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Fusion Reactor Systems Studies
Project #DE-FG02-89ER52158

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Progress Report for the Period
November 1, 1996 to October 31, 1997

also serves as Final Report

L.A. El-Guebaly, J.P. Blanchard, G.L. Kulcinski

Fusion Technology Institute
University of Wisconsin-Madison

August 1997

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I. Introduction

During FY97, the University of Wisconsin Fusion Technology Institute personnel have participated in the ARIES-RS and the ARIES-ST projects. The main areas of effort are:

- Neutronics analysis
- Shielding of components and personnel
- Neutron Wall loading distribution
- Radiation damage to in-vessel components
- Components lifetimes
- Embrittled materials designs issues
- Stress and structural analysis
- Activation, LOCA, and safety analysis
- Support and fabrication of components
- Vacuum system
- Maintenance

The UW FTI contributions to the ARIES-RS report is given in Appendix A. These are:

- Section 3.3 Neutronics
- Section 3.4 Electromagnetic Disruption Analysis
- Section 5.2.3 F/B/S Mechanical Configuration
- Section 5.2.4 Plasma Stabilizing Shells and Active Coils
- Section 5.3 Stress Analysis
- Section 5.4 Neutronics Summary
- Chapter 6 Radiation Protection and Shielding Analysis
- Section 7.5 Divertor Module Design
- Section 7.9 Particle Exhaust and Vacuum System Design

- Section 10.2 Radiological Criteria
- Section 10.3 Activation and Waste Disposal
- Section 10.4 Loss of Coolant Accident Analysis

Major contributions were written for the quarterly reports as given below:

•Seventh Quarterly Report:

- Center Post Shielding and Lifetime
- Blanket Neutronics
- Preliminary Safety Analysis Results for LAR Tokamak

•Eighth Quarterly Report:

- Neutron Wall Loading Distribution
- Blanket Neutronics
- Embrittled Material Design Issues for Fusion Devices
- Safety Issues

Several presentations and papers were given at technical conferences and a large number of electronic-mail and regular-mail communications were sent. Section 2 of this progress report summarizes the work accomplished and in progress for the ARIES-RS project during the contract period (November 1, 1996 to October 31, 1997).

Table 1 lists the presentations given by UW FTI personnel at the ARIES project meetings. Several papers were given at technical conferences and accepted for publications in refereed journals. These papers are:

- 1- L. El-Guebaly, "Overview of ARIES-RS Neutronics and Radiation Shielding: Key Issues and Main Conclusions", accepted for publication in Journal of Fusion Engineering and Design.
- 2- D. Steiner, L. El-Guebaly, H. Khater, E. Mogahed, et al., "ARIES-RS Safety Design and Analysis", accepted for publication in journal of Fusion Engineering and Design.
- 3- M. Tillack, L. El-Guebaly, J. Crowell, et al., "Configuration and Engineering Design of the ARIES Tokamak Power Plant", accepted for publication in the Journal for Fusion Engineering and Design.

- 4- F. Najmabadi, L. El-Guebaly, J. Blanchard, H. Khater, E. Mogahed, I. Sviatoslavsky, J. Crowell, et al., "Overview of ARIES-RS Reversed-Shear Power Plant Study", accepted for publication in the Journal of Fusion Engineering and Design.
- 5- D.K. Sze, L. El-Guebaly, I Sviatoslavsky, J. Blanchard, J. Crowell, H. Khater, E. Mogahed, et al., " The ARIES-RS Power Core- Recent Development in Li/V Designs", presented at the 4th International Symposium on Fusion Nuclear Technology (ISFNT-4), Tokyo, Japan, April 1997.
- 6- E. Mogahed, H. Khater, I Sviatostavsky, L. El-Guebaly, "ARIES-RS Loss of Coolant Accident Analysis", to be presented at the IEEE-97 Symposium, San Diego, CA, October, 1997.

**Table 1, University of Wisconsin Fusion Technology Institute
Presentations at the ARIES Project Meetings**

MEETING	UW AUTHORS	TITLE
5-7 March, 1997 UCSD	El-Guebaly	Overview of ARIES-RS Neutronics and Radiation Shielding
	El- Guebaly and Khater	Center Post Shielding/ Lifetime and Blanket Neutronics
	Khater, Mogahed, and El-Guebaly	Preliminary Safety Analysis for LAR Tokamak
17-19 June, 1997 UW	Khater, Mogahed and Wittenburg	Safety Issues
	El-Guebaly	3-D Neutron Wall Loading and Blanket Neutronics
	Blanchard and Crowell	Brittle Material Design Issues for Fusion Devices
	Blanchard and Crowell	Fusion Power Plant Design Issues

2. Summary of Tasks Accomplished During FY97

2.1 Neutron Wall Loading Distribution

The neutron wall loading (NWL) distribution was calculated by El-Guebaly, using the 3-D MCNP code. Since the geometry of the ARIES-ST device has not been selected yet, the FW configuration of the 3/2/96 LAR strawman ($A=1.25$, $R=5$ m) was used in the analysis. To accommodate the flaring of the center post, the design calls for a curved i/b FW that conforms to the plasma. As a result, the i/b NWL distribution is nearly constant from the midplane to the top/bottom. For the outboard side, the NWL peaks at the midplane, but the region near the divertor has less than one-fifth the peak value. The NWL in the divertor region is relatively low. Other data of interest is the average NWL on the i/b and o/b sides. These are 1.4 and 2.4 MW/m², respectively.

The NWL calculations provide also the neutron coverage fraction. It is used in the 1-D analysis to estimate the global neutronics parameters, such as integral heating, overall TBR, and overall neutron energy multiplication. For a LAR machine with $A=1.25$, the neutron coverage fraction (NCF) for the outboard blanket is 90%. For the o/b side, The NCF is larger than the FW area coverage fraction (ACF). The NCF and thus the outboard breeding decrease with the aspect ratio.

2.2 Center Post Shieldings and Lifetime

El-Guebaly and Khater performed neutronics and activation analyses to determine the center post (CP) lifetime for the 30 cm thick inboard shield used in the 3/2/96 LAR strawman. The predominant life-limiting factor for the CP is the Class C waste top-level requirement. Our results showed that for the 30 cm shield case the CP lifetime is 13 FPY, requiring two CP replacements over the 40 FPY life of plant. Another option is to shorten the CP lifetime and replace the CP with the outboard blanket every 3-4 FPY. In this case, the required inboard shield is 10-12 cm thick. The case of unshielded CP has a very short lifetime, in the order of a few months. The time and spatial average increase in Cu resistivity due to radiation-induced transmutations ranges between 5% and 15% for the 3 cases. The neutron-induced swelling in Cu is low if the CP temperature is kept below 180 oC.

For all shielded and unshielded cases, the DS GlidCop AL15 alloy will become extremely brittle shortly after operation. Two questions arose regarding the embrittlement of Cu alloys: 1-Should the CP be encased in a steel jacket for support?

2-Will micro-cracking affect the resistivity of Cu? The ARIES team will address those two questions during the course of the study. For the various inboard shielding options, the ARIES systems code will assess the impact of the following on the overall size and cost of the ST machine: CP lifetime, change in resistivity, CP replacement cost, availability, recovered inboard heating, and average MPD.

2.3 Blanket Neutronics

Initial calculations were performed by El-Guebaly for the four candidate breeders ($\text{Li}^{17}\text{Pb}^{83}$, Li, Li_2O , Li_2TiO_3) to determine the breeding capability of the proposed blanket designs. Based on a 1-D model with 90% o/b NCF for the $A=1.25$ machine, the 75 cm thick helium cooled LiPb/FS/SiC blanket provides an overall TBR of 1.12 for 90% enriched breeder. Replacing the SiC insulator by LiPb breeder increases the TBR to 1.24. Thickening the blanket to a meter increases the breeding by only ~2%, meaning that the optimum thickness for the LiPb/FS/SiC/He blanket is 75 cm. It is assumed that the second FS wall and the FS structure of the blanket will stabilize the kink mode of the plasma. This means there is no need for a separate kink shell in this blanket design. As mentioned before, the o/b blanket will provide less breeding if the aspect ratio is higher than 1.25. To satisfy the breeding requirement (overall TBR > 1.1) for the $A>1.25$ machines, an attempt should be made to reduce the SiC volumetric content below 15% and/or install breeding blanket on the i/b side.

A few Li blanket design options were analysed from the neutronics view point. Note that some options may have materials compatibility problems. The first option is similar to the LiPb blanket with Li breeder instead of LiPb. The second option is the ARIES-RS-type blanket with V structure. In all cases, the o/b blanket thickness was kept fixed at 75 cm. The results showed that the SiC insulator degrades the breeding of the Li breeder, the V structure results in more breeding than FS, and, for the same insulator/structure content, the natural Li provides less breeding than the 90% enriched LiPb. However, the ARIES-RS-type blanket (Li/V) breeds more tritium than the LiPb/FS/SiC/He blanket. For the $A=1.25$ machine, a 50 cm thick o/b Li/V blanket will satisfy the breeding requirement. For $A>1.25$ machines, a thicker Li/V o/b blanket (~70 cm) will provide the required breeding with no need to breed on the i/b side.

For the free-surface Li_2O pebble breeder, it is assumed that the o/b blanket has no FW and contains no structure nor multiplier. To achieve an overall TBR of 1.1, the o/b blanket should be 30 cm thick for 100% dense natural Li_2O . If the FW, structure, and kink shell are all included, the blanket will need to employ beryllium multiplier to achieve an adequate TBR, especially for high aspect ratio machines.

The titanate blanket will not provide tritium self-sufficiency (overall TBR > 1.1) for the ST design unless a sizable amount of beryllium (300 tonnes or more) is used in the outboard blanket (no breeding on inboard or behind divertor). The

option of using other multipliers (such as Pb, Be₂C, and BeO) was analysed and discarded for insufficient breeding. Moreover, the arrangement of the Be layers within the blanket was assessed along with the effect of Li enrichment. In summary:

- 1- A titanate blanket will never breed sufficient tritium with separate Be and Li₂TiO₃ layers for any enrichment
- 2- A homogeneous mixture of Be and Li₂TiO₃ with a ratio of 9:1 will provide the necessary breeding for 15% enrichment or more
- 3- 40-60 cm thick outboard blanket is needed, depending on the Li enrichment

The following issues/concerns were identified for the titanate blanket design:

- 1- The blanket will be expensive (SiC structure @ 400 \$/kg and 300 tonnes of Be @ 600 \$/kg)
- 2- The blanket will have a high replacement cost as radial segmentation of blanket is not possible
- 3- There are safety and resources issues for the 3000 tonnes of Be needed during the 40 FPY life of plant. Recycling of Be will solve the resources problem and reduce the volume of radwaste, but will not reduce the cost
- 4- The burnup of lithium is a concern because the breeder content is low

The overall neutron energy multiplication (M_n) of the system depends on the regions from which the nuclear heating is recovered. If the heating is recovered from the o/b side only, M_n is 1.0. M_n reaches a maximum of 1.15 if the heating is recovered from all regions (i/b, o/b, and divertor).

2.4 Embrittled Material Design Issues for Fusion Devices

Blanchard and Crowell investigated the implications of embrittled material design rules for the design of fusion devices. They applied the ITER Interim Structural Design Criteria (currently at draft 5) to two fusion components: a dispersion-strengthened copper centerpost design for ARIES-ST and a V-4Cr-4Ti first wall. This set of design criteria reduces limits on several stress measures as a material loses ductility. In the centerpost, embrittlement increased the ratio of peak membrane stress to allowable membrane stress from an acceptable 52% to an unacceptable 142% within 0.003 dpa. The embrittled vanadium alloy first wall had a 46% lower allowable heat flux than the ASME Boiler and Pressure Vessel Code (applicable to ductile materials) permits. Based on this scoping analysis, design stress limits were provided for the ARIES-ST centerpost, taking into account the embrittled state of the copper as a function of fluence.

2.5 Safety Analysis

Khater led the safety activity for the project and Mogahed carried out the Loss of Coolant Accident (LOCA) analysis for the Low Aspect Ratio (LAR) design.

Preliminary safety analyses were performed and the following issues were considered:

2.5.1 Waste Disposal

Used a composition of F82H ferritic steel which contains 20 wppm of silver. The high content of silver does not allow the first wall and shield to qualify as low level waste (LLW) according to Fetter limits. The Fetter limits can be met if the new composition of F82H ferritic steel (proposed by ORNL) which contains less than 1 wppm of silver is used. However, the new composition needs to be isotopically tailored to significantly reduce the Nb trace element. Results for shielded Central Post (CP) with F82H steel, a CP shielded with ORNL steel, and an unshielded CP are also compared. An unshielded CP will not meet the Class C waste with either the NCR or the Fetter limit criteria. The F82H steel is slightly superior with the NRC limits, but the ORNL steel is superior using the Fetter limits. The silver content of CP copper needs to be reduced by an order of magnitude to allow for its disposal as LLW according to Fetter limits. A shielded CP has about 6 FPY lifetime to meet the 10CFR61 NCR limits.

2.5.2 LOCA and Off-site Doses

A loss of coolant accident analysis was performed for a generic design with 30 cm inboard shield (consists of 65% steel, 15% SiC and 20% is coolant channels). The central post is made of copper with empty coolant channels. The cross-sectional of this empty coolant channels represent 30% of the total CP cross-sectional area. This analysis allowed conduction up/down the center post, but connected to an ultimate heat sink only through the steel jacketing at the outer radius of the CP (the remainder of the central post radiates to a central Cu column connected to the base of the machine (Heat Sink) and carry no current at all times.). This still caused high temperatures in the CP during a LOCA. The present CP design is intimately connected to very large busbars (an ultimate heat sink) either at the top or the bottom of the CP and also is connected to the TF coil outer legs at the other end of the CP (also an ultimate heat sink).

Off-site doses caused by radioactive material mobilization are calculated using temperature profiles obtained from a simple LOCA analysis. The release rates used are based on INEL's oxidation-driven volatility data. The LOCA is assumed to last for 24 hours at the following average temperatures:

- CP at 700 C
- FW and Shield at 800 C
- Reflector at 600 C

Assuming a leak rate of 1% per day and a containment factor of 99% results in an off-site dose which is less than the 10 mSv required to avoid evacuation in case of an accident.

2.5.3 Be Safety Issues

The large amount of Be used in the He/FS/Solid Breeder/Be/SiC design option presents a serious safety hazard. The issue of hydrogen combustion and over pressurization poses a serious threat to obtaining the desired level of radioactivity confinement. The oxidation of all Be would result in the release of about 90,000 kg of hydrogen and about 16,680 GJ of chemical potential energy. Only 400 GJ are needed to melt the 300 tones of Be under adiabatic conditions.

The Be concentration in air causing immediate danger to life and health is 10 mg/m³ (NIOSH Pocket Guide to Chemical Hazards). Using the NRC Regularity Guide (1.145) (Atmospheric Dispersion Models for Potential Accident Consequence Assessment at Nuclear Power Plants) while assuming 1 hour release and light wind speed, the 10 mg/m³ of Be concentration could be produced by the release of 110 kg of Be from the blanket. Avoiding the need for using Be by using a different breeder is recommended.

2.5.4 Change in Cu Resistivity of the Central Post

The 14 MeV neutrons produce large concentrations of transmutation products in copper. The thermal conductivity of copper alloy is very sensitive to the variation in the amount of transmutation products. Nickel, cobalt, and Zinc are of special concern. Shielding the center post, can result in a longer life-time (to meet the disposal limits) and reduce the amount of stable Ni produced by high energy reactions.

2.5.5 Safety of the He/83Pb17Li Dual Coolant Design

The major sources of a radiological hazard in a PbLi blanket are the two isotopes ²⁰³Hg and ²¹⁰Po, both of which are highly volatile materials. The ²¹⁰Po is considered the main safety hazard. It is produced from either ²⁰⁸Pb or ²⁰⁹Bi (impurity in Pb). By controlling the concentration of bismuth in the commercial lead (typically 500-1500 wppm) to below 10 wppm, it is felt the radiological hazard from ²¹⁰Po is minimized. This cleanup would be required for the initial supplies and on a continual basis to keep the concentration of bismuth at an acceptable level. The removal of bismuth can be accomplished by cooling the liquid to 300 C to allow growth and separation of Li₃Bi crystals.

2.6 Li₂O Blanket Design

One of the blanket options considered for ARIES-ST is the granular flow Li₂O scheme with no structural first wall. The option has been proposed by D.K. Sze (ANL) with collaboration from UW. The scheme calls for granules of Li₂O, on the order of 1.0 mm in diameter, flowing by gravity from the top to the bottom of the reaction chamber. The flow is directed at the top with SiC baffles. One layer of granular flow, several cm. thick, is open to the plasma. At mid-plane the granules intercept some of the charged particles in the scrape-off layer, thus reducing the heat load on the diverters. The diverters themselves have open features with exposed surfaces of granular flow. The charged particles impact the granules, which in themselves constitute a renewable surface. The energy of the charged particles is transferred to the granules and the problem of erosion is solved. Behind the first wall granular flow and separated from it by a SiC wall, there is a thicker zone of moving Li₂O granules which make up the energy absorbing and breeding part of the blanket.

There are many issues ^{that} need investigating in this blanket option. Aside from the mechanics of directing the flow around the reaction chamber, there are issues of outgassing from the granules, which may make pumping down the chamber difficult if not impossible. Although the Li₂O vapor is low Z, nevertheless its effect on the plasma has to be investigated. The issue of dust also can be detrimental to the operation of the plasma. On the other hand, there are many salient features in this blanket option. Among them is its low pressure, no heavily stressed blanket structures, the high potential coverage which will make breeding easy, simple SiC structure, low Z of the Li₂O vapor and the potential for substantially reducing the problems of the diverters. Another interesting aspect of this scheme is its potential for pumping the plasma ash, by having the impacting alpha particles getting trapped and swept away by the flowing granules. Finally, such a simple structure, once drained of granules will lend itself to easy maintenance.

2.7 Center Post Maintenance

Some work has also been done at by Sviatoslavsky in the area of CP maintenance. Several options for CP changeout have been proposed by the ARIES team, among them lifting the CP through the top by a crane, lowering it through the bottom with telescoping hydraulic pistons and turning the chamber on its side with

the option of horizontal CP extraction. The option of lowering the CP through the bottom appears to be the most desirable. Using telescoping hydraulic pistons with a stroke of > 30 m, appears challenging if at all possible. In this connection, a staged approach for lowering the CP has been proposed, in which the maximum stroke is reduced to an order of 5-8m, a much more manageable system. Another method has been proposed for lowering the CP into a cask using cables and pulleys operated by electric motors mounted on the outside of the cask. Both of these schemes are conceptual at this time and will require further evaluation.

2.8 Other Activities

The UW FTI personnel provided to the PPPL group the nuclear heating, atomic displacement, and transmutations parameters needed for the center post power losses calculations. In addition, we assisted the UCSD group in running the ASC, reviewed the strawman outputs, provided support for the maintenance activity, and responded to several national and international requests for information on the ARIES designs. Moreover, we actively participated in the March 1997 US/J workshop on Fusion Power Plants and in the June 1997 ARIES Materials Town Meetings organized by UCSD.

Appendix A

3.3. Neutronics

This section provides an overview of the neutronics activity performed for the various in-vessel components of the ARIES-RS power plant. The study is built upon the ARIES-II design [1] but addresses the engineering issues in greater detail. The neutronics assessment addresses the breeding level, service lifetime, radiation damage, shielding requirements, and personnel protection. An important outcome of the study is the specification of the radial builds that contain key component parameters in terms of sizes and optimal compositions. The material optimization was one of the themes of this study as it influences the neutronics results and helps meet the requirements while minimizing cost.

Major efforts were devoted to fulfill the top-level requirements which include the demonstration of a closed tritium fuel cycle, generation of no radioactive waste greater than Class C, and production of electricity at a competitive cost. The safety and economic requirements were constantly factored into the assessment to ensure that the most desirable safety features were integrated in the design in a cost-effective manner. The neutronics analysis and results for the individual components are summarized here with brief descriptions of the subsystems. Detailed technical information on each component can be found in the design description chapters of the individual components.

The neutronics calculations reported herein were performed using the discrete-ordinates DANTSYS code [2] with the P_3 - S_8 approximation and the FENDL-1 cross-section library [3] in the 46n-21g group structure. The model is a toroidal cylindrical geometry where the inboard and outboard sides are modeled simultaneously to simulate properly the neutron reflection and spectral effects between the two sides. The model includes all components comprising the fusion power core. Inside the bore of the TF magnets is the vacuum vessel (V.V.) which surrounds the internal removable components; these include the first wall (FW), blanket, reflector, shield, and divertor. These internal components serve to absorb most of the neutron energy and radiated heat from the plasma, protect the V.V. and magnets from excessive radiation damage, and breed sufficient tritium to assure the self-sustaining operation of the machine. The shield, V.V., and magnets are lifetime components designed to perform properly for ~ 50 years. The FW/blanket has limited life of ~ 3 years determined by the attainable radiation damage to the vanadium structure. Some components have an intermediate lifetime of ~ 10 years. The internals are divided toroidally into 16 sectors. One sector of the outboard contains the penetrations needed for the auxiliary RF heating and current drive systems. The entire torus is

housed in a cryostat surrounded by buildings containing the necessary support systems. The machine is maintained with robotic and remote handling systems.

A close attention was paid to the evolution of the nuclear cross section data library. The FENDL-1 library, used to evaluate the ARIES-RS study, was updated and improved through an international process [3]. When compared to the U.S. ENDF/B-V library [4, 5], used in the ARIES-II design, important differences were found between the two libraries as documented in Section 3.3.7.

3.3.1. Neutron Wall Loading Distribution

An essential element in the neutronics calculations is the poloidal distribution of the neutron wall loading (Γ). The distribution provides the peak values which are used to size the shield, determine components lifetimes, estimate damage levels, and assess the radiation environment around the torus. In essence, the ARIES systems code (ASC) can provide the distribution solely for a simple first wall that conforms the plasma surface. In fact, the ASC estimates fairly accurately the Γ variation over the inboard (i/b) and outboard (o/b) first walls. However, complex shapes, such as that of the divertor, cannot be modeled correctly and a simple wall that crosses the null point is used instead. This limitation has prompted the need for using the three-dimensional (3-D) Monte Carlo code MCNP [6] to accurately determine the wall loading distribution over the complex divertor surface. Furthermore, an accurate estimate for the wall loading at the critical shielding region behind the inner divertor plate where the shield recesses to accommodate the divertor system is essential to warrant adequate protection for the inner legs of the TF magnets.

The 3-D analysis was performed for an interim design with a major radius of 5.12 m and a fusion power of 1882 MW. The design has evolved during the course of the study and the final reference case calls for a major radius of 5.52 m and a fusion power of 2170 MW. The larger machine required a higher fusion power to generate the 1000 MW net electric power. Of interest is that the ASC results showed that the peak wall loadings at the i/b and o/b midplanes are almost identical for the two designs meaning that the 3-D Γ results for the divertor surface of the interim design are applicable to the final design.

The divertor shape, particularly the inner plate, was developed iteratively with guidance from the neutron wall loading analysis. The old divertor design had an almost flat inner surface that faces a large portion of the plasma and thus was subject to a high

neutron wall loading. It was thus recommended to curve the inner plate outward in the final design to help protect the surface of the recessed shield against source neutrons. The two divertor shapes were modeled for the MCNP code to quantify the drop in wall loading for the curved inner plate.

An output of the MCNP plotting routine is shown in Figure 3.3-1 for the FW configuration and old divertor design. Only the upper half of the machine was modeled due to symmetry. Figure 3.3-2 displays the latest divertor configuration where the inner plate is curved outward and the outer plate is curved inward. A combination of cones, tori, and cylinders was used to accurately model the first wall. The horizontal and vertical surfaces shown in the figures are used only to define the zones for the MCNP code. The FW was divided into 40 segments and particles crossing each segment were tallied. The 14 MeV neutrons were sampled from the plasma zone according to the source distribution provided by the ASC. The three central zones shown in the figures contain the source neutrons. The innermost plasma zone contains 63% of the source neutrons while the middle zone has 33% and the outer zone has 4%. The outermost zone (not shown) contains no neutrons. The direction of the source neutrons was sampled from an isotropic angular distribution. One million particles were sampled yielding a statistical uncertainty less than 1% at any FW segment.

The MCNP results show that the machine average neutron wall loading is 3.1 MW/m^2 for the case where the actual shape of the divertor is included in the FW area. Other parameters of interest are listed in Table 3.3-I along with those generated by the ASC for the final case. The machine average ASC wall loading is higher (4 MW/m^2) because it is based on a simple FW surface conforming the plasma. The wall loadings at the innermost surface of the divertor is 0.5 MW/m^2 for the flat inner plate of Figure 3.3-1 and 0.035 MW/m^2 for the curved inner plate of Figures 3.3-2. As expected, the curved surface provides better protection for the shield surface against source neutrons. As shown later in Chapter 6, the 0.04 MW/m^2 and the meter thick shield/V.V. result in an acceptable radiation level at the inner legs of the magnet. The poloidal variation of the wall loading is illustrated in Figure 3.3-3.

3.3.2. Tritium Self-Sufficiency

The top-level requirements state that the ARIES-RS design must demonstrate a closed tritium fuel cycle. This means the in-vessel components should supply all the tritium needed for the ARIES-RS operation (0.33 kg/day). An external tritium supply is only needed at the start of operation and for a short period of time until steady-state produc-

Figure 3.3-1. Vertical cut through ARIES-RS showing old divertor surfaces (output from MCNP plotting routine).

Figure 3.3-2. Vertical cut through ARIES-RS showing reference divertor surfaces (output from MCNP plotting routine).

Table 3.3-I.

**PEAK AND AVERAGE WALL LOADINGS FOR INBOARD,
OUTBOARD, DIVERTOR, AND OVERALL MACHINE**

	Interim Design	Final Design
Fusion power (MW)	1882	2167
Major radius (m)	5.12	5.52
Code	MCNP	ASC
Wall loadings (MW/m ²):		
Inboard peak	3.8	3.7
Outboard peak	5.3	5.6
Divertor peak	2.3	—
Inboard average	2.7	—
Outboard average	4.5	—
Divertor average	1.1	—
Machine average for a simple FW that crosses the null point	3.7	4.0
Machine average for the actual FW and actual divertor surface	3.1	—

Figure 3.3-3. Poloidal variation of neutron wall loading.

tion of tritium is reached. The tritium breeding ratio (TBR) in a self-sustained power plant must exceed unity by a small margin. This breeding margin is necessary mainly to supply the inventory for startup of other fusion reactors, to maintain the equilibrium holdup inventory, to provide adequate reserve storage inventory, and to compensate for the radioactive decay of T between production and use. A serious effort was developed to lower the breeding requirements by increasing T burnup in the plasma ($\sim 30\%$), ensuring extremely low T losses, achieving low T inventory in all subsystems, and requiring high reliability and short repair time for the T processing systems [1]. It appears that, with existing technology, a system with negligible T leakage can be achieved to guarantee a safe and reliable design.

In addition to the breeding margin, a provision should be made in the calculated TBR to account for uncertainties due to approximations and/or errors in the various elements of the calculations such as nuclear data, calculational method, and geometric representation [1]. It was found that the breeding margin amounts to only 0.01 for the ARIES-RS plasma parameters and T processing system whereas the largest source of uncertainty in the calculated TBR is that for the basic nuclear data ($\sim 6\%$) [7, 8, 9] and the geometrical approximations associated with the calculational model ($\sim 3\%$). These uncertainties constitute a large portion (0.09) of the excess breeding. In this regard, the ARIES-RS design will attain fuel self-sufficiency if the calculated TBR is equal to or exceeds 1.1. It is pertinent to mention that the TBR will be calculated for the reference design and the effects of the elements that degrade the breeding, such as penetrations, assembly gaps, and side walls, will all be included in the computed TBR.

An approximate method based on 1-D analysis has been established with the objective of ensuring that the error associated with the final results is within a few percent of the 3-D estimates [10]. As mentioned above, provisions were made in the tritium breeding requirements for such uncertainties in the results. Thus, the overall TBR has been estimated by coupling the 1-D results with the neutron coverage fractions of the i/b and o/b blankets. The neutron coverage fractions, representing the fraction of source neutrons going directly to each region, are 20% and 68% for the i/b and o/b blankets, respectively, and 12% for both the top and bottom divertor regions. These fractions were then modified for losses in available areas due to the presence of the plasma control ports and assembly gaps. The net effect of the ports (which occupy $\sim 1\%$ of the o/b FW area) and the 2 cm wide gaps on the overall TBR is about a 2% reduction. Unlike solid breeders, a Li/V system can easily achieve tritium self-sufficiency without a neutron multiplier [11].

The ARIES-RS design will rely on the i/b and o/b blankets to provide most of the required tritium and, therefore, no blanket will be installed in the divertor region. The reason is that the fraction of neutrons impinging on the divertor region is small relative to the i/b and o/b. Furthermore, these neutrons will be attenuated by the divertor plates and their support structures, leading to a further reduction in their contribution to the breeding. Minimizing the i/b radial build is well-known to be important for the overall size and cost of the machine. Therefore, it was decided to consider a thin blanket (20 cm) in the space limited inboard side and adjust the outboard blanket thickness to fulfill the breeding requirements. The blanket design is relatively simple. The lithium coolant/breeder flows poloidally from bottom to top. About 10% V structure is needed to support the blanket. The blanket is followed by a Li cooled reflector and shield. The analysis shows that 20 cm i/b blanket and 50 cm o/b blanket result in an acceptable TBR. The o/b blanket supplies $\sim 70\%$ of the tritium. The Li cooled reflector, divertor, and shielding components, other than FW and blanket, contribute $\sim 12\%$ to the overall breeding.

Designing the ARIES-RS plant for an overall TBR of 1.1 means that the actual achievable breeding ratio after the start of operation could range between 1.01 and 1.2 for a $\pm 9\%$ uncertainty. The achievable TBR in a realistic system will not be verified until after the operation of a demonstration plant. Underbreeding (overall TBR < 1.1) will generally place the plant operation at risk as the tritium bred may not suffice for machine operation. In this case, the plant will require an external tritium supply which is unacceptable for the ARIES designs as it violates the top-level requirements. It is therefore, imperative to conservatively design the ARIES plants with overbreeding blankets (overall TBR ≥ 1.1) providing that design solutions for reducing the breeding level by up to 20% should be established for all blanket designs. The adjustment in the design could take place during the second period of operation after changing out the blanket. For the Li/V system, a feasible solution would be to thin the o/b blanket by up to 20 cm. Alternatively, 2 or 4 o/b blanket modules could be converted into shield. Support calculations should be performed prior to the plant operation for the proposed solutions in order to assess the impact of the changes on the breeding level and related neutronics parameters.

3.3.3. Radiation Environment

The radiation levels at the ARIES-RS in-vessel components (FW, blanket, reflector, divertor, shield, and V.V.) and out-of-vessel components (magnet, cryostat, and

bioshield) are documented in this section. Also, the radiation effects on the various components (such as damage, limited lifetime, needed protection, limited personnel access, etc.) are discussed. ARIES-RS is a high power density machine that operates at a high level of neutron wall loading ($4\text{--}6\text{ MW/m}^2$) and thus has a relatively intense radiation environment inside and outside the torus. The vanadium structure exhibits high radiation damage and requires frequent replacement over the 50 years planned operation of ARIES-RS. The permanent components need a sizable shield to provide the necessary protection against radiation. For instance, in addition to the breeding blanket, about a meter of shield/V.V. is needed to adequately protect the magnets. The shield should also protect the V.V. and allow its reweldability at any time during operation. Hands-on maintenance is not feasible in the reactor hall as the biological dose outside the V.V. is quite high after shutdown. A 2.5 m thick concrete wall should surround the torus to protect the workers and general public against radiation during operation.

Radiation Damage to Structures

Two main structural materials are employed for ARIES-RS, vanadium and stainless steel (SS) alloy. V4Cr4Ti is the candidate vanadium alloy as it possesses high radiation resistance to neutron damage (refer to Section 5.2.2). Among the different types of steels, Tenelon was chosen for having superior shielding performance compared to modified HT-9 and other steels. For economic reasons, the use of V is limited to the plasma facing components where it is absolutely necessary for high temperature operation. The less expensive steel is used for components exposed to relatively lower radiation levels such as the back of the shield, V.V., magnet, and cryostat.

The atomic displacement, helium, and hydrogen production levels have the most impact on the neutron-induced effects in the V and steel alloys. The lifetimes of V and SS structures are determined by the dpa level attainable during operation. The criterion adopted in this study is that no more than 200 and 150 dpa are desirable for V and SS alloys, respectively [12]. A more restrictive limit is imposed on the steel of the V.V. To assure the reweldability of the V.V., the helium production level should not exceed 1 appm at any time during operation.

The radial variation of the neutron flux at the midplane of the outboard FW, blanket, shield, and V.V. is shown in Figure 3.3-4. For a neutron wall loading of 5.6 MW/m^2 , the FW flux peaks at a value of $6 \times 10^{14}\text{ n/cm}^2\text{s}$. The blanket and shield provide 5 orders of magnitude attenuation for the flux. The peak neutron-induced damage to the o/b FW structure at the end of one full power year (FPY) of operation are listed in Table 3.3-II. The radial variation of damage are illustrated in Figure 3.3-5, 3.3-6, and 3.3-7 for the outboard components which are subjected to the highest wall loading. The damage to

Figure 3.3-4. Radial attenuation of neutron flux in outboard components for 5.6 MW/m².

Figure 3.3-5. Radial variation of dpa rate in vanadium structure of outboard components for 5.6 MW/m^2 .

Figure 3.3-6. Radial variation of helium production rate in vanadium structure of out-board components for 5.6 MW/m^2 .

Figure 3.3-7. Radial variation of hydrogen production rate in vanadium structure of outboard components for 5.6 MW/m^2 .

Table 3.3-II.

**PEAK RADIATION DAMAGE TO
O/B V FIRST WALL AT 5.6 MW/m²**

Atomic displacement rate (dpa/FPY)	77
He production rate (appm/FPY)	315
H production rate (appm/FPY)	6,660
He appm/dpa	4.3
H appm/dpa	88

the V structure falls off rapidly as one moves radially away from the FW. Although the flux decreases by a factor of ~ 3 across the 50 cm thick blanket, the drop in damage, which is a spectrum dependent, ranges from 8 to 25, depending on the radiation effect. The analysis assumes that the gas production continues to build up within the structure with time. In reality, some of the gases will diffuse out of the hot structure (500–700°C) and the gas levels will be below the reported values. However, the MHD coating on the V structure will certainly affect the dynamics of the diffusion process. The available data are very limited and, therefore, the actual amount of gases retained in the irradiated V is not known at the present time.

The He production level at the V.V. at the end of the 40 FPY life is below the 1 appm limit, meaning that any part of the V.V. can be rewelded at any time during operation. Furthermore, the shield, which is primarily designed to protect the magnet, will also provide lifetime protection for the V.V. On the other hand, there is no experimental data on the limit for the reweldability of irradiated V alloys. It is predicted that the welding environment could be more important than the neutron-induced gas production for V [12]. Due to lack of data, a decision was made to assemble the in-vessel components using mechanical attachments and avoid welding the V components inside the high radiation environment.

Radiation Damage to Magnet

The radiation limits provided by the magnet designers are listed in Table 3.3-III. These limits must be met in order to avoid degradation of the magnet critical properties

Table 3.3-III.

RADIATION LIMITS FOR ARIES-RS SUPERCONDUCTING MAGNETS

Fast neutron fluence to Nb ₃ Sn conductor ($E_n > 0.1$ MeV)	10^{19} n/cm ²
Peak nuclear heating in winding pack	2 mW/cm ³
Total nuclear heating in magnets	50 kW
Peak dose to GFF polyimide insulator	10^{11} rad
Peak dpa to Cu stabilizer	6×10^{-3} dpa

due to radiation at any time during plant operation. The shield is designed primarily to protect the magnet. The blanket, reflector, and V.V. provide additional shielding for the magnet. Section 3.3.6 gives the radial dimensions of the various in-vessel components needed to reduce the radiation level below the specified limits. Details of the magnet damage profile and the basis for the shield design are documented in Chapter 6.

Radiation Level Outside Torus

Personnel accessibility to the torus and the feasibility of hands-on maintenance depend on the radiation level around the machine. A key factor in this assessment is the biological dose throughout the machine. This is shown in Figure 3.3-8 at different times after shutdown for the outboard FW and V.V. of ARIES-RS subjected to an average outboard neutron wall loading of 4.5 MW/m². According to DOE guidelines for protection of workers [13], personnel access is allowed in areas where the dose falls below 2.5 mrem/h. The results indicate that the area inside the cryostat is non-accessible and should be maintained by robotics and/or remote handling equipments. If hands-on maintenance or personnel access requirements are mandatory, then major changes to the shield design should be implemented in order to meet the 2.5 mrem/h limit. In this case, the machine will be heavily shielded as additional bulk shield, sizable shield around penetrations, local shields behind assembly gaps, and well shielded transporters and flasks are needed to meet the limit in the area outside the V.V. The extra shield will increase the cost of the machine by several mill/kW_eh, which is significant. However, once the machine is opened and the FW/blanket and shield are moved out for replacement, the outer areas will be highly contaminated. It is judged that the added cost and complexity offset the benefit of permitting hands-on maintenance in the limited area outside the V.V. for a short period

Figure 3.3-8. Contact dose at outboard first wall and vacuum vessel.

of time (a few hours) before opening the machine for maintenance. For these reasons, the ARIES-RS machine will be maintained by robotics and remote handling equipments. The specific requirements for remote handling and radiation hardened equipments are discussed thoroughly in Chapter 6.

The buildings surround the cryostat to protect the work-force and general public from radiation hazards within the plant. A 2.5 m thick steel-reinforced concrete is required to limit the dose to 0.5 mrem/h outside the bioshield during machine operation. The attainable dose level reflects a safety factor of 5 reduction in the absolute limit (2.5 mrem/h). A general guideline for the buildings containing the support systems is to limit as much as possible the areas where personnel access is not possible. Rooms for RF launchers and hot cells are non-accessible. The use of separation walls will create intermediate accessible areas close to these systems. Transition tunnels to hot cells should be well shielded. If personnel access is desirable outside the hot cell building, the walls should be 2 m thick. Other rooms containing less radioactive components (such as coils, cryostat dome, etc.) may have thinner walls (~ 0.5 m thick).

3.3.4. Components Lifetimes

For previous ARIES designs, the plant service lifetime was 40 years, consistent with the NRC fission plant license period. With a plant factor of $\sim 75\%$, the plant lifetime is ~ 30 FPY. It is prudent for advanced fusion designs to anticipate possible life extension by designing to a higher value, for example 40 FPY, as has been the case for ARIES-RS. For a plant factor of 80-90%, the service life could be extended to 45-50 years. This is a reasonable assumption as extended life of even 60-80 years are suggested for advanced fission plants. If desirable, a longer ARIES-RS life (> 40 FPY) will require a few centimeter additions to the radial dimensions of the replaceable components.

The lifetimes of the individual in-vessel components vary with the radiation damage level attainable during operation. Plasma facing components (PFC) are subjected to the highest flux and thus have the shortest lifetime. Larger cost items, such as shield and magnet, are designed to last for the plant life. The lifetime of the V structure is set by the 200 dpa limit. This limit implies that the PFC should be replaced every ~ 2.5 FPY at an end-of-life (EOL) fluence of 15 MWy/m^2 . The EOL fluence is based on a peak o/b neutron wall loading of 5.6 MW/m^2 that remains nearly constant over a poloidal height of a few meters around the midplane. Even though the i/b FW/blanket is subjected to a lower wall loading, it will be replaced with the o/b blanket on the same basis. There will certainly be an incremental cost associated with the early replacement of the

i/b blanket, but this will be offset by the gain due to the fewer maintenance processes for the combined i/b and o/b replacement scheme, shorter down time, and thus higher availability for the overall system. The replacement rate for the divertor plates is not determined by their radiation damage lifetimes, but rather by the W coating erosion rate. The divertor design was able to achieve an erosion lifetime similar to the radiation damage lifetime of the o/b FW/blanket. This achievement was essential to simplify the maintenance scheme and to meet the high availability goal.

The replaceable components include the first wall, divertor, blanket, reflector, divertor support structure, and outermost part of the i/b shield. The dpa level achieved after one FPY for all in-vessel components are listed in Table 3.3-IV. Note that all shields are lifetime components as they meet the 200 dpa limit at the 40 FPY end of plant life. It is pertinent to mention that the lifetime protection of the shield was achieved by adjusting the thicknesses of the o/b reflector, i/b replaceable shield, and divertor support structures so that the first layer of the shield has an acceptable damage level at EOL.

The desire for economic improvement led to an innovative replacement scheme. A cost saving was achieved by radially segmenting the replaceable in-vessel components into two zones so that the large and/or heavy outer segments can be reused. In addition to the cost savings, the segmentation will maximize the useful lifetime of the components and thus minimize the radwaste stream. The cost saving associated with this approach is evaluated with the perspective that the overall replacement cost is minimized and the radial partitioning is not too complex or time consuming. The replacement schemes for ARIES-RS are scheduled at ~ 2.5 FPY and 7.5 FPY. The second lifetime is 3 times the first in order to keep the 2.5 FPY components as thin as practically possible. Hence, the o/b blanket is divided into two zones (blanket-1 and blanket-2), the i/b blanket is separated from the i/b replaceable shield, and the divertor plates are attached to the divertor support structure. The inner components (i/b FW/blanket, o/b FW/blanket-1, and divertor plates) are replaced at ~ 2.5 FPY, while the outer components (i/b replaceable shield, divertor support structure, and o/b blanket-2/reflector) are replaced every ~ 7.5 FPY. Even though some components (like the o/b reflector and divertor support structure) have longer life, they will be replaced at 7.5 FPY to increase the availability of the system. This novel replacement scheme has reduced the annual replacement cost by ~ 2 mills/kW_eh ($\sim 3\%$ of COE) and decreased the cumulative radwaste by ~ 2000 tonnes ($\sim 13\%$ of FPC mass). The effect of the smaller frequency of replacement on the availability of the system was not quantified. Figure 3.3-9 displays the two sets of replaceable components. The outer 7.5 FPY components comprise a structural ring that provides poloidal continuity and attachment points for the inner 2.5 FPY components.

Table 3.3-IV.

**PEAK ATOMIC DISPLACEMENT IN THE VANADIUM STRUCTURE
OF IN-VESSEL COMPONENTS**

	Peak dpa @1 FPY	Lifetime for 200 dpa	
		FPY	MW _y /m ² *
Outboard Components (5.6 MW/m ²):			
FW and blanket-1	77	2.6	15
Blanket-2 and reflector	24	8.2	46
HT shield	5	40	225
Inboard Components (3.7 MW/m ²):			
FW/blanket	62	3.2	18
Replaceable shield	23	8.7	49
HT shield	4.8	42	235
Divertor Components (2.3 MW/m ²):			
Divertor plates	28	7.1	40
Support structure	17	11.8	66
HT shield	5	40	225

*Relative to o/b peak wall loading

Figure 3.3-9. Cross section of ARIES-RS showing lifetimes of various in-vessel components.

During maintenance, 1/16th of the ring is moved out radially as a single unit to a hot cell where the inner components are replaced and the outer components are reused until they reach their own useful life.

3.3.5. Nuclear Heat Loads

All in-vessel components (FW, blanket, divertor, reflector, and shield) are power production units except the rear portion of the shield. The exclusion of the back of the shield from producing power was made for economic and safety reasons as described later in Chapter 6. Briefly, a cost reduction was achieved by limiting the use of V in the shield and instead employing steel-based materials which are remarkably cheaper. This approach degrades the gross thermal conversion efficiency of the system somewhat since steel cannot operate at temperatures as high as V. The nuclear energy deposited at the back part of the shield will not be recovered and the dividing boundary between the vanadium and steel shields is determined such that $\sim 1\%$ of the power is dumped as low grade heat. The corresponding reduction in the useful thermal power was minimal (~ 20 MW) but the reduction in the shield cost was significant ($\sim 20\%$). Another advantage of this approach is that the steel shield operates at low temperature (200°C) and thus acts as a heat sink during a loss-of-coolant accident. In other words, the LT shield will help the FW temperature to drop faster after the first day in case of an accident (refer to Section 10.4).

The breakdown of the heat deposited in the FW, divertor, blanket, reflector, and shield is given in Table 3.3-V. The machine produces 2167 MW of fusion power and 80% of this power is carried by neutrons. Neutrons and gammas deposit their energies in the various components generating an additional 430 MW of thermal power. Hence, the design has an overall neutron energy multiplication (M) of 1.2. A large fraction of the power (70%) goes to the blanket.

One of the items that contributes to the attractiveness of the design is the neutron energy multiplication of the system. From an economic point of view, it is desirable to develop a system with high M that leads to a combination of high gross thermal efficiency, compact machine, and low cost of electricity. The energy multiplication of ARIES-RS is evaluated with the international FENDL-1 data library [3] following the same methodology used to estimate the overall TBR. Even though the two systems are quite similar, M of ARIES-RS is smaller than that of ARIES-II (1.37) which was evaluated with the U.S. ENDF/B-V data library [4, 5]. The difference in M is strictly attributed to the cross section library evaluation. The 12% reduction in M corresponds

Table 3.3-V.

**NUCLEAR HEATING DEPOSITED IN THE VARIOUS IN-VESSEL
COMPONENTS**

	Inboard	Outboard	Divertor	Total
FW or divertor plates	10	23	54	87
Blanket-1	285	680	—	965
Blanket-2	—	445	—	445
Reflector, replaceable shield, or divertor support structure	150	115	102	367
HT shield	48	122	46	216
Total	493	1385	202	2080

to a 1.5 mill/kW_eh increase in the ARIES-RS overall cost of electricity. This shows the important role of the cross section libraries in power plant designs and the need for validation and checking against experimental data.

3.3.6. Radial Builds

This section contains a description of the radial builds emphasizing the impact of the neutronics and shielding analyses on the dimensions and constituents of the in-vessel components. Tables 3.3-VI and 3.3-VII summarize the final dimensions and compositions provided by the engineering group with guidance from neutronics, thermal hydraulic, structural, economic, and safety analyses. Figures 3.3-10, 3.3-11, and 3.3-12 show schematically the i/b, o/b, and divertor radial builds. The given dimensions herein are at the i/b and o/b midplanes and for a vertical cross section through the divertor region.

The neutronics analysis forms the basis for most of the in-vessel component dimension/composition. The blanket is sized to supply the plant with the tritium needed

Table 3.3-VI.

RADIAL DIMENSIONS OF ARIES-RS COMPONENTS

	Inboard	Outboard	Divertor
Thicknesses (cm):			
FW or divertor plates	0.3	0.3	5
Blanket-1	20	20	0
Gap	1	1	1
Blanket-2*	—	30	—
Replaceable shield,* reflector,* or divertor support structure*	20	7	20
Assembly gap	1	1	1
Permanent shield:	56	70	82
HT shield	26	28	35
Gap	2	2	2
LT shield	28	40	45
Assembly Gap	2	> 2	> 2
Vacuum vessel	20	30	20
Total FW/B/R/S/V.V./gaps	120	161	130
V.V.-magnet gap	5	> 5	> 5
Winding pack	~ 50	~ 50	~ 50
Cryostat	24	24	24

*Comprise the structural ring

Table 3.3-VII.

MAIN COMPOSITIONS OF ARIES-RS COMPONENTS

First Wall	100% V Structure
Blanket	10% V Structure
	90% Li breeder/coolant
i/b Replaceable Shield	15% V Structure
	10% Li coolant
	75% V Filler
o/b Reflector	15% V Structure
	10% Li coolant
	75% Tenelon Filler
i/b permanent shield:	
HT shield	15% V structure
	5% Li coolant
	76% Tenelon Filler
	4% W structure
LT shield	15% Tenelon structure
	5% Helium coolant
	53% B ₄ C Filler (90% d.f.)
	27% WC Filler (95% d.f.)
o/b permanent shield:	
HT shield	15% V structure
	5% Li coolant
	73% Tenelon Filler
	7% W structure
LT shield	15% Tenelon structure
	5% Helium coolant
	80% Borated Tenelon Filler
Divertor Plates	90% V Structure
	6% Li coolant
	4% W coating

MAIN COMPOSITIONS OF ARIES-RS COMPONENTS (Cont.)

Divertor Support Structure	15% V Structure
	25% Li coolant
	60% MHT-9 Filler
Divertor permanent shield:	
HT shield	15% V structure
	5% Li coolant
	75% Tenelon Filler
	5% WC filler (95% d.f.)
LT shield	15% Tenelon structure
	5% Helium coolant
	77% B-Tenelon Filler
	3% WC filler (95% d.f.)
Vacuum Vessel:	
Inboard	35% Tenelon Structure
	5% Helium coolant
	40% B4C Filler (90% d.f.)
	20% WC Filler (95% d.f.)
Outboard	25% Tenelon Struc.
	5% Helium coolant
	70% B-Tenelon Filler
Divertor	35% Tenelon Structure
	5% Helium coolant
	57% B-Tenelon Filler
	3% WC filler (95% d.f.)
Magnet	73% 316SS structure
	14% Cu
	6% Nb3Sn/NbTi
	3% GFF Polyimide
	4% LHe
Cryostat	18% 316SS Structure
	82% Void

Figure 3.3-10. Inboard radial build.

Figure 3.3-11. Outboard radial build.

Figure 3.3-12. Divertor radial build.

for self-sufficient operation ($TBR \geq 1.1$). Moving away from the i/b midplane, the i/b blanket fans out and the i/b FW conforms to the plasma boundary. The i/b shield has vertically straightened surfaces. The i/b blanket is 35 cm thick at the top/bottom end. The curved blanket provides an additional shielding for the magnet and helps reduce the integral nuclear heating in the inner legs of the coil by a factor of ~ 2 , compared to a straightened blanket.

The radiation limits for the structure and magnet played a key role in determining the radial dimensions of the various components. For instance, the i/b replaceable shield and reflector are sized to result in 200 dpa to the V structure of the shield at the end of the 40 FPY plant life. The specified dimensions of the shields fulfill the radiation limits listed in Table 3.3-III and protect both magnet and V.V. for 40 FPY. The shield is divided into HT and LT shields for economic and safety reasons. The He cooled LT shield is sized to contain 1% (~ 20 MW) of the total nuclear heating deposited in all components. The space between the coolant channels of the shield is filled with inexpensive filler materials, instead of solid structure, to reduce cost. The i/b replaceable shield is made out of V, instead of steel, to reduce the decay heat in case of an accident. The W stabilizing shells are embedded in the shield and occupy 4-7% of the i/b and o/b HT shield.

The HT shield contains steel (Tenelon and MHT-9) filler while the use of more efficient materials (WC, B_4C , and borated steel) is limited to the LT shield where the radiation environment is less severe. To reduce the size and cost of the overall machine, the highly efficient and more expensive WC and B_4C fillers are utilized in the space-limited inboard side while borated steel is used elsewhere. WC filler is used in the 2 m high central zone around the i/b midplane whereas B_4C fills the remaining space of the i/b LT shield. Tenelon has a superior shielding performance compared to other steels. It is thus employed as the main filler and structural material for all steel-based components except in the divertor support structure. Instead, modified HT-9 is used for having lower decay heat, a property of great importance for highly irradiated components. The divertor support structure contains coolant tubes for divertor system, i/b blanket, and replaceable shield. Above/below the i/b blanket, the surface of the shield is recessed to accommodate the inner divertor plates. The shielding space therein is constrained and thus WC filler is used in the LT shield and V.V. to adequately protect the inner legs of the magnet.

The given dimensions of the shield are required to protect the magnets for peak i/b, o/b and divertor wall loadings of 3.7, 5.6, and 3 MW/m², respectively. Several scaling laws were developed to help the systems code adjust the LT shield size with any changes in

wall loading encountered during the design optimization process. The LT shield thickness (in m) scales with the peak wall loading ($\hat{\Gamma}$) as

$$0.28 + 0.048 \ln (0.2725 \times \hat{\Gamma}_{i/b}) \quad \text{for the i/b LT shield}$$

$$0.4 + 0.0735 \ln (0.178 \times \hat{\Gamma}_{o/b}) \quad \text{for the o/b LT shield}$$

$$0.45 + 0.0733 \ln (0.345 \times \hat{\Gamma}_{div}) \quad \text{for the divertor LT shield.}$$

The composition of the vacuum vessel is driven by shielding considerations. The V.V. is the closest component to the magnet and thus its composition affects the radiation level at the magnet. It is a double wall He cooled steel structure stiffened with steel ribs. For shielding considerations, the 13-23 cm thick space between the face sheets (3 cm thick each) is filled with borated steel plates except on the space-limited i/b side where more efficient shielding materials (WC and B₄C) are employed.

The elements surrounding the winding packs were included in the neutronics model as they provide additional shielding for the coils. The winding pack is surrounded by a 2 cm steel plate followed by 0.2 cm electric insulation, 1 cm steel jacket for handling protection, and 4 cm thermal insulation (Al foils separated by fiberglass papers). To reduce the radial standoff on the inboard side, the thermal insulation is placed in the gap between the V.V. and magnet. The cryostat surrounds the entire torus. It consists of 2 cm thick steel face sheets, inter-connected with steel ribs. The steel comprises 18% of the cryostat volume.

The radial builds include the gaps between the various in-vessel components. In addition to the 2 cm wide radial assembly gaps between the blanket/shield modules, toroidal/poloidal gaps are provided between the various components within each module. Generally, the toroidal/poloidal gaps between the same-temperature components are 1 cm thick while that between the permanent shielding components with different operating temperatures are at least 2 cm thick. The V.V. - magnet gap is 5 cm thick on the i/b and much wider elsewhere.

3.3.7. Impacts of New Data Library

Important differences were found due to the new Fusion Evaluated Nuclear Data Library (FENDL) that has been developed over the past few years. The library is constantly updated through an international process led by the International Atomic Energy Agency (IAEA). The FENDL library is a comprehensive collection of high-quality nuclear data selected from the various existing U.S. and international data libraries. The

selected data was checked against existing experimental data on tritium production, nuclear heating, and induced activation. The first version FENDL-1 library [3] is available and has been used in the neutronics and activation analyses of ARIES-RS. At present, IAEA is processing an improved FENDL-2 evaluation that will be available to the fusion community in the near future.

It was observed that some of the FENDL-1 results for the ARIES-RS design are quite different from the ARIES-II results evaluated with the U.S. ENDF/B-V library [4, 5]. This has prompted the need to benchmark the two libraries for the key neutronics parameters. We discovered that the FENDL-1 prediction for the H gas production in V, which is based on the U.S. ENDF/B-VI evaluation, greatly exceeds what would be obtained using the ENDF/B-V library. Table 3.3-VIII compares the two evaluations for the radiation damage to the vanadium structure of the o/b first wall. The FENDL-1 library predicts 10% higher dpa rate, 33% lower He production rate, and a factor of 3 higher H production rate for V. The latter raised some concerns within the materials community as hydrogen may embrittle the vanadium particularly during shutdown when the temperature drops below 500°C. Further investigation has revealed that among the various (n, H) cross sections for V, the $(n, n'p)$ cross section has increased significantly in the FENDL-1 evaluation. This inconsistency has been brought to the attention of the LANL data processing group to validate the ENDF/B-VI data for V. The identified discrepancy will be removed in the forthcoming FENDL-2 data library.

The tritium production level for the Li/V system is almost identical for the two libraries. On the other hand, a large discrepancy was found in the energy multiplication value. The difference is 12% (1.2 vs. 1.37) and is strictly attributed to the cross section data evaluation. Evidently, the ENDF/B-V evaluation tends to overestimate the nuclear heating deposited in the various components because the FENDL-1 results are in a good agreement with the old ENDF/B-IV heating values. The impact of the new library on the radiation effect at the magnet is shown in Table 3.3-VIII. The FENDL-1 library results in 5–20% lower magnet damage with the largest difference in the nuclear heating. Similar observations were made for a comparative study performed for the ITER design [14, 15]. In summary, this activity indicates that data uncertainties remain an important issue and highlights the need to maintain close and ongoing attention to the evolution of nuclear data libraries.

Table 3.3-VIII.

COMPARISON BETWEEN NEUTRONICS PARAMETERS
EVALUATED WITH FENDL-1 AND ENDF/B-V LIBRARIES

	FENDL-1	ENDF/B-V
First Wall Damage:		
Atomic displacement rate (dpa/FPY)	77	69
He production rate (appm/FPY)	315	450
H production rate (appm/FPY)	6,660	2,170
Overall TBR	1.1	1.1
Overall M	1.2	1.37
Magnet Damage:		
Peak fast neutron fluence (n/cm^2 , $E_n > 0.1$ MeV)	1×10^{19}	1.07×10^{19}
Peak nuclear heating (mW/cm ³)	0.4	0.5
Peak dose to GFF polyimide (rad)	1.4×10^{10}	1.5×10^{10}
Peak dpa to Cu stabilizer (dpa)	5.4×10^{-3}	5.7×10^{-3}

REFERENCES

- [1] F. Najmabadi et al., "The ARIES-II and -IV Second Stability Tokamak Reactors," University of California San Diego Report, UCSD-ENG-002 (1996).
- [2] R. Alcouffe, R. Baker, F. Brinkley et al., "DANTSYS: A Diffusion Accelerated Neutral Particle Transport Code System," Los Alamos National Laboratory report LA-12969-M (1995).
- [3] A. Pashchenko, H. Wienke, S. Ganesan, and P. McLaughlin, "FENDL/E-1.0, Evaluated Nuclear Data Library of Neutron Nuclear Interaction Cross Sections and Photon Production Cross Sections and Photon-Atom Interaction Cross Sections for Fusion Applications," Report IAEA-NDS-128 Rev. 2, International Atomic Energy Agency (Nov. 1995).
- [4] R. Kinsey, "ENDF/B Summary Documentation," National Nuclear Data Center, Brookhaven National Laboratory Report BNL-NCS-17541 (ENDF-201), 3rd Ed., ENDF/B-V (1979).
- [5] Y. Farawila, Y. Gohar and C. Maynard, "KAOS/LIB-V: A Library of Nuclear Response Functions Generated by KAOS-V Code from ENDF/B-V," Argonne National Laboratory Report ANL/FPP/TM-241 (April 1989).
- [6] "MCNP - A General Monte Carlo Code for Neutron and Photon Transport, Version 3A," Los Alamos National Laboratory report LA-7396-M, Rev. 2 (1986).
- [7] M. Z. Youssef and M. A. Abdou, "Uncertainties in Prediction of Tritium Breeding in Candidate Blanket Designs Due to Present Uncertainties in Nuclear Data Base," *Fusion Technology* **9** (1986) 286.
- [8] M. Z. Youssef, A. Kumar, and M. Abdou, "The Prediction Capability for Tritium Production and Other Reaction Rates in Various Systems Configurations for a Series of the USDOE/JAERI Collaborative Fusion Blanket Experiments," *Fusion Engineering and Design* **18** (1991) 407.
- [9] M. Abdou, H. Maekawa, Y. Oyama, et al., "Japan Atomic Energy Research Institute/United States Integral Neutronics Experiments and Analysis for Tritium Breeding, Nuclear Heating, and Induced Radioactivity," *Fusion Technology* **28** (1995) 5.
- [10] M. Sawan, Y. Gohar, and H. Attaya, "Three-Dimensional Neutronics Analysis of the U.S. Blanket Design for ITER," *Fusion Technology* **19** (1991) 1513.

- [11] L. A. El-Guebaly, "Breeding Potential of Candidate Breeders for the U.S. Demo Plant," *Proceedings of 16th IEEE Symposium on Fusion Energy*, Champaign, IL, Vol. 2 (Oct. 1995) 1198.
- [12] M. Billone, Argonne National Laboratory, private communication (1996).
- [13] "Standards for Protection Against Radiation," 10CFR Part 20, Nuclear Regulatory Commission (1975).
- [14] M. Sawan, "FENDL Neutronics Benchmark: Specifications for the Computational Neutronics and Shielding Benchmark," IAEA Nuclear Data Section Report INDC(NDS)-316 (December 1994).
- [15] M. E. Sawan, "Application of FENDL Nuclear Data to ITER Neutronics and Shielding Analyses," *Proceedings of 1996 Radiation Protection and Shielding Topical Meeting*, North Falmouth, MA, April 21-25, 1996, p. 679.

3.4. ELECTROMAGNETIC DISRUPTION ANALYSIS

3.4.1. Introduction

Scoping calculations were performed to estimate the electromagnetic forces and stresses in the ARIES-RS device resulting from plasma disruption events. A disruption event in a tokamak usually begins with a very rapid loss of plasma thermal energy confinement causing a decrease in plasma temperature. Cold plasmas have a high electrical resistance, so the plasma current decays rapidly.

When the plasma current decreases, the poloidal magnetic field decreases as well. This changing magnetic field produces a toroidal electric field which drives toroidal currents in the conducting material surrounding the plasma. The plasma acts as the primary winding of a transformer driving current in the secondary winding—the surrounding conductive material. Toroidal currents are also produced by plasma motion. In non-axisymmetric configurations, poloidal currents flow as well.

Because these induced currents flow across a magnetic field, they apply a Lorentz force to the components in which they flow. These forces can damage structures inside the reactor if they become excessive. If the current flows in the toroidal direction only, the magnetic force density (magnetic force per unit volume) at a given point is the product of the current density and the poloidal component of the magnetic field. Thus, the objective is to design the reactor so that the proportion of current flow to mechanical strength is appropriate for the field strength in each component. The problem is complicated by the interaction of the conducting components.

The poloidal component of the magnetic field results from several sources. The plasma current itself produces a poloidal field which, like the current, decreases to zero at the end of the disruption. Since the induced currents generally rise with time during the disruption, the resultant force peaks at some intermediate time. In this analysis, the poloidal field magnets are assumed to remain energized during the disruption, producing a steady poloidal field. Finally, poloidal fields result from the induced toroidal currents in the structure.

Non-axisymmetric geometries, such as from the segmentation of the first wall, blanket and shields, produce poloidal currents which interact with the strong toroidal field. The resulting forces may be significant; however, a more complex analysis would be required to evaluate these currents.

This analysis only considers the induction of current. It has been suggested that significant current may be deposited directly into the plasma-facing components by contact of the plasma with the wall. This effect is not considered in this analysis and in cases where plasma motion occurs, the size of the plasma is altered to prevent contact with the walls. The conductivity of the components may affect the stability of the plasma against disrupting in the first place. This interaction was also beyond the scope of this study.

There are a variety of techniques for evaluating the induced currents and resultant forces and stresses. In this analysis, the induced currents and forces were computed using the commercial finite element code ANSYS®. Toroidally symmetric geometry and loads were assumed; however, toroidal segmentation of the divertor plates was accounted for by considering the added electrical resistance from current flow through the support structure. Only the toroidally continuous components, the vacuum vessel, vertical stabilizing shells, and bucking cylinder, along with the divertor plates were considered. The currents and forces were found to be greatest at the end of the disruption event and were essentially independent of the duration of the disruption, except in the divertors. The greatest stresses appear in the outboard stabilizing shells. These components need to be supported continuously around their entire circumference to sustain a disruption loading. All components were able to withstand their electromagnetic loads. Further study which considers the three-dimensional nature of the problem should be undertaken when greater accuracy is warranted.

Several scenarios of fast plasma current quenches and vertical displacement events were considered. These were considered to be representative of the most severe disruption events seen in tokamak experiments.

3.4.2. Assumptions

As this analysis is a scoping calculation, several simplifying assumptions were made to reduce its computational burden. The components of the ARIES-RS device were idealized as toroidally continuous structures with toroidally uniform material properties. Several components in the reactor are segmented such as the first wall, blanket and shield modules. In these components, current must pass through the support structure to flow from one module to the next. In this analysis, it was assumed that the resistance of this path is large enough to neglect this current. Current loops will form in the modules due to non-uniformity of the induced electric field applied by the decaying plasma current. These current loops cross the toroidal magnetic field in the sides of the modules and may exert significant torques on the modules. However, a more sophisticated

model would be required to compute these currents. Only the toroidally continuous components, the vacuum vessel, vertical stabilizing shells, and bucking cylinder, along with the divertor plates were considered. All other structures were assumed to have no electrical conductivity.

The poloidal field coils remain energized during the disruption, maintaining the currents set at the beginning of the burn cycle when currents and fields are near their peak.

It is also assumed that the equilibrium plasma current is distributed over a circular region. See Figure 3.4-1. Within that circle, the current density is assumed to be given by

$$J = J_{max} \left(1 - \frac{r^2}{r_{minor}^2} \right)^{\alpha_J} \quad (3.4-1)$$

where r_{minor} is the plasma minor radius and

$$J_{max} = \frac{(1 + \alpha_J) I_{plasma}}{\pi r_{minor}^2} \quad (3.4-2)$$

which results in a total plasma current of I_{plasma} . In Equations 3.4-1 and 3.4-2, $r_{minor} = 1.31$ m, $I_{plasma} = 10.42$ MA and $\alpha_J = 0.374$. This distribution is plotted in Figure 3.4-2.

During the plasma current decay phase of a disruption, this equilibrium distribution is linearly ramped down to zero at all points. Vertical displacements of the plasma are considered by reducing the size of the plasma to prevent contact with the walls. See Figure 3.4-3.

3.4.3. Modeling

In modeling the ARIES-RS reactor, axisymmetric quadratic finite elements capable of modeling the radial and axial magnetic field and toroidal currents in the structure were used. The finite element model is shown in Figure 3.4-4.

The divertors were modeled as three 4.4 cm thick, toroidally continuous plates. However, the divertor plates are not toroidally continuous but instead are segmented into 16 modules. Currents induced in these plates must flow through the support structure to complete a toroidal circuit. To consider the additional resistance of this path, the resistivity of the divertor plates in the model was selected so that the total resistance in the model matched the total resistance through the plates and supports. The supports were assumed to be 1.5 m long with 14 cm² cross-sectional area on each side of each

Figure 3.4-1. Plasma current is distributed over a circle.

Figure 3.4-2. Plasma current distribution.

plate. Current loops within individual divertor plates were not considered. The electrical resistivities of the divertor plates and all other components in the model are shown in Table 3.4-I.

Three scenarios were chosen because they were representative of the most severe seen experimentally [1]. The first is a fast plasma current quench of 10 ms where the plasma remains stationary during the disruption. The second is a simultaneous plasma current quench and vertical plasma drift of 1.5 m occurring over 100 ms. The last is a slow vertical drift phase of 1.5 m lasting 2500 ms followed by a rapid plasma current quench of 10 ms. A current quench is modeled as a linear current ramp from 10.42 MA to zero.

3.4.4. Results

The currents and forces were generally found to be greatest at the end of the disruption events. The quench times considered here are sufficiently short so that all the components, except the divertor plates, experience a roughly linearly rise in current with time. The higher resistance of the divertor plates causes their current to level off before the end of the disruption. For the other components, the rate of the linear rise in current is inversely proportional to the duration of the disruption. Thus, their total current and load is essentially independent of the duration of the disruption.

The greatest forces appear in the outboard stabilizing shells. These components need

Figure 3.4-3. Vertical displacements of the plasma are treated by reducing the minor radius of the plasma.

Figure 3.4-4. The finite element model used to compute the induced currents in the structure from a disruption.

Component	Material	Thickness (cm)	Resistivity ($\mu\Omega\cdot m$)
divertor plates	100% vanadium	4.3	32*
inboard vacuum vessel	35% tenelon, 40% B ₄ C, 20% WC, 5% void	20	0.24
divertor region vacuum vessel	57% borated tenelon, 35% tenelon, 3% WC, 5% void	20	0.63
outboard vacuum vessel	70% borated tenelon, 25% tenelon, 5% void	30	0.66
inboard stabilizing shell	100% tungsten	5	0.36 @ 1100 °C
outboard stabilizing shell	100% tungsten	6	0.36 @ 1100 °C
bucking cylinder	100% tenelon	27	0.69

*effective resistivity after considering current path through support structure

Table 3.4-I. The electrical resistivities of the materials in the model.

to be supported continuously around their entire circumference to sustain a disruption loading. The bucking cylinder participates only weakly in the disruption. The currents and loads in this component are small compared with other structures. All components were able to withstand their electromagnetic loads if adequately supported.

For the 10 ms stationary plasma disruption, the eddy currents in the reactor peak at 10 ms with a total of 9.9 MA. The 100 ms disruption with plasma drift generates the same peak eddy currents at the end of the disruption as well. The 2500 ms drift and fast quench scenario induces a maximum of 9.8 MA at the end of the quench.

3.4.4.1. Vacuum vessel

The maximum current and loads in the vacuum vessel occur at the end of the disruption in all scenarios. The greatest current density appears on the inboard side on the midplane. Figure 3.4-5 shows the current density in the vacuum vessel at the end of the 10 ms quench. In all cases, the greatest electromagnetic pressure appears on the inboard side near the midplane (the magnetic field is greater there than at the midplane). The magnetic force density for the 10 ms quench is shown in Figure 3.4-6.

The maximum magnetic pressures and stresses for all three scenarios are shown in Table 3.4-II.

Figure 3.4-5. Maximum current density in the vacuum vessel following a 10 ms centered disruption.

Case	Peak Total Current (MA)	Maximum Magnetic Pressure (MPa)	Maximum Stress (MPa)
10 ms quench	4.6 @ 10 ms	0.66 @ 10 ms	9.3 @ 10 ms
100 ms quench with drift	4.9 @ 100 ms	0.66 @ 100 ms	9.2 @ 100 ms
2500 ms drift then 10 ms quench	3.6 @ 2510 ms	0.74 @ 2510 ms	10 @ 2510 ms

Table 3.4-II. Maximum magnetic pressures and stresses in the vacuum vessel

Figure 3.4-6. Maximum magnetic force density in the vacuum vessel following a 10 ms centered disruption.

3.4.4.2. Divertor

The divertor is modeled as three plates. The loads and stresses on the inboard plate are minimal. The loads and stresses for the center and outboard plates are summarized in Tables 3.4-III and 3.4-IV.

These tables list total radial and axial forces on each plate over 360° (divide by 16 to compute the force for a single module). Positive radial force is away from the machine's centerline; positive axial force is away from the midplane. The maximums are for either upper or lower divertor plates.

The greatest loads appeared in the outboard divertor plates. Note that no stresses exceed 10 MPa. Given that the allowable primary bending stress for vanadium up to divertor plate operating temperatures is 165 MPa, these stresses should not present a concern.

3.4.4.3. Vertical stabilizing shells

The loads on the tungsten vertical stabilizing shells are larger than the other components, due to their proximity to the plasma. In this idealized model, the loading is toroidally uniform, and if the shells were free to expand, they would act as free-standing rings and would not react radial loads into their support structure. However, non-uniform loads could cause the shells to buckle so supports will be needed.

Inboard vertical stabilizing shells. The maximum loads on the inboard vertical stabilizing shells are shown in Table 3.4-V. This table shows the maximum total current and maximum total radial and axial forces on the shells. These forces are the total over 360° (the net radial force is zero; this is the force per unit length multiplied by the circumference). Again, positive radial force is away from the machine's centerline; positive axial force is away from the midplane. Note that these are the maximums for either inboard shell.

The current and load in each inboard shell is concentrated on the near-midplane edge of the shell. See Figure 3.4-7 which shows the current density (A/m^2) and Figure 3.4-8 which shows the magnetic force density (N/m^3). Note in Figure 3.4-8 that the magnetic field due to the current in the shell itself causes the forces to be largely directed inward in the component. The forces due to this field sum to zero over the component. Only external magnetic fields cause a net load on the shell.

Case	Peak Total Current (MA)	Peak radial Force (MN)	Peak axial Force (MN)	Maximum Stress (MPa)
10 ms quench	0.022 @ 10 ms	-0.26 @ 10 ms	0.28 @ 10 ms	5.1 @ 10 ms
100 ms quench with drift	0.0066 @ 100 ms	-0.080 @ 100 ms	0.084 @ 100 ms	1.5 @ 100 ms
2500 ms drift then 10 ms quench	0.065 @ 2510 ms	-0.44 @ 2510 ms	0.59 @ 2510 ms	10 @ 2510 ms

Table 3.4-III. Maximum stresses and total radial and axial forces on the center divertor plate. Forces are a 360° total. Positive axial forces are away from the midplane.

Case	Peak Total Current (MA)	Peak radial Force (MN)	Peak axial Force (MN)	Maximum Stress (MPa)
10 ms quench	0.038 @ 10 ms	-0.57 @ 10 ms	0.320 @ 10 ms	9.5 @ 10 ms
100 ms quench with drift	0.006 @ 100 ms	-0.079 @ 100 ms	0.083 @ 100 ms	1.8 @ 100 ms
2500 ms drift then 10 ms quench	0.12 @ 2510 ms	-0.98 @ 2510 ms	-1.8 @ 2502 ms	7.5 @ 2510 ms

Table 3.4-IV. Maximum stresses and total radial and axial forces on the outboard divertor plate. Forces are a 360° total. Positive axial forces are away from the midplane.

Case	Peak Total Current (MA)	Peak Radial Force (MN)	Peak Axial Force (MN)
10 ms quench	0.93 @ 10 ms	3.8 @ 7 ms	9.7 @ 10 ms
100 ms quench with drift	0.82 @ 100 ms	3.4 @ 80 ms	9.2 @ 100 ms
2500 ms drift then 10 ms quench	1.5 @ 2510 ms	14 @ 2508 ms	21 @ 2510 ms

Table 3.4-V. Maximum total radial and axial forces on the inboard stabilizing shell.

Figure 3.4-7. Maximum current density in A/m^2 in an inboard vertical stabilizing shell following a 2500 ms vertical drift and fast quench.

Figure 3.4-8. Maximum magnetic force density in N/m^3 in an inboard vertical stabilizing shell following a 2500 ms vertical drift and fast quench.

Case	Peak Total Current (MA)	Peak Radial Force (MN)	Peak Axial Force (MN)
10 ms quench	1.7 @ 10 ms	-47 @ 10 ms	-13 @ 7 ms
100 ms quench with drift	1.6 @ 100 ms	-46 @ 100 ms	-12 @ 50 ms
2500 ms drift then 10 ms quench	3.2 @ 2510 ms	-89 @ 2510 ms	33 @ 2500 ms

Table 3.4-VI. Maximum total radial and axial forces on the outboard stabilizing shell.

These loads are substantial. Periodic spacing of supports would cause unacceptable bending stresses in the shells. Continuous support structure along the near-midplane edge is needed for these components.

Outboard vertical stabilizing shells. The maximum loads on the outboard vertical stabilizing shells are shown in Table 3.4-VI. This table shows the maximum total current and maximum total radial and axial forces on the shell as in Table 3.4-V.

The current and load in each outboard shell is concentrated on the near-midplane edge of the shell as in the inboard components. Continuous support along the near-midplane edge is necessary for these components.

3.4.5. Summary and Conclusions

The limitations of the analysis done here should not be overlooked. The currents in the first wall, blanket and shields may be significant and may generate significant torques on the modules. A more sophisticated model is needed to study these effects.

The present design of the vacuum vessel, divertor and bucking cylinder appear to be acceptable. The vertical stabilizing shells need to be supported continuously along their near-midplane edge. These supports must react substantial loads but should not be impractical.

REFERENCES

- [1] R. O. Sayer, "TSC Disruption Simulations for the ITER 24 MA Design," ITER/US/95/INV-VV-01, 1995.

5.2. Title

5.2.3. FW/B/S Mechanical Configuration

5.2.3.1. Overall Description

The in-vessel components of the ARIES-RS power plant are divided into three categories: components with a life of 2.5 FPY (full power years), components with a life of 7.5 FPY and components which can last the full life of the plant, projected at 40 FPY. The rationale for this segmentation is to reduce downtime for component replacement while maximizing the utilization of structural material and consequently, reducing the amount of radwaste.

Component lifetimes are determined by radiation damage to the load carrying structural materials. In this case, the lifetime of the V structure has been set at 200 dpa (displacements per atom), equivalent to a fluence of 15 MWy/m². The peak neutron wall loading on the OB (outboard) side is 5.6 MW/m² and thus the lifetime is rounded off to 2.5 FPY. Even though the IB (inboard) components and diverter plates do not achieve 200 dpa in 2.5 FPY, they will be replaced at the same frequency to avoid an intermediate replacement which will reduce plant availability. Each third replacement, the second category of components, namely those with a lifetime of 7.5 FPY will be replaced. Finally, the third category of components will not be replaced during the life of the reactor, except in the case of failure due to non-radiation damage reasons. This could be a coolant leak or a structural failure brought on by unusual events.

Figure 5.2-1 is a cross section of the blanket and shield components at a location between TF coils. The heavily cross hatched region consisting of the IB structural ring, the diverter structure and the OB reflector/blanket represent the 7.5 FPY life components, while everything inside this region represents the 2.5 FPY lifetime components. Likewise, everything external to the 7.5 FPY region has a life equal to the plant lifetime.

5.2.3.2. Mechanical Configuration

The FW/B/S of the ARIES-RS power plant consists of structure divided into zones which provide poloidal channels with flowing Li. Figures 5.2-2 and 5.2-3 are midplane

Cross Section of In-Vessel Components Between the TF Coils

Figure 5.2-1.

Midplane Cross Section of Outboard First Wall/Blanket/Shield Sector

Figure 5.2-2.

Midplane Cross Section of Inboard First Wall/ Blanket/ Shield Sector

Figure 5.2-3.

cross sections of the OB and IB FW/B/S sectors, respectively. One sector constitutes $1/16$ of the torus, or 22.5° as measured from the torus axis. The OB FW/B/S part of the sector as shown in Figure 5.2-2 is curved with the FW facing towards the axis of the torus, and thus the toroidal extent of the components increases from the FW to the back of the shield. Figure 5.2-3 which is the complementary IB part of the sector has the FW facing away from the torus axis, and the toroidal extent of the components decreases from the FW to the shield. The four zones of the OB and IB FW/B/S components are clearly delineated by gaps. The FW and front breeding zone is separated from the blanket/reflector zone by a gap. There are also gaps between the blanket/reflector zone and the high temperature shield and between the high temperature shield and the low temperature shield. Figures 5.2-4 and 5.2-5 are 3 dimensional views of the IB and OB parts of a sector. Figure 5.2-4 shows the IB blanket and reflector ending to make room for the diverter plates (see also Figure 5.2-1). Figure 5.2-5 clearly shows the three gaps between the various segments of the OB blanket/shield. The FW is made of V alloy, 0.3 cm thick and has a 2 cm thick channel immediately behind it with Li flowing in the poloidal direction. The backside of the channel is also a V plate, 2 cm thick in the OB blanket and 0.5 cm thick in the IB blanket. The remaining part of the FW/blanket consists of two poloidal channels, each 7.7 cm thick, separated by a 0.3 cm thick plate. Each blanket is closed off with a plate 0.3 cm thick on the OB side and 0.8 cm thick on the IB side. The side plates for both blankets are 1.0 cm thick. Reinforcement for the FW/blanket is provided by means of 0.5 cm thick radial ribs, spaced at 1.5° in the blanket and at 0.75° in the channels behind the FW.

From here on, the IB and OB sides are completely different. The OB side has another blanket/reflector zone while the IB side only has a reflector zone. Both sides then have a high temperature shield and a low temperature shield.

The OB blanket/reflector zone primarily consists of thick poloidal channels made of V alloy and flowing Li. The depth of this zone is 37 cm, which includes a 5 cm thick reflector made of tenelon. Tenelon is a high Mn steel manufactured by U.S. Steel. This tenelon is entirely encapsulated in V alloy structure. The IB reflector is 20 cm deep and consists of primarily V alloy filler material encapsulated in V alloy structure. Two 0.8 cm deep poloidal channels run in the poloidal direction through the reflector for cooling with Li.

The high temperature shield is 26 cm deep on the IB side and 28 cm deep on the OB side. In both cases, the shield consists of tenelon filler slab material encapsulated in V alloy structure. Two coolant channels, 1.0 cm deep on the IB side and 0.85 cm deep on the OB side provide Li cooling for the shield.

Perspective View of Partial Inboard First Wall/Blanket/Shield

Figure 5.2-4.

Perspective View of Partial Outboard First Wall/Blanket.Shield

Figure 5.2-5.

The low temperature shields have different material compositions on the IB and OB sides. The IB shield is 28 cm deep, and consists of 53% B_4C filler (90% d.f.) and 27% WC filler (95% d.f.), encapsulated in tenelon structure. The OB shield is 40 cm deep, and consists of 80% borated tenelon filler encapsulated in tenelon structure. Both IB and OB low temperature shields are cooled with He gas and the recovered energy is not used in the power cycle.

5.2.3.3. Blanket/Shield and Shield/Shield Connections

As has been described, the in-vessel components of the reactor consist of several layers of blanket and shield connected together. These layers, however, have to be decoupled during maintenance, because they achieve their end of life at different times.

The maintenance philosophy for the in-vessel components, is to remove sectors composed of IB and OB blankets, upper and lower diverters and OB shields. The sectors are moved radially out of the vacuum chamber through access doors provided behind each sector. After removal, the sectors are taken to a hot cell, where the short lived components are taken out and replaced with new ones. The sectors are then replaced into the vacuum vessel.

This procedure requires couplings between the components which can be disconnected. These couplings have to provide support between the components during handling, and must be capable of reacting disruption forces. For example, the OB vertical stabilizing shells are an integral part of the high temperature shield which is coupled to the low temperature shield on one side, and the blanket/reflector on the other. Because the OB vertical stabilizing shells are toroidally continuous, they will carry the brunt of a plasma disruption. These forces will have to be reacted by the coupling mechanism provided for the high temperature shield.

A suggested connection between the high temperature shield and the blanket/reflector segment has been discussed in Section 4.2.2 and is shown in Figure 5.2-6. Similar connections will be provided between the FW/front blanket segment and the blanket/reflector, as well as between the high temperature and low temperature shields. The connections consist of thick washers or discs which we will call links, manipulated with bolts from the sides of the sectors which engage overlapping side-walls of the segments being coupled. From Figure 5.2-6 it is evident that by moving the links inward, decouples the segments while moving them outward, couples them. These links are distributed along the overlapping sidewalls from the bottom to the top of the sector as shown in Figure 5.2-1. The link

Typical Connection Between the Blanket/Reflector and the High Temperature Shield

Figure 5.2-6.

at midplane connects the segments, while the remaining links fit into oblong slots which allow the segments to expand in the poloidal direction due to difference in temperature. The number and size of the links, as well as the strength and stiffness of the side-walls is determined from the magnitude of the disruption forces. Again using the OB vertical stabilizing shells as an example, we find from Table 5.2-II that the peak force on the OB shell in the radial direction is -89 MN, or 5.5 MN/sector. Assuming there are 10 links on each side (20 total) and a conservative design limit of 150 MPa for V alloy at 500°C, we arrive at a total engaged area of 360 cm², or 18 cm² per link. This means that each link will be 6 cm in diameter and engages to a depth of 3 cm. These links can easily react the vertical component of the disruption force which amounts to 2 MN per sector (see Table 5.2-II).

The question arises as to whether the bolts which drive the links will be able to operate after irradiation, or will they freeze up. Since the threaded portion of the mechanism is in the replaceable segment, the bolt can be drilled out should it fail to turn. Once the bolt is drilled out in the hot cell, the segments can be decoupled, and the replacement segment will have new links supplied with it. Finally, the links will be radiatively cooled by the surrounding actively cooled structure. These links may have to be made from W, as are the vertical stabilizing shells which are also radiatively cooled and are situated in similar locations.

5.2.3.4. Support of the Blanket/Shield Components

The previous section discussed the connections between the various segments of the in-vessel components. This section describes the gravity support and vertical stabilization for these components. Section 4.2.2 provides a detailed description of the method for decoupling a blanket/shield sector consisting of IB and OB blankets, upper and lower diverters and OB shields from their location in the vacuum chamber weighing 158 tonnes, and moving it out radially for removal to a hot cell. Briefly, the method is to insert between the sector base and the vacuum chamber floor, which sits on the main support structure, a rail system equipped with hydraulic elevating pistons and a low coefficient of friction (teflon) sliding pads. Before this can be done, a shield block has to be removed. The sector base is frozen to the support base with a layer of low melting metal, such as a Cu alloy. Once the metal is melted, the hydraulic pads are elevated to pick up the sector and the sector is slid out riding on the teflon pads. Inserting a new sector uses the reverse sequence. The sector is slid in on the rail system, hydraulic pistons align its location in the toroidal and poloidal directions, and the sector is leveled front to back.

The freezing metal is poured in to immobilize the sector. After the metal is frozen, the hydraulic pistons are lowered, the rail system is removed and a shielding block is inserted in its place.

The vacuum chamber of the reactor sits on a support base with columns located between the TF coils, firmly grounded in the main support foundation for the reactor. These columns share the space between the TF coils with the vacuum ducts leading from the vacuum chamber to the vacuum pumping station located in the lower levels of the reactor.

The OB low temperature shield has a rectangular flange which fits against the rectangular flange on the access port located between the TF coils as part of the vacuum vessel. This flange on the low temperature shield is bolted to the vacuum vessel flange and constitutes the primary vacuum seal for the reaction chamber. A secondary vacuum seal is provided at the external access door. The attachment also provides vertical stability to the sector by transferring loads to the vacuum chamber, which is toroidally continuous and is reinforced by the internally protruding structure making up the access door frame. Table 5.2-I gives some pertinent parameters of the blanket/shield.

5.2.3.5. Summary of Mechanical Configuration

The in-vessel components of the reactor are the FW, several zones of blankets, a reflector, a high temperature shield and a low temperature shield. The zones are divided into groups with lifetimes of 2.5 FPY, 7.5 FPY and 40 FPY components. The IB high temperature and low temperature shields are lifetime components, and are not removed as part of a maintenance sector. All the other components consisting of the IB blanket, OB blanket, upper and lower diverters, and OB shields can be removed as sectors and taken out radially through access ports between the TF coils. Parts of the blankets and reflectors, as well as the shields, with varying lifetimes are connected together with links which can be disconnected/removed in the hot cell. A special rail is inserted under the sector for removal during maintenance. Gravity loads are transferred through the vacuum vessel to a support base under the vessel, and then with columns between the TF coils to a lower support foundation. Vertical stability for the sectors is provided by bolting the low temperature shield flange to the access door flange provided by the vacuum vessel.

Table 5.2-I.

SOME PERTINENT BLANKET/SHIELD PARAMETERS

	Radial Depth (m)	Surface Area (m ²)	Volume (m ³)	Mass (tonnes)	Mass w/o Li (tonnes)
IB LT SHLD	0.28		29.63	164.1	
gap	0.002				
IB HT SHLD	0.26		30.13	230.3	
gap	0.001				
IB REFL.	0.20		27.7	153.4	
gap	0.001				
IB BLKT	0.20		29.3	31.06	
IB FW	0.003	94.3	0.28	1.84	
DIV. PLATES	0.067	128.5	8.61	54.1	
DIV. BLKT	0.20		21.96	125.7	
DIV. HT SHLD	0.45		67.6	511.8	
DIV. LT SHLD	0.35		52.6	403.2	
OB FW	0.003	231.6	0.69	4.5	
OB BLKT #1	0.20		60.64	64.3	
gap	0.001				
OB BLKT #2	0.30		97.89	103.7	
OB REFL.	0.07		24.10	164.2	
gap	0.001				
OB HT SHLD	0.28		106.9	853.6	
gap	0.002				
OB LT SHLD	0.40		167.9	1064.5	
One Sector				163.8	157.7

Highlighted masses constitute the removable sectors.

5.2.4. Plasma Stabilizing Shells and Active Coils

The plasma location within the reactor chamber is controlled with passive shells and with active coils. Just how this is accomplished is discussed in the Chapter 2, Section 2.2. In this section we only describe the geometry, design and physical location of the shells, as well as the design of the active coils.

There are three plasma stabilizing systems built into the reactor chamber. They are:

1. MHD stability and kink shells
2. Vertical stability shells
3. Vertical control active coils.

Each of these will be discussed separately.

5.2.4.1. MHD Stability and Kink Shells

These stabilizing shells are located on the outboard (OB) side only, extending poloidally from $+85^\circ$ to -85° from the plasma center at midplane. Fortuitously, they do not have to be electrically toroidally continuous and for this reason, it was possible to integrate them as part of the blanket structure. Figure 5.2-7 is a midplane cross section of an OB blanket/shield sector showing a 2 cm thick V alloy plate behind the first wall (FW). This plate which is 0.094 m from the plasma boundary doubles as the MHD and kink stabilizing shell. It is cooled from both sides by liquid Li flowing in the poloidal direction. The plates are part of the replaceable component of the reactor chamber and thus will be periodically changed out when the structure reaches the design limit of 15 MW-y/m².

5.2.4.2. Vertical Stability Shells

There are four toroidally electrically continuous vertical stabilizing shells built into the reactor chamber, two on the inboard (IB) side and two on the OB side. The shells on the IB side are made from W and are built into the permanent IB shield. They extend from +1.68 m to +2.43 m above midplane and from -1.68 m to -2.43 m below the midplane, are 5 cm thick, starting at a radius of 3.38 m from the reactor center and extending to 3.43 m. Since these IB shells are not part of the scheduled periodically

Mid-Plane Cross Section of Outboard First Wall/Blanket/Shield

Figure 5.2-7.

replaced components, they are made toroidally continuous and are lifetime components. The mass of each shell is ~ 15.5 tonnes.

The complementary OB shells are embedded in the blanket/shield sectors. Since these sectors have to be removed radially, the shells are made up of segments located between the reflector and the high temperature shield. Segment to segment connectors are provided which bridge the gap between adjacent sectors.

The two OB shells are also made of W, are symmetrically situated about the midplane and have the coordinates which extend from $R = 5.57$ m, $Z = +2.5$ m to $R = 6.65$ m, $Z = 1.65$ m for the upper shell and $R = 5.57$ m, $Z = -2.5$ m to $R = 6.66$ m, $Z = -1.65$ m for the lower shell. Another way of describing their location is between $\pm 55^\circ$ and $\pm 85^\circ$ from the horizontal as measured from the plasma center which is at a radius of 5.52 m. The shells are 6 cm thick and each weigh 59 tonnes. Each blanket/shield sector supports two shell segments which together weigh 7.38 tonnes.

Figure 5.2-8 is a side view cross section of the reaction chamber showing the OB stabilizing shells, wedged between the reflector and the high temperature shield above and below the midplane. Figure 5.2-7 is a midplane cross section of a blanket/shield sector. There is a 1.0 cm gap between the back of the reflector and the front of the high temperature shield. At the location of the OB vertical stabilizing shells, the high temperature shield will be recessed to make room for the 6 cm thick W shells. The shells will be attached to the high temperature shield, but will be in direct contact with the back of the reflector. The high temperature shield is a lifetime component of the reactor. Figure 5.2-9 shows how the segment to segment connections are made. Basically, a set of triangular W wedges which are manipulated mechanically are driven into the gap between adjacent shell segments providing an electrical path between them. These wedges are actuated from within the triangular protrusion from the vacuum vessel which exists in front of each TF coil and constitutes the poloidal component of the flange used to seal a sector within the reactor chamber. The drive mechanism itself is sealed within the triangular vacuum vessel housing and the radial motion of the wedges is provided by a shaft protruding from the housing. The wedges are mounted on arms similar to automobile windshield wipers making it possible for them to conform to the contour of the shells. When actuated, the wedges make contact with the shells providing an electrical path for current to flow through them toroidally around the torus. The current induced by the motion of the plasma provides restoring forces to control the plasma location. During maintenance, the wedges are retracted away which permits a blanket/shield sector to be extracted radially without interference. Some research and development will be required to insure that such a system can be made to work, or

Side View Cross Section in the Plane of a TF Coil Showing the Vertical Stabilizing Shells

Figure 5.2-8.

Method for Making Electrical Connection Between Adjacent Vertical Stabilizing Shells

Figure 5.2-9.

whether some compliant layer may be needed at the interface of the wedges with the shell to provide the required electrical conductivity.

The W shells will operate at a higher temperature than their surroundings and will be radiatively cooled. Nuclear heating for the 6 cm thick shell has been determined to be 5 W/cm³ which makes the surface heat flux from them equal to 15 W/cm². Assuming a TiO coating on the W, the emissivity is 0.9. With the high temperature shield and reflector temperatures at 650°C, the surface temperature of the W will be 1130°C. The increase in the electrical resistivity of W at this temperature has been factored in.

Electromagnetic forces on the shells during a plasma disruption are discussed in Section 3.4. Table 5.2-II gives the IB and OB peak disruption forces in the radial and vertical direction for three disruption scenarios: 10 ms plasma quench, 100 ms plasma quench with drift, and 2500 ms drift then 10 ms quench.

The worst forces arise from the last disruption scenario, namely the 2500 ms drift then 10 ms quench. For the IB shell the upward (away from the plasma) vertical force is 21 MN and the radial force (away from the plasma) is 14 MN. The pressures on the surrounding structures, taking areas into account are 19.6 MPa and 0.87 MPa for the vertical force and horizontal force respectively. Similarly, for the OB shell, the vertical force upward (away from the plasma) is 33 MN and the radial force (towards the plasma) is -89 MN. The corresponding pressures are 0.79 MPa and 2.58 MPa for the vertical force and horizontal force respectively.

Given that the shells are supported along their whole circumference both radially and vertically, the stresses on their surroundings, even with shock loading are very nominal. The global result of these forces on the blanket/shield sectors has not been determined. But since these sectors are so massive, it would not appear that these nominal forces when spread over the whole sector can lead to permanent damage.

5.2.4.3. Active Stabilizing Coils

Vertical plasma stability is augmented with active coils located on the OB side at the back of the low temperature shield. The operation of the active coils has been described in Chapter 2. Active control of the current in these coils provides the restoring forces needed to maintain the vertical location of the plasma.

There are two active coils symmetrically situated about the mid-plane. Their location is at a radius of 6.7 m from the center of the torus and at elevations $Z = \pm 2.4$ m. The coils are toroidally continuous, have a peak power of 20 MVA per coil, a peak current of

Table 5.2-II.

**PEAK DISRUPTION FORCES ON VERTICAL
STABILIZING SHELLS**

Case	Peak <i>r</i> Force	Peak <i>z</i> Force
Inboard Shell		
10 ms quench	3.8 MN	9.7 MN
100 ms quench with drift	3.6 MN	10 MN
2500 ms drift	14 MN	21 MN
then 10 ms quench		
Outboard Shell		
10 ms quench	-46 MN	-13 MN
100 ms quench with drift	-46 MN	-12 MN
2500 ms drift	-89 MN	33 MN
then 10 ms quench		

*negative sign indicates force vector towards plasma.

100 kA per coil, a peak voltage of 0.2 kV per coil and a resistive power of 1.0 MW per coil. Since the coils are mounted on blanket/shield sectors which are removed radially for maintenance, they have to be segmented and must have connections which bridge the gap between sectors.

Design of the Active Stabilizing Coils

Figure 5.2-10 is a side view of one quarter of the torus showing the upper active coil mounted on the back of the low temperature shield. A complementary lower coil is located mirror imaged with respect to the midplane. There are 16 sectors in the torus, each sector will have upper and lower coil segments mounted on it.

Two coil designs have been considered, one internally cooled and the other externally cooled. Figure 5.2-10 shows a cross section of the internally cooled coil encased in a steel casing. The conductor is made of dispersion strengthened (DS) Cu GlidCop C15715, a

Side View Cross Section of One Quarter of Plasma Chamber Showing the Location of the Active Stabilizing Coils and Views of the Internally Cooled Coil Design Option

Figure 5.2-10.

patented product of SCM Metal Products, Inc. There are four turns in each coil, trapezoidally shaped to conform to the contour of the low temperature shield with maximum utilization of conducting material. Each turn has a hole in the center provided for He gas cooling. Section A-A shows how He gas enters the coil turn and Section B-B shows how the coil is electrically insulated from the coolant manifold. The He gas from the manifold enters each coil turn, flows toroidally to the other end of the sector and exits through a return manifold.

Figure 5.2-11 shows the externally cooled conductor option. In this option, the conductors are cooled by the coil casing which has He gas flowing through it. Ohmic heating power has to be conducted across a layer of BeO, which is the electrical insulation as well as a graphite compliant layer. The casing is made clamshell wise, bolted at the top and bottom as shown in the figure. Section A-A shows the coolant channels in the casing and the electrical insulation on the conductor and the compliant layer between the electrical insulation and the coil casing. The graphite compliant layer is intended to aid in transferring heat from the coil turns to the casing through uneven surfaces. As in the internally cooled option, the conductor is made of DS Cu. Since the coil casing is the heat sink, it is important that it is a good thermal conductor as well as being strong. For these reasons, the coil casing will also be made of DS Cu. The coil casing is bolted to the back of the low temperature shield after the coils are properly aligned.

Electrical Aspects of the Coils

The coils are toroidally continuous and the electric current flowing in them, controlled by a feedback system, helps to restore the vertical position of the plasma when it deviated from its prescribed location. A power storage system, not discussed in this section, sloshes large amounts of power in and out of the coils in response to the demands of the feedback controller. Although the peak power per coil can reach as high as 20 MVA, only a small fraction of that (1.0 MW/coil) is dissipated resistively in a steady state mode. The cooling system for the coils is, therefore, designed to handle 1.0 MW/coil of ohmic heating. The peak current in the coil is 100 kA, or 25 kA per turn and the peak voltage between the terminals is 0.2 kV. This means that the peak turn-turn voltage which the electrical insulation has to stand off is 50 V.

The terminals for each coil will be located in one of the blanket/shield sectors. The terminals connect the external power supply/energy storage system to the coils. The bus-bars penetrate the vacuum vessel through vacuum power feed connectors which are recessed out of the way provided for sector removal through access doors. Jumper bus-bars are then used to electrically connect the feed-throughs to the terminals.

Externally Cooled Coil Design Option

Figure 5.2-11.

Sector-Sector Coil Connections

Toroidal continuity for the coils is provided by sector-sector connectors as shown in Figure 5.2-10 which is a cross section at an intermediate location between the access door and the region in the shadow of the TF coil. Immediately under the TF coil, the vacuum vessel penetrates into the space of the low temperature shield with a triangular protrusion. The sector-sector connectors are permanently housed within this triangular vacuum vessel protrusion.

Figure 5.2-12 is a top view of the inter-sector connection, also showing the interface at Section A-A. The heavy arrows in the top view indicate the direction of sector removal, while the sector-sector connecting segment stays in place within the vacuum vessel protrusion. This connecting segment must perform the difficult task of providing electrical continuity for the four turns of the coil, while preserving the integrity of the electrical insulation between the turns. Section A-A shows one of the interfaces at one end of the connection. The vertical line in the center of each turn provides the electrical continuity, and the electrical insulation separating each turn provides the voltage separation between the turns. A low temperature melting solder will be used at the electrical interface. During operation, the solder will be molten, providing good electrical conductivity at the junction. At the time of sector removal for maintenance, the solder will be melted by circulating hot He gas through the cooling channels.

The coil segments on the back of the low temperature shield are mounted with the capability of adjustment both toroidally and poloidally. The sector to sector connecting segments are also mounted on flexible supports. When a new sector is loaded into the reactor chamber, the coils are lined up with the connecting segments and then clamped to the back of the shield. Other clamps are used to secure the mating surfaces. When the reactor starts operating the solder tinned interface at the joint fills with the molten solder as the current begins to flow through the coils.

Thermal Hydraulics of the Coils

Three cases of the coils have been analyzed thermal hydraulically, one of the internally cooled option and two of the externally cooled option. The externally cooled option has been analyzed using two sided cooling of the casing, as shown in Figure 5.2-11 and also one sided cooling of the casing. In all of these analyses, finite elements were used to determine temperature distributions.

The thermal hydraulic assumptions for both internally and externally cooled options are given in Table 5.2-III. The power density in all cases is 2.0 W/cm^3 and the He coolant temperature is 100°C and 170°C for inlet and outlet respectively. The He gas velocity is

Top View of the Electrical Connection Between Adjacent Segments of the Active Vertical Stabilizing Coils

Figure 5.2-12.

Table 5.2-III.

**THERMAL HYDRAULIC ASSUMPTIONS FOR THE
ACTIVE STABILIZING COILS**

Internally cooled:

Hole diameter (cm)	1.0
Power density (W/cm ³)	2.0
Coolant inlet T (°C)	100
Coolant outlet T (°C)	170
Coolant pressure (MPa)	5
Coolant velocity (m/s)	20

Externally cooled:

Uniform casing temperature (°C)	170
Thickness of BeO insulation (cm)	0.1
Thermal cond. of BeO (W/cmK)	2.2
Thickness of C compliance layer (cm)	0.1
Thermal cond. of compl. layer (W/cmK)	0.01

20 m/s at a pressure of 5 MPa. Maximum temperature in the Cu is determined at the point where the He gas is 170°C, namely at the outlet.

In the externally cooled option, the analysis is performed with a uniform coil casing temperature of 170°C. Figure 5.2-13 shows the temperature distribution for the one sided cooled option as determined with finite element analysis. Cooling is provided on the outboard side of the coil, in the casing facing away from the shield. The other three sides are considered adiabatic with all the heat conducted to the cooled side, going through the electrical insulation and the compliant layer. As might be expected, the maximum Cu temperature of 298°C is on the extreme left corner of the conductor while the lowest temperature of 185°C is at the extreme right corner of the casing. Table 5.2-IV summarizes the thermal hydraulics analyses.

Finite Element Analysis of the Temperature Distribution in the One Sided Externally Cooled Vertical Stabilizing Coil Option

Figure 5.2-13.

Table 5.2-IV.

SUMMARY OF THERMAL HYDRAULICS

Internally cooled maximum temperature (°C)	240
Externally cooled maximum temperature	
Two sides cooling (°C)	227
One sided cooling (°C)	298

Selection of the Reference Coil Design

Of the three options analyzed, the externally cooled option using one-sided cooling has been selected as the reference design. In this option the operating temperature range of the Cu is from 230°C to 298°C and the average Cu resistivity is 3.4×10^{-6} ohm-cm, which is the resistivity used in the analysis. Using the coil casing cooling option obviates the need for electrically insulating the coil turns from the cooling manifold. Finally, the one sided cooling option simplifies the design and fabrication of the coil casing itself.

Structural Considerations

The primary loads on the vertical stabilizing coils come from $J \times B$ interactions, when the current in the coils is crossed with the poloidal field components at the coil locations. These forces have been determined to be 4.5 MN in the vertical direction and 1.3 MN in the horizontal direction. The specific forces on the coils are therefore 0.15 MN/m and 0.043 MN/m for the vertical and horizontal forces respectively.

In order to determine the distances between coil supports on the back of the shield, we calculate the combined moments of inertia for the coils plus coil cases about the horizontal and vertical axes. The moment of inertia against bending in the vertical direction (i.e. about the horizontal axis) is 4.18×10^7 cm⁴, and in the horizontal direction (i.e. about the vertical axis) is 2.13×10^7 cm⁴. Using a maximum stress of 100 MPa in the coil case sets the distance between supports at 2.5 m. The supports will be spaced at much shorter distances than 2.5 m and thus it can be concluded that supporting the coils would not pose a problem. Table 5.2-V summarizes the parameters of the active stabilizing coils.

Table 5.2-V.

DESIGN PARAMETERS OF ACTIVE STABILIZING COILS

Number of coils	2
Circumference (m)	42.1
Number of turns/coil	4
Cross section of each turn (cm ²)	5 × 6
Total coil volume (m ³)	0.505
Peak current in coil (kA)	100
Peak voltage (kV)	0.2
Conductor material	GlidCop C15715
Casing material	GlidCop C15715
Electrical insulation	BeO
Power density (W/cm ³)	2.0
Coolant	He gas

5.2.4.4. Summary of Plasma Stabilizing Shells and Active Coils

The MHD and kink stabilizing shells are integrated into the front part of the blanket structure, are made of V alloy, are not toroidally continuous and are cooled with liquid Li flowing in the poloidal direction. Their design is relatively straight forward.

Vertical plasma stabilizing shells are located on the IB and OB sides of the plasma, are toroidally continuous, made from W and are radiatively cooled. On the OB side, the electrical toroidal continuity between sectors is provided by means of W wedges which when actuated make contact between adjacent shell sectors. The electromagnetic forces on the shells have been determined. Since the shells are essentially supported along their entire circumference both radially and vertically, the nominal forces, when spread over the whole sector, can be adequately contained.

The active stabilizing coils are used to augment the vertical stabilizing shells for controlling the location of the plasma. There are two toroidally continuous coils located on the OB side of the low temperature shield. These coils are made of DS Cu, have four turns each and are actively cooled with He gas. Sector-sector connections are provided for toroidal continuity. An externally cooled option has been selected in which cooling is

provided on one side of the coil casing. Forces on the coils from $\mathbf{J} \times \mathbf{B}$ interactions have been determined and are well within design limits.

5. First Wall, Blanket and Shield Design and Analysis

5.3. Stress Analysis

5.3.1. Introduction

An analysis was performed to estimate the stresses in the first wall, blanket and shields of the ARIES-RS design. Results for a range of coolant pressures and geometries were determined to guide design changes. The analysis considered the effects of radiation damage to the materials.

This study evaluated the loads resulting from radiant surface heating, volumetric nuclear heating, differential neutron-induced swelling and coolant pressure. Only loads during normal operation were considered. Therefore, thermal and electromagnetic disruption loadings were not included.

In the ARIES-RS reactor design, each first wall/blanket/shield module is divided radially into several components separated by gaps. Nearest the plasma is the 20 cm thick first wall/blanket component. The remaining components (the blanket/reflector, high temperature shield, and low temperature shield) experience minimal stresses and are not a concern. Therefore, this study will focus on the first wall/blanket structure.

The structural material for the first wall/blanket component is V-4Cr-4Ti. The physical, thermal and mechanical properties of V-4Cr-4Ti are discussed.

The manner in which the first wall/blanket is supported can have a significant influence on its stresses. In this analysis, the first wall/blanket is free to expand but not bend. More compliant supports should reduce static stresses but may increase the danger from electromagnetic disruption loads.

This analysis computed the temperature distribution in the component by a numerical technique in order to consider the slight temperature dependence of thermal conductivity. The pressure, thermal and swelling induced stresses were evaluated by an analytical technique. Irradiation creep was also considered. The ASME Boiler and Pressure Vessel Code provides stress and temperature limits for code-qualified structural materials [1]. Although V-4Cr-4Ti is not a code-qualified material, the code's methodology for determining these limits is applied in this study.

Fatigue should not be an issue given the low number of operation cycles of the ARIES-RS device. Secondary and tertiary thermal creep is minimal and creep-rupture life is well in excess of the 2.6 year time between first wall replacements.

The study found that the stresses in the outboard first wall are marginally acceptable with slight modifications. The stresses in the back wall of the outboard first wall/blanket component will need to be decreased. Increasing the back wall's thickness to 7 mm will be sufficient. Present models suggest that irradiation creep deformations may become larger than the 5% strain limit that is recommended.

5.3.2. Assumptions

5.3.2.1. Configuration

In the ARIES-RS design, the inboard and outboard first walls are an integral part of a 20 cm thick first wall/blanket component. Figure 5.3-1 illustrates part of the cross-section of the outboard first wall/blanket component at the midplane. The poloidal direction is out of the plane of the figure; the toroidal direction is horizontal. There is a slight concave up curvature to this component which is not shown in the figure (curvature about the poloidal direction). The region shown spans about 2° . The full toroidal width of the first wall/blanket component is 15 large coolant channels (30 first wall coolant channels) across. The 2 cm thick plate located behind the first wall coolant channels serves as the kink stabilizing shell.

The dimensions shown are correct at the midplane. However, the component has a curvature about the toroidal direction, as well as poloidal. At other poloidal positions, the outboard first wall has a smaller radius from the reactor bore. Thus, the span (toroidal width) of the coolant channels is somewhat less than 85 mm at other poloidal locations (for example, at 1.6 m from the midplane it is 75 mm).

Figure 5.3-2 shows the inboard first wall/blanket component. There is a slight concave down curvature to this component which is not shown in the figure. Like its outboard counterpart, the inboard first wall/blanket component consists of a total of 30 large coolant channels and 30 first wall coolant channels. However, the smaller radius from the bore implies a smaller first wall coolant channel span (48 mm at the midplane).

Behind both the inboard and outboard first wall/blanket components is a 1 cm gap separating these components from the blanket/reflector components.

Figure 5.3-1. Reference design of the outboard first wall/blanket component for ARIES-RS.

Figure 5.3-2. Reference design of the inboard first wall/blanket component for ARIES-RS.

5.3.2.2. Material Properties

The first wall/blanket component is made of vanadium alloy, specifically V-4Cr-4Ti. The physical, thermal and mechanical properties of V-4Cr-4Ti are reported in the ITER Material Properties Handbook [2]. The data base for V-4Cr-4Ti is not as extensive as for the binary (V-Ti, V-Cr) and other ternary (V-Cr-Ti) alloys. Some material properties, therefore, are estimated based on data from other alloys.

The thermal conductivity, based on measurements of V-5Cr-5Ti, in the temperature range of room temperature to 600 °C is

$$k = 30.35[1 + 2.835 \times 10^{-4}(T - 20)] \quad (5.3-1)$$

where T is the temperature in °C and k is in W/mK. The coefficient of thermal expansion is

$$\alpha = 9.0793(1 + 1.2075 \times 10^{-4} T + 2.2774 \times 10^{-7} T^2 - 2.4013 \times 10^{-10} T^3)$$

where the units of α are K⁻¹.

The correlations for elastic and shear moduli are based on measurements of pure vanadium and V-5Cr-5Ti [3,4].

$$E = 125.6[1 - 7.69 \times 10^{-5}(T - 20)]$$

$$G = 45.9[1 - 1.82 \times 10^{-4}(T - 20)]$$

The units of E and G are GPa. Poisson's ratio is given by

$$\nu = \frac{E}{2G} - 1$$

The correlation for the design ultimate tensile strength was determined by fitting a polynomial to the available data and reducing by a factor of 1.15. This value is the ratio of minimum to average strength (at room temperature) of a metal with a substantial data base, 316LN steel.

$$S_{ud} = 424(1.0450 - 2.3735 \times 10^{-3} T + 6.2675 \times 10^{-6} T^2 - 4.7504 \times 10^{-9} T^3)$$

The design yield strength was determined by the same technique.

$$S_{yd} = 322(1.0635 - 3.3238 \times 10^{-3} T + 7.5229 \times 10^{-6} T^2 - 5.3461 \times 10^{-9} T^3)$$

The terms in parenthesis are unity at $T = 20$ °C. The units of S_{ud} and S_{yd} are MPa.

Note that although this alloy has a melting temperature of about 1900 °C, concerns of irradiation embrittlement restrict operations to less than 700 °C.

5.3.2.3. Loads

The surface heat flux on the outboard first wall at the midplane is 0.42 MW/m^2 . The peak heat flux is 0.47 MW/m^2 at 1.6 m from midplane; the peak nuclear heating is 35.6 MW/m^3 at the midplane.

On the inboard first wall, the heat flux at the midplane is 0.4 MW/m^2 . The peak heat flux is 0.48 MW/m^2 at 1.9 m from midplane; the peak nuclear heating is 31 MW/m^3 at the midplane.

The heat transfer coefficient from the channel walls to the liquid lithium coolant is $10000 \text{ W/m}^2\text{K}$. This value is not well established and is perhaps the greatest source of uncertainty in the results.

In this study, a coolant temperature of 550°C was assumed. This value is an estimate; clearly, the coolant temperature varies with poloidal position. Fortunately, material properties (and, thus, stress limits) are relatively insensitive to temperature.

The first wall is replaced every 2.6 full power years to limit its accumulated fluence to about 200 displacements per atom (dpa).

5.3.3. Methodology

The design limits on the ARIES-RS first wall/blanket component consist of limits on primary and secondary static stresses, maximum operating temperature, thermal creep, creep-rupture time, swelling, and irradiation creep. The stress limits are based on the methodology employed by the ASME Boiler and Pressure Vessel Code. The creep-rupture and excessive deformation limits are set by criteria specified in the Blanket Comparison and Selection Study (BCSS).

The stresses in this component result from several sources. Coolant pressure induces primary stresses, particularly at the pressure boundaries. Differential thermal expansion and differential neutron-induced swelling cause secondary stresses. In the first part of this section, the methods used to evaluate these stresses are detailed. This is followed by a discussion of the stress limits used in ARIES-RS. Finally, the design's conformity to creep-rupture and excessive deformation limits is evaluated.

Figure 5.3-3. The coolant pressure causes a bending stress in the first wall which peaks near the webs.

5.3.3.1. Evaluating Pressure Stresses

Coolant pressure induces large bending stresses and small membrane stresses in the first wall. If the coolant channel span (toroidal width) is large compared with the first wall thickness, beam theory can be used to predict the bending stresses. The peak stress in the first wall resulting from coolant pressure will be close to the webs. See Figure 5.3-3 which is an enlargement of the first wall in Figures 5.3-1 and 5.3-2.

If the outboard first wall is thin, the magnitude of its peak bending stress due to pressure is

$$\sigma_b = \frac{PL^2}{2t_{fw}^2} \quad (5.3-2)$$

where P is the coolant pressure, L is the first wall coolant channel span and t_{fw} is the first wall thickness. On the plasma facing side of the first wall, the peak stress is compressive in the toroidal direction. On the coolant channel side of the first wall, the peak stress is tensile with nearly the same magnitude. The exact value of the peak stress on the coolant channel side will depend on the size of the fillet where the first wall and the web meet. A small fillet radius implies a stress concentration which will raise the stress there to greater than the value given above. A large enough fillet should virtually eliminate the stress concentration. It is assumed that the fillet is large enough so that the stress concentration is small. A numerical technique such as the finite element method would be required to evaluate the stress distribution accurately for a given fillet size.

The coolant channels behind the kink stabilizing shell have twice the span of the first

wall coolant channels. The pressure load on the first wall places the webs in tension, exerting a bending load on the kink stabilizing shell. Equation 5.3-2 neglects the deflection of the kink stabilizing shell from this load. For outboard first wall thicknesses which are a significant fraction of the kink stabilizing shell thickness, this equation must be modified. This bending is much more significant in the inboard first wall where the 20 mm kink stabilizing shell is replaced by a 5 mm shell. Deformation of the shell increases the bending stresses in the first wall to

$$\sigma_b = \frac{PL^2}{2t_{fw}^2} \left[4 - \frac{3}{1 + (t_{fw}/t_k)^3} \right] \quad (5.3-3)$$

where t_k is the thickness of the kink stabilizing shell (or its inboard counterpart). The stiffness of the kink stabilizing shell ($t_k=20$ mm) reduces the difference between Equations 5.3-2 and 5.3-3 to about 1% for the reference design. However, for the reference design of the inboard first wall, the difference is 53%. The peak bending stress in the shell (inboard or outboard) is given by

$$\frac{3PL^2}{2t_k^2} \left[\frac{1}{1 + (t_{fw}/t_k)^3} \right]$$

5.3.3.2. Evaluating the Temperature Distribution

The surface heat flux on and volumetric nuclear heating in the first wall raise its temperature above that of the other structures in this component, which are heated only by nuclear heating. Differential thermal expansion causes compressive biaxial stresses in the first wall, which peak at the plasma-facing surface. To evaluate thermal stresses, the temperature distribution in all structures of the first wall/blanket must be determined.

All the structures are plates with coolant on one or both sides, allowing a one-dimensional thermal analysis. Conduction along plates is neglected. The steady-state one-dimensional heat equation with temperature dependent thermal conductivity is

$$\frac{d^2T}{dx^2} + \frac{1}{k} \frac{dk}{dT} \left(\frac{dT}{dx} \right)^2 = -\frac{q'''_{nuc}}{k} \quad (5.3-4)$$

where T is the temperature as a function of position x . The volumetric (nuclear) heating is q'''_{nuc} which may be a function of position. The thermal conductivity, k , is given in Equation 5.3-1. Note that this is a non-linear equation due to the temperature dependence of thermal conductivity.

Figure 5.3-4. Thermal analysis of the first wall. Both surface and volumetric heating are considered.

Consider the first wall. The boundary conditions are a known heat flux on the plasma-facing wall, q''_{pl} , and convection at the coolant channel wall. See Figure 5.3-4. This problem can be transformed from a boundary value problem to a more readily solved initial value problem by recognizing that the heat flux into the coolant is the sum of the plasma heat flux and the total nuclear heating,

$$q''_{pl} + \int_0^{t_{fw}} q'''_{nuc} dx = h(T|_{x=0} - T_{bulk}) = k \left. \frac{dT}{dx} \right|_{x=0}$$

where t_{fw} is the first wall thickness and h is the heat transfer coefficient. The temperature and temperature gradient at $x = 0$ can be expressed in terms of known quantities,

$$T|_{x=0} = \frac{1}{h}(q''_{pl} + \int_0^{t_{fw}} q'''_{nuc} dx) + T_{bulk} \quad (5.3-5)$$

$$\left. \frac{dT}{dx} \right|_{x=0} = \frac{1}{k}(q''_{pl} + \int_0^{t_{fw}} q'''_{nuc} dx) \quad (5.3-6)$$

where k is evaluated at $T|_{x=0}$. Equation 5.3-4 can be rewritten as a system of two variables,

$$\frac{d\Theta}{dx} + \frac{1}{k} \frac{dk}{dT} \Theta^2 = -\frac{q'''_{nuc}}{k}$$

and

$$\frac{dT}{dx} = \Theta$$

on the interval 0 to t_{fw} with the initial conditions given by Equations 5.3-5 and 5.3-6. This must be solved numerically. In this analysis, this system was solved by the fourth-order Runge-Kutta method.

The thermal conductivity of V-4Cr-4Ti is not a strong function of temperature (<3% change over 100°C) and the nuclear heating is approximately uniform in the first wall. Therefore, an approximation of the temperature distribution can be obtained by assuming each of these are constant and solving the resulting differential equation analytically. The result is

$$T = T_{bulk} + \frac{1}{h}(q_{pl}'' + q_{nuc}'''t_{fw}) + \frac{1}{k}(q_{pl}'' + q_{nuc}'''t_{fw})x - \frac{q_{nuc}'''}{2k}x^2$$

This approximation is compared to the numerical solution of Equation 5.3-4 in Figure 5.3-5. The figure shows the temperature distribution in a 5 mm first wall at the outboard midplane. The temperature differences across the wall from the two solutions agree within 1%. This error is much less than errors introduced by uncertainty in the heat transfer coefficient; either solution could be used. In this analysis, the numerical technique is used to produce results with the analytical approximation serving as verification.

The temperature distribution in back wall of the first wall/blanket component can be solved in a similar manner. Here, however, the surface heat flux is zero. (Radiation heat transfer to the blanket/reflector component is negligible.) The plates with coolant channels on either side exhibit a symmetric temperature distribution about the center. Their temperature distribution is solved for by treating half of their thickness with zero heat flux across the center.

Slightly higher temperatures will appear at the intersections of the plates. This effect was not considered. More sophisticated techniques are required to treat this.

5.3.3.3. Evaluating Secondary Stresses

Consider the evaluation of secondary stresses such as those resulting from differential thermal expansion and differential swelling. Swelling in the design will be characterized along with the other radiation effects at the end of this section. In the first wall/blanket component, the secondary stresses depend significantly on the manner in which the first wall/blanket component is supported. If the supports resisted expansion altogether, the

Figure 5.3-5. The temperature distribution in a 5 mm first wall at the outboard midplane. The numerical solution considers temperature dependence of thermal conductivity; the analytical approximation does not. The temperature drops across the wall agree within 1%.

stresses would be large. If the component were simply supported at its edges, it would bend about the toroidal and poloidal axes. Such a compliant support would tend to minimize secondary stresses but would lead to unacceptable stresses from electromagnetic disruption effects. In this analysis, the component is free to expand but no bending is permitted.

For this support condition, the poloidal strain of each point in any poloidal cross-section is identical so that the component elongates poloidally without bending. Similarly, the toroidal strain is uniform throughout any toroidal cross-section. In addition, equilibrium requires that the internal forces sum to zero. Let the x -axis correspond to the toroidal direction, y -axis correspond to the radial direction (from the plasma center, not the reactor bore) and z -axis correspond to the poloidal direction, as shown in Figure 5.3-1. The toroidal stress, σ_x , integrated over the component's toroidal cross-section is zero

$$\int \sigma_x dA_x = 0 \quad (5.3-7)$$

Similarly, the poloidal stress, σ_z , integrated over the poloidal cross-section is zero

$$\int \sigma_z dA_z = 0 \quad (5.3-8)$$

All radially (y) directed stresses and shear stresses are zero.

Assume the material deforms elastically. For biaxial stress, the stresses in a material at temperature T is

$$\sigma_x = \frac{E}{1 - \nu^2} (\epsilon_x + \nu \epsilon_z) \quad (5.3-9)$$

$$\sigma_z = \frac{E}{1 - \nu^2} (\epsilon_z + \nu \epsilon_x) \quad (5.3-10)$$

where ϵ_x and ϵ_z are the strains relative to the stress free state of the material at temperature T . The strains relative to the stress free state at a different temperature T_0 are

$$\epsilon_{x,0} = \epsilon_x + \alpha(T - T_0)$$

$$\epsilon_{z,0} = \epsilon_z + \alpha(T - T_0)$$

The no bending condition on the first wall/blanket component implies $\epsilon_{x,0}$ is identical and $\epsilon_{z,0}$ is identical at every point in the component, because the component is presumed stress free at temperature T_0 (the temperature at which it was assembled, usually room temperature).

Now consider swelling in the component. The linear swelling strain distribution in the component is given by ϵ_{swell} (linear swelling strain = 1/3 volumetric swelling). The strains relative to the stress free state at a different temperature T_0 and zero swelling are

$$\epsilon_{x,0} = \epsilon_x + \alpha(T - T_0) + \epsilon_{swell}$$

$$\epsilon_{z,0} = \epsilon_z + \alpha(T - T_0) + \epsilon_{swell}$$

and ϵ_x and ϵ_z are the strains relative to the stress free state of the material at temperature T and swelling given by ϵ_{swell} . Let the sum of thermal and swelling strains be

$$\epsilon_{sec} = \alpha(T - T_0) + \epsilon_{swell}$$

The stresses in the material are given by Equations 5.3-9 and 5.3-10

$$\sigma_x = \frac{E}{1 - \nu^2} [\epsilon_{x,0} + \nu\epsilon_{z,0} - (1 + \nu)\epsilon_{sec}] \quad (5.3-11)$$

$$\sigma_z = \frac{E}{1 - \nu^2} [\epsilon_{z,0} + \nu\epsilon_{x,0} - (1 + \nu)\epsilon_{sec}] \quad (5.3-12)$$

Inserting these into Equations 5.3-7 and 5.3-8,

$$\int \frac{E}{1 - \nu^2} [\epsilon_{x,0} + \nu\epsilon_{z,0} - (1 + \nu)\epsilon_{sec}] dA_x = 0$$

$$\int \frac{E}{1 - \nu^2} [\epsilon_{z,0} + \nu\epsilon_{x,0} - (1 + \nu)\epsilon_{sec}] dA_z = 0$$

Since $\epsilon_{x,0}$ and $\epsilon_{z,0}$ are constant over their respective cross-sections,

$$\left(\int \frac{E}{1 - \nu^2} dA_x \right) \epsilon_{x,0} + \left(\int \frac{\nu E}{1 - \nu^2} dA_x \right) \epsilon_{z,0} = \int \frac{E}{1 - \nu} \epsilon_{sec} dA_x$$

$$\left(\int \frac{\nu E}{1 - \nu^2} dA_z \right) \epsilon_{x,0} + \left(\int \frac{E}{1 - \nu^2} dA_z \right) \epsilon_{z,0} = \int \frac{E}{1 - \nu} \epsilon_{sec} dA_z$$

Solving this 2×2 system,

$$\epsilon_{x,0} = \frac{\left(\int \frac{E}{1 - \nu} \epsilon_{sec} dA_x \right) \left(\int \frac{E}{1 - \nu^2} dA_z \right) - \left(\int \frac{E}{1 - \nu} \epsilon_{sec} dA_z \right) \left(\int \frac{\nu E}{1 - \nu^2} dA_x \right)}{\left(\int \frac{E}{1 - \nu^2} dA_x \right) \left(\int \frac{E}{1 - \nu^2} dA_z \right) - \left(\int \frac{\nu E}{1 - \nu^2} dA_x \right) \left(\int \frac{\nu E}{1 - \nu^2} dA_z \right)} \quad (5.3-13)$$

$$\epsilon_{z,0} = \frac{\left(\int \frac{E}{1 - \nu} \epsilon_{sec} dA_z \right) \left(\int \frac{E}{1 - \nu^2} dA_x \right) - \left(\int \frac{E}{1 - \nu} \epsilon_{sec} dA_x \right) \left(\int \frac{\nu E}{1 - \nu^2} dA_z \right)}{\left(\int \frac{E}{1 - \nu^2} dA_x \right) \left(\int \frac{E}{1 - \nu^2} dA_z \right) - \left(\int \frac{\nu E}{1 - \nu^2} dA_x \right) \left(\int \frac{\nu E}{1 - \nu^2} dA_z \right)} \quad (5.3-14)$$

These can be used in Equations 5.3-11 and 5.3-12 to compute the stresses.

The first wall/blanket component is made of only V-4Cr-4Ti and this material's elastic modulus, Poisson's ratio and thermal expansion coefficient are mild functions of temperature. Therefore, an approximation of the stresses can be obtained by assuming constant properties and simplifying Equations 5.3-13 and 5.3-14. The result is

$$\sigma_x = \frac{E}{1-\nu} \left[\alpha \left(\frac{\int T dA_x}{A_x} - T \right) + \frac{\int \epsilon_{swell} dA_x}{A_x} - \epsilon_{swell} \right]$$

$$\sigma_z = \frac{E}{1-\nu} \left[\alpha \left(\frac{\int T dA_z}{A_z} - T \right) + \frac{\int \epsilon_{swell} dA_z}{A_z} - \epsilon_{swell} \right]$$

This approximation introduces errors as large as 4%. This method was used as a check of the solutions obtained by the more accurate method above.

By assuming the material behaves elastically, this analysis is only accurate if the material does not yield. As a result, this analysis may overestimate the actual maximum stresses. However, the ASME Boiler and Pressure Vessel Code's design rules specify requirements on the stresses computed by elastic analysis, not the actual stresses. These rules are discussed below.

5.3.3.4. Stress Limits

The ASME Boiler and Pressure Vessel Code provides stress and temperature limits for code-qualified structural materials [1]. Although V-4Cr-4Ti is not a code-qualified material, the code's methodology for determining these limits is applied in this study.

The average primary membrane stress, P_m , is limited by the allowable stress intensity, S_m

$$P_m \leq S_m$$

Primary loads are stress limited. In this study, primary stresses result from coolant pressure. The allowable stress intensity is determined by the minimum of either 2/3 the design yield stress or 1/3 of the design ultimate strength.

$$S_m = \min \left(\frac{2}{3} S_{yd}, \frac{1}{3} S_{ud} \right)$$

For V-4Cr-4Ti, the minimum S_m in the range from room temperature to 700°C is 108 MPa.

The maximum primary bending stress, P_b , and the maximum local primary membrane stress, P_l , are limited by

$$P_b + P_l \leq 1.5S_m = 162 \text{ MPa}$$

for a membrane in bending. The coolant pressure causes a bending stress (given by Equation 5.3-3) and a small membrane stress (less than 6 MPa) in the first wall.

Secondary stresses, Q , are limited in combination with primary stresses to less than $3S_m$,

$$Q + P_b + P_l \leq 3S_m = 325 \text{ MPa} \quad (5.3-15)$$

Secondary loads are strain limited. The secondary stresses in this study result from differential thermal expansion and differential swelling.

To apply Equation 5.3-15 to the total biaxial stress,

$$\sigma_x = \sigma_{sec,x} + \sigma_b + \sigma_{l,x}$$

and

$$\sigma_z = \sigma_{sec,z} + \sigma_{l,z}$$

($\sigma_{sec,x}$ and $\sigma_{sec,z}$ are computed by Equations 5.3-11 and 5.3-12; $\sigma_{l,x}$ and $\sigma_{l,z}$ are the toroidal and poloidal local membrane stresses, respectively), the von Mises stress is computed and compared to $3S_m$

$$\sigma_{von} \leq 3S_m$$

The von Mises stress is given by

$$\sigma_{von} = \sqrt{\sigma_x^2 - \sigma_x\sigma_z + \sigma_z^2}$$

5.3.3.5. Creep and Swelling Limits

Limited data exist for the thermal creep rates, irradiation creep rates and neutron-induced swelling rates of V-4Cr-4Ti. Criteria for designing against creep-rupture failure and excessive deformation is given in the Blanket Comparison and Selection Study (BCSS). Chung, Loomis and Smith [5] report secondary thermal creep rates for V-4Cr-4Ti at 600°C in the stress range 320–440 MPa. The primary stresses in the ARIES-RS outboard first wall reach the maximum of 162 MPa, as shown in the next section. Using a correlation of thermal creep rates from their data gives a total creep strain of only 0.038% over the first wall's 2.6 year life. The BCSS criteria set a maximum of 1%.

The BCSS recommends a primary membrane stress limit of 67% of the minimum stress to cause creep-rupture in a component lifetime. From creep-rupture data taken in the range 387–420 MPa, the rupture life of V-4Cr-4Ti at 700 °C and 260 MPa ($0.67 \times 260 \text{ MPa} = 173 \text{ MPa}$) is predicted to be 20 years [5]. Therefore, creep-rupture should not be an issue in this design.

Tertiary creep has only been observed at high stress (300-400 MPa) and should not be life-limiting in ARIES-RS.

The BCSS criteria place a 5% limit on volumetric swelling. The swelling data for V-4Cr-4Ti are limited to neutron fluences below 34 dpa and temperatures of 420 °C and 600 °C [6]. The largest observed volumetric swelling in these experiments has been 0.4% for both temperatures. Predicting the swelling behavior of V-4Cr-4Ti above 34 dpa is difficult. Assuming a linear increase of $0.4\%/34 \text{ dpa} = 0.01\%/dpa$ for the component lifetime of 200 dpa, the total swelling at end of life is 2.4%, less than the 5% limit.

The limited swelling data at 420 °C and 600 °C both show a maximum of 0.4% volumetric swelling, suggesting that swelling is insensitive to temperature. In this analysis, swelling is assumed temperature independent. Further evidence is needed to verify this.

Finally, the BCSS recommends a 5% limit on linear strain from swelling and irradiation creep. Swelling is strain limited and should not exceed a linear strain of $(0.01\%/dpa)/3 = 0.004\%/dpa$ for a maximum of 0.8% after 200 dpa. Coolant pressure loads are stress limited, so the rate of irradiation creep must be considered. Irradiation creep at 445 °C has been measured for stresses up to 120 MPa and fluences up to 3 dpa [7]. Irradiation creep rate (per unit fluence) was found to depend linearly on stress,

$$\dot{\epsilon}_{ic} = 3.3 \times 10^{-4} \sigma$$

where $\dot{\epsilon}_{ic}$ is irradiation creep rate in %/dpa and σ is stress in MPa. Extrapolating these results to the higher fluences, stresses and temperatures of the ARIES-RS first wall involves some uncertainty. Using the correlation above for the maximum coolant pressure stresses of 162 MPa at the first wall surface, the irradiation creep rate is $(3.3 \times 10^{-4})(162) = 0.053\%/dpa$, much greater than the swelling rate. Their combined deformations imply that 5% deformation will occur after $5/0.057 = 87 \text{ dpa}$. Thus, if the irradiation creep correlation above is applicable to these conditions, irradiation creep will cause deformations to exceed the recommended 5% limit before the end of life.

The differential swelling stresses from several percent strain would be large if irradiation creep did not provide a mechanism for relaxing these stresses. After some initial period, the linear swelling rate of $0.004\%/dpa$ is balanced by the irradiation creep rate.

Irradiation creep and swelling will be in equilibrium when swelling stresses are about $0.004/(3.3 \times 10^{-4}) = 12$ MPa which occurs after a few dpa in the first wall. Given that the total (primary + secondary) stress limit is about 325 MPa, differential swelling will not contribute significantly to the stresses in the ARIES-RS first wall.

Irradiation and thermal creep will tend to relax thermal stresses as well. Eventually, with the reactor on, the stresses should be largely due to primary stresses. When the reactor is off, thermal stresses will appear causing tension in the regions which had higher temperatures and compression in the regions which were cooler.

The change in thermal and elastic properties of V-4Cr-4Ti due to irradiation is expected to be negligible. Irradiation hardening increases the ultimate and yield strength at some temperatures. However, no design advantage of this strength increase is taken because overheating events could anneal out this hardening. The uniform and total elongations of V-4Cr-4Ti, irradiated up to 34 dpa with peak He contents of 75 appm (at test temperatures of 420–600 °C), remain above 8% and 10% respectively [8]. The elongation vs. damage of similar alloys (V-9Cr-5Ti and V-14Cr-5Ti) shows virtually no ductility loss from ≈ 30 dpa to 120 dpa, the limit of the experiments [8]. In this study, it is assumed the uniform elongation remains above 5%, allowing the irradiated material to be classified as ductile. Therefore, no penalty is imposed on the allowable stress.

5.3.4. Results

The maximum surface heat flux on the outboard first wall is 0.47 MW/m² at 1.6 m from the midplane. The maximum span of the outboard first wall as a function of first wall thickness at this location is shown in Figure 5.3-6. The region enclosed by the solid curve and the abscissa indicates combinations of first wall thickness and coolant channel span which meet the stress limit criteria. Outside of this region, the peak stresses exceed the maximum allowable. A coolant pressure of 0.5 MPa and a coolant temperature of 550 °C is assumed. In the rising portion of the line, the maximum span is dictated by the limit on primary bending stress. At greater wall thicknesses, the primary + secondary stress limit is more restrictive.

Notice that the reference design of 3 mm thick first wall and 75 mm span (marked by an 'x' in Figure 5.3-6) is just within the acceptable stress limits for the material. However, there is no margin for erosion of the first wall.

Compare this plot with Figure 5.3-7 which shows the design space for the lower surface heat flux and higher neutron wall loading at the midplane. The design space is larger;

Figure 5.3-6. The maximum span of the outboard first wall as a function of first wall thickness is indicated by the solid line. The surface heat flux and volumetric nuclear heating correspond to a point 1.6 m from the midplane. The surface heat flux is 0.47 MW/m^2 , the maximum for the outboard first wall. Wall thicknesses over 5.6 mm cause the maximum temperature to exceed 700°C . The reference design is indicated by the 'x'.

Figure 5.3-7. Maximum span of the outboard first wall vs. first wall thickness at the midplane. First wall surface heat flux is 0.42 MW/m². The reference design, indicated by the 'x', is not acceptable.

however, the primary bending stress limit (the rising portion of the line) remains the same since the coolant pressure is the same. The radius of the first wall (from the reactor bore) reaches a maximum at the midplane; thus, the span of the coolant channels is at its maximum of 85 mm. The resulting pressure stress of 203 MPa is greater than the 162 MPa limit. The maximum span for a 3 mm first wall is 75.7 mm at the midplane. A reduction in span (by dividing the first wall segments into more coolant channels toroidally) or a reduction in coolant pressure or an increase in first wall thickness will be necessary. The reference design calls for dividing the blanket into 15 coolant channels toroidally (the first wall has 30 coolant channels). Increasing this to 17 (34 for the first wall) would lower the primary stress to acceptable limits. However, there would remain no margin for erosion.

As these plots show, increasing the thickness of the outboard first wall would reduce pressure stresses to acceptable levels without causing excessive thermal stresses. The above figures also show the upper limit on first wall thickness as dictated by the limit on the maximum operating temperature of V-4Cr-4Ti (700 °C), rather than stress. The exact values of these upper limits are subject to some uncertainty, since the 550 °C coolant temperature is an estimate. However, variation of the coolant temperature would not affect the stress limit significantly, since material properties depend weakly on temperature.

A similar plot for the inboard first wall at the midplane is shown in Figure 5.3-8. The surface heat flux here is 0.40 MW/m². Note that the reference design span is smaller for the inboard first wall. As a result, the pressure stresses are much lower and the design is acceptable.

Like the outboard first wall, the maximum surface heat flux on the inboard first wall is not at the midplane. The maximum surface heat flux is 0.48 MW/m² and occurs at 1.9 m from the midplane. The maximum span vs. thickness of the inboard first wall at this location is plotted in Figure 5.3-9.

The maximum permissible span at a given thickness varies with coolant pressure as an inverse square root (see Equation 5.3-3). Thus, doubling the coolant pressure would decrease the maximum span by a factor $\sqrt{2} = 1.414$ (at any first wall thickness). Using this relation, the above results can be extended to any coolant pressure.

Membrane stresses appear in the blanket structure and are proportional to coolant pressure. Assuming a coolant pressure of 0.5 MPa, the average primary membrane stress in the webs connecting the outboard first wall to the kink stabilizing shell is about 8 MPa for the reference design. The corresponding webs on the inboard component experience

Figure 5.3-8. Maximum span of the inboard first wall vs. first wall thickness at the midplane. First wall surface heat flux is 0.40 MW/m^2 . The reference design, indicated by the 'x', is within stress and temperature limits.

Figure 5.3-9. Maximum span of the inboard first wall vs. first wall thickness at 1.9 m from the midplane. First wall surface heat flux is 0.48 MW/m². The reference design, indicated by the 'x', is within stress and temperature limits.

5 MPa of membrane stress. The membrane stress in the internal plates of the outboard blanket are higher at 11 MPa. These loads are well below the 108 MPa limit.

The kink stabilizing shell experiences bending stresses of 14 MPa in the reference design with a coolant pressure of 0.5 MPa; its inboard counterpart undergoes 57 MPa of bending stress. Including the minimal membrane stresses, these are below the 162 MPa limit.

The back wall of the first wall/blanket component (see Figure 5.3-1) is, like the first wall, a pressure boundary. The span of the channels adjacent to this wall is twice the span of the first wall. The reference design calls for a 3 mm thick back wall on the outboard component. This causes unacceptably high primary bending stresses. The thickness will need to be increased to at least 6.9 mm at the midplane for 0.5 MPa coolant pressure. On the inboard first wall/blanket component, the shorter span and thicker back wall result in acceptable stresses.

5.3.5. Summary and Conclusions

The reference design of the outboard first wall is acceptable if steps are taken to reduce the pressure stress at the midplane. These steps include: reduce the coolant channel span by dividing the first wall/blanket modules into more channels toroidally, increase the first wall thickness, and reduce the coolant pressure. A small change in any of these will make the design acceptable. However, a larger change is required to allow a margin for erosion of the outboard first wall and additional electromagnetic loads from disruptions. The maximum wall thickness seems to be determined by maximum temperature limits, rather than stress limits. The inboard first wall design is acceptable without modification.

At 3 mm, the back wall of outboard first wall/blanket component is too thin to support the pressure stress. At least a 7 mm thick back wall is needed.

Thermal creep deformation, creep-rupture times, and total swelling should not be issues for this design. Present irradiation creep data indicate that deformations due to irradiation creep may exceed the recommended 5% limit before the end of life. Irradiation creep data at higher fluence are necessary to evaluate this concern.

The reference design of the blanket and shields do not experience significant loads and are acceptable.

REFERENCES

- [1] ASME Boiler and Pressure Vessel Code (1989), Sec. III, Div. 1, App. III, pp. 268-270.
- [2] ITER Materials Properties Handbook, ed. J. W. Davis, Draft #3, Feb. 1996.
- [3] R. J. Farraro and R. B. McLellan, "High temperature elastic properties of polycrystalline niobium, tantalum and vanadium," *Metallurgical Transactions A*, 10A, Nov. 1979, pp. 1699-1702.
- [4] W. A. Simpson, "Room temperature elastic properties of V-5Cr-5Ti," Fusion Materials Semiannual Progress Report for period ending March 31, 1994, DOE/ER-0313/16 (Sept. 1994), pp. 258-259.
- [5] H. M. Chung, B. A. Loomis and D. L. Smith, "Thermal creep of vanadium-base alloys," in "U.S. Contribution, 1994 Summary Report, Task T12: Compatibility and Irradiation Testing of Vanadium Alloys," ed. D. L. Smith, ANL/FPP/TM-287, ITER/US/95/IV MAT 10, March 1995, pp. 87-94.
- [6] H. M. Chung, B. A. Loomis and D. L. Smith, "Effect of neutron irradiation on swelling of vanadium-base alloys," in "U.S. Contribution, 1994 Summary Report, Task T12: Compatibility and Irradiation Testing of Vanadium Alloys," ed. D. L. Smith, ANL/FPP/TM-287, ITER/US/95/IV MAT 10, March 1995, pp. 123-130.
- [7] V. M. Troyanov, *et. al.*, "Irradiation creep of V-Ti-Cr alloy in BR-10 reactor core instrumented experiments," presented as paper 110040-O at ICFRM-7, Obninsk, Russia, Sept. 25-29, 1995.
- [8] B. A. Loomis, H. M. Chung, and D. L. Smith, "Effects of neutron irradiation on tensile properties of vanadium-base alloys," in "U.S. Contribution, 1994 Summary Report, Task T12: Compatibility and Irradiation Testing of Vanadium Alloys," ed. D. L. Smith, ANL/FPP/TM-287, ITER/US/95/IV MAT 10, March 1995, pp. 139-146.

Chapter 6

Radiation Protection and Shielding Analysis

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6.1. Introduction

The shielding system is a major element of the fusion power core (FPC). It consists of the bulk (or magnet) shield, penetration shield, and biological shield. The primary function of the shield is radiation protection; protection of superconducting magnets, vacuum vessel, workers, and the public. As an element of the power core, the bulk shield must meet other requirements for power production and service lifetime. In addition, it serves as a heat sink for the FW/blanket decay heat during a loss-of-coolant accident. The biological shield controls the radiation level outside the plant and adds a confinement barrier against radiological release. All shielding elements must be integrated with the remainder of the FPC to meet the assembly, mechanical support and attachments, and maintenance requirements. The system requirements developed for the shield stem from these essential shielding functions. This chapter documents the shielding system requirements and describes in detail the shield design, emphasizing the attainment of the requirements. These economic and safety requirements were integrated into the ARIES-RS design from the beginning in a cost-effective manner. Safety requirements severely limit the material choices for all in-vessel components, while economics-based performance requirements provide strong incentives to operate at high coolant temperature and thermal efficiency. The economic requirements constrain many aspects of the design and urge designers to optimize the performance of all in-vessel components, employ low cost materials, and extend the useful life of all components as much as practically possible.

The project has evaluated several candidate engineering design options and finally adopted features of the engineering systems of the ARIES-II design [1] as a starting point [2]. Hence, an extensive assessment of improvements to the ARIES-II shield design options has been performed along with the economic impact of the various options on the overall cost of the machines. The shielding analysis forms the basis for the shield design, using material optimization as one of the central themes of the study. Several shielding components of ARIES-II were modified to improve the performance and economics. Section 6.3 highlights the improvements made to the bulk shield, the rationale for making the changes, and the design optimization activities that enhanced the performance and reduced the cost of the shield. It also highlights the ARIES-RS safety issues that were solved by design improvements. Design features for the penetration shield and bioshield are discussed briefly in Sections 6.4 and 6.5. The constraints on the design imposed by the maintenance scheme are presented in Section 6.6. Lastly, principal conclusions of the radiation protection and shielding analysis are summarized in Section 6.7.

6.2. System Requirements for Shield

A set of system requirements for the shield of U.S. fusion power plants were developed concurrently with all plant subsystems. The requirements relate to the performance, economic, and safety features of the design as seen from the utility point of view [3]. During the course of the study, examinations were made of the ability of the reference shield design to meet the requirements. As listed in Table 6.2-I, the requirements are:

1. Provide Magnet Protection

The prime function of the bulk shield is to protect the superconducting magnet against radiation. The magnet limits provided in Table 6.2-I are set by the magnet designers to assure the proper performance of the magnet during operation.

2. Provide Vacuum Vessel Protection

Controlling the neutron-induced helium level in the steel of the vacuum vessel enables rewelding of the vacuum vessel at any time during the 50 y planned operation. It is likely that the magnet shield will also provide lifetime protection for the vacuum vessel.

3. Lifetime Component

The shield is massive and costly and, therefore, should be a lifetime component requiring no replacement during plant operation due to radiation damage considerations. Its own lifetime protection is provided by sizing the blanket and reflector so that the atomic displacement to the V structure of the shield is below 200 dpa.

4. Power Production Component

About 20% of the nuclear heating is generated in the shield. This must be recovered as high grade heat to improve the economics, meaning that the shield should be a power production component.

5. Safety and Environmental Impact

Shielding materials that exhibit radiological disintegration during operation or decompose at high temperatures ($\sim 800^{\circ}\text{C}$) during a loss-of-coolant accident (LOCA) should absolutely be excluded. As a general design requirement, the shield should qualify as a Class C low level waste at the end of plant operation. This means both vanadium and steel alloys should be produced with strict specifications to control impurities of radiological concern, such as Nb, Mo, Ag, Ir, etc.

Table 6.2-I.

**SYSTEM REQUIREMENTS DEVELOPED
FOR THE SHIELD OF U.S. POWER PLANTS**

• Provide lifetime protection for magnets	$< 10^{19} \text{ n/cm}^2$ $< 2 \text{ mW/cm}^3$ $< 10^{11} \text{ rad}$ $< 6 \times 10^{-3} \text{ dpa}$
• Provide lifetime protection for vacuum vessel	$< 1 \text{ He appm}$
• Must be a lifetime component	$< 200 \text{ dpa for V}$ $< 150 \text{ dpa for steel}$
• Must be a power production component	
• Must have low safety and environmental impact:	
– Low level waste (Class C with impurity control)	
– No hazardous materials	
– No damage in case of LOCA/LOFA	
• Must have a reasonable cost ($< 200 \text{ M\$}$)	
• Must meet stress and temperature limits	
• Must be reliable, maintainable, and replaceable	
• Should not expose workers to more than 2.5 mrem/h during operation	

6. Economic Requirement

The shield is a major cost item. Reducing its cost to below 200 M\$ is the most challenging item on the list of requirements. A serious effort is made in the ARIES-RS study to fulfill this cost requirement.

7. Stress and Temperature Requirements

Proper arrangement of the coolant channels within the shield assures that the stress and temperature limits for V and steel structures are not exceeded. The design limits on peak temperatures for V and steel are 700 and 550°C, respectively.

8. Reliability, Maintainability, and Replaceability

The shield configuration should be simple and have the ability to be quickly maintained. Even though the shield is a permanent component, the design should allow for its removal if the magnet needs replacement due to malfunction. During shutdown, the maintenance process will be performed using robotics and remote handling equipments.

9. Radiological Protection

The bioshield that surrounds the torus should be sized to limit the biological dose to the public and workers to 2.5 mrem/h during operation.

As highlighted throughout this chapter, all shielding system requirements have been met through a series of design improvements.

6.3. Cost-Effective Bulk Shield Design

6.3.1. Design Choices

Vanadium alloys and low-activation ferritic steels are the most promising candidates for the bulk shielding components. V offers significant advantages in terms of low activation characteristics and high thermal performance capability. On the other hand, low-activation steels are relatively inexpensive, have a higher shielding performance than V, and have the largest database and industrial support, and hence the smallest uncertainty in performance. Vanadium alloys certainly offer the benefits of operating at higher wall loadings (making the design more compact and less expensive), operating at higher

temperatures (meaning higher thermal conversion efficiency), and having lower radioactive inventory and afterheat. Special materials, such as B_4C and WC, offer advantages for local shielding applications. Their high cost limits their use in large quantities so that most of the shielding materials still would be vanadium or steel.

Previous designs traditionally employed water-cooled steel shields to protect the magnets. For safety and technical reasons, the water-cooled steel shield cannot be used in the Li/V designs. The use of lithium breeder will probably make the use of water (or organic coolant) in the shield impossible for safety reasons. No coolant other than Li is known to be compatible with V.

6.3.2. Rationale for Design Changes

Even though V alloys offer several advantages over steels, the economic advantage of V is limited. Because V structure is expensive (300\$/kg), power plants made entirely out of V structure will not be competitive (all costs reported herein are quoted in 1992 dollars). Thus, some design tradeoffs are necessary in order to improve the economics of Li/V designs and minimize the impact on the safety and environmental features of the design. For instance, the space between the coolant channels of the shield could be filled with cheap filler materials, instead of being made out of fabricated structures. There is no structural role envisioned for the filler materials. Another improvement that helps reduce the cost of the shield is the use of steel filler, instead of V filler [4]. Besides having a lower unit cost, steel has better shielding performance than V and thus results in thinner radial builds and a smaller machine. The economic analysis provided by the ARIES systems code (ASC) has indicated that these modifications to the shield design have lowered the cost of electricity by tens of mills/kW_eh. However, the shield still represents a major cost item and needs further improvements.

6.3.3. Shield Improvements

This section illustrates the steps by which the shield was modified while satisfying the prescribed system requirements. During the study, the ASC was heavily used to quantify the cost savings of the proposed changes before incorporating them in the ARIES-RS design. The starting configuration of the shield is taken from the ARIES-II design and consists of 40 cm steel shield followed by 23 cm B_4C shield on the inboard. A less efficient, cheaper steel shield is utilized in the less constrained outboard and divertor regions. All shields contain 15% V structure and 5% Li coolant, by volume. A breakdown

of the cost has revealed that the outboard shield constitutes more than 50% of the shield cost and, more importantly, the V structure comprises ~ 60% of the overall shield cost, even though it occupies only 15% of the shielding space. The cost breakdown is illustrated in Figure 6.3-1. These findings have prompted the need to minimize the use of V structure in the ARIES-RS shield (and external systems) and instead utilize less expensive steel structure to reduce cost. It is essential, therefore, to limit the use of V to components where high temperature performance is most needed, i.e., in the plasma facing components and blanket.

The following three main modifications would offer potential improvements to the shield design:

1. The unit cost of steel should be revisited and compared with estimates from other designs for components with similar level of complexity.
2. Further enhancement to the shielding performance is needed by optimizing the composition and employing more efficient, inexpensive shielding materials.
3. The use of advanced V material must be limited to those regions where it is absolutely necessary for high temperature operation.

The impact of the shield improvements on the overall size and cost was assessed using the ASC. Several runs were made to quantify the cost saving of each change made to the shield design. The changes were implemented one at a time sequentially and then the incremental cost reduction for each sequential change was determined.

The impact of changing the unit costs of fabricated steel structure and filler was investigated first. The ARIES-II design was based on unit costs for steel structure of 68 \$/kg and steel filler of 25 \$/kg. Since the shield and vacuum vessel (V.V.) are well protected and not subjected to as high a radiation level as the FW/blanket, any type of steel could be used in these components. There are several low activation steels (MHT-9, Tenelon, Fe1422, 316SS, ORNL 9Cr-2WVTa, F82H-M, MANET, etc.) that are readily available for use in fusion power plants. As shown later, steel will be employed in the outer part of the shield, V.V., and all ex-vessel components. The steel shield and V.V. will run at low temperatures relative to the FW/blanket; will not suffer as much radiation damage (meaning simpler welds or less inspection); will have much simpler configuration, fewer attachments, and less plumbing; and will thus have the ability to be quickly maintained. These features should translate into lower costs for such moderate-complexity components. Other designs [5, 6] have quoted lower cost estimates such as

Figure 6.3-1. Breakdown of the shield showing V alloy is dominating the cost.

~ 35 \$/kg for steel structures and ~ 10 \$/kg for steel fillers. These values are based upon industrial experience with large steel structures and cost estimates for similar shielding and V.V. components. Using these lower unit costs, the shield and V.V. cost has been reduced substantially by ~ 100 M\$ and the overall cost of electricity (COE) has dropped by 3 mills/kW_eh.

The second change enhances the performance of the shield by employing more efficient shielding materials such as tungsten carbide and borated-steel [7]. Specifically, boron carbide was replaced by tungsten carbide (65 \$/kg) in the inboard side and the steel filler was replaced by borated-steel filler (10 \$/kg) in the outboard and divertor regions. These modifications have reduced the radial builds of ARIES-II by 8-20 cm, the major radius by 8 cm, and the COE by 2 mills/kW_eh.

The third change is to limit the use of the vanadium structure in the shield. Since steel cannot operate at temperatures as high as V, the use of steel structure in the entire shield will certainly degrade the overall thermal conversion efficiency of the system. In addition, the option of running the steel shield at very low temperature is not viable because the nuclear heating generated in the shield is significant ($\sim 20\%$ of the total heating) and cannot be dumped as low grade heat without negatively impacting the power balance of the machine. For these reasons, V structure should be used in all or part of the shield to increase the ability to extract the heat with Li coolant at high efficiency for the purpose of generating electricity. An attractive solution is to divide the shield into two parts: the inner part follows the blanket and operates at a high temperature, while the outer part operates at a relatively lower temperature ($\sim 200^\circ\text{C}$). Hence, the high temperature (HT) shield along with the FW and blanket employs V structure whereas the low temperature (LT) shield utilizes stainless steel as the main structural material. The heat extracted from the LT shield will not be recovered. The tradeoffs between stainless steel and advanced materials will thus depend on the dividing boundary between the two layers of the shield and on how much power could be dumped as low grade heat without overly affecting the power balance. It is anticipated that the allowable reduction in the useful thermal power to be on the order of 1-5%. The cost saving due to segmenting the shield is 2 mills/kW_eh. This modification should not impact the safety characteristics of the design as the shield is subjected to low radiation flux, generating low levels of radioactivity and afterheat. In fact, segmenting the shield into two zones helps the design in case of an accident. It is true that the gap between the two zones will slow down the conduction of decay heat from the HT shield to the LT shield, but more importantly, the LOCA analysis (documented in Chapter 10) has shown that the He cooled LT shield which operates at a temperature below 200°C (as compared to

Table 6.3-I.
COST IMPACT OF CHANGES MADE TO
THE SHIELD DESIGN (IN 1992\$)

	Shield Cost (M\$)	COE (mills/kW _e h)	Incremental Reduction in COE (mills/kW _e h)
ARIES-II Design	366	73.8	
Modified Design:			
Reduced steel unit cost	295	70.9	2.9
More efficient shield	248	69.0	1.9
HT/LT shield	164	66.8	2.2

600°C in ARIES-II), acts as a heat sink and helps the FW temperature to drop faster after the first day following any accident.

By making the three changes mentioned above, the COE has been reduced substantially as compared with the original design. Table 6.3-I details the impact of the sequential changes made on the cost of the shield and COE for the ARIES-II design. Note that the reduced unit cost of steel results in the largest cost reduction. Table 6.3-II compares the original and modified design, emphasizing the impact of the changes made on the machine sizes, shield dimensions, and total (direct and indirect) costs.

These results pertain to the case where the LT shield is sized to contain 1% (~ 20 MW) of the total nuclear heating generated in the in-vessel components. For this particular case, the modified shield contains ~ 5% V and ~ 10% steel structures. The impact of dumping more heat (e.g., 5%) in the LT shield was quantified and the results are given in Table 6.3-III for 1 GWe power plants with 46% net thermal efficiency. Note that as more heat is dumped, the LT shield gets thicker allowing more V structure to be replaced with steel, thus resulting in a lower cost. Moreover, the thicker i/b LT WC shield overprotects the magnet. A thinner i/b shield is thus needed for the 5% case to meet the magnet

Table 6.3-II.

**SELECTED PARAMETERS FOR THE ORIGINAL ARIES-II
AND MODIFIED DESIGNS**

(1 GWe net power with 46% thermal efficiency)

Design	ARIES-II	Modified
HT/LT shield thickness (cm):		
Inboard	70/0	30/32
Outboard	100/0	30/50
Divertor	105/0	60/36
Major radius (m)	5.6	5.52
Minor radius (m)	1.4	1.38
Neutron energy multiplication	1.38	1.366
Masses (tonnes):		
Shield	6,020	5,090
FPC	10,800	9,730
Shield Cost (M\$)	366	164
V.V. Cost (M\$)	50	27
Total Cost (M\$)	4,170	3,700
COE (mills/kW _e h)	74	67

Table 6.3-III.

**COST IMPACT OF ALLOWING HIGHER FRACTION
OF THE NUCLEAR HEATING IN THE LT SHIELD**

Heating in LT Shield	1%	5%
Neutron Energy Multiplication	1.366	1.31
HT/LT Shield Thickness (cm):		
Inboard	30/32	15/42
Outboard	30/50	15/63
Divertor	60/36	36/57
R (m)	5.52	5.48
a (m)	1.38	1.37
Shield Cost (M\$)	164	134
COE (mills/kW _e h)	67	66

radiation limits, leading to a smaller machine. This tradeoff indicates that throwing away more heat to make a less expensive shield may in fact pay off.

6.3.4. ARIES-RS Final Shield Design

This section provides the final dimensions and composition of the bulk shield which is designed primarily to protect the superconducting magnet against radiation. The mechanical and thermal analyses of the shield are covered in Chapter 5. Figures 6.3-2, 6.3-3, and 6.3-4 display an elevation view of ARIES-RS and cross sections of the i/b and o/b in-vessel components. The ARIES-RS shield is based on the modified ARIES-II shield [4, 8] with several changes added to reflect the latest physics and engineering evolutions in the ARIES-RS design. The main changes are:

Figure 6.3-2. Elevation view of ARIES-RS.

Dimensions in meter

Figure 6.3-3. Cross section of inboard blanket and shield.

1. Several in-vessel components were resized to protect the permanent components (shield, V.V., and magnet) for the longer life of ARIES-RS (40 FPY vs. 30 FPY for previous ARIES designs). This required a 2-3 cm addition to the radial dimensions of the i/b replaceable shield and o/b reflector.
2. The calculational model was updated to include the latest changes to the FW, blanket, and magnet designs and the neutron source strength was normalized to the higher wall loadings of ARIES-RS. Section 3.3.6 documents the latest parameters for the various components and Figures 3.3-10, 3.3-11, and 3.3-12 depict the radial builds.
3. The new configuration of the V.V. was considered in the analysis. Instead of solid 10 cm thick steel structure, the new V.V. consists of a double wall steel structure stiffened with steel ribs. For shielding considerations, the space between the 3 cm thick walls is filled with WC/B₄C fillers on the inboard side and with borated steel plates on the divertor and outboard sides.
4. A few modifications have been implemented in the design to help reduce the FW temperature during LOCA. In response to the safety analysis, which proceeded iteratively with guidance from the neutronics analysis, the steel filler of the inboard replaceable shield was replaced by V filler to reduce the decay heat and the peak FW temperature attainable during LOCA. An additional change is the use of helium gas to cool the LT shield and V.V. The He cooled components nominally operate at 200°C (as opposed to 600°C in ARIES-II) and act as a heat sink, helping the FW temperature to drop faster after the first day following an accident.
5. Safety factors were introduced to the shielding analysis to correct the radiation damage results obtained from the 1-D calculations. These safety factors account for uncertainties in nuclear data, calculational model, and effect of radiation streaming through the 2 cm wide radial gaps between modules [9, 10]. The streaming effect has been mitigated by offsetting the blanket gaps from the shield gaps. The recommended safety factors for the ARIES-RS design are 2 for the integral nuclear heating and 3 for the local radiation effects, such as fast neutron fluence, peak nuclear heating, peak dose to insulator, and peak dpa to stabilizer. The impact of including the safety factors in the analysis is to thicken the shield by ~ 6-7 cm.
6. The cross section library was updated and the international FENDL-1 library is now used in the shielding analysis in place of the U.S. ENDF/B-V library. The new library results in 4-20% lower radiation effects at the magnet with the largest

reduction in the nuclear heating (refer to Section 3.3). This reduction in damage translates into 1-2 cm thinner shield.

The shielding model used the discrete-ordinates code DANTSYS [11] with the FENDL-1 library in 46n - 21g group structure. The i/b and o/b components of ARIES-RS were modeled in toroidal cylindrical geometry around the machine axis and the neutron source strength was normalized to the peak wall loadings.

Table 6.3-IV lists the various shield dimensions required to adequately protect the magnet. The HT shield contains steel (Tenelon and MHT-9) filler while the use of more efficient shielding materials (WC, B₄C, and borated steel) is limited to the LT shield where the radiation environment is less severe. To reduce the size and cost of the overall machine, the highly efficient and more expensive WC and B₄C fillers are utilized in the space-limited inboard side while borated steel is used elsewhere. WC filler is used in the 2 m high central zone around the i/b midplane whereas B₄C fills the remaining space of the i/b LT shield. Tenelon has a superior shielding performance compared to other steels. It is thus employed as the main filler and structural material for all steel-based components except in the divertor support structure. Instead, modified HT-9 is used for having lower decay heat, a property of great importance for highly irradiated components. Near the end of the study, new information became available from the materials community on the ORNL 9Cr-2WVTa steel and the international IEA modified F82H steel [12]. Due to the near completion stage of the ARIES-RS design, it would have been difficult to replace the Tenelon and MHT-9 by the recommended steels and complete the redesign within the remaining time of the study. Nevertheless, the low activation IEA Mod F82H steel will be considered in future designs.

Table 6.3-IV indicates that all radiation levels are within the allowables. The replaceable components (FW, blanket, reflector, and divertor support structure) are sized to result in ≤ 200 dpa at the V structure of the shield. All shields are thus lifetime components. The magnet radiation effects are below the limits, meaning that the magnets will perform properly for 40 FPY and there is no need for magnet annealing. The shield will also provide lifetime protection for the V.V. as evidence by the fact that the neutron-induced helium level is below the 1 appm limit. This warrants the ability to reweld the V.V. at any time during the 50 years of operation. It should be mentioned that the V.V. and magnet radiation damage reported in Table 6.3-IV include a safety factor of 3 that accounts for the uncertainties in the various elements of the analysis. The integral nuclear heating to the 16 TF magnets amounts to 6 kW. This corresponds to an acceptable cryogenic load of ~ 2 MW.

Table 6.3-IV.

**KEY BLANKET AND SHIELD DIMENSIONS AND
RADIATION EFFECTS AT SHIELD, V.V., AND MAGNET**

	Inboard	Outboard	Divertor
Thickness (cm):			
FW or divertor plates	0.3	0.3	5
Blanket	20	50	0
Replaceable shield, reflector, or divertor support structure	20	7	20
Permanent shields:			
HT shield	26	28	35
LT shield	28	40	45
Vacuum vessel	20	30	20
Total (excluding gaps)	114	155	125
Peak neutron wall loading (MW/m ²)	3.7	5.6	2.3
Peak dpa to V of shield (dpa@40 FPY)	190	200	200
Peak He production in V.V. (appm@40 FPY)	0.2	3×10^{-2}	6×10^{-3}
Damage to magnet:			
Peak fast neutron fluence to Nb ₃ Sn (n/cm ² @40 FPY, E _n > 0.1 MeV)	1×10^{19}	1×10^{19}	8×10^{18}
Peak nuclear heating (mW/cm ³)	0.4	0.2	0.17
Peak dose to GFF polyimide (rad@40 FPY)	1.4×10^{10}	7×10^9	6×10^9
Peak dpa to Cu stabilizer (dpa@40 FPY)	5.4×10^{-3}	4×10^{-3}	3.3×10^{-3}

In standard diverted tokamaks, a critical shielding area exists above/below the i/b blanket where the shield recesses to accommodate the inner divertor plate. The shapes of the i/b blanket and divertor plate were designed to protect the recessed surface and the magnet against source neutrons. 3-D analysis indicated that the neutron wall loading at the recessed surface is relatively low (0.035 MW/m^2) due to the shadowing effect of the inner plate (see Section 3.3.1). The radial space therein is approximately one meter thick. It consists of 20 cm structural ring, 26 cm HT shield, 28 cm LT shield, 20 cm V.V., and a few 1–2 cm wide assembly gaps. The calculations showed that the one meter shielding space adequately protects the inner legs of the magnets providing that WC filler is used in both V.V. and LT shield.

6.4. Local and RF Penetration Shields

Penetrations are well-known to degrade the performance of the shield as radiations stream through and produce local hot spots at the magnets and external components. The divertor pumping ducts and RF launchers are the major penetrations in ARIES-RS. Small penetrations for pellet fueling and diagnostics are expected to be much smaller and less of a concern. Assembly gaps will enhance the streaming of radiation [10]. There are 2 cm wide radial gaps between the 16 i/b and o/b blanket/shield modules. The streaming through the gaps is alleviated in ARIES-RS by providing bends in the o/b gaps and offsetting the i/b blanket gaps from the i/b shield gaps (see Chapter 4).

Radiation streaming through the large penetrations of ARIES-RS is evaluated in a qualitative manner and estimates for local shields around penetrations were obtained by scaling from previous 3-D streaming analyses. The design calls for a divertor pumping duct for each divertor sector, totaling 32 ducts for the top and bottom divertors (see Chapter 7). The ARIES-RS divertor pumping ducts do not present a serious streaming problem since none of the ducts are in direct view of the plasma and 2–3 bends are provided in each duct to minimize the streaming (refer to Figure 3.3-2). Moreover, there is ample space at the top/bottom of the device for supplementary shielding if needed.

The current drive system consists of three kinds of RF launchers that drive $\sim 10\%$ of the plasma current. The RF system is compact and all launchers fit in a single o/b module occupying $\sim 2.5 \text{ m}^2$ (1%) of the o/b FW area. The launchers are exposed to the highest neutron wall loading of the machine ($5\text{--}5.6 \text{ MW/m}^2$). The Li cooled V folded waveguides, comprising less than 25% of the launcher volume, provide some attenuation for the streaming neutrons. Elements susceptible to radiation damage, such

Table 6.4-I.

**KEY PARAMETERS FOR RF LAUNCHERS AND REQUIRED
PENETRATION SHIELD**

Launchers	ICRH	HFFW	LHW
Location	above o/b midplane	below o/b midplane	below o/b midplane
Number of units	1	1	5
Dimensions (m):			
Toroidal	1.53	1.6	0.68-0.79
Poloidal	0.51	0.5	0.24-0.27
Radial	0.8	0.2	1.5
Number of coaxial lines	12	12	—
Diameter of coax (cm)	16	10	—
Thickness of penetration shield (cm)	50	20	80

as electrical insulators, are located in well protected areas and are not in direct view of the plasma. The location, size of penetration, and estimates for the penetration shield required to protect the sides of the magnet are summarized in Table 6.4-I. A more detailed description of the RF system is given in Chapter 9. The coaxial lines are embedded in the shield and can accommodate several bends to minimize the streaming. The size of the penetration shield depends on the opening area and radial depth. The LHW launcher penetrates deeply through the entire blanket and shield and thus requires a relatively thicker penetration shield. Since all three launchers are located in a single module, the combined coaxial lines and LHW penetration can be surrounded by one meter thick shield. There is enough space inside the maintenance port on the outboard side to accommodate this shield without any access problem.

6.5. Biological Shield

The primary function of the bioshield is to provide radiological protection for workers and the public. The cryostat is surrounded by a 2.5 m thick cylindrical steel-reinforced concrete bioshield. In the calculational model, the outboard blanket, shield, and V.V. were included while the magnets were excluded in order to obtain the highest dose outside the bioshield. The dose during operation is below 0.5 mrem/h. This is consistent with DOE guidelines for protection of workers and reflects a factor of 5 reduction in the absolute limit (2.5 mrem/h).

The dose after shutdown is below the operational dose by approximately five orders of magnitude. Nevertheless, the dose inside the bioshield remains excessive and exceeds the limit for hands-on maintenance even at the end of the 2 months maintenance period. Therefore, personnel access to reactor hall is not allowed at any time during maintenance. This is further explained in the following section.

6.6. Remote Handling Requirements

Maintainability is a top-level requirement for the ARIES-RS power plants. The maintenance process includes in-situ inspection and replacement of the in-vessel components, transportation of components out of and into the V.V., and transportation of radioactive components in flasks to the hot cell. The maintenance components will thus be exposed to various gamma radiation levels that vary by 6-7 orders of magnitude between the FW and V.V.

The biological dose to personnel is a key factor in determining the type of maintenance process, hands-on or remote. Figure 6.6-1 illustrates the variation of contact dose with time after shutdown for the outboard FW and V.V. of ARIES-RS. The maintenance activity is likely to start no sooner than one day after shutdown to take advantage of the significant reduction in dose after 24 h. Obviously, hands-on maintenance is not feasible within the cryostat during the 1-2 months maintenance period and thus the ARIES-RS fusion power core must be maintained by both robotics and remote handling systems.

A machine designed for hands-on maintenance would be heavily shielded. In this case, a major redesign for the shielding system is needed by employing efficient gamma absorbing materials, e.g. lead, in the shield and thickening the bulk, penetration, and transporter/flash shields. Also, careful attention to streaming through penetrations and assembly gaps is essential. The heavy shield will certainly add several mills/kW_eh to the

Figure 6.6-1. Contact doses at the surfaces of the outboard first wall and vacuum vessel for an average o/b neutron wall loading of 4.5 MW/m^2 .

cost of electricity. The benefit of hands-on maintenance could be limited. The hands-on process will practically be allowed only in the limited area outside the V.V. for a short time of a few hours before opening the machine. Once the access ports are opened to pull out the FW/blanket/divertor for replacement, radiation will stream through the maintenance ports and contaminate the ex-vessel area. Clearly, the added cost and complexity offset the benefit of allowing hands-on maintenance within a limited area for a short period of time.

In general, the remote maintenance process takes longer time than the hands-on process [13]. However, employing more versatile robotics and manipulators will help shorten the remote maintenance time. The ARIES-RS machine has the ability to be quickly maintained. During the maintenance process, a full sector containing all the replaceable components will be removed horizontally as a single piece, eliminating the need for in-vessel operations. All coolant disconnections are made behind the shield in the port area. The full sector is transported through large ports to hot cells using shielded casks (refer to Chapter 4). Thus, the majority of the remote handling components will be subjected to low or moderate radiation levels while a few components will be exposed to the high PFC radiations, particularly in the hot cells. The maintenance elements are sensitive to heating doses caused by decay gamma radiation. The total dose is the sum of doses acquired each time the equipment is moved into the torus for routine maintenance.

The anticipated conditions for ARIES-RS during maintenance are summarized in Table 6.6-I. These conditions have led to a set of requirements on design specifications for robotics and remote handling (RH) equipments. Among these requirements, RH tools should tolerate high doses, should operate successfully for one or more maintenance period (1000–2000 h), should be made out of high heat resistance materials, should perform properly in an inert environment, should use liquid lithium or helium gas for cooling, and should not contain air, CO₂, N, or water in large quantities that may jeopardize the safety of the Li/V system. The first requirement is the most stringent to fulfill. It needs a dedicated effort to develop radiation hardened tools by improving the materials composition and fabrication process to make the sensitive components less susceptible to radiation damage. The testing program for radiation hardness is costly, especially for dose levels above 10⁵ rad. The rad-hard components will certainly be expensive due to their special design and limited marketability. Thus, the operation and maintenance (O&M) annual cost for fusion power plants should account for the expensive rad-hard components needed for RH systems.

Detailed lists of materials and critical components for RH equipments are documented in references 10–13. Almost all RH devices experience performance degradation in high

Table 6.6-I.

**ANTICIPATED CONDITIONS FOR ARIES-RS ONE DAY AFTER
SHUTDOWN DURING THE MAINTENANCE PROCESS**

In-vessel conditions:Dose rate of $\sim 10^7$ rad/h near FW

Components at 150–500°C

Inert gas atmosphere

Gamma energy 0.1–12 MeV

Ex-vessel conditions:Dose rate < 10 rad/h

Components at 25–150°C

Inert gas atmosphere

Soft gamma rays

radiation environments. In general, radiation degrades the optical, electrical, mechanical, and physical properties of sensitive elements and shortens their useful lifetimes. There is no common radiation limit for all RH equipments. Rather, there are several dose limits for sensitive elements used in optical devices, transmission lines, motors, sensors, and electronics. The most noticeable gamma radiation effects are the transmission losses in fiber optic signal cables, opaqueness of plastic fibers and darkness of lenses, degradation of optical properties, failure of solid state components, poor picture in TV cameras, breakdown of electric insulators, hardening of grease lubricants, erosion of coatings, degradation of adhesive bonds, hardening of seals causing leakage, and high noise level, spurious signals, ionization, and excitation in detectors.

Many RH components have been tested over the past 20 years in several gamma irradiation test facilities [13–17]. Commercially available off-the-shelf RH components, developed primarily for fission plants, are moderately expensive. However, these components are not suitable for fusion plants which could have 10^4 times greater dose to RH systems than fission. As a matter of fact, many off-the-shelf diagnostic components that were used for several months of DT operation in the TFTR experimental test facility [17] failed at a relatively low radiation level of 10^4 rad.

U.S. and Japan are currently developing rad-hard RH components for ITER and the forthcoming fusion power plants. The results of the irradiation tests performed at ORNL program [13-15, 18,19] and JAERI [16, 20] are summarized in Tables 6.6-II, 6.6-III, and 6.6-IV. At present, several irradiation tests are underway for other sensitive elements. Nevertheless, an extensive radiation hardening R&D program is still needed as ITER rad-hard tests have not exposed all materials and components to the high doses encountered in ARIES. For some components, ITER irradiation doses are ~ 1000 times lower than that of ARIES.

Future maintenance activities will identify the RH components needed for the various maintenance processes of ARIES-like power plants. Several design guidelines should be followed to assure the success of the maintenance operation. These include:

1. The RH components should receive doses lower than the absolute radiation limit by a safety margin of 2-3.
2. Potential design solutions should be developed to mitigate the radiation effects. For instance, sensitive elements can be surrounded by local shields, which could be bulky and expensive.
3. The self-shielding effect within large RH devices should be estimated as substantial shielding could be provided by massive parts.
4. Low radiation resistance components can be moved away from the high radiation zone and, in this case, signals must be transmitted by a relay system through wiggling channels and mazes.

6.7. Conclusions

Detailed shielding analyses raise no serious difficulties with ARIES-RS shield. A dedicated effort was devoted to the bulk shield in particular as it represents a major cost item for advanced designs. Neutronics and economics considerations were used iteratively to guide the shield toward an optimal configuration while maintaining the attractive safety features of the design. Significant savings in shield cost were obtained by implementing several cost-effective improvements. These are:

1. The use of expensive V alloy is limited to those regions where high temperature operation is absolutely necessary.

Table 6.6-II.

ORNL REMOTE HANDLING ACTIVITIES

	Failure Threshold (rad)	Irradiation Time (h)
Cables:	$10^9 - 10^{10}$	100 - 1000
Flexible cable		
Cable connectors		
leaky coaxial cable		
Bus bar materials		
Drives:	$10^8 - 10^9$	100 - 1000
Motors		
Contractor spindle drives		
Sealed lead-acid batteries		
Sensors:	$10^8 - 10^9$	100 - 1000
Load cells		
Switches		
Proximity sensors		
Optics:	$10^6 - 10^7$	100 - 1000
Monochrome tube-type cameras		
Fiber optic cables and connectors		
Fiber optic and sapphire windows		
CO ₂ laser mirrors		
Electronics:	$10^5 - 10^7$	< 1
Solid state components		
Microprocessor control system		

Table 6.6-III.
ORNL TESTING RESULTS

Items	Elements	Dose Limit (rad)	Irradiation Time (h)
Motors	Lubricant	2×10^{11}	40 y
	Seals	$> 10^9$	
Laser Beam	Lenses	10^7	
Reflecting mirrors	Mirrors	$> 10^7$	
Electronics	Chips	$10^3 - 10^4$	
	Flexible cables	10^9	
	Rigid cables	$10^{13} - 10^{14}$	
	Transistors	10^5	
	Semiconductors	10^5	
	Electron tubes	10^9	
	Capacitors	10^{10}	
	Switches	10^8	
	Connectors	10^9	
Periscope		2×10^9	800
Fiberscope	Mirrors	1.2×10^{10}	
	Glass	10^{10}	
	Optical fibers	1.2×10^{10}	
	Adhesive for lenses	6×10^9	
Cameras	All parts	5×10^7	250
Proximity detectors		10^9	

Table 6.6-IV.

JAERI TESTING RESULTS

Items	Elements	Dose Limit (rad)	Irradiation Time (h)
Motors	Grease lubricant	7×10^9	4,500
Laser cables	Optical fibers	10^8	1000
Laser beam	Lenses	3×10^9	2000
Reflecting mirrors	Mirrors	5×10^9	3,333
Electronics	Ceramic insulated cables	10^{10}	
Periscope	Mirrors & lenses	2×10^9	800
Fiberscope	Mirrors	1.2×10^{10}	
	Glass	10^{10}	
	Optical fibers	1.2×10^{10}	
	Adhesive for lenses	6×10^9	

2. Segmentation of the shield into high temperature and low temperature zones offers a major reduction in shield cost.
3. The nuclear heat deposited in the HT shield ($\sim 20\%$ of total heating) is recovered as high grade heat to enhance the overall power balance of the machine.
4. LT shield, vacuum vessel, and other external components (having low levels of nuclear heating that can be dumped without significantly affecting the power balance) can employ a cheap stainless steel as the main structural material, instead of V.
5. The use of cheap steel filler (rather than V filler) in the shield reduces the cost tremendously. Power plants made entirely out of solid V structure will not be economically competitive. Fillers have no structural role and thus have lower unit costs compared to structures.

6. Careful attention is paid to the arrangement of the high performance materials (normally expensive) within the shield. Highly efficient, expensive materials, such as WC and B_4C , are used only in the space-constrained inboard side to reduce the overall size and cost of the machine while less efficient, cheaper materials is used in the diverter and outboard sides.
7. Designing the shield to last for the entire plant life without replacement due to radiation damage considerations is essential to reduce the overall cost and to minimize the radwaste stream. Lifetime protection of the shield is achieved by properly sizing the blanket and reflector.
8. The shield is a moderate-complexity component and thus is not costed on the same basis as the blanket and plasma facing components. The shield has lower fabrication, installation, inspection, and quality assurance costs.

The high performance cost-optimized shield of ARIES-RS emphasizes the attainment of the top-level requirements. For instance, the bulk shield provides lifetime protection for the vacuum vessel and magnet, costs < 200 M\$, produces 20% of the power, generates Class C low level waste, and suffers no damage in case of LOCA. Moreover, the bulk shield has a simple configuration and a few attachments and plumbing. It is thus reliable and has the ability to be quickly maintained. Qualitative shielding assessments indicated no streaming problems for the divertor and CD penetrations. All penetrations include 1-2 bends and/or are not in direct line-of-sight with the plasma to minimize streaming effects.

The torus and its ancilliary system within the bioshield are maintained with robotics and remote handling system. Personnel access into the cryostat is not feasible at any time after shutdown. The development of radiation hardened tools is a necessity for the maintenance activity of ARIES-RS. Many new materials are currently coming into the market with improved radiation resistance. Benefits will follow the development of rad-hard components at ORNL and JAERI for the experimental device ITER. An aggressive R&D program is still needed to develop more radiation resistance components for ARIES-like power plants.

REFERENCES

- [1] F. Najmabadi, R. W. Conn, and the ARIES Team, "The ARIES-II and ARIES-IV Second Stability Reactors," *Fusion Tech.* **21** (1992) 1721.
- [2] M. S. Tillack, M. Billone, and L. El-Guebaly ^{et al.,} "Engineering Options for the U.S. Fusion Demo," *Proceedings of 16th IEEE Symposium on Fusion Engineering*, Champaign, IL, Vol. 1 (Oct. 1995) 1202.
- [3] F. Najmabadi and the ARIES Team, "Assessment of Options for Attractive Commercial and Demonstration Fusion Power Plants," *Proceedings of ANS 12th Topical Meeting on Technology of Fusion Energy*, Reno, Nevada, June 16-20, 1996.
- [4] L. A. El-Guebaly, "Neutronics Assessment for the ARIES Advanced Reactor Studies," *Fusion Engineering and Design* **28** (1995) 658.
- [5] B. Nelson, "ITER Vacuum Vessel Cost Estimate," Oak Ridge National Laboratory Memo to ITER U.S. Home Team (July 1993).
- [6] "Inertial Fusion Energy Reactor Design Studies, Prometheus-L and Prometheus-H," McDonnell Douglas Aerospace Report, MDC 92E0008 (March 1992).
- [7] L. A. El-Guebaly, "Neutronics Analysis for the Stellarator Power Plant Study SPPS," *Proceedings of 16th IEEE Symposium on Fusion Engineering*, Champaign, IL, Vol. 2 (Oct. 1995) 1162.
- [8] L. A. El-Guebaly and C. G. Bathke, "Cost-Effective Shield Designs for Environmentally Attractive Fusion Power Plants," *Fusion Technology* **30**, No. 3 (1996) 1058.
- [9] L. A. El-Guebaly, "Overview of the US-ITER Magnet Shield: Concept and Problems," *Fusion Technology* **19** (1991) 1475.
- [10] L. A. El-Guebaly and M. E. Sawan, "Shielding Analysis for ITER with Impact of Assembly Gaps and Design Inhomogeneities," *Proceedings of 8th International Conference on Radiation Shielding*, Arlington, TX (April 24-28, 1994).
- [11] R. Alcouffe, R. Baker, F. Brinkley et al., "DANTSYS: A Diffusion Accelerated Neutral Particle Transport Code System," Los Alamos National Laboratory report LA-12969-M (1995).

- [12] R. Klueh, Oak Ridge National Laboratory, private communication (Sept. 1996), and R. L. Klueh and M. L. Grossbeck, "Impurity Content of Reduced-Activation Ferritic Steels and Vanadium Alloy," Fusion Materials Semiannual Progress Report for Period Ending December 31, 1996, DOE/ER-0313/21.
- [13] T. Burgess et al., "Design Guidelines for Remotely Maintained Equipment," ORNL/TM-10864 (1988).
- [14] K. Vandergriff, "Design Equipment for Use in Gamma Radiation Environments, ORNL/TM-11175 (1990).
- [15] J. Draper et al., "Radiation Effects and Component Hardening Testing Program at the Oak Ridge Natikonal Laboratory," *Proceedings of ANS 5th Topical Meeting on Robotics and Remote Systems* (April 1994).
- [16] K. Obara et al., "Irradiation Tests of Critical Components for Remote Handling in Gamma Environment," JAERI-Tech 94-003 (Aug. 1994).
- [17] A. Ramsey, "D-T Radiation Effects on TFTR Diagnostics," PPPL-3010 (Oct. 1994).
- [18] ITER Design Manual, "Selection of RH Components and Materials to Minimize Radiation Damage," Vol. II, Manual 14, Chapter 7 (1995).
- [19] T. Burgess et al., "ORNL Experience in the Development and Testing of Rad Hard Remote Handling System Components," ITER presentation (Dec. 1994).
- [20] K. Shibnuma, "Remote Handling: Design Status and Critical Issues," ITER presentation (Feb. 1995).

7.5. Diverter Module Design

7.5.1. Overall Description

The diverter in ARIES-RS is of a double-null configuration with identical surfaces symmetrically distributed about the reactor midplane. There are 16 blanket/shield sectors in the reactor, each with a pair of diverter modules, one on top and one on the bottom. Each module consists of three plates: the inboard (IB) plate, the central (C) plate and the outboard (OB) plate. Figure 7.5-1 shows the diverters as they are located in the reactor and Figure 7.5-2 is a cross section of the plates with the radial and vertical dimensions of the start and end points of the plates indicated. Figure 7.5-1 shows the separation between the IB and C plates and between the C and OB plates. These slots are there to allow pumping of the neutrals, D_2 , T_2 , He and any other species used in the radiative mantle. The neutrals accumulating in the throats of the diverter slots enter the slots and are pumped out by the reactor vacuum system.

Figure 7.5-3 is a cross section of the diverter plates showing the internal geometry. There are three thickness zones: the front zone consists of 2 mm thick W tiles and is the surface which faces the plasma and experiences ion impacts; the second zone is 8 mm thick V and contains the coolant channels which are nominally rectangular, 4 mm $\times$ 8 mm; the last zone is also V of 40 mm thickness. The last zone acts as the structural component for the plates lending them rigidity and stability. Coolant manifolds are situated toroidally on the ends of each plate in the back, out of reach and sight of the plasma.

Figure 7.5-4 shows all three diverter plates rolled out flat next to each other plotted on the same scale, with the toroidal width on the vertical axis and the distance along the plate on the horizontal axis. The supply and return manifolds are indicated as is the coolant flow direction. The inboard diverter slot is situated between the supply manifold on the inboard plate and the supply manifold of the central plate. Similarly, the outboard diverter slot is situated between the return manifold of the central plate and the supply manifold of the outboard plate. It is interesting to note that in all three cases, the toroidal width of the plates increases in the same direction as the coolant flow. To maintain a constant flow velocity in the channels while still providing the coolant coverage on the plates, the aspect ratio of the channels will be changed, holding the cross-sectional area constant. Table 7.5-I gives the geometric parameters of the diverter plates.

The plates will be supported on the structural ring which surrounds the reactor core and constitutes part of the reflector (see Section 4.2). These supports will be made

Side View of the ARIES-RS Plasma Chamber

Figure 7.5-1.

Diverter Configuration Showing Inboard, Central and Outboard Diverter Plates

Figure 7.5-2.

Perspective View of a Segment of the Diverter Plate

Figure 7.5-3.

All Three Diverter Plates Rolled Out Flat

Figure 7.5-4.

Table 7.5-I.

DIVERTER PLATES GEOMETRIC PARAMETERS

	Inboard	Central	Outboard
Front surface area (m ²)	1.23	2.85	2.03
Top to bottom distance (m)	0.80	1.12	1.13
Small toroidal dimension (m)	1.43	1.46	1.79
Large toroidal dimension (m)	1.55	1.76	1.90
Increase in channel width (%)	8.5	21	5.9
Number of channels in plate	147	150	183
Mass of plate (kg)	405	609	669
Avg. increase in toroidal dimension (cm) from 300 K to 873 K	0.93	1.06	1.14

adjustable such that the plates can be properly aligned within the reactor sector. When the sectors are assembled into the plasma core, they are aligned in the reactor using the sector alignment mechanism described in Section 4.2.2.

7.5.2. Fabrication of the Diverter Modules

The diverter plates are fabricated from the vanadium alloy V-4Cr-4Ti and have 2 mm thick W castellations bonded to the surface facing the plasma. Two methods have been proposed for fabricating the plates, both of which depend on the ability to perform diffusion bonding of V alloys. Although there is no reason to doubt that V alloys can be diffusion bonded, there has been no experience to date in this area. Vanadium is a refractory metal possessing high temperature capability, and for this reason diffusion bonding would require higher temperatures than is commonly needed for diffusion bonding of steel alloys and other common metals.

Figure 7.5-5 shows the first proposed method for fabricating the diverter plates. A 7 mm thick V alloy plate is cut and bent to the required shape. The shaping is compounded by the fact that all the plates have both toroidal and poloidal curvature. A computer controlled mill is then used to machine the 4 mm deep channels. This technology is in hand and can be easily implemented. Next, a 1 mm thick sheet is cut to size and

One Possible Method for Fabricating the Diverter Plates

Figure 7.5-5.

shape, and then diffusion bonded to provide the closure for the channels. Now comes the most difficult part of the process, and that is the attachment of the 40 mm thick backing plate. Cutting, bending and fitting up such a plate in preparation for diffusion bonding it to the coolant assembly will be quite challenging. The next step is to weld on the manifolds. Finally the last step in the fabrication is to apply the W castellations. Here again, there is no experience in attaching W to V alloys. Brazing might be an option using high temperature brazing materials. One such example is V-30% Cb-5% Ta which melts at 1815°C but has a remelt temperature of 2300°C. Figures 7.5-6a and 7.5-6b are perspective views of the assembly steps for the outboard plate showing the compound curvatures that are required.

An alternate method for fabricating the plates is shown in Figure 7.5-7, requiring only one diffusion bonding step. In this method a cast or forged plate block is used which has already been radiographed for imperfections. The block is machined to the required size and shape, after which the channels are machined into it. The closure for the channels is then diffusion bonded to the block. The remaining steps will be the same as in the previous assembly procedure.

7.5.3. Support and Alignment of Diverter Plates

The diverter plates are supported on the structural ring which subtends the reactor plasma core and makes up part of the reflector. This structural ring shown in Figure 7.5-8 is the common element which holds together the inboard and outboard replaceable blanket/shield components. The upper extremity of Figure 7.5-8 shows the diverter plates supported on bolts fixed to the structural ring. These bolts as shown in Figure 7.5-9 are made of V alloy, are hollow and are threaded with opposing threads on either end. Each bolt will have a hexagonal hole on the end which attaches to the structural ring. The plates are aligned by turning the bolts with a hex wrench from the outside of the structural ring. The number, location and orientation of the bolts depends on the total force on the plate from the worst vertical disruption event (VDE), and on the requirements for alignment. The mass of the plates (405-670 kg, see Table 7.5-I) is small compared to the disruption loads and thus the disposition of the bolts will depend on the magnitude and direction of the disruption forces.

Table 7.5-II gives the peak disruption forces on the center and outboard diverter plates as reported in Section ???. In this section we will focus on the outboard plate because it is the largest of the three and has the highest peak loads. The table gives the peak force due to a 10 ms plasma quench with no plasma movement and a 2500 ms plasma

Perspective View of the Assembly Steps for the Outboard Diverter Plate

Figure 7.5-6.

An Alternate Method for Fabricating the Diverter Plates

Figure 7.5-7.

Cross Section of the In-Vessel Components Showing the Structural Ring

Figure 7.5-8.

Adjustable Diverter Bolt Shown Connecting the Diverter Plate to the Structural Ring

Figure 7.5-9.

Table 7.5-II.

PEAK DISRUPTION FORCES ON DIVERTER PLATES

Center Divertor Plate

Case	Max. Stress	Peak r Force	Peak z Force
10 ms quench	5.1 MPa	-.26 MN	.28 MN
100 ms quench with drift	1.5 MPa	-.080 MN	.084 MN
2500 ms drift then 10 ms quench	10 MPa	-0.44 MN	0.59 MN

Outboard Divertor Plate

Case	Max. Stress	Peak r Force	Peak z Force
10 ms quench	9.5 MPa	-.57 MN	.32 MN
100 ms quench with drift			
2500 ms drift then 10 ms quench	7.5 MPa	-0.98 MN	-1.8 MN

drift with a 10 ms quench. The latter case gives the highest peak loads which for the outboard plate are -0.98 MN in the radial direction and -1.8 MN in the vertical direction. A negative sign means the force is inward, or towards the plasma. Since the plate is predominantly vertical, we will first address the horizontal force which tends to push the plate towards the axis of the tokamak, putting the bolts in tension. This pressure per outboard plate is 0.032 MPa. To determine the type of loading on the plate we have to calculate the natural frequency, which for a rectangular plate simply supported along all edges is given by [1]:

$$f = \frac{K_n}{2\pi} \sqrt{\frac{Dg}{wa^4}}$$

where D is the plate flexural rigidity, w is the loading pressure including the mass of the plate, a the plate short dimension and K_n is given as [1]:

$$K_n = \pi^2 \left[m_a^2 + \left(\frac{a}{b} \right)^2 m_b^2 \right]$$

where m_a and m_b are vibration modes in the a and b directions (b is the plate long dimension). The flexural rigidity of the plate $D = \frac{Et^3}{12(1-\nu^2)}$ for V-4Cr-4Ti at 600°C is 1.2 MNm. Using $m_a = m_b = 1$ for $a/b = 0.6$ gives $K_n = 13.4$, and a $w = 0.032$ MN/m² we obtain $f = 54$ Hz. This frequency has a period of 18.5 ms. The rise time constant for the load is 2–3 ms [see Section 3.4] with a similar decay time constant. Assuming a triangular load pulse with a $td/T = 4/18.5 - 6/18.5 = 0.22 - 0.32$ where t_d is the load pulse and T is the natural period of the plate. The dynamic load factor (DLF) for such a system is between 0.6 and 1.0 [2] of the static load condition. To be on the conservative side we use a DLF of 1.0.

The bolt shown in Figure 7.5-9 has an OD of 2.5 cm and an ID = 2.0 cm with a cross-sectional area of 1.77 cm². The resultant force vector composed of the vertical force (1.8 MN) and the horizontal force (0.98 MN) is 2.05 MN or 0.128 MN per module for the outboard plate and is oriented at an angle of $\sim 29^\circ$ to the vertical. For a DLF of 1.0 the loading we use is the static load of 0.128 MN which requires 7.3 bolts at a stress of 100 MPa. Eight bolts spaced at 62 cm along the long dimension and 56.5 cm along the short dimension, oriented at $\sim 29^\circ$ to the vertical will be required to support the outboard plate module against the worst vertical disruption event. The estimated nuclear heating in the bolts will be dissipated by radiation to the cooled support ring.

The central plate has a surface area smaller than the outboard plate and the peak forces are smaller by a factor of 2–3. Supporting it would be simpler than the outboard plate.

The bolts are used to align the plates within the blanket/shield sector. Figure 7.5-10 shows how a bolt is attached to the structural ring while passing through coolant passages. This is performed at the factory during the assembly of the sector. When the sector is inserted into the reactor torus, proper alignment of the whole sector is performed using the sector hydraulic alignment mechanism described in Section 4.2.2.

7.5.4. Summary of Diverter Module Design

The diverter modules can be summarized by the following:

- The diverter plates are made of the V alloy V-4Cr-4Ti, are 4.8 cm thick including 2 mm W castellations on the surface facing the plasma. They have rectangular coolant channels running in the direction of the short dimension of the plate.

A Method Shown for a Diverter Bolt Penetrating Coolant Channels in the Structural Ring

Figure 7.5-10.

- Fabrication of the plates depends on the ability to diffusion bond large complex V to V plates and on the ability of applying W castellations on the surfaces facing the plasma.
- Support of the plates is dominated by the electromagnetic forces due to vertical disruption events (VDE). The outboard plate will require 8 bolts spaced at ~ 60 cm and oriented at 29° to the vertical to react the dynamic loading from a VDE.
- The plates are aligned within the reactor sectors by turning the support bolts. Subsequent alignment of the sectors in the reactor torus is performed with the hydraulic sector alignment system.

REFERENCES

- [1] Raymond J. Roark and Warren C. Young, Formulas for Stress and Strain, Fifth Edition McGraw-Hill Book Co., 1975, pp. 579.
- [2] John M. Biggs, Introduction to Structural Dynamics, McGraw-Hill Book Co., pp. 47.

7.9. Particle Exhaust and Vacuum System Design

7.9.1. General Description

The ARIES-RS reactor has a fusion power of 2167 MW which means that 7.6953×10^{20} #/s of T and D are burned. Given the burn fraction of 0.304, with no recycling, dictates that there are the following ion fluxes in the exhaust system:

$$\begin{aligned} T &= 1.7618 \times 10^{21} \text{ \#/s} \\ D &= 1.7618 \times 10^{21} \text{ \#/s} \\ He &= 7.6953 \times 10^{20} \text{ \#/s} \\ Ne &= 1.36 \times 10^{20} \text{ \#/s} \end{aligned}$$

When neutralized and recombined the particle fluxes become:

$$\begin{aligned} T_2 &= 8.809 \times 10^{20} \text{ molecules/s} \\ D_2 &= 8.809 \times 10^{20} \text{ molecules/s} \\ He &= 7.6953 \times 10^{20} \text{ molecules/s} \\ Ne &= 1.36 \times 10^{20} \text{ molecules/s} \end{aligned}$$

Helium exhaust and transport studies on TEXTOR-94 [1] have shown that a pumping rate amplification of a factor of 5 is needed to maintain the He ash concentration at the desired level. The ITER vacuum system has also adopted this technique. A factor of 5 increment means that the pumping system must be capable of evacuating 4.405×10^{21} molecules/s for each D_2 and T_2 , 3.848×10^{21} molecules/s of He and 6.8×10^{20} molecules/s of Ne. Using the average diverter plate surface temperature of 650°C (923 K) we obtain the following throughputs:

$$\begin{aligned} \dot{Q}_{T_2} &= (4.405 \times 10^{21}) (1.03 \times 10^{-22}) (923 \text{ K}) = 418.8 \text{ tl/s} \\ \dot{Q}_{D_2} &= \phantom{(4.405 \times 10^{21}) (1.03 \times 10^{-22}) (923 \text{ K})} = 418.8 \text{ tl/s} \\ \dot{Q}_{He} &= (3.848 \times 10^{21}) (1.03 \times 10^{-22}) (923 \text{ K}) = 365.8 \text{ tl/s} \\ \dot{Q}_{Ne} &= (6.8 \times 10^{20}) (1.03 \times 10^{-22}) (923 \text{ K}) = 64.6 \text{ tl/s} \\ \dot{Q}_{TOTAL} &= 1268 \text{ tl/s} \end{aligned}$$

Based on an overall pressure of 1 Pa in the diverter throat we now obtain the partial pressures of each species. For T_2 and D_2 it is 5×10^{-3} ton, for He it is 2.19×10^{-3} ton and for Ne it is 3.9×10^{-4} ton.

These neutral particles which collect in the diverter throat are pumped through the diverter slots provided on the inboard (IB) and the outboard (OB) sides of the diverter plates. Figure 7.9-1 shows an IB slot located at $R = 3.89$ m and an OB slot located at $R = 4.74$ m. Similar slots are located on the bottom divertor. The neutrals, after passing through the slots, collect in a plenum located behind the central diverter plate. The plenum is toroidally continuous and has exhaust ducts located in each blanket/shield sector, 16 on the top and 16 on the bottom. Since the pumping station is located in the reactor basement area, the neutrals which are exhausted above the top diverter must be ducted to the bottom, which is an additional impedance over that of the lower diverter. To compensate for this, the lower diverter duct impedance will be made higher than its complementary upper diverter duct. In this way, the total impedance for both upper and lower diverters will be equal. Finally, the gases must be directed to the pumping station through vacuum vessel ducts. The sections that follow discuss the gas flow regimes and calculations are made of the conductances of the various vacuum system components.

7.9.2. Determination of Flow Regimes

The first step in designing a vacuum system is determination of the flow regime, whether it is molecular or viscous. To do this, the mean free path for T_2 , D_2 He and Ne, at the pressure in the diverter throat of 1 Pa and a temperature consistent with the diverter plates has to be determined. If the mean free path, defined as the distance a particle travels before it collides with another particle is less than a critical dimension of the confining channels then the flow is viscous. Conversely, if the mean free path is larger than the channel critical dimensions, then the flow is molecular. The mean free path is given by [2]:

$$\lambda = \frac{kT}{\pi P \delta^2 \sqrt{2}}$$

where λ is the mean free path in cm, k the Boltzmann constant (1.03×10^{-22} torr-liters/K), T the temperature in K, P the pressure in torr, and δ the gas kinetic diameter in cm. For $\delta = 2.7 \times 10^{-8}$ cm for D_2 and T_2 , and 2.20×10^{-8} for He [2] at $P = 7.6 \times 10^{-3}$ torr and a temperature of 923 K, the mean free paths are:

$$\begin{aligned} D_2, T_2 &= 3.86 \text{ cm} \\ \text{He} &= 5.82 \text{ cm} \end{aligned}$$

Similarly the mean free path for Ne is 4.0 cm.

Side View of the Chamber Showing the Location of the Divertor Slots

Figure 7.9-1.

The slots between the diverter plates are on the order of 4 cm wide and thus the flow through them is in a transition range between molecular and viscous. On the other hand the subsequent ducts leading to the pumping station will have dimensions much larger than the mean free paths of the neutrals. The flow through them will be viscous.

7.9.3. Conductances of Vacuum System

The pumping speed of the cryopumps is usually determined by two considerations: the need for evacuating the neutral particles at a rate consistent with the desired pressure in the diverter throat and the time desired between cryopump regeneration. Typically, one hour of pumping is desired before switching over to another set of pumps in order to regenerate the loaded set. Taking the throughput of 1268 tl/s and correcting it to a temperature of 450 C, we obtain a throughput of 993 tl/s, and the accumulation of 1 hour is 3.57×10^6 tl/h. A loading up of 10 tl/cm² is considered optimum for cryo-condensation panels, which means that a surface area of 3.57×10^5 cm² will be needed in the cryopumps. The advertised pumping capability for compound cryopumps is 10 l/s.cm² for D₂, T₂ and 1.0 l/s.cm² for He, giving the system a pumping speed of 3.57×10^6 l/s for T₂, D₂ and 3.57×10^5 l/s for He. The Ne throughput is very low (~ 5%) and since it has a boiling point of 27.2 K at one atmosphere, it would get pumped very easily with cryopumps. The boiling points of D₂ and T₂ are 23.6 K and 25.1 K respectively. Helium is pumped by cryo-trapping, which means that the He atoms are immobilized and trapped on the panels by the condensing hydrogen species.

It is now possible to determine the required effective speed of the vacuum system for each species by dividing the throughput by the base pressure of the species in the diverter throat. For D₂, T₂ it is

$$\frac{837.6 \text{ tl/s}}{5 \times 10^{-3} \text{ t}} = 1.675 \times 10^5 \text{ l/s}$$

and for He it is

$$\frac{365.8 \text{ tl/s}}{2.19 \times 10^{-3} \text{ t}} = 1.675 \times 10^5 \text{ l/s}$$

Knowing what the effective speed needs to be and the speed of the pump, we can now determine the required conductances for the cumulative effect of the diverter slots, the diverter ducts and the vacuum vessels ducts.

It has been mentioned earlier in this section that the neutral particles will be pumped out from both upper and lower diverters. Those that come out at the upper diverter

will be ducted to the lower portion of the vacuum vessel. This additional impedance in the flow of neutrals from the upper diverter has to be factored into the effective pumping speed to avoid preferentially pumping the lower diverter neutrals at a higher rate than those of the upper diverter. These impedances will be determined in the following sections.

7.9.3.1. Conductance of the Diverter Slots

To be on the conservative side we will calculate the conductance of the diverter slots in both flow regimes. The conductance of an orifice in the molecular flow regime is [2]:

$$C_{\text{orif. mol.}} = \left(\frac{kT}{2\pi m} \right)^{\frac{1}{2}} A$$

where k is the Boltzmann constant, T the temperature in K, m the mass of the gas molecule and A the area of the slot. This translates to:

$$C_{\text{orif. mol.}} = 3.64 \left(\frac{T}{M} \right)^{\frac{1}{2}} A \text{ liters/s}$$

where M is the gas molecular weight and A the area in cm^2 .

The diverter slots are situated at radii of 3.89 m and 4.74 m for the inner and outer slots respectively. If the slots are 4 cm wide, the total area for the upper and lower slots is $2.169 \times 10^4 \text{ cm}^2$. The conductance are: T_2

$$C_{T_2} = 3.64 \left(\frac{923}{6} \right)^{\frac{1}{2}} (4.338 \times 10^4) = 1.98 \times 10^6 \text{ l/s}$$

and for D_2 and He,

$$C_{D_2, \text{He}} = 3.64 \left(\frac{923}{4} \right)^{\frac{1}{2}} (4.338 \times 10^4) = 2.4 \times 10^6 \text{ l/s}$$

The conductance of an orifice in the viscous flow regime is [2]:

$$C_{\text{orif. vis.}} = \left[\frac{2\gamma kT}{m(\gamma - 1)} \right]^{\frac{1}{2}} \left(\frac{P_2}{P_1} \right)^{\frac{1}{\gamma}} \left[1 - \frac{P_2}{P_1} \right]^{\frac{1}{\gamma}} A$$

where γ is the ratio of specific heats $\frac{C_p}{C_v}$ for the gas, k the Boltzmann constant, T the temperature in K, m the mass of the gas molecule, P_1 and P_2 the pressures on either side

of the orifice and A the area of the orifice. For D_2 and T_2 , $\gamma = 1.405$, $m = 6.7 \times 10^{-24}$ g and 10.05×10^{24} g respectively, and $A = 4.338 \times 10^4$ cm². By setting $C_{orif. vis.} = 2 \times 10^6$ l/s we can calculate $\frac{P_2}{P_1}$, which for T_2 is 0.9. This means that a pressure of 6.84×10^{-3} torr downstream from the orifice will give a conductance of 10^6 l/s for T_2 gas in the viscous flow regime. Likewise for D_2 and He, $\frac{P_2}{P_1} = 0.93$ requiring a downstream pressure of 7.07×10^{-3} torr.

It has been shown that diverter slot widths of 4 cm will provide a conductance on the order of 2×10^6 l/s for both molecular and viscous flow regimes. To determine the overall conductance of the vacuum system we next calculate conductances for the diverter ducts and the vacuum vessel ducts.

7.9.3.2. Conductance of the Diverter and Transfer Ducts

Having determined the required effective speed of pumping, the conductances of the diverter slots and the pumping speed of the pumps, it is possible to determine the needed conductances of the remaining vacuum system. Assuming that the throughput will be equally divided between the upper and lower diverters, we will calculate the conductances needed from the upper and lower streams separately. The effective pumping speed is given by:

$$\frac{1}{S_{\text{effect.}}} = \frac{1}{S_{\text{cryopump}}} + \frac{1}{C_{\text{div. slot}}} + \frac{1}{C_{\text{system}}} \left(\frac{s}{l} \right)$$

where $S_{\text{effect.}}$ is the effective pumping speed, S_{cryopump} is the speed of the cryopumps, $C_{\text{div. slot}}$ is the conductance of the diverter slots and C_{system} is the conductance of the remaining system, all in l/s. For the upper side, the remaining system is composed of the conductances of the upper diverter duct, the top to bottom transfer duct and the vacuum vessel duct. For the lower side, it is the diverter duct and the vacuum vessel duct. Assuming that one half of the effective speed and pump speed is applied to each of the diverters, we calculate C_{system} , which will be the same for both sides. Setting $S_{\text{eff.}} = \frac{1.675 \times 10^5}{2}$ l/s, for the pump $S_{\text{cryopump}} = \frac{3.57 \times 10^6}{2}$ l/s, and for the diverter slot $\frac{2 \times 10^6}{2}$ l/s. we solve for $C_{\text{syst.}}$ which is equal to 9.634×10^4 l/s.

If we assume for the present $C_{\text{syst.}}$ is composed of three equal conductances for the upper side and two equal conductances for the lower side we get:

$$\text{Upper diverter: } C_{\text{div. duct}} = C_{\text{transf. duct}} = C_{\text{vac. vessel}} = 2.89 \times 10^5 \text{ l/s}$$

Making the vacuum vessel ducts uniformly equal, leaves only the diverter duct to be determined for the lower diverter:

Lower diverter: $C_{\text{div. duct}} = C_{\text{vac. vessel}} = 2.89 \times 10^5 \text{ l/s}$

There are 16 diverter ducts at the top and 16 on the bottom, and there are 16 transfer ducts to move neutrals from the region behind the upper diverter shield to the region behind the lower diverter shield.

The conductance of a duct in the viscous flow regime is [2]:

$$C_{\text{duct. visc.}} = \frac{\pi}{128} \frac{D^4}{\eta L} \bar{p} \text{ (l/s)}$$

where D is the diameter in cm, η the viscosity in poise, L the length in cm and $\bar{p}$, the average pressure in torr. The viscosity for H_2 as a function of temperature is given in Figure 7.9-2.

Using $C_{\text{duct. visc.}}$ for the upper system of $2.89 \times 10^5 \text{ l/s}$ divided among 16 ducts gives a required conductance/duct of $1.806 \times 10^4 \text{ l/s}$. The viscosity of H_2 at 823 K is 176×10^{-6} poise, $L = 180 \text{ cm}$ and arbitrarily picking $\bar{p}$ of $6.9 \times 10^{-3} \text{ torr}$, we can solve for D which turns out to be 42.9 cm. The equivalent area is 1445.5 cm^2 which gives a rectangular duct of $20 \text{ cm} \times 72.3 \text{ cm}$. So that such a duct would not degrade the strength of the structural ring, it will be oriented with the long dimension in the radial direction. The pressure above the upper diverter shield will be $6.73 \times 10^{-3} \text{ torr}$.

If we select a pressure below the lower diverter shield of $6.4 \times 10^{-3} \text{ torr}$, we can now calculate the dimensions of the top to bottom transfer duct. Here again the required conductance/duct is $1.806 \times 10^4 \text{ l/s}$, the viscosity is the same, the distance $L = 800 \text{ cm}$ and $\bar{p}$ is $6.57 \times 10^{-3} \text{ torr}$. The transfer duct diameter is 63 cm, giving an equivalent area of $3117 \text{ cm}^2/\text{duct}$. This transfer duct will be contained in a triangular wedge formed by the vacuum vessel at the confluence of adjacent blanket/shield modules. The rule of thumb for triangular ducts is that the area must be increased by 50% over a circular duct as long as $\frac{h}{b} < 2.5$ [3]. Using these criteria, we get a triangular duct with a base of 61 cm and a height of 153 cm. Such a duct will fit very well in the designated area.

The lower diverter duct is exhausting to a pressure of $6.4 \times 10^{-3} \text{ torr}$ and thus $\bar{p} = 6.62 \times 10^{-3} \text{ torr}$. Otherwise using the same parameters as the upper duct we obtain a diameter of 36.4 cm. The equivalent rectangular duct would be $20 \text{ cm} \times 74 \text{ cm}$.

These diverter ducts start from the plenums behind the central diverter plates, penetrating through the structural ring and then through the shield, exiting into the space between the shield and the vacuum vessel wall. The neutrals from the upper space travel through the transition ducts to join the neutrals in the lower space. They are then evacuated by the vacuum system located in the basement of the reactor.

Viscosity of Hydrogen as a Function of Temperature

Figure 7.9-2.

7.9.3.3. Conductance of the Vacuum Vessel Duct

There are 16 vacuum vessel ducts leading from the lower space between the shield and the vacuum space, to the cryopump station. The conductance of each of these ducts needs to be $\frac{2.8 \times 10^5}{16} = 1.806 \times 10^4$ l/s. Using the viscosity for H_2 at 723 K of 162×10^{-6} poise, a duct length of 800 cm and a $\bar{p}$ of 6.2×10^{-3} torr, the diameter of the duct is 62.6 cm.

7.9.3.4. Overall System Effective Pumping Speed

As shown in Section 7.9.3 it is now possible to calculate the effective pumping speed of the entire system.

$$S_{\text{cryopump}} = 3.57 \times 10^6 \text{ l/s}$$

$$C_{\text{div. slots}} = 2 \times 10^6 \text{ l/s}$$

$$C_{\text{div. ducts}} = 5.78 \times 10^5 \text{ l/s}$$

$$C_{\text{vac. vessel}} = 2.89 \times 10^5 \text{ l/s}$$

The effective pumping speed is

$$\frac{1}{S_{\text{effect.}}} = \frac{1}{3.57 \times 10^6} + \frac{1}{2 \times 10^6} + \frac{1}{5.78 \times 10^5} + \frac{1}{2.89 \times 10^5}$$

$$S_{\text{effect.}} = 1.675 \times 10^5 \text{ l/s}$$

The pressure in the diverter throat is

$$\frac{\text{throughput (tl/s)}}{S_{\text{effect.}} \text{ (l/s)}}$$

$$P_{\text{div. throat}} = \frac{1268 \text{ (tl/s)}}{1.675 \times 10^5 \text{ (l/s)}} = 7.57 \times 10^{-3} \text{ torr.}$$

which is the desired pressure.

7.9.4. Cryogenic and Power Requirements of the Pumps

The surface area of each set of cryopumps is 35.7 m². The estimated liquid He consumption for such a pump is 265 l/h made up of cooling the neutral particle from 77 K to 20 K, condensing the D₂ and T₂ then freezing them, radiation losses to a 77 K chevron and conduction losses through the supports. Similarly the liquid N₂ consumption is estimated at 130 l/h made up of cooling the neutrals to 77 K, radiation from the hot surroundings and conduction through the supports. The power requirement is ~ 0.7 kW at 4.2 K and 6 kW at 77 K. These add up to 270 kW of power at room temperature.

7.9.5. Summary and Conclusions

The vacuum system for the ARIES-RS power plant is relatively conventional. The primary pumps used are compound cryopumps. A dilution factor of 0.2 is used in the He pumping rate based on experiments on Textor [1] and adopted for ITER. This means that the pumping speed is 5X higher than needed to evacuate the particles. Even with this enhanced pumping rate, the total active cryopump surface area is ~ 36 m².

The system is flexible to accommodate a lower burnup fraction which would increase the DT exhaust rate and decrease the He exhaust rate. This can be accomplished by minor increases in the cryopump surface area and increases in the conductances of the various ducts, giving a higher effective pumping speed.

Table 7.9-I summarizes the parameters of the particle exhaust system.

Table 7.9-I.

PARAMETERS OF THE PARTICLE EXHAUST SYSTEM

D ion flux in exhaust	$1.7618 \times 10^{21} \text{ \#/s}$
T ion flux in exhaust	$1.7618 \times 10^{21} \text{ \#/s}$
He ion flux in exhaust	$7.6953 \times 10^{20} \text{ \#/s}$
Ne ion flux in exhaust	$1.360 \times 10^{20} \text{ \#/s}$
D ₂ throughput in exhaust	4.18.8 tl/s
T ₂ throughput in exhaust	4.18.8 tl/s
He throughput in exhaust	365.8 tl/s
Ne throughput in exhaust	64.6 tl/s
Total pressure in diverter throat	$7.6 \times 10^{-3} \text{ torr}$
D ₂ T ₂ partial pressure	$5.0 \times 10^{-3} \text{ torr}$
He partial pressure	$2.19 \times 10^{-3} \text{ torr}$
Ne partial pressure	$3.90 \times 10^{-4} \text{ torr}$
Conductance of diverter slots	$2 \times 10^6 \text{ l/s}$
Conductance of diverter ducts	$5.78 \times 10^5 \text{ l/s}$
Conductance of transition ducts	$2.89 \times 10^5 \text{ l/s}$
Conductance of vacuum vessel ducts	$2.89 \times 10^5 \text{ l/s}$
Cryopump pumping speed	$3.57 \times 10^6 \text{ l/s}$
Effective pumping speed	$1.675 \times 10^5 \text{ l/s}$
Cryopump surface area	35.7 m ²
Time between cryopump regeneration	1 h
L He consumption of cryopumps	265 l/h
L N ₂ consumption of cryopumps	130 l/h
Total power consumption of cryogenerators	270 kW

REFERENCES

- [1] A. P. Zwicker et al., "Helium Exhaust and Transport Studies in the TEXTOR-94 Tokamak," Oak Ridge National Laboratory, 3rd International IEA Workshop on Helium Transport and Exhaust, Charleston, SC (Sept. 26, 1995).
- [2] Gerhard S. Lewin, "Fundamentals of Vacuum Science and Technology," McGraw-Hill Book Co.
- [3] Guthrie and Wakerling, "Vacuum Equipment and Techniques," McGraw Hill Book Co., New York (1949).

10.2. Radiological Criteria

10.2.1. Introduction

The goals and objectives developed by the ARIES team for U.S. fusion power plants state that in order to demonstrate that fusion is ready for commercialization it must be a safe, environmentally attractive, and licensable power source at an economically superior total cost. Those goals and objectives led to a set of requirements on materials and technology choices and design specifications. Among these requirements, fusion plants should generate only low level waste no greater than Class C and all waste should be recycled and/or disposed at an acceptable level. The purpose of this section is to discuss the limits for Class C waste and the impact that impurities can have on achieving these limits.

10.2.2. Waste Disposal Limits

It is likely that U.S. fusion plants would be licensed and regulated by the Nuclear Regulatory Commission (NRC) and other environmental agencies. Thus far, the NRC guidelines for Class C waste, currently documented in the 10CFR61 report [2], were basically developed for nuclear waste generated by medical and industrial facilities, and fission power plants. Therefore, NRC regulations do not contain certain materials that are of interest to fusion, such as V, W, Si, Sn, . . . etc. NRC eventually will develop a complete list of radwaste limits specifically for fusion plants.

Over the past several years, Fetter [3] used and modified the NRC methodology to develop waste disposal limits for a wide range of radionuclides which are of interest to fusion researchers. He also used different conversion factors and transfer coefficients and limited the dose to the whole body rather than to a single organ, as in NRC. In addition, he used his work to reevaluate the isotopes that already have NRC limits and, in some cases, arrived at different limits than those of NRC. The Fetter's limits have not been endorsed by NRC and are not in the form of regulations. Therefore, they can not be considered as official limits for U.S. fusion waste.

The development of waste disposal limits for the U.S. radwaste is beyond the scope of this study. Due to uncertainties in the present regulations and the lack of an official and reliable set of waste disposal limits, the ARIES team conservatively required all in-vessel components to meet both NRC and Fetter's limits for Class C waste until NRC develops official guidelines for fusion plants.

10.2.3. Limits on Alloy and Impurity Contents

Careful attention to alloying choices and impurity levels is essential to achieve activation levels that allow fusion to meet its goals for waste disposal and safety. Even tiny levels of certain impurities can dramatically affect the radiological characteristics of the system. At this time, there are several remaining issues which need to be resolved in order to obtain materials which are sufficiently well characterized and optimized to meet the criteria for attractive power plants. Choices made in ARIES-RS for the reference analyses were based on existing information, with attempts to incorporate new improvements as they become available.

Low activation materials are defined and judged based on:

1. U.S. criteria for waste disposal, including NRC limits and Fetter's predictions.
2. Limits related to safety, including elements that contribute to volatile releases during accidents and accident severity through decay heat.
3. Limits related to recycling or maintenance, usually set as a radiation exposure level for workers.

The information defining low activation materials is an essential factor in selecting constituents for alloying elements and in specifying limits for impurities. This information is useful to guide the materials development but none of it should be taken as final as these criteria are not in the form of regulations. Furthermore, evolution of these rules to the application of fusion systems in 2050 or beyond can only be predicted in the most general sense, with no degree of certainty. The development of low activation materials, as with many fusion power technologies, must be approached from both "roll-forward" and "roll-back" directions. In other words, progress is achieved from the basis of existing materials and technologies, but with an eye towards improvements that are necessary to allow an attractive end-product. Power plant studies play an essential role by identifying areas in which low activation materials need to be improved. This information then can be used by the materials community to devise ways to achieve those alloy and impurity limits.

Typically, alloying elements are a concern for accident safety, whereas impurities are a concern for waste disposal. For example, alloying with W and Mn is limited by accident considerations for materials subjected to a high radiation environment. On the other hand, the concentrations of Nb, Mo, Ag, Co, N, Ni and other impurities have been

restricted based on waste disposal criteria. Most of the remaining issues in this section relate to impurities.

While the needed limits on impurities could be achieved with today's technology, a few of the candidate materials have been produced with specification limits to control the impurity contents of radiological concern. There are two obvious routes to control the impurities: the selection of the source of ores and the cleanup of processing equipments. For example, with these two steps the Nb content in vanadium alloys can be reduced from the common 4-40 ppm range to the 0.5 ppm range.

In some cases, the available information on impurity content for low activation steels of interest appears inaccurate and/or out-of-date. When the data were developed, there has probably been little interest in establishing analysis for some impurities at very low levels as activation analyses may have indicated no safety concerns for these impurities. For instance, the impurities for modified HT-9 and Tenelon steels listed in the BCSS report [4] were prepared for different purposes. However, the impurities for both steels are identical (refer to Table 10.2-I). It seems likely that actual measurements were not performed for Tenelon and the impurity contents were simply taken as that of MHT-9. Some impurities were certainly below the detection limits for the techniques used 10-20 years ago. A dedicated effort is needed to establish reference materials and to accurately characterize them using modern techniques.

A waste disposal issue for iridium has emerged recently when the new international FENDL-1 activation library [5] was introduced to the ARIES-RS safety analyses. The old ACTL [6] library used in previous ARIES designs did not contain activation cross sections for Ir. However, more recent information has indicated that the forthcoming FENDL-2 library predicts lower cross sections for Ir meaning that Ir at less than 1 ppm should not present a waste disposal problem for steel-based power plants. This highlights the need to maintain close and ongoing attention to the evolution of nuclear data libraries. It appears likely that Ir was below the 2 ppm detection limit for the less sensitive techniques used to develop data on impurity content for MHT-9 and Tenelon steels. The actual level of Ir could have been well below this value as more recent analyses show $\text{Ir} < 0.02$ ppm for other low activation steels [7]. This further supports the need for more accurate impurity characterization.

During the course of the ARIES-RS design study, new information has become available to improve the impurity content values of the vanadium and steel alloys used in the fusion power core. Klueh et al. [7] have recently conducted several measurements to obtain concentrations of important impurity elements for reduced-activation ferritic steel and vanadium alloys. Due to the near completion stage of the ARIES-RS study, it would

Table 10.2-I.

**COMPOSITION OF ALLOYING AND IMPURITY
ELEMENTS (in wt.%) FOR LOW ACTIVATION
STEELS AND VANADIUM ALLOYS**

Alloy	MHT-9	Tenelon	IEA Mod F82H	V4Cr4Ti (ORNL)	V4Cr4Ti (ANL)
B	*10	*10	*3	*5	—
C	0.15	0.15	0.1	*80	*50.0
N	*10	*50	*50	*85	0.01
O	*70	*70	*50	—	0.03
Mg	—	—	—	—	—
Al	*80	*80	0.01	*< 10	0.02
Si	0.2	0.2	0.1	0.074	0.04
P	0.013	0.013	*50	*< 30	*4
S	*40	*40	*20	*< 10	*3
Cl	—	—	—	—	*1
K	*3	*3	—	—	*0.1
Ti	0.1	*30	*50	3.9	4
V	0.3	*20	0.2	balance	91.89
Cr	11	15	8	3.8	4
Mn	0.53	15	0.1	—	—
Fe	balance	balance	balance	—	*40
Co	*50	*50	*16-28	*0.2-4.2	—
Ni	*60	*60	*474	*11	*3
Cu	*30	*30	0.01	*< 50	*2
As	—	—	*20	—	*2
Zr	*10	*10	—	—	*1
Nb	*1.1	*1.1	*2.4-3.3	*32-64	*0.5

*wppm

Table 10.2-I (Cont.)

Alloy	MHT-9	Tenelon	IEA Mod F82H	V4Cr4Ti (ORNL)	V4Cr4Ti (ANL)
Mo	*2.7	*2.7	*19-21	*329-370	*4
Pd	—	—	* < 0.05	*0.2	—
Ag	*0.9	*0.9	*0.05-0.1	*0.6-0.86	*0.01
Cd	*1	*1	*0.05-0.4	*0.3-1.6	—
Sn	*30	*30	*10	—	—
Sb	*5	*5	*5	—	—
Ba	*2	*2	—	—	—
Ta	*4	*4	0.04	—	*0.2
W	2.5	0.01	2	—	*1
Os	—	—	* < 0.05	* < 0.05	—
Ir	*2	*2	*0.02-0.05	*0.05-0.5	—
Pb	*5	*5	—	—	—
Bi	*2	*2	*0.05-0.2	*0.05-0.46	—
Eu	—	—	*0.02-0.05	*0.05-0.2	—
Tb	*2	*2	* < 0.02	*0.05-0.2	—
Dy	—	—	* < 0.05	* < 0.05	—
Ho	—	—	* < 0.05	—	—
Er	—	—	* < 0.05	*0.1	—
U	—	—	*0.003-0.05	*0.13-0.21	—

*wppm

have been difficult to replace the MHT-9 and Tenelon of the present design by other steels and complete the redesign with the remaining time of the study. Nevertheless, the recommended steel (IEA modified F82H) should be considered in future designs.

Compared to MHT-9 and Tenelon, the Japanese steel IAE Mod F82H [7, 8] exhibits lower levels of Ir, Ag, Co, Cd, Bi, and Tb impurities and higher Nb and Mo impurities, as indicated in Table 10.2-I. Special purification techniques will be used in the future to reduce the Nb and Mo in the IEA Mod F82H steel to acceptable levels. The results of the IEA Mod F82H steel should be representative of what can be achieved with reasonable care for steels prepared to reduce the content of problem impurities. Of interest is that the Ag and Ir impurities can be controlled to extremely low levels of 0.1 and 0.02 ppm, respectively. Therefore, these low levels of Ag and Ir impurities were used for the MHT-9 and Tenelon steels in place of the BCSS information to perform the ARIES-RS waste disposal analysis.

Klueh also examined a V4Cr4Ti alloy purchased on a "best-effort" basis from Tele-dyne Wah Chang, Albany, Oregon. The impurity results are listed in Table 10.2-I along with the latest recommendation for the trace element specification for the V4Cr4Ti alloy provided by ANL [9, 10] and used in the ARIES-RS analysis. The ANL composition is based on results from analyses of the V4Cr4Ti and similar VCrTi alloys [10]. The composition is considered attainable without significant impact on processing cost. Klueh's measurements show much higher impurity levels for Nb, Ag, Mo, and Ir. Precautions should be taken to restrict these impurities in order to qualify the V alloys for Class C low level waste in ARIES-like designs.

REFERENCES

- [1]
- [2] "Licensing Requirements for Land Disposal of Radioactive Waste," Nuclear Regulatory Commission, 10CFR part 61, *Federal Register*, **FR 47** (1982) 57446.
- [3] S. Fetter, E. T. Cheng and F. M. Mann, "Long Term Radioactive Waste from Fusion Reactors: Part II," *Fusion Eng. Design*, **13**, (1990) 239.
- [4] D. Smith et al., "Blanket Comparison and Selection Study," Argonne National Laboratory report ANL/FPP/TM-122 (1984).
- [5] A. Pashchenko, H. Wienke, S. Ganesan, and P. McLaughlin, "FENDL/E-1.0, Evaluated Nuclear Data Library of Neutron Nuclear Interaction Cross Sections and Photon Production Cross Sections and Photon-Atom Interaction Cross Sections for Fusion Applications," Report IAEA-NDS-128 Rev. 2, International Atomic Energy Agency (Nov. 1995).
- [6] M. A. Gardner and R. J. Howerton, "ACTL: Evaluated Neutron Activation Cross Section Library — Evaluation Techniques and Reaction Index," Lawrence Livermore National Laboratory report UCRL-50400, Vol. 18 (1978).
- [7] R. Klueh, Oak Ridge National Laboratory, private communication (Sept. 1996), and R. L. Klueh and M. L. Grossbeck, "Impurity Content of Reduced-Activation Ferritic Steels and Vanadium Alloy," to be published in *J. of Nuclear Materials*.
- [8] R. L. Klueh, *Proceedings of the IEA Working Group Meeting on Ferritic/Martensitic Steels*, Oak Ridge National Laboratory Report ORNL/M-3777 (1994).
- [9] H. Attaya, "Summary Report for ITER Task-D4: Activation Calculations for the Lithium Vanadium ITER Design," Argonne National Laboratory Report ANL/FPP/TM-282 (Feb. 1995).
- [10] D. Smith, Argonne National Laboratory, private communication (June 1996).

10.3. ACTIVATION AND WASTE DISPOSAL

The safety characteristics of fusion reactors are dependent on the choice of their structural material. The level of radioactivity induced in any reactor structure depends on the constituent elements of the irradiated material, level of neutron flux and time of irradiation. Low activation materials (LAMs) are an attractive choice because they result in lower levels of off-site doses in the case of accidental release of their radioactive inventories during an accident. LAMs can also facilitate better waste management at the end of the reactor life.

Detailed activation analysis was performed to identify the safety, environmental and radwaste characteristics of the ARIES-RS reactor. The reactor uses one of the promising low activation alloys as structural material. The reactor structure is made of vanadium alloy and cooled with liquid lithium. The activity, decay heat and biological hazard potential (BHP) were calculated as a function following shutdown. Such an evaluation of the structure activity and biological hazard potential is needed to evaluate the potential impact of a radioactive inventory release at the onset of an accident. Results of the decay heat calculation are used to examine the thermal response of the reactor structure following a loss of coolant accident (LOCA). The waste disposal ratings (WDR) of the different reactor components at the end of their lifetimes were also evaluated. The waste disposal rating is needed to determine if a given structure component would satisfy the regulatory criteria for shallow land burial as low level waste (LLW).

10.3.1. Calculational Procedure

The neutron fluxes used for the activation calculations were generated by the one-dimensional discrete ordinates neutron transport code ONEDANT [?]. A 46-group neutron and 21-group gamma coupled cross section library containing P_3 legendre expansions of the scattering cross sections based on the FENDL1.0 [?] basic data files was used in the transport calculations. Two sets of calculations were performed for the reactor structure as well as for the divertor structure. The reactor structure calculations used toroidal cylindrical geometry models with the inboard and outboard sides modeled simultaneously. The average neutron wall loadings on the inboard and the outboard sides are 2.9 and 4.5 MW/m², respectively. On the other hand the average neutron wall loading on the divertor surface is 1 MW/m². The activation calculations were conducted using the DKR-ICF computer code [?] with activation cross sections taken from the FENDL1.0 library [?]. The DKR-ICF cross section library includes data for 36 neutron transmutation reactions as well as 8 decay reaction types.

The ARIES-RS reactor was assumed to operate continuously for 40 full power years (FPY). The first wall and blanket modules were assumed to be replaced every 2.5 FPY. The replaceable shield and reflector were assumed to survive for 7.5 years. The permanent shield and vacuum vessel were assumed to stay in place for the duration of the reactor life-time. Similarly, the divertor plate would be replaced every 2.5 FPY. The divertor supporting structure has a life-time of a 7.5 FPY. The divertor permanent shield was also assumed to survive for a 40 FPY. The calculations were performed with the assumption of continuous operation rather than pulsed operation. Although assuming continuous operation does not affect the calculated activities for radionuclides with half-lives much less than the shortest period of continuous operation or much greater than the reactor lifetime, the radioactive inventory of radionuclides with intermediate half-lives is overestimated by the inverse of the reactor availability [?].

The structure activation results were utilized in the radwaste classification performed using the WDR [?] computer code. In addition, the structure activation results were used in the off-site dose calculations presented in this chapter. The materials used in the different regions of the reactor and divertor are presented in Table ?? and Table ??, respectively. The elemental compositions of the vanadium alloy (V-4Cr-4Ti) used in this analysis were provided by Argonne National Laboratory [?]. The compositions of the low activation austenitic steel (Tenelon) and the low activation ferritic steel (modified HT-9) were taken from the Blanket Comparison and Selection Study (BCSS) report [?].

10.3.2. Activity, Decay Heat and Biological Hazard Potential (BHP)

At shutdown, the first wall and blanket, reflector, and shield specific activities on the inboard side are 16.4, 42.3, and 7.6 MCi/m³, respectively. On the outboard side, the first wall and blanket, reflector, and shield specific activities are 16.6, 10.7, and 3.9 MCi/m³, respectively.

Figure ?? shows the total activity induced in the different regions of the reactor as a function of time following shutdown. The amount of radioactivity generated in the inboard and outboard sides of the first wall and blanket at shutdown are almost equal. On the other hand, the inboard region of the reflector produces about four times the activity induced at shutdown as the outboard region. The activity induced in the inboard side of the permanent shield is twice the amount induced in the outboard side.

The first wall and blanket and replaceable shield short-term activities are dominated by ⁴⁸Sc ($T_{1/2} = 43.7$ hr), ⁵¹Cr ($T_{1/2} = 27.7$ day), ⁴⁷Sc ($T_{1/2} = 3.349$ day), and ⁴⁵Ca ($T_{1/2}$

Table 10.3-I.
STRUCTURAL MATERIALS USED IN THE REACTOR ANALYSIS

Inboard	
Vacuum Vessel	35% Tenelon, 60% WC (.95)
LT WC-Shield	80% WC (.95), 15% Tenelon
HT Shield	15% V-4Cr-4Ti, 80% Tenelon
RS Shield	90% V-4Cr-4Ti
Blanket	10% V-4Cr-4Ti
First Wall	100% V-4Cr-4Ti
Outboard	
First Wall	100% V-4Cr-4Ti
Blanket	10% V-4Cr-4Ti
Reflector	15% V-4Cr-4Ti, 75% Tenelon
HT Shield	15% V-4Cr-4Ti, 80% Tenelon
LT Shield	80% Borated Tenelon, 15% Tenelon
Vacuum Vessel	70% Borated Tenelon, 25% Tenelon

Figure 10.3-1. Specific Activities induced in the Different Regions of ARIES-RS.

Table 10.3-II.
STRUCTURAL MATERIALS USED IN THE DIVERTOR ANALYSIS

Divertor Plate	90% V-4Cr-4Ti, 4%W
Support Structure	15% V-4Cr-4Ti, 60% Modified HT-9
HT Shield	15% V-4Cr-4Ti, 80% Tenelon
LT Shield	15% Tenelon, 80% Borated Tenelon
Vacuum Vessel	35% Tenelon, 60% Borated Tenelon

= 162.7 day). On the other hand, the reflector and permanent shield short-term activities after shutdown (≤ 1 day) are dominated by ^{51}Cr , ^{54}Mn ($T_{1/2} = 312.2$ day), ^{56}Mn ($T_{1/2} = 2.578$ hr), and ^{187}W ($T_{1/2} = 23.9$ hr).

In the period between 1 day and 1 year after shutdown, ^{54}Mn and ^{60}Co ($T_{1/2} = 5.27$ yr) dominate the activity induced in the reflector and permanent shield. During the same period of time, the first wall and blanket activity is dominated by ^{49}V ($T_{1/2} = 337$ day), ^{45}Ca , and ^{46}Sc ($T_{1/2} = 83.81$ day). Finally, the long-term activities induced in both the reflector and shield come from the steel components and are dominated by ^{14}C ($T_{1/2} = 5730$ yr), $^{93\text{m}}\text{Nb}$ ($T_{1/2} = 16.1$ yr), ^{94}Nb ($T_{1/2} = 2 \times 10^4$ yr), and ^{93}Mo ($T_{1/2} = 3.5 \times 10^3$ yr).

The temporal variation of the decay heat generated in the different regions of the reactor are shown in Figure ???. The decay heat generated in the different regions of ARIES-RS are dominated by essentially the same isotopes that dominate the level of activity in the reactor. The short-term decay heat generated in the first wall and blanket and replaceable shield are due to ^{48}Sc and ^{52}V ($T_{1/2} = 3.76$ min). ^{46}Sc and ^{49}V are the dominant nuclides up to one year following the first wall and blanket and replaceable shield replacement or the reactor shutdown. ^{94}Nb and ^{14}C dominate the decay heat generated in the first wall and blanket and replaceable shield several hundred years following the end of their lifetimes. In the permanent shield case, ^{56}Mn and ^{52}V produce most of the decay heat generated within the first 8 hours. Within the first year after shutdown, ^{56}Mn and ^{60}Co are the major sources of decay heat. The long-term decay heat is governed by the decay of ^{94}Nb and $^{108\text{m}}\text{Ag}$ ($T_{1/2} = 130$ yr).

Figure 10.3-2. Decay Heat induced in the Different Regions of ARIES-RS.

Figure ?? shows the total integrated decay heat generated in the different regions of the first wall and blanket and shield, respectively. These results are useful for predicting the thermal response of the blanket and shield to a LOCA.

The biological hazard potentials were calculated using the maximum permissible concentration limits in air for the different isotopes according to the NRC regulations specified in 10CFR20 [?]. Figure ?? shows the biological hazard potentials in air as a function of time following shutdown for the different regions of ARIES-RS. The short-term BHP is dominated by ^{49}V and ^{48}Sc in the case of the first wall and blanket, and ^{54}Mn , ^{56}Mn , and ^{52}V in the case of the reflector and permanent shield. While ^{49}V is responsible for most of the BHP in the first wall and blanket for times (≤ 10 years), ^{60}Co and ^{54}Mn are the major sources of mid-term BHP generated in the reflector and permanent shield. Finally, in addition to ^{94}Nb , the long-term BHP is produced by ^{93}Mo and ^{108m}Ag in case of the first wall and blanket, and reflector and permanent shield, respectively.

10.3.3. Radwaste Classification

The radwaste of the different regions of the reactor were evaluated according to both the NRC 10CFR61 [?] and Fetter [?] waste disposal concentration limits (WDL). The 10CFR61 regulations assume that the waste disposal site will remain under administrative control for 100 years. The dose at the site to an inadvertent intruder at the end of the 100 year period is limited to less than 500 mrem/year. The waste disposal rating (WDR) is defined as the sum of the ratio of the concentration of a particular isotope to the maximum allowed concentration of that isotope taken over all isotopes and for the particular class. If the calculated $\text{WDR} \leq 1$ when Class A limits are used, the radwaste should qualify for Class A segregated waste. The major hazard of this class of waste is to the individuals responsible for handling it. However, such waste is not considered to be hazardous following the loss of institutional control over the disposal site.

If the WDR is > 1 when Class A WDL are used, but ≤ 1 when Class C limits are used, the waste is termed Class C intruder waste. It must be packaged and buried such that it will not pose a hazard to an inadvertent intruder after the 100 year institutional period is over. Class C waste is assumed to be stable for 500 years. Using Class C limits, a $\text{WDR} > 1$ implies that the radwaste does not qualify for shallow land burial.

Fetter developed a modified version of the NRC's intruder model to calculate waste disposal limits for a wider range of long-lived radionuclides which are of interest for fusion researchers than the few that currently exist in the current 10CFR61 regulations. Fetter's

Figure 10.3-3. Integrated Decay Heat in the Different Regions of ARIES-RS.

Figure 10.3-4. Biological Hazard Potential in the Different Regions of ARIES-RS.

Table 10.3-III.
FIRST WALL AND BLANKET WASTE DISPOSAL RATINGS

WDR	INBOARD	OUTBOARD
Class A (10CFR61)	0.2 (2) ^{94}Nb , ^{14}C	0.3 (1.77) ^{94}Nb , ^{14}C
Class C (10CFR61)	0.02 (0.2) ^{94}Nb , ^{14}C	0.032 (0.19) ^{94}Nb , ^{14}C
Class C (Fetter)	0.016 (0.16) ^{94}Nb , ^{26}Al	0.029 (0.17) ^{94}Nb , ^{26}Al

model included more recent transfer coefficients and dose conversion factors. However, while the NRC model limits the whole body dose to 500 mrem or the dose to any single organ (one of seven body organs) to 1.5 rem, Fetter limits are based on the maximum dose to the whole body only.

10.3.3.1. Analysis of First Wall and Blanket, Reflector, and Shield

Specific activities calculated by the DKR-ICF code were used to calculate the waste disposal ratings. The waste disposal ratings for Class A and Class C low level waste are shown in Tables ??, ??, and ??. The results in the tables are given for both classes with the activities averaged over the total volume of the inboard and outboard sides of the first wall and blanket, replaceable shield and reflector, and permanent shield and vacuum vessel. The values in the tables are given for both non-compacted and compacted (in parentheses) values. Non-compacted values are based on averaging the specific activities over the total volume of a particular region assuming that internal voids will be filled with concrete before disposal. On the other hand, compacted values correspond to crushing the solid waste before disposal. The 10CFR61 Class A WDR is given after a waiting period of

Table 10.3-IV.
WDR OF THE REPLACEABLE SHIELD AND REFLECTOR

WDR	INBOARD	OUTBOARD	OUTBOARD (Reduced Ir and Ag)
Class A (10CFR61)	3.6 (4) ^{94}Nb , ^{14}C	1.43 (6.43) ^{94}Nb , ^{60}Co	1.43 (6.43) ^{94}Nb , ^{60}Co
Class C (10CFR61)	0.356 (0.396) ^{94}Nb , ^{14}C	0.083 (0.373) ^{94}Nb , ^{14}C	0.083 (0.373) ^{94}Nb , ^{14}C
Class C (Fetter)	0.276 (0.307) ^{94}Nb , ^{26}Al	0.87 (3.92) $^{192\text{m}}\text{Ir}$, $^{108\text{m}}\text{Ag}$	0.12 (0.54) ^{94}Nb , $^{108\text{m}}\text{Ag}$

about 10 years to allow for the specific activity of short-lived nuclides ($T_{1/2} \leq 5$ years) to drop below 7000 Ci/m^3 . The 7000 Ci/m^3 limit is 10 times larger than the limit specified by the NRC for Class A disposal of short-lived nuclides where the waste form is not specified. In comparison with other isotopes for which limits are given for different waste forms, the factor of 10 is used for isotopes contained in metal waste. Since the NRC regulations do not specify any limit for short-lived activity for Class C LLW, the Class C WDR values were calculated after 1 year cooling period for both 10CFR61 and Fetter limits.

As shown in Table ??, after 2.5 FPY, the first wall and blanket would qualify for disposal as Class C low level waste according to both NRC and Fetter limits. ^{94}Nb is the major contributor to WDR for both limits. ^{14}C and ^{26}Al are the second major contributors to the waste disposal rating for the NRC and Fetter limits, respectively.

Results in Table ?? show that the WDR of the replaceable shield and reflector is mostly dominated by the same nuclides as in the case of the first wall and blanket. After 7.5 FPY, the outboard reflector would not qualify for disposal as Class C LLW according to Fetter limits if the waste was compacted.

Table 10.3-V.
WDR OF THE PERMANENT SHIELD AND VACUUM VESSEL

WDR	INBOARD	OUTBOARD	INBOARD (Reduced Ir and Ag)	OUTBOARD
Class A (10CFR61)	3.525 (3.7) ^{94}Nb , ^{60}Co	3.39 (3.56) ^{94}Nb , ^{60}Co	3.525 (3.7) ^{94}Nb , ^{60}Co	3.39 (3.56) ^{94}Nb , ^{60}Co
Class C (10CFR61)	0.238 (0.25) ^{94}Nb , ^{14}C	0.25 (0.26) ^{94}Nb , ^{14}C	0.238 (0.25) ^{94}Nb , ^{14}C	0.25 (0.26) ^{94}Nb , ^{14}C
Class C (Fetter)	0.977 (1.03) $^{192\text{m}}\text{Ir}$, ^{94}Nb	1.1 (1.155) $^{192\text{m}}\text{Ir}$, $^{108\text{m}}\text{Ag}$	0.25 (0.26) ^{94}Nb , $^{108\text{m}}\text{Ag}$	0.27 (0.28) ^{94}Nb , $^{108\text{m}}\text{Ag}$

After 40 FPY, the permanent shield and vacuum vessel would only qualify for disposal as Class C LLW according to NRC limits. In the mean time, the waste would not qualify for disposal as Class C LLW according to Fetter limits if the waste was compacted. As shown in Table ??, $^{192\text{m}}\text{Ir}$ is the major contributor the WDR if Fetter limits were used. A recent low activation material study [?] indicated that other low activation steels contain much smaller concentration of Ir and Ag as impurities. The new concentration of Ir and Ag are two orders and one order of magnitude lower, respectively.

Replacing the Tenelon used in ARIES-RS shield with any of the reduced-activation alloys mentioned in the study would certainly result in allowing the shield to qualify for disposal as Class C LLW according to Fetter limits. As shown in Tables ?? and ??, reducing the Ir and Ag impurities included in the Tenelon and modified HT-9 alloys used in this analysis to 0.02 and 0.1 wppm, respectively, would result in allowing all reactor components to qualify for Class C LLW.

Table 10.3-VI.
DIVERTOR WASTE DISPOSAL RATING

WDR	Class A (10CFR61)	Class C (10CFR61)	Class C (Fetter)	Class C (Fetter) (Reduced Ir and Ag)
Divertor Plate	0.9 (0.96) ⁹⁴ Nb, ¹⁴ C	0.97 (0.103) ⁹⁴ Nb, ¹⁴ C	0.23 (0.25) ^{186m} Re, ⁹⁴ Nb	0.23 (0.25) ^{186m} Re, ⁹⁴ Nb
Divertor Structure	3.36 (4.48) ⁹⁴ Nb, ¹⁴ C	0.235 (0.31) ⁹⁴ Nb, ¹⁴ C	3.19 (4.25) ^{192m} Ir, ^{108m} Ag	0.43 (0.57) ⁹⁴ Nb, ^{108m} Ag
HT-Shield	3.48 (3.66) ⁹⁴ Nb, ⁶⁰ Co	0.3 (0.31) ⁹⁴ Nb, ¹⁴ C	3.69 (3.88) ^{192m} Ir, ^{108m} Ag	0.36 (0.38) ⁹⁴ Nb, ^{108m} Ag
LT-Shield	0.064 (0.067) ⁹⁴ Nb, ⁶⁰ Co	0.006 (0.0063) ⁹⁴ Nb, ¹⁴ C	0.111 (0.116) ^{192m} Ir, ^{108m} Ag	0.008 (0.009) ⁹⁴ Nb, ^{108m} Ag
Vacuum Vessel	4.8×10^{-4} (5.1×10^{-4}) ⁹⁴ Nb, ⁶⁰ Co	4.48×10^{-5} (4.72×10^{-5}) ⁹⁴ Nb, ¹⁴ C	7.02×10^{-4} (7.39×10^{-4}) ^{192m} Ir, ^{108m} Ag	5.9×10^{-5} 6.3×10^{-5} ⁹⁴ Nb, ^{108m} Ag

10.3.3.2. Divertor Analysis

The waste disposal ratings of the different regions of the divertor are shown in Tables ?? for Class A and Class C low level wastes. As shown in the table, after 2.5 FPY, the divertor plate would easily qualify for disposal as low level waste according to both NRC and Fetter limits. ^{94}Nb and ^{14}C are the major contributors to WDR according to the NRC limits. On the other hand, $^{186\text{m}}\text{Re}$ is the major contributor to the WDR according to Fetter limits. $^{186\text{m}}\text{Re}$ is produced in the tungsten component of the divertor plate .

After 7.5 FPY, the support structure could only qualify for disposal as LLW according to NRC limits. Similar to the divertor plate, ^{94}Nb and ^{14}C are the major contributors to WDR if NRC limits were used. The waste disposal rating according to Fetter limits is dominated by contributions from $^{192\text{m}}\text{Ir}$ and $^{108\text{m}}\text{Ag}$.

After 40 FPY, the divertor's high temperature shield would only qualify for disposal as Class C LLW according to NRC limits. On the other hand, both of the low temperature shield and vacuum vessel would meet both limits for disposal as LLW. As shown in the table, $^{192\text{m}}\text{Ir}$ is the major contributor the WDR according to Fetter limits. As mentioned before, replacing the Tenelon used in the HT-shield with a different reduced-activation steel alloy would allow the HT-shield to qualify for disposal as Class C LLW according to Fetter limits. As shown in the table, all of the divertor components would qualify for disposal as Class C LLW according to Fetter limits of the lower level if Ir and Ag impurities are used in the analysis.

REFERENCES

- [1] R. O'Dell et al., "User's Manual for ONEDANT: A Code Package for One-Dimensional, Diffusion-Accelerated, Neutral Particle Transport," Los Alamos National Laboratory report, LA-9184-M (1982).
- [2] A. Pashchenko et al., "FENDL/E-1.0, Evaluated Nuclear Data Library of Neutron Nuclear Reaction Cross Sections and Photon Production Cross Sections and Photon-Atom Interaction for Fusion Applications," Report IAEA-NDS-128 Rev. 2, International Atomic Energy Agency (Nov. 1995).
- [3] D. L. Henderson and O. Yasar, "DKR-ICF: A Radioactivity and Dose Rate Calculation Code Package," University of Wisconsin Fusion Technology Institute report, UWFD-714, Vol. 1 (April 1987).
- [4] S. E. Spangler, J. E. Sisolak and D. L. Henderson, "Calculational Models for the Treatment of Pulsed/Intermittent Activation within Fusion Energy Devices," *Fusion Eng. Design*, **22**, (1993) 349.
- [5] H. Y. Khater, "WDR: A Code for Radwaste Classification," unpublished.
- [6] A. Attaya, "Summary Report for ITER Task-D4: Activation Calculations for the Lithium Vanadium ITER Design," Argonne National Laboratory Report ANL/FPP/TM-282 (Feb. 1995).
- [7] D. Smith et al., "Blanket Comparison and Selection Study," Argonne National Laboratory report, ANL/FPP/TM-122 (1984).
- [8] "Standards for Protection Against Radiation," Nuclear Regulatory Commission, 10CFR part 20 (1975).
- [9] "Licensing Requirements for Land Disposal of Radioactive Waste," Nuclear Regulatory Commission, 10CFR part 61, *Federal Register*, **FR 47** (1982) 57446.
- [10] S. Fetter, E. T. Cheng and F. M. Mann, "Long Term Radioactive Waste from Fusion Reactors: Part II," *Fusion Eng. Design*, **13**, (1990) 239.
- [11] R. L. Klueh and M. L. Grossbeck, "Impurity Content of Reduced-Activation Ferritic Steels and a Vanadium Alloy," to be published in *Journal of Nuclear Materials*.

10.4. LOSS OF COOLANT ACCIDENT ANALYSIS

10.4.1. Introduction

This section investigates the consequences of a loss of coolant accident (LOCA) in the rare event that a coolant tube ruptures and the coolant is drained from the reactor. It is assumed that the plasma is quenched simultaneously with the onset of a LOCA. Even though the neutron heating is no longer there, the lack of coolant in the coolant channels causes the temperatures to rise in the various sections of the blanket and shield due to afterheat.

The thermal response of the blanket and shield following a LOCA is determined by a two-dimensional finite element model. Several cases are investigated using different shielding materials (Tenelon and Vanadium) with different afterheat characteristics. Several alternate boundary conditions are also considered, primarily having to do with surface emissivities as well as location and availabilities of heat sinks. The goal of this investigation is to determine the temperature history of the different components as a function of time, and ultimately, the highest temperature reached. This will aid in determining whether the first wall or any other component material will be damaged as a result of this temperature, and will have to be replaced before reactor operations can be resumed.

10.4.2. Methodology

10.4.2.1. Accident Conditions

In order to perform the ARIES-RS LOCA analysis, a number of assumptions have to be made. These assumptions are:

1. All of the coolant is drained from all the blanket/shield sectors leaving the coolant channels empty and making it possible for coolant channel surfaces to radiate to each other.
2. The plasma is quenched instantaneously upon the onset of a LOCA. An appropriate shutdown mechanism must be in place for this to happen. If it takes 10 seconds for the plasma to be shutdown, the heat generated amounts to $\sim 1\%$ of the integrated afterheat in the first week after LOCA. Even though this does not appear to be very much, the immediate effect on the first wall could be significant.

3. The TF coils on the inboard (IB) side of the plasma chamber are well insulated and are not available as a heat sink.
4. All parallel surfaces radiate to each other.
5. Both inboard and outboard (OB) blanket/shields are solved interactively and can, therefore, radiate to each other across the plasma space.
6. There is no heat transfer by conduction between the different zones. Within each zone, ten percent of the facing areas is assumed to have connecting structure for internal support and can, therefore, also conduct heat.
7. Thermal emissivity is taken as 0.5 for external surfaces and 0.8 for internal surfaces. The higher value for the internal surfaces is due to the effect of Li coolant on the channel internal coating.
8. The heat sink is located where the vacuum vessel is connected to the reactor support structure and to the access ports, and is assumed fixed at 100°C.
9. Temperature dependent thermo-physical properties are used for all the materials.
10. The material properties of the different zones in the radial build have been adjusted (linearly proportional to volumetric ratio) to preserve the density, heat capacitance and thermal conductivity of the actual composition.

10.4.2.2. Modeling the Inboard and Outboard Blanket/Shield

A 2-D model in cylindrical coordinates is employed for finite element analysis at the reactor midplane. A cylindrical coordinate system (axisymmetric) is used because it preserves the polar effect as the radius changes. The commercial finite element code ANSYS 5.2 [1] is used in this analysis. Tables 10.4-I and 10.4-II give the radial build and the material compositions, respectively for the IB and OB sides. Note that Table 10.4-I gives the radial composition under the TF coils as well as between them. The finite element model used in the analysis is shown in Figure 10.4-1 with the two sides appearing next to each other. In the actual case, the first walls for the IB and OB sides would be facing each other. Each separate zone is shown connected with a sidewall, which is the only thermally conducting member between the various materials in that zone.

Table 10.4-I.
**RADIAL BUILD IN CM FOR THE INBOARD/OUTBOARD SIDES
 OF THE BLANKET/SHIELD USED IN LOCA ANALYSIS**

Radial Build	Inboard		Outboard	
FW	0.3		0.3	
Blanket-1	20		20	
Gap	1		1	
Blanket-2	—		30	
RS/Reflector	20		7	
Assembly gap	1		1	
	Under Coils		Between Coils	
	Inboard	Outboard	Inboard	Outboard
Permanent shield	56	70	90	82
HT shield	26	28	28	35
Gap	2	2	2	2
LT shield	28	40	60	45
Assembly gap	2	2*	—	2*
Vacuum vessel	20	30	—	20
Total FW/B/R/S/VV/gaps	120	161	149	130
VV management gap	5*	5*	5*	5*

*Minimum gap width

Table 10.4-II.
THE MATERIAL COMPOSITIONS FOR THE
REGIONS LISTED IN TABLE 10.4.I

Inboard	
First wall	100% V structure
Blanket	10% V structure + 90% Li
Replaceable shield	15% V structure + 10% Li + 75% V filler
Permanent shield	
HT shield	15% V structure + 76% Tenelon filler + 4% W structure + 5% Li
LT shield	15% Tenelon structure + 5% Li + 53% B ₄ C filler (90% d.f.) + 27% WC filler (95% d.f.)
Vacuum vessel	35% Tenelon structure + 5% He coolant + 40% B ₄ C filler + 20% WC filler
Outboard	
First wall	100% V structure
Blanket	10% V structure + 90% Li
Reflector	15% V structure + 10% Li + 75% Tenelon filler
Permanent shield	
HT shield	15% V structure + 5% Li + 73% Tenelon filler + 7% W structure
LT shield	15% Tenelon structure + 5% Li + 80% borated Tenelon filler
Vacuum vessel	25% Tenelon structure + 5% He coolant + 70% B-Tenelon filler

Figure 10.4.1 Finite element model for the inboard and the outboard of ARIES.RS.

10.4.3. Thermal Loads, Initial Conditions and Boundary Conditions

The only thermal loads considered in this analysis are those generated by afterheat following the onset of the LOCA. These afterheat loads are specific to each material, are dependent on the degree of activation and are variable with time. Two main cases have been studied. The first is the baseline case which is described by the radial build given in Table 10.4-I with the material composition shown in Table 10.4-II. The second is the same as the first case but with a change in the reflector material filler from Tenelon to vanadium. This change determines the effect of the reduced afterheat in vanadium relative to Tenelon. The lower maximum temperature using vanadium comes at an increase in cost of the material, ultimately reflected in the cost of electricity (COE). Various boundary and initial conditions are used to determine the best combination for maintaining the maximum first wall temperature below the recommended value of 1100°C.

The initial conditions for the two cases are as follows:

- The initial temperature of the first wall/blanket/reflector/HT shield is 600°C.
- The initial temperatures of the LT shield is 300°C.
- The initial temperature of the vacuum vessel is 150°C.

The boundary conditions are as follows:

- The sides of the model are reflecting boundaries, namely, they are adiabatic.
- The back of the vacuum vessel releases heat by radiation and by partial conduction through 10% of its facing area, to a sink at 100°C.

The afterheat variation in the OB and IB sides for the two cases considered is shown in Figure 10.4-2 and Figure 10.4-3.

10.4.4. Results and Discussion

Two sets of thermal emissivities have been used for the two options with different reflector material fillers. In the first set, inner surfaces had an emissivity of 0.8 and the outer surfaces, 0.5. In the second set, both inner and outer surfaces had emissivities of 0.8. Table 10.4-III lists the four cases with respect to material and emissivities, then gives maximum temperature value in C and the time at which it is reached in seconds. A maximum temperature of 1277°C occurs in Case #1 on the OB side, and is for the Tenelon option with inner surface and outer surface emissivities of 0.5 and 0.8 respectively. Its counterpart using vanadium is Case #3 where the temperature is 1176°C, or 101°C lower.

It has been suggested that it may be difficult to maintain a stable layer of any material on the outer surface of the first wall due to sputtering, and thus, it should not have an emissivity of 0.8. A run has been made using the conditions of Case #2 where all the surfaces have emissivities of 0.8, but substituting 0.5 for the outer surface of the first wall. The increase in the maximum temperature was only $\sim 4^\circ\text{C}$.

During the study it was decided to reduce the temperature in the low temperature (LT) shield to 150°C in order to alleviate excessive heating in the access doors. This

Figure 10.4.2 Specific afterheat in a 100% dense material (Tenelon in O/B reflector) (reference case).

figure 10.4.3 Specific afterheat in a 100% dense material (Vanadium in O/B reflector).

Table 10.4-III.

SUMMARY OF THE FOUR CASES OF LOCA ANALYSIS

Case #1: Tenelon in the OB reflector. The thermal emissivity of the outer surfaces = 0.5, and emissivity of inner surfaces = 0.8.

Case #2: Tenelon in the OB reflector. The thermal emissivity of the outer surfaces = 0.8, and emissivity of inner surfaces = 0.8.

Case #3 : Vanadium in the OB reflector. The thermal emissivity of the outer surfaces = 0.5, and emissivity of inner surfaces = 0.8.

Case # 4 : Vanadium in the OB reflector. The thermal emissivity of the outer surfaces = 0.8, and emissivity of inner surfaces = 0.8.

Location	Tenelon in the OB Reflector				Vanadium in the OB Reflector			
	Case #1		Case #2		Case #3		Case #4	
	Max. (°C)	Time (s)	Max. (°C)	Time (s)	Max. (°C)	Time (s)	Max. (°C)	Time (s)
<u>Inboard</u>								
First wall	1264.0	48150	1201.0	39150	1170.0	54150	1107.0	45150
Middle reflector	1162.0	60150	1100.0	51150	1093.0	60150	1034.0	51150
Back reflector	1121.0	60150	1056.0	54150	1060.0	60150	998.5	51150
Front HT shield	1086.0	60150	1036.0	54150	1027.0	60150	979.9	51150
Back HT shield	943.5	69150	879.5	63150	902.9	66150	841.8	57150
Front LT shield	854.9	78150	822.8	69150	813.3	75150	784.1	63150
Vacuum vessel	211.5	95930	221.2	84150	202.2	93410	212.8	81150
<u>Outboard</u>								
First wall	1277.0	45150	1209.0	36150	1176.0	54150	1111.0	45150
Front reflector	1229.0	33150	1172.0	21600	1067.0	54150	1001.0	45150
Back reflector	1191.0	33150	1130.0	21600	1035.0	54150	967.0	45150
Front HT shield	1120.0	36150	1077.0	21600	978.7	51150	928.0	45150
Back HT shield	931.4	60150	870.8	60150	838.0	60150	778.9	60150
Front LT shield	796.5	81150	796.2	72150	698.3	86770	700.7	75150
Vacuum vessel	414.4	14990	437.3	12040	364.7	16540	385.3	12940

necessitated the use of He gas in a separate cooling loop. Two cases have been analyzed; Case #5 with a starting temperature of the LT shield of 150°C and allowing it to heat up and Case #6, also with a starting temperature of 150°C and allowing it to stay at that level. In both cases the reference configuration and other boundary condition are the same as in Case #1. Table 10.4-IV summarizes the results of Cases #5 and #6.

The results show that the gains are small, 16°C in Case #5 and 37°C in Case #6, both values still above $T_{\max} = 1100^{\circ}\text{C}$. Table 10.4-V summarizes the results of using 150°C in the LT shield and holding it there, while enhancing emissivity to 0.8 everywhere (same as Case #2). This Case #7 produces a reduction in the OB side temperature of 35°C over Case #2, also a marginal improvement. None of the attempts tried so far have managed to achieve the goal of $< 1100^{\circ}\text{C}$. Figure 10.4-4 is a bar chart summarizing the maximum temperature in all seven cases. The lowest temperature of 1111°C is in Case #4, which has vanadium in the OB reflector and optimistic emissivities. Please refer to Table 10.4-III for the boundary condition in Cases #1 to #4. The boundary conditions for Cases #5 to #7 are given below:

Case#5 Case #1+ low temperature shields (LT shld) starts at a temperature of 150°C and heat up.

Case#6 Case #1+ low temperature shields (LT shld) starts and stays at a temperature of 150°C.

Case#7 Case #2+ low temperature shields (LT shld) starts and stays at a temperature of 150°C.

Materials degrade from both high temperature and from time at that temperature. For example it is known that V-4Cr-4Ti shows no grain growth at 1000°C up to 600 hours, but exposure to 1100°C for more than two hours can cause excessive grain growth [2]. Typical short term temperature vs. time plots for Cases #6 and #7 are shown in Figures 10.4-5 and 10.4-6, respectively. In Case #6 the OB first wall will be at a temperature $> 1200^{\circ}\text{C}$ for ~ 10.5 hours and $> 1100^{\circ}\text{C}$ for ~ 20 hours. In Case #7 the OB first wall will be at a temperature $> 1100^{\circ}\text{C}$ for about 10 hours. These temperatures and durations do not comply with the recommended values, however, Case #7 is better than #6. A longer term temperature-time history for all the zones in Case #7 is shown in Figures 10.4-7 and 10.4-8 for the IB and OB sides respectively. These figures show that both IB and OB first walls cool down to $< 500^{\circ}\text{C}$ in seven days with the remaining zones, well below that.

Table 10.4-IV.
SUMMARY RESULTS OF CASES #5 AND #6 OF LOCA ANALYSIS

Location	Case #5		Case #6	
	Max. (°C)	Time (s)	Max. (°C)	Time (s)
<u>Inboard</u>				
First wall	1248.0	0.4369E+05	1227.0	0.3080E+05
Middle reflector	1141.0	0.5869E+05	1088.0	0.3080E+05
Back reflector	1099.0	0.6169E+05	1032.0	0.3080E+05
Front HT shield	1062.0	0.6169E+05	977.6	0.2161E+05
Back HT shield	920.5	0.7069E+05	771.7	0.1510E+05
Front LT shield	830.0	0.8652E+05	150.0	0.000
Vacuum vessel	205.8	0.9893E+05	150.0	0.000
<u>Outboard</u>				
First wall	1262.0	0.4069E+05	1245.0	0.2780E+05
Front reflector	1217.0	0.2869E+05	1211.0	0.2160E+05
Back reflector	1178.0	0.2160E+05	1171.0	0.1985E+05
Front HT shield	1102.0	0.2569E+05	1090.0	0.2160E+05
Back HT shield	899.0	0.5869E+05	831.6	0.1985E+05
Front LT shield	751.3	0.8652E+05	150.0	0.000
Front vacuum vessel	390.8	0.1649E+06	150.0	0.000

Table 10.4-V.
SUMMARY RESULTS OF CASE #7 OF LOCA ANALYSIS

Location	Max. (°C)	Time (s)
<u>Inboard</u>		
First wall	1162.0	21600
Middle reflector	1016.0	22700
Back reflector	954.0	21600
Front HTshield	920.9	19880
Back HT shield	656.0	12150
Front LT shield	150.0	0.000
Vacuum vessel	150.0	0.000
<u>Outboard</u>		
First wall	1174.0	21600
Front reflector	1146.0	19880
Back reflector	1100.0	18150
Front HT shield	1036.0	18150
Back HT shield	708.9	18150
Front LT shield	150.0	0.000
Front vacuum vessel	150.0	0.000

Figure 10.4.4 Temperature comparison between all seven cases investigated.

Figure 10.4.5 Temperature variation of the first wall and the front of the hot temperature shield for case # 6.

Figure 10.4.6 Temperature Variation of the First Wall and the Front of the Hot Temperature Shield for the final case.

Figure 10.4.7. A general temperature variation history during the first week after shutdown of the inboard.

Figure 10.4.8. A general temperature variation history during the first week after shutdown of the outboard.

10.4.5. Summary and Recommendations

All the attempts to limit the first wall temperature during a LOCA to 1100°C for only several hours have failed, although some have come very close (see Case #4). It should be mentioned that these analyses have been made assuming that coolant is lost in all the sectors simultaneously. This is rather severe, since the survival of one coolant loop would provide a heat sink at the first wall, where the sectors with the LOCA loop would radiate to sectors which are still cooled, reducing the maximum temperature by a lot.

One of the primary reasons for this difficulty is the number of gaps between the zones in both IB and OB radial build necessitated by the desire to separate the zones and extend their lifetimes. Eliminating conduction between the zones severely limits heat transfer across the radial build to the heat sinks in the back.

The attempts made to reduce the maximum temperature during LOCA are:

- Replace the filler material in the OB reflector (Tenelon) by vanadium.
- Use optimistic values of emissivity.
- Reduce the LT shield temperature from 300°C to 150°C .

In the very best case (Case #4) which has a vanadium reflector and an emissivity of 0.8 everywhere, the maximum first wall temperature is 1111°C , and if the emissivity of the first wall is reduced to 0.5, it only rises to 1115°C . This case seems realistic and comes within 15°C of satisfying the requirement of 1100°C .

It has also been suggested [3], that a passive closed Li coolant loop can be incorporated into the back of the first wall segment connected to the back of the LT shield. This loop will be inactive during operation due to MHD effects. However, during LOCA with the magnetic field turned off, it will operate by natural convection with a temperature difference of only 50°C . Such a loop can limit the first wall temperature to $\sim 800^{\circ}\text{C}$. Although this is an interesting idea and should be explored further, it is not an entirely fail safe system since this loop could also rupture and itself experience a LOCA.

REFERENCES

- [1] ANSYS 5.0 User's Manual, Updo, DN-R300:50-2 (1992).
- [2] Mike Billone, private communications (Sept. 9, 1996).
- [3] Siegfried Malang, private communications (1996).