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**CONCEPTUAL DESIGN STUDY
OF THE
HYLIFE LITHIUM FALL
LASER FUSION CHAMBER**

MASTER

**FY 1979 ANNUAL REPORT
TO
LAWRENCE LIVERMORE LABORATORY
PURCHASE ORDER 4424409**

Rockwell International

Energy Systems Group
8900 De Soto Avenue
Canoga Park, California 91304

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Prepared by
ROCKWELL INTERNATIONAL CORPORATION
Energy Systems Group
8900 De Soto Avenue
Canoga Park, California

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PART A

FUSION CHAMBER DESIGN STUDIES

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1.0 INTRODUCTION

During FY 1979, the Energy Systems Group of Rockwell International continued to conduct conceptual design studies of the HYLIFE laser fusion target chamber in support of the System Studies for Laser Fusion Project at Lawrence Livermore Laboratory under LLL Purchase Order 4424409 (Ref. 1).

This work was a continuation of the studies of target chambers begun in FY 1978 under LLL Purchase Order 8730803 (Ref. 2). In FY 1978, conceptual design studies (Refs. 3,4) of the target chamber defined the general configuration and dimensions of the chamber and the inlet plenum, orifice plate, and nozzle plate concepts required to generate the desired lithium jet fall. Preliminary studies were performed of the target chamber interfaces with the liquid lithium supply system, the laser system, and the target chamber mounting and support system. Preliminary analyses were made of means for providing a laser beam port through the main jet fall and of secondary lithium fall concepts to protect those regions of the chamber wall above and below the laser beam port.

Also in FY 1978, target chamber environmental effects resulting from typical thermonuclear burns were evaluated. A number of studies were conducted of the lithium wall interactions resulting from such effects and the resulting stress levels in the first metal wall. Preliminary calculations of the thermal stress in the first metal wall were also performed. The outlet region of the target chamber was outlined conceptually, and preliminary design considerations were given to the annular graphite reflector regions of the target chamber and the associated liquid lithium coolant passages.

In FY 1979, the basic configuration of FY 1978 was used as a reference point. Many studies were then made to either generate more design information concerning this design or to produce new concepts that would lead to a superior configuration. Among these activities were a detailed

weight analysis, an assembly sequence, a chamber vibration analysis, a splash baffle stress study, and an analysis of first wall thermal stresses. Then some new concepts were introduced that pertained to the first wall, the lithium inlet nozzle, the chamber supports, the inlet piping, and pressure vessel.

2.0 RESULTS AND CONCLUSIONS

The results of and the conclusions that may be drawn from the FY 1979 ESG target chamber study are as follows:

- 1) The total weight of the FY 1978 target chamber is 2,308,489 kg (5,090,218 lb).
- 2) A satisfactory assembly sequence of the FY 1978 reference target chamber consisting of 27 steps has been achieved.
- 3) The vibration modes of the reference chamber structures have been calculated and are well above the rep rate of 1.0 Hertz.
- 4) The splash baffle stresses and deflections due to the fall are very low.
- 5) Approximate thermal and stress analyses show that some structures such as the first wall have, in the FY 1978 design, unacceptably high temperatures and thermal stresses.
- 6) First wall designs, having a finely divided, well-cooled, and resilient structure, have been conceived which appear to meet temperature, thermal stress, and impact requirements.
- 7) Nozzle inlet designs having similar characteristics also appear to meet these requirements.
- 8) An improved chamber support method has been designed.
- 9) A conceptual scheme whereby the inlet lithium piping has been formed into an annulus surrounding the chamber and which may eliminate the need for the graphite blanket has been proposed.

3.0 REFERENCE FY 1978 TARGET CHAMBER DESIGN

This section discusses the physical basis and the design characteristics of the FY 1978 laser fusion target chamber which was used as the reference design in FY 1979.

The thermal power of the HYLIFE power plant is generated in the fusion chamber. A deuterium-tritium (DT) pellet is injected into the chamber approximately once every second where it receives the energy of two high-power laser beams. The resulting thermonuclear burn yields about 2700 MJ (2.56×10^6 Btu) of energy consisting of about 68% high-energy neutrons and hard X-rays, and 32% ionized pellet debris and soft X-rays. A lithium waterfall surrounds the pellet at the time of the thermonuclear burn and absorbs most of this energy. The neutrons penetrate deeply into the lithium fall and are absorbed in all of the lithium in the target chamber. The ionized pellet debris and the soft X-rays are absorbed in a very thin layer of lithium. A graphite reflector, at the sides of the chamber, reflects neutrons back into the chamber and improves the neutron economy. Some of the neutrons react with the lithium to form tritium, and some of these reactions produce additional neutrons. The tritium is eventually removed from the lithium to be used in the manufacture of more DT pellets. The additional neutrons contribute to the total energy per pulse which is 3175 MJ (3.01×10^6 Btu).

The lithium waterfall also serves to protect the target chamber structures from the radiations. The high-energy (~ 14 MeV) neutrons can cause atomic displacements and helium production. With a lithium thickness of about 1 m between the reacting pellet and the chamber structures, the material damage is less than currently accepted limits for 2-1/4 Cr - 1 Mo ferritic steel in a 30-year plant.

The establishment of a lithium fall of about 1 m thick at the mid-plane, no closer than 0.5 m from the pellet, and having a fall height of 8 m, takes a lithium flow rate of about $129 \text{ m}^3/\text{s}$ (2.05×10^6 gpm). It

is assumed that a new fall must be established following each pellet reaction--that is, the time for the lithium to fall from the top to the bottom must not exceed the repetition period of about 1 sec.

The lithium flow rate required to reestablish the fall greatly exceeds the flow rate required to remove the thermal power. As a consequence, the average temperature rise of the lithium is about 12°C (22°F)--that is, the lithium is very nearly isothermal. This is advantageous in that thermal transients caused by splashing lithium are thereby minimized.

The reference target chamber is a vertical, cylindrical tank of height 23.5 m (77 ft) and inner diameter 10 m (32.8 ft). It consists of the lithium inlet and waterfall nozzle plate at the top, an outlet pipe and splash baffle at the bottom, and the first wall, graphite reflector, and pressure wall at the sides. The reference target chamber is shown in Figure 3.1. Its main features are described below.

Lithium enters the top of the reference target chamber in 16 pipes, 1.22 m (48 in.) in diameter. These empty into a pool above the orifice plate to a depth of about 1 m (3 ft). The orifice plate distributes the flow in the second pool which supplies lithium to the perforated nozzle plate. The nozzle plate forms a large number of lithium jets which comprise the main lithium fall in the chamber. The upper and lower pools are provided with slosh baffles to control the movement of lithium.

At the center of the upper pool and orifice plate is a vertical graphite plug. The purpose of the plug is to provide access to the inside of the chamber. There is a hole at the center of the nozzle plate just below the graphite plug. This allows the vapor pressure in the chamber to be nearly equal on both sides of the nozzle plate and thereby avoids disturbing the nozzle flow.

The reference first wall is 5 cm (2 in.) thick and is fabricated from 2-1/4 Cr - 1 Mo ferritic steel. The function of this wall is to contain the lithium flow as it falls from the inlet header and is disassembled by the pellet reactions. The graphite reflector has a total thickness of 30 cm (12 in.) and is clad with 2-1/4 Cr - 1 Mo. Its function is to reflect and moderate neutrons, thus improving the breeding ratio and neutron economy. The pressure vessel is 8 cm (3.1 in.) thick and also is constructed of 2-1/4 Cr - 1 Mo steel. It is a pressure vessel in the sense that it withstands atmospheric pressure on the outside, while the chamber alternates between a near-vacuum to an appreciable pressure following each pellet reaction.

The lithium outlet at the bottom of the reference chamber is a single pipe having a diameter of 5 m (16.4 ft). At the entrance to this pipe is a lithium pool. The pool must be protected from the direct action of the pellet reaction which could create shock waves that might damage the lithium pumps and piping. This protection is provided by a grid-like splash baffle a few meters above the pool. The splash baffle also minimizes the splashing of lithium brought on by both the fall itself and the force of the pellet reaction.

The data pertaining to the reference target chamber are summarized in Table 3.1.

TABLE 3.1
REFERENCE TARGET CHAMBER DATA
(Sheet 1 of 2)

Pellet Power	2,700 MWt
Electric Power	1,253 MWe
Chamber Size	
Height (overall)	32.4 m
Fall Height	8 m
Inner Diameter	10 m
Pulse Frequency	1 Hz
Fall Re-establishment Time	≤1 sec
Flow Rate (total)	129 m ³ /sec
Mid-plane	
Annulus Inner Radius	0.5 m
Annulus Outer Radius	2.5 m
Jet Diameter	0.10 m
Number of Jets	1,200
Flow Rate	92.3 m ³ /sec
Nozzle	
Annulus Inner Radius	0.5 m
Annulus Outer Radius	5 m
Velocity	4.4 m/sec
Pool Height	1.0 m
Jet Diameter (exit)	0.15 m
Nozzle Injection Angle (from vertical)	0°/45°

TABLE 3.1
(Continued)
REFERENCE TARGET CHAMBER DATA
(Sheet 2 of 2)

Orifice Plate

Annulus Inner Radius	0.76 m
Annulus Outer Radius	5 m
Orifice Diameter	0.2 m
Number of Orifices	1,140
Pool Height	1.1 m

Top Penetrations

Number of Entering Lithium Pipes	16
Diameter	1.2 m

Chamber Pressure

Before Pulse	~0.1 torr
After Pulse	~2.0 atm

Lithium Temperature, Inlet	500°C
----------------------------	-------

Rise	12°C
------	------

Maximum Wall Temperature	550°C
--------------------------	-------

Wall Components

2-1/4 Cr - 1 Mo Steel, Graphite,

Lithium Coolant

First Wall Thickness	5 cm
----------------------	------

Pressure Vessel Thickness	8 cm
---------------------------	------

4.0 REFERENCE CHAMBER WEIGHT ANALYSIS

A detailed weight estimate of the reference target chamber design was made. The weight of the lithium was excluded in this study. The reason for making the weight study was primarily to determine the amount of strategic metal (such as chromium) that is in the reference target chamber. The weight study also generated mechanical data concerning the target chamber which has facilitated studies of vibration and stress.

The target chamber consists of three materials: 2-1/4 Cr - 1 Mo ferritic steel, graphite, and zirconium. A summary of the weights of these materials is shown in Table 4.1:

TABLE 4.1
MATERIAL WEIGHTS IN TARGET CHAMBER

Material	Specific Gravity (gm/cm ³)	Weight	
		kg	lb
Steel			
2-1/4 Cr - 1 Mo	7.83	2,045,368	4,510,035
Graphite	1.94	259,650	572,529
Zirconium	6.50	<u>3,471</u>	<u>7,654</u>
		2,308,489	5,090,218

A more detailed weight summary is shown in Table 4.2:

TABLE 4.2
TARGET CHAMBER COMPONENT WEIGHTS

	Weight	
	kg	lb
Pressure Vessel		
Access Flange Lifting Lug (2 required)	835	1,842
Access Blind Flange	28,316	62,437
Access Weld Neck Flange	29,332	64,677
Access Sweepolet	29,330	64,672
Main Vessel Lifting Lug (3 required)	3,732	8,230
Upper Head	78,186	172,400
Lower Head	64,426	142,034
Upper Pressure Vessel Cylinder	130,729	288,257
Main Support Ring	460,484	1,015,368
Lower Pressure Vessel Cylinder	150,519	331,894
Lithium Inlet Pipes (16 required)	11,595	25,568
Lithium Outlet Pipe (1 required)	14,360	31,663
	<u>1,001,844</u>	<u>2,209,042</u>
Outer Graphite Cylinder		
Graphite	129,495	285,537
Steel	<u>86,942</u>	<u>191,707</u>
	216,437	477,244
Inner Graphite Cylinder		
Graphite	123,995	273,410
Steel	<u>83,271</u>	<u>183,612</u>
	207,266	457,022
First Wall	158,935	350,452
Orifice Plate	32,253	71,119
Nozzle Plate	53,858	118,758
Slosh Baffle	86,769	191,325
Guide Baffle	70,905	156,345
Splash Baffle	199,026	438,853
Orifice Plate Supports	1,996	4,401
Slosh Baffle Supports	2,620	5,777
Nozzle Plate Supports	2,453	5,410
Guide Baffle Supports	1,700	3,749
Splash Baffle Supports	1,700	3,749
Graphite and First Wall Supports	7,485	16,505
Access Plug Assembly and Liner	53,388	117,720
Miscellaneous Hardware	<u>209,863</u>	<u>462,747</u>
TOTAL	2,308,489	5,090,218

5.0 REFERENCE CHAMBER ASSEMBLY SEQUENCE

An assembly sequence study was conducted of the HYLIFE lithium waterfall laser fusion target chamber. The study considered site vs shop fabrication, handling, and weldability, and incorporates current concepts in the advanced fast breeder reactor program.

Figures 5.1 through 5.10 present a numbered, step-by-step writeup keyed to a pictorial assembly sequence of the target chamber. The sequence is grouped by subassemblies with the nomenclature and weld symbols noted. Some of the weld symbols have a CP and a Code. The "CP" indicates complete penetration welds for pressure boundaries, and the code number refers to the step number in Table 5.1.

In the process of doing this study and subsequent analytical work, certain problem areas were indicated where redesign would reduce the thermal stresses, the gross weight, the overall cost, and improve the fabricability.

A few of these improvements to the reference design are:

- 1) Eliminate the access port plug and liner assemblies and replace with an inspection port in the upper head that would be cut open for inspection purposes.
- 2) Instead of a forged support ring, replace with a torsion box construction.
- 3) Support all internals (baffles, orifice plate, nozzle plate, and graphite) from the first wall rather than from the pressure vessel.

Table 5.1 lists the 27 steps of the target chamber assembly sequence.

PRESSURE VESSEL ASSEMBLY

Figure 5.1

PRESSURE VESSEL ASSEMBLY

Figure 5.2

GRAPHITE BLANKET INSTALLATION

Figure 5.3

FIRST WALL ASSEMBLY

Figure 5.4

FIRST WALL INSTALLATION

Figure 5.5

SLOSH BAFFLE INSTALLATION

Figure 5.6

LITHIUM INLET NOZZLE & ACCESS PORT INSTALLATION

Figure 5.7

ACCESS PLUG ASSEMBLY

Figure 5.8

PRESSURE VESSEL CLOSURE

Figure 5.9

COMPLETED ASSEMBLY

Figure 5.10

TABLE 5.1

ASSEMBLY SEQUENCE OF REFERENCE TARGET CHAMBER

1. Weld outlet nozzle to lower head
2. Weld graphite blanket and first wall supports to lower head
3. Weld support flange to lower pressure vessel cylinder
4. Weld upper pressure vessel cylinder to support flange
- NOTE: Welds 5 through 7 may be made at this time, but assembly will have to be stored until final closure
5. Weld access nozzle to upper head
6. Weld access flange to access nozzle
7. Weld lifting lugs to upper head
8. Weld lower head assembly to lower pressure vessel cylinder
9. Install outer and inner rows of graphite blanket blocks in pressure vessel
10. Weld splash baffle supports to first wall
11. Install splash baffle in first wall on supports and weld in place
12. Weld nozzle plate supports to first wall
13. Install nozzle plate in first wall on supports and weld in place
14. Weld guide baffle supports to first wall
15. Install guide baffle in first wall on supports and weld in place
16. Weld orifice plate supports to first wall
17. Install orifice plate in first wall on supports and weld in place
18. Install first wall assembly on graphite blanket and first wall supports and weld in place
19. Weld slosh baffle supports to pressure vessel assembly
20. Install slosh baffles in pressure vessel on supports and weld in place
21. Install and weld in place access port liner
22. Install and weld (16) lithium inlet nozzles
23. Assemble and weld access plug liner
24. Weld access plug liner assembly to access port flange
25. Install top head assembly over access port liner and weld to pressure vessel
26. Install access plug liner flange on access plug liner and weld
27. Insert access plug in access plug liner and seal weld flanges

6.0 REFERENCE CHAMBER VIBRATION ANALYSIS

The structural evaluation of the reference laser fusion target chamber assembly was limited to determining the natural frequencies of the individual components that make up the target chamber assembly. The analysis has led to the preliminary results which are presented below. The design evaluated is presented in Figure 3.1.

The frequency evaluation of the target chamber was performed using the SAP V finite element computer code. The assembly was modeled using the pipe element option of the code. The model is shown in Figure 6.1 and includes the primary tank, first wall, graphite blankets, and the closure plug. The lithium inlet and outlet piping were not included in the model as they were assumed to be flexible relative to the target chamber assembly support ring and would not influence the assembly frequencies. Individual items such as the orifice plate, guide baffle, nozzle plate, and splash baffles were not specifically modeled, but the mass effects were included. Frequencies of these components will be determined in the future using simplified hand methods.

The results of the analysis are shown in Figures 6.2, 6.3, and 6.4. The first horizontal mode frequency of the primary tank is 9.98 Hz. The first wall horizontal frequency is 8.67 Hz, and the plug assembly horizontal frequency is 11.87 Hz. The first vertical natural frequency of the assembly is 25.02 Hz. Continuing analysis will provide the exciting frequencies of the target chamber internal plates and baffles. The above frequencies were determined using a coarse-mesh, simplified, finite element model, and are intended only as an approximation of the critical assembly frequencies.

Table 6.1 summarizes the results of the vibration study. No frequency found is close to the repetition rate of 1.0 Hz.

TARGET CHAMBER
DYNAMIC MODEL

Figure 6.1

04/25/79
 IAXIS= 3 ALPHA= 0.00 BETA= 0.00

Figure 6.2
 PRIMARY TANK

Figure 6.3

LASER FUSION TARGET CHARGER DYNAMIC ANALYSIS --CASTLE-- 4-23-79
 UNDEFORMED SHAPE
 04/25/79
 AXIS= 3 ALPHA= 0.00 BETA= 0.00

FIRST WALL

MODE	1	5
FREQUENCY ω Hz	8.67	11.87

Figure 6.4

LASER FUSION TARGET CHAMBER DYNAMIC ANALYSIS --CASTLE-- 4-23-79

04/25/79
TAXIS= 3 ALPHA= 0.00 BETA= 0.00

PLUG

TABLE 6.1

FREQUENCY SUMMARY

COMPONENT	DESCRIPTION	WEIGHT LBS	FREQUENCY HZ
PRIMARY TANK		2,210,000	10.0
FIRST WALL		350,000	8.7
PLUG		63000	11.9
GRAPHITE SHIELD		934000	8.7
SLOSH BAFFLE		191000	142
ORIFICE PLATE		71000	41
GUIDE BAFFLE		156000	176
NOZZLE PLATE		119000	70
SPLASH BAFFLE		439000	58

Presented in Table 6.2 are the results of an analysis of the laser fusion target chamber splash baffle. The analysis was performed in order to verify the structural integrity of the baffle when subjected to pressures imposed by the lithium waterfall. The results indicate that the baffle stresses are very low for the applied pressures and that the maximum baffle deflections are small.

Table 6.2
**SPLASH BAFFLE STRESS & DEFLECTION
 SUMMARY**

CASE	STRESS ~ PSI			DEFLECTION ~ IN.		
	STATIC	PRESSURE	TOTAL	STATIC	PRESSURE	TOTAL
1. 6.48 PSI ON 9.6 FT. DIAMETER CENTRAL CIRCLE	600	445	1045	.03	.02	.05
2. 12.96 PSI ON 9.6 FT DIAMETER CENTRAL CIRCLE 1/2 SECOND ON, 1/2 SECOND OFF	600	890	1490	.03	.04	.07
3. 47.70 PSI ON 9.6 FT DIAMETER CENTRAL CIRCLE 0.01 SECOND EVERY SECOND	600	3280	3880	.03	.14	.17

7.0 FIRST WALL THERMAL STRESS

An investigation of thermal stresses in the first wall and other structures was undertaken. Thermal stresses can arise when heat is generated within relatively thick structures causing the internal regions to be hotter than the surface(s). Heat is generated by the production of gamma rays resulting from the absorption of neutrons.

The maximum thermal stress can be approximated by the following equation in which the first wall is considered to be a thin-walled cylinder cooled only on one side:

$$S = \frac{E \alpha Q T^2}{4 (1-\nu) K}$$

where S = maximum stress
 E = modulus of elasticity
 α = coefficient of expansion
 Q = power density
 T = wall thickness
 ν = Poisson's ratio
 K = thermal conductivity

If the wall is cooled on both sides, the stress is then reduced by a factor of 4 and is calculated from the following equation:

$$S = \frac{E \alpha Q T^2}{16 (1-\nu) K}$$

The heating rates in the HYLIFE first wall were estimated by W. Meier (Ref. 4) averaged over a cylindrical first wall for various lithium wall thicknesses. These heating rates are shown in Table 7.1, increased by 50% to be applicable to the midplane where the radiation is more intense:

TABLE 7.1
FIRST WALL HEATING RATES

Lithium Fall Thickness (cm)	Energy Deposited Per Pulse (MeV)	Power Density	
		(Mwt/m ³)	(Btu) hr/ft ³ x 10 ⁻⁶
20	1.65	97.8	9.46
40	1.05	62.2	6.02
60	0.60	35.6	3.44
80	0.37	21.9	2.12
100	0.26	15.4	1.49
120	0.18	10.6	1.03

The thermal stress depends on four properties of the structural material as shown in the above equations. Table 7.2 lists the properties of 2-1/4 Cr - 1 Mo ferritic steel at a temperature of about 600°C (1100°F) which were used in the approximate analysis:

TABLE 7.2
PROPERTIES OF 2-1/4 Cr - 1 Mo

	Metric	Engineering
K	0.346 W/Cm-K	20 Btu/hr-ft-°F
E	138 GPA	20 x 10 ⁶ psi
α	15.5 x 10 ⁻⁶ Cm/ Cm-K	8.6 x 10 ⁻⁶ in.- °F
ν	0.275	0.275

Figure 7.1 shows the temperature differences generated in target chamber first walls for various wall thicknesses and various protective lithium fall thicknesses. In this instance, the wall is cooled only on

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one side. The ΔT increases as the square of the wall thickness and also increases rapidly when the wall thickness is reduced.

Figure 7.2 shows the maximum thermal stress for the same conditions. The stress is seen to rapidly reach high levels for other than very thin first walls. Figure 7.3 shows the stresses when the wall is cooled on two sides. Here, as the thermal stress equations indicate, the stresses are reduced by a factor of 4. They are still, however, rather high stresses. It is not at all certain that a simple cylindrical first wall can have cooling performed effectively on two sides.

The significance of these temperatures and stresses can be seen by referring to Table 7.3 which gives the allowable stress values for various candidate materials for 100,000 hours at 538°C (1000°F):

TABLE 7.3
ALLOWABLE STRESS VALUES FOR CANDIDATE MATERIALS
(100,000 hours at 538°C (1000°F))

Alloy	Stress	
	MPa	ksi
2-1/4 Cr - 1 Mo	43	6.3
2-1/4 Cr - 1 Mo - Nb*	46	6.6
5 Cr - 1/2 Mo	39	5.6
9 Cr - 1 Mo	59	8.5
9 Cr - 2 Mo - V - Nb	121	17.5
12 Cr - 1 Mo - V - W	124	18.0
316	106	15.4

* Similar stress values are expected for the 2-1/4 Cr - 1 Mo - Ni - Nb steel.

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Using the reference conditions of a wall protected by 100 cm lithium and constructed of 2-1/4 Cr - 1 Mo, and assuming the allowable stress of 43 MPa (6300 psi), it is seen from Figure 7.2 that the first wall is limited to a thickness of 1.25 cm (1/2 in.). The design wall thickness of 5 cm (2 in.), which was based on an FY 1978 lithium impact study (Ref. 3), is evidently too thick from the standpoint of thermal stress. It is for this reason that new design concepts for the first wall and other chamber structures are needed to meet the combined problems of thermal stress and lithium impact.

8.0 FIRST WALL CONCEPTS

In view of the conflicting requirements that high thermal stresses and severe lithium impacts place on the first wall, an effort to come up with improved first wall concepts was initiated. The requirements and constraints of the first wall may be summarized as follows:

TABLE 8.1
FIRST WALL REQUIREMENTS

-
- o Achieve a 20 to 30-year lifetime.
 - o Be capable of relatively easy replacement.
 - o Survive 1 Hz pellet pulses.
 - o Function in lithium at 400 to 500°C (750 to 930°F).
 - o Survive intense radiation fluence.
 - o Withstand or avoid severe thermal stresses.
 - o Stabilize lithium motions.
 - o Protect chamber blanket and pressure vessel.
-

It was decided that a satisfactory first wall would need to have a finely divided and resilient structure that was well cooled, either actively or passively. A finely divided structure would have a large surface area which would facilitate proper cooling. A finely divided structure also has a very short heat path between the internal metal and its surface so, in principle, not only could the surface be kept cool, but the internal metal would be only slightly hotter than the surface and have only moderate thermal stresses.

Such a first wall may be configured in many ways. It is assumed that it will be of metal construction such as the ferritic alloy 2-1/4 Cr - 1 Mo. A simple concept would be to make the first wall out of several layers of metal wire screens that are welded or mechanically

fastened together. Such a construction lacks any positive means for lithium to flow through the layers to cool them. However, enough lithium may impact the screens following each pulse to provide adequate cooling.

The screens need not be fastened together in the radial direction or, as a compromise, they may be joined only at occasional points. In the circumferential direction, they may be continuous or in sections. They may also overlap. A continuous construction would provide a positive resistance to impacts but with the risk of high stress, while the discontinuous approach would provide a more resilient first wall that, however, might move about with a large amplitude.

Screen can be produced by industry in a wide variety of designs. Almost any combination of wire diameter and pitch can be considered, as shown in Figures 8.1 and 8.2. The wire diameter should be small enough to avoid thermal stresses while the spacing should be large enough to minimize any possible plugging by the lithium. The layers of screen should be thick enough to have a reasonable amount of inertia to provide stability to the first wall.

The wire screen would be hung from the top of the target chamber near the location of the nozzle plate. It will form a skirt just inside the graphite blanket that hangs down to the bottom of the chamber. The skirt may simply hang freely or it can be attached to the base of the chamber.

As it is a flexible structure, the screen-type first wall may change shape during operation. A ballooning effect at the equator and various vertical and azimuthal waves may occur. A design consideration is the nature and extent of such motions that can be permitted. The design of the first wall may allow for replacement during the life of the plant. This would be especially feasible if the screen wall is made of many narrow strips that could be removed from a small-diameter port at the top of the chamber.

Balanced
Belting

Rod
Reinforced
Belting

Sectional
Belting

Duplex
Belting

Grates
Belting

Double
Balanced
Belting

Compound
Balanced
Belting

Figure 8.1

metal mesh belting

Metal Mesh Belting is made of various metals which will withstand both atmospheric and chemical corrosion, or of an alloy which will resist heat treating furnace temperatures. Belts of stainless steel are used when danger of product contamination must be eliminated.

Nine basic constructions with mesh openings ranging from 1/84" to 2 1/2" and gauge sizes from 3 to 32 W & M gauge are available. Specific openings can be selected to suit the product being conveyed, and to permit exacting product treatment. Belt weight, tensile strength, and carrying capacity can be controlled by selecting the correct specification.

A variety of selvages, retaining edges, dividers, and flights are available to fit each belt to its particular job. Chain edges are furnished for applications requiring positive drive.

BALANCED BELTING

A belt of high tensile strength and low ultimate cost. Alternating spirals and crimped connector rods "lock" construction, thus promoting straight travel. This feature also provides resistance to elongation and width contraction.

ROD REINFORCED BELTING

Highest tensile strength combined with low thermal capacity make Rod Reinforced Belting an excellent choice for heavy duty, high temperature applications up to 2200° F.

SECTIONAL BELTING

Features low initial cost for a metal mesh type of belt. Assembly consists of alternating right and left hand sections of one-direction weave to counteract side-creeper tendencies.

OTHER METAL MESH BELTING

Duplex Belting is woven in pairs of spirals for close mesh, high strength, and smooth carrying surface. Grates is similar to Balanced Belting except spirals are closely wound to produce a semi-closed mesh. Has some advantages of Balanced construction plus greater strength. Double Balanced is similar to Balanced, except woven of pairs of spirals with greater angle to the weave for more opening. Compound Balanced offers densest weave and smooth surface.

GRATEX BELTING

The GRATEX belt is similar to the BALANCED belt, except that the spirals are more closely wound to produce a semi-closed mesh. Because of the proximity of the spirals to each other, straight connecting rods are used rather than the crimped connectors of the BALANCED construction. The closeness of the weave results in a stronger construction while retaining the advantages of the BALANCED weave.

Figure 8.2

$\frac{1}{16}$ " thick

G-21-8-4

$\frac{5}{16}$ " thick

G-42-10-6-10

$\frac{1}{2}$ " thick

G-35-21-6-10

GRATEX belting is designated by numerals indicating, respectively, number of "turns" or "loops" per foot of width, number of straight connectors per foot of length, and gauge of wire. When the gauge of the spiral differs from that of the straight connector, the third numeral indicates the gauge of the straight connector and the last numeral the gauge of the spiral.

Standard specifications shown in bold face.

Mesh Designation	Approx. Mesh	Dia. of Wire in.	Cross- Sec. Area sq. in./ft. ²	† Max. Working Tension lb./ft. ²	Weight lb./sq. ft.
G-21-8-4	$\frac{1}{2}$ "	.225	1.570	6680	10.00
G-35-21-6-10	3	.192-.135	1.002	4760	8.50
G-35-30-10	3	.135	1.002	4010	9.00
G-36-8-9-12	3	.148-.105	.629	2910	3.06
G-36-8-10-12	3	.135-.105	.629	2830	3.00
G-40-21-9-11	$3\frac{1}{2}$	.148-.120	.912	4105	6.50
G-42-10-6-10	$3\frac{1}{2}$	.192-.135	1.202	5710	6.50
G-42-22-9-12	$3\frac{1}{2}$	.148-.105	.734	3395	4.88
G-42-18-12	$3\frac{1}{2}$	.105	.734	2935	4.38

* Units per foot of belt width.

† For steel and stainless steel belts operating at temperatures to 600°F. maximum. For special conditions, higher temperatures, and an explanation of "Maximum Working Tension" see BELT ENGL. WEARING section.

CAMBRIDGE

Two different approaches were taken in first-wall conceptual design. In both of these approaches, the first wall consisted of 24 panels that were hung from the nozzle plate to form a protective curtain in front of the graphite blankets. Both Figures 8.3 and 8.4 show this concept. The panels are 10 cm (4 in.) thick, 1.5 m (60 in.) wide, and 8.0 m (26 ft) in height. The panels overlap to provide better wall protection.

The first approach is to fabricate the panels from several layers of woven-wire belting using wire made of 2-1/4 Cr - 1 Mo ferritic steel. Such a construction has the resiliency required to withstand the buffeting that it would receive from the lithium waterfall following each pulse. It also has a large surface area so that thermal stresses would tend to be small. This first wall would be passively cooled; that is, cooling is presumed to be effected by the lithium that drenches the first wall after each fusion pulse. The geometry of such a wire matrix panel would be carefully selected to ensure that all of the wires were properly cooled.

Table 8.2 lists the typical characteristics of a wire matrix first wall concept:

TABLE 8.2
WIRE MATRIX FIRST WALL CONCEPT

o	Wire Belt Construction 2-1/4 Cr - 1 Mo
o	6.35 mm (1/4 in.) Diameter Wire
o	Panel Construction (24 Panels):
	Height = 8 M (26 ft)
	Width = 1.5 M (60 in.)
	Thickness = 10 Cm (4 in.)
o	Some Panel Overlap
o	Hung from Top, Restrained at Bottom
o	Passive Cooling - Rely on Lithium Splash
o	Replacement Capability

Figure 8.3

WIRE MATRIX FIRST WALL CONCEPT

Figure 8.4

TUBULAR MATRIX FIRST WALL CONCEPT

Since cooling cannot be assured at this time in the above concept, an arrangement that does give positive lithium cooling was considered. This is shown in Figure 8.4. The panels are similar to the others in overall size and shape, but are built up of steel tubes with wire spacers. Lithium enters the tubes at the top of the chamber and leaves at the bottom providing positive cooling. The tube-and-wire matrix is expected to provide a resilient structure.

Table 8.3 lists the characteristics of a typical tubular matrix first wall concept:

TABLE 8.3
TUBULAR MATRIX FIRST WALL CONCEPT

o	Tube Construction with Wire Spacers
o	1.91 Cm (3/4 in.) O.D., 0.165 Cm (0.065 in.) Wall
o	2-1/4 - 1 Mo Ferritic Steel
o	Panel Construction (24 Panels):
	Height = 8 M (26 ft)
	Width = 1.5 M (60 in.)
	Thickness = 10 Cm (4 in.)
o	Some Panel Overlap
o	Hung from Top, Restrained at Bottom
o	Active Cooling (Lithium in Tubes)
o	Replacement Capability

9.0 INLET NOZZLE CONCEPTS

The ideas proposed for the first wall, namely, the use of finely divided, well cooled, and resilient structures, are also applicable to the chamber inlet nozzle. Two nozzle designs based on these principles have been considered. They both employ a grid-type nozzle region with a straight-down fall (although other fall types can readily be incorporated). At the center is a small circular plate from which lithium sprays into the main fall. The only differences between these nozzles is in the region between the outer surface of the fall and the first wall. In one concept (see Figure 9.1), this part of the nozzle is made up of tubes with wire belt spacers having a construction similar to the tube and wire first wall concept that was discussed previously. In the other concept (see Figure 9.2), this region is simply a well that is filled with lithium.

In the 1978 reference design, the nozzle plate used a modified ASME nozzle shape to create a jet fall that converged inward. The objectives of this design were to create a stable fall, cover the entire nozzle plate, and (by concentrating the lithium at the midplane) reduce the total lithium flow rate. However, a straight-down fall may also be satisfactory and, in addition, is a simple and low-cost approach. Local breakup of the fall is not likely to be a problem because of the statistically random nature of the fall. A straight-down fall protects the outer nozzle region adequately because of the high effective lithium density (low velocity) and the slant angle from the fusion pulse point through the fall to the nozzle plate. Concentrating the fall near the pulse point is not necessarily beneficial because the fall is then likely to be rapidly disassembled, with possible damage incurred by the first wall and other chamber structures. Finally, a straight-down fall with its finely divided and rain-like character will tend to minimize the propagation of shock waves and maximize the condensation of lithium vapor.

Figure 9.1

TUBULAR NOZZLE PLATE CONCEPT

Figure 9.2

BOX STRUCTURE NOZZLE PLATE CONCEPT

The central region of the nozzle has a lithium spray to reduce the effects of short-range radiations on this structure. This spray is not effective in preventing damage by the high-energy neutrons, therefore, the spray plate is designed so that its loads are relatively low. Also, it is designed to be easily removable so occasional replacement can be accomplished. This is compatible with the concept of occasionally replacing the first wall panels.

In the concept where the outer nozzle region is constructed of steel tubing and wire belting, the idea is to provide a resilient structure that is well cooled. The resiliency is provided by the tendency for the tube-wire array to deflect somewhat during the impact of lithium and thus avoid high stresses. Some of the main lithium flow is diverted through this tubing to both cool this region and provide additional energy absorption capability. This lithium continues downward to cool the first wall panels. Table 9.1 lists the features of this approach to nozzle design:

TABLE 9.1
TUBE NOZZLE CONCEPT

Tube Region

Is Outside of Fall Region
Cooled by Lithium that Enters First Wall
Finely Divided

Grid Region

Guides Lithium Fall
Well Cooled by Lithium
Finely Divided

Spray Region

Relatively Small, Central Region
Exposed to Fusion Pulse
Has Downward and Outward Lithium Spray
Is Designed to be Replaceable

In the other outer nozzle region concept, the lithium flow is contained in a well before it continues downward through the first wall. This is a simpler approach, but is more vulnerable to lithium impact.

Table 9.2 shows the characteristics of a plate nozzle with a grid and spray region:

TABLE 9.2
PLATE NOZZLE CONCEPT

Plate Region

Is Outside of Fall Region
Cooled by Inlet Lithium
Moderately Finely Divided

Grid Region

Guides Lithium Fall
Well Cooled
Finely Divided

Spray Region

Relatively Small, Central Region
Exposed to Fusion Pulse
Has Downward and Outward Lithium Spray
Is Designed to be Replaceable

10.0 OTHER CHAMBER CONCEPTS

A revised chamber support concept is shown in Figure 10.1. The support ring has been elevated several meters above the midplane to just above the nozzle region. It has been converted to a box structure that is well cooled by a lithium flow. This construction places the ring in a lower radiation environment, lessens the structure temperatures, and greatly reduces the inherent thermal stresses. This design is also structurally more efficient and is lower in cost. Table 10.1 lists the chief features of this improved chamber support method:

TABLE 10.1
CHAMBER SUPPORT CONCEPT

Main Support at Top of Chamber
Box Beam Ring
Laminated Construction
Cooled by Lithium
Located in Low-Radiation Region
Snubbers at Base of Chamber
Prevents Lateral Motion

Another concept has been to convert the inlet piping into an annular duct outside of the first wall and inside the pressure vessel. This lithium layer provides a large energy absorption capability so that the graphite blanket may be eliminated if desired. It is believed that the graphite blanket may prove to be a component that is high in cost and low in reliability. Figure 10.1 shows these nozzles, support ring, and inlet piping concepts. Table 10.2 shows the rationale for further consideration of this new approach to inlet piping, pressure vessel, and chamber blanket:

Figure 10.1

ADVANCED CHAMBER CONCEPT

TABLE 10.2
RISER PIPING CONCEPT

-
- o Riser Pipe is an Annulus Around Chamber
 - o Adds Lithium Layer Around Chamber
 - o Increases Energy Absorption
 - o May Eliminate Graphite Blanket
 - o Will Cool Pressure Vessel Wall
 - o Will Support Pressure Vessel Against Impacts
-

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PART B

MATERIALS CONSIDERATIONS

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MATERIALS CONSIDERATIONS

1.0 INTRODUCTION

A review of materials properties of various alloys in the previous contract period led to the selection of ferritic steel for the first wall of a laser fusion vessel. The principal reasons for this were lithium compatibility, adequate mechanical strength, resistance to radiation damage, and low cost. The alloy 2-1/4 Cr - 1 Mo was given particular attention because of its widespread use for pressure vessels. In the present report, ferritic steels with 9% and 12% chromium are considered because of their higher creep strength. However, for service temperatures of 500°C, it appears that the strength increment gained by a higher chromium content is more than offset by increased cost and possible future shortages of chromium.

The effects of radiation on graphite in bulk and fiber forms is reviewed. Graphite is not dimensionally stable during neutron irradiation and designs may have to allow for volume increases of 10%.

2.0 FERRITIC ALLOY HT9

2.1 SWELLING

The temperature dependence of swelling is shown in Figure 1.⁽¹⁾ At a fluence of $20 \times 10^{22} \text{ n/cm}^2$ ($E < 1 \text{ MeV}$), the swelling is about 0.75%.

An equation for swelling, taking into account data from transmission microscopy investigation and ion bombardment investigation, was developed by J. F. Bates et al.:⁽²⁾

$$\Sigma = R \left[\phi t + 1/\alpha \ln \left\{ \frac{1 + \exp \alpha (\tau - \phi t)}{1 + \exp(\alpha \tau)} \right\} \right] \quad (1)$$

where Σ = negative percentage density change

ϕt = fluence, 10^{22} n/cm^2

$\alpha = 0.75 (10^{22} \text{ n/cm}^2)^{-1}$

$R = 0.085 \left[-1 \times 10^{-4} (T - 500)^2 \right] \quad (\%/10^{22} \text{ n/cm}^2)$

$\tau = 10.0 (10^{22} \text{ n/cm}^2)$

$T = ^\circ\text{C}$

An investigation of swelling resistance of HT9 was made by F. A. Smidt, Jr.⁽³⁾ using a heavy ion bombardment with 2.8 MeV $^{56}\text{Fe}^+$ ions. His conclusions are:

FIGURE 1. TEMPERATURE DEPENDENCE OF SWELLING IN HT9

- 1) The maximum swelling at 150 dpa occurs at $\sim 500^{\circ}\text{C}$ (932°F)
- 2) The maximum swelling is $0.085\%/10^{22}\text{n/cm}^2$ ($E > 0.1\text{ MeV}$).

Thus, swelling will not pose any problem for a vessel made of HT9. Damage will accrue at about 2 dpa per year with 50 cm of lithium protection of 1 MW/m^2 . In 30 years, less than 0.5% swelling will occur.

2.2 RADIATION-INDUCED CREEP

Two articles, written by B. A. Chin and E. R. Gilbert^(4,5) give an empirical equation for in-reactor creep:

$$\bar{\epsilon} = \bar{B} \bar{\sigma}^n \phi t \quad (2)$$

$$\bar{B} = f(\phi t, T) \quad (2a)$$

$\bar{\epsilon}$ = effective strain

$\bar{B}$ = average creep coefficient

$\bar{\sigma}$ = effective stress

n = stress exponent (found to be $f(T)$)

ϕt = fluence

HT9 has good resistance to creep up to about 600°C; above this temperature, the resistance rapidly diminishes. Figure 2 shows a creep rate which is independent of fluence. This suggests that the microstructural processes associated with creep have reached a steady state. More data are given in the work of B. A. Chin and J. L. Straalsund⁽⁶⁾ on pressurized tubes. The data are summarized in Table 1.

TABLE 1
IN-REACTOR CREEP OF HT9

Temperature (°C)	Fluence (n/cm ²)	Stress (ksi)	Diametral Change $\Delta D/D_0$ %
445	4.3 x 10 ²²	8	0.08
		16	0.25
		24	0.43
	6.6 x 10 ²²	8	0.16
		16	0.43
		24	0.70
500	4.2 x 10 ²²	8	0.14
		16	0.37
		24	0.80
	6.8 x 10 ²²	8	0.16
		16	0.61
		24	1.20
560	4.4 x 10 ²²	4	0.08
		8	0.17
		16	0.55
	7.0 x 10 ²²	5	0.16
		9	0.30
		17	0.85
640	4.1 x 10 ²²	2.5	0.09
		4	0.26
		8	0.88
	6.5 x 10 ²²	2.5	0.17
		4.75	0.38
		9	1.14

FIGURE 2. LINEAR FLUENCE DEPENDENCE OF IN-REACTOR CREEP.

2.3 UNIRRADIATED STRESS-RUPTURE BEHAVIOR

B. A. Chin and J. L. Straalsund⁽⁶⁾ give an equation for the stress to rupture of unirradiated HT9:

$$\text{Log} \left[\text{Log} (\sigma^*/\bar{\sigma}) \right] = A + B \text{Log} \theta \quad (3)$$

$\bar{\sigma}$ = effective stress, MPa

$$\theta = t_r \exp (-Q/RT)$$

t_r = time to rupture, hr

T = temperature, $^{\circ}\text{K}$

For HT9, $\sigma^* = 700 \text{ MPa}$

$Q = 70,000 \text{ cal/mol}$

$A = 1.41$

$B = 0.109$

Table 2 summarizes the stress rupture data.

TABLE 2
STRESS-RUPTURE OF HT9

Temperature	Stress		
	56 ksi	70 ksi	80 ksi
482 $^{\circ}\text{C}$	10 ⁵ hr	10 ⁴ hr	10 ³ hr
538 $^{\circ}\text{C}$	3 x 10 ³ hr	3 x 10 ² hr	3 x 10 ¹ hr
593 $^{\circ}\text{C}$	10 ² hr	10 ¹ hr	10 ⁰ hr

2.4 EFFECT OF RADIATION ON TENSILE PROPERTIES

The effect of radiation on the tensile properties of HT9 is given in Table 3.⁽⁷⁾ Substantial increases in strength occur, and the retained ductility is good. The displacement damage is about 30 dpa, equivalent to that which would be produced in a vessel wall, protected by 40 cm of lithium, after about 10 years of constant exposure at 1 MW/m^2 . The probable helium content is about 5 appm, much less than the 100 appm expected to accrue for the same 10 years.

TABLE 3
TENSILE PROPERTIES OF HT9[†] AT 500°C

	Unirradiated	Irradiated*
Yield Strength	262 MPa	531 MPa
Ultimate Strength	462 MPa	593 MPa
Total Elongation	-	8%

[†]Heat treatment: .5h 1050C AC + 2.5h 780C AC

*Irradiated to a fluence of $5 \times 10^{26} \text{ n/m}^2$ (E> .1 MeV) at 500°C

3.0 FERRITIC ALLOY 9 Cr - 1 Mo - V + Nb

This steel derives its strength characteristics through the control of the microstructure via the addition of V, Nb, and B.⁽⁸⁾ The yield and tensile strengths of this steel at 500°C are compared with those of 2-1/4 Cr - 1 Mo and HT9 in Table 4. The temperature dependence of the yield and tensile strength of these steels are given in Figures 3 and 4; Type 316 stainless steel is shown for comparison. The strength of 2-1/4 Cr - 1 Mo exceeds that of the other ferritic steels at all temperatures.

TABLE 4
STRENGTH OF FERRITIC STEELS* AT 500°C

Steel	Yield (MPa) [†]	Tensile (MPa)
2-1/4 Cr - 1 Mo ^{**}	400	510
9 Cr - 1 Mo - V + Nb	359	476
HT9	262	462

[†]100 MPa = 14,500 psi

*Normalized and tempered

**Data taken from Reference 9

Fig. 3 COMPARISON OF 0.2% YIELD STRENGTH OF 9Cr-1Mo, 2 1/4Cr-1Mo, HT9, AND 316 SS

**Fig. 4 COMPARISON OF ULTIMATE TENSILE STRENGTHS OF
9Cr-1Mo, 2 $\frac{1}{4}$ Cr-1Mo, HT9, AND 316SS**

4.0 COMPARATIVE STRESS-RUPTURE BEHAVIOR OF FERRITIC ALLOYS

The stress-rupture behavior for the three ferritic steels is shown in Tables 5 and 6. At temperatures near 600°C (chosen for comparative purposes), 2-1/4 Cr - 1 Mo has the weakest stress-rupture strength. This also is the case near 500°C, but the margin is much reduced. Figure 5 gives a comparison of the stress-rupture behavior of 2-1/4 Cr - 1 Mo and 9 Cr - 1 Mo with and without V + Nb. The 9 Cr - 1 Mo - V + Nb is superior even when the test is made at 538°C as compared to 454°C for 2-1/4 Cr - 1 Mo. ⁽¹⁰⁾ Also shown are the results of test on a much weaker commercial 9 Cr - 1 Mo steel without the additions of V and Nb.

TABLE 5
STRESS-RUPTURE OF FERRITIC STEELS[†] IN 10,000 HR

Steel	Temperature (C)			
	510	565	593	600
2-1/4 Cr - 1 Mo	193*	110	-	-
9 Cr - 1 Mo - V + Nb	-	-	150	-
HT9	281	-	-	110

[†]Normalized and tempered

*Values in MPa

TABLE 6
STRESS-RUPTURE OF FERRITIC STEELS[†] IN 100,000 HR

Steel	Temperature (C)			
	510	565	593	600
2-1/4 Cr - 1 Mo	-	15*	-	-
9 Cr - 1 Mo - V + Nb	-	-	125	-
HT9	-	-	-	65

[†]Normalized and tempered

*Values in MPa

FIGURE 5 STRESS-RUPTURE OF SEVERAL FERRITIC STEELS

5.0 EFFECT OF RADIATION ON FATIGUE OF FERRITIC STEEL

There are no data on the effect of radiation on fatigue of ferritic steels at temperatures of interest for a first wall. In order to make an estimate of what such an effect might be, it is necessary to review the case of austenitic stainless steel. This effect is presented in terms of crack growth rate based on linear elastic fracture mechanics.

5.1 FRACTURE MECHANICS MODEL

Linear elastic fracture mechanics (LEFM) was originally developed to explain static failure in brittle materials. The basic assumption in LEFM is that a crack or crack-like defect exists in the component.

The model is derived from the definition of a stress intensity factor, k , which is a measure of the magnitude of the stress in the vicinity of the crack tip and is a function of the crack size, nominal stress, and crack and member geometry. Because fatigue crack propagation is a localized phenomenon that depends on the magnitude of stresses at the crack tip, the stress intensity concept provides a quantitative interpretation of fatigue crack growth.

Crack growth can be modeled in the following way:⁽¹³⁾

$$da/dN = f(\Delta\sigma, a, c) \quad (4)$$

da/dN = change in crack length per cycle of applied load

$\Delta\sigma$ = alternating stress range

a = crack length

c = constant dependent upon part geometry, mean load, other conditions.

This equation assumes that the subcritical fatigue crack growth per cycle is small compared to the crack length, a . The stress intensity factor, k , is also a function of applied nominal stress and the crack length, therefore,

$$da/dN = g(\Delta k) \quad (5)$$

Experience indicates that

$$da/dN = C_0 (\Delta k)^n \quad (6)$$

where:

C_0 = empirical constant that is material dependent

Δk = stress intensity factor range

n = slope of the $\log da/dN$ vs $\log \Delta k$ plot.

Equation 6 can be integrated to yield cycles-to-failure, N_f . The stress intensity factor range is expressed as

$$\Delta k = A\Delta\sigma\sqrt{a} \quad (7)$$

where:

A = factor reflecting specimen and crack geometry

N = integrated from 0 to N_f

a = integrated from initial crack length a_i to a_c .

The resulting expression is:

$$N_f = \frac{2}{C_0 (\Delta S)^n A^n (n-2)} \left[a_i^{(2-n)/2} - a_c^{(2-n)/2} \right] \quad (8)$$

The critical crack length can be determined from Equation 7,

$$a_c = (k_c / A \sigma_{\max})^2 \quad (9)$$

where k_c is the critical stress intensity factor. Laboratory testing is done using precracked or notched specimens and the advance of the crack is measured as a function of cycles.

5.2 COMPARISON OF FERRITIC AND AUSTENITIC STEELS

J. M. Barson⁽¹⁴⁾ gives equations for fatigue-crack growth at room temperature for several ferritic steels, some plain carbon and some low alloy:

$$da/dN = 3.6 \times 10^{-10} (\Delta k_I)^{3.0} \quad (10)$$

and several 300 Series austenitic stainless steels;

$$da/dN = 3.0 \times 10^{-10} (\Delta K_I)^{3.25} \quad (11)$$

Based on tests up to about 10^5 cycles, there is little difference between them.

5.3 EFFECT OF RADIATION ON FATIGUE-CRACK GROWTH OF TYPE 304 STAINLESS STEEL

D. J. Michel^(15,16) reviewed the experimental results concerning the effects of fast-neutron irradiation on fatigue-crack growth in austenitic stainless steels. The techniques of LEFM have been successfully applied to relate the crack growth rate to the stress intensity factor. Figure 6 gives the results of irradiated and unirradiated Type 304 stainless steel. Based on data up to 10^5 cycles, the conclusions from these reports are:

- 1) Neutron radiation up to fluences of $5 \times 10^{26} \text{ n/m}^2$ produced no significant effect on crack propagation rate of annealed austenitic stainless steel in air when the test temperature ($+27^\circ\text{C}$) is within $\pm 50^\circ\text{C}$ of the irradiation temperature.
- 2) An increase in irradiation and test temperatures to 593°C produced a small increase in crack propagation rate in annealed austenitic stainless steel when compared with the unirradiated condition at 593°C .

Fig. 6. Effect of Irradiation on Fatigue-Crack Growth in Type 304 Stainless Steel (Ref. 15, 16)

- 3) There is no significant effect of neutron radiation at 427°C on crack propagation rate in the cold-worked steels.
- 4) An increase in irradiation and test temperatures of the cold-worked steel from 427 to 593°C results in a substantial increase in crack propagation rate which exceeds the increase produced in unirradiated material by the equivalent increase in test temperature.

5.4 FATIGUE-CRACK GROWTH IN FERRITIC STEELS

C. R. Brinkman, et al.⁽¹⁷⁾ shows in Figure 7 the effect of temperature and frequency of fatigue-crack propagation in 2-1/4 Cr - 1 Mo steel where R is the ratio of minimum-to-maximum load. The data indicate only a small frequency effect over the range 40-300 cycle/min at 510°C at 3×10^6 cycles. The rate of crack growth increases markedly with increasing mean stress. The results of a number of tests at 510°C are shown in Figure 8. The data were reanalyzed with the concept of effective stress intensity factor

$$k_{\text{eff}} = k_{\text{max}} (1-R)^m \quad (12)$$

where m varies with the material. For 2-1/4 Cr - 1 Mo, $m = 0.5$ gave the results shown to the right in Figure 8.

5.5 EFFECT OF LOW-TEMPERATURE RADIATION ON FATIGUE OF FERRITIC STEELS

The fatigue-crack propagation behavior of A533-B steel was studied within the framework of linear-elastic fracture mechanics.⁽¹⁸⁾ The

Fig. 7. Effect of temperature and frequency of fatigue crack propagation of 2 1/2 Cr-1 Mo steel.

Fig. 8. Effect of stress ratio on fatigue crack propagation of 2½Cr-1 Mo steel at 950°C (510°C) in air.

A533-B steel has this nominal chemical composition: C, 0.22; Mn, 1.48; P, 0.012; S, 0.018; Si, 0.25; Ni, 0.68; Mo, 0.52 (wt %). Tests were conducted at 24°C (75°F) and 288°C (550°F) on unirradiated material and on material irradiated at 288°C to $2.3\text{--}2.8 \times 10^{23} \text{ n/m}^2$ and $5.3\text{--}5.7 \times 10^{23} \text{ n/m}^2$ ($E > 1 \text{ MeV}$). The heat treatment included: normalizing at 915°C, 4 h, air cool; austenizing at 870°C, 4 h, water quench; tempering at 665°C, 4 h, furnace cooled; stress relief of the cut sections was done at 620°C for 40 h followed by a furnace cool.

A sinusoidal loading wave form was employed at 10 Hz (600 cpm), and the cyclic stress ratio, $R = \sigma_{\min}/\sigma_{\max}$, was zero for all tests. Crack lengths were determined optically using a traveling microscope. In the case of the irradiated specimen, the reading was taken through a lead-glass viewing window. The specimens were precracked at least 1.3 mm (0.05 in.). Fatigue crack-growth rates, da/dN , were based on the incremental change in crack length between reading, Δa , divided by the number of loading cycles for that increment, ΔN . Stress intensity factors were computed from Equations 13 and 14.

For the wedge opening load specimen (irradiated specimen),

$$K = (P/B\sqrt{a}) \left[30.96 (a/w) - 195.8 (a/w)^2 + 730 (a/w)^3 - 1186.3 (a/w)^4 + 754.6 (a/w)^5 \right] \quad (13)$$

For the compact tension specimen (unirradiated specimen),

$$K = (P/B\sqrt{w}) \left[29.6 (a/w)^{0.5} - 185.5 (a/w)^{1.5} + 655.7 (a/w)^{2.5} - 1017.0 (a/w)^{3.5} + 638.9 (a/w)^{4.5} \right] \quad (14)$$

where:

K = stress intensity
P = applied load
B = specimen thickness
a = crack length
w = specimen width

using the average crack length for each increment. The stress intensity factor range, ΔK , was calculated using Equation 15.

$$\Delta K = K_{\max} - K_{\min} \quad (15)$$

The data were plotted as $\log (da/dN)$ versus $\log (\Delta K)$, and as will be shown, the results satisfied the general Equation 16 for crack propagation:

$$da/dN = C (\Delta K)^n \quad (16)$$

where: C, n are constants for a given material/environment combination.

The results for the tests conducted at 24°C and 228°C are given in Figures 9 and 10. In both sets of data, there are some suggestions of a slope transition at lower values of ΔK . In general, the unirradiated material test results of this study are in good agreement with those of other investigators (test under similar conditions).

The results shown in Figures 9 and 10 lead to several observations. First, there is essentially no difference in the cracking behavior of unirradiated material at 24°C and at 288°C. However, Watson et al.⁽¹⁹⁾ noted an increase in the fatigue crack-growth rate with increasing temperature when the testing was carried at a frequency of 0.16 Hz (10 cpm). Second, crack-growth behavior of unirradiated and irradiated material are equivalent at 24°C as evidenced by Figure 9. This is generally consistent with the data of Watson et al.⁽¹⁹⁾ in Figure 11. Third, the fatigue crack-growth rate at 288°C of the unirradiated material appears to be slightly higher than that for the irradiated material as revealed by Figure 10. Also, there is no difference between the results for fluences of $2.3\text{--}2.8 \times 10^{23} \text{ n/m}^2$ and those of $5.3\text{--}5.7 \times 10^{23} \text{ n/m}^2$. However, Watson's et al. results showed a slightly higher crack-growth rate for the irradiated samples tested at 288°C, see Figure 12.

Frequency, temperature, and neutron fluence are not considered to be major variables in these tests. Thus, the differences noted above between the work of James et al.⁽¹⁸⁾ and Watson et al.⁽¹⁹⁾ are considered within the normal scatter of the data for this type of test. Therefore, the conclusion is that there is no difference in crack-growth rate between irradiated (at 288°C and up to $5.7 \times 10^{23} \text{ n/m}^2$) and unirradiated samples tested at 288°C.

C. R. Brinkham et al.⁽²⁰⁾ gave results of monotonic tensile tests on 2-1/4 Cr - 1 Mo conducted over the temperature range 200°C to 500°C that

Fig. 9. Fatigue-crack propagation behavior of ASTM A533 Grade B, Class 1 steel at 75°F (24°C). Irradiation temperature was 550°F (288°C).

Fig. 10. Fatigue-crack propagation behavior of ASTM A533 Grade B, Class 1 steel at 550°F (288°C). Irradiation temperature was 550°F (288°C).

Fig. 11. Data of Watson et al. for ASTM A533 Grade B, Class 1 steel tested at 77°F (25°C).

Fig. 12. Data of Watson et al. for ASTM A533 Grade B, Class 1 steel tested at 550°F (288°C).

showed a peak in ultimate strength and an associated decrease or minimum in ductility. This was attributed to dynamic strain aging. The peak in ultimate strength was also strain-rate dependent and generally decreased with increasing strain rate for annealed (furnace-cooled) material. They point out that subtle differences in chemistry, temperature, heat treatment, and strain rate are likely to result in hardening characteristics that will persist to very low strain ranges, and therefore, have a marked influence on the long-life fatigue resistance of this material (Figures 13 and 14). Figure 15 plots the stress to maintain a constant strain range at half the cycle life versus temperature. It shows that there is a peak in the region between 315°C and 430°C, and that this peak is a function of strain range, alloy chemistry, and thermomechanical treatment. This peak in stress range is in the same temperature region as the peak in ultimate strength observed in the tensile tests.

Their conclusions are:

- 1) Cycle life is both temperature- and strain-rate dependent (strain range of 0.5% over the temperature range 315°C to 593°C and strain rate of 4×10^{-3} to 4×10^{-5} sec⁻¹). The peak in cycle hardening generally occurred for all heats at 371°C. Decreasing the strain rate results in lower cyclic lifetimes over this temperature interval. Maximum reduction occurs at higher temperatures (482°C, 593°C).

Fig 13. Total, elastic and plastic strain range vs cycles to failure for 2 1/4 Cr-1 Mo steel (isothermally annealed) at 427°C, showing transition fatigue life, N_{IT} , and break points, N_B .

Fig 14. Stress range at $N_f/2$ as a function of temperature at several total strain ranges ($\Delta \epsilon_t$) for four heats of 2 1/4 Cr-1 Mo steel.

FIGURE 15 FATIGUE OF 2-1/4 Cr - 1 Mo and 9 Cr - 1 Mo - V + Nb
UNDER TENSION - COMPRESSION LOADING

- 2) Compressive hold periods introduced in each cycle generally reduces the fatigue lifetime over the temperature range 371°C to 593°C. At high strain ranges (1 to 2%), hold periods have only a small effect on lifetimes; cycle life is generally not dependent upon the type of hold period. However, at strain ranges equal to or less than 0.5%, the compressive hold periods are much more deleterious to fatigue lifetime than are the tensile holds. Further, heat-to-heat variations in cycle lifetime are more pronounced at the lowest strain ranges.
- 3) With increasing hold period and temperatures equal to or less than 482°C, the limited data suggest that there is a tendency towards a smaller reduction in fatigue life. Further, with increasing hold time, there is an indication of diminished variation in lifetime due to heat-to-heat variations.

In summary, irradiation does not seem to have a major influence on the fatigue behavior of ferritic steels, up to 300°C and a fluence of $5 \times 10^{23} \text{ n/m}^2$. Based on Watson's work,⁽¹⁹⁾ however, a small effect may be anticipated at higher temperatures and fluences.

Results on austenitic stainless steels indicate that irradiation at 427°C and fluences up to $5 \times 10^{26} \text{ n/m}^2$ have no effect on the crack growth rate. Testing at a temperature above that of the irradiation temperature (593°C in this case) did result in a small increase in crack growth rate. According to the literature, the crack growth rate of austenitic stainless steel is greater than that of the ferritic steels.

Thus, we conclude that up to 427⁰C, there is probably no influence of irradiation on fatigue of 2-1/4 Cr - 1 Mo as long as the cyclic stresses occur at the same temperature as the irradiation. Changes in temperature over a long time period, however, are unknown. Changes in strain range have a deleterious effect on the high-cycle fatigue life. The effect of irradiation on this phenomena is also unknown.

6.0 FATIGUE OF UNIRRADIATED 2-1/4 Cr - 1 Mo AND 9 CR - 1 Mo - V + Nb

The fatigue strength of 9 Cr - 1 Mo - V + Nb (C-E) is better than that of 2-1/4 Cr - 1 Mo above 5×10^4 cycles. Figure 15 shows that 9 Cr - 1 Mo tested at 593°C has a longer lifetime than the 2-1/4 Cr - 1 Mo tested at 427°C above 5×10^4 cycles. (12, 21)

7.0 IMPACT STRENGTH OF FERRITIC STEELS

Table 7 gives the values for the Ductile-Brittle Transition Temperature (DBTT) for the ferritic steels. The DBTT of 2-1/4 Cr - 1 Mo quenched and tempered is lower than that of 9 Cr - 1 Mo - V + Nb (C-E) but both are under -30C. Figure 16 shows the impact strength as a function of temperature for HT9 and 9 Cr - 1 Mo - V + Nb.

TABLE 7
IMPACT STRENGTH OF FERRITIC STEELS

Steel	68 Joule Transition Temperature °C	Upper Energy Shelf (Joule)
2-1/4 Cr - 1 Mo Normalized and Tempered	-17.8°C (0°F)	170.8J (125 ft-lb)
2-1/4 Cr - 1 Mo Quench and Tempered	-55°C (-67°F)	158.6J (117 ft-lb)
HT9 (12 Cr)	5.6°C (42°F)	180.3J (133 ft-lb)
C-E 9 Cr - 1 Mo -V + Nb	-37.8°C (-36°F)	202.0J (149 ft-lb)

FIGURE 16 IMPACT STRENGTH AS A FUNCTION OF TEMPERATURE
FOR HT9 AND 9 Cr - 1 MO - V + Nb

8.0 GRAPHITE

8.1 TYPES OF GRAPHITES

The term graphite has been applied to a variety of materials in which the structure consists of a series of parallel layers of carbon atoms arranged on a hexagonal network. The most obvious feature of the graphite structure is that it is extremely anisotropic. The distance between the neighboring atoms in the layer plans is 141 pm, as compared with a spacing of 336 pm between neighboring planes.

Graphite can be manufactured from almost any organic material that leaves a high carbon residue upon heating. Petroleum coke is used extensively as a filler or dry material for graphite. Other filler materials include: carbon black, natural graphite, and anthracite. Properly sized filler material is blended with a carbonaceous binder to form a mix which may then be either extruded or molded and then fired to a prescribed temperature which may be as high as 3000°C or as low as 700°C.

Graphite is unique in its position among materials of construction, resembling both ceramics and metals in some of its properties. Its extremely high binding energy gives it the highest temperature stability of elemental solids. It has no liquid phase below about 10 MPa (100 atm) of pressure, and it sublimes at about 3700°C at 0.1 MPa (1 atm).

Graphitizing carbons begin to become ordered on heating through the range 1200-2200°C, and, in most cases, further crystal growth takes place on heating to still higher temperatures. A nongraphitizing carbon does not have a three-dimensional order even when heated for long periods over 2000°C. Natural graphite usually contains a large amount of impurity, and, although it can be purified chemically, it is not normally used in nuclear reactors.

Pyrolytic graphite is formed by decomposition of simple gaseous hydrocarbons at high temperature. A deposit of carbon is formed on the substrate; the form of the deposit varies greatly with conditions in the chamber, the total pressure, the partial pressure of the hydrocarbon, and the temperature of the substrate. The deposits are polycrystalline, but, under good conditions, there is a high degree of preferred orientation of the layer plane.

Synthetic graphite is produced by decomposition of solid hydrocarbons followed by heat treatment to promote crystal growth. The size of the particles in the bulk synthetic graphite is determined by the particle size of the starting material. Each particle contains many crystallites which are usually between 20-200 nm across. The density of the particles is 2.1-2.2 Mg/m³. The theoretical crystal density is 2.27 Mg/m³. Typical properties of nuclear graphite and other types of graphite that have been used in irradiation experiments are given in Table 8.⁽²²⁾

TABLE 8 TYPICAL PROPERTIES OF VARIOUS GRAPHITES AT ROOM TEMPERATURE (22)

Type of Graphite	Direction	Apparent Density (g.cm ⁻³)	Particle Density (g.cm ⁻³)	Electrical Resistivity (ohm. cm)	Thermal Conductivity (c.g.s. units)	Thermal Expansion (per °C)
Natural Crystals	c-axis	2.26	—	0.01 ~ 1.0†	~ 0.2‡	27 × 10 ⁻⁶
	a-axis			0.38 × 10 ⁻⁴	~ 1	- 1.3 × 10 ⁻⁶
Compacted Natural Graphite	// pressing	2.24	—	4.3 × 10 ⁻³	—	17 × 10 ⁻⁶
	⊥ pressing			9.3 × 10 ⁻⁴	—	2.5 × 10 ⁻⁶
Pyrolytic*	c-axis	2.1	—	~ 1.0	0.013	23.6 × 10 ⁻⁶
	a-axis			0.53 × 10 ⁻⁴	2.77	- 0.23 × 10 ⁻⁶
Synthetic Graphite (1)	// extrusion	1.6	2.15	11 × 10 ⁻³	0.34	1.2 × 10 ⁻⁶
	⊥ extrusion			15 × 10 ⁻³	0.27	2.3 × 10 ⁻⁶
Synthetic Graphite (2)	// extrusion	1.75	2.15	7.5 × 10 ⁻³	0.45	0.88 × 10 ⁻⁶
	⊥ extrusion			11.3 × 10 ⁻³	0.30	2.7 × 10 ⁻⁶

* Unpublished data given by R. Taylor.

† The higher value is preferred (see Chapter 4).

‡ Smith (1954). See also Ubbelohde and Lewis (1960).

8.2 EFFECT OF RADIATION ON BULK GRAPHITE

The crystalline structure of graphite is generally highly anisotropic. For this reason, radiation-induced property changes are strongly related to this structure. The interstitial, occupying a position between basal planes, has a very low activation energy of 0.45 eV for motion parallel to the basal planes but a large energy of 2.8 eV for motion in the C-axis direction.⁽²³⁾

Figures 17 and 18 show the behavior of graphite with 5 and 20% initial porosity irradiated at temperatures between 400-1600°C.⁽²³⁾ We can see that the percent volume change in the case of 5% initial porosity at 400°C increases with increased doses up to 50 dpa from 0 to 5%. In the case of 20% initial porosity, there is a decrease in percent volume change which gets leveled at -5% from 20-50 dpa.

Results of tests that were conducted on three kinds of graphite are given below. The test conditions were: irradiation temperatures 427-927°C (800-1700°K) with neutron fluences up to 1.1×10^{26} n/m² (11 dpa).

H-451

H-451 nuclear-grade graphite made by Great Lakes Carbon Corporation is a near isotropic, coarse grained, extruded product that was developed

INITIAL POROSITY = 5%

FIGURE 17. Irradiation Induced Volume Change in Graphite
(Initial Porosity = 5%)

FIGURE 18. Irradiation Induced Volume Change in Graphite
(Initial Porosity = 20%)

for HTGR. It has a density of 1.77 Mg/m^3 (77% of theoretical density). The H-451, after continuous fast-neutron irradiation, first shrinks then expands.⁽²⁴⁾

Tests on similar near-isotropic nuclear-grade graphite which took place at Dounreay Fast Reactor between $327\text{--}727^\circ\text{C}$ (600–1000K) have extended the fluence to $5 \times 10^{26} \text{ n/m}^2$ (50 dpa)⁽²⁵⁾. Similar results were obtained.

AXF

AXF grade produced by the POCO Graphite Company has a finer grain, a strength about 3-4 times higher, and a fatigue life about 50% higher than H-451 in the unirradiated condition. It also shrinks and then expands during irradiation.

CVD

CVD pyrocarbon has high strength and good stability under neutron irradiation. It does not shrink or expand during irradiation as much as H-451 and AXF and is not as highly anisotropic as the more familiar pyrolytic graphite.

Table 9 (a,b,c) shows values for the thermal and mechanical properties of unirradiated and irradiated graphites and isotropic pyrocarbon. Table 10 shows the thermal and mechanical properties of CVD

TABLE 9 (a)
H-451 GRAPHITE: THERMAL AND MECHANICAL PROPERTIES (24)

Property	Direction in Log	Unirradiated Value at:					Value After Irradiation to 10^{26} n/m ² at:				
		300K	800K	1300K	1800K	2300K	300K	800K	1300K	1800K	2300K
Thermal Conductivity (W/m-K)	Axial	141	82	58	48	42	--	25	34	41	42
	Radial	134	79	55	46	40	--	23	31	39	40
Thermal Expansivity (10 ⁻⁶ K ⁻¹)	Axial	4.0	4.0	4.6	5.0	5.4	--	4.6	2.6	2.5	--
	Radial	4.5	4.5	5.1	5.5	5.9	--	5.2	2.9	2.8	--
Specific Heat, C _p (J/kg-K)	--	710	1620	1890	1990	2050	710	1620	1890	1990	2050
Tensile Strength (MPa)(a)	Axial	13.7	15.4	17.1	18.9	20.6	--	23.1	28.4	26.7	--
	Radial	10.8	12.2	13.5	14.9	16.2	--	18.3	22.4	21.1	--
Flexural Strength (MPa)(a)	Axial	20.3	22.9	25.4	28.0	30.5	--	34.4	42.1	39.6	--
	Radial	17.4	19.6	21.8	24.0	26.1	--	29.4	36.2	33.9	--
Compressive Strength (MPa)(a)	Axial	51.9	58.4	64.9	71.4	77.9	--	87.6	107.6	101.0	--
	Radial	49.6	55.8	62.0	68.4	74.9	--	83.7	102.8	96.7	--
Young's Modulus (GPa)(a)	Axial	7.9	8.5	9.1	9.7	10.3	--	19.1	25.0	19.4	--
	Radial	6.9	7.4	8.0	8.5	9.0	--	16.7	22.0	17.0	--
Poisson's Ratio(a)	All	0.11	0.11	0.11	0.11	0.11	--	0.11	0.11	0.11	--
Tensile Fatigue Limit (99% survival to 10 ⁵ cycles, MPa) (a)(b)	Axial	6.0	6.8	7.5	8.3	9.0	--	11.8	14.5	13.6	--
	Radial	5.4	6.1	6.8	7.4	8.1	--	9.9	12.1	11.4	--

(a) Data at high temperatures extrapolated. (b) Tension-compression stress cycle.

TABLE 9 (b)
POCO ANF GRAPHITE: THERMAL AND MECHANICAL PROPERTIES (24)

Property	Unirradiated Value at:					Value After Irradiation to 10^{26} n/m ² at:				
	300K	800K	1300K	1800K	2300K	300K	800K	1300K	1800K	2300K
Thermal Conductivity (W/m-K)	100	63	45	37	32	--	18	25	31	32
Thermal Expansivity (10 ⁻⁶ K ⁻¹)	7.1	7.1	7.6	8.1	8.5	--	7.7	2.4	2.8	--
Tensile Strength (MPa)	33	37	41	46	50	--	56	67	61	--
Flexural Strength (MPa)	55	62	69	76	83	--	95	113	101	--
Compressive Strength (MPa)	110	124	137	152	165	--	189	225	201	--
Young's Modulus (GPa)	11.9	12.8	13.7	14.6	15.5	--	29.8	36.8	25.6	--
Poisson's Ratio	0.25	0.25	0.25	0.25	0.25	--	0.25	0.25	0.25	--
Tensile Fatigue Limit (99% survival to 10 ⁸ cycles, MPa)	16	17	19	22	24	--	--	--	--	--

TABLE 9 (c)
LTI PYROCARBON: TYPICAL THERMAL AND MECHANICAL PROPERTIES (a)(24)

Property	Unirradiated Value at:					Value After Irradiation to 10^{26} n/m ² at:				
	300K	800K	1300K	1800K	2300K	300K	800K	1300K	1800K	2300K
Thermal Conductivity (W/m-K)	10.0	7.1	7.5	7.5	7.5	10.0	7.1	7.7	12.1	12.8
Thermal Expansivity (10 ⁻⁶ K ⁻¹)	3.8	5.7	--	--	--	--	6.0	--	4.6(b)	--
Tensile Strength (MPa)	250	280	320	290	265	--	330	380	350	--
Flexural Strength (MPa)	380	420	480	440	400	--	500	575	530	--
Compressive Strength (MPa)	760	840	960	880	800	--	1000	1150	1060	--
Young's Modulus (GPa)	18	19.4	20.7	20.0	18.5	--	50.0	50.0	36.0	--
Poisson's Ratio	0.22	0.22	0.22	0.22	0.22	0.22	0.22	0.22	0.22	0.22
Tensile Fatigue Limit (MPa, 99% survival to 10 ⁸ cycles)	250	280	320	290	265	--	330	380	350	--

(a) LTI pyrocarbon properties are adjustable within fairly wide ranges through proper selection of deposition conditions, and the properties above are to be regarded only as typical mid-range values.

(b) Estimate.

TABLE 10
CVD β -SILICON CARBIDE: THERMAL AND MECHANICAL PROPERTIES (24)

Property	Unirradiated Value at:				Value After Irradiation to 10^{26} n/m ² at:			
	300K	800K	1300K	1800K	300K	800K	1300K	1800K
Thermal Conductivity (W/m-K)	67	37	25	15	--	9	12	15
Thermal Expansivity (10^{-6} K ⁻¹)	2.9	4.9	4.9	5.0	2.9	4.4	4.9	5.0
Specific Heat, C_p (J/kg-K) (b)	690	1150	1255	1360	690	1150	1255	1360
Tensile Strength (MPa) (a)	360	360	450	560	360	360	450	560
Flexural Strength (MPa) (b)	830	830	1030	1300	830	830	1030	1300
Compressive Strength (MPa) (d)	2900	2900	3600	4500	2900	2900	3600	4500
Young's Modulus (GPa) (b)	430	400	370	230	430	400	370	230
Poisson's Ratio (c)	0.24	0.24	0.24	0.24	0.24	0.24	0.24	0.24
Tensile Fatigue Limit, (99% survival to 5×10^5 cycles, MPa) (c)	290	290	360	450	--	--	--	--

(a) As-deposited surface. Temperature dependence assumed similar to flexural strength. (b) Polished surface. (c) Data for different type of silicon carbide. (d) Assuming compressive/tensile strength ratio of 8.

silicon carbide for comparison. H-451 at 427°C (800K) has a higher thermal expansion after irradiation while the thermal conductivity is lower. The Young's modulus, after irradiation, is more than twice the unirradiated value. The tensile fatigue life after irradiation is also higher.

The same trends in the results are observed for POCO AXF graphite after irradiation. The LTI pyrocarbon shows hardly any change in the properties after irradiation to 10^{26} n/m². This same result also applied to CVD silicon carbide, see Table 10.

8.3 RADIATION CREEP IN GRAPHITE

When graphite is irradiated under an external stress, it undergoes plastic deformation in the direction of the stress in addition to the dimensional changes which would occur without stress. Observed irradiation creep strains are at least an order of magnitude larger than any strain found in graphite in the absence of irradiation.

Transient, or first-stage creep, can be described by the equation (Reference 26):

$$\epsilon_1 = \frac{\rho \sigma}{E} (1 - \exp^{-aN}) \quad (17)$$

where:

σ = stress

E = Young's modulus

N = neutron fluence

P = constant nearly equal to 1

a = function of temperature

Transient creep strain is relatively unimportant compared with steady-state creep strain which is given by (Reference 24):

$$\epsilon_{II} = K\sigma N \quad (18)$$

where K is a proportionality factor called the radiation-creep coefficient. Technologically, K is the most important factor to know. It is found inversely proportional to Young's modulus and dependent upon the neutron flux. (27, 28) Figure 19 shows the radiation-creep coefficient of graphite as a function of temperature; the creep coefficient increases with increasing temperature. (26)

Kelly et al. (29,30) have developed the best model for explaining radiation creep in graphite. This model assumes that graphite is a single-phase material and the only deformation mode for creep is basal plane shear. Graphite normally contains a high density of basal plane dislocations which are kept immobile by pinning points. Neutron irradiation destroys old pinning points and creates new pinning points believed to be submicro-

Fig. 19. Radiation creep coefficient of graphite as a function of temperature. A compilation of data from various sources.

scopic clusters of 4 ± 2 interstitial atoms. Under these circumstances, dislocations can move through a lattice under an applied stress and cause basal shear. The pinning-point density increases quickly upon irradiation and saturates after low neutron fluences.

8.4 EFFECT OF RADIATION ON CARBON AND GRAPHITE CLOTHS AND FIBERS

The amount of neutron irradiation data on carbon and graphite cloths is sparse. The few papers that are available deal with structural and mechanical property changes of cloth at a maximum fluence up to $\sim 1 \times 10^{25} \text{ n/m}^2$. These fluences are quoted in terms of "Equivalent Fluence for Damage." Morgan calculates that a fluence of $1.01 \times 10^{25} \text{ n/m}^2$ produces one displacement per atom in graphite.⁽³¹⁾

Three series of cloths and fibers were irradiated in EBR-II, in three different capsules, to fluences of 3.5, 7.3, and $10 \times 10^{25} \text{ n/m}^2$ at 470°C .⁽³²⁾ Materials referred to as carbon are produced by pyrolysis at relatively low temperatures $\leq 1000^\circ\text{C}$. Graphite products result from an additional step in which the temperature is raised to 2000°C or more. Cloth may be either two- or three-dimensional. The former is a thin, single layer of fabric prepared by a simple under-and-over weave. Three-dimensional cloth is prepared by means of specialized looms.

Table 11 lists materials irradiated in this study. All axial dimensional changes observed, as well as at least some radial dimensional

TABLE 11* Materials Description^(a)CLOTHS:

<u>Sample</u>		<u>Material</u>		<u>2- or 3- Dimensional</u>	<u>Weave</u>	<u>Type</u>
<u>No.</u>	<u>Type</u>	<u>Precursor</u>	<u>Type</u>		<u>Before or After Pyrolysis</u>	
1	Thornel-50	Rayon	Graphite	2	After	8-Harness Satin
2	GSGC-2	Rayon	Graphite	2	Before	Square
3	WCA	Rayon	Graphite	2	Before	Square
4	GSCC-2	Rayon	Carbon	2	Before	Square
5	Thornel-400	PAN	Carbon	3	After	Orthogonal
6	Thornel-400	PAN	Carbon	3	After	Angle Interlock

FIBERS:

<u>Sample</u>		<u>Material</u>		<u>Crystal Orientation</u>
<u>No.</u>	<u>Type</u>	<u>Precursor</u>	<u>Type</u>	
1	Thornel-50S (WYH-140 1/2)	Rayon	Graphite	High
2	GSGY-2	Rayon	Graphite	Low
3	WYB-85 1/2	Rayon	Graphite	Low
4	GSCY-2	Rayon	Carbon	Low
5	Thornel-400 (WYM-30 1/0)	PAN	Carbon	Moderate
6	Type A	PAN	Carbon	Low
7	VSA-11	Pitch	Graphite	High
8	Thornel-75S (WYJ-160 1/2)	Rayon	Graphite	Very High
9	C-20	Rayon	Carbon	Low
10	Thornel-300 (WYP-30 1/0)	PAN	Carbon	Moderate
11	VYB-70 1/2	Rayon	Carbon	Low

(a) Fibers in cloth samples 1-6 are the same as fiber samples 1-5 with the exception of the #3 samples where the cloths and fibers are similar but not identical.

*Ref. 32

TABLE 12*. Axial Shrinkages of Fibers in Graphite and Carbon Cloths Irradiated at 470°C^(a,b)

		Shrinkage (%)								
Cloth		$3.5 \times 10^{21} \text{ cm}^{-2}$		$7.3 \times 10^{21} \text{ cm}^{-2}$			$10 \times 10^{21} \text{ cm}^{-2}$			
No.	Type	W	F	W	F	A	W	F	A	
1	Thornel-50	8.1 ± 1.0	(c)	20.1 ± 1.2	(c)	19.0 ± 0.3	(c)	(c)	26.7 ± 0.4	
2	GSGC-2	8.6 ± 0.3	8.4 ± 1.1	12.1 ± 1.2	14.5 ± 0.8	12.8 ± 1.0	14.9 ± 1.4	17.8 ± 1.2	14.8 ± 1.1	
3	WCA	5.1 ± 0.6	8.2 ± 0.5	13.0 ± 2.3	14.8 ± 0.8	12.5 ± 0.5	13.6 ± 0.4	22.0 ± 1.1	16.8 ± 0.4	
4	GSCC-2	6.3 ± 0.3	7.1 ± 0.2	12.9 ± 1.0	14.5 ± 0.3	12.3 ± 0.2	17.4 ± 2.2	18.7 ± 1.8	17.4 ± 1.4	

- (a) W and F values are for the two orthogonal directions in the cloth; A values are taken from area measurements. See text for additional explanations.
- (b) Uncertainties listed are two times the standard deviations of the percentage differences between irradiated and unirradiated lengths.
- (c) All yarn ends in this direction were obscured thereby preventing length measurements; see text.

*Ref. 32

TABLE 13*. Mean Equivalent Diameter Changes of Irradiated Fibers

No.	Fiber Type	Diameter Change (%) ^(a)	
		$3.5 \times 10^{21} \text{ cm}^{-2}$	$10 \times 10^{21} \text{ cm}^{-2}$
1	Thorne1-50S (WYH-140 1/2)	-3.1 ± 3.2	+30.6 ± 2.3
2	GSGY-2	-5.5 ± 2.4	- 3.6 ± 3.6
3	WYB-85 1/2	-0.4 ± 2.4	- 6.7 ± 6.6
4	GSCY-2		- 6.8 ± 1.9
5	Thorne1-400 (WYX-30 1/0)		- 4.2 ± 2.8
6	Type A	-5.5 ± 2.3	+ 0.3 ± 3.2
7	VSA-11		+32.9 ± 8.0
8	Thorne1-75S (WYJ-160 1/2)		- 5.0 ^(b)
9	C-20	-0.7 ± 2.2	- 4.0 ± 1.4
10	Thorne1-300 (WYP-30 1/0)		-15.8 ± 2.9
11	VYB-70 1/2		-19.3 ± 4.1

(a) Uncertainties listed are two times the standard deviations of the percentage difference between irradiated and unirradiated mean diameters. See text for discussion of additional uncertainties.

(b) Only one irradiated fiber measured.

*Ref. 32

changes, are quite large when compared with changes observed in typical nuclear graphites under similar conditions. Only five different types of fibers from the first capsule were successfully remounted after irradiation. Fibers from the second capsule were lost during capsule reassembly. All 11 types of fibers from the third capsule were successfully remounted. When looking at the cross section, irradiation caused no noticeable change in appearance such as shape, cracks, pores, etc. Table 12 lists changes in mean equivalent fiber diameter of the cloth. Table 13 lists the same for fibers.

Three of the four types of two-dimensional cloth samples irradiated in this study remained intact even though the fibers have undergone axial shrinkages of 18 to 27%. Other changes observed for the two-dimensional cloth were large density increases and all but one had a large decrease in surface area. Radial dimensional changes of 11 different types of fibers varied from shrinkage of 19% through almost no change to swelling of 33%. Figures 20, 21, and 22 show the surface area change, density change, and length change as a function of equivalent fission fluence for the four two-dimensional cloths.

FIGURE 20. Surface area of graphite and carbon cloths, ⁽³²⁾

FIGURE 21. Density of graphite and carbon cloths.(32)

FIGURE 22. Axial length changes of fibers in graphite and carbon cloths irradiated at 470°C.⁽³²⁾

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