantial clad melting. The low power/high temperature condition occurs at the end of the depressurization accident when the core pressure has equalized with the containment. Similarly, the margin tests will explore the model conditions of high temperatures up to clad melting.

In combination with the other verification test programs, the CFTL results will yield an experimental basis for the clad PC-5 temperature limit.

4.3.3. Local Flow Blockage

Local flow blockage tests are needed to demonstrate that certain small blockages do not propagate. Local flow blockages in fuel and blanket assembly geometries will be tested with electrically heated bundles.

Three types of local blockage that may be tested are (1) partial clad ballooning followed by clad breach, (2) full ductile ballooning, and (3) bowing. Although these phenomena were described in Section 3 as being extremely unlikely, some blockage testing is planned to

* Alloy trademark of Kanthal Corp., Bethel, Connecticut.

improve the degree of confidence in the GCFR core design. Partial ballooning could cause blockage on one side of a rod if the clad breach produced a flap of clad material (even though expected failure modes are cracks or pinholes). Postulating full ductile ballooning could reduce cooling to six adjacent rods (even though bulging to one side is more credible). Excessive rod bowing could reduce cooling between two rods (even though bowing is limited by reducing design life).

Local flow blockage test objectives are included in the CFTL program, but the examples given above are not necessarily credible nor inclusive. Additional flow blockage tests will be considered for incorporation into the planned electrically heated rod bundle experiments: SMART, DACT, or CFTL.

4.4. IN-PILE PC-5 TEMPERATURE LIMIT TESTS

In the preceding sections, out-of-pile tests have been described which are planned for verification of the GCFR PC-5 (faulted) temperature limit. Extensive data will be obtained on the performance of prototypic, but unirradiated, rod bundles in both the DACT and CFTL heated rod tests. To extend the data base acquired in the DACT and CFTL tests to include the effects of irradiation, a limited number of small bundle in-pile tests are planned which envelop the DBDA conditions. The plans for these tests are at a very early conceptual stage. Consequently, the following descriptions should be regarded only as representative of the test type presently conceived to be required to complete the data base necessary for demonstration of the GCFR faulted temperature limit.

4.4.1. General Description

For final verification of the GCFR PC-5 temperature limit, the effects of irradiation upon the rod characteristics are concluded to be required. These effects include neutron-fluence-induced metal

swelling, flux-enhanced creep damage, helium embrittlement from (n, α) reaction, fuel-clad interaction effects, and burnup effects upon the fuel column. The nature of the in-pile tests under development, including the effects of irradiation, for final verification of the GCFR faulted temperature limit is generally as follows:

1. Steady-state irradiation of rods to predetermined burnups.
2. Testing of the pre-irradiated rods in an appropriate test facility wherein the DBDA flow, pressure, and power conditions during the heatup and subsequent cool-down phase are enveloped.
3. Post-test examinations of the rods to evaluate their performance with regard to maintenance of design cooling geometry and correlation of the results to the data acquired in the more extensive out-of-pile test programs.

The in-pile tests are to be performed with fuel rods; no in-pile PC-5 limit tests are being planned for blanket rods. The fuel rods have more severe irradiation effects (fluence and burnup), and correlation of the fuel rod tests with the corresponding out-of-pile test data will provide an adequate data base to envelop blanket rods.

4.4.2. In-pile Faulted Limit Test Bundle

To obtain prototypic fluence-to-burnup, steady-state irradiations of rods in a fast flux facility are indicated. To acquire information on the in-pile transient response of bundles, sufficient pre-irradiated rods are required for the testing of bundles. Tests are required at more than one level of burnup to demonstrate performance at any time in life as the fuel-clad gap changes.

To maintain the scope of the steady-state irradiation program to within practical proportions, yet retain essential bundle characteristics, 19-rod bundles have been selected as appropriate for the in-pile PC-5 limit tests. The applicability, or necessary scaling requirements, of the 19-rod bundle size will have been previously demonstrated in the DACT and CFTL tests.

Two levels of irradiation conditions are considered appropriate:

1. Burnup/fluence condition at end of incubation period before onset of metal swelling where maximum pellet/clad interaction effects are predicted.
2. Burnup/fluence condition at suitable period after initiation of swelling to have sufficient data for performance prediction.

In addition to the above irradiation conditions, tests on unirradiated samples are also being considered to demonstrate the in-pile test facility, to correlate with the out-of-pile tests, and to represent the beginning-of-life (BOL) condition. The in-pile test rods are planned to have near-prototypical active core length and some length of axial blanket at each end of the active core (≥ 100 mm).

4.4.3. Test Facility

The nature of the in-pile PC-5 limit tests has been identified to consist of low-power, low-cooling tests of 19-rod bundles of irradiated fuel rods. The basic facility requirements can be characterized as being the following:

1. Spatially shaped neutron source for establishing the rod powers at GCFR shutdown conditions (~ 3 kW/rod).

2. Controlled helium cooling of the rods at flow rates and pressures that simulate the cooling during the heatup phase of the DBDA (~ 1 g/s per rod at ~ 1.3 MPa).
3. Enveloping the DBDA heatup and subsequent cool-down transient by either flow or power control.

For the above basic requirements, two test facility options have been identified to date. The first of these is the Gas Reactor In-pile Safety Tests (GRIST-2) facility (Ref. 4-10). GRIST 2 is a series of tests which are currently under consideration to study the phenomenology and integral behavior of GCFR core disruptive accidents (CDAs). The tests are being planned to be performed in a helium loop facility in the TREAT (upgrade) reactor. The helium loop is anticipated to have all the necessary circulation, data acquisition, flow control, cooling, equipment, etc. that would be necessary to perform the faulted limit tests.

The second facility that has been considered is the sodium loop safety facility (SLSF) coupled with engineering test reactor (ETR). A preliminary feasibility study (Ref. 4-11) has indicated that portions of the SLSF installed at the ETR could be used to perform testing at the desired conditions. The testing could be performed by utilizing the existing SLSF helium secondary system heat-dump loop as the primary coolant supply. The secondary pressure boundary design of the SLSF in-pile tube would be used with some top-closure modifications, and the extensive data acquisition systems and computer controllers would be directly applicable. New equipment would include a primary pressure boundary for the in-pile tube, test train assembly, PES, and possibly a filtration system to assure that the SLSF circulator system does not become contaminated.

4.5. QUALITY ASSURANCE

The planned research and testing outlined in this report will be performed in accordance with the requirements of the GA quality assurance program (Ref. 4-12). Requirements for the performance and verification of test results, technical relationships, coordination requirements, and approvals necessary prior to testing are established in the GA quality assurance program document for the GCFR (Ref. 4-13), and in contractual terms and conditions. Assurance of compliance is accomplished by continuous monitoring through review and evaluation of documents, activities, and performance at GA and at the facilities of other program participants. Supporting documentation for this report is maintained by GA and the contributing organizations and will be available for review and evaluation.

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5. SUMMARY AND CONCLUSION

The criteria and parameters that determine the cladding PC-5 temperature limit have been fully reviewed. The primary criterion is maintaining design cooling geometry: no beyond-design assembly deformations and no melting. The three parameters that determine the limit are (1) strength (including irradiation effects), (2) melting, and (3) oxidation (air ingress following the DBDA). The detailed evaluation of these parameters indicates that an appropriate cladding PC-5 temperature limit is 1300°C (2370°F). The estimated margin below an unacceptable cladding condition is 70°C (130°F). The limit is used in a very conservative manner as a 3σ hot spot with conservativisms in the systems design as well.

This limit will be supported by a verification test program which will include material properties testing, components structural testing, heated rod experiments, and in-pile transient testing. There is a high probability that the planned verification test program will confirm the 1300°C (2370°F) limit. The margin in this limit is consistent with the level of sophistication of the verification testing program and the level of confidence in the evaluation models used to calculate accident temperatures. NRC approval of this temperature limit, conditioned by successful completion of the supporting verification testing program described herein, is requested.