TIBER—Tokamak Ignition/Burn Experimental Research Final Design Report

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CONTENTS

<u>ABSTRACT</u>	1
<u>1.0. OVERVIEW</u>	5
1.1. DESIGN DESCRIPTION	6
1.2. NUCLEAR HEATING OF TF COILS	11
1.3. PLASMA OPERATION	12
1.4. MAGNET DESIGNS	15
<u>2.0. PLASMA MODEL FOR REFERENCE-CASE TIBER OPERATION</u>	21
2.1. INTRODUCTION	21
2.2. TIBER OPERATING SCENARIO	22
2.2.1. The Ignition Mode	23
2.2.2. The Steady-State Current-Drive Mode	24
2.3. THE PLASMA MODEL AND REFERENCE-CASE TIBER PARAMETERS	27
2.3.1. Power Balance	27
2.3.2. Current-Drive Power Requirements	30
2.4. LASER-DRIVEN PELLET INJECTION FOR TIBER	34
<u>3.0. THE EDGE PLASMA AND THE PUMPED LIMITER</u>	45
3.1. INTRODUCTION	45
3.2. DESIGN OBJECTIVES OF THE PUMPED LIMITER	45
3.3. CONFIGURATION OF THE PUMPED LIMITER	47
3.4. THE COMPUTATIONAL MODEL	49
3.5. POWER TRANSPORT IN THE STREAM REGION	51
3.6. POWER TRANSPORT IN THE LIMITER REGION	55
3.7. POWER BALANCE AT THE LIMITER SURFACE	58

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3.8.	HEAT LOAD ON THE LIMITER	60
3.9.	GAS PENETRATION	61
3.10.	VACUUM PUMPING	63
3.11.	EROSION BY SPUTTERING	68
3.12.	DISCUSSION OF THE LIMITER DESIGN	69
	<u>4.0. MAGNETIC CONFIGURATION</u>	75
4.1.	PLASMA EQUILIBRIUM	75
4.2.	MAGNETIC ANALYSIS	79
	<u>5.0. TF-COIL NUCLEAR SHIELDING</u>	81
5.1.	INTRODUCTION	81
5.2.	RESULTS	87
5.3.	RECOMMENDATIONS	91
	<u>6.0. PUSHER COIL DESIGN</u>	93
6.1.	OW FIELD CONDUCTOR	93
6.2.	HIGH FIELD CONDUCTOR	94
	<u>7.0. TF WINDING-PACK DESIGN</u>	97
7.1.	PRELIMINARY WORK	97
7.1.1.	Heat Removal	97
7.1.2.	Mechanical Loads	98
7.1.3.	Stability	98
7.2.	CURRENT DEVELOPMENTS	99
7.2.1.	Updated Critical Current Database	101
7.2.2.	Corrections for Cooldown Strains	104
7.2.3.	Stability	106
7.2.4.	Effect of End-of-Life Damage to Copper Stabilizer	110

7.2.5. Limiting Current	111
7.2.6. Protection	112
7.3. SUMMARY	116
<u>8.0. MECHANICAL STRUCTURE</u>	123
8.1. FUNDAMENTAL CONSIDERATIONS FOR TOKAMAKS	123
8.2. THE COLD ISLAND	123
8.3. PF COIL REMOVAL	129
8.4. OVERTURNING MOMENT ON INBOARD LEGS	132
8.5. THE UPPER COIL CASE	134
8.6. THE SHIELD	135
8.7. INSTALLATION OF NEUTRON SHIELD	136
8.8. THE VACUUM ENCLOSURE	138
8.9. COMBINED STRESSES IN THE TF-COIL INBOARD LEGS AND THE CENTER POST	139
8.9.1. Thrust on Center Post	139
8.9.2. Torque on Inboard Legs	139
8.9.3. TF-Coil Shear Connections	144
8.9.4. Torque on Center Post	146
8.10. COMBINED STRESSES IN THE UPPER TF-COIL CASE	147
8.11. COMBINED STRESS IN THE LARGE PF-COIL SUPPORT STRUCTURE	149
<u>9.0. CRYOGENIC SYSTEM</u>	153
9.1. INTRODUCTION	153
9.2. COIL DESIGN AND ANALYSIS	154
9.2.1. TF Coil - Neutron Heat Load	154
9.2.2. TF Coil - Thermal Heat Load	158

9.2.3. Pusher Coil - Neutron Heat Load	159
9.2.4. Pusher Coil - Thermal Heat Load	159
9.2.5. PF Coils - Neutron Heat Load	160
9.2.6. Conductor-Lead Cryogenic Loads	161
9.3. CRYOGENIC SYSTEM	162
9.3.1. General	162
9.3.2. Helium Refrigerator System (4.5 K)	163
9.3.3. Helium Refrigerator System (1.8 K)	169
9.3.4. Helium System	172
9.3.5. Liquid Nitrogen System	175
9.4. COST SUMMARY	175
9.5. CONCLUSION	176
10.0. <u>SYSTEM INTEGRATION AND COSTS</u>	177
10.1. TIBER WORK BREAKDOWN STRUCTURE	177
10.2. COMPILATION OF COSTS	179
11.0. <u>IMPROVED CONCEPTS FOR STEADY-STATE TOKAMAKS</u>	183
11.1. AN APPROACH TO CONCEPT IMPROVEMENT	183
11.2. PARAMETRIC SCANS VS ASPECT RATIO	186
11.3. MAGIC5	200
11.3.1. Plasma Performance Modelling of Magic5	201
11.4. COST REDUCTIONS AND PERFORMANCE IMPROVEMENTS	213
11.5. CONCLUSIONS	232

ABSTRACT

The Tokamak Ignition/Burn Experimental Research (TIBER) device is the smallest superconducting ignition tokamak designed to date. To reduce the size and cost of the tokamak, we had to make a number of aggressive design assumptions. For example, plasma shaping is used to achieve a high plasma beta (the ratio of plasma pressure to magnetic field pressure). In addition, neutron shielding is minimized to achieve the desired small device size (major radius of 2.6 m, plasma height of about 0.8 m). However, the superconducting toroidal-field magnets must still be shielded sufficiently to reduce the neutron heat load and the gamma-ray dose to various components of the device. In particular, the insulation must retain adequate strength and electrical properties after irradiation to very high end-of-life neutron fluences (greater than 10^{19} neutrons/cm²) and gamma-ray doses (above 10^{10} rads), especially in those portions of the magnet adjacent to the shield penetrations required for diagnostics on the plasma-heating systems. For TIBER, the peak fusion heating rate in the first layer of the superconducting toroidal-field coil is calculated to be 22 mW/cm³.

In TIBER a high-field (14-T) plasma-shaping coil is used in the usual position of the ohmic heating coil. By shaping the poloidal flux to produce a modest plasma indentation, we achieve a plasma beta of 10% (about twice that normally achieved in a tokamak). All inner-radius components of the tokamak must be kept as small as possible so that the plasma major radius can be minimized and plasma shaping can be maximized. Accordingly, high magnet current densities of 4 kA/cm² and an integrated structural design are required.

Noninterlinking toroidal- and poloidal-field coils are used in TIBER to permit easy maintenance. The entire tokamak is enclosed in a single vacuum vessel, similar to the practice on the Magnetic Fusion Test Facility (MFTF) at LLNL. This minimizes the internal dimensions of the device and provides easily serviced external vacuum joints for rapid disassembly. Full shielding of the magnet systems is achieved with fitted shield modules that are accessible for maintenance and repair without magnet removal.

The fusion heat deposited in the magnets (about 43 kW) is removed by flowing liquid helium through the heat conductors. In steady state, the helium liquefier required to support the entire cryogenic heat load is 50 kW, about three times the size of the system constructed for the MFTF. An additional 26,000-litre Dewar and heat exchanger is needed to precool the helium before it enters the magnet coils. After circulating through the coils, the helium is allowed to expand through a Joule-Thomson valve and refill the storage Dewar.

The use of fast-wave, lower hybrid rf heating induces a plasma current above 10 million amperes at a low plasma density of about 0.4×10^{14} ions/cm³. Then a laser-driven pellet fuel injector raises the plasma density to 3×10^{14} ions/cm³, with continued fast- and slow-wave lower hybrid heating to achieve ignition. Because the current drive is not efficient at high plasma density, the current would decay in about 300 seconds. During this period, all plasma heating is turned off to study pure ignition physics. As the plasma current decays to 7 million amperes and the plasma density drops to 10^{14} ions/cm³, slow-wave lower hybrid heating would

again be used to sustain a steady-state plasma with an average wall loading of 0.8 MW/m^2 (peak 2.1 MW/m^2) and a plasma Q (ratio of energy applied to energy released) of about 10. Thus, the TIBER device can be used to study ignition physics and, when operating in a steady-state, current-driven mode, to study plasma-wall interactions, helium ash removal, and neutron damage effects.

1.0. OVERVIEW (C. D. Henning)

A Tokamak Ignition/Burn Experimental Research device (TIBER) has been designed to represent the smallest superconducting ignition experiment consistent with the Office of Fusion Energy (OFE) Mission-II physics and engineering objectives. To reduce the size and cost of the tokamak, more aggressive design assumptions are necessary. Plasma shaping is used to achieve higher beta, and neutron shielding is minimized. However, the superconducting toroidal field (TF) magnets must be sufficiently shielded to reduce neutron heat load to the coolant, neutron fluence to the superconductor and stabilizer, and gamma doses to the insulator.¹ In particular, the insulation must retain adequate strength and electrical properties after irradiation to end-of-life fluences above 10^{19} n/cm² and gamma doses above 10^{10} rads, especially in those portions of the magnet adjacent to shield penetrations for diagnostics on plasma-heating systems. The peak nuclear heating rate in the superconducting TF coil is 22 mW/cm³, resulting in a total system heat load of 50 kW. Both the TF and poloidal field (PF) coils would be constructed of force-cooled Nb₃Sn to achieve high current density with acceptable nuclear heat removal and neutron damage limits.

1.1. DESIGN DESCRIPTION

A point design of the TIBER device is summarized in Table 1-1. As depicted in Fig. 1-1, a high-field (14-T) pusher coil is used in the normal position for the ohmic heating coil. By effecting a modest plasma indentation, it achieves a beta of 10.6 percent. All inner-leg components of the tokamak must be kept as small as possible so that the plasma radius can be minimized and plasma shaping maximized. Accordingly, magnet current densities of 4 kA/cm^2 and an integrated structural design are necessary. The neutron shielding of the inner leg ranges between 40 and 45 cm.

Noninterlinking TF and PF coils are used in TIBER for easy maintenance. Also, the entire tokamak is enclosed in a single vacuum vessel. This is similar to the practice on the Mirror Fusion Test Facility. In this way internal dimensions and complications are minimized, leading to easily serviced external vacuum joints for rapid disassembly.

The critical part of the TIBER configuration is the region between the center post and the plasma at the midplane of the machine. The radial build of the post coils and shield determines the overall size of the device. Accordingly, we have concentrated on the design of the pusher and TF coils near the center post. The magnetic pressure on the inboard side of the TF coil case, about 60 MPa (9 ksi), is partially offset by the radial outward pressure of the pusher coil, which may develop as much as 20 MPa (3 ksi); note that no credit is taken for this offset in the design. There is also a large hoop stress on the TF coil that will be shared by the TF coil case and the center post.

Table 1-1. TIBER reference-case physics parameters.

Fixed parameters

Major radius $R = 2.60$ m	Toroidal field $B_T = 5$ T
Minor radius $a = 0.73$ m	Fractional radiation losses $(1 - f_q) = 0.2$
Aspect ratio $A = 3.6$	Ion confinement: Neoclassical/2
Elongation 1.94	Electron confinement: Neoalculator/2
Triangularity 0.60	Beta: $0.04 I_p / (a B_T)$
Indentation 0.05	

<u>Variable parameters</u>	<u>Pulsed ignition mode</u>	<u>Steady-state current-drive mode</u>
Plasma current I_p (MA)	9.7	7.4
Avg. toroidal beta β_T (%)	10.6	8.1
Ignition margin M	1.5	0.60
Ion/electron transport χ_i/χ_e	1.1	0.1
Ion temperature T_i (keV)	10	30
Electron temperature T_e (keV)	10	24
Avg. edge safety factor q_e	2.2	3.2
Neutron wall load Γ_n (peak) (MW/m^2)	4.0 (1.6 avg.)	2.1 (0.8 avg.)
Fusion power P_{fusion} (MW)	440	222
Avg. density n_e (10^{20} m^{-3})	3.3	0.94
Current drive power P_{CD} (MW)	0	22.4

Figure 1-1. TIBER cross section with pusher coil and shaped plasma.

All of the structural elements will be operating at 1.8 to 4.3 K, so as to allow the use of high design stresses. We have selected a peak design stress limit of 536 MPa (80 ksi) for the stainless steel pusher-coil structure and those portions of the TF coil in the region of the pusher coil. The critical region of the TF coil has been modeled with the stress analysis code GEMINI, which has proved to be a useful tool to help us understand the complex stresses and deflections in the coil.

To conserve space in TIBER, we have considered both a pumped limiter and an open, internal divertor. Either choice requires an analysis of the edge plasma and recycling at the dump plates. Initial calculations indicate that

a large recycling fraction is needed to cool the edge plasma and reduce the sputter erosion at the dump plates and at the walls. The choice of parameters and of either a pumped limiter or an open divertor depends on the space available outside the separatrix. This causes a compromise between the space available for neutron shielding of the magnets and the space needed for recycling the edge plasma.

The dump plates in TIBER are similar to those in the halo dump in MARS.² The plates are sized and contoured to handle the heat load without regard to gas removal. The small pumping ports that remove the gas through the plates are located as needed to control the amount of recycling. In the case of a pumped limiter, this vented-port concept has the additional advantage of moving the leading edge out of the plasma to where heat and erosion problems are minimized. Figure 1-2 sketches a pumped limiter using this concept. Each small port has its own leading edge, but the large surrounding area allows adequate cooling.

Because of the toroidal magnetic field, the plasma contacts the dump plates at grazing incidence and cannot stream through the small ports to be removed by the vacuum pumps. Gas is removed through the ports when the gas pressure on the plasma side of the plates exceeds that in the pumping duct behind the plates. This sets an upper limit on the gas pressure in the duct, but this limit is rather high. When the recycling fraction of the edge plasma is sufficiently high to protect the dump plates, the gas pressure on the plasma side must be nearly equal to the plasma pressure there. This pressure is on the order of 0.1 torr. It should, therefore, be possible to obtain a pressure of a few tens of millitorr in the duct that

Figure 1-2. A pumped limiter with a ported neutralizer plate. Although not clear from the figure, the field lines intersect the plate at grazing angles of incidence.

would allow mechanical pumps to remove the gas. The duct space behind the plates can be thin, tapering up to only 4 cm maximum, due to the high pressure and the fact that the duct space extends completely around the torus.

One 0.19-m-diameter vacuum pipe at the top and another at the bottom passes between each of the TF coils. The shielding displaced by these pipes is located between the coils, so that the neutron flux to the coils is only increased slightly.

1.2. NUCLEAR HEATING OF TF COILS

Estimates of nuclear heating in the TF coils for three cases are given in Fig. 1-3 where the major radius and the mode of plasma removal (divertor or pumped limiter) are varied. These estimates are based on 1-D slab-shielding calculations applied to the TIBER geometry using a generic tokamak neutron-source distribution. The major element in the shield is tungsten, making it space efficient, but expensive. Thus, it should be used only where space is limited, mainly on the inner leg.

Using this generic neutron-source distribution from Ref. 3 results in a peak neutron wall loading of 2.1 MW/m^2 on the outer leg. This distribution is very favorable because it allows for the possibility of a nuclear-component test program at near reactor levels, even though the average wall loading is 0.8 MW/m^2 . Certain groups of tests suitable for TIBER have been identified. These are

- Structural mechanics
- Breeder/multiplier structure interaction
- Thermal hydraulics
- Tritium production
- Nuclear heating and low fluence damage
- Instrumentation and control

Case	P-fusion (MW)	Heating (kW)	Wall loading (MW/m ²)
1: R = 2.6 m with divertor (shown)	200	415	2.1
2: R = 2.73 m with divertor	237	96	2.3
3: R = 2.6 m with pumped limiter	200	43	2.1

Figure 1-3. TIBER--estimate of nuclear heating in the coils and cases and in the first wall loading at midplane of outer leg.

1.3. PLASMA OPERATION

As illustrated in Fig. 1-4, we envision that fast-wave, lower hybrid heating would be used to induce a plasma current of 10.6 MA at low-plasma density around 0.4×10^{14} ions/cm³. Then, a laser-driven pellet fuel injector would raise the density to 3×10^{14} ions/cm³, with continued fast-

Figure 1-4. Both pure ignition and high-Q steady-state physics can be explored in a Mission-II device.

and slow-wave, lower hybrid heating to achieve ignition. Because the current drive is not efficient at high plasma density, the current would decay with a time constant of 300 seconds. During this period all plasma heating would be turned off in order to study pure ignition physics. As the plasma current decays to 7.4 MA and density drops to 0.94×10^{14} ions/cm³, the slow-wave, lower hybrid heating would again be used to sustain a steady-state plasma with a wall loading of 0.8 MW/m^2 and a plasma energy gain Q of about 10. Thus, the TIBER device can be used to study ignition physics and

can also be operated in a steady-state, current-driven mode to study plasma-wall interactions, helium ash removal, and numerous neutron-damage effects.

The confinement scaling used for the TIBER operation depicted in Fig. 1-3 is separated into different scalings for ions and electrons. The electron confinement is taken to be neoalculator, $\chi_e = 1.5 f_e a \sqrt{k} / (R^2 n_{20})$, where f_e is an anomaly factor to account for high beta deterioration ($f_e = 2$ is used). The ion confinement is taken to be neoclassical

$$\chi_i = 4.1 \times 10^{-2} f_i (R/a \sqrt{k})^{3/2} \langle q \rangle^2 n_{20} / (B_t^2 T_{10}^{1/2})$$

where f_i is an anomaly factor ($f_i = 2$). Using the Troyon-Wesson beta limit

$$\langle \beta_t \rangle < 0.04 \frac{I_p \text{ (MA)}}{a B_t \text{ (T)}}$$

the ignition condition is

$$I_p^2 \geq \frac{938 M_{eff} a T_{10} \left(\frac{\chi_i}{\chi_e} + \frac{T_e}{T_i} \right) \left(1 + \frac{T_e}{T_i} \right)^2 f_e}{R^2 B_t^2 \sqrt{k} f_\alpha (\sigma v_{corrected})}$$

where M_{eff} is the effective ignition margin. During the 50-s, high density, pure-ignition period shown in Fig. 1-4, $M_{eff} > 1.5$ and $T_e/T_i \approx 1$. During

the steady-state current drive ($t > 300$ s in Fig. 2-4), $M_{eff} = 0.6$ and $T_e/T_i \simeq 0.8$ result from the high temperatures with the current drive power input augmenting the alpha heating. The current-drive power input P_{cd} is calculated from Karney and Fisch⁴:

$$P_{cd} = 2.78 \frac{I_p (MA) R(m) n_{20}}{\left[1 + \left(\frac{T_{10}}{2.5} \right)^{1.16} \right]}$$

where the $1 + (T_{10}/2.5)^{1.16}$ factor in P_{cd} is a fit to Karney and Fisch curves for current drive efficiency, averaged over parallel indexes $\eta_{||} = 1.5$ to 2.0, taking into account favorable relativistic effects at high T_e .

1.4. MAGNET DESIGNS

One of the more challenging magnet designs is the 14-T pusher coil shown in Fig. 1-5. However, we utilized conductor designs that have proven to be functional and within the present manufacturing capabilities. The outer niobium-titanium section of the pusher coil has the same conductor as was used for the MFTF Yin-Yang coils with an inner niobium-tin coil like that used for the MFTF choke coil. Each coil is immersed in superfluid helium to increase its current density and stability.

The original MFTF Yin-Yang conductor produced a peak field of 7.8 T at 4.2 K. Reduced temperature operation at 1.8 K yields an even higher

Figure 1-5. The TIBER pusher (plasma-shaping) coil.

critical current in the superconductor at 10-T fields. This implies that the current in the original MFTF winding pack can be increased by 28% to satisfy the requirements of the Nb-Ti background coils. The heat flux for stability in the high field pusher is $(1.28)^2 \times 0.19$ or 0.31 W/cm^2 , which is manageable using He II. Hence, the Yin-Yang conductor in the outer section of the pusher coil can operate at a current of 7399 A. Hoop forces must be reacted to the external case structure.

For the inner high field section of the pusher coil, a Nb₃Sn:Ti conductor originally tested at 12.7 T for MFTF can be used with He II in

fields of 14 T due to the low temperature enhancement of J_c . Thus we can use a conductor for the insert pusher subcoils similar to the $Nb_3Sn:Ti$ one fabricated by Furukawa for the MFTF high-field choke coils. However, in this application the extra copper stabilizer should be annealed for high conductivity to limit the wetted perimeter heat fluxes to 1.0 W/cm^2 . This implies that the large 335 MPa stresses externally must be reacted to prevent damage to the strain-sensitive $Nb_3Sn:Ti$. Since the radial pack thickness is only 12.5 cm, the coil design permits transmitting the conductor stresses to a 2-cm-thick stainless-steel outer case surrounding the $Nb_3Sn:Ti$ subcoils. The coil consists of twenty subcoils each clad in 1-cm-thick coil cases in order to increase the effective coil modulus and transmit the radially compressive forces from the TF coils to the additional supporting structure located at the magnet bore. The fringe fields from this coil supply an approximate 0.5 T on the plasma "bean-contour."

The TIBER TF coils, whose parameters are summarized in Table 1-2, are made of internally cooled Nb_3Sn conductors similar to those used in the Westinghouse LCP coil and the MIT test coil. The tensile load on the straight leg of the TF coil has been calculated. Since the conductor loads are transmitted first to the sheath and then to the case, the sheath material shares part of the load. This sheath stress is found to be 266 MPa (39 ksi).

The centering force of the TF coils causes a compressive stress, which is felt by both the pusher coil and the outermost layer of the TF winding.

Table 1-2. TIBER TF-coil pack parameters.

Parameter	Value
Coil cross section (straight leg)	0.143 m^2
Winding pack cross section	0.0903 m^2
Number of turns	192
Pack current density	45 A mm^{-2}
Effective area	21.67 mm^2
% steel	25
% insulator	17
% conductor	35
% helium	23

Nuclear heating of 43 kW in the TF coils must be removed by flowing the helium through the conductors. Because of the competing effects of heat removal and heat generation by friction, there is a minimum temperature increase in the flow path as flow rate is increased. By pancake-winding the coils, two in hand, and by injecting flow on the inner layer, there are 36 flow paths per coil. With an inner-conductor nuclear-heating rate of 8.1 W/cm^3 , a flow rate of 6 kg/s is required with inlet conditions of 6 atm and 4.5 K, and outlet conditions of 2 atm and 6.0 K. These parameters represent the maximum refrigeration requirements since they result from an attempt to obtain the minimum helium temperature rise. If a greater

temperature rise can be tolerated, less refrigeration power will be required.

In steady state the helium liquefier necessary to support the cryogenic heat load is 50 kW. This is about three times the system already constructed for MFTF. An additional 26,000-litre Dewar and heat exchanger would be necessary to precool the helium before entering the coils. After exiting the coils, the helium would expand through a Joule-Thomson valve to refill the storage Dewar. Using actual costs from the MFTF system, the cost of the helium liquefier and transfer system for TIBER is estimated to be \$18.6 million and to be well within the state of the art.

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2.0. PLASMA MODEL FOR REFERENCE-CASE TIBER OPERATION (B. G. Logan)

2.1. INTRODUCTION

The primary objective of TIBER is to design the most compact and inexpensive superconducting tokamak for ignition experiments, rather than a machine to explore highest possible beta regimes. Our physics choices and assumptions for TIBER were guided by that primary objective. For example, high beta is sought through strong plasma cross-section shaping at moderately low aspect ratio; higher beta second-stability regimes requiring larger aspect ratios were not assumed. Confinement assumptions are neoalcalator for electrons and neoclassical for ions (somewhat optimistic), but both degraded by factors of two. The quantitative scaling laws for beta and confinement were given by Sheffield and Uckan,¹ who provided physics support for the Mission-II work; their recommendations were quite consistent with the objectives of this study.

Perhaps the most ambitious, or least conservative, feature of TIBER is that the current is taken to be provided entirely by rf drive, with no provision for supplementary inductive current drive. Although additional inductive drive could have been designed into TIBER with some increase in the major radius (and might arguably be a worthwhile insurance policy), we chose instead to first seek in TIBER the extreme minimum in major radius that could be engineered in a superconducting ignition machine because that kind of design had never been explored before. However, since rf current

drive heats the plasma more than does inductive current drive, the rf would have to be turned off for short periods (for a few alpha relaxation times) to study ignition under conditions where alpha heating could totally dominate. The added benefit of rf current-drive capability would then be the ability to study long term plasma-wall interactions in TIBER, in addition to ignition for short periods. In this sense TIBER could provide more reactor-relevant physics data than the short-pulse Mission-I devices currently being studied.

2.2. TIBER OPERATING SCENARIO

As illustrated in Fig. 2-1, the TIBER operating scenario is chosen to provide experiments with both pure ignition for short pulses as in current Mission-I designs, and to explore long-pulse physics as was envisioned in the previous Tokamak Fusion Core Experiment (TFCX). As in TFCX, a total of 40 MW of current drive is initially used to ramp up the current to 10 MA at low density (few 10^{13} cm^{-3}), at which point the pellet fueling rate is increased to raise the density (and n_T with density) to reach ignition at 10 keV. Because the current is established largely before the density is raised, the 40 MW of rf only dissipates and heats plasma instead of driving current near the end of the ramp-up phase ($t=150 \text{ s}$ in Fig. 2-1). The 40 MW of rf are assumed to be composed of an appropriate mix of fast and slow lower-hybrid (LH) waves, but it might also consist of lower-frequency fast waves at $\omega = 1.5 \omega_D$, or alternatively of ECH. The appropriate mix of rf is left for future work, but further discussion relating to these aspects of current drive is given in Sec. 11.0 by Joel Schultz.

Figure 2-1. TIBER operating scenario for study of both short-pulse ignition and plasma-wall interactions in steady-state burning DT plasma.

2.2.1. The Ignition Mode

The ignition phase illustrated in Fig. 2.1 lasts until about 50 seconds after rf turn off, during which the plasma current decays about 10%. The plasma current would decay during this high density period even if the rf were left on, since current-drive efficiency would be submarginal at the higher density. The current could be maintained inductively during this phase if one added ohmic heating coils (at the expense of increasing the inner bore of TIBER), but the classical L/R time (500 seconds) is long enough to allow ignition for long periods compared with alpha relaxation

times, as required in Mission-I designs, without any inductive drive. The long L/R time would not be possible with sawteething, but since the current profile established during the ramp up will likely be broad (even slightly hollow) with current drive, sawteething should not occur. Also, the broad current profile should allow densities to climb temporarily above the Murakami limit, using pellet injection.

The purpose of the ignition phase is to study the effects of pure alpha heating on profiles and confinement, without the presence of any auxiliary heating that the rf current drive would unavoidably introduce. This is an important issue, not just because ignition per se is part of the mission, but because the deterioration of energy confinement time τ_E with auxiliary power P_{aux} seen in the Tokamak Fusion Test Reactor (TFTR) and other tokamaks may be the result of heating profiles and anisotropy induced in the particle distributions by the auxiliary heating. Hopefully, isotropic alpha heating would be more benign to confinement than auxiliary heating. To ensure sufficient confinement for ignition, the ramp up in current and density is chosen to provide an ignition margin $M = 1.5$ as in TFCX (but using the neoalcatraz x 0.5 scaling for Mission II).

2.2.2. The Steady-State Current-Drive Mode

The steady-state current-drive regime depicted in Fig. 2-1 follows the ignition period ($t > 300$ s) and is reached by turning rf back on and decreasing the fuel injection rate, allowing the density to drop to the point where the plasma dielectric constraint ($\omega_{pe}^2/\omega_{ce}^2 \approx 0.36$) permits access for lower-hybrid fast and slow waves at parallel index of refraction

$n_{||} \sim 1.5$ to 2. The presumption is that an appropriate combination of fast and slow waves, where the fast waves drive current mostly in the plasma center and slow waves drive current mostly on the outboard plasma edge, can produce the current density and safety factor (q) profile calculated for the TIBER equilibria (see Fig. 4-3 in Sec. 4.0 by Bulmer).

As the density drops, the rf current-drive power P_{CD} is ramped up to sustain the plasma current and also raise the plasma burn temperature. The plasma current that must be sustained in the steady-state current-drive mode is lower than in the ignition mode because the additional power input due to current drive allows power balance to be achieved at reduced confinement, and this requires reduced current. The minimum effective ignition margin appropriate for the steady state with current drive is $M_{eff} =$

$[1 + (0.2 Q_{CD})^{-1}]^{-1} \sim 0.67$ for $Q_{CD} = P_{fusion}/P_{CD} = 10$. The desired plasma temperature is higher to increase the current-drive efficiency and Q_{CD} , and also to permit modest $\omega_{pe}^2/\omega_{ce}^2$ for accessibility at moderate betas. High electron temperatures would also be beneficial for efficient fast-wave coupling at ICRF frequencies, if such were desired for the current drive. (See Section 11.0 by Joel Schultz.)

The calculated current-drive power to sustain the steady state ($P_{CD} \approx 20$ MW) is calculated to be less than the 40 MW of rf provided for combined current ramp up and heating to ignition. However, the simple model described in the next section used to estimate the steady-state current-drive power in the reference TIBER case neglects many nonideal effects that might lower the current-drive efficiency by perhaps a factor of 2. Since all the rf power is assumed to be capable of cw operation, and could be used even in the steady state as well as the ramp-up period, a TIBER Q_{CD} as low as 5 could still operate in steady state.

Perhaps the most serious potential problem affecting the TIBER current-drive efficiency is ion cyclotron damping of LH waves by larger Larmor-radius alpha particles, a problem pointed out by Rip Perkins during the course of this study. The proposed fix by Perkins (and Schultz) is to use fast waves at $\omega \approx 1.5 \omega_D$, together with a larger aspect ratio $R/a \approx 5$, so that all cyclotron resonances lie outside the plasma. This solution would require increasing the TIBER major radius to at least 4 meters, and would be the basis for future iterations of the TIBER design (see Sec. 11.0 by Joel Schultz). The present TIBER design at $R/a = 3.6$ could achieve ignition in any case, since few alphas are produced in the current ramp-up phase. If the alpha-particle damping were as strong as linear theory predicts, the TIBER steady state regime depicted in Fig. 2-1 might have to be run on nearly pure deuterium (after the DT ignition, by switching DT pellets to mostly D pellets). This would require all 40 MW of available rf at 20% lower density and beta to make up for lack of alpha heating. Some tritium burn and alpha particle production in steady state would always be possible, since the alpha particle damping is proportional to both alpha density $n_\alpha \sim P_{\text{fusion}}$ and to the alpha distribution slope $\partial f_\alpha / \partial v^2$. Because the rf power damped by alphas could be acceptable even at levels comparable to the fusion alpha slowing-down (heating) power, $\partial f_\alpha / \partial v^2$ would clearly be reduced (quasilinear flattening), reducing the alpha damping rate. Calculation of this nonlinear effect on alpha damping is outside the scope of this study, however. Another proposed fix to avoid alpha damping (suggested by Bob Conn) is to use ECH to drive the current. Again, however, lower $Q_{CD} \approx 5$ would be obtained with ECH.

The purpose of the steady-state current-drive mode in TIBER is to investigate long-pulse burning plasma physics (at least at some significant

percentage of alpha production and ash removal) and long term plasma-wall interaction and impurity control. Depending on the total integrated machine-operating time set by availability, a total neutron fluence of 1 to 2 MW·yr/m² may be accumulated, permitting some useful blanket testing. All of the TIBER subsystem components are designed to have a minimum life that is long enough to permit such nuclear-testing capability.

2.3. THE PLASMA MODEL AND REFERENCE-CASE TIBER PARAMETERS

2.3.1. Power Balance

We present here a simple model to quickly determine basic plasma parameters for the TIBER reference-case design. We start with an expression for power balance, including the current drive power P_{CD} :

$$M P_{\text{transport}} = 0.2 P_{\text{fusion}} f_{\alpha} + P_{CD} \quad (1)$$

where

$$P_{\text{transport}} = \left\{ \frac{n(3/2 T_i)}{\tau_{Ei}} + \frac{n(3/2 T_e)}{\tau_{Ee}} \right\} \text{Vol} , \quad (2)$$

$$\tau_{Ei} = \frac{a^2 \kappa}{4\chi_i} , \quad (3)$$

$$\tau_{Ee} = \frac{a^2 \kappa}{4\chi_e} , \quad (4)$$

$$\text{Vol} = 2\pi^2 a^2 R \kappa \quad (5)$$

$$\kappa = b/a , \quad (6)$$

f_α is the fraction of alpha power available to supply transport losses (alpha power minus radiation losses) and M is the ignition margin. Equation (1) can be rewritten as

$$M_{\text{eff}} P_{\text{transport}} = 0.2 P_{\text{fusion}} f_\alpha , \quad (7)$$

where the effective ignition margin with current drive is given by

$$M_{\text{eff}} = \frac{M}{1 + \frac{P_{\text{CD}}}{0.2 P_{\text{fusion}} f_\alpha}} = M [1 + (0.2 Q_{\text{CD}} f_\alpha)^{-1}]^{-1} . \quad (8)$$

In the limit $P_{\text{CD}} \rightarrow 0$, $Q_{\text{CD}} = P_{\text{fusion}}/P_{\text{CD}} \rightarrow \infty$, $M_{\text{eff}} \rightarrow M$, and one obtains the usual definition of M with ignition.

As suggested by Sheffield and Uekan, the ion heat conductivity χ_i is taken to be neoclassical

$$\chi_i = 4.1 \times 10^{-2} f_i \left(\frac{R}{a} \right)^{3/2} \frac{\langle q \rangle^2 n_{20}}{B_T^2 T_{10}^{1/2}} , \quad (9)$$

where f_i is a chosen anomaly factor, and

$$\bar{a} = a [2\kappa^2/(1 + \kappa^2)]^{1/2} \approx a \sqrt{\kappa} . \quad (10)$$

The density n_{20} is in units of 10^{20} m^{-3} , the toroidal field B_T is in tesla, and the ion temperature T_{i0} is in units of 10 keV. The electron heat conductivity is taken to be neoalculator

$$\chi_e = 1.5 f_e a \sqrt{\kappa} / (R^2 n_{20}) \quad , \quad (11)$$

where f_e is a given anomaly factor to account for degradations due to high beta and ($\alpha + \text{rf}$) heating. We will take $f_i = 2$, $f_e = 2$, and $f_\alpha = 0.8$ as given example parameters for TIBER. Pressure balance requires

$$\beta_T = 0.4 \frac{n_{20} T_{i0}}{B_T^2} \left(1 + \frac{T_e}{T_i} \right) \quad , \quad (12)$$

and the maximum β_T for MHD stability is taken to be a Troyon scaling

$$\beta_T = 0.04 \frac{I_p (\text{MA})}{a B_T} \quad , \quad (13)$$

with a slightly optimistic coefficient to take some credit for the unusual indentation and high triangularity in the TIBER plasma shape. The fusion power is given by

$$P_{\text{fusion}} = \frac{1}{4} n^2 \langle \sigma v \rangle_{DT} \left(17.6 \frac{\text{MeV}}{\text{fusion}} \right) \text{Vol} \quad . \quad (14)$$

A convenient expression for $\langle \sigma v \rangle_{DT}$ in the range $T_i = 10$ to 20 keV is

$$\langle \sigma v \rangle_{DT} = 1.09 \times 10^{-22} T_{i0}^2 (\text{m}^3/\text{s}) \quad . \quad (15)$$

Equation (15) overpredicts $\langle \sigma v \rangle_{DT}$ at ion temperatures above 20 keV, and one needs to multiply Eq. (14) by a correction $[\sigma v]_{\text{corrected}} \equiv \langle \sigma v \rangle_{\text{actual}} / (1.09 \times 10^{-22} T_{10}^2)$ when applicable (e.g., $[\sigma v]_{\text{corrected}} = 0.68$ when $T_{10} = 3$). Thus,

$$P_{\text{fusion}} = 15.2 (n_{20} T_{10})^2 a^2 R \kappa \cdot [\sigma v]_{\text{corrected}} (\text{MW}) . \quad (16)$$

Combining Eqs. (1) through (16) gives the required plasma current I_p (in MA) for power balance:

$$I_p^2 \geq \frac{938 M_{\text{eff}} a T_{10} \left(\frac{\chi_i}{\chi_e} + \frac{T_e}{T_i} \right) \left(1 + \frac{T_e}{T_i} \right)^2 f_e}{R^2 B_T^2 \sqrt{\kappa} f_a [\sigma v]_{\text{corrected}}} . \quad (17)$$

At high temperatures $T_{10} \gg 1$, electron heat-conduction losses dominate in the steady-state current-drive mode, but at $T_{10} = 1$ in the high M_{eff} ignition mode, χ_i is comparable to χ_e in TIBER due to small a . Results of Eq. (17) are presented in Table 2-1 for both ignition and steady-state operation in TIBER. As expected, less current is required for steady state (ie., less confinement is needed).

2.3.2. Current-Drive Power Requirements

Figure 2-2 shows current-drive efficiency calculated by Karney and Fisch (see page 28, Fig. 3 of Ref. 2) as a function of parallel index $n_{\parallel}$ and finite electron temperature $\theta = T_e(\text{keV})/511$. Note that relativistic effects at high $\theta = 0.05$ appropriate for TIBER steady-state current-drive regimes

Table 2-1. TIBER reference-case physics parameters.

Fixed parameters

Major radius $R = 2.50$ m	Toroidal field $B_T = 5$ T
Minor radius $a = 0.73$ m	Fractional radiation losses $(1 - f_\alpha) = 0.2$
Aspect ratio $A = 3.6$	Ion confinement: Neoclassical/2
Elongation 1.94	Electron confinement: Neoalculator/2
Triangularity 0.60	Beta: $0.04 I_p / (a B_T)$
Indentation 0.05	

<u>Variable parameters</u>	<u>Pulsed ignition mode</u>	<u>Steady-state current-drive mode</u>
Plasma current I_p (MA)	9.7	7.4
Avg. toroidal beta $\beta_T(\%)$	10.6	8.1
Ignition margin M	1.5	0.60
Ion/electron transport χ_i/χ_e	1.1	0.1
Ion temperature T_i (keV)	10	30
Electron temperature T_e (keV)	10	24
Avg. edge safety factor q_e	2.2	3.2
Neutron wall load Γ_n (peak) (MW/m^2)	4.0 (1.6 avg.)	2.0 (0.8 avg.)
Fusion power P_{fusion} (MW)	440	222
Avg. density n_e (10^{20} m^{-3})	3.3	0.94
Current-drive power P_{CD} (MW)	0	22.4

Figure 2-2. Current-drive efficiency as a function of parallel index $n_{||}$ and electron temperature (scaled to TIBER parameters).

increase the current-drive efficiency by almost a factor of two in the range $1.5 < n_{||} < 2$, and mitigate the sensitivity of current-drive-efficiency reduction with upshifts in $n_{||}$ that may occur in LH current drive. Note in Fig. 2-2 that the left-hand scale was adjusted from that in Ref. 2 for $R = 1$ meter and $n_{20} = 1$, to the appropriate Rn_{20} for TIBER.

Besides wave accessibility, an important consideration in selecting the minimum $n_{||}$ (minimum $n_{||} \rightarrow$ maximum current-drive efficiency) is the condition for having a sufficient number of tail electrons in a near-Maxwellian distribution at the associated resonant phase velocity, and resonant energy E_{res} , to carry the required current:

$$\frac{1}{4} n_e \bar{v}_e \exp(-E_{\text{res}}/T_e) \gg j_{\text{required}}/q - I_p/(\pi a^2 \kappa) \quad (18)$$

If we require the left-hand side of Eq. (18) to be 5 times the right-hand side, then for the TIBER steady-state parameters given in Table 2-1

($n_e = 9.4 \times 10^{19} \text{ m}^{-3}$, $T_e = 24 \text{ keV}$, $\bar{v}_e = 1.0 \times 10^8 \text{ m/sec}$, and $I_p = 7.4 \text{ MA}$), we must have $\exp(-E_{\text{res}}/T_e) > 3 \times 10^{-2}$, or $E_{\text{res}} < 3.5 T_e = 84 \text{ keV}$. Now using

$$n_H^2 = \frac{m_e c^2}{2 E_{\text{res}}} \quad (19)$$

we find $n_H > 1.74$. At this value of n_H , and $\theta = 0.05$ ($T_e = 24 \text{ keV}$), the current-drive efficiency from Fig. 2-2 is about 0.37 A/W, but because this efficiency would vary less than $\pm 10\%$ over the likely range for TIBER $1.5 < n_H < 2$, at least 0.33 A/W at $n_H = 2$ would still give $Q_{\text{CD}} = 10$ in TIBER. For these values of n_H , the variation of current drive power P_{CD} and $Q_{\text{CD}} = P_{\text{fusion}}/P_{\text{CD}}$ with temperature (assuming $T_e/T_i = 0.8$) would be given approximately by

$$P_{\text{CD}} = 2.78 \frac{I_p (\text{MA}) R(m) n_{20}}{\left[1 + \left(\frac{T_{10}}{2.5} \right)^{1.16} \right]} (\text{MW}) \quad (20)$$

and

$$Q_{CD} = 0.545 \frac{T_{10} \left[1 + \left(\frac{T_{10}}{2.5} \right)^{1.16} \right]}{(1 + T_e/T_i)} a B_t \sqrt{\kappa} \cdot [\sigma v_{corrected}] , \quad (21)$$

where we have used pressure balance and the Troyon beta limit to relate density to temperature and field. Equation (21) shows that Q_{CD} increases strongly with temperature. It is for this reason, as well as to reduce the dielectric constant at moderate beta, that the steady-state current-drive mode of TIBER requires burn at higher temperatures ($T_i = 30$ keV) than those that would maximize ignition margin at a given field and beta ($T_i = 10$ keV). The factor $[1 + (T_{10}/2.5)^{1.16}]$, obtained by fitting the θ dependence in Fig. 2-2, enhances the benefit of high temperature even more than was seen in previous current-drive-efficiency scalings. Improved efficiency with temperature results from favorable relativistic effects (increasing θ) in the recent Karney and Fisch calculations. The assumed confinement scaling in Eq. (17) indicates only a weak penalty in current for higher temperature $I_p \sim \sqrt{T_{10}}$. However, we cannot now accurately predict the actual temperature dependence of χ_e expected in future ignition experiments.

2.4. LASER-DRIVEN PELLETT INJECTION FOR TIBER

Deep pellet fueling would be desirable for TIBER to minimize plasma-wall interactions such as sputtering, to reduce DT gas recycling near the

wall, and to mitigate tritium flows in the vacuum system. In addition it may be possible to increase density and $n\tau$ beyond normal Murakami limits (Alcator-pellet results) if the pellets penetrate halfway or more to the axis. Also, the net outward particle transport flux due to the deep fueling may generate appreciable bootstrap current, further raising Q_{CD} . To obtain the deep fueling, however, requires pellet velocities much greater than the 1 km/s velocities possible with pneumatic drives, especially when the effects of fast alpha heating of the pellet are taken into proper account. Lasers and rail guns have been previously proposed to obtain higher pellet velocities, but the poor mechanical strength of DT ice limits the maximum acceleration "g" forces unless one allows the pellet to disintegrate (even vaporize and ionize) and expand at sound speed transverse to the direction of pellet acceleration. Here we will use a laser-ablation-driven pellet-plasma injection scheme first invented by Logan for use in fueling the Fusion-Power-Demonstration (FPD) and MINIMARS tandem mirror reactors. The following model describes the penetration of the resulting high beta pellet plasma due to its dynamic pressure $1/2\rho v^2 \gg B_T^2/2\mu_0$.

Figure 2-3 is a schematic layout of a laser-fuel-injection system for TIBER. A conventional DT ice pellet pneumatic injector fires pellets at the rate of 10 per particle confinement time (3 Hz); each pellet is sufficiently large to deposit 10% of the plasma inventory. Just as the ice pellet arrives at the separatrix between the confined plasma and the scrape-off layer traveling at a speed of 1 km/s, a laser beam is fired following up behind the pellet as shown in Fig. 2-3, and hitting the backside of the ice

Figure 2-3. Laser-formed pellet plasma expands and penetrates inertially until dynamic pressure balance with the field is reached.

pellet. At laser intensities of $5 \times 10^{12} \text{ W/cm}^2$ (10 to 100 times lower intensity than those used in inertial fusion experiments today, but 10 to 100 times higher than the intensity that would otherwise be limited by the strength of the DT ice), the laser prepulse is sufficient to shock-heat the ice pellet to 5 eV preheat temperature. Then the main laser pulse, interacting at the critical plasma-density layer, generates several megabars of ablation pressure, accelerating the pellet to velocities on the

order of 50 km/s in a pulse length of order $r_0/\sqrt{T_0/m_1} \approx 70$ ns, where r_0 is the characteristic "payload" pellet plasma radius at the preheat temperature T_0 , and at a pellet plasma density roughly equal to the equivalent solid density $n_0 \approx 6 \times 10^{22} \text{ cm}^{-3}$. The payload plasma radius r_0 is somewhat smaller than the initial DT ice pellet radius due to the mass lost in ablation (typically 25 to 50% less mass in the payload), and the laser pulse length is limited to the time it takes for the critical-plasma density layer to "burn through" the pellet plasma, expanding spherically at the preheat sound speed $\sqrt{T_0/m_1}$. Since the center of mass of the pellet plasma is accelerated forward to a velocity $v_F > \sqrt{T_0/m_1}$, the pellet plasma sweeps forward as an expanding plume with an initial 30-to-35-degree half-cone angle. The pellet plasma initial pressure $n_0 T_0$ and forward dynamic pressure $1/2(n_0 m_1) v_F^2$ both exceed the local magnetic field pressure $B_T^2/2\mu_0$ by many orders of magnitude, so the pellet plasma trajectory is unaffected by the magnetic field initially. Thus, the penetration mechanism of this pellet plasma scheme is entirely mechanical and not limited at all by either the strength of DT ice or the ice ablation rates of conventional pellet schemes.

Figure 2-4 shows the expected trajectory of the pellet plasma jet injecting into the TIBER plasma cross section. The pellet plasma mass expands spherically (3D) as it travels forward, until zero-order pressure balance is reached with the main field ($\beta_T = 1$):

$$n_1 T_1 = B_T^2/2\mu_0, \quad (22)$$

Figure 2-4. Laser-driven fuel injection for deep penetration fueling in TIBER.

with

$$n_1 = M_p / \left(\frac{4}{3} \pi r_1^3 m_{DT} \right), \quad (23)$$

where n_1 , T_1 , r_1 are the $\beta_T = 1$ plasma density, temperature, and radius, M_p is the pellet payload mass ($\sim 1/2$ the initial DT ice mass), and $m_{DT} = 2.5$ amu is the average DT ion mass. The temperature T_1 depends not so much on the initial preheat temperature T_0 as it does on the hot-alpha-particle energy density $1.5 \beta_\alpha (B_T^2 / 2\mu_0)$. The parameter β_α is the component

of β_T due to the hot alpha pressure. These hot alphas can freely penetrate the $\beta_T = 1$ plasma bubble due to their large Larmor radius $\rho_\alpha > r_1$. The fast alphas heat the pellet plasma mainly by electron drag at a density-independent rate τ_{drag}^{-1} according to the following equation:

$$\frac{dT}{dt} \approx \frac{\beta_\alpha (B_T^2 / 2\mu_0)}{2n_e \tau_{\text{drag}}} = 1300 \frac{\beta_\alpha B_T^2}{T^{3/2}} \quad (24)$$

The pellet plasma temperature as a function of time is thus given by

$$T(t) \approx 25 (\beta_\alpha B_T^2 t)^{2/5} \text{ (keV)} \quad (25)$$

Using this relationship, plus Eq. (23), and

$$r_1(\tau_1) = \int_0^{\tau_1} \sqrt{\frac{T(t)}{m_{DT}}} dt \quad (26)$$

yields

$$r_1 = 33 M_p^{0.375} \beta_\alpha^{0.125} B_T^{0.5} \quad (27)$$

$$\tau_1 = 1.84 \times 10^{-4} M_p^{0.3125} / (\beta_\alpha^{0.0625} B_T^{0.75}) \quad (28)$$

and

$$T_1 = 0.8 \beta_\alpha^{0.375} B_T^{0.5} M_p^{0.125} , \quad (29)$$

where r_1 is in meters, τ_1 in seconds, T_1 in keV, B_T in tesla, and M_p = pellet payload mass in kg. Typically, $\beta_\alpha \approx 0.15$, $B_T \approx 0.015$ for TIBER, $B_T = 5$ T, and $M = 2 \times 10^{-6}$ kg, so the 3-D expansion phase takes $\tau_1 \approx 1.2 \mu s$ to reach $\beta_T = 1$ at a radius $r_1 \approx 6.4$ cm and at a temperature $T_1 = 0.072$ keV.

After reaching $\beta_T = 1$, the pellet plasma bubble stops expanding in the direction transverse to the magnetic field, while continuing to expand along the magnetic field (1-D expansion shown in the bottom frame of Fig. 2-4). The forward (radially inward) travel of the $\beta_T = 1$ pellet-plasma volume slows down due to the net outward force on that volume resulting from the gradient in toroidal-field pressure:

$$\frac{d}{dt} \left(\frac{1}{2} M_p v_F^2 \right) = - \frac{B_T^2}{2\mu_0} \left(4 \frac{r_1}{R} \right) r_1 \xi(t) v_F(t) . \quad (30)$$

Here

$$\xi(t) = \int_0^t \sqrt{\frac{T}{m_{DT}}} dt . \quad (31)$$

The range of the pellet plasma penetration is given by

$$\Delta r = V_{F0}(\tau_1 + \tau_2) - 2.5 \times 10^{11} \frac{r_1^2}{MR} B_a^{0.2} B_T^{2.4} \tau_2^{3.2} \quad (32)$$

where $V_{F0} = V_F$ just after the laser pulse, and where the time τ_2 for the toroidal field gradient to slow down the pellet plasma ($V_F \rightarrow 0$) is given by

$$\tau_2 = \frac{3.9 \times 10^{-6}}{0.09 B_T^{1.09}} \left(\frac{MR V_{F0}}{r_1^2} \right)^{0.455} \quad (33)$$

Typical parameters for TIBER using this model are given in Table 2-2. The assumption here is that once the pellet plasma comes to a stop ($\tau_2 = 6.4 \mu s$), dissipation, such as diffusion of magnetic field and plasma current into the $\beta = 1$ pellet-plasma volume, ends up spreading the payload plasma inventory over the toroidal plasma volume. Such subsequent diffusion processes are complicated and need to be studied in future experiments.

The choice of laser type to provide the requirements shown in Table 2-2 is also the subject of future design work and laser technology development. However, following the suggestion of George Gorker [Grumman, Fusion Engineering Design Center], who has recently surveyed various candidate laser types under development for the Inertial Confinement Fusion (ICF) and Strategic Defense Initiative (SDI) programs (CO_2 , Nd, and KrF), we are assuming that the KrF lasers best satisfy the requirements in Table 2-2.

Table 2-2. Laser-driven pellet-injection parameters for TIBER.

Parameter	Value
Pellet mass before laser pulse (DT ice)	4 mg
Pellet diameter before laser pulse (DT ice)	3.2 mm
Laser pulse duration	70 ns
Laser light energy (0.25- μ KrF light)	16 kJ
Laser light intensity on the pellet	5×10^{12} W/cm ²
Pellet mass just after laser pulse (5 eV DT plasma) $t = 70$ ns	2 mg
Pellet plasma radius just after the laser pulse (5 eV DT plasma)	2.5 mm
Pellet plasma forward velocity just after laser pulse	50 km/s
Pellet plasma energy	4 kJ
Laser/pellet repetition rate	3 Hz
Laser system efficiency (light/electrical)	5%
Average laser electrical power consumption	1 MW _e

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3.0. THE EDGE PLASMA AND THE PUMPED LIMITER (W. L. Barr)

3.1. INTRODUCTION

Previous studies have analyzed various schemes for controlling impurities and the edge plasma. Methods that have been reported include several types of divertors, pumped limiters, and scoop limiters.^{1,2,3,4} Reference 1 contains a brief review of the evolution of poloidal divertors, and points out that the method of impurity control has shifted from massive pumping systems to high recycling divertors. It appears that a poloidal divertor would be the logical choice for TIBER, just as it was found to be for INTOR.¹ However, two of the primary objectives of TIBER are that it be small and that the cost be a minimum. Both of these objectives are better met with a limiter than with any type of divertor provided that the limiter can produce conditions in the edge plasma that are compatible with good, high-beta confinement in the interior plasma. For that reason we are studying a new type of pumped limiter that is very compact and that appears to provide the conditions needed for TIBER to meet its goals.

3.2. DESIGN OBJECTIVES OF THE PUMPED LIMITER

The goal of our limiter design is to provide conditions at the outer boundary of the core plasma similar to those that have been shown to result in good energy confinement ("H-mode") in several operating tokamaks.^{5,6,7} The edge plasma conditions required for that mode of operation are not completely understood, but it appears that flat profiles

are required in the interior with a steep temperature gradient into the edge plasma. Also, while a good vacuum external to the plasma is not clearly necessary, it is known that gas puffing can destroy the H-mode. Gas influx into the interior plasma in TIBER is nearly eliminated by surrounding it with a shielding edge plasma that is impervious to gas and impurities. This creates the effect of a good vacuum. Any edge plasma that is impervious to gas is necessarily an excellent vacuum pump.⁸ Its pumping speed exceeds that of a cryopump of the same area, and it does not have the severe restrictions on heat and radiation that would prevent the use of a cryopump in the plasma edge of a tokamak.

The maintenance of such a halo plasma requires fueling to make up for losses at the ends, and power to keep the electron temperature high enough to give a high rate of ionization for gas and impurity atoms. We fuel the edge plasma by recycling it at the ends. Power is available from the escaping hot ions, but it is only transferred to the edge plasma electrons if their density is high enough and their temperature T_e is low enough for drag to be important. The lifetime of the fast ions is short once they are on field lines that are intercepted by the limiter. A high edge plasma density is therefore required for both the absorption of power and for shielding, but there is an optimum electron temperature that is high enough for ionization but low enough for drag. It appears that we can maintain the needed density and temperature by controlling the recycling.

The analytical study by D.E.Post et al.⁹ of particle continuity and transport in a divertor plasma with high recycling shows the general existence of two stable solutions, provided that the heat flux is

sufficiently large with respect to the particle flux. One solution has high density and low temperature, and the other has low density and high temperature. It is observed experimentally that the high density mode can be switched on by a gas puff.⁹ We assume here that TIBER can select the high-density, low-temperature mode for its edge plasma operation.

3.3. CONFIGURATION OF THE PUMPED LIMITER

Most previous pumped limiter designs have assumed such shapes as a flat blade,^{10,11} a mushroom,¹² or a scoop.⁴ These shapes require a leading edge that is difficult to cool, and, as an additional problem, must compromise the vacuum pumping in order to provide a shape that distributes the heat load.

Figure 3-1 shows one of the two vented-port types of poloidal, pumped limiters in TIBER. A similar one is located at the top of the torus to share the heat load. The idea here is to choose a shape for the neutralizer plate that best distributes the heat load without regard for the vacuum pumping ducts. Small pumping ports are then distributed on the surface of the plate to remove gas to the vacuum duct behind. By distributing the ducts we gain local control of the fraction of the gas being pumped and of the fraction being recycled. There is no leading edge other than the many small ones around each port, which are more easily cooled than an extended knife-edge would be. This concept is similar to the recycling halo dump used in MARS (Mirror Advanced Reactor Study).¹³ There, the ions strike the plate at near normal incidence and a small fraction of them passes through

Figure 3-1. The vented-port type of pumped limiter in TIBER. Gas is removed at 10 millitorr through the many small ports to the slot-shaped pumping duct. The average heat load is 1.25 MW/m^2 and the peak is about 2 MW/m^2 .

to the pumping duct, while most of the remainder are recycled. In TIBER, the edge plasma flows along the magnetic field lines that intersect the plates at near grazing incidence. (See Fig. 3-1.) All of the ions are neutralized on the plasma side of the neutralizer plates, and a fraction of the resulting gas is pumped through the ports, while the remainder is recycled into the edge plasma.

3.4. THE COMPUTATIONAL MODEL

The model we use to calculate the profiles of temperature and density in the edge plasma allows each part to be calculated in one dimension. A self-consistent description of the edge plasma is then obtained by varying parameters in the calculation until the solutions and derivatives are continuous.

We imagine the spiraling flux tube containing the edge plasma to be deformed into a straight cylindrical annulus that intersects a limiter in the outer shadow zone at each end. Because of the symmetry we need consider only half of the length of the flux tube that connects the two limiters. The scale length is therefore $L = 2\pi Rq/4$. This gives $L = 15.5$ m, since $R = 2.6$ m and the safety factor at the limiter radius is $q = 3.8$ in steady state. Figure 3-2 shows schematically the three radial zones and the limiter region, and indicates the coordinate axes X and Z with the Z axis parallel to the magnetic field direction. This model is similar to that of Waltz and Burrell,¹⁴ except that here the fluid flow is simpler because the flow is almost stagnant and the interaction with gas is confined almost entirely to the limiter region. The "shadow zone" contains those field lines that intersect the limiter. Most of the interaction with gas takes place in the "limiter region" of the shadow. Conditions in the "interior zone" are assumed here to be known. There is some interaction in the "boundary zone" with gas near the limiter, and it is here we expect to find the steepest gradients in temperature and density.

Figure 3-2. Schematic diagram showing the regions and zones used in calculating the edge plasma parameters. The shadow zone contains field lines that intersect the limiter plates. Open arrows indicate plasma, solid ones indicate gas.

Hot ions enter the boundary uniformly along its length, and diffuse on to the shadow zone. Electron drag on these fast ions in the shadow supplies the power to the electrons, which is then transported to the limiter region by electron thermal conduction and by convection. In this "stream region" of the shadow we assume there is little power loss to gas ionization, and we determine the electron temperature T_e from the power flow. This gives the Z dependence of T_e in the stream region, whereas the X dependence is obtained from the radial variation of fast ion power, which is assumed for this model to vary exponentially with X . In the limiter region, gas is formed as edge plasma ions and fast ions contact the plates and are neutralized. Because the mean free path for neutrals in the edge plasma is much shorter than the length of the limiter region, the attenuation of gas as it moves out from the limiter plate is essentially a one-dimensional problem.

3.5. POWER TRANSPORT IN THE STREAM REGION

The radial flux $\Gamma_{\perp}$ of fast ions into the shadow zone is taken to be uniform in Z, and to decay exponentially with distance $X_0 - X$ into the shadow:

$$\Gamma_{\perp}(X) = \Gamma_{\perp}(X_0) \exp\left(-\frac{X_0 - X}{\delta}\right),$$

where X_0 is the thickness of the shadow (See Fig. 3-2) and δ is the decay length. The thickness of the shadow is equal to twice the maximum gyroradius for the alpha particles. Because the magnetic field strength varies inversely with major radius R, the shadow thickness is proportional to R, as indicated in Fig. 3-1. The decay length is determined by radial diffusion in the shadow, and is rather small because the lifetime of fast ions in the shadow is short. Bohm diffusion then gives a decay length of about $\delta = 10$ mm for particles, and we assume that the decay length for energy is half of that.

Drag on these fast particles heats the warm electrons in the shadow and provides the power needed to maintain the edge plasma. This power input is proportional to the density of fast particles and has the same X and Z dependence as that density. The power input per unit length to the shadow electrons in a flux tube of thickness dX and located at X is

$$dP_L = \frac{\tau_{\parallel}}{\tau_d} \frac{P_{\text{chg}}}{L \delta} \exp\left(-\frac{X_0 - X}{\delta}\right) dX,$$

where $\tau_d = 4 \times 10^{13} T_e^{3/2}/n$ is the drag time, and $\tau = L/v_f$ is the transit time for the fast ions with thermal speed v_f . The power P_{chg} is the total power carried by the fast particles, and is equal to the sum of the alpha power, P_α , plus the current-drive power, P_{CD} :

$$P_{chg} = P_\alpha + P_{CD} \quad .$$

In TIBER, $P_\alpha = 39$ MW and $P_{CD} = 20$ MW, giving $P_{chg} = 59$ MW. The power input per unit length to the warm shadow plasma in dx at x is therefore

$$dP_L = \frac{p P_{chg} \exp\left(-\frac{x_0 - x}{\delta}\right)}{(2e)4 \times 10^{13} v_f \delta T_e^{5/2}} dx \quad . \quad (1)$$

Here, we have replaced the plasma density n by $n = p/(2eT_e)$, making use of the fact that pressure p is constant along a field line when both flow and parallel electric field can be neglected.

Power balance requires that in steady state the power put into the shadow along its length must equal the power lost at the ends. Whether this power is directly dumped on the limiter plates by the fast particles, or whether it first is partly transferred to the edge plasma and to gas in the limiter region, the longitudinal energy transport in the stream region (where sources and flow velocity are both small) remains essentially the same.

Power transport in the stream region is determined by the mean free path for electrons. If the electron scattering mean free path λ_e is much

smaller than the length L , then the power flow is by electron thermal conduction. The electron mean free path is approximately

$$\lambda_e(m) \approx 1 \times 10^{16} T_e^2(eV)/n(m^{-3}) .$$

For most of the edge plasma, $\lambda_e \ll L$, and transport is by classical conduction. In regions where λ_e is not much less than L , we use the semi-empirical result of Rognlien¹⁵:

$$Q_e = (Q_{\text{cnd}}^{-3/4} + Q_{\text{cnv}}^{-3/4})^{-4/3} . \quad (2)$$

Here

$$Q_{\text{cnd}} = -k \frac{dT_e}{dz} \quad (3)$$

is the classical electron thermal conduction where

$$k(W/m/eV) \approx 2100 T_e^{5/2} , \quad (4)$$

and

$$Q_{\text{cnv}} = \gamma n e v_e T_e \quad (5)$$

is the electron convective heat transport. Here v_e is the thermal speed of the electrons, and γ is the ratio of the energy loss per electron to the

electron temperature. Rognlien's result¹⁵ indicates that the energy transport is limited by the slower of either classical conduction or convection.

The energy flow equation for the stream region is dominated by the power input and the power flow terms because there is little interaction with gas, and because the flow velocity is much smaller than thermal speeds everywhere except very near the limiter plate. The power flow along the field direction then simplifies to

$$(2\pi adX) \frac{dQ_e}{dZ} = dP_L . \quad (6)$$

After anticipating that $\lambda_e \ll L$, and substituting from Eqs. 2-4, this becomes

$$- 2100 \frac{d}{dZ} \left(T_e^{5/2} \frac{dT_e}{dZ} \right) = pF T_e^{-5/2} , \quad (7)$$

where

$$F = \frac{P_{chg}}{8 \times 10^{13} \text{ ev cm}^2 \text{ s}} \exp \left(- \frac{X_0 - X}{\delta} \right) .$$

Equation 7 can be integrated analytically to give

$$Z = T_0^3 \left(\frac{1050}{pF} \right)^{1/2} h \left(\frac{T_e}{T_0} \right) ,$$

where

$$h(y) = \int_y^1 \left(\frac{y^5}{1-y} \right)^{1/2} dy ,$$

and T_0 is the value of T_e at $Z = L$. We have made use the fact that $Z = L$ is a symmetry plane with $\frac{dT_e}{dZ} = 0$ there. Because the value of $h(y)$ is insensitive to y when $y \ll 1$, we can determine T_0 by setting $T_e \sim 0$ and $h = h(0)$ at the limiter surface. This gives

$$T_0 \approx \left(\frac{16 L}{5\pi} \right)^{1/3} \left(\frac{pF}{1050} \right)^{1/6} .$$

The power flow in the stream region must join smoothly to the flow in the limiter region, and this provides a boundary condition at that interface.

3.6. POWER TRANSPORT IN THE LIMITER REGION

In the limiter region, the interaction with recycled gas is important, and the transport is described by the one-dimensional fluid equations:

$$\frac{d\Gamma}{dZ} = S , \quad (8)$$

$$\frac{d}{dZ} (p + M_1 v \Gamma) = 0 , \quad (9)$$

$$\frac{d}{dz} (Q_e + 5e\Gamma T_e) = pF T_e^{-5/2} - eES, \quad (10)$$

where S is the particle source strength

$$S = n n_0 \langle \sigma v \rangle.$$

Here $\langle \sigma v \rangle$ is the total rate of ionization, and n_0 is the neutral density, which is determined from a separate calculation discussed in the next section. The energy E is the total energy cost per new ion-electron pair:

$$E = \chi_i + \frac{3}{2} (T_i + T_e) + \gamma_r T_e,$$

where χ_i is the ionization energy and $\gamma_r T_e$ is the average energy lost by radiation per ionization. We solve the three fluid equations, joining the solution smoothly to that from the stream region. We use as dependent variables the electron temperature T_e , the particle flux Γ , and plasma pressure p . From these, we can determine the plasma density

$$n = p/(2eT_e),$$

and the flow velocity

$$v = \Gamma/n.$$

Equations 8-10 are integrated numerically, and the amount of recycling (i.e., n_0) is adjusted to obtain an electron density and

temperature in the stream region that will absorb a large fraction of the incident power from the fast ions. When the density is high enough to absorb much power from the fast ions, it is also high enough and warm enough to give a very high attenuation of any incident gas and impurities. The variation of n_0 with X is determined from a separate calculation of the penetration of gas into the plasma for assumed values of T_e and n . This gives an initial value for the source function S to start the calculation with. The gas calculation is discussed in the next section. Figure 3-3 shows a typical plot of the plasma density and temperature in the limiter region for the particular value, $X = X_0$. The boundary condition at the sheath near the limiter surface is discussed in the next section.

Figure 3-3. The variation of plasma temperature T and density n along the flow direction in the limiter region. The plasma interacts with gas over most of the length of the limiter region because of the grazing incidence. See Fig. 3-4. Here the neutral density $n_0 = 1.4 \times 10^{18}/m^3$, the electron density at the end of the flow region is $n_e = 10^{20}/m^3$, the electron temperature at the end of the stream region is $T_e = 20$ eV, and the distance from the limiter surface is $X = 0.1$ m.

3.7. POWER BALANCE AT THE LIMITER SURFACE

Of primary interest to the design of TIBER are the details of the power flow to the limiter and the resulting erosion rate, which depends on the energy and flux of the impacting ions. Because the "limiter region" is defined to be the region where gas is recycled, the length through this region varies with X , as seen in Fig. 3-4. The boundary condition at the sheath, where the plasma contacts the limiter surface, is taken to be that the energy flux in the direction of the magnetic field equals the total power absorbed per unit of cross-sectional area on that field line. The energy flux Q_{sh} across the sheath¹⁶ is

$$Q_{sh} = \frac{1}{2} n v_e T_e g .$$

Here, v_e is the thermal speed of the electrons at the sheath, and the factor g has the value $g = 0.12$ for a D^+ plasma if there is no secondary electron emission.¹⁶ Some authors prefer to write Q_{sh} in terms of the sound speed for the ions. In that case the factor g has a different value.

The total power absorbed Q_{abs} along the length of a field line per unit of cross-sectional area is approximately equal to the analytic value for the case where the stream region occupies the entire length L . From Eq. (7) this is seen to be

$$Q_{abs} = \{4200 \text{ pF} \times [T_0 - T_e(0)]\}^{1/2} .$$

Table 3-1. Parameters at the symmetry plane and the power density normal to the field lines at the sheath.

X(m)	p(Pa)	T ₀ (eV)	n ₀ (10 ²⁰ m ⁻³)	Q _{abs} (MW/m ²)
0.100	1.0 x 10 ⁴	65.3	4.9	288
0.093	3.8 x 10 ³	49.3	2.4	106
0.086	1.4 x 10 ³	37.0	1.2	38
0.072	1.7 x 10 ²	20.7	0.3	5
0.065	5.5 x 10 ¹	15.3	0.1	1.3

will ignore the differences in heat load that are due to the method of transport, we can now estimate the distribution of heat on the limiter.

3.8. HEAT LOAD ON THE LIMITER

We have assumed that the energy flux falls off exponentially with radial distance into the shadow region. With this assumption we can determine the approximate shape of the limiter surface that is required to distribute the heat load. The shape, relative to the straightened field lines in our model, is that of a mushroom surface, as in Ref. 12 where the energy flux was also assumed to vary exponentially. The actual shape, after mapping it back to the true field lines, is concave toward the plasma, as shown in Fig. 3-1.

Figure 3-4. Enlarged view of the limiter region of Fig. 3-2, showing the varying thickness of shadow plasma to shield the interior plasma against gas from the limiter.

Upon equating Q_{sh} to Q_{abs} we obtain an equation relating the plasma pressure p in the stream region to the available power (contained in F). As before, T_0 is the electron temperature at the midplane. Table 3-1 displays the values of plasma pressure in the stream region at several different radial distances into the stream region. Also shown are the corresponding values of T_0 and n at the symmetry plane and the values of $Q_{abs} = Q_{sh}$ at the limiter surface, where we set $T_e(0) = 5 \text{ eV}$.

The heat load on the limiter plates is partly due to the energy flux Q_{sh} and partly due to the remainder of P_{chg} that is not transferred to the edge plasma, but is carried to the plate by the fast component. Even at these relatively high plasma pressures, most of the power is delivered to the plates by the fast ions.

By accounting for all of the power at the limiter, we are ignoring the fact that radiation and charge exchange will redirect some of the power onto other surfaces. For this reason, the present treatment should overestimate the heat load on the limiter. The particle and power flux due to charge-exchange atoms is discussed in the section on gas penetration. Since we

The total surface area of the limiter plates A_{lim} , including both top and bottom limiters, is determined from the ratio of the total heat $P_{chg} = 59 \text{ MW}$ to the desired average heat load 1.25 MW/m^2 , or $A_{lim} = 47 \text{ m}^2$. The magnetic field lines were plotted with the EFFI code¹⁷, and the energy flux mapped onto the limiter as shown in Fig. 3-1. That shape gives a peak heat load of just under 2 MW/m^2 and an average heat load of 1.25 MW/m^2 . The erosion of the limiter surface due to sputtering by fast ions and neutrals is discussed in a later section.

3.9. GAS PENETRATION

The calculation of the sink, $S(X,Z)$, in the limiter region of the shadow requires the analysis of the gas penetration into the plasma. The interaction of gas with a plasma is especially complicated if the gas and plasma are both hydrogenic, because in that case there is an unusually large number of possible reactions that can occur. Charge-exchange reactions are of particular importance because they usually dominate the interaction. Successive charge exchange between H atoms and H^+ ions can allow H atoms to penetrate much deeper into a plasma than other atoms. This fact means that if the edge plasma is a shield against hydrogen, it is also a much better shield against other impurities. We therefore concentrate on the penetration of H. (We use "H" here to represent any isotope of hydrogen.)

A one-dimensional computer code was used to study the penetration of H into an H^+ plasma. The code is simple and flexible enough to allow easy

study of the effects of changes in the plasma parameters. A more sophisticated code such as DEGAS¹⁸ must be used for the final design.

Figure 3-4 is an expanded view of the limiter region of the shadow, from Fig. 3-2. Figure 3-4 shows the varying thickness of the shadow zone in the limiter region. At each of several Z locations in this region we make a 1-D calculation of the penetration of gas out from the limiter plate.

Figure 3-5(a) shows the density of neutral atoms, and (b) shows the production rate of cold ions as a function of distance X out from the neutralizer plate, per unit flux of incident atom. The four curves in the figure show the ions produced from molecules, Franck-Condon neutrals, charge-exchange products, and the total of these three. The tic marks along the X-axis show the varying zone sizes, and the large tic mark indicates the depth beyond which the temperature and density are assumed to be uniform. In this calculation, both the electron temperature and density are assumed to vary exponentially between specified values at the sheath and at the interior plasma, which is assumed uniform.

The production rates of cold ions give the source S, as a function of X, to use in the power sink in the fluid calculations. The solutions are then self-consistent in the sense that the average plasma parameters from one calculation are used to determine the penetration of gas in the other calculation, and the resulting neutral density is used to determine the plasma parameters. One weakness of this method is that only average values are passed from one calculation to the other.

Figure 3-5. Neutral density (a) and production rate of cold ions (b) per unit flux of incident atoms plotted as functions of distance X out from the limiter surface. The four curves in each figure give the atoms or ions from molecules, Franck-Condon neutrals, charge-exchange neutrals, and the sums of these.

3.10. VACUUM PUMPING

Gas is produced on the plasma side of the limiter plates as ions are neutralized and as reflux atoms recombine. Nearly all of the ion current to the limiter is due to the warm ions near the plate. These ions flow along the magnetic field and enter the sheath with energy equal to half the local electron temperature, as required by the Bohm sheath condition. After acceleration through the sheath, these ions impact the limiter with about

$4 T_e$ of energy. When the energy of the accompanying electron and of recombination are included, the energy deposited on the limiter per ion is about $7 T_e$. A rough value of the ion current to the limiter is therefore obtained by dividing $7 T_e \approx 35$ eV into the ~ 10 MW carried by the warm plasma, giving $I_{lim} \approx 3 \times 10^5$ A.

The impact energy of about 35 eV results in a reflection coefficient¹⁹ of about 65 %, leaving 35 % to recombine into molecules. Each ion that recombines results in about 0.051 reflux Franck-Condon atoms to the limiter, and about 0.224 charge-exchange reflux atoms to the limiter (as determined from the numerical calculation of gas penetration). Then, each of these reflux atoms results in about 0.45 reflux atoms. The total current of reflux atoms is therefore

$$I_{reflx} \approx 0.39 I_{lim} \approx 1.2 \times 10^5 A_a ,$$

where A_a indicates "equivalent amperes of atoms." Reflux atoms impact with an average energy of about 200 eV, determined by the ion temperature at their birth points. At this energy the particle reflection coefficient¹⁹ is only 40%, so that the total production rate of molecules at the surface is

$$I_{mo} = 0.35 I_{lim} + 0.6 I_{reflx} \approx 1.75 \times 10^5 A_a .$$

Molecules must flow away at the rate that they are produced, giving

$$\frac{1}{2} n_{mo} v_{mo} e A_{lim} = I_{mo} .$$

Since $A_{lim} = 47 \text{ m}^2$ and molecular speed is $v_{mo} = 1450 \text{ m/s}$ at the 500 K temperature of the limiter, we can solve for the molecular density to obtain

$$n_{\text{mo}} = 3 \times 10^{19} \text{ molecules/m}^3 .$$

At 500 K, this gives a gas pressure of $p_{\text{lim}} = 1.5 \times 10^{-3}$ torr directly in front of the plates.

Because the reflux atoms tend to impact the limiter at normal incidence, any atom that enters one of the small ports will pass on through to the pumping duct.²⁰ Including this effect the total rate of removal of gas and atoms through the ports is

$$I_{\text{ports}} = f_A I_{\text{refl}} + 6.8 (p_{\text{lim}} - p_{\text{duct}}) C_{\text{ports}} , \quad (11)$$

where $f_A = \frac{A_{\text{ports}}}{A_{\text{lim}}}$ is the fraction of the limiter surface occupied by the ports, p_{duct} is the gas pressure inside the duct, and C_{ports} is the combined gas conductance of the ports:

$$C_{\text{ports}} = \frac{1}{4} v_{\text{mo}} A_{\text{ports}} \left(1 + \frac{4L_{\text{ports}}}{3d_{\text{ports}}} \right)^{-1} .$$

The units are mks as before except that now pressure is in torr. Here, L_{ports} is the length of a port, and d_{ports} is the diameter. Equation 11 gives the relationship between the area ratio f_A , the pressure in the duct p_{duct} , and the ratio $\frac{L_{\text{ports}}}{d_{\text{ports}}}$. When we choose any two of these quantities, the third one is determined by this condition in order to remove gas at the rate that gas is added to the system.

We choose to operate the duct at $p_{\text{duct}} = 0.01$ torr to decrease the size of the duct and of the pipes to the external pumps, and to allow the pumps

to operate at the rather high pressure of about 1 millitorr. A reasonable value for the ratio $\frac{L_{\text{ports}}}{d_{\text{ports}}} = 7$ then gives a reasonable value of $f_A = 0.14$.

This corresponds to a combined port area of $A_{\text{ports}} = 6.5 \text{ m}^2$.

Because the duct is rather thin, as seen in Fig. 3-1 and as shown below, the ducts cannot be longer than about 60 mm, even though about 20 mm is available inside the outer Be tiles. Setting $d_{\text{ports}} = \frac{60 \text{ mm}}{7}$ then gives the area of each port. It therefore takes 1.1×10^5 ports to give the combined 6.5 m^2 , and the average spacing between port centers is 21 mm. This spacing is comparable to the molecular mean free path for ionization, which is about 15 mm in the plasma near the plate. It is reasonable to space the holes about a mean free path apart, because this results in a nearly uniform gas source at the limiter.

The pumping duct behind each half of each of the two limiters has the shape of a very wide but thin rectangular channel. Each must conduct one fourth of the 3 kA of current, or $0.11 \text{ torr} \cdot \text{m}^3/\text{s}$ each, at this temperature. If we allow a 2 millitorr pressure drop along the 0.9 m length of each duct, the required conductance is $55 \text{ m}^3/\text{s}$ each. For wide slot-shaped ducts the conductance is given by

$$C_{\text{duct}} \simeq \frac{2v_{\text{mo}} a^2 b}{3L_{\text{duct}}},$$

where $v_{\text{mo}} = 1450 \text{ m/s}$ is the molecular speed, a is the thickness, $b \simeq 2\pi R = 10.7 \text{ m}$ is the width, and $L_{\text{duct}} = 0.9 \text{ m}$ is the length. This gives the thickness at the exit end because the gas is added nearly uniformly along its length and the full amount is conducted only at the exit end. This expression then gives $a = 69 \text{ mm}$ for the maximum thickness. Notice that the

required thickness varies as the square root of the gas load, so that the thickness should increase as the square root of distance along the duct if gas is added uniformly along it.

Gas is removed from the ducts by 32 circular pipes, one at the top and one at the bottom between each pair of TF coils. Each of these pipes has a length inside the neutron shielding of about 1.3 m. The diameter of the pipes is increased outside the shielding where more space is available, so that we need consider only the conductance of the inner 1.3 m here. Each pipe conducts $1/32$ of the 3000 A of input current, or $13.75 \text{ millitorr} \cdot \text{m}^3/\text{s}$ per pipe. For the pressure to drop to 1 millitorr at the pump end, the required conductance is $1.5 \text{ m}^3/\text{s}$ per pipe. The expression for the conductance of a pipe, as used for the small ports above, gives $d_{\text{pipe}} = 0.19 \text{ m}$ for the diameter of the pipes where they are inside the shielding and must be small. Their relative size can be seen in Fig. 3-1. Their location between the TF coils and radially out where the spacing between coils is rather large prevents them from displacing much of the important shielding between the coils and the plasma.

To summarize the vacuum pumping system, we note the following: Small pumping ports in the limiter surface, and aligned normal to it, accept a large enough fraction of the reflux atoms to allow the pumping duct to operate at the rather high pressure of 10 millitorr. At this pressure, the duct behind the ports is less than 70 mm thick because it is so wide, extending all around the machine. Then, 32 circular pipes carry the gas to external mechanical vacuum pumps, which can therefore operate at a pressure slightly less than 1 millitorr.

3.11. EROSION BY SPUTTERING

Erosion of the limiter results from the bombardment by ions and atoms with energies above the threshold for sputtering. The two classes of particles with energies above the threshold are the primary fast ions and charge-exchange neutrals. Primary fast ions lose only a fraction of their energy by drag in the edge plasma before they impact the limiter. These ions have energies that place them near the peak in the sputtering curves, but they impact at near grazing incidence. Sputtering yield increases with angle up to a maximum at about 80° from normal. For larger angles of incidence, the yield decreases and approaches zero as more of the incident particles are reflected.²¹ At near grazing incidence, the roughness of the surface determines the sputtering yield.²² It is therefore reasonable to assume, as we do here, that the yield at grazing incidence is roughly equal to that at normal incidence.

The dense edge plasma shields the core plasma from most sources of impurities, but at its innermost region, the sputtered atoms from the limiter are shielded by only a thin boundary plasma zone. To reduce the effect of any sputtered metal on the plasma, and to allow for erosion, the surface of the limiter is covered with about 20 mm of Be tile. The 3 kA of fast primary ions impact the limiter with about 6 keV of energy. For 6 keV D and T ions incident normally on Be, the sputtering yield is about 0.010 atoms per ion for D^+ , and about 0.04 for T^+ .²³ This results in a contribution to the gross erosion rate of 2.5 mm/yr at the Be tiles.

Sputtering by charge-exchange neutrals produces a more serious erosion problem. These reflux atoms can be classified according to whether they were produced far enough out into the plasma to have energies above the

threshold for sputtering. The current of neutrals from regions where energies are above the 30 eV threshold is about half of

$I_{\text{reflx}} = 1.2 \times 10^3$ A, calculated in the section on vacuum pumping. The average impact energy of these reflux atoms is about 200 eV, giving them a sputtering yield of 0.02 for D+ on Be and 0.03 for T+. This results in a very large contribution to the gross erosion rate of 51 mm/yr.

The combined erosion rate due to sputtering would be intolerable if it were not for the fact that most of the sputtered Be is ionized near the surface and accelerated by the sheath back onto the surface. Redeposition²⁴ replaces most of the sputtered metal, and results in an erosion rate that is much smaller than the gross rate.

The largest ion flux is that of the warm, edge plasma ions. They enter the sheath with energies of about 5 eV and are then accelerated by the sheath potential up to about 35 eV before they impact the surface. Since the electric field in the sheath is normal to the surface, the ions also are moving nearly normal to the surface at impact in spite of the magnetic field. This impact energy is roughly equal to the threshold energy for sputtering of Be, so that the sputtering yield for this group of ions is negligible relative to that for the charge-exchange atom flux. Franck-Condon neutrals also bombard the surface, but their energy is only about 3 eV, well below threshold.

3.12. DISCUSSION OF THE LIMITER DESIGN

The description of the TIBER edge plasma is preliminary, but it does show that the necessary ingredients exist in TIBER to produce the desired edge plasma parameters. Drag on the fast escaping particles tends to

maintain the edge plasma T_e in the desired range $10 < T_e < 100$ eV and because drag decreases as T_e increases, the power absorbed should remain stable. Further, TIBER's vented-port type of pumped limiter allows gas recycling at the limiter to be controlled. As Ref. 9 shows quite generally for a divertor plasma, the plasma temperature in the recycle region decreases linearly with recycle ratio, and density increases as the $3/2$ power of that ratio. It appears therefore that both the power and the temperature of the dense edge plasma can be held in the desired ranges by controlling the recycle fraction. The more complicated question of gas and impurity penetration into the core plasma near the nose of the limiter is not easily answered. Gas produced at the outer parts of the limiter are recycled there, but some gas and sputtered Be is necessarily produced at the inner surface near the core plasma, and some of that penetrates at least into the boundary zone. A calculation of the power and particle balance here will require more work.

It is very encouraging that a set of edge plasma parameters can be found that is consistent, at least in this preliminary analysis. Power to maintain the warm, dense, edge plasma can be absorbed from the fast component, and the temperature of the warm plasma tends naturally to a desirable value: low enough to reduce sputtering and high enough to attenuate gas and impurities. By using the directed motion of the reflux atoms, we can allow the vacuum system to operate at a conveniently high pressure, thus minimizing the size of the ducts.

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4.0. MAGNETIC CONFIGURATION (R. H. Bulmer)

We used the Tokamak MHD equilibrium model developed by Strickler¹ to define the TIBER magnetic configuration. Our goal was to produce a coil set and plasma boundary shape consistent with high β operation ($\langle\beta\rangle \approx 10\%$) by controlling the elongation, triangularity, and inboard indentation of the plasma surface.

4.1. PLASMA EQUILIBRIUM

In the Troyon-Wesson formula, the β limit depends only on plasma current, toroidal field, and minor radius. The plasma boundary shape factors enter into the stability criterion when the equilibrium conditions are satisfied at the desired current, field and radius. Our design point for TIBER equilibrium analysis uses

Plasma current (I_p)	=	10 MA
Toroidal field (B_T^p)	=	5 T
Minor radius (a)	=	0.72 m
Major radius (R_0)	=	2.6 m
Plasma-edge safety factor (q_{edge})	=	2.2

These parameters are self-consistent with a plasma elongation of 2.0, triangularity 0.5, and an indentation of 0.033.

Although the external pusher coil has a weak effect on the plasma indentation, it is important in that it aids the other equilibrium-field (EF) coils in improving both the elongation and triangularity.

In order to achieve a maintainable configuration, all of the equilibrium field coils are external to the TF coils. Therefore, a highly

distributed EF coil set is ineffective (as well as undesirable). We find that a total of six EF coils (in addition to the pusher coil) provide a sufficient current distribution to satisfy the equilibrium requirements. This configuration is shown in Fig. 4-1, and the major coil parameters are listed in Table 4-1.

Figure 4-1. TIBER equilibrium model.

Figure 4-2 shows the equilibrium flux surfaces for the configuration. Control of the pressure, flux, and current profiles is necessary in order to hold the safety factor, q , at the desired value of 2.2 on the plasma edge.

Table 4-1. TIBER EF coil parameters.

Coil	Quantity	Z_{coil} (m)	r_{coil} (m)	Current (MA)
Pusher	1	0	0.75	18.00
EF1	2	± 1.80	0.75	- 7.50
EF2	2	± 3.25	2.00	- 2.35
EF3	2	± 1.50	5.00	5.86

Figure 4-2. TIBER equilibrium flux surfaces.

In Strickler's equilibrium model, these profiles are controlled by several input parameters that must be adjusted until the safety factor constraint is satisfied. The resultant profiles are shown in Fig. 4-3.

Figure 4-3. TIBER equilibrium profiles.

4.2. MAGNETIC ANALYSIS

The magnetic field analysis in the equilibrium model utilizes the 2-D, axisymmetric nature of the configuration to simplify the equations. Subsequent field and force calculations require a 3-D model to properly treat the discrete TF coils. We have chosen to use 16 TF coils, each carrying 4.06 MA (65 MA total) to provide the 5-T field.

Each TF coil is modeled with four elements for the EFFI² program. The inner leg of each TF coil is a large-radius (500-m) arc which connects to two identical 1-m-radius sections. The outer leg of each coil is a 2.51-m-radius arc. The mean turn length in the TF coils is 13.66 m. The TF coil model is depicted in Fig. 4-4, and the complete EFFI model is shown in Fig. 4-5.

Figure 4-4. TIBER toroidal field model.

Figure 4-5. TIBER magnetic coil configuration.

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5.0. TF-COIL NUCLEAR SHIELDING (J. D. Lee)

5.1. INTRODUCTION

The TF superconducting coils (SC) must be shielded from the DT fusion neutrons to limit damage and heating to acceptable levels. This shielding must have minimal impact on the size and cost of T1BER. We are endeavoring to meet these objectives by setting aggressively high damage and heating limits for the SC and by using space-efficient high-density tungsten (W) shielding on the inner leg and lower cost steel shielding for the outer leg.

Work to date has resulted in a design with a minimum inner-leg shield thickness of 40 cm that may just meet our three-year lifetime requirement and that has peak SC heating of 21 mW/cm^3 . This design has a 2.6-m major radius and employs a pumped limiter. Total heating in the TF coils and their cases is 43 kW with all tungsten shielding. With steel shielding beyond the inner leg, total TF coil heating will increase to about 60 kW. Outer first wall loading at the midplane is 2.1 MW/m^2 . These results are preliminary in that they are from a crude 2-D approximation of the toroidal geometry and from an assumed fusion neutron-source distribution.

The radiation limits, taken from pages A27 and A36 of Ref. 1, are summarized below

- Electrical and thermal insulation - 10^{11} rads.
- NbTi SC - 10^{21} n/cm^2 ($E > 0.1 \text{ MeV}$) accompanied by a 25% drop in critical current, 70% of which is annealable at room temperature. Most of the drop in critical current (20%) has occurred beyond 10^{18} n/cm^2 .

- Nb_3Sn SC - 1 to 4×10^{19} n/cm² ($E > 0.1$ MeV). Based on very limited data. Damage is not annealable.

Because of the time and expense involved in doing detailed 2- or 3-D analyses, a simplified 2-D method was developed and used to analyze the configuration. This method consists of the following steps:

1. Calculate heating and damage rates in the SC vs shield thickness using 1-D slab geometry.
2. Calculate 14-MeV neutron power and currents (wall loading) vs poloidal position at the TF coils using coil and plasma geometries and an assumed source profile.
3. Combine steps 1 and 2 to get a 2-D estimate of heating and damage rates. A major assumption is that heating and damage effects are dominated by 14-MeV neutron transport.

The heating and damage rates vs shield thickness were calculated in a 1-D slab geometry using the ONEDANT code and a 30-neutron plus 12 gamma XSLIB data library based on ENDF/B-V. This Los Alamos National Laboratory code and its data library were provided by the University of Wisconsin, where they were used for the MARS² Project.

The configuration used to calculate radiation levels in the coil vs shield thickness is shown in Fig. 5-1. The shield consists of a thick zone of W(79 w/o) + H₂O(10 v/o) + boric acid (1 v/o) + FE 1422 (10 v/o) followed by a thin zone (4 cm) where TiH replaces the W. This shield results from a preliminary optimization study (see p. A15 of Ref. 1). Heating levels, neutron flux ($E > 0.1$ MeV), Cu dpa and insulator dose rates in the coil vs shield thickness at a wall loading of 1.0 MW/m^2 are given in Figs. 5-2 and 5-3. The total heating includes both coil and case heating.

The TF coil design differs from that used for the 1-D calculations in that the space between the shield and the SC coil is taken to be 5 cm, instead of 15 cm, and the effective steel thickness in the case is 2 cm, instead of 6 cm. To account for this difference the local heating and damage rates are increased by a factor of 2 over that calculated using the 1-D slab model. Total heating in the coil plus case is taken to be the same.

The second step in the method is to use the toroidal plasma and coil geometries and the assumed relative and neutron source profile³ to determine neutron fraction and wall loading at the TF coil vs poloidal position (see Fig. 5-4).

In the third step the neutron fraction and shield thickness at each poloidal location are used along with shield attenuation factors from step 1 to estimate coil heating. Wall loading and shield thickness at coil inner-leg poloidal locations along with damage response vs shield thickness from step 1, are used to estimate local peak damage and heating rates in the coil winding.

Figure 5-1. Neutron shielding model.

Figure 5-2. Neutron flux and heating vs shield thickness.

Figure 5-3. Insulator dose rates and Cu dpa vs shield thickness.

FPY denotes full-power year.

Figure 5-4. TIBER with 2.6-m major radius and a pumped limiter.

This method of analysis is expected to overestimate heating and damage on the inner TF leg. Heating in the outer leg is also overestimated by not accounting for coil segmentation, but it is underestimated by not accounting for penetrations.

5.2. RESULTS

The methodology described above was used to evaluate various TIBER configurations in which the major radius and mode of plasma removal (divertor or pumped limiter) were varied. Total heating in the TF coils (windings plus cases) found for these variations are listed in Table 5-1.

Table 5.1. Total heating in the TF coils.

Case	Plasma fusion (MW)	Heating (kW)
1. R=2.6 m with divertor	200	415
2. R=2.73 m with divertor	237	96
3. R=2.6 m with pumped limiter	200	43

The pumped limiter case has significantly lower heating. This occurs because the pumped limiter takes up less shield space on the inner leg than does the divertor. Tables 5-2 and 5-3 list shield thickness and coil heating vs poloidal position for the 2.6-m major radius cases with a divertor and a limiter, respectively. Our case is the 2.6-m pumped limiter one.

Most of the heating (98%) occurs in the inner leg. This allows us to consider switching to a shield based on steel of lower cost for regions beyond the inner leg. The last column in Table 5-3 gives heating values, assuming steel shielding beyond the inner leg. Steel shield attenuation factors relative to tungsten are taken from Gohar. (See p.4-68 of Ref. 1). With steel shielding beyond the inner leg, total TF coil heating increases by 50%, to 62 kW. To reduce this increase to 10% (to 46 kW) would require adding about 13 cm to the steel shield. If steel shielding is used in the inner leg as well, the inner-leg shield thickness

Table 5-2. Total nuclear heating in coils plus cases normalized in 200 MW_e fusion power (2.6-m case with divertor).

Poloidal position number	Neutrons (%)	Shield thickness(cm)	(Shield attenuation factor) ⁻¹	Heating (kW)
1	2.35	41	0.0018	14
2	2.4	37	0.0033	25
3	2.45	30	0.011	86
4	2.7	28	0.015	130
5a	2.1	27	0.018	121
5b	2.1	35	0.0048	32
6	0.9	52	3 E-4	0.9
7	3.15	55	1.8 E-4	1.8
8	6.45	55	1.8 E-4	3.7
9	5.5	70	2 E-5	0.4
10	6.05	76	9 E-6	0.2
11	6.7	80	4 E-6	0.08
12	<u>7.1</u>	82	9 E-7	<u>0.02</u>
Totals	50x2=100			415

Table 5-3. Estimate of nuclear heating in coil plus cases normalized to 200-MW_e fusion power (2.6-m case with pumped limiter).

Poloidal position number	Neutrons (%)	Shield thickness(cm)	(Shield attenuation factor) ⁻¹	Heating (kW)	Heating ^a (kW)
1	2.35	45	9.6 E-4	7.2	7.2
2	2.4	43	1.3 E-3	10.0	10.0
3	2.45	40	2.1 E-3	16.5	16.5
4	2.7	45	9.6 E-4	8.3	8.3
5a	2.1	65	4 E-5	0.3	0.8
5b	2.1	80	4 E-6	0.03	0.9
6	.9	80	4 E-6	0.01	0.3
7	3.15	75	9 E-6	0.09	2
8	6.45	70	2 E-5	0.4	6
9	5.5	75	9 E-6	0.2	4
10	6.05	80	4 E-6	0.08	2
11	6.7	83	3 E-6	0.06	2
12	<u>7.1</u>	85	2 E-6	<u>0.05</u>	<u>2</u>
Totals	50x2=100			43	62

^aWith steel shielding beyond inner leg.

must be increased about 10 cm to maintain the same level of heating and damage rates.

Peak heating and damage rates in the TF coil windings occur in the inner leg. Table 5-4 lists peak heating, neutron flux, insulator dose rates, and copper dpa rates vs poloidal position along the inner leg. Neutron damage to the Nb_3Sn SC is life limiting. Comparing the peak neutron flux of $1.1 \times 10^{19} \text{ n/cm}^2/\text{yr}$ to the fluence limit of 1 to $4 \times 10^{19} \text{ n/cm}^2$ gives a lifetime ranging from 0.91 to 3.6 years at full power. The desired lifetime is 3.2 full-power years.

5.3. RECOMMENDATIONS

More rigorous multidimensional analyses will be needed to establish benchmarks for the methods used and to optimize design. The latter will be accomplished by trading off shielding thicknesses and compositions with system size and cryogenic heating loads. Neutron fluence limits on the life-limiting Nb_3Sn superconducting coils also need refining.

Table 5-4. Estimate of local peak heating, flux, insulator dose rates, and Cu dpa in the TF coils vs poloidal position for a fusion power of 200 MW (2.6-m case with pumped limiter).

Poloidal position number	Wall loading at TF coil (MW/m ²)	Minimum shield thickness (cm)	Flux			
			Heating (MW/cm ³)	E > 0.1 MeV (n/cm ² ·yr)	Dose rate (rad/yr)	Cu dpa (yr ⁻¹)
1	1.71	45	18	9.4E18	2.0E10	7.2E-3
2	1.27	43	18	9.4E18	2.0E10	7.2E-3
3	0.96	40	21	1.1E19	2.2E10	9.0E-3
4	0.96	45	10	5.0E18	1.0E10	4.0E-3
5	0.82	65	0.37	1.8E17	4.0E8	1.5E-4

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6.0. PUSHER COIL DESIGN (R. W. Hoard)

The pusher coil for TIBER consists of two zones: an inner $\text{Nb}_3\text{Sn:Ti}$ subcoil and an outer Nb-Ti pack (see Fig. 6-1). The Nb_3Sn conductor is approximately $1.0 \times 0.5 \text{ cm}^2$ and is envisioned to be similar to that presently used in the MFTF 12.7-T choke-coil design (i.e., two pieces of stabilizer soldered to a Nb_3Sn insert) to reduce winding strains. The stabilizer is annealed soft copper so it is necessary to cwind the conductor with a 3-mm-thick stainless steel band (21-6-9). Magnetic strains are limited to 0.003. The Nb-Ti subcoil is allowed strains up to 0.0045 so its turns do not have to be self-supporting; however all of the forces should be transferred to an outer coil case of approximately 2.0-cm-thick stainless. This is because the average stress on the 38-cm-thick pack is about 40 ksi.

6.1. LOW FIELD CONDUCTOR

The original MFTF conductor produced a peak field of 7.8 T at 4.2 K. Reduced temperature operation at 1.8 K yields an even higher critical current in the superconductor at 10-T fields. This implies that the current in the original MFTF winding pack can be increased by $4000/3122 \text{ A/cm}^2$ or by 28% to satisfy the requirements of the Nb-Ti background coils in the high-field pusher magnet. The projected increase in the wetted-perimeter heat flux is $(1.28)^2 \times 0.19$ or 0.31 W/cm^2 , which is manageable with He II.

Hence, we propose using the Yin-Yang conductor for this application by increasing the operating current to 739 A. Forces must be reacted to the case supporting structure.

6.2. HIGH FIELD CONDUCTOR

A $\text{Nb}_3\text{Sn:Ti}$ conductor, originally operated in the MFTF choke coils at 13-T fields, can be used with He II in fields of 14 T due to the low temperature enhancement of J_c . This motivates us to design TIBER to use a conductor for the insert pusher subcoils similar to that fabricated by Furukawa ($\text{Nb}_3\text{Sn:Ti}$) for the MFTF high-field choke coils, which operate at a peak field of 12.7 T. However, in this application the extra copper stabilizer should be made of an annealed, high-conductivity material to limit the wetted-perimeter heat fluxes to 1.0 W/cm^2 . This implies that the large 50-kip (340-MPa) stresses must be reacted to prevent damage to the strain-sensitive $\text{Nb}_3\text{Sn:Ti}$. Since the radial pack thickness is only 12.5 cm, the coil design depends upon transmitting the conductor stresses to a 2-cm-thick stainless steel outer case surrounding the $\text{Nb}_3\text{Sn:Ti}$ subcoils. The coil is composed of twenty subcoils, each clad in 1-cm-thick coil cases in order to increase the effective coil modulus and transmit the radially compressive forces from the TF coils to the additional supporting structure located at the magnet bore. The fringe fields from this coil supply an approximate 0.5 T on the plasma "bean-contour."

In general, we have chosen to use conductor designs that have proven to be both functional and within the present experience of the manufacturing industry. This decision should dramatically lower the costs and risks of the proposed TIBER reactor experiment, because the only new technology being introduced is the deployment of superfluid refrigeration systems.

Figure 6-1. TIBER pusher coil.

7.0. TF WINDING-PACK DESIGN (J. R. Miller)

7.1. PRELIMINARY WORK

A preliminary toroidal-field (TF) coil winding pack was designed to show the feasibility of producing a 10-T maximum field with a pack current density of 40 A/mm^2 for 2.7-kW-per-coil nuclear heating. The design, reported at the 6th Topical Meeting on the Technology of Fusion Energy,¹ using a cable-in-conduit conductor (CICC) took into account several key issues summarized here.

7.1.1. Heat Removal

Local heat transfer was not seen as a critical issue. Instead, constraining the coolant temperature rise in a conductor flow path to a value consistent with a comfortable level of conductor stability was identified as most important. Simple but conservative formulae were used to select flow path length, injection temperature and pressure of the supercritical helium coolant, pressure drop through a passage, outlet temperature, and temperature rise in an innermost turn. Double pancakes with two-in-hand winding and helium injection at the inside crossovers would appear to give cooling channels sufficiently short to limit the temperature to a maximum of about 5.6 K at the outlets. The pressure drop, estimated to be about 2.4 atm, allows an inlet pressure in the neighborhood of 5 atm, which is a range where the heat capacity of helium is very high. More

detailed calculations, performed in conjunction with the refrigeration system design, indicated that these preliminary estimates were indeed quite conservative.

7.1.2. Mechanical Loads

Tensile and centering loads on the inner, vertical leg of a TF coil were estimated in order to examine their impact on winding pack design. The centering force causes a build up of pressure on the outermost turns of a TF-coil inner leg (those turns nearest the center of the machine). That pressure was estimated to be about 53 MPa. A CICC with the dimensions and conduit wall thickness of the Westinghouse/Airco conductor for the Large Coil Project (LCP) has been demonstrated to be capable of sustaining such loads in an impregnated winding.² The TIBER conductor therefore was given similar sheath dimensions. Tensile loads were assumed to be shared between the coil case and the steel in the sheath of the CICC windings. The estimated stress for the baseline TF coil cross section was modest, 266 MPa. Approximately 62% of the tensile load was born by the case in this configuration, and the remaining 38% by the steel in the CICC windings.

7.1.3. Stability

The fraction of conductor inside the sheath of the CICC and the fraction of copper inside the conductor were optimized for maximum conductor stability subject to the constraints that the fraction of copper not be less

than the "manufacturable" limit and that the heat transfer surface provided by the cable strands not fall below the value determined by the limiting current for multiple stability.³ It should be remembered that these limits are not rigid but are prudent for preliminary design.

The stability available with the optimized design was quite high. Calculations were based on data for critical current density vs temperature representative of the Airco, bronze-process, multifilamentary (MF) Nb_3Sn conductor developed for the Westinghouse LCP coil (see Fig. 7-1).⁴ The data were degraded to account for compressive prestrain due to differential cooldown stresses developed in a cable inside a steel conduit (the lower curve in Fig. 7-1). Even assuming the high field turns to be exposed to the outlet temperature of 5.4 K, the ratio of operating current to critical current would be $I/I_c = 0.59$. The current-sharing temperature was calculated to be $T_{cs} = 7.4$ K. These figures indicate that some level of stability might be traded for higher winding-pack current density.

7.2. CURRENT DEVELOPMENTS

Time has allowed us to iterate the various preliminary choices of machine parameters, and these choices have had an impact on the design of the winding pack. Because it appeared in the first cut that there was indeed margin for change without unduly sacrificing stability, heat removal, or reliability, the design has been reexamined to determine just how much

Figure 7-1. Critical current density over the noncopper fraction for single strands of the MF-Nb₃Sn conductor developed for the Westinghouse LCP coil. The solid line below the one fitting the data points represents a conservative accounting for the compressive strain imposed on the conductor by being reacted and cooled down to operating temperature inside a steel sheath.

change might be accommodated. Meanwhile, new technological developments have occurred, and clarification of some key constraints placed on the winding pack by other features of the TIBER design has been obtained.

7.2.1. Updated Critical Current Database

Significant advances in MF-Nb₃Sn conductor technology have been achieved by adding Ti to the compound and by fabricating conductors with ultrafine filaments. Suenaga⁵ recently illustrated these advances with the exposition of some of his own measurements (see Fig. 7-2). The solid curves represent conductors with Ti additions and the broken curves, those with pure Nb₃Sn. More detail about the variations responsible for the range of data is not necessary here. It is sufficient to note that all conductors represented in Fig. 7-2 give higher critical current than the conductor represented in Fig. 7-1.

The design of a CICC requires more information about the critical current than a single value at a particular field and temperature. Because CICC stability is obtained by providing a temperature margin within which heat deposited in the conductor is absorbed by interstitial helium, it is essential to incorporate knowledge of the temperature variation of the critical current in the design process. There is an extensive data base, including temperature-dependent data, associated with the conductor and represented in Fig. 7-2 by the emboldened solid curve connecting open diamond data points.⁶ Figure 7-3 displays part of this data base.

It is clear that, over the temperature range of interest, the temperature dependence of the critical current at a particular field is essentially linear. Therefore, it can be fully represented by the vertical and horizontal intercepts, $J_{c0}(B)$ and $T_c(B)$ respectively. These intercepts,

Figure 7-2. Recent data showing the improvements in MF-Nb₃Sn conductor technology. The solid curves represent conductors with Ti additions to the compound, and the broken curves represent improvements obtained by geometrical changes in the starting billet, i.e., smaller filaments, more Nb, etc. The highlighted solid curve with open diamond points is from the same data base as Fig. 7-3.

Figure 7-3. Critical current density over the noncopper fraction vs temperature at various fields for a Ti-modified MF-Nb₃Sn conductor. The data represented here are from the same data base as that represented by the highlighted solid curve in Fig. 7-2.

obtained from the data of Fig. 7-3 and similar data on identical conductors, are correlated with field variation in Figs. 7-4 and 7-5. Figure 7-5 shows that a linear variation of T_c with B is an adequate representation, but it is apparent that values of J_{co} must be obtained by extrapolation or interpolation from a plot like that of Fig. 7-4. For the present iteration

Figure 7-4. A Kramer correlation of the extrapolated, zero-temperature, critical current density vs field from the reference conductor data base.

of winding pack design, $J_{\infty} = 1196 \text{ A}\cdot\text{mm}^{-2}$ and $T_c = 11.89 \text{ K}$ have been chosen as representative for the 10-T-design maximum field. Note that these values correspond to data at the lower edge of the band in Fig. 7-2 at 10 T, and as such, represent conservative choices.

7.2.2. Corrections for Cooldown Strains

The materials inside a CICC all have different coefficients of thermal expansion. As a result, it should not be too surprising that the action of cooling down from the Nb_3Sn formation temperature ($\sim 1000 \text{ K}$) to the operation

Figure 7-5. A linear correlation of the extrapolated, zero-current, critical temperature values vs field from the reference conductor data base.

temperature of the magnet places the superconducting Nb_3Sn filaments under compression. The amount of compression depends on fractions of the various materials and their physical and mechanical properties. It also appears to depend on the compactness of the cable inside the conduit. A correlation of existing data suggests that the filaments of a conductor of the description being proposed for TIBER will experience a compressive strain of about 0.4% due to cooldown.⁷

Ekin has given a formalism for predicting the strain degradation of critical current in Nb_3Sn conductors.⁸ His predictions are depicted

graphically in Fig. 7-6 for the conductor selected for TIBER. The upper curve corresponds to 4.2-K operation and the lower curve to 5.6-K operation. A background field of 10 T is assumed for both. The result of Fig. 7-6 can alternatively be expressed as a reduction of both J_{co} and T_c . It is straightforward to show that 0.4% compressive strain yields new values of $J_{co} = 1038 \text{ A}\cdot\text{mm}^{-2}$ and $T_c = 11.29 \text{ K}$ at 10 T.

Selection of these values for design purposes should be considered conservative on two counts. First, the turns of conductor in the inner, vertical leg will be subjected to significant tensile load that should reduce the compressive strain caused by cooldown and improve the critical properties at the actual operating conditions.⁷ Development is underway that may allow accounting for this improvement by design. Second, Hoenig and his colleagues at MIT have demonstrated that alternate sheath materials can be selected to reduce the cooldown strain.⁹ Thus methods are in the offing to provide gains in critical current in a CICC without further development directly on the superconductor, but for now we take account of these developments only as a margin of conservatism.

7.2.3. Stability

The sudden deposition of energy that a CICC can absorb per unit volume of conductor without experiencing an irreversible quench is referred to as its stability margin ΔH . It has the form¹⁰

Figure 7-6. Fraction of the maximum available (unstrained) critical current vs intrinsic filament strain for the reference conductor at 10 μ and at 4.2 K and 5.6 K.

$$\Delta H = S_{He} (T_{cs} - T_b) (A_{He}/A_{cond}) .$$

Here S_{He} is the effective volumetric heat capacity of the helium between the initial bulk fluid temperature T_b and the current-sharing temperature of the conductor T_{cs} . The last factor, the ratio of helium and conductor cross sections, merely refers the absorbed energy to the conductor volume.

The influence of stability on conductor design can be better visualized by casting the above in another form that results from the linear variation of critical current with temperature already discussed:

$$\Delta h = \frac{\Delta H}{S_{He} T_c} = \frac{(1 - f_{cond})}{f_{cond}} \left[\left(\frac{T_c - T_b}{T_c} \right) - \frac{J}{f_{cond} (1 - f_{Cu}) J_{co}} \right] .$$

In this form, the dimensionless stability parameter Δh has the properties of the coolant factored out leaving only the parameters representing the conductor and the operating conditions: f_{cond} is the fraction of conductor inside the conduit (thus, $1 - f_{cond}$ is the fraction left for helium), f_{Cu} is the fraction of copper in the conductor strands, and J is the desired current density over the cable space, i.e., the area inside the conduit.

Contours of the stability parameter are plotted in the (f_{cond}, f_{Cu}) plane in Fig. 7-7 for the conductor critical parameters already discussed

Figure 7-7. Contours of the stability parameter in the $(f_{\text{cond}}, f_{\text{Cu}})$ plane with $J_{\text{Co}} = 1038 \text{ A}\cdot\text{mm}^{-2}$, $T_{\text{C}} = 11.29 \text{ K}$, $T_{\text{L}} = 5.6 \text{ K}$, and $J = 77.6 \text{ A}\cdot\text{mm}^{-2}$.

and for $T_{\text{b}} = 5.6 \text{ K}$ and $J = 77.6 \text{ A}\cdot\text{mm}^{-2}$, corresponding to $J_{\text{pack}} = 45 \text{ A}\cdot\text{mm}^{-2}$ for the present case. It is apparent that for a particular copper fraction in the conductor, there is a unique conductor fraction that gives the highest stability. We do not have complete freedom to choose the configuration giving highest stability; e.g. we have already mentioned a possible "manufacturing" limit on the copper fraction, and there are other limits. It will, however, be convenient to examine the constraints on conductor design in the light of a mapping like Fig. 7-7 in order that we choose the configuration giving the maximum available stability subject to the constraints.

7.2.4. Effect of End-of-Life Damage to Copper Stabilizer

The current shield design for TIBER will allow damage to the copper stabilizer of the conductor in some sections of a TF coil of up to a level $D = 0.009$ dpa per year. This damage will cause an increase in the residual resistivity of the matrix according to the following¹¹:

$$\Delta\rho_0 = s[1 - \exp(-iD/s)] \quad ,$$

where s is the saturation level and i is the saturation rate. Sawan¹² gives $s = 3 \text{ n}\Omega\cdot\text{m}$ and $i = 720 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$ while Klabunde et al.¹¹ give $s = 4 \text{ n}\Omega\cdot\text{m}$ and $i = 649 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$. We use $s = 3.46 \text{ n}\Omega\cdot\text{m}$ and $i = 696 \text{ n}\Omega\cdot\text{m}\cdot\text{dpa}^{-1}$ as a compromise. Note that for the anticipated damage only the saturation level and not the rate is important. Magnetoresistivity is accounted for by the relation

$$\rho(B) = \rho_0 [1 + 0.0339(B/\rho_0)^{1.07}] \quad ,$$

where ρ_0 includes the effects of radiation damage. This equation was obtained from a fit to the data of Fig. 3.4.3 [Kohler plot for unoxidized, oxygen-free, high-conductivity (OFHC) copper] in Ref. 13.

For a CICC design the copper resistivity does not to first order affect the stability level provided.¹⁴ It does influence the amount of conductor surface required to effectively utilize the heat capacity of the interstitial helium as can be seen by the variation of the "limiting" current for multiple stability J_{lim} with $\rho^{-1/2}$. The requirement for better heat transfer as copper damage accumulates may already be met by the high flow rates required for steady state heat removal, however. A more important requirement will be that of protection as the coils approach end of life.

7.2.5. Limiting Current

The limiting current density in a CICC is the value below which full stability is available even with initially stagnant helium inside. As such it may be viewed as a good indicator that heat transfer from the cable to the helium is good enough even without being augmented by net flow. It is prudent initially to choose a conductor configuration for which the limiting current density J_{lim} exceeds the design value for the cable-space current density J . Towards the end of life as copper damage accumulates to reduce J_{lim} , the loss will be offset by the flow already required for heat removal. In Fig. 7-8, the contours of the stability parameter have been overlaid with contours of $J_{lim} = J$ for both initial and end-of-life residual resistivity. The present design will observe the constraints on conductor configuration due to the former and ignore those due to the latter.

Figure 7-8. Contours of $J_{lim} = J$ superimposed on the mapping of the stability parameter from Fig. 7-7. Curve (a) assumes copper with a residual resistance ratio, RRR = 100 at 10 T and no damage. Curve (b) assumes 0.009 dpa/yr for three years.

7.2.6. Protection

There are two areas that must be examined to evaluate whether protection is adequate for a coil using a CICC: maximum pressure inside the conduit in the event of a quench and maximum hot-spot temperature in the event of a quench. The former depends strongly on distance between flow connections.¹⁵ That distance will be short in the TIBER design to

facilitate heat removal; therefore pressure rise should not be too great a problem. However, a full evaluation must await further tuning of the design. The latter can be addressed now.

The upper limit to the cable-space current density in terms of the allowed maximum hot-spot temperature can be determined from the following expression:

$$J \leq \sqrt{(2/\tau)} \left[(1 - f_{\text{cond}}) f_{\text{cond}} f_{\text{Cu}} \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{He, init}} c_{v, \text{He}}}{\rho_{\text{Cu}}} dT \right. \\ \left. + f_{\text{Cu}}^2 f_{\text{cond}}^2 \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{Cu}} c_{\text{Cu}}}{\rho_{\text{Cu}}} dT \right. \\ \left. + (1 - f_{\text{Cu}}) f_{\text{Cu}} f_{\text{cond}}^2 \int_{T_b}^{T_{\text{max}}} \frac{\mu_{\text{non-Cu}} c_{\text{non-Cu}}}{\rho_{\text{Cu}}} dT \right]^{1/2},$$

where μ_i and c_i are the density and heat capacity, respectively, of the various materials and ρ_{Cu} is the electrical resistivity of the copper in the conductor. The dump time constant τ is given simply by

$$\tau = (2 E_s / V_d I_{\text{op}}) ,$$

where E_s is the stored energy per TF coil, V_d is the dump voltage across the terminals of an individual coil, and I_{op} is the operating current.

The observation of a conductor temperature rise in experiments that simulate the complete quenching of a CICC is consistent with the assumption that the initial density of the helium and the constant-volume heat capacity of the helium (essentially constant and approximately equal to $3.1 \text{ kJ} \cdot \text{kg}^{-1} \cdot \text{K}^{-1}$) are the appropriate choices for the values in the first term of the above equation.¹⁵

Figure 7-9 shows contours of J equal to the protection limit, calculated with the end-of-life resistivity in the copper. Both contours were calculated with $T_{\text{max}} = 100 \text{ K}$, but the upper was calculated with $V_d = 5 \text{ kV}$ and the lower was calculated with $V_d = 10 \text{ kV}$. It should be noted that essentially the same constraints on conductor parameters as given by the $(T_{\text{max}}, V_d) = (100 \text{ K}, 10 \text{ kV})$ limits are obtained with $(200 \text{ K}, 5 \text{ kV})$ or $(300 \text{ K}, 3 \text{ kV})$. All three of these contours are shown on the same mapping in Fig. 7-10.

It is more or less a matter of philosophy, and a question of which area to put the most development effort into, as to which contour in Fig. 7-10 is seen - the most reasonable constraint. However, there is in fact little difference between them, and protection of the TF coils at end of life looms as the most severe constraint to pushing the winding pack current density much above $45 \text{ A} \cdot \text{mm}^{-2}$ while providing a void fraction of around 40% to facilitate heat removal and to lessen the amount of compressive prestrain on the superconductor, while maintaining the copper fraction at 60% or above for ease of manufacture, and while providing a stability parameter of 0.1 or

Figure 7-9. Contours of J at a protection limit superimposed on the mapping of the stability parameter from Fig. 7-7. Both curves assume copper resistivity at end-of-life damage level and a maximum allowable hot-spot temperature of 100 K. Curve (a) assumes a maximum allowable dump voltage of 10 kV, curve (b) 5 kV.

gre: for reliable operation. For these reasons we select a winding pack current density of $45 \text{ A}\cdot\text{mm}^{-2}$, which will be achieved with a CICC having $f_{cond} = 0.6$ and $f_{Cu} = 0.6$.

Figure 7-10. Similar contours of J at the protection limit superimposed on the mapping of the stability parameter from Fig. 7-7. Each assumes copper resistivity at end-of-life damage level but different choices of (T_{max}, V_d) : curve (a), (100 K, 10 kV); curve (b), (200 K, 5 kV); and curve (c), (300 K, 3 kV).

7.3. SUMMARY

Tables 7-1 and 7-2 summarize the coil and winding pack parameters, respectively. Figure 7-11 shows the winding pack configuration consistent with Tables 7-1 and 7-2, and Fig. 7-12 shows a unit cell of the winding.

Table 7-1. TF coil parameters.

Coil parameter	Value
Total current	65 MA•turns
Number of coils	16
Field on plasma axis	5.0 T
Plasma radius	2.6 m
Mean radius of the inner leg	1.18 m
Mean radius of the outer leg	4.5 m
Mean half height	2.5 m
Mean perimeter	13.7 m
Maximum field	10.0 T
Stored energy/coil	150 MJ
Dump voltage	5.0 kV
Operating current	21 kA

Table 7-2. Coil pack parameters.

Coil cross section (straight leg)	0.143 m^2
Winding pack cross section	0.0903 m^2
Number of turns	192
Pack current density	$45 \text{ A}\cdot\text{mm}^{-2}$
Effective area of a turn	21.67 mm^2
Conductor cable-space current density	$77.6 \text{ A}\cdot\text{mm}^{-2}$
Overall materials fractions	
f'_{steel}	0.26
f'_{insul}	0.17
f'_{cond}	0.34
f'_{He}	0.23
Materials fractions in the CICC	
f_{cond}	0.60
f_{He}	0.40
f_{Cu}	0.60

Figure 7-11. A schematic of the coil cross section at the midplane of the inner vertical leg. Open squares represent turns of the winding pack in the present configuration. Squares covered over by the cross-hatching of the coil case represent deleted turns that were present in the preliminary design with a winding-pack current density of $40 \text{ A} \cdot \text{mm}^{-2}$.

Figure 7-12. Representation of a unit cell of the winding pack. The steel-jacketed CICC is surrounded by insulation. Dimensions are in mm.

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8.0. MECHANICAL STRUCTURE (W. S. Neef and B. M. Johnston)

8.1. FUNDAMENTAL CONSIDERATIONS FOR TOKAMAKS

In all fusion machines using the tokamak geometry, one must pay special attention to the stress generated in the toroidal field (TF) coil's inboard legs and in the center post, against which all TF coils bear. The magnetic loading on TF coils develops a large overturning moment. At the inboard legs this moment has the opposite sense from the overturning moment of the outboard legs. Besides the overturning moments, a huge thrust against the center post results from the interaction between current direction and the toroidal magnetic field. Figures 8-1, 8-2, and 8-3 show one of the TF coils. Each figure illustrates the sense and magnitude of one of the magnetic force components: radial, axial, and azimuthal. The azimuthal component produces the large overturning moments. These large forces have been dealt with successfully by other tokamak designers. Here the problem is even more severe due to the compactness of the coil geometry and the high magnetic fields required for ignited plasma performance.

8.2. THE COLD ISLAND

TIBER employs superconductor for both TF coils and all poloidal field (PF) coils including the center post coils. Since temperatures below 5 K are necessary for operation of all the coils it is essential to thermally isolate all coil cases from the tokamak's vacuum enclosure, which is essentially at room temperature.

Figure 8-1. TIBER toroidal field coil magnetic force (Y or azimuthal component only).

It is not practical to absorb the large coil forces by bracing each coil case against warm structural supports. The heat flow path thus introduced would overwhelm any 5-K refrigeration plant of reasonable cost. Our solution is to develop a "cold island" of structure and magnets capable of holding the coils in an undeformed condition while minimizing the heat transfer to the "island" from the environment.

The "island" consists of

1. A large plate-like platform with a rigidly mounted center post and eight legs (Fig. 8-4).

Figure 8-2. TIBER toroidal field coil magnetic force (Z component only).

2. The small-diameter PF coils and the "pusher coil" between them with appropriate spacers. These three coils fit over the center post.
3. The sixteen TF coils one of which is shown in Fig. 8-5.

Figure 8-3. TIBER toroidal field coil magnetic force (X component only).

4. The large-diameter PF coils and their connecting structure. These all attach to the outboard vertical legs of the TF coils.

Figure 8-6 shows that assembly. We call it here the "fence."

5. The intermediate-diameter PF coils at the top and bottom of the coil array.

Figure 8-4. Coil support platform for "cold island" in TIBER.

Below the plate-like disk eight pedestal legs extend with an attachment point for a turnbuckle hanger at the very bottom of each leg. Eight rods each 2.5 m long, and adjusted to share the load, suspend the "cold island." The pedestal legs extend into a basement whose warm walls form the lower portion of the vacuum containment. These eight rods serve as the primary structural connection with the environment. Earthquake stability is provided by snubber rods in a horizontal plane. In normal service the clevis pins of the snubbers would be thermally insulated and would not contribute more than 1% to the normal heat leak.

Figure 8-5. One of the sixteen toroidal field coils for TIBER.

Figure 8-6. TIBER coil locations.

Jacks would temporarily support this "cold island." After installation of all magnets and adjustment of the suspension turnbuckles the jacks would be lowered allowing the turnbuckles to take the full weight of the "cold island" assembly.

8.3. PF COIL REMOVAL

Establishment of this "cold island" creates a problem with potentially serious implications for assembly and maintenance. The two large-diameter

poloidal coils surround the sixteen TF coils. One PF coil plane is 1.5 m above the plasma centerline and the other is 1.5 m below the centerline. Their presence traps the TF coils and all coil shield segments, preventing radial motion away from the center post.

These two large PF coils must be moved to allow TF coil or shield replacement. We have joined those PF coils by a heavy structural ring so the assembly forms a "fence" around the TF coils. This "fence" is almost 4 m high, nearly 12 m in outside diameter, and averages about 0.15 m in average thickness. It would weigh about 343 tonnes. Vital to its function is the tapered inside bore of the "fence". (See Fig. 8-6). When in operational position this "fence" is engaged with the coil case of each of the sixteen TF coils. Several large tapered dowels (driven by gear-motor assemblies mounted on the "fence") engage both the upper and lower halves of the TF coils. These large dowels have two purposes. First, they support the weight of the "fence" as it girdles the "cold island." Second, they transmit the outer leg overturning moment to that portion of the "fence" that separates the large PF coils. This applies a shear load to the center of the "fence" (i.e., the top half tries to turn clockwise around the center post while the bottom half tries to turn counterclockwise).

When the "fence" must be removed, eight 18-in.-diameter support rods move up from the machine support area beneath the tokamak. Those rods take up the "fence" weight. The tapered dowels are then extracted by their motors. Coil current connections and cryogenic plumbing lines are disengaged. The "fence" assembly is then lowered hydraulically into an annular storage pit under the tokamak. The importance of the taper is seen.

It allows immediate disengagement of the "fence" from the TF coil cases as translation to storage begins. No close diametral tolerance of large cylindrical surfaces need be achieved. Large axial motions are possible with no drag due to machining imperfections. Figure 8-7 shows the "fence" assembly in both its operational and its stored position.

Figure 8-7. TIBER outer-poloidal-field coil-retraction method.

8.4. OVERTURNING MOMENT ON INBOARD LEGS

Our first approach to reacting the overturning moments on the TF coil inboard legs was to transmit the torque to the 1-m-diameter center post. Simple analysis made it immediately obvious that the post, while still deformed elastically, is much too small to resist more than about 10% of the total torque. Two possibilities remain:

1. A structure could be designed to span the radial distance from the inboard legs to the outer PF coils. Such a horizontal plate would transmit the inboard leg's azimuthal forces to a structure attached to the PF coil assembly (the "fence"). Hence the large torque would not be transmitted through the TF-coil upper case. If the upper case had to transmit that load, its mass and dimensions would impair access of the neutron shield. As described later, that shield must be assembled radially between the TF coils. We have tried to limit the TF-coil case width to about 0.5 meters.

The base of the machine acts as the horizontal plate transmitting the TF-coil overturning moments on the bottom half of the tokamak. A similar structure might span the top of the machine. It would be massive and would have to be removed to allow TF coil replacement or replacement of the intermediate PF coil directly above the plasma.

2. The inboard legs of the sixteen TF coils can be mechanically joined in such a way as to transmit vertical shear between adjacent coil cases. By interlocking that closely spaced group of coil cases a cylinder is formed with a very large polar moment of inertia. Preliminary analysis revealed that with about 3 cm of coil case thickness both inboard and outboard of the vertical leg of the TF conductor bundle, the "cylindrical" structure could restrain the magnetic forces. The predominant force has two major components. A large radial force component directed inward toward the center post creates a crushing pressure of about 62 MPa (9.3 ksi) at the outside surface of the pusher coil and its adjacent spacer rings. A comparably large force tries to rotate the "cylinder" about the center post. The sense of that force changes so the lower half of the "cylinder" experiences a torque opposite to that experienced by the upper half. The magnitude of that azimuthal force varies with distance from the midplane.

The latter approach is believed to be a more economical design. Maintenance and coil replacement are much more easily accomplished without a torque structure blocking crane access to TF coils and shield segments. Our work so far indicates that joining adjacent TF-coil inboard legs will result in a good torque-reacting structure. In a later section of this chapter we describe the means of locking together adjacent coil cases.

The analysis of this complex structure and equally complex set of magnetic loads must use finite-element analytic techniques. The results

presented here divide the structure into the number of elements that could be dealt with by available time and manpower. The conclusions must be regarded as tentative until a more elaborate and detailed array of elements can be generated and analyzed by the GEMINI¹ code.

GEMINI evolved from the early SAPIV. It was developed at LLNL for use with CDC-7600 computers. Since then a version for the CRAY was developed and that is the one we used. It calculates element stresses as well as nodal displacements. Both dynamic and static analysis can be performed.

In order to perform the finite-element stress analysis it is necessary to know the magnetic loads on the several coils. The code employed, EFFI,² was developed at LLNL and has been refined and used extensively for the past decade at magnetic fusion research laboratories throughout the United States. The curved sections of the TF coils were broken into chordal elements that coincide with the node locations used in the GEMINI code.

8.5. THE UPPER COIL CASE

So far we have described the load-carrying elements for the inboard legs of the TF coils, and another structure for the outboard legs. Because of symmetry about the horizontal midplane the sum of the torques for both structures reduces to zero. But the top and bottom legs of the TF coils also experience substantial magnetic loading. The upper leg might be visualized as a beam spanning the gap between the inboard structure described before as the "cylinder" and the outboard structure called the "fence." We have analyzed the hollow box beam formed by the coil case. The

end conditions for that beam were described to the GEMINI code as spring constants for the three translational degrees of freedom of each node. The only nodes for which spring constants needed definition were those that describe the two end planes. The box beam so analyzed is curved and is about 2.3 meters of arc length. More moment of inertia was required to handle bending moments. For ease of assembly we maintained the overall coil-case width at a constant value and instead increased the outside wall's thickness because no space restrictions existed there.

Exactly the same magnetic force operates on the bottom leg of the coil. The coil case sits on the pedestal in that region. The coil case has been extended to form a flat bottom surface. Stresses calculated for the top leg will be higher than those for the bottom leg, due to the extra thick outer case on the bottom. Therefore we analyzed only the top leg.

8.6. THE SHIELD

As in all fusion machines a neutron shield is required. Here it protects the coils from neutron damage and also from neutron heating. Due to the large amount of energy absorbed by this shield it must operate warm and must be actively cooled, probably by water. Obviously this shield must be inside the bore of the TF coils, which are at 5 K. The weight of the shield is close to 900 tonnes and must be born by the "cold island" support pedestal. Radiation shields between the neutron shield and the superconducting magnet cases are essential. Since the neutron shield mass must be carried on the top surface of the pedestal plate, a very good load-bearing material must provide superior thermal insulation between the warm

shield and the "cold island." The load must distribute to an annular ring of about 4 m outside radius and 3 m inside radius. Only about two-thirds of that area is available for shield support due to the presence of the TF coil bases. A material capable of supporting 5.6 kg/cm^2 (80 psi) while retaining exceptionally low thermal conductivity will be required. Such materials exist, but further development will reduce their conductivity and decrease the size of the helium liquefaction facility. One example is the LI-900 insulation developed for the NASA Space Shuttle. At its lowest density of 0.08 kg/cm^3 it supports 2.8 kg/cm^2 (40 psi). It is also available in densities up to 20 lb per cubic foot.

8.7. INSTALLATION OF NEUTRON SHIELD

The shield consists of 32 major segments. The largest is a rectangular prism with a large D-shaped hole through it. Sixteen of these slide in between the TF-coil outboard legs. One slips radially inward (toward the post) until it nearly contacts the inboard legs of two adjacent TF coils. These segments do not protect the outboard legs of the TF coils. Each TF coil must have within its bore, adjacent to the outboard leg, a lunar-shaped segment of neutron shield. Figure 8-8 illustrates the two types of neutron shield segments and the way they fit together.

During assembly it will be necessary to first install the smaller (lunar) segments. They must be translated radially inward between the TF coils. Once in far enough, they must be moved azimuthally so each segment is centered to protect the outboard leg on one TF coil. Their weight can be

Figure 8-8. Wedge shield module in line with TF coil.

supported by attachment to the TF coil through long, low-conductivity support rods. After installing the lunar segments, the larger prisms can be moved in radially as previously described.

It may prove desirable to transfer the support of the lunar segments to the two adjacent prism-shield segments and thence to the support pedestal. Whichever method can be accomplished with the smallest heat loss to the "cold island" will be preferred.

8.8. THE VACUUM ENCLOSURE

We have already mentioned the two basement structures that form the lower vacuum boundary. One encloses the eight pedestal legs that support the "cold island." The second is an annular trench into which the "fence" (PF coil assembly) is retracted at times of TF coil and/or shield segment replacement.

The tokamak itself sits on the pedestal plate slightly above the containment building's main service floor. A stainless steel "bell jar" fits over the entire machine to form the vacuum boundary above floor level. This structure is best described as a cylindrical wall with sixteen equally spaced, large access doors. Closing the top end of this 13-m-diameter cylinder is a domed cover. This cover must be removed to allow installation or replacement of a TF coil.

We imagine the building will enclose some sort of polar bridge crane. It will be necessary to have a lay-down site next to the experiment for this single large cover plate. The lay-down site need not be at floor level.

The sixteen doors are spaced exactly between adjacent TF coils. The radial movement of the shield segments is through those doors. The TF coils will have to move radially to be disengaged from the shear connections joining their inboard legs. The "bell jar" is large enough to allow about 1 m of TF-coil radial motion. A TF coil is removed vertically by crane. A TF coil storage frame on the main floor would be needed to lay down such a coil.

8.9. COMBINED STRESSES IN THE TF-COIL INBOARD LEGS AND THE CENTER POST

8.9.1. Thrust on Center Post

The EFFI-code force output for the inboard TF coil legs is listed in Table 8-1, and Fig. 8-9 explains the conductor segment labeling used.

The radially inward force on the inboard TF coil leg creates a uniform pressure of 63.9 MPa (9,270 psi) on the small PF coils, the "pusher coil," and the spacer rings.

Those coils and spacer rings must be dimensioned and shimmed to make a close fit on the post. This eliminates induced shear and limits the radial stress in the post, as well as in the coil cases and spacer rings, to 63.9 MPa. If the post were not present the tangential stress at the coil and spacer bores would be 176 MPa and induced shear would be 88 MPa.

8.9.2. Torque on Inboard Legs

The inboard leg of a TF coil has the cross section illustrated in Fig. 8-10. There are 192 conductors but their presence was neglected in making the stress calculations, since their structural relationship to the coil case is not yet well defined.

Simple torsion calculations were made to predict approximate shear in the TF coil case at the tokamak midplane. They neglect any restraint from the top/bottom TF-coil case so the stress calculated is higher than the one obtained if the whole system could be included. The total magnetic torque

Table 8-1. EFFI force calculations for the inboard leg of a toroidal field coil.

Segment	F_x neutrons (radial)	F_y neutrons (azimuthal)	F_z neutrons (axial)
1	-4.5789 E6	1.7836 E6	1.2781 E4
2	-4.5797 E6	1.8160 E6	1.0817 E4
3	-4.5814 E6	1.9306 E6	8.8533 E3
4	-4.5829 E6	2.1883 E6	6.8881 E3
5	-4.5839 E6	2.3444 E6	4.9212 E3
6	-4.5846 E6	1.6632 E6	2.9531 E3
7	-4.5849 E6	5.6255 E5	9.8445 E2
8	-4.5849 E6	-5.6255 E5	-9.8445 E2
9	-4.5846 E6	-1.6632 E6	-2.9531 E3
10	-4.5839 E6	-2.3444 E6	-4.9212 E3
11	-4.5829 E6	-2.1883 E6	-6.8881 E3
12	-4.5814 E6	-1.9306 E6	-8.8533 E3
13	-4.5797 E6	-1.8160 E6	-1.0817 E4
14	-4.5789 E6	-1.7838 E6	-1.2781 E4

Figure 8-9. Segment identification for force data in table.

Figure 8-10. Toroidal-field-coil inboard leg showing conductor location and case walls.

is 2.3×10^8 N·m. The shear stress at the machine midplane was shown to be 347 MPa, a reasonable stress level for type 304 LN steel at 4.5 K.

GEMINI uses the Von Mises criteria for failure (also known as the "maximum distortion energy theory"). It has gained general acceptance as applying to both brittle and ductile materials. The effective stress (S_f) calculated for this complex array of TF-coil legs, radial spacers, and post is 607 MPa psi. This is higher than the 460 MPa usually used for 304 LN

stainless steel at a temperature of 4.5 K. We intentionally use the larger number to acknowledge metallurgical developments that are inevitable between now and the year 1990 or beyond, when TIBER might be constructed. If such improvements are late in arriving, we have space available on the side of the coil cases facing plasma, i.e., the bore of the TF coils, to thicken the coil cases, and thereby reduce the effective stress.

A small penalty is paid for thicker coil cases. The amount of structure at 4.5 K increases. Since that particular structure acts as part of the superconductor's neutron shield, the heat load on the cryogenic system will increase as much as 10%. This is deemed an acceptable price.

The model we used for GEMINI is shown in Fig. 8-9. This was repeated sixteen times to form a body of revolution about the Z axis. The distribution of TF-coil forces causes considerable variation in effective stress along the length of the inboard leg of the TF coil. We illustrate that variation in Fig. 8-11, where only Von Mises effective stresses are plotted. The reason that two values (S_F^+ and S_F^-) are shown is that calculations for both sides (i.e., the side facing the superconductor and the side visible to an observer) of the plate are made. The difference is small. We have plotted not only values for the TF coil case but values for the post and for the pusher coil "washers" as well. The latter are seen to be lower than the case stress value. The S_F of 534 MPa for the pusher coil "washers" indicates that the number and thickness assumed are nearly optimum. There would be nine discs, each 1 cm thick, equally spaced along the length of the pusher coil.

Figure 8-11. Von Mises stress at important points in TF coil, spacer, pusher coil, and center post.

8.9.3. TF-Coil Shear Connections

The greatest challenge in assembling the TF coils is to join them near the center post in a manner that permits shear stresses to be transmitted between adjacent coil cases. The tendency to "overturn" is thus resisted. Figure 8-12 shows two methods for locking together adjacent coil cases. In

Figure 8-12. Two methods for locking the TF coils together.

each illustration the viewer is looking radially inward toward the center post, between two TF-coil cases. It is easily seen that to put the leg in static equilibrium large shear forces must be exerted on the vertical sides of the coil case. One solution, which also helps the reader visualize this requirement, is to put rack gear teeth on adjacent faces of neighboring coil cases. Such teeth would not permit sliding between those surfaces as the individual coils attempted to topple (overturn). We considered such a toothed surface but concluded the machining accuracy required on large steel assemblies would be too costly. Instead we suggest a method producing an effect that is similar but much simpler to manufacture and assemble.

A number of pilot semicircular grooves would be machined in the case sides. The centerlines of the grooves would have to point near the centerline of the post after all the TF coils were in position. Adjacent case sides would have mirror-image grooves and, if alignment were perfect, a

pin could be inserted in the "hole" so formed. That level of precision is certainly too costly and probably impossible. Instead we would rough-machine the grooves to about 75% of their final diameter. After locating the TF coils against the post, the holes would be enlarged and reamed using the small and mismatched grooves as a "pilot hole." Calculations of shear indicate that twenty 2.5-cm-diameter dowels at each case interface would develop the needed balancing shear force at a stress level of 330 MPa. As no other loading exists on the dowels this should be a tolerable stress level. The dowels can not be much larger without breaking through the case wall. A leak in that location would allow helium coil coolant into the plasma vacuum environment.

Machining for these dowel "teeth" would be done with the coils in final position by a special machine tool. The dowels would have to protrude to allow gripping them for removal. Removal of a TF coil would be accomplished after removal of all shear dowels in contact with the coil being changed. Some consideration has been given to using tapered pins in tapered, reamed holes. The removal problem would be greatly relieved.

8.9.4. Torque on Center Post

The total twist created by the magnetic forces exerted on the column formed by the sixteen legs of the TF coils is about 0.2° . If the center post were somehow connected to the TF coil legs, that post would also twist 0.2° . A simple calculation shows that the torsional shear stress produced in the post is 77 MPa and the torque related to 0.2° angular deformation is

only 6% of the torque being resisted! We concluded that any attempt to make the center post share the torque load is unjustifiable. Such a structure would call for costly splines parallel to the post centerline. They would have to be machined on four places: the nose of each TF coil, the outside of both spacer sleeves, the inside of both spacer sleeves, and the outside surface of the post at two locations. At this juncture we prefer to let the center post serve two functions only: resist the TF leg radial force, and locate the PF coils and "pusher coil."

8.10. COMBINED STRESSES IN THE UPPER TF-COIL CASE

The upper TF coil case makes a bridge-like connection between assemblies of inboard-leg/center-post assembly and the outer PF-coil assembly. There are sixteen coils, hence sixteen "bridges." A similar geometry exists at the bottom of the TF coils. The TF coils are attached at their bottom to a support platform. At that point the TF coil case is radially deeper and the platform structure further reinforces the coil case. We therefore analyzed only the upper "bridge" for this report.

It was necessary to add considerable cross-sectional area to the coil case in this "bridge" area in order to withstand the bending loads applied by the magnetic forces. The case was deepened radially to nearly double its usual dimension. The material was added on the back of the conductor bundle (as viewed from the plasma), though this choice was not forced.

Use of the GEMINI code required the end nodes of this "bridge" structure to have spring loads applied to them. These spring constants must

be representative of the structure to which the "bridge" is joined. At this stage of design we had to approximate the area and length of material joined to particular end nodes. Using estimated areas and lengths it is possible, with known values for the elastic modulus, to calculate equivalent spring constants in the local orthogonal coordinate system. This procedure leads to quite different results from those assuming built-in end conditions for the "bridge" ends.

Eventually it will be possible to model the entire coil and structure system for the TIBER machine. Then such spring constants will not have to be estimated as the GEMINI code will provide self-consistency throughout a mesh array that defines the whole system. It is necessary to estimate boundary conditions only when just a portion of the structure is being defined.

The highest stressed region is along the case surface facing the plasma, near the connection to the side walls. We find the maximum effective stress to be 607 MPa on the surface of the plate, as previously discussed. There are some elements of structure not taken into account by this simplified analysis. We are confident that our model of the entire system will show that actual stress levels are somewhat lower.

It should be mentioned here that the contribution of the steel present within the conductor cross section has not been taken into account. This steel will probably be in the form of hollow, square steel tubes containing the superconductor and flow area for forced flow liquid helium. (This is commonly referred to as the "rope-in-a-pipe" design.) The effect of this additional steel is to increase the moment of inertia of the typical coil-

case cross section by about 15%. This will further reduce the operating stress level.

8.11. COMBINED STRESS IN THE LARGE PF-COIL SUPPORT STRUCTURE

Two large-diameter, poloidal-field coils encircle the TF coils. They are in planes 1.5 meters above and below the horizontal midplane of TIBER. We have previously described how a cylindrical structure (dubbed the "fence") joins these two PF coils. The "fence" is used to react the overturning torque on the outer legs of the TF coils. It also integrates an assembly that is lowered into a parked position for TF coil or neutron shield removal.

Loads on the structure arise from two major sources: First, from the magnetic forces on the two PF coils and second, from the magnetic force on the outer legs of the TF coils. The TF coil cases are not very wide. In this simple analysis we neglect their strength to resist overturning moments. A more complete analysis using finite-element techniques will show the load sharing capability of those TF coil cases.

Table 8-2 shows the relative magnitudes of the magnetic forces on the outer PF coil connecting structure.

Table 8-2. Magnetic force magnitudes on outer PF-coil connecting structure.

Force direction	On TF Coil	On PF Coil
Radially outward (a bursting force putting the "fence" in circumferential tension)	$62 \times 10^6 \text{ N}$	$249 \times 10^6 \text{ N}$
Axial (putting the midplane of the "fence" in compression or tension)	$302 \times 10^6 \text{ N}$	$-60.5 \times 10^6 \text{ N}$
Torque shear (the top half of the "fence" twisting clockwise while the bottom half twists counterclockwise)	$0.87 \times 10^9 \text{ N}\cdot\text{m}$	0

If one assumes a wall thickness for the "fence" of 15 cm the various midplane stresses can be calculated. They are as follows:

Circumferential tension due to burst forces	252 MPa
Axial tension	50 MPa
Torsional shear	72.5 MPa

The Von Mises stress, * often used as a failure criteria, is 231 MPa. This is conservative for type 304 LN stainless steel at the operating temperature of 4.5 K.

We believe our analysis demonstrates the feasibility of this design approach. Stresses are higher than normally employed, but not out of reason. Should new materials be late in development, there is still room for compromises by adding more steel at the expense of added thermal loading on the magnet refrigeration system. We also have looked at alternate design approaches that seem more costly now, but are reasonable design alternatives.

$$* \text{Von Mises stress} = \sqrt{S_{\max}^2 - S_{\max} S_{\min} + S_{\min}^2} .$$

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9.0. CRYOGENIC SYSTEM (D. S. Slack)

9.1. INTRODUCTION

The TIBER Mission-II cryogenic system is of a type that is well developed. Examples are the Mirror Fusion Test Facility (MFTF) A and B systems at Lawrence Livermore National Laboratory (LLNL) that already employ many of the system components discussed here. One advantage of using proven hardware is a sound cost basis that is less subject to engineering-development and other variable costs.

Obtaining this equipment could use the business approach that was successful in helping MFTF-A and B meet cost limitations and scheduling deadlines. In this approach the contracting laboratory writes a performance specification for system subcontractors able to build both the refrigerators and distribution systems. This allows the construction subcontractor to do systems integration within the cryogenic system. Systems to be integrated at the contracting laboratory level are then limited to interfaces between the cryogenic supplies and the TIBER core. This integration can be performed by one or two project engineers within the laboratory, thereby eliminating the need for more extensive management teams.

We began by considering the requirements of the TIBER coils. These coils were under the constraints of the physics objectives. Most of the design effort went to the TF coils because they had high heat loads, complex shapes, etc. It was necessary to weigh design and cost considerations for the coils and cryogenic systems. Generally, a simple coil configuration was

sought at the expense of a complex cryogenic system. For example, by using parallel paths and connections, we were able to simplify TF coil construction, but only by increasing the delivery pressure required by the cryogenic system.

We considered both a cryogenic system capable of steady-state operation and one operating with 300- μ s pulses. The former was selected because it offers a substantial increase in TIBER physics capability with only a modest increase in cost.

This section contains the following information:

- Performance specifications for the cryogenic system sufficient to form the basis of a specification for prospective subcontractors.
- Design and analysis of the cryogenic system.
- Thermal and pressure drop analysis of the magnet loads.
- Definition of interfaces between the cryogenic and other systems.

9.2. COIL DESIGN AND ANALYSIS

9.2.1. TF Coil - Neutron Heat Load

Neutron heating of the TF coils places the most significant load on the cryogenic system. Size constrains the inner leg of the TF coil shielding to be thin (45 cm) resulting in heat loads up to 22 mW/cm³. Shielding between the plasma and the outer leg of the coil is significantly thicker. The coil composite also serves as a shield so that neutron heating on turns nearest the plasma is greatest. The results of the calculations

that follow are summarized in Table 9-1, which shows how neutron heating decreases in turns further from the plasma.

Table 9-1. Neutron heat distribution in TF coil.

Turn number	Neutron heating (kW)
<hr/>	
1	10.66
2	8.10
3	6.19
4	4.69
5	3.58
6	2.73
7	2.08
8	1.56
9	1.20
10	0.91
11	0.69
12	0.52

For cryogenic-system design purposes, the total heat load to each turn of the coil and resultant temperature at the end of the turn is calculated. This heat load is used with the field intensity at the turn to determine the temperature margin (cryostability) of each turn. The TF coil parameters used in these calculations are given in Fig. 9-1. Heat disposition in each coil was determined by integration.

Figure 9-1. TF coil parameters. The neutron heat to TF coils is $Q_{NT} = 43$ kW for the entire machine. The coil radiation heat attenuation parameter (e -fold distance) is $a = 8$ cm.

Solving for temperature at the end of each turn requires an iterative procedure due to the strong interdependence of the pressure and enthalpy. A calculator code provided a sufficiently accurate solution for preliminary design purposes. The results, plotted in Fig. 9-2, are based on the following coil geometry:

16 coils/machine

20 paths/coil

12 turns/path wound in pancake from coil inner diameter

to outer diameter

1.14-cm² flow area path

0.06-cm hydraulic diameter

43-kW total neutron heat absorbed

and the coil heat distribution given in Table 9-1. A 2100-g/s flow rate maintains the coil temperature below 6 K (conductor current sharing does not start until the temperature is above 7.5 K). This temperature difference leads to ample stability so the refrigerator is able to provide 2100-g/s flow for the TF coils.

Notes	Flow (g/s)	P _{in} (atm)	P _{out} (atm)	Coil design
A	2100	3.0	0.8	2 (12 turns/path)
B	2100	3.5	1.8	2 (12 turns/path)
C	4000	3.0	2.5	1 (6 turns/path)

Figure 9-2. TF-coil temperature distribution.

It is noteworthy that peak temperatures in the coil are not at the outlet, but instead are nearer the middle of the cryogen path. This is due to the relatively low heat load on the outer turns and to the Joule-Thompson expansion of the helium within the coil.

9.2.2. TF Coil - Thermal Heat Load

Room-temperature radiation (called thermal radiation) and conduction can be estimated by making a comparison to measured 1.50-W/m^2 heat loads of the MFTF-A magnets.

The room-temperature radiation surfaces on the inner or plasma side of the coils have an approximate total area of 30 m^2 . A conservative heat-load estimate is 1.50 W/m^2 for heat from LN_2 shields, from the supports due to thermal conduction, and from additional room-temperature radiation. This estimate assumes that there are five layers of 0.3-emissivity, foil reflectors. The additional heating in the absence of an LN_2 shield is 730 W.

Not having an LN_2 shield between the magnet case and the shielding on the inner leg greatly increases heating to the magnet. However, this heat load is still small compared to the overall cryogenic limit, and the elimination of LN_2 shielding on the inner leg saves space in this critical area. But LN_2 shielding will be used for the remainder of the coil surfaces to give a heat load of $1.5\text{ W/m}^2 \times 200\text{ m}^2 = 300\text{ W}$. The total thermal heat load to the TF coils is therefore $730\text{ W} + 300\text{ W} = 1030\text{ W}$.

This conduction and radiation heat load is almost insignificant compared to neutron heating. Nevertheless, it has been incorporated in the TF-coil temperature plot (Fig. 9-2) for completeness.

9.2.3. Pusher Coil - Neutron Heat Load

The neutron heat load on the pusher coil is much less than that on the TF coils because the TF-coil shielding is effective and the pusher coil's area exposed to the plasma is smaller. Calculations of TF-coil heat load showed that 1.7 kW of neutron energy exits the TF coils, and that a portion of this energy enters the pusher coils. Estimating this amount from two-dimensional scalings of the coil geometries while accounting for the higher neutron flux on the inner leg gives an approximate value of 500 W for the neutron heat load.

9.2.4. Pusher Coil - Thermal Heat Load

Most of the 1.8-K pusher-coil surface are exposed to the 4.5-K surfaces of the TF coil casings and the TIBER center post. A smaller portion of the coil is exposed to 80-K surfaces on the top and bottom of the cryostat. Consequently, the radiation heat load to the pusher coil is only a few watts and can be neglected. The conduction heat load from the surrounding 4.5-K structures is significant because interconnecting structures must be strong enough to transmit TF coil forces to the center post. This conduction heat load is estimated to be 320 W. Table 9-2 summarizes the pusher-coil heat loads.

Table 9-2. Pusher-coil heat-load summary.

Source	1.8-K heat load (W)
Pusher coil (radiation)	10
Pusher coil (conduction)	320
1.8-K piping (nitrogen traced)	70
Neutrons (steady-state operation)	500
Total	900

The pusher coil can be supplied by a forced-flow loop using the main compressors to move 1.8-K helium to a Dewar under the TIBER core. Conduction through the helium pipes at 1 atm will cool the coil.

9.2.5. PF Coils - Neutron Heat Load

There is much less neutron heating to the PF coils than to the TF coils due to a lower view factor and more effective shielding from the plasma. This heat load has been calculated to be 1900 W. Because the neutron heat load to the PF coils is a small percentage of the total, this estimate is sufficiently accurate for cryogenic system design.

The amount of thermal heating to the PF coils can be readily ascertained from data and measurements for existing coils, such as those used in MFTF-A and B. Heating mechanisms of the PF coils and existing coils are essentially the same: radiation heating from iN_2 -cooled surfaces and conduction heating from supports, lead streams, diagnostics, etc. These comparisons lead to a total calculated PF-coil thermal heating of 900 W.

9.2.6. Conductor-Lead Cryogenic Loads

The transition of the magnet current conductor from cryogenic temperature to room temperature is brought about by counter flowing helium along the conductor. Thus the refrigeration system must also serve as a liquefier to replenish helium returned to room temperature by way of the magnet leads. Experience with MFTF-A and B has shown the required flow rate to maintain conductor lead stability to be 0.11 g/kA per pair of leads. TIBER current lead-stream-flow requirements are shown in Table 9.3. Such helium flow requirements are easily satisfied by conventional refrigerator design. Table 9-4 summarizes the total TIBER cryogenic requirements, which form the basis of the following cryogenic system design.

Table 9-3. Lead stream flow requirements.

Magnet	Quantity	Current each (kA)	Lead stream flow (g/s)
TF	16	20	35.2
PF	4	20	8.8
Pusher	1	10	1.1
Total			45.1

Table 9-4. Summary of coil cryogenic requirements.

Coil	Total load (kW)	Neutron load (kW)	Thermal load (kW)	Required flow (g/s)	Inlet temp (K)	Outlet temp (K)	Inlet pressure (atm)	Outlet pressure (atm)
TF	45	43	2	2100	4.5	5.5	3.5	1.3
Pusher	1.0	0.5	0.3	N/A	1.8	1.9	1.0	1.0
PF	2.8	1.9	0.9	130	4.5	5.5	3.5	1.3

9.3. CRYOGENIC SYSTEM

9.3.1. General

The cryogenic system, designed to meet the coil requirements of Sec. 9.2, consists of the following:

- Refrigerator-compressor system for 4.5-K nominal operations.
- Refrigerator-compressor system for 1.8-K pusher coil.
- Storage Dewars for ^4He .
- Distribution system.
- Helium recovery system including a compressor purifier, gas bag, and high pressure storage.
- Liquid-nitrogen storage Dewar and distribution system.

For all of these components, a specific, conservative design that includes reserve capacity is proposed here for accurate costing. The main cryogenic plant can be located some distance from the TIBER core. Thus, valuable space near the core is not required. The entire cryogenic flow to the core can be supplied by a relatively small (3- to 4-in.) helium supply and return line. Similarly sized lines for 1.8-K helium and liquid nitrogen can be used. The ability to remotely locate the cryogenic equipment is an additional advantage of the forced-flow TF-coil system.

Figures 9-3 and 9-4 are system schematics for the 4.5-K and 1.8-K systems, respectively. Refrigerators are shown in more detail in Figs. 9-5 and 9-6, and an overall cryogenic-system block diagram appears in Fig. 9-7.

9.3.2. Helium Refrigerator System (4.5 K)

The 4.5-K helium refrigerator contains heat exchanger, expanders, valving, and a bank of compressors to provide helium flow to the refrigerator and cryogenic loads (magnet coils). This design scales up the MFTF-B system by a factor of 5 in order to achieve the required 50-kW capacity and 2300-g/s flow to the magnets. This is accomplished by proportionally increasing the number of parallel compressor units in the first and second stage, and increasing the size and number of refrigerator heat exchangers and turbines.

Figure 9-3. Schematic of 50-kW, 4.5-K system for TF and PF coils.

Figure 9-4. Schematic of 1-kW, 1.8-K system for pusher coil.

Figure 9-5. Detailed schematic of 50-kW, 4.5-K refrigerator.

Figure 9-6. Detailed schematic of 1.8-K, 1-kW refrigerator.

Figure 9-7. Block diagram of overall TIBER helium cryogenic system.

Our reasons for modeling the system after MFTF-B are the following:

- The operating efficiency of the system is close to optimum. That is, helium from the compressors is expanded almost entirely by turbines (not Joule-Thompson valves) to provide a high refrigeration-to-compressor power ratio.
- The system is fully engineered and tested. Consequently, minimal design cost and risk are involved.

The refrigerator schematic in Fig. 9-5 gives significant state points, and Table 9-5 lists the major components.

9.3.3. Helium Refrigerator System (1.8 K)

The 1.8-K refrigerator system, like 4.5-K system, is modeled after an existing and proven system. In this case, the model was a 1.8-K, 300-W unit at Stanford's High Energy Physics Laboratory. The unit, built in 1975 by A.D. Little, Inc., provides refrigeration for a rf accelerator cavity. It has been in operation for about 30,000 hours at 1.8 K without significant problems. The system employs about 600 feet of 1.8-K transfer line (about the same length required here). It is about 1/3 the size required for TIBER.

Some concerns about using 1.8-K helium (helium II) have been put to rest since this unit began operation. First, superfluid leaks caused by the near-zero helium-II viscosity have been found to be no more of a problem than leaks with regular helium. Second, the problem of air leakage into the

Table 9-5. Equipment list of 50-kW, 4.5-K refrigerator system.

First-stage compressor

15 each Howden Model 327 screw compressors with 400 HP drive in parallel, or equivalent number of larger units.

Second-stage compressor

10 each Howden Model 321 screw compressors with 2200 HP drive, or equivalent number of larger units.

Turbine number 1

5 each L'air Liquide Model C6 or equivalent number of larger units.

Turbine number 2

5 each L'air Liquide Model C5 or equivalent number of larger units.

Turbine number 3

5 each Rotoflow or equivalent number of larger units.

Oil removal equipment

Three stages of Monsanto/Brinks cooling filters sized for 10-ft/min flow velocity plus charcoal and particulate filters.

Heat exchangers

Made by Train Inc. or Stewart Warner, with equivalent UA and longitudinal heat conduction per unit flow as MFTF-B units.

Note: Train Inc. has the ability to make larger sizes permitting one or two parallel paths of heat exchangers.

Cold box

The cold box containing heat exchangers and associated plumbing is a single vessel. Because of its larger size it can be mounted horizontally with the heat exchangers inside mounted vertically to reduce longitudinal heat conduction.

Additionally, the unit can be placed at ground level to eliminate the need for structures, dome lifting equipment, etc.

system at the 10-torr compressor return pressure was solved using a rather simple overpressure oil seal on subatmospheric compressor shafts.

The 1.8-K system for TIBER will furnish 1000 W to the pusher coil. Refrigeration is provided by a 10-g/s bleed stream of 4.5-K liquid helium from the main 50-kW system plus a low temperature expander between 7 K and 4 K. The low pressure (10-torr) return gas is boosted to one atmosphere by three stages of Roots (lobe-type) compressors plus one screw compressor stage. The 4.5-K, 50-W refrigerator compressors will then boost pressure to 20 atm. Interconnecting the 4.5-K and 1.8-K systems at the compressors eliminates the need for flow balancing controls between the two systems and reduces the amount of machinery required. The refrigerator schematic is shown with significant state points in Figs. 9-6 and 9-7. Table 9-6 lists the major components.

Table 9-6. Major equipment list for 1.8-K refrigerator system.

First three compressor stages

Roots (lobe-type) compressors with overpressure oil seals on shaft, 150 HP per stage.

Fourth stage compressor

Screw type (Howden or equivalent), 250 HP.

Turbine

L'air Liquide, gas bearing (may not be required with the LHe transfer rate shown in Fig. 9-6).

Cold box

Approximately 8 ft in diameter and 20 ft long.

9.3.4. Helium System

Figure 9-7 is a block diagram of the overall TIBER helium distribution system including refrigerators, recovery system, helium storage, and Dewars. This schematic, in conjunction with the associated equipment list (Table 9-7) outlines the entire helium cryogenic system. In operation, warm helium is refrigerated to 4.5 K and 1.8 K by the refrigerator-compressor systems already described. In both systems the compressors force helium through the loads and provide flow through the refrigerators. The 1.8-K system employs conduction heat transfer between the pusher coil and the adjacent Dewar.

The 10,000-litre Dewar, filled to about 2/3 capacity (6,700 litre) with 4.4-K liquid helium, will serve as a thermal reserve or ballast. If, for example, the total cryogenic load were to rise from 50 to 60 kW momentarily, the additional 10 kW could be handled for about 15 minutes by helium boil-off in this Dewar before system operation deteriorated.

The surge tank provides a gaseous (warm) helium ballast. When helium is required, for example during cool down, while filling the system with liquid helium, or when making up for leaks, helium flows from the surge tank to the compressor suction. If excessive helium is in the system, as during warm-up, helium flows from the compressor discharge back to the surge tank.

The helium recovery system completes the closed-loop helium system by transferring helium as required between the low-pressure surge tank and the high pressure storage by way of a gas bag, recovery compressor, and purifier.

Table 9-7. Major-equipment list for helium-cryogenic system.

- 4.5-K, 50-kW refrigerator system (Figure 9-3).
 - 1.8-K, 1.0-kW refrigerator system (Figure 9-4).
 - 4.4-K Dewar - 10,000 litre (1/2 size of that used on MFTF-A).
 - Surge tank - 3,000 ft³ (same as used on MFTF-B).
 - Gas bag - 10,000 ft³ (1/4 size of that used on MFTF).
 - Recovery compressor - 15 g/s, 2400 psi, 150 HP
(1/3 size of that used on MFTF).
 - Purifier - LN_2 temperature, absorption type
(same as on MFTF except 1/3 the capacity).
 - High pressure storage.
3,000 actual ft³ at 2,400 psi (1/4 volume of that used on MFTF-B). This is sufficient to store all the helium in the system and to permit warm-ups without loss of helium.
-

A magnet current lead-stream flow of 45.1 g/s is returned to the 4.5-K system compressor suction via a small lobe-type (Roots) blower with a bypass controller. The purpose of the bypass controller is to maintain a constant pressure differential across the individual bleed stream control valves on each lead. Returning this gas to the compressor suction rather than to the recovery system permits use of a smaller recovery system.

Cool down is accomplished by flowing gas through the 4.5-K refrigerator to the magnets. Initial cooling is done by the LN_2 heat exchanger in the cold box that also serves as an auxiliary heat exchanger. At about 90 K the cold box turbines are started to complete the cool down. Beginning with a room temperature system, cool down to operating conditions can be accomplished in about one day.

During regeneration, which absorbs any frozen deuterium or tritium, the magnet surfaces are warmed to about 15 K. With a forced-flow system, regeneration can be brought about easily using cold box taps at the 15-K to 25-K points, and an external electric heater, shown in Fig. 9-5.

Quench protection is achieved by using a burst disk vented to atmosphere by way of a stack near the TIBER core. During a quench, helium would flow to the burst disk through the helium return line, as shown in Fig. 9-7. This piping is sufficiently large to limit quench overpressures to safe levels, even in the event that all the magnets are quenched simultaneously. Helium from the TIBER core would be lost. Unlike earlier magnet designs the chances of a quench occurring during the life of TIBER are very unlikely. Consequently, it is economically advantageous to accept potential helium loss rather than to install equipment to capture helium.

In the event of a quench, controls will be used to isolate the magnets from the cryogenic system.

9.3.5. Liquid Nitrogen System

Liquid nitrogen (LN_2) is used in the precooling heat exchanger of the 4.5-K cold box. It is also used to thermally shield the refrigerator cold box, Dewars, piping to the TIBER core, and the TIBER core. Based on the MFTF-system scaling and adjustment for the different design configurations, we estimate that approximately 50 kW of refrigeration at 80 K will be required by TIBER. This can be supplied by LN_2 trucks, since only about one trailer per day is required during operation.

We propose a subcooler arrangement to supply LN_2 to the various loads. An LN_2 subcooler consists of pumps that circulate the LN_2 under pressure to keep the nitrogen in a liquid state throughout the loads and piping. Consequently, problems associated with two phase flow, such as boil-off in the loads, are eliminated. After each pass through the loads the LN_2 passes through a heat exchanger containing boiling LN_2 and is re-cooled to about 80 K. MFTF-A used a 100-kW subcooler and MFTF-B uses a 400-kW plus 100-kW subcooler. These designs can be scaled down for TIBER.

9.4 COST SUMMARY

The cost summary in Table 9-8 is based largely on delivered prices of hardware used on MFTF-A and B. An exception is TIBER's 4.5-K, 50-kW

Table 9-8. Cryogenic system costing.

Item	Cost (\$k)
Refrigerator/compressor system 4.5 K, 50 kW	18,700
Refrigerator/compressor system 1.8 K, 1.0 kW	1,200
Dewars	50
Piping installation	1,200
Gas bag	100
Compressor	200
Purifier	250
HP tank storage	400
Surge tank	100
Liquid nitrogen system	500
Engineering	1,200
Total	24,200

refrigerator, which differs from the MFTF-B 11-kW system. However, numerous comparisons show refrigerator costs scale as the 0.7 power of size.

9.5 CONCLUSION

The TIBER cryogenic system is patterned after the well-developed systems of MFTF-A and B. One advantage of designing around existing hardware is that the system is less subject to engineering-development and other variable costs.

Most of our effort went toward planning for the TF coils. It was necessary to weigh design and cost considerations for the coils and cryogenic system. Generally, simple coil configurations were sought at the expense of a complex cryogenic system.

10.0. SYSTEM INTEGRATION AND COSTS (J. N. Doggett)

This section includes a subsystem listing, the basis for selecting subsystem technology from other studies and costing. In the TIBER configuration study we have concentrated our effort on the areas of the system that would most benefit from our approach to reducing the cost of an ignited superconducting magnet tokamak through minimizing the size of the device. This then led to a fairly detailed look at the pusher coil, TF coils, center post and support structure. We also developed a geometry that would be relatively easy to maintain after neutron activation of its internal components. Beyond these selected subsystems, our approach has been to invoke the designs and costs developed by others in characterizing the various alternative Tokamak Fusion Core Experiment (TFCX) concepts.

10.1. TIBER WORK BREAKDOWN STRUCTURE

We have chosen to use the same Work Breakdown Structure (WBS) used for the various versions of TFCX. This arrangement, identified in Table 10-1, facilitates easy comparisons between the two studies and at the same time allows us to incorporate many of the TFCX subsystem costs into TIBER in an unambiguous way.

Table 10-1. TIBER Work Breakdown Structure (WBS).

Designator	Description
Σ	Summary
A	Project management & systems engineering
B	Energy and particle removal
C	First wall assembly
D	Vacuum vessel assembly
E	Shielding system
F	TF magnet system
G	PF magnet system
H	Tokamak structure
I	Remote maintenance
J	Diagnostics
K	LHRF ^a system
L	ICRH ^b system
M	ECRH ^c system
N	Electric power system
O	Instrumentation and control system
P	Water cooling system
Q	Cryogenic system
R	Fueling system
S	Vacuum pumping system
T	Buildings and facilities
U	Cleanup, disposal & monitoring system

^aLower hybrid radiofrequency.

^bIon-cyclotron resonant heating.

^cElectron-cyclotron resonant heating.

10.2. COMPILATION OF COSTS

Table 10-2 summarizes TIBER costs. In order to obtain useful results from the limited resources available to the TIBER study, we concentrated on those areas of the TIBER concept that were the most critical to achieving the goals of the project. These areas had mostly to do with the magnetic configuration to support the physics assumptions and with the details of coil design. In addition close attention was given to the shielding and divertor design because both have large impacts on the performance and design of the magnet system. Other areas of the conceptual design were simply based on the work of others, particularly on TFCX, as a matter of practicality.

The subsystem costs were either based on the methodology developed for TFCX, or adopted directly when we could comfortably assume equal complexities and costs for TIBER and TFCX candidate designs. The costing basis and unit quantities are also shown in Table 10-2. A more detailed breakdown and justification for these costing elements may be found in the TFCX Preconceptual Design Report.¹

We neither expected nor found a radical reduction in cost from TFCX options for TIBER. However the TIBER design does promise a higher performance option for a somewhat smaller investment than any of the four versions of TFCX. The comparable total costs are given in Table 10-3. Most of the savings are attributable to TIBER's more aggressive approach to magnet design from both a current-density and heating standpoint. These factors had a significant impact on the size (cost) of the machine. If we were to investigate other subsystems, including the conventional facilities, we probably could further reduce the total cost of TIBER.

Table 10-2. TIBER costing.

WBS	Description	Total (\$M)	Quantity	Unit	Basis
Σ	Summary	963.2			
A	Project mgmt & systems engr	93.6			8% of subsys. +\$24M-TFCX
B	Energy and particle removal	32.3	47	m ²	\$0.6875M/m ² - TFCX
C	First wall assembly				
	Inner wall	5.3	32	m ²	\$0.166M/m ² - TFCX
	Outer wall	3.6	59.6	m ²	\$0.0612M/m ² - TFCX
D	Vacuum vessel assembly	10.5	875	m ²	\$0.012M/m ² - TFCX
E	Shielding system				
	Steel	14.1	653	Mg	SS water at \$21.6/kg - TFCX
	Tungsten	27.2	225	Mg	Tungsten at \$121./kg - TFCX
	Concrete	4.8	4600	m ³	Concrete at \$0.00105M/m ³ - TFCX
F	TF magnet system				
	Winding	15.6	130	Mg	NbTi winding \$120/kg - TFCX
	Superconductor	29.3	8880000	kAmT	\$3.3/kAmT - TFCX
	Case	36.1	440	Mg	SS \$82/kg - TFCX
G	PF magnet system				
	Small PF coil				
	Winding	1.3	11	Mg	NbTi winding \$120/kg
	Superconductor	1.8	550000	kAmT	\$3.3/kAmT
	Case	0.6	7	Mg	SS \$82/kg - TFCX
	Middle size PF coil				
	Winding	1.6	13	Mg	NbTi winding \$120/kg
	Superconductor	0.5	140000	kAmT	\$3.3/kAmT
	Case	1.0	12	Mg	SS \$82/kg - TFCX
	Large PF coil				
	Winding	8.8	73	Mg	NbTi winding \$120/kg
	Superconductor	6.4	1950000	kAmT	\$3.3/kAmT
	Case	22.1	270	Mg	SS \$82/kg - TFCX

Table 10-2. TIBER costing (continued).

WBS	Description	Total (\$M)	Quantity	Unit	Basis
	Pusher coil				
	Winding	1.3	11	Mg	NbTi winding \$120/kg
	Superconductor	3.5	1060000	kAmT	\$3.3/kAmT
	Case	0.3	4	Mg	SS \$82/kg - TFCX
H	Tokamak structure	7.1	253	Mg	SS \$28/kg - TFCX
I	Remote maintenance	18.5			TFCX constant
J	Diagnostics	41.0			TFCX constant
K	LHRF system	176.9	40	MW	\$3.538/W - TFCX and 0.8 absorption
L	ICRH system	0.0			
M	ECRH system	0.0			
N	Electric power system	72.1			Estimate from TFCX costs HPSC ^a
O	Instrm. and control system	54.1			TFCX constant
P	Water cooling system	18.7	100	MW	\$M=0.47 _{0.8} (capacity in MW)
Q	Cryogenic system	25.0			MFTF estimate
R	Fueling system	13.6			TFCX constant
S	Vacuum pumping system	19.5			TFCX constant
T	Buildings and facilities	180.0			Smallest TFCX
U	Cleanup, disp.& monit. syst.	15.1			TFCX constant
	Total subsystems	869.6			
	Total system	963.2			

^aHigh performance superconducting.

Table 10-3. Total cost comparison for TFCX and TIBER.

Machine	Cost (\$M)
TFCX	
HPCU ^a	1005.78
NPCU ^b	1129.58
HPSC	1167.72
NPSC ^c	1248.14
TIBER	963.20

^aHigh performance copper.

^bNominal performance copper.

^cNominal performance superconducting.

REFERENCE

1. Tokamak Fusion Core Experiment, Cost Estimates and Summary Tables,
Princeton Plasma Physics Laboratory, Princeton, NJ,
A-Axxx-8406-012 (1984).

11.0. IMPROVED CONCEPTS FOR STEADY-STATE TOKAMAKS (J. H. Schultz)

11.1. AN APPROACH TO CONCEPT IMPROVEMENT

The TIBER concept demonstrates the possibility of an ignited, superconducting, steady-state tokamak, with a major radius comparable to those of the intermediate-size, copper-coil tokamaks, now in operation. TIBER was conceived as a demonstration experiment that would break the 3.0-m and $Q=10$ current-drive barriers simultaneously, using design innovations to reduce the fundamental limitations of superconducting tokamaks. However, its parameter set was not selected with the aid of any searches through parameter space. Parametric scans have traditionally taken the form of optimization of cost for a given performance measure. While this form of optimization is useful in confirming that a given set of parameters is within a reasonable range of cost/performance space, cost/performance optimization is frequently a bad method for parameter selection. Algorithms for cost and for physics performance generally have large, fundamental uncertainties, while even the "intermediate" algorithms for engineering allowables vs load usually have nontrivial uncertainties; the uncertainties of a sizing/costing program are the product of these uncertainties, and inevitably very large. Parametric variations frequently show cost/performance variations of $\pm 10\%$ about an optimum value, in which the increased cost and performance at the extremes is trivial in comparison with the fundamental uncertainties. The second defect of cost/performance optimization is that it tends in practice to suppress searches for multiple missions or topological improvements. As a typical example, a design with a

10% lower ignition margin that could achieve steady-state operation is judged inferior to a short-pulse design with the higher ignition margin. It is frequently more useful to look for logical breakpoints, that don't seem to be outside the "range of insensitivity" in cost/performance space, at which an enhanced mission becomes possible or an important failure mechanism is eliminated. A second approach, more difficult to achieve, is to identify an improved concept that places the mission in a totally different regime of parameter space. Examples of mission enhancements that might be possible for a TIBER-class machine include the following:

- (1) Entrance into the second critical beta regime. This might logically be easier to achieve in a machine that ignites at a lower beta and a higher aspect ratio.
- (2) Steady-state operation with slow-wave current drive. This would be desirable for several reasons. Slow-wave current drive is the only form of noninductive current drive that has been definitively demonstrated to exist. A plasma operating from the slow wave then would greatly enhance the credibility of steady-state operation. Furthermore, the slow wave has not been demonstrated to be inadequate as a reactor concept. The STARFIRE study showed that $Q > 30$ could be achieved, if the slow wave were absorbed near the edge of a plasma, a concept that is not incompatible with the current profiles of high beta plasmas. A modification of the HFCTR by Yuen and Cohn showed that high Q was achievable with full penetration of the slow wave in high-field,

high-aspect-ratio reactors. Because of the speculative nature of global plasma behavior with edge current drive, the concept has not enjoyed wide popularity, neither has it been refuted, and therefore we consider the third possibility.

- (3) Steady-state operation with both slow-wave and fast-wave current drive. This concept involves not merely two experiments, instead of one, but promises the best of both possible worlds. The slow wave is better for edge current drive and for low-beta, low-temperature plasmas. The fast wave is better for central absorption and for high-beta and high-temperature plasmas. A machine provided with both types of current drive would not have its mission endangered by confinement, pressure, or profiles that were significantly different from those expected. It is not even inconceivable, using square waveguide, to use the same rf sources, power supplies, and transmission systems for both the slow wave and fast wave, creating any combination of the two by inserting or deleting twist guides. Finally, the unprecedented control of plasma q profiles available with a combination of central current drive, edge current drive and electric field would offer the best opportunity for entering the second critical beta regime without plasma indentation or for optimizing the combined beta and transport profiles in the first critical beta regime.

Therefore, a useful purpose for a parametric study would be to examine whether there is any section of parameter space that would simultaneously

permit fast-wave and slow-wave steady-state burn, without costing significantly more than TIBER.

11.2. PARAMETRIC SCANS VS ASPECT RATIO

If elongation is fixed at the highest value felt to be controllable, the most significant variable parameter is the aspect ratio. It was decided not to hold ignition margin fixed, both because of the fundamental uncertainties in the ignition margin, and because some correlations predict that aspect ratio is good for ignition margin while others predict that it is bad. Some predict that safety factor is good and others that it is bad; some predict that size is very good, others that it has no effect. Ignition margin, then, does not appear to have the required qualities needed for the selection of size, aspect ratio, and safety factor. Furthermore, in the past year, not only have a dozen analytical correlations for ignition margin continued to hold the field, but less important controversies have arisen over whether one should include ion losses and radiation in the definition of ignition margin or whether one should use a weighted algebraic or root-mean-square combination of different expressions for the ignition margin, in case each correlation represented a parallel loss channel. At this point, with more possible definitions of ignition margin than there are atoms in the universe, we decided to run parametric scans two ways: one with fusion power and wall loading constant, and one with fusion power and maximum field at the superconductor held constant. Whereas the absolute limits on wall loading and superconductor field are debatable, there is no question that

their magnitudes are strongly constrained. Fusion power, despite all geometric variations, is still the best single parametric measure of cost, and is certainly the best single parametric measure of reactor relevance. A complete discussion of the results of the study summarized below is given in a separate report.¹

The relevance of fusion power emerged from the studies in two ways, one trivial, the other significant. The trivial relevance can be seen when wall loading is fixed, and is duplicated by the tokamak systems code at Oak Ridge. At fixed wall loading, most costs scale with the square of the characteristic dimension $D^2 = a R_0 \kappa$, because fusion power, vacuum vessel surface and mass, armor mass, limiter mass, and water requirements all scale exactly with D^2 . Here a is the plasma minor radius, R_0 the plasma major radius, and κ is the plasma elongation. Since Q is a slow function of everything, auxiliary power and launcher cost also tend to correlate strongly with D^2 . Magnet and power supply costs are not proportional to D^2 , but have a strong positive correlation with it.

Facility, site, diagnostic, and control tend to be flat and dampen other geometric dependencies. Thus, fixing D and wall loading goes most of the way toward fixing cost. The more interesting result is that fusion power P_f is seen to be significantly correlated with the wide variety of definitions of ignition margin, MI . Over a broad range of aspect ratios and sizes, if one wants to satisfy $MI=1.5$ for most of the ignition margin correlations, the fusion power must be 300 MW. The relation is nearly linear, so that a fusion power of 200 MW provides $MI=1$ for most of the definitions of ignition margin. Because both cost and performance are so

strongly related to fusion power, it is not illogical to simply design an experiment to provide as much fusion power as the program can afford once wall loading is fixed by engineering considerations.

A typical scan of reactors with fixed wall loading and maximum field at the TF coils is shown in Fig. 11-1. This figure and all figures following in Sec. 11.2 assume plasma elongation $\kappa = 1.6$, shield thickness $t_{\text{shield}} = 35$ cm, wall loading $P_w = 2 \text{ MW/m}^2$, and the minimum safety factor $q_{\text{lim}} = 2.1$. Both R_0 , the plasma major radius, and D_{char} , the plasma characteristic dimension have approximately parabolic dependences on aspect ratio, A , but the variation of D_{char} is extremely insensitive to aspect ratio, over a broad range from $2.5 < A < 7.5$. The lowest value of R_0 occurs at an aspect ratio of 3, whereas the lowest value of characteristic dimension occurs at an aspect ratio of 4. In the studies with fixed fusion power and wall loading, the characteristic dimension has no sensitivity to aspect ratio. These results agree with those of previous parametric studies, but they tend to be forgotten. It appears that

- The smallest size tokamak reactor, even with beta linearly dependent on inverse aspect ratio, is not at the smallest possible aspect ratio.
- The smallest characteristic dimensions occur at medium aspect ratio, not low aspect ratio, for superconducting tokamaks. However, tokamak characteristic dimensions should probably not be minimized for a fixed mission, because of the cost and size insensitivities.

Figure 11-1. Plasma major radius and plasma characteristic dimension vs aspect ratio. This calculation assumes a maximum field in toroidal coil, $B_{max,tf} = 12$ T.

Low-aspect-ratio tokamaks are not the smallest because superconducting tokamaks have limited maximum field, and the smaller tokamaks have a worse ratio of field at the coil to field at the plasma. This makes the low-aspect-ratio tokamaks larger than medium-aspect-ratio machines, when performance is fixed. As shown in Fig. 11-2, when maximum field and wall loading are fixed, the fusion power, relatively flat when $3 < A < 5$, rises steadily at high aspect ratios and sharply at low aspect ratios. Results similar to this led us to drop the so-called "spherical tokamak" range of aspect ratios from the study. A thorough scan with a fixed aspect ratio of 1.5 was made, but no attractive solutions were identified. Superconducting spherical tokamaks were always significantly larger than higher-aspect-ratio machines with the same engineering and performance constraints.

Figure 11-2. Fusion power vs aspect ratio. This calculation assumes $B_{\text{max,tf}} = 12 \text{ T}$.

The principal disadvantage of the higher aspect ratios is the higher stored energy in the TF coils, due to the lower beta limits. As shown in Fig. 11-3, this variation is actually faster than linear and appears to be parabolic, reflecting the fact that more field is needed at lower beta and that the machine must also get larger, when the aspect ratio is higher than optimum. The stored energy also increases more rapidly than linearly on the low-aspect-ratio side of the optimum, which occurs at an aspect ratio of about 3.

A typical scan of ignition margin, according to contradicting models of energy confinement, is shown in Fig. 11-4. This shows the well-known result that Mirnov confinement improves with inverse aspect ratio, while neoclassical confinement improves with aspect ratio. It is of a little more interest is to note that, if a conservative design approach is taken, i.e., using only the lower of the two confinement scaling laws or a root-mean-square conductivity based either on a model treating transport scalings as parallel

Figure 11-3. Toroidal-field stored energy vs aspect ratio. This calculation assumes $B_{\text{max},\text{tf}} = 12 \text{ T}$.

Figure 11-4. Ignition margin vs aspect ratio. This calculation assumes $B_{\text{max},\text{tf}} = 12 \text{ T}$.

loss mechanisms or as alternative "failure" mechanisms (one or the other of which will be true), then the sensitivity of ignition margins to aspect ratio is much lower than its sensitivity to Mirnov or neoalculator scaling alone.

An advantage of the higher aspect ratios is that they require lower current for fixed performance. As shown in Fig. 11-5, the plasma current is inversely proportional to the aspect ratio, when fusion power and wall loading are fixed. As mentioned above, the cost of plasma current is always underestimated during the preconceptual design phase, because of the inevitable lag of design for disruption vs design for normal operating conditions.

For fixed wall loading and fusion power, the field on axis rises linearly with the aspect ratio, as shown in Fig. 11-6. The maximum field at the coil is highly insensitive to aspect ratio in the medium- to high-aspect-ratio range, whereas it climbs sharply at low aspect ratio, due to the rapidly deteriorating maximum field to plasma field ratio. This again illustrates why low-aspect-ratio designs are suboptimal for superconducting tokamaks. Every increase of 1 in the aspect ratio raises the field on axis by 1.2 T, when fusion power, $P_f = 300$ MW and wall loading, $P_w = 2.0$ MW/m².

A second disadvantage of higher-aspect-ratio tokamaks is that they can have significantly higher refrigeration loads in the TF inner legs for fixed shielding, as shown in Fig. 11-7. This effect saturates at high aspect ratios, because it is predominantly a geometric effect, due to ratios of projected surface areas. Part of this effect is caused by a greater nuclear heating in mW/cm³ in the first layer of conductor, as shown in Fig. 11-8. Both ranges of refrigeration requirements and nuclear heating can be accommodated at extra cost by larger refrigerators or circulators. Previous parametric studies that fixed one or the other of these parameters have tended to mask the unfavorable effect of aspect ratio on inner leg heating,

Figure 11-5. Plasma current vs aspect ratio. This calculation assumes a fusion power, $P_f = 300$ MW.

Figure 11-6. Toroidal field vs aspect ratio. This calculation assumes $P_f = 300$ MW.

Figure 11-7. Refrigeration load in the toroidal-field inner legs vs aspect ratio. This calculation assumes $P_f = 300$ MW.

Figure 11-8. First-layer-superconductor nuclear heating vs aspect ratio. This calculation assumes $P_f = 300$ MW.

while exaggerating the effect on major radius, by insisting on additional shielding without checking whether this was cost effective.

The principal motivation for selecting higher aspect ratios is the improvement in current-drive performance. This improvement can be explained in three ways, two of which are illustrated in Fig. 11-9. This figure shows a scan of the energy multiplication Q for current drive, according to two different correlations. The simplest expression for current drive Q :

$$Q = 0.88 B_t \propto \kappa$$

was suggested by Rutherford.² The popularity of this correlation has probably embarrassed Rutherford, but, if used in the right spirit, the correlation's simplicity and insight balances its lack of sophistication. Rutherford assumes a fixed value of the energy of the electron tail and of the optimum temperature. The electron tail energy is determined by the parallel index of refraction of the launched wave. We have indeed found that the $n_{||}$ needed to satisfy the avoidance of mode conversion and edge absorption, the measure of electron tail energy, is a first-order function of reactor parameters. Furthermore, whereas optimum operating temperature is only a second-order function of machine parameters, it can vary by a few degrees in either direction from Rutherford's assumed value of 16 keV, depending on what is being optimized (e.g., Q for an experimental reactor, net electricity, or return on investment for a commercial reactor. For slow-wave current drive, optimizing Q dictates operating temperatures significantly higher than those giving the maximum fusion power. This effect

Figure 11-9. Lower hybrid Q vs aspect ratio. This calculation assumes $P_f = 300$ MW.

is even more dramatic for fast-wave current drive. However, optimizing net electricity tends to push the operating temperature sharply back towards the maximum power temperature of about 13 keV.) The parametric studies vs aspect ratio constrain n_e and select the electron temperature that gives the highest Q. However, despite the lack of total self-consistency, Rutherford's simplification of the problem has significant value. It illustrates the consequences of all current-drive mechanisms with a slowly varying normalized average current density to power ratio, $\langle J/P \rangle$. It is also a method for mentally separating variables. When more sophisticated calculations predict trends differing from those predicted by nIR (density, current, radius) scaling, the explanations for the differences tend to provide independent design insights.

In the scan shown in Fig. 11-9, both the Rutherford and the central-mode-conversion models have the qualitative similarities of improving with aspect ratio. However, the mode conversion model has a much stronger

dependence. The simplified nIR scaling tends to be too optimistic at low aspect ratios and too pessimistic at high aspect ratios. For the case of $P_f = 300$ MW and $P_w = 2.0$ MW/m², the crossover is at an aspect ratio of 3.0 and a field on axis of about 5.0 T. The effect of this is that the feasibility of a Q=10 experiment at higher aspect ratios is greater than the Rutherford approximation would indicate. At 5.0 T, the Rutherford equation predicts that $Q = 7$ and that, in order to achieve $Q = 10$, it would be necessary to increase the aspect ratio to about 12, which may be incompatible with the specified elongation and safety factor. By contrast, the mode conversion limit predicts Q=10 at an aspect ratio of 5.1.

The lower hybrid power that must be absorbed by the plasma decreases dramatically as the aspect ratio is increased from 2 to 3.5, then saturates, despite the linear improvement in Q, as shown in Fig. 11-10. The reason for this is that the current-drive design algorithm selects the plasma temperature that gives the highest Q. It turns out that, at higher fields, the best temperature is lower, and thus closer to the maximum fusion power temperature, than at lower fields. Thus, whereas the fusion power capability of the machines at different aspect ratios is held constant, the fusion power of the highest-Q experiment is actually rising with aspect ratio.

The final constraint to place on the parallel index of refraction is that of edge absorption. This is a soft constraint, because some shift of current towards the edge is desired in high beta plasma. It is therefore not imposed when calculating Q, but is output as a check on credibility. In Fig. 11-11, the bottom of the waveguide spectrum, $n_{||,1}$ is set equal to the

Figure 11-10. Lower hybrid power vs aspect ratio for a fixed maximum toroidal field at coil. This calculation assumes $P_f = 300$ MW.

Figure 11-11. Parallel index of refraction vs aspect ratio. This calculation assumes $P_f = 300$ MW.

minimum index of refraction for central mode conversion $n_{\parallel, \min}$. The top of the waveguide spectrum, $n_{\parallel, 2}$ is set equal to $1.2 n_{\parallel, 1}$, as a weighted consensus of how narrow a bandwidth can practically be achieved. The term $n_{\parallel, \max}$ is the maximum index of refraction, before the wave is absorbed by edge electrons without penetrating to the center. As can be seen, below an aspect ratio of 4, very little of the wave spectrum penetrates to the center, and at aspect ratios of 4.5 and above, most of the wave penetrates to the center. This turned out to be a "universal" result, over a broad range of powers, wall loadings and maximum fields. The reason for this is that, with elongation and safety factor fixed, toroidal beta is determined by aspect ratio alone. Wave penetration is a function of B, n and T, but in a D-T reactor, temperature is constrained to a narrow range for optimal performance. If temperature is constrained to be a constant, then both mode conversion and edge absorption are functions of beta only. This observation can be restated that central penetration of the slow wave requires an average beta of 5% or less. This is not contradicted by the STARFIRE design, which used edge absorption at 7% beta, or the HFCTR current drive reactor, which used central absorption at 4.5% beta.

A parametric scan of lower-hybrid current-drive performance vs aspect ratio was performed with both wall loading and maximum field at the coils held constant. Neither wall loading nor the maximum field at the coils turned out to be significant factors in lower-hybrid current-drive performance. The power requirements and penetration of slow-wave current drive were strong functions only of aspect ratio and fusion power. Good penetration and Q are achieved only above aspect ratios of about 4.5 to 5

and below average toroidal betas of 4 to 5%. This result correlates strongly with the results of previous design studies. In fact, the Magic5 tokamak recommended below is nearly identical in field with the other two slow-wave, pressure-limited tokamaks in the literature, Alcator DCT and Yuen's steady-state version of HFCTR.³ Even STARFIRE, with edge current drive is not significantly different. Thus, until otherwise demonstrated, it appears that slow-wave theory requires reactors with $B_t \approx 7$ T and $A \approx 5$.

The requirement for a plasma with an aspect ratio of 5 dovetails with a suggestion made by Perkins⁴ to eliminate the competition of electrons with alphas in the damping of the fast wave. This competition, although weakly understood, threatens to ruin the reactor feasibility of the fast wave. A study of current drive Q vs parallel index of refraction and temperature for several next-step reactors, done by the author,⁵ showed that the relations suggested by Perkins would reduce the maximum attainable Q with the fast wave by greater than a factor of 2 in all cases. A 50-50 split between alpha and electron absorption would reduce a $Q=30$ theoretical current drive to a $Q=15$ drive, which is the difference between success and failure in a reactor concept. Perkins suggested driving the fast wave at 2.5 times the resonant frequency of deuterium, thus placing the harmonic resonance layers for deuterium, tritium and alphas outside the plasma, so long as the aspect ratio was greater than or equal to 5.

11.3 MAGIC5

The realization that an experiment with a high probability of success for both fast- and slow-wave current drive would require an aspect ratio of 5

both constrains and simplifies the parameter-selection problem greatly. In a scan of fields from 8 to 12 T and wall loadings from 0.5 to 4.0 MW/m², the most attractive experiment with an aspect ratio of five had a major radius of 3.95 m and a field on axis of 6.88 T. Major machine parameters are shown in Table 11-1. This machine is comparable in size to the superconducting versions of TFCX. As a check, the Fusion Engineering Design Center's tokamak systems code was run on both TFCX-S and Magic5. The code predicted that Magic5 would be less expensive by \$70 M. The detailed cost comparison is shown in Table 11-2. Considering that current drive wasn't taken into account, and that the design center's code has a systematic bias in favor of high plasma current, because of the lack of disruption modeling in its sizing and costing algorithms, the qualitative indications are that Magic5 would cost less than TFCX, while providing substantially greater performance.

11.3.1. Plasma Performance Modelling of Magic5

The steady-state performance of Magic5 was evaluated at its Troyon pressure limit over a range of temperatures, as expressed by the relation

$$\langle \beta_t \rangle = 0.033 \frac{I_p}{a B_t}$$

where β_t is the volume averaged toroidal beta, I_p is the plasma current (MA), a is the minor radius (m), and B_t is the toroidal field on axis (T). The results of this evaluation are described in more detail, and compared with similar evaluations of TIBER and JET in a separate report.⁵ To

Table 11-1. Mission-II, A=5, steady-state tokamak parameters.

Parameter	Description	Value
R_o (m)	Major radius	3.95
a (m)	Minor radius	0.79
κ	Plasma elongation	1.6
Δ	Plasma triangularity	0.3
B_t (T)	Toroidal field on axis	6.88
q_{lim}	Engineering plasma safety factor	2.1
β_t (%)	Volume average toroidal beta	4.05
I_p (MA)	Plasma current	5.98

Table 11-2. Cost comparison of TFCX-S with Magic5 according to Fusion Engineering Design Center tokamak system code.

System	TFCX-S Nov. 1984 (\$M)	Magic5 Jan. 1985 (\$M)
Building/site improvement	168	159.5
First wall/armor/limiter	38	20.2
Shield and vacuum duct	46	37.5
Magnets	266	231.8
Cold structure	3.0	5.0
RF heating	78	105 @56 MW
Vacuum systems	128	116
PF power supplies	28	16.7
Heat transport	6	6.8
Cryogenics	2	11 @10.2 kW
Tritium system	28	27.6
Maintenance	35	31.6
Instrumentation	63	63
AC power	8	10.5
Total direct cost	897	842

evaluate fast-wave current drive, four different methods for selecting the parallel index of refraction were analyzed: (1) the lowest accessible value, (2) a wave with 3 times the electron thermal velocity, (3) the value corresponding to all the plasma current electrons being carried in a Maxwellian tail, and (4) the value needed for single-pass wave absorption. This same technique was also applied to TIBER, in the above mentioned study.

The parallel indices of refraction for Magic5 as functions of temperature are shown in Fig. 11-12. They are similar in shape and magnitude to those calculated for TIBER.⁵ The power requirements for Magic5, shown in Fig. 11-13, are 5 to 10% lower than those for TIBER over a range of indices of refraction and temperatures. The difference is probably not significant but, given the strong beta dependence of fast-wave absorption, it is interesting to learn that that high-field, low-beta plasmas will have about the same Q as low-field, high-beta plasma with similar fusion power densities. The power requirements of high-frequency fast waves in competition with alpha absorption are also lower, as shown in Fig. 11-14, but unacceptable for both TIBER and Magic5. The key advantage of Magic5 is that it has the option of using low-frequency fast-wave current drive at $2.5 \omega_{CD}$, thus avoiding the competition with the fast wave. The maximum theoretical Q at 18 keV (no competition with other absorption mechanisms, no randomization or absorption by reflections) of a fast wave at the accessibility limit is 19, as shown in Fig. 11-15. This is 1.5 higher than that calculated for TIBER and 4 higher than that for the Joint European Torus (JET). A Maxwellian at 20 keV has a Q of 10.1, thus achieving the goal of a Q=10 experiment. The Q=10 experiment on TIBER was calculated to

Figure 11-12. Parallel indices of refraction vs average temperature for Magic5.

be feasible at an ion temperature of 30 keV and an electron temperature of 24 keV, with no competition from alpha absorption. The model used here predicts $Q=10$ for TIBER at 25 keV with equal electron and ion temperatures. However, using Perkins' first-order expression for global competition with alpha absorption,⁴ the Q is much worse. For TIBER, the best identified Q was 4.2 at 15 keV using a wave designed for single-pass absorption. The Q with global competition with alphas in Magic5 of 3.5 is worse than that of TIBER, as shown in Fig. 11-16, but Magic5 has the option of low-frequency fast-wave drive that avoids alpha absorption. The efficiency of current drive of Magic5 with a Maxwellian tail, expressed in amperes/watt is slightly worse than that of TIBER, because of the higher major radius, as

Figure 11-13. Power requirements of high-frequency fast waves in competition with alpha absorption vs temperature for Magic5.

shown in Fig. 11-17. However, Magic5 has only 54% as much current as TIBER, so the power levels required at any temperature are less. The normalized $\langle J/P \rangle$ of Magic5 is negligibly better^{6,7} than that of TIBER, as shown in Fig. 11-18. The fusion power is about the same as that of TIBER, as shown in Fig. 11-19, whereas the wall loading of 1.58 MW/m^2 is $3/4$ that of TIBER, as shown in Fig. 11-20. The densities are slightly lower than those in TIBER, and less than the Murikami limit above 21 keV, as shown in Fig. 11-21. Magic5 achieves ignition margins greater than 1.5 for neoalculator, Mirnov, and Asdex H-mode scaling, but does not ignite for Goldston or Perkins scaling, as shown in Fig. 11-22. Neoalculator scaling would allow ignition out to 26 keV, while Mirnov would allow it out to 20 keV. As with TIBER, a burn margin (α plus injected power/losses)

Figure 11-14. Parallel indices of refraction for Magic5 vs temperature.

Figure 11-15. Q of fast-wave current drive, with alpha absorption vs average temperature for Magic5.

Figure 11-16. Hypothetical Q of fast-wave current drive, without alpha absorption vs average temperature for Magio5.

Figure 11-17. Current-drive efficiency expressed as the ratio of current to power, with alpha absorption vs average temperature for Magio5.

Figure 11-18. Normalized $\langle J/P \rangle$ of Magic5 with a Maxwellian tail and no alpha vs average electron temperature.

Figure 11-19. Fusion power vs average electron temperature for Magic5.

Figure 11-20. Neutron wall loading vs average electron temperature for Magic5.

Figure 11-21. Average electron density vs average electron temperature for Magic5.

Figure 11-22. Ignition margin vs average electron temperature for Magic5.

greater than one would be achieved for Goldston, but not Perkins scaling, out to 11 keV, as shown in Fig. 11-23.

The number of passes for the absorption of a Maxwellian tail wave is several dozen, about the same as calculated for JET and about the number for TIBER, as shown in Fig. 11-24. This number of passes may be lower than cavity Q, avoiding wall absorption as a dominant loss, but it is somewhat difficult to imagine that phase coherence would be preserved. Since the theoretical efficiency of single-pass-absorption current drive is not much worse than that of a Maxwellian, it appears that the selection of this index of refraction or one even higher may be the correct design procedure.

The width of a four element array is comparatively small, because of the high field of Magic5, as shown in Fig. 11-25. Even a Maxwellian would

Figure 11-23. Burn margins corresponding to a fast-wave Maxwellian vs average electron temperature for Magic5.

require a port only a little more than a meter wide at 20 keV, while a single-pass absorption wave would only require 44 cm. This presents the possibility of having even more elements in parallel for better spectrum control. The frequency equal to 2.5 times the central-deuterium cyclotron frequency ω_{CD} is 131 MHz, in the vhf range.

The slow, lower-hybrid wave in Magic5 would have, as shown in Fig. 11-26, a maximum Q at 18 keV of 10.7, essentially identical to the theoretical efficiency of the fast-wave drive. This presents the possibility of making an orderly transition from a slow-wave to a fast-wave plasma or of mixing slow-wave with fast-wave current drive for a single plasma, perhaps with slow wave for the edge and fast wave for the center.

Figure 11-24. Number of passes for the absorption of a fast wave vs average electron temperature for Magic5.

Because of the low beta, the penetration of the slow wave is as good as that for JET, as shown in Fig. 11-27.

11.4. COST REDUCTIONS AND PERFORMANCE IMPROVEMENTS

The Mission-I and Mission-II next-step tokamaks of FY85 have fulfilled their respective missions more dramatically than might have originally been imagined. The Tokamak Fusion Core Experiment (TFEX) tokamaks of FY84 were divided into nominal and high-performance superconducting and copper

Figure 11-25. Width of waveguide array (4 element, 90° phasing) vs average electron temperature for Magic5.

Figure 11-26. Slow-wave, lower-hybrid Q vs average electron temperature for Magic5.

Figure 11-27. Parallel indices of refraction of a slow wave vs average electron temperature for Magic5.

options. When evaluated, these options were all found to be well within a factor of two of each other in terms of both cost and performance (ignition margins, burn time). In FY85, the next-step tokamak efforts were split into a short-pulse, copper mission and a long-pulse, superconducting mission. This time, the divergence was much more marked. Mission I made significant reductions in the burn time, so that ignition would be achieved without requiring steady-state impurity control or even wall cooling. The reduction in burn times was accompanied by innovations in TF magnet design and by the development of a rationale for higher stress allowables. This led to a remarkable improvement in machine cost, by approximately a factor of four over costs of the FY85 devices. The Mission-II tokamaks, TIBER and Magic5, achieved small reductions in cost over the costs of the TFCX superconducting

concepts, along with major improvements in performance. Instead of the low-density, pulsed current-drive concepts of TFCX, both TIBER and Magic5 developed strategies for true steady-state burn and cooling at $Q=10$ and power levels comparable to the auxiliary heating power on JET and Tokamak Fusion Test Reactor (TFTR). The more expensive Magic5 included the abilities to do $Q=10$ experiments with either slow-wave or fast-wave current drive, to avoid fast-wave current drive competition with alpha absorption or wall reflection, and to achieve control of q profiles, hopefully allowing enhancement of beta above the Troyon limit.

Future work on superconducting next-step tokamaks can proceed along the same two lines of simultaneous concept improvement and cost reduction. One approach would be to search for aggressive concept improvement and modest cost reductions. Since a tokamak in the billion dollar range is no longer possible within the short-term budgetary constraints of the domestic fusion program, this approach would be oriented towards long-term and international missions. Some of the improvements mentioned above for the Magic5 should be taken from the preconceptual to the conceptual level. These include the strategies for combining slow-wave with fast-wave current drive and for using q -profile control to increase the Troyon parameter without major modifications to the first wall, shield, and TF magnets. Other improved concepts might include the use of compressional Alfvén-wave current drive or polarized fuel to search for a $Q=15$ to 20 steady-state current-drive plasma.

In order to achieve significant cost reductions without significant performance reductions, lessons can be drawn from this year's Mission-I tokamaks. These tokamaks indicate clearly that there is a broad range of

parameter space in which focussed engineering design innovation in the inside leg of the TF coils can lead to global reductions in machine size and cost, and that, similarly, these reductions can be accompanied by increases in toroidal field without the expected increases in magnet stored energy. It seems evident that the smallest possible long-pulse superconducting tokamak cannot be as small as the smallest possible short-pulse copper tokamak, because of the additional constraints on nuclear shielding and on the maximum field in a superconductor. By the same logic, it is the relaxation of these constraints that is most likely to reduce the size of a superconducting tokamak.

Three analyses were performed to evaluate the sensitivity of machine cost to relaxation of constraints on superconducting tokamaks. It should be noted first that, unfortunately, none of the previous generation of superconducting tokamaks satisfy the $aB^2 = 40$ specification that is met by all of the inexpensive copper tokamaks. The Magic5 tokamak is fairly close with $aB^2 = 37$, whereas all four of the TFCX devices, FED, and TIBER are closer to 20, as shown in Table 11-3. The first study merely brings the superconducting tokamaks up to aB^2 speed by holding aB^2 fixed at 40 and the field at the toroidal field coil $B_{max,tf}$ fixed at 12 T, and then scanning aspect ratios, primarily to calculate the effect on the characteristic dimension, TF stored energy, and fusion power. As seen in Fig. 11-28, it is possible to identify a tokamak with as small a characteristic dimension as that of Magic5, but not one as small as that of TIBER, with the constraint of $aB^2 = 40$. It is no longer possible to find a solution with a stored

Table 11-3. Recent evolution of next-step tokamaks.

Machine	R_O (m)	a (m)	κ	D_{char} (m)	αB^2 (m·T ²)	W_{tf} (GJ)	P_f (MW)
FED 10 T	5.0	1.3	1.6	3.22	27.5	23	450
TFCX							
SC-Nom	4.08	1.52	1.6	3.15	21.1	7.1	267
SC-HP	3.61	1.3	1.6	2.74	23.2	5.44	270
Cu-Nom	3.35	1.3	1.6	2.64	20.8		229
Cu-HP	2.60	1.04	1.6	2.08	21.1		197
TIBER	2.63	0.73	1.94	1.93	18.3		400
Magic5	3.95	0.79	1.6	2.23	37.0	6.4	320
ISPO424	1.62	0.53	1.6	1.17	42.0	2.35	305
LITE	1.76	0.55	1.6	1.24	43.0	1.2	300
Ignitor	1.01	0.39	1.6	0.79	60.9	0.47	108

Figure 11-28. Plasma major radius and plasma characteristic dimension vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 45 cm.

energy, W_{tf} , 6.4 GJ or less, like the Magic5. As shown in Fig. 11-29, the optimum stored energy in the TF system has a sharp minimum of 7.3 GJ at an aspect ratio of 4.6. The results shown in Figs. 11-28 and 11-29 are also found in the studies with increased field and decreased shielding. The minimum stored energy is always at an aspect ratio between 4.5 and 5.0, while the minimum characteristic dimension is at an aspect ratio between 8 and 10. Plasma current, fusion power, wall loading, refrigeration power,

Figure 11-29. Toroidal-field stored energy vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 45 cm.

current drive power and toroidal field/ohmic heating (TF/OH) feasibility all improve monotonically with aspect ratio, when aB^2 and $B_{\max,tf}$ are constrained. Mirnov ignition margin gets worse with aspect ratio and tends to equal 1.5 at an aspect ratio of 5. Since no cost factor can be identified that would improve below the minimum stored-energy aspect ratio, the lower aspect ratios favored in the past few years appear to be very suboptimal for an advanced, superconducting next-step tokamak. This is not, perhaps, surprising, since the aB^2 constraint is derived from the neoalculator

scaling, with its highly favorable dependence on aspect ratio. The need for shielding and thermal isolation in superconducting tokamaks further increases the significance of the coil/plasma field ratio over its significance in copper coil machines.

If the TF superconducting magnet could operate at 14 T, the characteristic dimension could be decreased by 20%, corresponding to a 44% reduction in size of those components related to characteristic dimension. On the other hand, the magnet stored energy would be reduced by 8%, despite the additional magnet field. The fusion power would be reduced by 23% to 187 MW, as shown in Table 11-4. The main obstacles to a 14-T design might be the procurement of adequate superconductor and the current density in the throat. As shown in Fig. 11-30, the available space in the throat would require a current density in the TF inner-leg envelope of better than 1.3 kA/cm^2 , which is within the 1983 guidelines of 2 kA/cm^2 . This however does not meet the field-dependent 1984 TFCX design guidelines limiting the envelope density to 1.1 kA/cm^2 at 14 T. Therefore technical innovations would be required to increase the current density in the throat by 20% or better. Innovations of this sort are being targeted by the Multipurpose Coil experimental program, proposed jointly by LLNL and MIT. This challenge would be similar to that met by the copper TF throat designers this year. (However, since TF stored energy and Mirnov ignition margin are still varying slowly, a less innovative approach suggested by Fig. 11-30 is to simply increase the aspect ratio to 6, which would actually decrease the characteristic dimension along with the fusion and current drive power.

Table 11-4. Possible options for advanced next-step tokamaks.

Parameter	Description	Assumed value
aB^2 (m·T ²)	Product of minor radius, and field on axis squared	40.0
κ	Plasma elongation	1.6
Δ	Plasma triangularity	0.3
q_{lim}	Minimum safety factor	2.1

Parameter	$B_{max,tf} = 12$ T $t_{shield} = 45$ cm	$B_{max,tf} = 12$ T $t_{shield} = 25$ cm	$B_{max,tf} = 14$ T $t_{shield} = 45$ cm	$B_{max,tf} = 14$ T $t_{shield} = 25$ cm
R_o (m)	3.9	3.51	3.29	2.78
a (m)	0.71	0.625	0.55	0.55
A	5.48	5.6	5.9	5.02
D_{char} (m)	2.1	1.87	1.71	1.57
P_f (MW)	272	230	187	242
P_w (MW/m ²)	1.9	2.05	2.0	3.1
B_t (T)	7.5	8.0	8.5	8.5
W_{tf} (GJ)	7.35	5.64	6.77	5.09

Figure 11-30. Current density in the toroidal-field inner-leg envelope vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 45 cm and $B_{max,tf} = 14$ T.

If the thickness of the neutron/gamma radiation shield were reduced by 20 cm, the characteristic dimension could be reduced by 8.9%, while the TF stored energy could be reduced by 23%, as shown in Figs. 11-31 and 11-32. The fusion power would be reduced by 15.5%. As the size of the internal components is proportional to the square of the characteristic dimension, this essentially means that all parts of the nuclear island are shrinking by

Figure 11-31. Plasma major radius and plasma characteristic dimension vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm.

about 1% per centimeter of shield. This result is similar to those of previous trade studies, which have cited savings of \$10 M/cm for billion dollar experiments. The principal problem that arises from reducing the shield is the nuclear heating of the inner leg of the TF coil. Using the algorithms for sizing tungsten shields, a 25-cm shield (including 10 cm of steel vacuum vessel and TF case) would give a peak heating in the first superconductor layer of 150 mW/cm^3 and a total refrigeration load on the

Figure 11-32. Toroidal-field stored energy vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm.

inner legs of 100 kW, as shown in Figs. 11-33 and 11-34. The 1984 high-performance, superconducting TFCX-HPSC demonstrated the feasibility, although perhaps not the desirability, of a lightly shielded next-step experiment with specific heating of the inside leg of 50 mW/cm^3 . The high, instantaneous, refrigeration load was handled by storing helium gas between shots and replenishing inlet helium with a much smaller refrigerator/liqefier sized for the average load. Since the duty factor of the next-step experiment has been drastically reduced for Mission-I

Figure 11-33. Specific heating of first superconducting layer vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm.

tokamaks, this is certainly a possible option for Mission II. The specific heating is, however, ambitiously high. It would be preferable to reduce it by a factor of 3 or 4. This would probably require (1) more optimized shield material selection,* (2) innovations in machine design, allowing the

*The NUWMAK neutronics design indicated the possibility of achieving significant improvements over tungsten by using an optimized blend of materials.⁸

Figure 11-34. Total refrigeration load on inner legs vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm.

decrease in radial build to be taken from parts of the tokamak, other than the neutron/gamma shield, or (3) increasing the aspect ratio, thereby further decreasing the fusion power and wall loading.

If innovations were possible in both the TF inner-leg design and the inner-shield design, then the tokamak would continue to shrink proportionally. As shown in Figs. 11-35 and 11-36, the characteristic dimension could be reduced by 25.3%, while the stored energy was reduced by 32% from that of a 12-T, 45-cm shield machine. We selected a set of

Figure 11-35. Plasma major radius and plasma characteristic dimension vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm, and $B_{max,tf} = 14$ T.

Figure 11-36. Toroidal-field stored energy vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 25 cm, and $B_{\text{max,tf}} = 14 \text{ T}$.

parameters with a lower aspect ratio of 5.0 and higher power, in order to keep the Mirnov ignition margin at 1.5. However, a higher aspect ratio would again greatly ease the engineering feasibility of the tokamak, without increasing its weight. As shown in Fig. 11-37, the highest Q of a slow wave experiment is now only 8.8, but the current drive power at the highest Q has been reduced to 15 MW. A cost/performance comparison between the 14-T, 25-cm machine and ISP042 is made in Table 11-5, showing the possibility of a major

Figure 11-37. Q vs aspect ratio for an advanced, superconducting next-step tokamak. The shield thickness is 45 cm and $B_{\max,tf} = 14$ T.

improvement in mission with a moderate cost increase over the copper short-pulse ignition option.

Table 11-5. Cost/performance comparison: Advanced Tokamak vs ISP042.

Parameter	14-T, 25-cm Advanced Tokamak	ISP042	Ratio
D_{char}	1.57	1.17	1.34
W_{tf}	5.09	2.35	2.16
P_f	242	305	0.79
t_{flat}^a	500	5	100

^aFlat top time.

11.5. CONCLUSIONS

- Two significant improvements in next-step, long-pulse superconducting tokamak concepts were achieved in the past year. TIBER demonstrated the feasibility of a superconducting ignition experiment with a less-than-3.0-m major radius and the possibility of a steady-state Q=10 current drive experiment. Magic5 established the principle that tokamak plasmas with an aspect ratio of 5 define a much more conservative approach to steady-state current drive by eliminating its two most bothersome problems: fast wave competition with alpha absorption and wall reflection, and failure of the slow wave to penetrate. If both fast wave and slow-wave current drive perform as expected, improvement of plasma beta by q-profile control is proposed.
- The significance of TIBER and Magic5 is that they both have smaller characteristic dimensions than last year's superconducting TFCX options, yet perform in a superior way by allowing true steady-state current drive.
- Directions for future effort have been identified that might allow further significant decreases in size and cost, without sacrificing the simultaneous goals of ignition and steady-state current drive. The copper machine solution of higher-field, more compact tokamaks holds the most promise for superconducting tokamaks as well, although at higher aspect ratios.

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