137
3-31-80

GA-A15749
UC-20d

DR. 973

MASTER

ASSESSMENT OF MARTENSITIC STEELS AS STRUCTURAL MATERIALS IN MAGNETIC FUSION DEVICES

by

**J. M. RAWLS, W. Y. K. CHEN, E. T. CHENG
J. A. DALESSANDRO, P. H. MILLER, S. N.
ROSENWASSER, and L. D. THOMPSON**

**Prepared under
Contract DE-AT03-76ET51011
for the San Francisco Operations Office
Department of Energy**

JANUARY 1980

GENERAL ATOMIC COMPANY

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

NOTICE

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States nor any agency thereof, nor any of their employees, makes any warranty, expressed or implied, or assumes any legal liability or responsibility for any third party's use or the results of such use of any information, apparatus, product or process disclosed in this report, or represents that its use by such third party would not infringe privately owned rights.

Printed in the United States of America
Available from
National Technical Information Service
U.S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22161
NTIS Price Codes: Printed Copy A04; Microfiche Copy A01

GA-A15749

UC-20d

ASSESSMENT OF MARTENSITIC STEELS AS STRUCTURAL MATERIALS IN MAGNETIC FUSION DEVICES

by

**J. M. RAWLS, W. Y. K. CHEN, E. T. CHENG
J. A. DALESSANDRO, P. H. MILLER, S. N.
ROSENWASSER, and L. D. THOMPSON**

DISCLAIMER

This book was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise, does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

**Prepared under
Contract DE-AT03-76ET51011
for the San Francisco Operations Office
Department of Energy**

**GENERAL ATOMIC PROJECT 3235.881.001
JANUARY 1980**

GENERAL ATOMIC COMPANY

CONTENTS

1.	INTRODUCTION	1-1
2.	PROPERTIES AND BEHAVIOR OF MARTENSITIC ALLOYS PERTINENT TO FUSION APPLICATIONS	2-1
2.1.	Mechanical Behavior	2-2
2.2.	Radiation Damage	2-5
2.3.	Application Experience	2-5
2.4.	Metallurgical Performance Evaluation of HT-9	2-7
2.5.	Identification of Key Issues and Major Data Requirements	2-10
3.	LIFETIME ANALYSIS	3-1
3.1.	Model Employed	3-1
3.2.	Results and Discussion	3-4
3.3.	Implications for Potential Application to ETF	3-6
4.	MEASUREMENT OF MAGNETIC PROPERTIES	4-1
4.1.	Introduction	4-1
4.2.	Previous Results	4-1
4.3.	Current Experiments	4-2
4.4.	Results	4-4
4.5.	Conclusions	4-9
5.	SYSTEMS QUESTIONS	5-1
5.1.	Description of Numerical Tools	5-2
5.2.	Magnetic Perturbations - Toroidal Field	5-5
5.3.	Magnetic Perturbations - Poloidal Field	5-6
5.4.	Magnetic Perturbations - Transient Analysis	5-8
5.5.	Magnetic Loads	5-11
5.6.	Safety and Environmental Considerations	5-13
	APPENDIX A	A-1

FIGURES

2-1	Minimum ultimate strength of HT-9 and other candidate alloys.	2-3
2-2	Effect of 80,000 hour, 600°C service exposure on HT-9 stress-rupture properties	2-8
2-3	Effect of 80,000 hour, 600°C service exposure on HT-9 tensile properties	2-8
2-4	Effect of 80,000 hour, 600°C service exposure on HT-9 impact properties	2-9
3-1	Systems lifetime comparison of HT-9 with 20% CW 316 for a neutron wall loading of 1 MW/m ²	3-5
4-1	Comparison of hysteresis loops of HT-9 at 500°C for two different maximum primary currents	4-5
4-2	Comparison of the hysteresis loops for 12 Cr 1 Mo at room temperature and a typical operating temperature in reactor applications	4-6
4-3	Comparison of 9 Cr and 12 Cr martensitic steel hysteresis loops at 500°C	4-7
4-4	Variation in hysteresis loops with bias field	4-8
4-5	Magnetic properties in a simulated tokamak environment	4-9
5-1	STARFIRE toroidal field ripple showing the effects of a ferromagnetic (FM) blanket with and without penetrations ...	5-6
5-2	STARFIRE poloidal field perturbation due to a ferromagnetic blanket	5-7
5-3	Magnetic loading on the ferromagnetic blankets for the GA TNS, STARFIRE and ETF designs	5-13

TABLES

2-1	Nominal composition of alloy HT-9 in weight percent	2-2
2-2	Allowable 300,000 hour design stresses (S_{mt}) of HT-9 and 316 based on the criteria of ASME Boiler and Pressure Vessel Code Case N47 (elevated temperature service)	2-3
2-3	Thermal stress resistance of candidate fusion alloys at 500°C	2-4
2-4	Foreign usage and performance of 9-12 Cr steels in fossil power plants	2-6
2-5	Current data base for HT-9 and modified 9 Cr-1 Mo	2-11

4-1	Characteristics of toroidal transformers used for material evaluation	4-3
5-1	Parameters used for field perturbation and force computations	5-5
5-2	Radial body forces and equivalent inward pressures at two values of M^*	5-12
5-3	Radioactivity after shutdown in HT-9 and 316 SS-based blankets (assume 1 MW/m ² wall loading for 6 years)	5-14
5-4	Biological hazard potential after shutdown in HT-9 and 316 SS-structured blankets (assume 1 MW/m ² wall loading for 6 years)	5-15
5-5	Afterheat following shutdown in HT-9 and 316 SS-based blankets	5-15

1. INTRODUCTION AND SUMMARY

First wall/blanket lifetime has been identified as a key issue in the ultimate commercialization of fusion power. Because of the considerable cost and time involved in first wall/blanket changeouts, the structural materials employed must exhibit long life behavior under severe operating conditions. The current U.S. program to develop materials for commercial reactors is focused on several classes of metallic materials, including austenitic stainless steels, precipitation-hardened Fe-Ni-Cr alloys, reactive metal alloys (titanium) and refractory alloys (vanadium and niobium). Recent results from fission reactor irradiation experiments in the United States and Europe suggest that another group of alloys, the Fe-Cr martensitic stainless steels, might offer the potential of significantly greater first wall/blanket lifetimes and, therefore, should be given serious consideration. This manuscript documents the results of preliminary experiments and analyses to assess the feasibility of incorporating ferromagnetic martensitic steels in fusion reactor designs and to evaluate the possible advantages of this class of material with respect to first wall/blanket lifetime.

The general class of alloys under consideration are ferritic steels containing from about 9 to 13 percent Cr with some small additions of various strengthening elements such as Mo. These steels are conventionally used in the normalized and tempered condition for high temperature applications and, as discussed in Section 2, can compete favorably with austenitic alloys up to about 600°C. Although the heat treatment can result in either a tempered martensite or bainite structure, depending on the alloy and thermal treatment parameters, this general class of materials will be referred to as "martensitic" stainless steels for simplicity. These steels have been employed successfully in such high temperature applications as steam turbines, jet engines and gas turbines. The commercial 12 Cr-1 Mo-0.3 V Sandvik alloy, HT-9, has been used in Europe for steam pipe lines and for tubes/headers in fossil-fired boilers for over ten years and is currently being considered

for a number of thermal and fast reactor components including steam generators, recuperators, auxiliary heat exchangers and steam piping.

On the basis of irradiation test results, HT-9 was among a group of six prime advanced candidate materials identified by the National FBR Cladding/Duct Materials development program and is in fact the only commercial candidate alloy. Because of the encouraging and rapidly expanding data base from the FBR Program, the significant application history and the current interest in the alloy, HT-9 was selected as a representative martensitic alloy for the current evaluation.

The material properties of martensitic alloys pertinent to fusion applications are summarized in Section 2. The radiation damage resistance is discussed and the resistance to damage from cyclic fatigue and creep fatigue is shown to be superior to other candidate materials. The applications history of martensitic steels is briefly reviewed and results are presented of an evaluation of a specimen subjected to 80,000 hours of 600°C operation. Finally, a detailed evaluation of the known data bases for martensitic steels has been conducted in the light of fusion design requirements in order to identify the key issues that must be resolved for application in the Engineering Test Facility.

In Section 3, a comparison of expected lifetimes is presented for tritium breeding blanket modules for 20% CW 316 stainless steel and HT-9. The life of the module was optimized for each material by varying the wall thickness. Maximum lifetime is obtained by trading off life governed by crack growth with that governed by maximum allowable creep strain. It is shown that HT-9 can give approximately five times greater life compared to 316 stainless steel for typical tokamak reactor operating conditions.

The magnetic properties of martensitic steels required for a thorough evaluation of the systems questions involved in their potential use in magnetic confinement devices were not known at the outset of the study. While it was known that the material responds linearly, i.e. as a paramagnet, when immersed

in a saturating magnetic field and subjected to variations in that field, no measurements had been made of its response to time-changing fields normal to the saturating field, which is the situation in toroidal, current-carrying magnetic confinement devices.

To investigate such effects, measurements were made in a simulated tokamak environment; the results of these investigations are reported in Section 4. Hysteresis loops for a variety of ac primary currents were obtained on two samples of 12 Cr 1 Mo (HT-9) and one sample of 9 Cr 2 Mo as a function of a steady-state bias field up to 60,000 gauss over a temperature range of 25 to 550°C. The samples were in the form of a toroidal transformer and the bias field was applied both parallel and perpendicular to the symmetry axis. The saturation inductance decreases with increasing temperatures for both materials. The materials are quite similar in magnetic behavior and they become paramagnetic when the internal magnetizing bias field is greater than 500 to 2000 oersted. The permeability for the fields arising from currents in the poloidal coils approaches unity under reactor conditions. The small departure from unity depends on the geometry and the value for the saturation induction, which is about 11,000 gauss in 12 Cr 1 Mo at 500°C.

The properties so deduced were used as the basis of an analysis of the systems implications of using martensitic steels as a structural material in a magnetic fusion environment. The studies reported in Section 5 focus two potential problem areas: the effects on plasma performance and the impact on the mechanical design of the blanket. A 3-D field code IRON allowing the presence of saturable ferromagnetic material was developed to facilitate the investigation. This investigation encompassed generic studies as well as specific applications to TNS, ETF, and STARFIRE designs.

One major concern in the area of plasma performance is the effect of the ferromagnetic blanket on toroidal field ripple. Results indicate that because the blanket material is highly saturated by the toroidal field, the effect of an axisymmetric blanket on toroidal field ripple is very small. Even the additional ripple produced by penetrations in the blanket such as

neutral beam ducts was found to be insignificant. Another concern is the interaction of the ferromagnetic blanket with the plasma shaping and control system, in particular the field-shaping coils. The high level of saturation by the toroidal field is found to reduce the perturbations to only about 3% of the poloidal field. In addition, the effective permeability μ is nearly unity, so the operation of the field-shaping coils is not expected to be significantly affected. The most important question from the point of view of engineering impact appears to be the magnetic forces on the blanket. Results indicate that for a martensitic steel blanket in a typical reactor, the magnetic loading does not exceed the equivalent of a few atm of static pressure, which should not pose a significant mechanical design problem. Section 5 also substantiates the claim that martensitic steels have environmental and safety advantages over austenitic steels in fusion applications.

The conclusion of the study as a whole is that there are no major systems issues involved in the application of martensitic steels as structural materials in magnetic fusion devices. The following important characteristics relative to other candidate materials

- potentially superior radiation resistance
- favorable thermal-physical properties
- adequate structural properties to 550°C
- desirable from a resources perspective
- improved safety and environmental features

then suggest increased emphasis on the unresolved materials questions involved in specific fusion applications.

2. PROPERTIES AND BEHAVIOR OF MARTENSITIC ALLOYS PERTINENT TO FUSION APPLICATIONS

The general class of alloys under consideration are ferritic steels containing from about 9 to 13 percent Cr with some small additions of carbide stabilizing and/or strengthening elements such as Mo and V. These steels are conventionally used in the normalized and tempered condition for high temperature applications (Ref. 2-1) and, as discussed below, can compete favorably with austenitic alloys up to about 600°C. Although the heat treatment can result in either a tempered martensite or bainite structure, depending on the alloy and thermal treatment parameters, this general class of materials will be referred to as "martensitic" stainless steels for simplicity. These steels have been employed successfully in such high temperature applications as steam turbines, jet engines and gas turbines. The 12 Cr-1 Mo-0.3 V alloy type has been used commercially for high temperature applications in Europe for over ten years. The W containing alloy specified in Germany as DINX20CrMoWV121, Material No. 1.4935 (marketed by Sandvik in the U.S. as alloy HT-9) is currently being considered for a number of thermal and fast reactor components including steam generators, recuperators, auxiliary heat exchangers and steam piping.

On the basis of irradiation test results, HT-9 was among a group of six prime advanced candidate materials identified for detailed study in the United States National FBR Cladding/Duct Materials Development Program and is in fact the only commercial candidate alloy. Because of the encouraging and rapidly expanding data base from the FBR Program, the significant application history and the current interest in the alloy, HT-9 was selected as a representative martensitic alloy for the current evaluation. It should be noted that other alloys in this class, such as the modified 9 Cr-1 Mo material being developed for the FBR program, may eventually prove superior for fusion application, but their current data base was insufficient to assess their potential.

The nominal composition of alloy HT-9 is given in Table 2-1. For high temperature, extended time service, the alloy is usually heat treated at 1050°C for 0.5 hr, air cooled and then tempered at 780°C. Although higher

TABLE 2-1
NOMINAL COMPOSITION OF ALLOY HT-9 IN WEIGHT PERCENT

Fe	Cr	Ni	Mo	W	V	Si	Mn	C
Balance	11.5	0.5	1.0	0.5	0.3	0.25	0.5	0.20

strengths are obtainable in 12 Cr steels with lower tempering temperatures, the 780°C treatment ensures long time stability of the tempered martensite and strengthening carbide precipitates to about 600°C service temperature.

2.1. MECHANICAL BEHAVIOR

The strength of HT-9 is compared to that for several other candidate first wall/blanket alloys in Fig. 2-1. The variation of minimum ultimate tensile strength with temperature is plotted for HT-9 (Ref. 2-2), 20 percent cold worked 316 stainless steel (20CW316), annealed 316 stainless steel (316), Inconel 718 (Ref. 2-3), Titanium alloys Ti-6Al-4V and Ti-6Al-2 Sn-4 Zr-2 Mo (Ref. 2-4), Vanadium alloy V-20 Ti (Ref. 2-5) and Niobium alloy Nb-1 Zr (Ref. 2-6). It is evident that the short term strength of HT-9 is between that for 316 and 20CW316 to about 500°C, is equivalent to that for 316 at about 550°C, and is similar to that for Ti-6Al-4V at about 550°C. Above 500°C HT-9 short term strength decreases rapidly.

The maximum allowable stress intensities, S_{mt} , of HT-9 and 316, as defined by the ASME Boiler and Pressure Vessel Code case for elevated temperature design of class 1 nuclear components (N47), are shown for 300,000 hr lifetime as a function of temperature in Table 2-2. (It should be noted that HT-9 is not currently an ASME Code approved material. However, approximate S_{mt} values were calculated (Ref. 2-2) on the basis of the Case N47 stress criteria.) The allowable design stress intensities take into account both time-independent and time-dependent creep strength behavior and indicate that for lifetimes on the order of 40 years, which are desirable for commercial power plants, the maximum nominal design temperature for HT-9 components is about 600°C.

Fig. 2-1. Minimum ultimate strength of HT-9 and other candidate alloys

TABLE 2-2

ALLOWABLE 300,000 HOUR DESIGN STRESSES (S_{mt}) OF HT-9 AND 316 BASED ON THE CRITERIA OF ASME BOILER AND PRESSURE VESSEL CODE CASE N47 (ELEVATED TEMPERATURE SERVICE)

Material	S_{mt} (MPa)					
	R.T.	400°C	500°C	550°C	600°C	650°C
HT-9	227	196	110	62	32	14
316	138	110	108	87	50	31

Thermal stresses are an important source of cyclic fatigue and creep fatigue damage, particularly in pulsed fusion systems. The comparative resistance to thermal stress can be estimated by a parameter, M, defined by

$$M = \frac{2\alpha_y K(1 - \nu)}{\alpha E} ,$$

where α_y is the yield strength, K is the thermal conductivity, ν is Poisson's ratio, α is the thermal expansion coefficient, and E is Young's Modulus. Thermal stress resistance increases with increasing M. The thermal stress resistance of HT-9 at 500°C is compared to that of other candidate materials in Table 2-3. The significantly higher thermal conductivity and lower thermal expansion coefficient of ferritic HT-9 relative to austenitic 316 provides the former with a significantly greater resistance to thermal stress effects. Thermal stress resistance of HT-9 is slightly lower than that for the Ti alloys and for the precipitation strengthened Ni alloy 718.

TABLE 2-3
THERMAL STRESS RESISTANCE OF CANDIDATE FUSION
ALLOYS AT 500°C

Alloy	Thermal Stress Resistance
	M (W/m) $\times 10^{-3}$
Annealed 316	1.3
20CW316	4.9
HT-9	8.5
Ti-6Al-4V	10.2
Ti-6Al-2Sn-4Zr-2Mo	10.4
Inconel 718	10.7
V-20Ti	12.3
Nb-1Zr	19.8

2.2. RADIATION DAMAGE

The primary reason for investigating the application of HT-9 is, of course, its apparent resistance to neutron radiation damage. Most of the pertinent information with respect to void swelling, in-reactor creep, and embrittlement has been developed in the United States FBR program, and has not yet been released for general dissemination. Although discussion of the radiation damage resistance of HT-9 must therefore be qualitative, a number of relevant observations can be made on the basis of EBR II data to greater than 1×10^{23} n/cm² ($E > 0.1$ MeV).

Void swelling in HT-9 is at least an order of magnitude less than in 20CW316. Current results indicate that swelling is even lower than the 3.7 percent, 500°C peak swelling at 150 dpa previously reported for HT-9 under heavy ion bombardment (Ref. 2-7). The currently published equation for 20CW316 (Ref. 2-8) predicts peak swelling of about 35 percent at 580°C and 150 dpa. In addition, in-reactor creep resistance of HT-9 appears better than that for 20CW316 to temperatures in excess of 600°C (Ref. 2-9).

Although only low helium levels and low helium/dpa ratios relative to fusion neutron conditions have been evaluated, tensile ductility retention of HT-9 appears superior to that for 20CW316 (particularly when tested above the irradiation temperature) and vastly superior to that for the precipitation strengthened nickel alloys up to 600°C. Furthermore, the microstructure of HT-9 appears stable under irradiation in the temperature range of interest, but the shift in Ductile-Brittle Transition Temperature after irradiation must be determined.

2.3. APPLICATION EXPERIENCE

The class of 9-12 Cr steels has been used extensively in power generation applications throughout Europe for over 25 years. While most of these applications have been directed towards superheater, reheaters, and evaporators for oil- and coal-fired power plants, much experience and performance history information pertinent to fusion application has been generated. In

addition to fossil power plant applications, this class of steels has been employed in nuclear power plants. In the Hinkley Point "B" AGR reactor in the United Kingdom, superheaters and reheaters are 9 and 12 Cr steels (Ref. 2-10). In the U.S., the Fort St. Vrain HTGR employs standard 410 and 422 (12 Cr) stainless steel grades in the helium circulators and compressors (Ref. 2-11). Other applications have included gas and steam turbine components, aircraft and missile thin-wall pressure vessels, and petroleum industry pressure vessel linings (Ref. 2-12).

Other proposed nuclear applications for 9-12 Cr steels, presently under consideration, include ducts, cladding, and steam generators for the U.S. LMFBR and GCFR, steam generators for the Japanese FBR, ducts and steam generators for the French Super Phenix, steam generators, helium-helium heat exchangers, helium circulators, and steam piping for the U.S. HTGR, and steam generators for the U.S. LWR.

Two of the largest users for fossil plants in Europe have been Sulzer Brothers (Switzerland) and the United Kingdom Central Electricity Generating Board (CEGB). Table 2-4 indicates the performance and failure histories for

TABLE 2-4
FOREIGN USAGE AND PERFORMANCE OF 9-12 Cr STEELS
IN FOSSIL POWER PLANTS

LOCATION	USAGE	ALLOY	TEMPERATURE (°C)	SERVICE STATUS (KHrs)	FAILURE HISTORY
UK	13 PLANTS	9 CrMo (V)	450 - 660	≤ 110	1 FAULTY WELD 1 FAULTY WELD REPAIR FIRESIDE CORROSION
	12 PLANTS	12 CrMo	430 - 850	≤ 45	OVERHEATING > 850°C
AUSTRIA BELGIUM CZECHOSLOVAKIA GERMANY SWITZERLAND TURKEY YUGOSLAVIA	98 TONS	9 CrMo	≤ 610	≤ 110	1 FAULTY WELD REPAIR
	> 250 TONS	12 CrMo (V, W)	≤ 610	≤ 130	FIRESIDE CORROSION

these two organizations. In the U.K., CEGB has 25 operating fossil power plants which use 9 and 12 Cr steels. Application temperatures were as high as 850°C, but successful long-term operation temperatures were limited to the range of 600°C-650°C maximum. At the higher temperatures, overheating contributed to a significant amount of creep damage with correspondingly shorter rupture lives. Of particular interest is the relatively few weld failures. Welding is usually performed following a careful procedure involving pre- and post-weld heat treatments. Only two failures have been reported which related directly to welding.

In the lower part of Table 2-4, the countries and total tonnages are reported for Sulzer Brothers fossil power plants. Again, excellent operating performance with only limited failures has been observed.

2.4. METALLURGICAL PERFORMANCE EVALUATION OF HT-9

In order to quantify the long-term high-temperature service performance of candidate martensitic stainless steel for fusion application, General Atomic, in conjunction with Sulzer, evaluated the behavior of HT-9 superheater tubing from a welded boiler in the Reutlingen, Germany coal-fired power plant. Tubes were removed from service after 80,000 hours at temperatures near 600°C. In addition to 720 normal operating starts/shutdowns (50 from room temperature), the tube had also experienced six off normal thermal shocks related to power failures at the plant. The tubes were subjected to relatively low stresses of approximately 20-50 MPa.

The interrupted service evaluation included stress-rupture, tensile in the temperature range of 23°C-600°C, Charpy V-notch impact toughness, and microstructural evaluation. The properties of unexposed tubing were determined simultaneously for comparison.

Figure 2-2 shows the curves obtained in the stress-rupture tests with stress plotted as a function of the Larson-Miller parameters (Ref. 2-14). Overaging of carbides after 80,000 hours of in-service exposure decreased

Fig. 2-2. Effect of 80,000 hour, 600°C service exposure on HT-9 stress-rupture properties

stress-rupture lifetimes, particularly at the higher stress levels. As indicated by Fig. 2-2, the residual creep properties were quite good compared to properties before service. It should also be mentioned that total creep strains did not change significantly. Figure 2-3 shows the room- and elevated-temperature tensile properties determined and, again, strength properties were only slightly reduced after long-term service and the tensile

Fig. 2-3. Effect of 80,000 hour, 600°C service exposure on HT-9 tensile properties

ductilities remained quite close to pre-service values over the entire test temperature range. Figure 2-4 shows the impact toughness values obtained at 22°C and -45°C, for half-width specimens. Impact toughness decreased; however, substantial toughness remained even at -45°C. This alloy (HT-9) undergoes a transition in impact fracture behavior from ductile to brittle with decreasing temperature, and following the service exposure, the ductile-to-brittle transition temperature (DBTT) shifted to a somewhat higher temperature, although the precise DBTT was not determined in these tests.

Transmission electron microscopy was employed to study the microstructural changes occurring with the service exposure. Overall structural stability appeared to be excellent with the most prominent feature noted as the enhanced precipitation of $M_{23}C_6$ carbides at martensite interlath boundaries. Some precipitation of a Fe_2Mo laves phase was also noted but was not found to be morphologically detrimental to the mechanical properties. The decrease in impact toughness properties was most likely associated with the carbide precipitation. Thus, the effects of a long-term service exposure did not lead to dramatic reductions in mechanical properties, consistent with the overall good thermal stability of HT-9.

Fig. 2-4. Effect of 80,000 hour, 600°C service exposure on HT-9 impact properties

2.5. IDENTIFICATION OF KEY ISSUES AND MAJOR DATA REQUIREMENTS

Based on a detailed evaluation of the known data bases for martensitic steels and fusion design requirements, the key issues which must be resolved for application of 9-12 Cr steels in the Engineering Test Facility (ETF) and future fusion systems were identified as follows:

- fracture resistance - particularly the change of fracture toughness at lower temperature as a result of the ductile-to-brittle transition behavior characteristics of these steels;
- the effect of the fusion reactor environment on structural properties - including the effects of displacement damage and transmutation products from interaction with fusion 14 MeV neutrons and the compatibility with candidate coolants;
- weldability/fabricability - specifically pre- and post-weld heat treatment requirements, cracking susceptibility, repair procedures in configurations pertinent to fusion applications, and the sensitivity of weld procedures to microstructure and properties.

In connection with the above evaluations and assessment of the data base which exists for the two leading martensitic steel candidates for fusion application, HT-9 and developmental modified 9 Cr-1 Mo alloy (being developed by ORNL for the FBR program under DOE funding) was completed. Table 2-5, which shows the properties of interest and an assessment of our current level of knowledge, indicates major data voids in the area of fatigue and fracture related properties, weld properties, irradiation performance and coolant compatibility effects for both alloys.

As a result of this study, a comprehensive testing program, with the objective of establishing the application feasibility and performance potential of 9-12 Cr magnetic steels in ETF and other fusion systems, was recommended to DOE-OFE and a major national program was initiated in FY-80.

TABLE 2-5
CURRENT DATA BASE FOR HT-9 AND
MODIFIED 9 Cr-1 Mo

PROPERTY	HT-9		MODIFIED 9 Cr - 1 Mo	
	UNIRRADIATED	IRRADIATED	UNIRRADIATED	IRRADIATED
TENSILE	●	○	●	○
STRESS-RUPTURE	●	○	●	○
CREEP	●	○	●	○
VOID SWELLING		○		○
CREEP-FATIGUE	○	○	●	○
FATIGUE CRACK GROWTH	○	○	○	○
FRACTURE TOUGHNESS	○	○	○	○
IMPACT TOUGHNESS	●	●	●	○
WELD PROPERTIES	●	○	●	○
COOLANT COMPATIBILITY	●	○	○	○
PHYSICAL PROPERTIES	●	○	●	○
MAGNETIC PROPERTIES	●	○	●	○

○ NONE REPORTED ○ INSUFFICIENT FOR ETF DESIGN ● SUFFICIENT FOR ETF DESIGN

REFERENCES

- 2-1 KING, R. T., GOODWIN, G. M., HEESTAND, R. L., DEVAN, J. H. and MCCURDY, H. C., "Alternate Structural Materials for Liquid Metal Fast Breeder Reactors," Symposium on Structural Material for Service at Elevated Temperatures in Nuclear Power Generation, A. O. Schaefer, Ed., Houston, Texas, November 1975, ASME, New York, p. 375.
- 2-2 CHAFEY, J. E. and WATTIER, J. B., "Estimation of Allowable Design Stress Values for 12 Cr-1 Mo-0.3 V Steel," General Atomic Report GA-A14610, February 1978.
- 2-3 SPANNER, J. C., Ed., *NUCLEAR SYSTEMS MATERIALS HANDBOOK, Vol. 1, DESIGN DATA*, TID 26666, Hanford Engineering Development Laboratory (1976).
- 2-4 *METALLIC MATERIALS AND ELEMENTS FOR AEROSPACE VEHICLE STRUCTURE*, HIL-HBKC-5B, Sections 5.3.3 and 5.4.3, USAF (1975).
- 2-5 GOLD, R. E., HARROD, D. E., AMMON, R. L., BUCKMAN, R. W., JR. and SVEDBERG, R. S., "Technical Assessment of Vanadium Base Alloys for Fusion Reactor Applications," Final Report No. COO-4540-1, Westinghouse Electric Corp., April 1978, p. 112.

- 2-6 DAVIS, J. W. and KULCINSKI, G. L., "Assessment of Titanium for Use in the 1st Wall/Blanket Structure of Fusion Power Reactors," EPRI ER 386, McDonnell Douglas Astronautics Company, April 1977, p. 10.
- 2-7 SMIDT, F. A., MALMBERG, P. R., SPRAGUE, J. A. and WESTMORELAND, J. G., "Swelling Behavior of Commercial Ferritic Alloys, EM-12 and HT-9, as Assessed by Heavy Ion Bombardment," *IRRADIATION EFFECTS ON THE MICRO-STRUCTURE AND PROPERTIES OF METALS*, ASTM STP #611, American Society for Testing and Materials (1976) p. 227.
- 2-8 SPANNER, J. C., Ed., *NUCLEAR SYSTEMS MATERIALS HANDBOOK, Vol. 1, DESIGN DATA*, TID 26666, Hanford Engineering Development Laboratory (1976), Section 5.
- 2-9 CHIN, B. A., GILBERT, E. R., PAXTON, M. M. and STRAALSUND, J. L., "A Comparison of the In-Reactor Creep Behavior of Selected Commercial Alloys at a Temperature of 593°C," presented at the 9th ASTM International Symposium on Effects of Radiation on Structural Materials, Richland, Washington, July 1978.
- 2-10 WYATT, L. M., "The Performance of Cr-Mo Steels in the Boilers of CEBG Power Stations," *FERRITIC STEELS FOR FAST REACTOR STEAM GENERATORS*, edited by S. F. Pugh and E. A. Little (1978), Proceedings of an International Conference held by the British Nuclear Energy Society at the Institution of Civil Engineers, London, May 30 through June 2, 1977.
- 2-11 SMITH, A., General Atomic Company, private communication.
- 2-12 BRIGGS, J. Z. and PARKER, T. D., *THE SUPER 12% CR STEELS*, Climax Molybdenum Company, Ann Arbor, Michigan (1965).
- 2-13 FRICKER, H., "Sulzer Experience with High Chromium Ferritic Steels," Report TA 2730, Sulzer Brothers, Switzerland, February 1977.
- 2-14 WALSER, B., "The Metallurgical and Mechanical Properties of X20CrMoWV121 After Service Exposure in a Fossil Fired Boiler," Report 222/1511, Sulzer Brothers, Switzerland, January 1977.

3. LIFETIME ANALYSIS

The primary incentive for using HT-9 in a fusion environment is its resistance to irradiation damage which means increased lifetime for components.

Lifetime of the first wall/blanket has a significant impact on the economics of a fusion reactor for numerous reasons. The first is the downtime required to periodically replace components whose lifetimes are used up, in the sense that reliable operation is no longer obtained. This is particularly important since the components need to be removed and replaced using remote maintenance equipment which is notoriously slow and expensive. A second factor contributing to cost is the cost for multiple components. This includes not only the cost for material and fabrication, but also the added expense of managing an inventory of replacement components. A third factor is related to the waste management of the activated structural materials. Components with extended lifetimes will be activated to a greater degree than shorter-lived components, but the volume of activated material that needs to be handled and disposed of will be significantly less. In summary, increased component lifetime reduces cost and makes fusion power more attractive in many ways.

3.1. MODEL EMPLOYED

A computer systems code was developed for a typical helium cooled canister type tritium breeding blanket module in order to provide a basis for comparison of different materials. In this analysis, the lifetime of a module made from HT-9 is compared to that using 20% CW 316 stainless steel. It is important not to simply take an existing module design based on one material and substitute another and calculate a lifetime. Each

design must be optimized for a given choice of material; this is done here by letting the wall thickness be an independent variable.

For comparison purposes, numerous blanket design parameters are held constant. The module diameter is assumed to be 0.5 m and the helium pressure is 40 atmospheres (600 psi). The helium inlet temperature is fixed at 250°C and the outlet temperature at 525°C. The front wall of the module is exposed to the plasma heat flux without using a divertor or a separately cooled first wall. The neutron wall loading is 1 MW/m² which is 80% of the total wall loading. A repetitive duty cycle of 300 s burn time followed by a 30 s dwell is used to calculate first wall operating temperature, the temperature difference across the wall, and the cyclic variation of temperature with time. (The thermal analysis was obtained from the General Atomic Demonstration Power Reactor study (Ref. 3-1) and was extended for the different choices of material.) The analysis is for the very front of the dished (ellipsoidal) head of the module where the temperature is a maximum and the pressure stress is uniform tension in all directions, given by the familiar relation pR/t . Superposed on this steady stress is a bending stress produced by the temperature difference across the wall thickness. Linear elastic stress analysis has been assumed throughout, assuming shakedown during the first few cycles after startup. The bending stress is cyclic and is the source of fatigue and crack growth failures.

Temperature dependent material properties are used in all calculations since considerable temperature variations are encountered. The instantaneous coefficient of thermal expansion is used in the calculation, rather than the mean value from room temperature since the cycling is about an elevated average temperature.

Before any lifetime calculations commence, the acceptability of the module design is checked against Section III of the ASME Boiler and Pressure Vessel Code, namely, that the primary (pressure) stress is less than S_m , where S_m is the maximum of either 0.9 of the yield strength or 1/3 of the ultimate

strength, both at the wall-averaged temperature. In addition, the maximum stress due to pressure and temperature is ascertained to be less than $3 S_m$. If either of these two criteria are not met, no further calculations are performed.

Material property data for both 20% CW 316 and HT-9 were the most recent data available as of June 1979. Whenever possible, material data for 316 was obtained from the Nuclear Systems Materials Handbook (Ref. 3-2). In addition, References 3-3 and 3-4 were used. For HT-9, material data was obtained from unpublished data from the U.S. LMFBR fuel cladding and duct program.

Four mechanisms of failure were considered in determining the lifetime of the blanket module: combined creep rupture-fatigue, crack growth of an assumed initial flaw, volumetric swelling, and strain (distortion) limits. Each calculation is discussed separately.

Linear interaction of fatigue and creep rupture is assumed. Life use fractions are calculated, as recommended by Code Case N47 of the ASME Code, so that the sum of the creep rupture use fraction and the low and high cycle fatigue use fractions equals unity. Creep rupture life is based on the primary pressure stress and the maximum wall-averaged temperature for the entire duty cycle time. Both low cycle fatigue, due to startup/shutdown cycling, and high cycle fatigue, due to thermal cycling of the first wall, are considered. Their use fractions are added together just as the fatigue fraction is added to the creep rupture fraction. It is implicitly assumed that 40,000 duty cycles occur each startup/shutdown cycle.

For the crack growth calculation, a 2:1 elliptical surface flaw is assumed to be on the inside surface of the blanket module wall, in the worst orientation. Thermal cycling of the module wall produces time varying tensile stresses which together with the primary stress causes the flaw to grow until the flaw propagates through the entire thickness of the wall, at which time failure occurs. The mode of failure is leakage of helium coolant into the plasma chamber, a phenomenon which prevents the plasma from reacting.

The initial depth of the flaw is assumed to be 0.75 mm. Elastic stress intensity magnification factors as a function of crack depth for the case of bending were obtained from Ref. 3-5. The number of cycles for the flaw to travel one tenth the distance to failure was calculated ten successive times and the total number of cycles to failure was determined. Lifetime is then simply the total number of cycles times the cycle time, in this case, 330 s.

Life based on volumetric swelling is based on stress-free swelling data which assumes an incubation dose during which no swelling takes place, followed by an exponential increase with fluence. A fluence of 1 MW-yr/m^2 is taken to represent $2.2 \times 10^{22} \text{ neutrons/cm}^2$. This was determined by taking into account that 14 Mev fusion neutrons produce approximately 60% more dpa's than that obtained from EBR II fluences ($E \geq 0.1 \text{ Mev}$). A 3% limit on volumetric swelling was somewhat arbitrarily chosen as the end-of-life criterion. This represents a linear strain of 1%.

Life based on strain limits was determined when the combined irradiation and thermal creep reached 3% linear strain. The material data used for this calculation are referred to as in-reactor creep. It is a function of both temperature and stress.

3.2. RESULTS AND DISCUSSION

Figure 3.1 is a comparison of lifetimes for the two materials as a function of maximum wall temperature, which is controlled by the coolant outlet temperature. For both materials, it was found that lifetime is not limited by the mechanism of combined creep-fatigue and thus does not appear in the figure. This is understandable since fatigue is a less stringent requirement than crack growth propagation and strain limits control before creep rupture occurs. Lifetime based on swelling is straightforward and is primarily a function of wall-averaged temperature. HT-9's superior resistance to swelling compared to 316 is clearly evident. At a maximum wall temperature of 550°C , the ratio of swelling lifetimes is 5 to 1.

Fig. 3-1. Systems lifetime comparison of HT-9 with 20% CW 316 for a neutron wall loading of 1 MW/m^2

Lifetime based on crack growth and strain limits is a function of wall thickness and hence is controllable. For each material and for a helium outlet temperature of 525°C , the wall thickness was varied until the lifetime based on crack growth equaled that based on strain limits. For example, increasing the wall thickness decreases the crack growth life since the temperature difference across the wall thickness is increased which means increased bending stress. At the same time, increasing the wall thickness increases the strain limit life since the primary stress is reduced and creep strain is inversely proportional to primary stress. For an outlet coolant temperature of 525°C , life based on crack growth equals that based on strain limits when the wall thickness is 1.2 cm for HT-9 and 0.83 cm for 316. This means that the primary stress for the HT-9 module (86.2 MPa or 12,500 psi) is less than that for the 316 module (123 MPa or 17,800 psi). Wall-averaged temperature for both materials is approximately the same, 465°C , with HT-9 having a slightly greater temperature difference across the wall; but the thermal bending stress is less, 17 MPa

(24,800 psi) as compared to 241 MPa (34,900 psi) for 316. The load ratio (minimum stress divided by maximum stress) is the same for both materials, - 0.33.

It is clear from Fig. 3.1 that the lifetime of the HT-9 module wall, 17.1 MW-yr/m^2 , is approximately five times greater than that of 20% CW 316, 3.5 MW-yr/m^2 . This is attributable to the fact that the HT-9 wall thickness is greater than that of 316, which decreases the primary stress to achieve greater life for the same strain limit, yet not at the expense of decreased crack growth life because the thermal stresses are lower than those of 316. While for this study the specific result for the ratio of lifetimes of HT-9 and 316 depends upon specific design criteria, e.g. the allowed linear strain, the factor of five improvement observed in HT-9 does indicate a significant advantage of using martensitic stainless steels. To more accurately determine the lifetime ratio as well as the actual number of MW-yr/m^2 of life requires additional effort in both depth and breadth. In particular, the lifetime calculation is very dependent on the end-of-life criteria chosen for such things as swelling and maximum strain limits. These criteria can be developed with more confidence only by involving more people in the design of the first wall/blanket and developing designs in greater detail. Also, as pointed out earlier, separate blanket designs need to be developed for each material in order to optimize the advantages each material has to offer.

3.3. IMPLICATIONS FOR POTENTIAL APPLICATION TO ETF

Introduction of martensitic stainless steels to the ETF program would not dramatically impact the design of the first wall/blanket since the proposed doses are presently believed to be within the capability of 20% CW 316 stainless steel. However, introduction of martensitic stainless steels in ETF in conjunction with 316 will allow a comparison of performance to substantiate claims of extended lifetime. In addition, material behavior in a fusion environment can be quantified. Nothing can be a better test for resistance to fusion spectrum irradiation than actual large-dose exposure only the ETF can provide.

REFERENCES

- 3-1 "Doublet Demonstration Fusion Power Reactor Study," Fusion Engineering Staff, General Atomic Report GA-A14742.
- 3-2 Nuclear Systems Materials Handbook, Vol. I, Design Data, TID-26666, Hanford Engineering Development Laboratory (1976).
- 3-3 SOO, P. and McANDREW, J., "Type 303 and Type 316 Stainless Steel Data for High Temperature Design," Westinghouse Report WARD 3045T2C-3.
- 3-4 JAMES, L. A., "Fatigue-Crack Growth in 20% Cold-Worked Type 316 Stainless Steel at Elevated Temperatures," Nuclear Technology 16, 316 (October 1972).
- 3-5 SHAH, R. C. and KOBAYASHI, A. S., "On the Surface Flaw Problem," The Surface Crack: Physical Problems and Computational Solutions, ASME Publication, 79-124 (1972).

4. MEASUREMENT OF MAGNETIC PROPERTIES

4.1. INTRODUCTION

There is very little information in the literature on the magnetic properties of ferromagnetic stainless steels, particularly as a function of temperature and previous cold work and tempering. Some preliminary measurements on 12 Cr 1 Mo (HT-9) were reported (Ref. 4-1) at the First Topical Meeting on Fusion Reactor Materials. This section reports some additional measurements on both 12 Cr 1 Mo and 9 Cr 2 Mo as a function of temperature and bias field. Walter Praeg has done some interesting and important measurements on the latter alloy (Ref. 4-3).

Since these steels are hard magnetic materials, there is considerable area within the hysteresis loop, whose shape changes as the maximum inducing magnetic field is varied. Also the magnetic properties depend on the past history of the material; they are influenced by heat treatment and work hardening.

It is fortunate for tokamak reactor applications that the material near the plasma is completely saturated by the applied steady-state toroidal field and the key parameter in quantifying the magnetic effects on reactor design and operation is the value of the saturation induction.

4.2. PREVIOUS RESULTS

In Ref. 4-1, two types of experiments were reported. In the first, a hollow cylinder with a length several times the outer radius was placed in a steady magnetic field parallel to the symmetry axis. There were two coils wound around the cylinder. One was located about halfway between the equatorial plane and the top end of the cylinder and the other was about halfway between the equatorial plane and the bottom end of the cylinder. The ratio

of the measured mutual inductance to the calculated value for the same geometry in vacuum was determined at 200°C. As shown in Table 5 of Ref. 4-1, the ratio fell from 23 at zero field to about unity for magnetic fields stronger than 2000 oersted. The accuracy of the experiment was only about 10%. In this geometry, in which the fields coupling the coils are nearly parallel to the axis, a value of unity is expected for large bias fields.

The second experiment employed a bias field which was parallel to the symmetry axis of a toroidal transformer with two complete sets of circular windings. The field created by currents in the primary windings is orthogonal to the steady-state bias field, which can be quite large. As Table 4 of Ref. 4-1 indicates, the ac permeability drops from a value of 76 at zero bias to a value of about 1.3 for external bias fields greater than 20,000 oersted. That the permeability does not reach unity is to be expected for this geometry where it should approximate $(B_o + B_{sat})/B_o$. Here B_o is the inducing field in the steel in the absence of a primary current and B_{sat} is the intrinsic induction associated with complete alignment of the magnetic domains in the material (Ref. 4-4).

The calculation of magnetic effects under reactor conditions where the electrical currents and geometry have been specified is very tedious but the only magnetic parameter required is the value of B_s . The IRON code described in Ref. 4-2 and Section 5 performs these calculations expeditiously.

4.3. CURRENT EXPERIMENTS

One purpose of the current experiments has been to compare two representative martensitic stainless steels under simulated tokamak reactor conditions. The most important parameter to measure is the saturation induction, B_s , as a function of temperature. A second purpose has been to examine the response of a material placed in a large ($> B_s$) steady state field B_o when subjected to a smaller, sinusoidal field, thus simulating the toroidal and poloidal fields, respectively. It is found that the area within the hysteresis loops vanishes and the B vs H relationship becomes linear, indicating

that the material is now paramagnetic. The permeability approaches $(B_o + B_s)/B_o$ as $B_o \gg B_s$. If the bias field is parallel to the sinusoidal field, the permeability is unity for $B_o \gg B_s$.

Three toroidal transformers were built; two from 9 Cr 2 Mo and the other from 12 Cr 1 Mo (HT-9). Their characteristics are given in Table 4-1.

Glass covered, #32, copper wire was used for the windings. One layer of glass tape separated the primary from the secondary. A chromel alumel thermocouple was fastened with glass tape to the secondary to measure the temperature of the sample. The transformer was wound with several layers of wet asbestos paper tape until the hole in the torus was almost filled. Successive layers of Min-K insulation, aluminum foil and glass tape were added until the shape approximated an oblate spheroid 4 cm in height and 8 cm in diameter.

The primary served two functions. It provided a magnetizing field and also served as a heater. About ten watts would take the sample to 500°C. The voltage across a shunt resistance in series with the primary was connected to the x-axis of an oscillograph and the voltage developed by the

TABLE 4-1
CHARACTERISTICS OF TOROIDAL TRANSFORMERS USED
FOR MATERIAL EVALUATION

Material	N_P	N_S	A_{core}^2 cm ²	ΔA^2 cm ²	R cm
12 Cr 1 Mo parallel	151	129	0.273	0.048	1.39
12 Cr 1 Mo perpendicular	148	115	0.273	0.048	1.39
9 Cr 2 Mo parallel	186	156	0.341	0.065	1.67

A_{core} is cross sectional area of magnetic core

ΔA is area between core and center of primary wires

R is major radius of torus

N_P is the number of turns in the primary winding

N_S is the number of turns in the secondary winding

secondary was fed through an integrating circuit to the y-axis. The resulting hysteresis loop was photographed and all measurements reported here were made at 60 Hz. The sample size was small enough that eddy current effects were minimal.

The general procedure was to keep the primary current steady until the desired temperature was reached. After the hysteresis loop was photographed, measurements were also made at smaller and larger primary currents as rapidly as possible because the core would cool off or heat up. As the thermal time constant was several minutes, the changes in core temperature from the steady state value are believed to be small.

The three transformers were placed in a cylinder of tightly packed Min-K insulation about 9.5 cm in diameter and 30 cm long. The central coil was of 12 Cr 1 Mo and its axis was horizontal. The other coils were placed as close as possible with their axis vertical. The package slid tightly into a non-magnetic stainless steel tube 150 cm long and the 18 leads run through a hole in the rubber stopper which was stuffed with silicone rubber. The stainless steel tube could be evacuated and back filled with nitrogen or dry air. This assembly was in turn inserted into a glass dewar which penetrated the high field superconducting solenoid. It has a 40 cm bore and a length of 42 cm. The central transformer was located at the center of the superconducting solenoid.

4.4. RESULTS

The hysteresis loops for no bias field were obtained with the sample out of the superconducting magnet. Figure 4-1 shows the hysteresis loops for two different maximum primary currents for 12 Cr 1 Mo at 500°C. Figure 4-2 indicates how the hysteresis loop changes as a function of temperature. The areas become smaller and the saturation inductance decreases with increasing temperature. The maximum magnetizing field used was 300 oersted. To the reactor designer, the only information of interest is the value of the magnetic induction at which the material is well saturated.

Fig. 4-1. Comparison of hysteresis loops of HT-9 at 500°C for two different maximum primary currents

Because of the finite time constant in the integrating circuit and the 300 oersted limit on the magnetizing field, the absolute value for the saturation induction may be in error by 10%, but the relative values for different temperatures and materials are accurate to a few percent.

Figure 4-3 compares 12 Cr 1 Mo with 9 Cr 2 Mo at 500°C. The saturation induction is slightly larger for 9 Cr 2 Mo. Table 4-2 lists some values of the induction near saturation at two temperatures for the three samples.

Fig. 4-2. Comparison of the hysteresis loops for 12 Cr 1 Mo at room temperature and a typical operating temperature in reactor applications

TABLE 4-2
SATURATION INDUCTION AS A FUNCTION OF TEMPERATURE

Temperature	25°C		500°C	
	H	B	H	B
	Oersted	Gauss	Oersted	Gauss
12 Cr 1 Mo, parallel	124	14,100	122	11,100
12 Cr 1 Mo, perpendicular	127	14,200	125	11,900
9 Cr 2 Mo, parallel	133	16,200	127	13,300

Fig. 4-3. Comparison of 9 Cr and 12 Cr martensitic steel hysteresis loops at 500°C

Because of scheduling difficulties and the high cost of liquid helium, only one set of measurements with a large bias field was possible. The striking result is the manner in which the hysteresis loop diminishes in area and becomes a straight line whose slope decreases as the bias field increases. Figures 4-4 and 4-5 illustrate this for the 12 Cr 1 Mo sample with its symmetry axis parallel to the bias field.

When the bias field is increased above 40,000 oersted, appreciable forces are exerted on the primary wires when they carry a current of 10 amp or more. There is evidence that some motion resulted. Before two of the

Fig. 4-4. Variation in hysteresis loops with bias field

primaries developed an open circuit at 60,000 oersted, wiggles appeared about the straight line of the B vs H relationship. A possible interpretation is that motion of the sample or primary leads moved the secondary leads across the large bias field inducing a spurious voltage. The behavior of the 9 CR 2 Mo sample was quite similar to that shown in Fig. 4-5 and the 12 Cr 1 Mo sample except that the value of B_s inferred from the slope of the B vs H line was about 14,000 gauss at 500°C .

Calculation of B_s from the B vs H curve when a bias field is present is not straightforward. The slope of the line is larger than the true permeability, μ , of the core material because the bias field in the sample is less than the value of the bias field produced by the superconducting magnet. For an infinite cylinder in a uniform field, the magnetizing field inside the sample is $2/(1 + \mu)$ times the value of the uniform field. For example,

Fig. 4-5. Magnetic properties in a simulated tokamak environment

in a field of 20,000 oersted with a saturation induction of 10,000 gauss, the permeability, instead of being 1.50, which is its value for a bias field parallel to the axis, now has an apparent value of 1.62. In Fig. 4-5, the saturation inductance has been calculated by an iterative technique from the apparent slope of the B vs H line and the results are consistent with the value of B_s found from the normal hysteresis loop with no bias.

In the case of the coil with the symmetry axis perpendicular to the bias field, part of the torus is parallel to the bias field and part is perpendicular. In this case, the B vs H line approached unity more rapidly as the bias field was increased. This is to be expected since at 40,000 oersted, part of the torus has an effective permeability of unity and part a value of 1.25. Unfortunately, no quantitative data was obtained at very large fields because the primary circuit opened up before calibrated photographs were obtained.

4.5. CONCLUSIONS

For 9 to 12% Cr martensitic stainless steels under reactor conditions where the wall temperature is expected to be about 500°C and the steady

state toroidal field about 40,000 gauss, the saturation induction is about 11,000 to 13,000 gauss. This means that the permeability as far as poloidal field coils are concerned is less than 1.33. In Section 5, it is shown that this requires only minor modifications in reactor design and operations.

There are, however, two additional complications. The poloidal field coils cannot be tested with the toroidal field coils off. Secondly, during startup, the value of the saturation induction will be somewhat larger until the walls reach operating temperature. This is not a major concern, but it should be taken into account in the plans for achieving ignition.

REFERENCES

- 4-1 ROSENWASSER, S. N., MILLER, P., DALESSANDRO, J. A., RAWLS, J. M., TOFFOLO, W. E. and CHEN, W., "The Application of Martensitic Stainless Steels in Long Lifetime Fusion First Wall/Blankets," First Topical Meeting on Fusion Reactor Materials, Miami Beach, Florida, January 29-30, 1979, General Atomic Company Report GA-A15356.
- 4-2 CHEN, W. Y., PEURON, U. A., MILLER, P. H., Jr., RAWLS, J. M. and ROSENWASSER, S. N., Paper 34-04, 8th Symposium on Engineering Problems of Fusion Research, San Francisco, Nov. 13-16, 1979, "Magnetic Aspects of Martensitic Stainless Steels on Structural Materials for Tokamak Reactors," General Atomic Report GA-A15619, October 1979.
- 4-3 PRAEG, W. nF., Paper 4302, 8th Symposium on Engineering Problems of Fusion Research, San Francisco, Nov. 13-16, 1979, Magnetization Hysteresis and Eddy Current Shielding of 9% Cr Stainless Steel as a Function of Temperature.
- 4-4 The terminology used follows the definitions in ASTM Specification A127, with the exception that in the 2nd paragraph of Section 4.2, B_0 is used instead of H.

5. SYSTEMS QUESTIONS

Martensitic 9-12% chromium steels may offer significant advantages as a first wall/blanket material in terms of greater wall lifetime in comparison with commonly considered materials such as 20CW316 (Ref. 5-1). However, it is important to evaluate the magnetics impact of a ferromagnetic blanket in a tokamak reactor. Studies reported here have focused on two potential problem areas: the effects on plasma performance and the impact on the mechanical design of the blanket. A 3-D field code IRON allowing the presence of saturable ferromagnetic material was used for the investigation.

One major concern in the area of plasma performance is the effect of the ferromagnetic blanket on toroidal field ripple. The code IRON described in Section 5.1 was utilized extensively to analyze the toroidal field ripple of tokamak reactors with axisymmetric ferromagnetic (FM) blankets. The reactors considered have sizes and configurations corresponding to TNS (Ref. 5-2), ETF (Ref. 5-3), and STARFIRE (Ref. 5-4). The additional effects created by blanket penetrations such as neutral beam ducts were also studied.

Another concern is the interaction of the ferromagnetic blanket with the plasma shaping and control system, in particular the field-shaping coils. The code IRON was used to study the poloidal field configuration in the presence of a toroidal field, which tends to put the FM blankets into a high level of saturation. The perturbations to the poloidal fields introduced by the FM blankets were computed, and the implications on the required compensations in the poloidal field controls are discussed. The possible time delay in poloidal field control caused by the presence of the FM blankets was also studied.

The most important question from the point of view of engineering impact appears to be the magnetic forces on the blanket. The magnetic

forces on blanket sections of different geometries were analyzed using the code IRON, which was benchmarked by comparison with analytic calculations for axisymmetric geometries. The implications of the resultant forces on the mechanical design of the blanket are discussed.

5.1. DESCRIPTION OF NUMERICAL TOOLS

The 3-D saturable iron field code IRON was developed based on a numerical formulation very similar to that utilized in the field code GFUN3D developed by the Rutherford Laboratory (Ref. 5-5). However, IRON was not designed for the generality GFUN3D was implemented for, so a considerably simplified coil geometry was assumed. In the present version, only a set of toroidal coils and a set of axisymmetric poloidal coils are considered. The second simplification is in the manner in which the shapes of the ferromagnetic elements are specified. Analytic expressions are used to specify the three-dimensional boundary surfaces of the ferromagnetic elements. Typically the surfaces are of cylindrical, spherical or ellipsoidal shapes and the bodies may be further separated into smaller, usually periodic structures, by means of planes. A large number of insulating gaps may be defined by means of pairs of cutting planes. Such a scheme can most effectively take into account the inherent toroidal and poloidal symmetries of the reactor geometries.

The field $\vec{H}$ and magnetization $\vec{M}$ are solved based on an integral equation scheme similar to GFUN3D. The scalar potential ϕ_m is given by

$$\phi_m = \frac{1}{4\pi} \int \frac{\vec{M} \cdot \vec{r}}{r^3} dV \quad , \quad (5-1)$$

and the field due to the magnetized body is

$$\vec{H}_m = - \vec{\nabla} \phi_m \quad . \quad (5-2)$$

The total field is the sum of the field due to the current flow $\vec{H}_c$ and $\vec{H}_m$

$$\vec{H} = \vec{H}_c + \vec{H}_m \quad (5-3)$$

Furthermore, the magnetization $\vec{M}$ is related to $\vec{H}$ by

$$\vec{M} = \chi \vec{H} \quad (5-4)$$

The numerical scheme involves subdividing the FM region into sub-elements (denoted by Greek indices $\alpha, \beta, \dots$) each assigned a constant magnetization $\vec{M}_\alpha$. The subdivision should be sufficiently fine so that discretizing errors are minimized, while keeping the number of sub-elements below a limit so that computation time does not become excessive. If $\vec{H}_\alpha$ and $\vec{H}_{c\alpha}$ are the total field and the field due to coil currents at the center of sub-element α , respectively, then for the case with N sub-elements,

$$\begin{aligned} \vec{H}_\alpha &= \vec{H}_{c\alpha} + \sum_{\beta=1}^N \vec{C}_{\alpha\beta} \cdot \vec{M}_\beta \\ &= H_{c\alpha} + \sum_{\beta=1}^N \chi_\beta \vec{C}_{\alpha\beta} \cdot \vec{H}_\beta \end{aligned} \quad (5-5)$$

The components of the demagnetization tensor $\vec{C}_{\alpha\beta}$ are given by

$$\begin{aligned} C_{\alpha\beta,ij} &= \frac{1}{4\pi} \int_{V_\beta} \left[3(x_{\alpha i} - x'_{\beta i})(x_{\alpha j} - x'_{\beta j}) r^{-5} \right. \\ &\quad \left. - \delta_{ij} r^{-3} - \frac{4}{3}\pi \delta_{ij} \delta(\vec{x}_\alpha - \vec{x}'_\beta) \right] dv'_\beta, \end{aligned} \quad (5-6)$$

where i, j denotes the coordinate indices (x, y, z) , and $\delta(\vec{r})$ is the Dirac delta function. Then the problem is reduced to iteratively solving for

the local field $\vec{H}_\alpha$ and susceptibility χ_α from the set of 3N equations

$$\sum_{\beta=1}^N \sum_{j=1}^3 \chi_\beta \left(c_{\alpha\beta,ij} - \delta_{ij} \delta_{\alpha\beta} \right) H_{\beta,j} = H_{c\alpha,i} \quad (5-7)$$

As compared with GFUN3D in which the basic sub-elements are tetrahedral in shape, in IRON the sub-elements are defined by cutting the FM region with planes perpendicular to the rectangular coordinate axis. In the optional case where cylindrical coordinates are used, the boundaries of sub-elements are simply constant r, z or θ surfaces. The numerical computation of the demagnetization tensor $\vec{C}_{\alpha\beta}$ is simply a summation of Eq. (5-6) over a number of lattice points, and the integrand is a simple power expression free of the singularity and multivaluedness problems which plague the analytically integrated expressions for the demagnetization tensor of tetrahedral sub-elements (Ref. 5-5). Notice that the singularity in Eq. (5-6) when the field point and source point coincide can be easily cut off because the integration of the first two terms of the integrand in Eq. (5-6) yields zero when the field point is situated at the geometric center of a rectangular cube. It was discovered that for a finite sized element such a numerical scheme converges rapidly because the integrand behaves as r^{-3} . Such an approach also avoids the difficulty in implementing the analytical integration scheme for tetrahedral elements which requires a large number of coordinate transformations.

The accuracy of IRON was checked against analytical computations for simple geometries such as a simple ferromagnetic sphere in a uniform external field. In general, the agreement was quite satisfactory. Accuracy to within a fraction of a percent can be achieved typically with about 50 sub-elements. The fact that most of the cases studied involved highly saturated FM elements implies fairly rapid convergence in the iteration. Typically less than five iterations were required to achieve the desired accuracy. The inherent toroidal symmetry also helps greatly in reducing the amount of computation time needed to solve the problem.

5.2. MAGNETIC PERTURBATIONS - TOROIDAL FIELD

Toroidal field ripple computations were based on reactor geometries representing three conceptual designs denoted as GA TNS, ETF and STARFIRE. The characteristic parameters most relevant in this context are given in Table 5-1. The blanket material was assumed to have a saturation magnetization of 1 T, as shown in the experimental results described in Section 4. This is reached when the ambient field is above 2 T, which is the case for all three examples.

Figure 5-1 is a plot of the midplane toroidal field ripple as a function of radius for STARFIRE, for the three cases: (1) non-magnetic blanket, (2) axisymmetric FM blanket, and (3) FM blanket with 0.5 m $\times$ 0.5 m penetration. It can be seen that the axisymmetric FM blanket causes insignificant modification to the field ripple. Even for the case with penetration, the ripple over the plasma region is very modest. Similar results were obtained for studies made on the GA TNS and ETF. Thus it can be concluded that due to the high level of saturation, the FM blankets have effective permeability close to unity, and therefore do not cause any significant changes to the toroidal field ripple.

TABLE 5-1

PARAMETERS USED FOR FIELD PERTURBATION AND FORCE COMPUTATIONS

	GA TNS	ETF	STARFIRE
Plasma major radius, R(m)	3.6	5.0	6.92
Plasma current (MA)	11.4	5	13.5
Number of TF-coils	12	12	12
Total TF ampere turns (MA)	90	132	183
Blanket thickness (m)	1	1	1.3
Outer blanket inside radius (m)	4.6	7.1	8.4
Outer blanket outside radius (m)	5.6	8.1	9.7
Inner blanket inside radius	--	2.8	4.5
Inner blanket outside radius	--	3.0	4.9

Fig. 5-1. STARFIRE toroidal field ripple showing the effects of a ferromagnetic (FM) blanket with and without penetrations

5.3. MAGNETIC PERTURBATIONS - POLOIDAL FIELD

The perturbations in the poloidal fields introduced by the FM blankets have been studied by comparing the poloidal field distributions for the same poloidal current distribution with and without the FM blankets. Because the currents in the poloidal system are much smaller than those of the toroidal system and act on a more open magnetic circuit, the poloidal magnetization cannot change the permeability significantly. This means the parametric coupling between the two systems is very small. Furthermore, it is clear from energy considerations that the permeability reached when building up the final state of currents is independent of the order in which the poloidal and toroidal currents are built up. This is due to the fact that hysteresis effects are negligible at the high level of saturation that the final toroidal currents generate. All of these expectations were verified

during the course of the computational studies. While hysteresis effects were not modeled, it is clear that the hysteresis loop normally traversed by the high magnetic field will likely be so small as to be entirely undetectable.

Figure 5-2 is the plot of poloidal field perturbation as a function of the z-coordinate for various radii in the plasma region for STARFIRE. It can be seen that throughout the plasma region field perturbations as high as 3% can be generated by the presence of the FM blankets. However, since in practice all the poloidal coils are actively controlled for the stability and position control of the plasma, such a small perturbation can be easily countered by adjustment of the control parameters.

Fig. 5-2. STARFIRE poloidal field perturbation due to a ferromagnetic blanket

5.4. MAGNETIC PERTURBATIONS - TRANSIENT ANALYSIS

Several types of transient flux excursions are influenced by the presence of the magnetized blanket. The following events can be considered as the most important operational transients that could eventually be analyzed with sufficient detail with a suitable computational model:

1. Ohmic heating by means of rapidly changing currents in an OH-coil system.
2. Plasma control by means of controlled current excursions in a set of field control coils.
3. Plasma interruption.

As the details of the blanket, especially the shape and degree of subdivision, are not known at this time, we present here only a few broad inferences drawn from basic principles and from transient analyses with a highly idealized model.

Generally speaking, we need now to be concerned only about those relative changes in the transient behavior that are caused by the presence of a sufficiently subdivided magnetic structure. It is assumed from the outset that subdivision into essentially insulated parts will be necessary for any type of metallic blanket, ferromagnetic or not.

In general, the subdivision of the conducting material in electromagnetic power equipment is carried so far that the significant dimension, namely the one which is perpendicular to the direction of the magnetic flux, is roughly of the order of the skin depth at the appropriate frequency. Thus copper busbars in 60 Hz power apparatus are about 5.5 mm thick and steel laminations in transformers about 0.5 mm thick. For pulsed power applications, the thickness can be larger if adequate cooling or heat sinking is provided. In this context, it may be useful to get an idea of what the skin depth is

for magnetically saturated materials at frequencies that roughly correspond to typical plasma interruption ramp times (assumed to be equal to one quarter of the period):

Interruption time	100 μ s	1000 μ s
Approximate equivalent frequency	2500 Hz	250 Hz
Skin depth for $\rho = 96 \mu\Omega$ cm	0.01 m	0.03 m

The relative permeability μ_r used in this calculation [$\delta = (2\rho/\mu_r\mu_0\omega)^{-1/2}$] was 1.2, in agreement with the measured values reported in Section 4, but its exact value is of very little significance here considering the much larger uncertainty in the equivalent frequency and the details of the geometry.

Eddy current effects in cylindrical blankets can be modeled by means of two skin coils, one on each surface of the blanket. If the blanket has one or more radial cuts, their effect is taken into account by specifying a negative number of turns (-1) to one of the coils. Multiple gaps only increase the resistance of the skin-deep current paths.

The applicability of employing the skin depth in this manner has been shown for instance by the successful use of this approach in magneformer design. Further justification is found from the fact that the results are very insensitive to the length of the ramp time.

To estimate the reduction of inductance due to skin effect, the solution of a simplified diffusion equation which describes the penetration of flux into magnetic material was integrated.

To get an idea on how far the effective permeability is actually reduced under transient conditions, we assume an equivalent sinusoidal applied field so that the concept of skin depth can be used. Furthermore, we assume that the blanket is an infinitely long slab of thickness $2b$. The well-known steady state distribution (Ref. 5-6) of the flux is, when x is measured from the vertical center plane,

$$B = B_o \frac{\cosh [(1+j) (x/\sqrt{2\delta})]}{\cosh [(1+j) (b/\sqrt{2\delta})]} \quad (5-8)$$

With $u \equiv (b/\sqrt{2\delta})$ the total flux is

$$\phi = \int_{-b}^b B dx = 2B_o \delta \left[\left(\sin^2 u + \sinh^2 u \right) / \left(\cos^2 u + \sinh^2 u \right) \right]^{1/2} \quad (5-9)$$

Since the dc-flux would be $\phi_o = 2b B_o$ the flux reduction factor due to variable field on the surface is

$$\frac{\phi}{\phi_o} = \frac{\delta}{b} \left[\left(\sin^2 u + \sinh^2 u \right) / \left(\cos^2 u + \sinh^2 u \right) \right]^{1/2} \quad (5-10)$$

The maximum change in the inductance of the coil most affected, namely an equivalent field control coil, was obtained this way. It is equivalent to the actual set of field coils in the sense that it creates the same vertical field at the major radius. Then a transient analysis of an equivalent circuit was carried out using an eigenvector method which gives the semi-analytically computed time-history of the poloidal field at selected points in the plasma region.

The same transient analysis carried out with unaffected inductance values and the difference between the poloidal field magnitudes was calculated as a percentage of the total poloidal field.

It turns out from the integrated solution of the magnetic diffusion equation that the inductance changes only a few percent for a ramp time of 1 ms. It depends on the ratio of the cross-sectional area of the blanket and the poloidal flux area within the entire F-coil. Typical values are about 2% for ETF and STARFIRE and about twice that much for GA TNS, which has only half as much coil area. The final outcome of these and other approximate calculations is that the additional perturbations and time

delays caused by the magnetization of the blanket are small enough not to warrant more accurate analysis. The problems are much the same as if the blanket was nonmagnetic. A few percent increase in time delays or magnitudes is of the same order of magnitude or smaller than the combined errors in model, data and analysis.

5.5. MAGNETIC LOADS

The analysis of stationary and transient body forces was limited in most cases to simple cylindrical structures. Two methods were used, both of which utilized the Maxwell stress tensor. An option in the IRON program permits the computation of all force components in a body of somewhat arbitrary but analytically definable shape through numerical integration over a loosely fitting surface that encloses the magnetic body. A simpler analytical method (Appendix A) gives the radial body force and the equivalent external pressure over an infinitely long cylindrical shell. The methods generally agree (for sufficiently long cylinders) within 15-25%. The simple analytical method always gives the higher values. Since the numerical method is much more time consuming, the results that are given in Table 5-2 have been computed analytically. Thus the forces are somewhat on the pessimistic side.

The table shows that the static pressures amount to only a few atmospheres. Since the forces are directed towards the axis, it should be possible to design the blanket modules so that a self-supporting "keystoned" structure is achieved. The wedge-shaped modules will be under compression. Even in the case of an ellipsoidal blanket this should be possible. The STARFIRE design was also analyzed with the IRON program with a mostly ellipsoidal shell and it was found that the average radial body-force density (without the inner wall) amounts to $1.4 \times 10^6 \text{ n/m}^3$ for $M = 1 \text{ T}$, in agreement be expected from the analytical results.

Figure 5-3 shows a series of analytically computed equivalent magnetic pressures as a function of magnetization times packing factor. This product was chosen as the argument because in the reactor blanket, which serves both

TABLE 5-2
RADIAL BODY FORCES AND EQUIVALENT INWARD PRESSURES
AT TWO VALUES OF M*

	Design					
	GA TNS		ETF		STARFIRE	
	Inboard	Outboard	Inboard	Outboard	Inboard	Outboard
Major radius	3.6		5.0		6.92	
On axis toroidal field (T)	5		5.3		5.3	
Blanket:						
Inside radius (m)	--	4.6	2.8	7.1	4.5	8.6
Outside radius (m)	--	5.6	3.0	8.1	4.9	9.7
Inward pressure (atm)						
M = 0.5 T	--	2.8	2.3	1.9	2.5	2.35
M = 1 T	--	6.4	5.15	4.4	5.5	5.3
Body force (n/m ³)						
M = 0.5 T	--	0.31×10^6	1.3×10^6	0.21×10^6	0.7×10^6	0.2×10^6
M = 1 T	--	0.7×10^6	2.7×10^6	0.46×10^6	1.5×10^6	0.44×10^6

* M = saturation magnetization times packing factor.

Fig. 5-3. Magnetic loading on the ferromagnetic blankets for the GA TNS, STARFIRE and ETF designs

for breeding and energy conversion, a large fraction of the volume is occupied by the breeding materials and coolant flow channels, and the FM material only occupies typically 10–20% of the volume. The low packing factor effectively reduces the magnetic loading on the blanket.

With regard to the transient loads generated on the blankets due to plasma disruption, a FM blanket should behave almost the same as a non-FM blanket.

5.6. SAFETY AND ENVIRONMENTAL CONSIDERATIONS

The radioactivity and biological hazard potentials (BHP) for ferritic steel-structured blankets are important for maintenance and environmental safety considerations. The afterheat of the blanket is a crucial parameter both for blanket operation after shutdown and in the case of a loss of coolant flow accident. In order to estimate these parameters quantitatively, several neutronics calculations have been performed using the one-dimensional transport code, ANISN (Ref. 5-7), with P_3S_6 approximations, in cylindrical geometry.

The blanket model adopted consists of a 10 mm first wall, a tritium breeding zone and a graphite reflector. The breeding zone is composed of 10% structure and 90% Li_2O by volume. The lithium breeding compound, Li_2O , is assumed to be 80% dense to account for the packing effect. In the neutron flux calculation, the breeding zone thickness is 0.25 m which is adequate to generate a total tritium breeding ratio in excess of unity. The results for the blanket with HT-9 and 316 SS as structural material are 1.19 and 1.17, respectively. Hence HT-9 does have a modest advantage in terms of breeding efficiency.

The fluxes so obtained are then used as input to the fusion radioactivity calculated code DKR (Ref. 5-8). The results are shown in Tables 5-3, 5-4 and 5-5 for radioactivity, BHP and afterheat, respectively, and in each instance the results for 316 SS are exhibited for comparison with HT-9. A standard operational scenario is adopted for purposes of comparison, specifically, continuous operation for 6 years with a neutron wall loading of 1 MW/m^2 , followed by shutdown. As seen in Table 5-3, the radioactivity in the HT-9-structured blanket at shutdown is about 1.17 curies per watt (thermal), which is about 10% lower than in the 316 SS-structured blanket. At 100 years after shutdown, it is about 1.8×10^{-5} curies per watt, which is more than a factor of 2 lower than in the 316 SS-based blanket.

TABLE 5-3
RADIOACTIVITY AFTER SHUTDOWN IN HT-9
AND 316 SS-BASED BLANKETS (ASSUME
 1 MW/m^2 WALL LOADING FOR 6 YEARS)

TIME AFTER SHUTDOWN	STRUCTURAL MATERIAL*	
	316 SS	HT-9
0	1.31	1.17
1 DAY	7.8×10^{-1}	6.6×10^{-1}
1 MONTH	6.6×10^{-1}	5.9×10^{-1}
1 YEAR	3.7×10^{-1}	4.0×10^{-1}
100 YEARS	4.3×10^{-5}	1.8×10^{-5}

* UNITS: CURIE PER WATT (THERMAL)

TABLE 5-4

BIOLOGICAL HAZARD POTENTIAL AFTER SHUTDOWN IN HT-9 AND 316 SS-STRUCTURED BLANKETS (ASSUME 1 MW/m² WALL LOADING FOR 6 YEARS)

TIME AFTER SHUTDOWN	STRUCTURAL MATERIAL*	
	316 SS	HT-9
0	6.7×10^2	3.2×10^2
1 DAY	4.1×10^2	2.5×10^2
1 YEAR	9.7×10^1	5.2×10^1
100 YEARS	4.3×10^{-1}	2.1×10^{-2}

* UNITS: km³ OF AIR/kW

TABLE 5-5

AFTERHEAT FOLLOWING SHUTDOWN IN HT-9 AND 316 SS-BASED BLANKETS

TIME AFTER SHUTDOWN	STRUCTURAL MATERIAL*	
	316 SS	HT-9
0	1.4	1.3
1 DAY	1.5×10^{-1}	5.9×10^{-2}
1 MONTH	1.3×10^{-1}	5.4×10^{-2}
1 YEAR	4.7×10^{-2}	3.0×10^{-2}

* UNITS: PERCENT OF TOTAL OPERATING POWER

This trend also affects the biological hazard potential. It is about 3.2×10^2 km³ of air/kW_{th} at shutdown and decreases to about 5.2×10^1 and 2.1×10^{-2} km³ of air/kW_{th} 1 year and 100 years after shutdown, respectively, for the HT-9-structured blankets. As illustrated in Table 5-4, the biological hazard potential for the 316 SS-based blankets is about a factor of 2 higher than that in the HT-9-based blankets.

As seen from Table 5-5, the afterheat at shutdown for HT-9 and 316 SS-based blankets are about the same, 1.3 percent of total operating power. However, at one day after shutdown, it drops to about 5.9×10^{-2} and 1.5×10^{-1} percent of operating power for HT-9 and 316 SS-based blankets, respectively. The afterheat for the HT-9 blanket maintains a lower level than that of the 316 SS blanket thereafter.

From the above discussion, we may conclude that the post-irradiation effects for the HT-9-based blanket, i.e., the amount of after-shutdown radioactivity, BHP and afterheat, are smaller than for the 316 SS-based blanket. From these points of view, HT-9 alloy is a preferable choice of structural material than 316 SS for fusion applications.

REFERENCES

- 5-1 ROSENWASSER, S. N., et al., "The Application of Martensitic Stainless Steels in Long Lifetime Fusion First Wall/Blanket," paper presented at the First Topical Meeting on Fusion Reactor Materials, Bal Harbour, Florida (1979).
- 5-2 "GA TNS Project, Status Report for FY-78," General Atomic Report GA-A15100, Vol. V (1978).
- 5-3 SAGER, P. H., "Design Considerations for the Fusion Engineering Test Facility," paper to be presented at this conference.
- 5-4 BAKER, C. C., et al., "STARFIRE - Commercial Tokamak Power Plant Overview," paper to be presented at this conference.
- 5-5 ARMSTRONG, A., et al., "New Developments in the Magnet Design Computer Program GFUN3D," Proc. 5th Intl. Conf. on Magnet Technology, Rome, Italy, April 1975.
- 5-6 GREENWOOD, A., "Electrical Transients in Power Systems," Wiley-Interscience (1971).
- 5-7 ENGLE, W. W., Jr., "Uses Manual for ANISN," Report No. K-1693, Oak Ridge Gaseous Diffusion Plant (1967).
- 5-8 SUNG, T. Y. and VOGELSANG, F., "DKR: A Radioactivity Calculation Code for Fusion Reactors," Report No. UWFD-170, Nuclear Engineering Dept., University of Wisconsin (1976).

APPENDIX A

A SIMPLE ANALYTICAL METHOD FOR THE CALCULATION OF FORCES ON MAGNETIZED BLANKET REACTORS

Consider a long cylindrical gapped blanket magnetized in the azimuthal direction by a large number of toroidal field coils or by a single current-carrying concentric rod with symmetric return paths. The resulting vacuum field is given by

$$H_o = \frac{I_T}{2\pi r} \quad (A-1)$$

We assume that the toroidal current I_T is large enough so that the blanket saturates and we can use the simple linear expression

$$B_f = M + \mu_o H_f \quad (A-2)$$

for the magnetization curve. In typical fusion confinement systems the effective saturation magnetization M would be of the order of 1 tesla or

less for a martensitic stainless steel and the induction B_f is larger than 3 tesla. Equation A-2 can be written

$$H_f = (B_f - M)/\mu_o \quad . \quad (A-2a)$$

Neglecting the fringing flux and applying Ampere's Law $I_T = \int H ds$ so the magnetic circuit around a blanket which contains n identical sectors and insulating gaps of length g we have, for any r between r_i and r_o

$$I_T = ngH_g + n\ell_f H_f \quad , \quad (A-3)$$

or, per sector:

$$I_g \equiv \frac{I_T}{n} = gH_g + \ell_f H_f = gH_g + \ell_f \frac{B_f - M}{\mu_o} \quad ,$$

therefore:

$$H_g = \frac{I_g + \ell_f M/\mu_o}{g + \ell_f} \quad , \quad (A-4)$$

where $\ell_f = (2\pi r - ng)/n$ is the length of the flux path in any "iron" sector. Furthermore we apply the usual continuity of field condition on the curved surfaces: $H_\theta = H_f$, $r = r_i$ or $r = r_o$. Continuity of flux, on the other hand, requires that $B_f = \mu_o H_g$ across the boundaries of the air gaps. With Eq. A-2 substituted, this gives

$$H_\theta = H_f = H_g - \frac{M}{\mu_o} \quad . \quad (A-5)$$

Equations A-4 and A-5 give the necessary field values for the evaluation of forces when using the Maxwell tensor approach. The forces due to magnetization are obtained most conveniently by calculating a surface integral of Maxwell stresses over any closed surface that contains only the magnetized body in question.

The normal and tangential components of the Maxwell stresses, considered as tractions per unit area, are

$$t_n = 0.5 \mu_o (H_n^2 - H_t^2)$$

$$t_t = \mu_o H_n H_t$$

We consider first the outer and inner curved surfaces $r = r_o$ and $r = r_i$. Here $t_n \equiv t_r$, $t_\theta \equiv t_\theta = 0$, $H_n \equiv H_r = 0$ and $H_t \equiv H_\theta = H_f = H_g - M/\mu_o$. The x directed components of the radial force per unit axial length are

$$F'_{x_o} = -0.5 \mu_o \int_{-\alpha/2}^{\alpha/2} H_{\theta_o}^2 \cos \theta r_o d\theta = -\mu_o H_{\theta_o}^2 r_o \sin \frac{\alpha}{2}$$

$$F'_{x_i} = 0.5 \mu_o \int_{-\alpha/2}^{\alpha/2} H_{\theta_i}^2 \cos \theta r_i d\theta = 0.5 \mu_o H_{\theta_i}^2 r_i \sin \frac{\alpha}{2}$$

The net x-force due to these components is

$$F'_x = F_{x_i} + F_{x_o} = \mu_o \sin \frac{\alpha}{2} \left[r_i H_{\theta_i}^2 - r_o H_{\theta_o}^2 \right],$$

where

$$H_{\theta_i} = H_{f_i} = H_{g_i} - \frac{M}{\mu_o} = \frac{I_g + \ell_i M/\mu_o}{g_i + \ell_i} - \frac{M}{\mu_o}$$

$$H_{\theta_o} = H_{f_o} = H_{g_o} - \frac{M}{\mu_o} = \frac{I_g + (r_o/r_i) \ell_i M/\mu_o}{(r_o/r_i) (g_i + \ell_i)} - \frac{M}{\mu_o},$$

This conforms with the notion of a hydrostatic pressure acting perpendicularly to the flux lines. According to the same point of view, there is also a tension acting along the flux lines. The forces due to this tension will be calculated next.

An azimuthally directed force F_θ acts on each gap surface and gives rise to a radially directed net force $F''_x = 0.2 F_\theta \sin(\alpha/2)$. The magnitude of F_θ is obtained by integrating the normal Maxwell stress $t_n = 0.5 \mu_o H_g^2$ from r_i to r_o , hence

$$F''_x = -2 \sin(\alpha/2) \int_{r_i}^{r_o} \left[\frac{I_g + (r/r_i) \ell_i M/\mu_o}{(r/r_i) (g_i + \ell_i)} \right]^2 dr$$

The net x-directed force $F_x = F'_x + F''_x$ which we assume to act at the centroid of the rigid sector is, after the evaluation of the integral

$$F_x = \frac{-\mu_o \sin(\alpha/2)}{(g_i + \ell_i)^2} \left\{ r_o \left(\frac{r_i}{r_o} I_g + \ell_i M/\mu_o \right)^2 - r_i \left(I_g + \ell_i M/\mu_o \right)^2 + \left[I_g^2 r_i^2 \left(\frac{1}{r_i} - \frac{1}{r_o} \right) + \left(2 I_g r_i \ell_i M/\mu_o \right) \ln \frac{r_o}{r_i} + (r_o - r_i) \left(\frac{\ell_i M}{\mu_o} \right)^2 \right] \right\}$$

This force is computed per unit axial length of the cylindrical blanket (or shield) sector. The net body force per unit volume or an equivalent external hydrostatic pressure may be more illustrative. The result for the body force per unit volume is

$$f_b = \frac{2\mu_o I_g^2 \sin(\alpha/2) r_i/\ell_i}{\pi(r_o^2 - r_i^2)} \left(\frac{r_i}{r_o} - 1 - \frac{\ell_i M}{\mu_o I_g} \ln \frac{r_o}{r_i} \right)$$

and the equivalent external pressure is given by

$$p_e = \frac{\mu_o I_g^2 r_i/r_o}{2\pi \ell_i} \left[\frac{r_i}{r_o} - 1 - \frac{\ell_i M}{\mu_o I_g} \ln \frac{r_o}{r_i} \right]$$