Design and analysis of the ARIES-ACT1 fusion core


University of California San Diego
9500 Gilman Drive
La Jolla, CA 92093-0417
mtillack@ucsd.edu

Abstract

ARIES-ACT1 is the latest in a series of tokamak power plant designs that capitalize on the high temperature capabilities and attractive safety and environmental characteristics of SiC composites coupled with a self-cooled PbLi breeder. This combination offers both design simplicity and high performance, capable of operating at very high coolant outlet temperature in a moderately high power density device. Blankets are supported within a poloidally continuous He-cooled steel structural ring, which adds robustness and minimizes loads on the SiC modules. In order to withstand high local surface heat flux in the divertor (of the order of 14 MW/m² time-averaged), a He-cooled tungsten-alloy divertor was adopted. About 25% of the total “high-grade” heat is thus removed by helium, to be combined with the blanket heat in order to feed the power cycle. In addition to the in-vessel power-producing elements of the design, this article also summarizes the key features and analysis of the vacuum vessel and power conversion system.

Keywords: ARIES, power plant, blanket, divertor
I. Introduction

The primary functions of the fusion power core are: (1) to maintain the environmental conditions needed to operate a high-performance steady-state plasma and contain hazardous species, (2) to shield the superconducting magnets and other ex-vessel components and personnel from radiation and high heat fluxes, (3) to provide high-grade heat in the coolants for the purpose of generating electricity, and (4) to enable fuel self-sufficiency by producing and releasing adequate amounts of tritium. There are many additional requirements on the operation of the power core, related to safety, reliability, lifetime, etc. These requirements for an attractive power plant are applied in all ARIES studies, and can be found elsewhere (see for example [1]). The purpose of this study is to show through analysis that our proposed design concepts can fulfill the functions and satisfy the requirements for an attractive power plant, and to highlight key R&D needed to further demonstrate this.

Figure 1 shows a cross section of one of the sectors of the ACT1 fusion core, including the primary elements: first wall/blanket with embedded passive plasma stability shells, divertor, structural rings, coolant manifolds and vacuum vessel with port extensions. The maintenance concept adopted for this design, as with all of the ARIES tokamak designs since ARIES-RS [2], utilizes horizontal movement of full sectors through large port extensions in the vacuum vessel. The sectors, or “replacement units”, are self-supporting with their gravity loads transferred downward into support pillars. Much more detail on the configuration and maintenance of the power core can be found in an accompanying article [3]. Table I summarizes the key device parameters that impact power core performance.

Figure 1. Main elements of the ARIES-ACT1 fusion core
In this article we present the key design choices and detailed thermal, fluid and mechanical analysis of these components. Nuclear analysis, including volumetric heating, tritium production, activation, decay heat and others, can be found in an accompanying article [4].

**First wall/blanket and coolant manifolds**

ARIES-ACT1 adopts a high-performance concept of a fusion power core, with the ability to survive moderately high power density plasmas (peak neutron wall loading less than 4 MW/m² and peak time-averaged surface heat flux on the first wall below 0.5 MW/m²) with coolant outlet temperature of the order of 1000°C. SiC composite structural materials open a pathway to these high temperatures and power densities, and simultaneously provide advantages in afterheat and waste disposal due to their low activation. Since their first introduction in a fusion power plant study [5] materials research programs around the world have been initiated to explore advanced SiC composite properties, fabrication and joining techniques. For the ARIES-ACT1 blanket, we adopted the general configurational aspects of the self-cooled SiC-composite blanket used in the ARIES-AT study [6], with several modifications and improvements to add detail to the design and enhance the attractiveness. Special attention was given to PbLi coolant manifolding in order to assure acceptable pressure and also balanced flow distribution in the many parallel channels of the first wall and blanket.

Active coils and passive conducting shells are required in ARIES-ACT1 for plasma stability [7]. The passive shells are placed behind the first blanket module on both the inboard and outboard sides of the machine. They are fabricated from tungsten alloy (similar to the divertor), cooled by He and supported through attachments. On the inboard side, the shells are attached to, and embedded within the structural ring. On the outboard side, the shells are embedded into the inner side of the outer blanket modules. ELM control coils have not been included in the current design due to uncertainties in the need for them and their characteristics.
In this study, we attempted to design systems for coolant routing that minimize liquid metal MHD effects. The choice of SiC structures helps to reduce MHD pressure drop overall, but two important effects remain in liquid metal cooling systems even with electrically insulating walls throughout: (1) localized 3D MHD pressure drops can be large, and can dominate the total pressure drop, and (2) flow distribution into multiple parallel channels can be difficult to control. The basic concept of the blanket module flow paths is simple: straight poloidal flow with 180˚ bends at the top. The greatest challenge for MHD flow control is the manifolding at the bottom of the fusion core, where a small number of feed pipes coming from the ring headers must branch into many parallel channels for the individual modules. Options for simplified manifolding that minimizes MHD effects are discussed in an accompanying paper [3] and estimates of the 3D MHD pressure drops are made below in Section II.B.

**Divertor**

The surface heat flux profile in the divertor is highly uncertain, even for near-term machines like ITER. Using the ITER methodology for calculating the power decay length in the plasma edge [8], the peak divertor heat flux in ARIES-ACT is expected to exceed 10 MW/m², even considering flux expansion and divertor tilting. One of the design options that can achieve both high temperature (for power conversion) as well as high heat flux capability is the He-cooled tungsten alloy divertor. This concept was introduced in ARIES-ST [9], and has become the subject of much attention worldwide [10,11]. Extensive analysis was performed in this study to demonstrate adequate heat removal using impinging jet configurations, and a complementary effort on experimental verification has been carried out [12]. An innovative transition joint was evaluated to remove dissimilar metal joining from the high heat flux region (see Section III.D), thus avoiding one of the most serious concerns for divertor reliability. The largest remaining uncertainty for tungsten-alloy divertors relates to the base material properties and joining, including the effects of irradiation. Ductility, creep strength and temperature window of operation are all important factors that determine the feasibility of this design concept. Research on structural alloys of tungsten for fusion has increased greatly in recent years due to its importance in the design of an attractive fusion power plant [13,14].

**Structural ring**

The structural ring surrounds the blanket and is used to attach individual blanket and divertor segments and support their gravity and electromagnetic loads. The ring is an integral part of the replacement units, being removed into hot cells for removal and maintenance of individual parts. This component also contributes to shielding of the vacuum vessel and superconducting coils. In ARIES-ACT1, we chose to use advanced low-activation ferritic steel as the structural material for this component. Helium coolant removes 76 MW of high grade nuclear heat from the steel. There is a strong desire to operate this component at a high temperature in order to contribute to power conversion. As shown in Section V.C, the desired coolant outlet temperature is 700˚C, which would require a high-performance dispersion-strengthened alloy. Lower coolant outlet temperature could be employed if high-performance steels are not available, with a relatively modest impact on conversion efficiency.
**Vacuum vessel**

Our fusion core is designed in such a way that the vacuum vessel is not required to provide mechanical support of other components, and is not required for shielding of magnets. Its functions are simply to provide a vacuum boundary and containment of radioactivity, including tritium. To best serve these functions, we chose to operate the vessel at an elevated temperature of the order of 350-500˚C and to use He as coolant. The absence of water and the high operating temperature are expected to reduce tritium inventory. The large maintenance ports inside the vessel provide such a large enclosed volume that a complete loss of helium from the power core (including the ex-vessel inventory) would lead to an overpressure less than 1 atm in case of a loss-of-coolant accident.

In this temperature range, and considering the expected end-of-life fluence, a low-activation ferritic steel is desired. However, the steels typically used in first walls applications (e.g., F82H or EUROFER97) have special microstructures that require post-weld heat treatment. We chose a low-activation 3Cr-3WV bainitic steel [15] that provides lower activation than 316SS and no need for post-weld heat treatment. The vessel is composed of two face sheets and strengthened by the extensive use of ribs. Helium flows between the face sheets to control the temperature, but more importantly as a barrier to tritium transport out of the vessel. The total thickness of the double-walled vessel is only 10 cm.

**Power conversion systems**

Achieving high thermal conversion efficiency is important not only because it directly affects the cost of electricity, but system studies show that the design space for a tokamak power plant is significantly larger when the efficiency is high. Less demand on other systems, especially the plasma, enables a more modest plasma beta, lower peak toroidal field, and other advantages.

Many power cycles are possible, including steam Rankine, supercritical CO₂ and He Brayton. The compatibility of the power conversion system with the primary coolant composition and operating conditions is a major factor in choosing a cycle. For ARIES-ACT1, similar to several of our previous studies, the He Brayton cycle was chosen because it offers a good match to the high-temperature cycle using He and PbLi coolants on the primary side. It has several important advantages: He is chemically inert, permeation of tritium is easier to control, and the performance continues to improve as the turbine inlet temperature increases.

Similar to the dual-cooled design of ARIES-ST [9] and ARIES-CS [16], the combination of heat from both He and PbLi adds some complexity to the power conversion system. Cycle calculations are presented in Section V. The primary heat exchangers are assumed to be made of SiC; the technology is already available commercially. Detailed design of the heat exchanger internals has not been performed in this study.
II. First wall/blanket

II.A Introduction

The general design concept adopted for the ARIES-ACT1 first wall and blanket was originally proposed in the ARIES-AT study [17]. This design utilizes lead-lithium coolant and breeder in a self-cooled configuration (SCLL) with SiC composite structures. It provides excellent safety and environmental characteristics as well as very high performance due to the high temperature capability of SiC composites in contact with PbLi eutectic. The modest peak surface heat flux expected in ARIES-ACT1 is easily managed with the double-pass configuration in which the colder inlet coolant (740°C) is sent first to the outer annular region, turned 180° at the top and then passed more slowly through the bulk of the blanket. The annular channel design maintains all structure temperatures below the bulk coolant outlet temperature. The MHD pressure drop is expected to be modest as a result of the low electrical conductivity of SiC and the simple flow paths. Table II summarizes some of the key parameters of the first wall and blanket.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Units</th>
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</thead>
<tbody>
<tr>
<td>PbLi inlet temperature</td>
<td>740</td>
<td>°C</td>
</tr>
<tr>
<td>PbLi outlet temperature</td>
<td>1030</td>
<td>°C</td>
</tr>
<tr>
<td>Peak structure temperature</td>
<td>960</td>
<td>°C</td>
</tr>
<tr>
<td>Peak first wall coolant velocity</td>
<td>4</td>
<td>m/s</td>
</tr>
<tr>
<td>Peak central duct coolant velocity</td>
<td>0.17</td>
<td>m/s</td>
</tr>
<tr>
<td>Total surface heating</td>
<td>128</td>
<td>MW</td>
</tr>
<tr>
<td>Total volumetric heating</td>
<td>1560</td>
<td>MW</td>
</tr>
<tr>
<td>Maximum pressure in access pipes</td>
<td>2.8</td>
<td>MPa</td>
</tr>
<tr>
<td>Maximum pressure in outer blanket duct</td>
<td>1.95</td>
<td>MPa</td>
</tr>
<tr>
<td>Maximum pressure across inner duct</td>
<td>0.3</td>
<td>MPa</td>
</tr>
</tbody>
</table>

SiC composite parameters:
- Peak allowable structure temperature: 1000 °C
- SiC/SiC elastic stress limit: 190 MPa
- SiC – PbLi compatibility limit: 1000 °C
- Fluence lifetime (burnup of C): 3 %
- Cost of fabricated structures: 600 $/kg

Figure 2 shows a drawing of the blanket with integral first wall. Each sector contains three segments that are independently cooled: one inboard and two outboard. Figure 2a shows one full outboard segment containing 8 parallel double-walled pipes. Figure 2b is an expanded view of the internal geometry of the pipes. Ribs between the inner and outer pipes provide mechanical strength to each module, and thick sector side-walls react internal pressure stresses where there is no adjacent module.

Both steady and transient particle loads to the first wall of a tokamak are highly uncertain. If a plain SiC composite wall facing the plasma proves unacceptable due to particle fluxes and off-normal events, then we considered that a tungsten coating may be applied to all plasma-facing
surfaces. Various techniques for bonding W onto SiC have been demonstrated, including diffusion bonding, sinter bonding and liquid phase sinter bonding using hot-pressing. [18] An important feature of this duplex structure is the very similar thermal expansion coefficients of the two materials, which prevents large differential thermal stresses.

Figure 2. Depiction of the ARIES-ACT1 blanket concept: (a) outboard blanket sector closest to the plasma, including lower manifolds, (b) cross section of the internal construction of the outboard blanket including one sector side wall.

The development of SiC composites for fusion applications continues throughout the world, with growing confidence in their use as engineering materials in nuclear systems. Recent progress on composite development, basic properties, joining, compatibility and irradiation effects have been summarized recently by Katoh *et al.* [19]. For the purpose of engineering analysis, properties of SiC composites were obtained from the previous ARIES-AT study [17] and the ARIES Town Meeting on SiC composites held in the year 2000 [20]. Some of the key parameters are listed in Table II.

In the following subsections, we present the analysis performed to help define the major parameters of the first wall and blanket, including configuration, dimensions and operating parameters (*e.g.*, flow rates, temperatures, pressure stress). Section II.B summarizes simplified analysis of MHD pressure drop, Section II.C describes heat transfer for laminarized MHD flow that considers the effect of first wall curvature (but not complex 3D effects on flow distributions), and Section II.D presents 3D thermal and pressure stress analysis of representative blanket modules.
II.B MHD pressure drop and flow control

II.B.1 Introduction

The exclusive use of low electrical conductivity SiC composite structures (<500 $\Omega^{-1}m^{-1}$ [21], 3-4 orders of magnitude lower than steel) in contact with PbLi coolant is expected to reduce the MHD pressure drop considerably, making it possible to design a self-cooled blanket with acceptable pressure stresses. The fully-developed pressure gradient in a straight duct with uniform magnetic field is quite low because that geometry forces induced currents to pass through thin boundary layers and low-conductivity walls, which keeps the magnitude of the currents low. However, any real blanket requires coolant manifolds, bends, expansions and contractions, and routing through varying toroidal and poloidal fields. The resulting 3-dimensional MHD effects can be large, and can dominate the pressure drop, flow distribution and velocity profiles in the blanket. The majority of our effort on coolant channel and manifold design focused on reducing 3D effects.

3D effects occur when the electric potential is unbalanced from ideal 2D profiles, resulting in “short circuits” within the bulk of the coolant, away from the boundary layers. The voltage $V_{ab}$ across any duct along the direction perpendicular to the magnetic field $B$ is related to the electric field $E$ induced by the fluid velocity $u$:

$$V_{ab} = \int_{a}^{b} E \cdot dl = \int_{a}^{b} u \times B \cdot dl$$

where $a$ and $b$ are the locations of the two walls. If we can maintain constant voltage along the direction of flow, then we can minimize the generation of 3D currents.

In this work, our goal was to develop credible reduced-MHD designs in sufficient detail to allow for more thorough evaluation by MHD codes and experimental verification. We did not attempt the difficult task of analyzing complex 3D MHD phenomena. Instead, we used a simplified technique for estimating the added 3D pressure drop ($\Delta p_{3D}$) above and beyond the normal 2D pressure gradient from individual elements of a cooling system. [22]. Semi-empirical correlations have been collected for various geometries and wall conductance ratios, and placed in a form related to the fluid inertia ($\rho u^2/2$), the MHD interaction parameter (N) and a semi-empirical constant k:

$$\Delta p_{3D} = kN\left(\rho u^2/2\right)$$

For flows with geometrical changes in a uniform magnetic field typically $0.25 < k < 2$. For a change in transverse field strength, $k$~0.1–0.2 (depending on the abruptness of the change in B). For an inlet or outlet manifold, Smolentsev et al. recommended $k$=1.5 [23].
II.B.2 Geometric perturbations

Figure 3 shows several design elements that are expected to have relatively small or large 3D effects. For example, a contraction or expansion of a duct in the direction perpendicular to B does not create a voltage imbalance, because the higher velocity in the smaller flow area is balanced exactly by the smaller integration path. Conversely, a contraction in the same direction as B leads to higher velocity with the same integration path, hence a parallel voltage difference. By avoiding “bad” design elements, we strive to maintain the flow in a quasi-2D condition with only minor perturbations to the fully-developed currents and velocities.

The flow paths for ARIES-ACT1 were designed to be as simple as possible. Within the blanket segments, the flow is purely poloidal, with a single 180° bend at the top. This bend is made perpendicular to the main toroidal field, which maintains relatively constant cross-field voltage (Fig. 3a). The bend also includes a transition from many first wall channels to the one central duct. The details of this flow merging will result in some additional 3d effects, which require further effort to fully understand. At the bottom of the segments, we expect to include regions in which the radial dimension changes to accelerate the flow into the manifold region. This is a cross-field variation that also should have a modest 3d effect.

![Figure 3. Design elements that produce (a) small 3D effects or (b) large 3D effects.](image-url)

Manifolds are the most challenging design feature for controlling MHD. 3D effects are difficult to avoid in manifolds where a small number of channels split into a larger number. We examined several options for manifolds at the bottom of the sector, with attention to fabricability, maintenance, and penetrations through the vacuum vessel. These are described in more detail elsewhere [3,24]. Flow paths that cross from toroidal to poloidal directions were avoided, since this geometry has been known to create difficulties for many years [25]. The reference design we chose is shown in Figure 4. The coolant from each individual blanket module (inboard and outboard) is routed away from the sector and through the vacuum vessel. Manifolding is placed outside of the toroidal field coils. The concept uses large “brazeing blocks” that allow for disconnecting sectors during maintenance.
II.B.3 Magnetic field variations

One unavoidable source of 3D MHD effects arises from variations along coolant paths in the magnetic field strength and direction. Coolant must enter and exit the high toroidal field region. It must travel along paths with varying toroidal and poloidal fields. In the case of the toroidal field, the field direction remains roughly constant along the predominantly poloidal flow paths and varies in magnitude gradually throughout the region within the coils. The largest effect will occur at the entrance and exit between TF coils, where the field strength changes abruptly. The local pressure drop is estimated with a k-factor of 0.1-0.2.

The poloidal fields created by the PF coils and the plasma current are more complex, as they vary both inside and outside of the TF coils. An analysis was performed to determine the best path for feed pipes that avoids the highest poloidal fields. Figure 5 shows a map of the poloidal field contours superimposed on top of a CAD drawing of the lower half of the machine. These field lines result from both the distributed plasma current and the PF coil currents under flat-top steady state conditions.

The results show that the poloidal field strength is nearly constant within the blanket (around 1 T). Near the vacuum vessel, the poloidal field increases to around 1.5 T. By routing the inlet/outlet pipes through the vacuum vessel port (and then down through the port door floor), the high field strength region near the PF coils is avoided. In the region of the ring headers, where the parallel ducts are finally connected to the supply lines, the toroidal field is nearly zero and the poloidal field is below 0.5 T. The maintenance implications of the pipe routing are discussed elsewhere [3].
II.B.4 Evaluation of pressures

We performed a thorough evaluation of the flow paths from the inlet ring headers all the way through the power core and back to the outlet ring headers in order to apply “good” MHD design principles to the maximum extent possible. In order to evaluate pressure stresses in the blanket and access pipes, pressure drops were estimated using analytic formulas for 2D pressure gradient and the semi-empirical formula above for 3D effects. For the 2D pressure gradient, we considered the fact that SiC is not a perfect insulator. However, the wall conductance ratio is numerically similar to the inverse Hartmann number, such that the impact of finite conductivity of the walls, even in the worst case, should be less than a factor of 2 in the calculation of fully-developed pressure gradient.

The static head due to gravity is a major contributor to the local pressures. It is important for safety reasons to locate the PbLi heat exchanger above the highest location in the blanket to encourage thermal convection under loss-of-flow accidents. Considering these competing needs, we located the heat exchanger just 4 m above the highest point in the blanket. Pressures in the current design are summarized in Figure 6.
II.C Liquid metal heat transfer

II.C.1 Introduction

Knowledge of blanket structure temperatures is important to ensure all materials and interfaces remain within acceptable ranges, and that the thermal stresses in all structures are within allowable values. In order to estimate heat transfer in the first wall and blanket, analysis of the coolant temperature profile was performed under the assumption of laminar flow conditions.

The transition between laminar and turbulent MHD flow in insulated ducts is complex. Quasi-two-dimensional turbulence, in which the vorticity of the turbulent fluctuations is predominantly aligned with the direction of the field, tends to be quite long-lived and may result from the reorganization of the flow as it enters into the magnetic field. In an infinitely long, electrically insulated channel, all turbulence fluctuations are eventually damped when $Ha/Re > 0.008$ [26], but the entry lengths can be long. For our design, $Ha/Re$ is smaller than $10^3$ in the FW channels (due to the high velocity), but much larger than 0.008 in the large central channels. This is a positive outcome, because we desire improved heat transfer in the FW channels and not in the central duct (where it would cause overheating of the walls separating the FW and central channels). Therefore, the most conservative assumption is laminar flow throughout all of the channels.

For the sake of simplification, we assumed that the flow is fully developed at the inlet to the FW channels, and that the flow is well distributed and fully developed in the central duct following the 180° bend at the top of the blanket. The velocity profile in the central duct is...
assumed to be flat (slug flow), whereas the profiles in the first wall channels must include the effect of the FW curvature.

A special issue arises with heat transfer in channels with curved walls. As shown in Figure 7, curvature leads to a variation in path length along magnetic field lines from one side of the duct to the other; this leads to a variation in the effective Hartmann number, which then leads to varying drag on the fluid. Detailed studies suggest that a good approximation of the velocity profile can be made by assuming the velocity is inversely proportional to the path length of the magnetic field between walls, which implies stagnated regions in the corners. [27] We included this feature in the thermal analysis, and found it contributes about 30°C additional temperature rise at the plasma-facing surface.

![Figure 7. Curved geometry of the first wall region](image)

II.C.2 Thermal analysis methodology and results

In order to determine the temperature profile in the FW cooling channels, central duct and walls, the energy equation was solved in 2D (radial-poloidal plane) assuming toroidal symmetry. The velocity was toroidally averaged through the channel accounting for the variation caused by the curved wall. Thin MHD boundary layers were not modeled (the velocity was not forced to zero at walls). We assume the fluid is well mixed at the top bend. Conduction is allowed between the inner and outer ducts; in fact, substantial heat is exchanged in this way. Figure 8 depicts the grid, with the plasma heat flux applied to the left side of the figure. Volumetric nuclear heating is included, with the profile obtained from 3D neutronics [4].

In this geometry, the energy equation can be expressed as follows:

\[ u(x) \frac{\partial e(x,z)}{\partial z} = k \frac{\partial^2 T(x,z)}{\partial x^2} + Q(x) \]

where \(u(x)\) is the poloidal velocity as a function of depth \(x\), \(e\) is the energy, \(z\) is the distance along the flow direction, \(k\) is the thermal conductivity (of either the coolant or the wall), \(T\) is the temperature and \(Q\) is the volumetric heat rate. This equation was converted to a simpler initial value problem and solved by relaxing an initial guess of the temperature profile toward a steady state solution of the following equation:

\[ \frac{de}{dt} = k \frac{\partial^2 T}{\partial x^2} + Q - u \frac{\partial e}{\partial z} = 0 \]
Figure 8. Discretization of the grid for liquid metal heat transfer analysis

Figure 9 shows the temperature profiles \( \text{vs.} \) distance from the plasma-facing surface at three vertical locations (bottom, middle and top). The radial decay in volume heating is apparent in the central duct. The effectiveness of the annular design in keeping the structures cooler than the peak temperature in the PbLi is also apparent.

Figure 9. Heat transfer results for the outboard blanket module facing the plasma

II.D Blanket thermomechanical analysis

Detailed thermal and pressure stress analyses were performed to determine all design parameters of the inboard and the first and second outboard blanket segments, including the size of the module, the numbers of the modules per sector, first wall curvatures, wall thickness, rib thickness and rib spacing. These parameters were optimized based on the primary stress limit
and the SiC volume fraction, which affects cost and tritium breeding capability. For SiC/SiC composite, the conventional stress limits (ASME code 3S_m) cannot be applied directly when finite element modeling (FEM) is used to evaluate allowable stresses. The non-isotropic behavior of SiC/SiC as well as non-linearity arising from the unique damage mechanisms must be taken into account. Our best guidance to date is to maintain the combined primary and secondary elastic stress below 190 MPa, although this may be too conservative. [17] We allocated 100 MPa to primary stress alone, in order to leave margin for thermal stresses.

The blanket modules must accommodate a static coolant pressure due to gravity (considering the total height of the power core, heat exchanger and Pb-17Li coolant ring header underneath the reactor) and MHD pressure drops through the blanket and manifolds. As illustrated in Figure 6, the total pressure is ~1.95 MPa within the first wall annular ducts of the inboard blanket and 1.65 MPa within the center duct. For the inner modules of the outboard blanket, the maximum primary stress at the first wall is ~49 MPa, and local stress at the sharp corner between the first wall and rib is ~ 88 MPa.

The finite element code ANSYS [28] was used to predict thermo-mechanical behavior of the blanket. Full 3D analysis was performed on a finite poloidal slice of the blanket modules, since a full 3D analysis of the entire blanket is difficult and unnecessary at this stage of preconceptual design. The location of the slice is near the bottom of the sector, where the pressure from coolant weight is the highest. Figures 10 and 11 show results for a module at the sector end (where additional reinforcement is needed) and for a central duct. The end wall is unconstrained, whereas symmetry conditions are applied on walls that contact neighboring modules. These walls exhibit constant stress, as seen in Figures 10 and 11. The bottom of the domain is constrained against vertical displacement; all other displacements and bending are allowed. The top of the domain is fully unconstrained.

![Figure 10](image_url)  
**Figure 10.** Distribution of primary stress for the outer module of the inboard blanket (the deformation is exaggerated by 64 times)
Figure 10 shows the distribution of the primary stress for the outer module (at the toroidal end of the sector) of an inboard blanket. The outer side walls of the outer modules (see also Figure 2-b) have no pressure balance from the outer side wall of another module and therefore must be reinforced by increasing the thickness of the outer side wall and the number of ribs. Figure 11 is an example result showing the distribution of the primary stress for the inner module of the first (plasma-facing) outboard blanket. The primary stress for the second outboard blanket is not shown. The results indicate that the 100 MPa limit is approximately met. Stress concentrations occur at the corners between the flat side walls and the first wall, and at the locations where the ribs attach to the walls (to resist bending of the large plates).

Figure 12 illustrates the importance of the ribs interconnecting the inner and outer blanket ducts for control of pressure stresses. For this calculation, we assumed a static pressure of 0.8 MPa from the coolant weight near the bottom of the blanket (at the outlet), and varied the pressure difference across the inner duct due to MHD pressure drop from the inlet to the outlet. By connecting the inner and outer pipes, either in manufacture or by brazing, the box is significantly stiffened and can handle a much higher pressure drop. Therefore, the reference design for ARIES-ACT1 assumes a rigid connection between the inner and outer pipes (as opposed to ARIES-AT, which allowed the inner pipe with attached ribs to slide freely within the outer pipe).

Thermal stress analysis of the inboard blanket was performed and the results are summarized in Table III. Two locations are listed in the table: near the bottom (where the pressure stress is highest) and near the top (where the temperature gradient, front to back, is greatest). In all cases the combined primary and secondary stresses are well below the allowable limit of 190 MPa.
Figure 12. Primary stress reduction achieved by brazing the inner and outer blanket pipe.

The front-to-back module temperature differences are modest in our design concept (see Section II.C.2): ~100 °C at the bottom of the blanket and ~130 °C at the top. The peak local total stresses occur at the sharp corner beteen the first wall and rib, and are mainly due to pressure stresses; they are ~141 MPa at the bottom and 142 MPa at the top. The maximum thermal stress is located at the top of the blanket module where the primary stress is low because of lower coolant pressure (~0.95 MPa in the annular ducts and ~0.85 MPa in the center duct).

Table III. Combined primary and secondary stresses of the inboard blanket (all in MPa)

<table>
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<tr>
<th></th>
<th>First wall</th>
<th>Corner of first wall</th>
<th>Side wall</th>
<th>Back wall</th>
<th>Stress limit</th>
</tr>
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<tr>
<td><strong>Bottom of blanket</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>Primary stress</td>
<td>49</td>
<td>88</td>
<td>39</td>
<td>44</td>
<td>100</td>
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<tr>
<td>Primary+secondary stress</td>
<td>105</td>
<td>142</td>
<td>79</td>
<td>84</td>
<td>190</td>
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<td><strong>Top of blanket</strong></td>
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<td>Primary stress</td>
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<tr>
<td>Primary+secondary stress</td>
<td>103</td>
<td>141</td>
<td>76</td>
<td>81</td>
<td>190</td>
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</tbody>
</table>

III. Divertor

III.A Introduction

The divertor is subjected to the most extreme conditions of any component in a fusion power plant: the highest heat fluxes, strongest plasma interactions and most severe transients. It must
withstand neutron fluxes similar to the first wall, with a lifetime (and reliability) consistent with high plant availability. Large uncertainties exist in the loading conditions and the response of materials in this environment. The materials and design choices are extremely limited.

We chose to adopt tungsten alloy structures with He coolant, similar to the ARIES-ST study [9]. This material choice has received considerable design effort and is the subject of ongoing R&D efforts in several countries. The choice of tungsten to serve also as the plasma-facing armor was motivated in part by the general consensus in the fusion community that it has the potential to provide acceptable erosion and neutron damage rates, although this remains to be demonstrated. It maintains good mechanical strength at very high temperature, which allows us to utilize the large amount of deposited energy in an efficient power cycle. In fact, its preferred temperature range of operation is so high that it presents difficulties in matching the coolant and structure temperatures with steel structures in the power core and piping.

Several design options have been considered, all of which utilize impinging jet cooling configurations to provide good cooling with acceptable pressure drop. The use of He in an impinging jet configuration has been explored extensively in this project and elsewhere [29], and has been shown capable of removing heat fluxes in the predicted range for our design concept, up to ~14 MW/m². The two main variants of impinging jets include the linear “slot jet”, which employs a single orifice aligned along the axis of the manifold pipe (Figure 13a), and an array of circular jets emanating from a thimble, or “finger” (Figure 13b). More details on design options can be found elsewhere [10,30].

![Figure 13. Slot jet and circular jet array configurations](image)

The fundamental tradeoff between configuration options is the need for smaller structures and more complexity in order to survive higher heat flux. This will likely increase the cost and decrease the reliability of these structures. For a slot jet, the maximum allowable heat flux is below 10 MW/m²; finger-type jet arrays may be capable of removing heat fluxes as high as 15 MW/m². Since the expected heat flux in our divertor exceeds 5 MW/m² only in a narrow region of the plasma “footprint”, a combination of linear slot-jets and circular jets was adopted as our
reference concept. This design variant uses inlet cartridges containing both kinds of jets, and a housing structure that guides the flow past the heated wall and then back to the outlet manifold.

Figure 14. The reference divertor concept: combined plate and finger elements

Figure 14 depicts the design concept, including all of the main features of the divertor plate (i.e., excluding the support structure and shielding behind the plates). Coolant enters the steel cartridges and is directed to the front plate through orifices. The front plate is shaped to accept flow from either a linear slit or an array of circular holes. The armor is an integral part of this front plate. After passing the heated wall, the coolant returns to the back of the cartridge and is sent finally to the exit manifolds.

An important element of our divertor design concept is the interface between steel inlet and outlet coolant channels and the tungsten structures. Since tungsten and steel possess very different coefficient of thermal expansion (CTE), differential stresses are a serious concern that limit performance and provide likely sites for failure. In addition, the operating temperature windows of steel and tungsten exhibit little or no overlap. Our design avoids the direct connection of steel to tungsten within the high heat flux region. The steel inlet cartridges are not mechanically attached to the surrounding structure except at the ends of the divertor plates where the heat flux is low and an intermediate material is used. These transition joints use Ta alloy, with CTE between that of steel and tungsten. More details on the joint design and analysis are found in Section III.E.

Table IV summarizes the design parameters adopted for our He-cooled W divertor. The allowable temperature range for structural tungsten alloys has been set to a minimum of 800 °C based on embrittlement under neutron irradiation [31,32] and a maximum temperature of 1300°C due to recrystallization (and creep). Both of these are highly uncertain. We allow the tungsten armor, which serves no structural function, to exceed the recrystallization temperature and limit its use to 2/3 of the melting point.
### TABLE IV. ARIES ACT1 divertor parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coolant</td>
<td>He</td>
<td></td>
</tr>
<tr>
<td>Coolant pressure</td>
<td>10</td>
<td>MPa</td>
</tr>
<tr>
<td>Surface power to divertor</td>
<td>276</td>
<td>MW</td>
</tr>
<tr>
<td>Peak time-averaged heat flux</td>
<td>13.7</td>
<td>MW/m²</td>
</tr>
<tr>
<td>Peak heat flux during ELMs</td>
<td>4.25</td>
<td>GW/m²</td>
</tr>
<tr>
<td>Inboard slot length</td>
<td>48</td>
<td>cm</td>
</tr>
<tr>
<td>Outboard slot length</td>
<td>76</td>
<td>cm</td>
</tr>
<tr>
<td>Armor thickness</td>
<td>5</td>
<td>mm</td>
</tr>
<tr>
<td>W-alloy structure min allowable T</td>
<td>800</td>
<td>°C</td>
</tr>
<tr>
<td>W-alloy structure max allowable T</td>
<td>1300</td>
<td>°C</td>
</tr>
<tr>
<td>W armor maximum allowable temp</td>
<td>2190</td>
<td>°C</td>
</tr>
<tr>
<td>He inlet temperature</td>
<td>700</td>
<td>°C</td>
</tr>
<tr>
<td>He outlet temperature</td>
<td>800</td>
<td>°C</td>
</tr>
<tr>
<td>Elastic limits of W</td>
<td>See Table VIII</td>
<td></td>
</tr>
<tr>
<td>Uniform elongation of W at 1200°C</td>
<td>1%</td>
<td></td>
</tr>
</tbody>
</table>

In the remainder of this section, we present thermal, fluid and mechanical analysis of this design. Only selected results are presented; much more detail on this design has been published previously. In Section III.B, we summarize considerations in choosing the main structural materials, including tungsten alloy and high-performance steel for the coolant manifolding. In Section III.C, we summarize the heat transfer and pressure drop correlations that are used in systems analysis. Section III.D presents thermo-fluid analysis using ANSYS CFX. An assessment of different turbulence models is made and an example 3D analysis of the finger design is presented. This analysis was used to optimize the jet layout. In Section III.E, we summarize the results of elastic-plastic stress analysis. Analysis of fracture mechanics and creep of the ARIES-ACT divertor are presented in an accompanying paper [33]. Finally, Section III.F summarizes the design and analysis of the transition joint between the steel inlet/outlet pipes and the tungsten alloy plate structures.

### III.B Materials considerations

Materials used in the divertor and associated cooling system include pure W armor, W-alloy structures, Ta-alloy for the transition joint, and an advanced steel for the inlet cartridges and outlet manifolds. It is beyond the scope of this article to explain in detail all of the characteristics and issues with the various options for these materials; here we only summarize the key choices with references to the existing literature.

Probably the most important, and most uncertain material in the divertor is the structural alloy of tungsten. Several alloy classes are available, and the state of knowledge is not mature enough to downselect today. However, an active research program is underway to improve the knowledge base and develop strategies for alloy improvement [13,34]. The key issues from a design point of view include fracture toughness, creep strength and temperature window. In
addition to basic material properties, there is a need to develop fabrication technologies, joining and inspection techniques.

Limited ductility under the influence of 14-MeV neutron irradiation is a concern for virtually all structural materials to be used in a fusion power core, and a problem that must be addressed in order to bring fusion to commercial reality. While improvements to the material microstructure are important to pursue, design strategies can also help reduce the requirements on ductility (e.g. uniform elongation) and fracture toughness. Analysis of the highest stress regions in the divertor structure indicate that our design can tolerate cracks of sufficient size to be detectable prior to operation, with maximum stress intensity in the range of $10-20 \text{ MPa-m}^{1/2}$ [33]. This offers a relatively modest goal for alloy development.

The W-alloy temperature window of operation is equally important in this design, due to the requirement to operate ferritic steel with the same coolant. Our design attempts to alleviate the temperature window mismatch by using an advanced high-temperature steel alloy (discussed below), by minimizing stresses in the steel, and by designing the coolant paths in such a way that the tungsten can operate hotter than the steel even using the same coolant. We tried to be conservative in our design by adopting a relatively high minimum allowable temperature of 800°C (to ensure ductility under irradiation); there is reason to believe this value could be reduced [13].

The use of W-alloy for the divertor structure is motivated in part by the desire to avoid “duplex” structures in the high heat flux region, where differential thermal expansion can create large stresses and lead to reliability concerns. Still, somewhere in the system we need to transition from W-alloy to steel piping that transports coolant to and from the heat exchanger. The transition joint (see Section III.F) is a key aspect of our design. Even outside the high heat flux region, differential stresses between W and steel (caused by heat-up and cool-down) are still too large, and would lead to ratchetting at an abrupt transition. To reduce stresses and accommodate more strain at the transition, we adopted Ta-alloy interlayer. Ta has a coefficient of expansion between that of steel and tungsten, and a relatively high uniform elongation under irradiation (~2%) at operating temperature. Alloys of Ta, such as T-111 (Ta-8W-2Hf) and Ta-2.5%W provide higher yield strength and higher uniform elongation than pure Ta. We chose Ta-2.5%W to avoid activation concerns with Hf. A special concern with Ta that needs to be addressed is hydrogen embrittlement.

For the steel inlet cartridges in the ACT-1 divertor, the helium inlet temperature needs to be above ~700°C to ensure that the structural tungsten sections operate above ~800°C to avoid radiation hardening-induced embrittlement. There is no surface heating in the cartridges and volumetric heating should be fairly uniform; therefore, thermal stresses should be low. Given the low pressure drop in the jets (0.3 MPa) the pressure stresses in the cartridge are also expected to be low.

Currently, the only credible reduced activation materials option for operation in the 700-800°C regime is a nanostructured ferritic alloy such as 14YWT. The NFAs have outstanding high temperature strength and creep properties in this temperature range (Figures 15 and 16). Other important characteristics of the NFAs are: (a) the absence of a ferrite to austenite phase
change, thus ensuring that during off-normal temperature excursions above 800°C the alloy will remain in the ferrite phase field, and (b) the exceptional thermal stability of the dispersion of the nano-features. The only radiation effects issue is related to helium generation since all parts of the cartridge are operating well above the temperature range of radiation effects stemming from point defect generation, defect clustering and radiation-enhanced diffusion. The stress levels in the cartridge may be too low to trigger significant helium embrittlement phenomena related to stress-induced bubble growth. Increased Cr levels or alloying with Al are options for further alloy development if it becomes necessary to increase oxidation resistance, depending on helium impurity control.

Figure 15. Larson-Miller plot illustrating the superior high-temperature creep rupture behavior of the NFA alloys compared to a conventional 9Cr ferritic-martensitic steel [35].

Figure 16. Temperature dependence of yield stress for several NFAs and a reduced activation 9Cr ferritic-martensitic steel [36].
For all of the ferritic-martensitic and ferritic structural materials, creep, creep rupture and microstructural stability phenomena are primarily driven by thermal effects at operating temperatures >600˚C. In this situation, the most significant radiation effects phenomena will stem from the accumulation of transmutation-induced helium and the possible effects of grain boundary helium bubbles on creep rupture life. This is a regime that has received very little attention from fusion materials programs and clearly there will be a need for detailed thermal/structural analysis of specific components and development of a greatly expanded data base on the effects of long-term service at high temperatures on microstructural stability and critical properties such as strength, ductility, fracture toughness, and also on time-dependent properties such as creep and fatigue. Design of such an experimental program must be underpinned by microstructural modeling of the behavior of helium in the high temperature regime and its impact on fracture and on creep cavity formation.

III.C Heat transfer and pressure drop correlations

The heat flux limit and pumping power penalty associated with various divertor design concepts are critical both for choosing a design option and also as input to systems analysis. Constraints on peak divertor heat flux have a large impact on the design space for the power plant, potentially increasing the size and affecting other major device parameters. Based on detailed analysis, we developed correlations relating the He pumping power required to maintain the structures within their temperature and stress limits for both the plate and finger designs. Results are shown in Figure 17 for 600˚C and 700˚C He inlet temperature and a maximum allowable temperature of 1300 °C in the structural tungsten (non-structural armor is allowed to exceed the recrystallization limit). The pumping power is presented as a fraction of the thermal power incident on the surface, to provide a more meaningful metric of performance.

![Figure 17](image)

Figure 17. Heat flux handling limits for plate and finger designs with 600˚C and 700˚C inlet He

The results demonstrate the strong relationship between the scale length and complexity of a design and the performance attainable. Roughly speaking, the number of elements required if a divertor is completely covered with one design type is $10^3$, $10^5$ and $10^6$ for the plate, T-tube and finger designs. Corresponding dimensions are roughly 100, 10 and 1 cm. The impact on reliability is unknown, as is the impact on the cost and required design margins. Since our
design point is expected to exceed 10 MW/m², peak average heat flux locally, where the plate-type divertor performance begins to degrade, we adopted a design that utilizes high-performance fingers only in the narrow region with high heat flux, and linear slot jets everywhere else. The pumping power is calculated by applying two different pressure drop calculations in the high flux and low flux zones.

The true time-dependent heat flux on the divertor surfaces consists of steady, unsteady and transient parts. The normal operating condition is expected to include a long period of steady heat flux (of the order of 1 s) followed by a short burst of high heat flux (of the order of 1 ms) due to edge localized modes (ELM’s) in the plasma, associated with the H-mode of operation [37]. Off-normal operating conditions that we may need to accommodate include disruptions and uncontrolled vertical displacement of the plasma. In both cases (ELM’s and disruptions), the thermal and mechanical effects are localized very close to the surface due to the short time scales, and therefore do not impact the time-averaged thermal hydraulic and structural analysis. More details on the localized effects of transients can be found elsewhere [33]. We also consider the possibility that startup and shutdown conditions in the divertor will be different than normal operating conditions, and will have time scales requiring analysis of the entire structure. These were not performed in the current study.

III.D Thermo-fluid analysis

A detailed design description of the ARIES-ACT divertor is presented in an accompanying paper [3] and earlier publications [10,30,38]. All the divertor concepts considered here (finger, plate-type, T-Tube, and integrated finger/plate) utilize impinging-jet cooling schemes to enhance heat transfer in the high heat flux zone. The 3D CFD code ANSYS CFX [28] was used as a computing tool to predict the thermo-fluid behavior and optimize the layout of multiple jets (or slot-jets), the numbers and size of the jets in order to improve thermal performance for pushing to higher surface heat flux while maintaining the maximum structural temperature and pumping power remain design limits. There are a few of turbulent models for the turbulent flow modeling in the ANSYS CFX, and some of the models are accurate and suitable only in special flow regimes. For the impinging-jet heat transfer, the standard k-ε, RNG k-ε and k-Ω turbulent models were recommended by ANSYS based on experiments and numerical simulation results by using CFX. [39] A comparison study of using the three turbulent flow models were made based on the plate-type divertor in order to understand and choose the turbulent flow model for our divertor design. Table V summarizes typical design parameters utilized in the CFD calculations. Inlet turbulent conditions and design parameters utilized in the simulation are assumed to be the same for all the three turbulent flow models.

Table VI summarizes thermo-fluid results of using different turbulent flow models. The results indicate that the k-Omega turbulent flow model predicts the highest temperature and the lowest pressure drop among the three turbulent models. However, the temperature calculated by the k-Omega model can be lowered to the same temperature as the one using the standard k-ε by slightly reducing the size of the slot-jets to increase the jet velocity and the HTC (heat transfer coefficient) while keeping identical pressure drop. After the adjustments were made, the temperature and pressure drop for using the three turbulent models are very consistent. The standard k-ε
A turbulent flow model was selected for performing the design iteration and optimization of both finger and plate-type divertor concepts because of fast convergence.

The design parameters of the finger divertor utilized in the CFD calculation are summarized in Table VII. Figure 18 illustrates an example of the HTC and temperature distributions of the finger divertor. The corresponding maximum HTC is $\sim 9.26 \times 10^4$ W/m$^2$-K at the heat exchange surface while the pumping power is less than 10\% of the removed divertor thermal power. The maximum temperature of the W armor is 2177 °C; the maximum temperature at the interface between the W armor and the thimble (underneath the W armor) is 1295 °C, which is within the 1300 °C re-crystallization limit assumed for the VM-W. The minimum temperature of the finger unit is 857 °C, which is above the 800 °C for avoiding the embrittlement for the W-alloy.

Table V. Design parameters of the plate-type divertor

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>He pressure, MPa</td>
<td>10</td>
</tr>
<tr>
<td>Peak heat flux, MW/m$^2$</td>
<td>9</td>
</tr>
<tr>
<td>Volumetric heating rate, MW/m$^3$</td>
<td>17.5</td>
</tr>
<tr>
<td>He inlet temperature, °C</td>
<td>700</td>
</tr>
<tr>
<td>He outlet temperature, °C</td>
<td>800</td>
</tr>
<tr>
<td>Nozzle width D, mm</td>
<td>0.5</td>
</tr>
<tr>
<td>Nozzle-to-wall distance H, mm</td>
<td>1.2</td>
</tr>
<tr>
<td>Length of the plate, cm</td>
<td>100</td>
</tr>
<tr>
<td>Width of the plate, cm</td>
<td>19.2</td>
</tr>
<tr>
<td>Height of the plate, cm</td>
<td>6</td>
</tr>
<tr>
<td>Width of the channel, cm</td>
<td>2</td>
</tr>
<tr>
<td>Number of the parallel channel per plate</td>
<td>8</td>
</tr>
</tbody>
</table>

Table VI. Comparison of different CFX turbulent flow models

<table>
<thead>
<tr>
<th>Turbulent flow model</th>
<th>Maximum W/He interface temperature, °C</th>
<th>Maximum W structure temperature, °C</th>
<th>Maximum W armor temperature, °C</th>
<th>Pressure drop, MPa</th>
</tr>
</thead>
<tbody>
<tr>
<td>Standard k-ε</td>
<td>1144</td>
<td>1290</td>
<td>1807</td>
<td>0.270</td>
</tr>
<tr>
<td>RNG k-ε</td>
<td>1199</td>
<td>1361</td>
<td>1875</td>
<td>0.255</td>
</tr>
<tr>
<td>k-Omega</td>
<td>1207</td>
<td>1365</td>
<td>1883</td>
<td>0.252</td>
</tr>
</tbody>
</table>

Table VII. Design parameters of the finger divertor

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>He pressure, MPa</td>
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</tr>
<tr>
<td>Peak heat flux, MW/m$^2$</td>
<td>14</td>
</tr>
<tr>
<td>Volumetric heating rate, MW/m$^3$</td>
<td>17.5</td>
</tr>
<tr>
<td>He inlet temperature, °C</td>
<td>700</td>
</tr>
<tr>
<td>He outlet temperature, °C</td>
<td>800</td>
</tr>
<tr>
<td>Nozzle diameter in middle, mm</td>
<td>1.0</td>
</tr>
<tr>
<td>Nozzle-to-wall distance, mm</td>
<td>1.2</td>
</tr>
<tr>
<td>Tile width over the flat, mm</td>
<td>23.0</td>
</tr>
</tbody>
</table>
Outer diameter of the thimble, mm 20
Outer diameter of the cylindrical ring, mm 18
Outer diameter of the cartridge, mm 13.6
Thickness of the front plate, mm 8.0

Figure. 18. CFX thermo-fluid results of the finger divertor, a. distribution of the heat transfer coefficient, b. temperature distribution
III.E Elastic-plastic thermomechanical analysis

Stress analysis is used to establish design features (such as shapes and dimensions), to determine permissible operating conditions and to verify that structural materials remain within their allowable design limits. Since high performance of the divertor has such a major impact on the overall power plant design, we include plastic deformations in our analysis. Thermal stress relaxation can allow higher heat fluxes than would be predicted by the use of purely elastic analysis using $3S_m$ limits of the ASME Code.

Both elastic and the plastic responses, including stresses and deformations, were calculated and optimized by performing design iterations using Pro/Engineer, CFX and ANSYS Workbench. We start with full 3D thermo-fluid analysis to calculate the material temperature and the fluid pressure drop, and then the nodal temperature and the pressure distribution at the interface of the fluid and the solid are mapped to the ANSYS structural model for the elastic-plastic analyses. The detailed design iteration and optimization can be found in Refs. [38] and [40]. Table VIII lists the materials database of the W used in thermo-mechanical analysis including $S_m$ and $3S_m$ values. The lack of suitable data for W alloys led us to use properties for pure W.

Table VIII. Materials database of W used for thermomechanics calculations [41]

<table>
<thead>
<tr>
<th>T, °C</th>
<th>Thermo-physical properties</th>
<th>Mechanical properties</th>
<th>$S_m$ and $3S_m$ values</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\rho$, kg/m$^3$</td>
<td>$k$, W/mK</td>
<td>$C_p$, J/kgK</td>
</tr>
<tr>
<td>20</td>
<td>19,298</td>
<td>173</td>
<td>129</td>
</tr>
<tr>
<td>500</td>
<td>19,178</td>
<td>133</td>
<td>144</td>
</tr>
<tr>
<td>700</td>
<td>19,125</td>
<td>122</td>
<td>150</td>
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<tr>
<td>900</td>
<td>19,070</td>
<td>114</td>
<td>155</td>
</tr>
<tr>
<td>1100</td>
<td>19,014</td>
<td>108</td>
<td>160</td>
</tr>
<tr>
<td>1300</td>
<td>18,956</td>
<td>104</td>
<td>165</td>
</tr>
<tr>
<td>1500</td>
<td>18,895</td>
<td>101</td>
<td>170</td>
</tr>
</tbody>
</table>

Due to resource limitations, we are not able to evaluate all of the structural design criteria used by the ITER team [41]. The primary design criterion we adopted for the nonlinear structural behavior of high temperature components is to limit the maximum plastic strain accumulated over the operating life below 50% of the uniform elongation. [42,43] The uniform elongation of W or W-alloy is highly dependent on the material temperature, and very limited data were found in the literature. For pure tungsten, the allowable plastic strain is ~0.8% at the temperature of 270 °C and 1% at the temperature of ~1200 °C.

A bilinear isotropic hardening material model was selected from the ANSYS built-in material models for the elastic-plastic analyses to simulate the material responses of both the elasticity and the plasticity. A linear elastic and ideal plastic model was assumed. Thermal and radiation induced creep were not included in the present study. The following mechanical boundary
conditions were applied: (1) no channel bending and in-plane back side, and (2) rotational periodic-symmetry condition in the plane of 0 and 60 degrees for the finger divertor, and plane symmetry condition in the middle of the cooling channel for the plate-type divertor. This is a conservative boundary condition since the entire surface at the back plate is restrained from free bending.

The results of elastic-plastic analysis indicate that the maximum principal plastic strain is ~0.13% in the W armor and 0.04% in the W-alloy thimble (well below the allowable plastic strain of 1.0% at a temperature of 1200 ºC). Both the finger and plate-type divertor designs meet the plastic strain criterion. The maximum surface heat flux that can be accommodated is ~14 MW/m² for the finger divertor and ~9 MW/m² for the plate-type divertor. More details on the analysis can be found in [38] and [40].

III.F Divertor transition joint

III.F.1 Design definition

An essential feature of the ARIES-ACT1 divertor is the avoidance of an interface between the steel coolant manifolding and tungsten structure within the high heat flux region. The large difference in the coefficient of thermal expansion (CTE) between steel (12.7x10⁻⁶/°C) and W-alloy (4.5x10⁻⁶/°C) can create high thermal stresses, which tends to limit performance and degrade reliability. Instead, a special interface joint has been designed outside the high heat flux region. Even in that location, high stresses can occur as a result of heat-up and cool-down from room temperature to warm standby to full operating temperature. Additionally, the temperature windows for ferritic steel and tungsten have little or no overlap, such that a ductile intermediate material like Ta can help isolate the two.

As shown in Figure 19, a Ta-alloy ring, with CTE intermediate between steel and tungsten (6.3x10⁻⁶/°C) was used to help soften the abrupt transition. Our analysis showed that pure Ta exhibits ratchetting, so alloys with higher yield strength and higher uniform elongation were considered, including T-111 (Ta-8W-2Hf) and Ta-2.5%W. We chose Ta-2.5%W to avoid activation concerns with Hf. Note, other solutions, such as graded material composition have been proposed to alleviate thermal stresses due to differential expansion, but our concept appears easier to fabricate and provides acceptable performance.

Figure 20 shows an expanded view of a cross section through the transition region. This design was adopted due to its simplicity. It relies heavily on the ability to braze steel to Ta and Ta to W. Standard brazing materials that are suitable for these material combinations include Cu and Ni, but further R&D will be needed to identify and qualify acceptable braze materials for the fusion environment. Issues related to fabrication of this design as well as alternative designs are described in more detail elsewhere [44, 45].
III.F.2 Analysis

Detailed 2D and 3D elastic-plastic thermo-mechanical analyses were conducted to study the behavior of this transition joint. The results consider various loading conditions including fabrication processes, warm and cold shutdown. Figure 21 depicts the temperature history from fabrication through operations. The material starts at 1050 °C in a stress free state when the braze is applied, is cooled to room temperature and then tempered at 750 °C. After tempering, the parts are cooled to room temperature for installation. Two operating scenarios were considered after initial operation at full temperature: warm shutdown and cold shutdown. Shutdown conditions may provide the most severe constraint on operation. Stress relaxation at high temperature can cause stress reversals that result in tensile loading following shutdown, where ductility is reduced. Peak stresses were monitored through all load cycles.

In order to allow for stress relaxation, we included plasticity in all of our calculations. We adopted an inelastic design requirement that limits the total accumulated plastic strain below 50% of the uniform elongation [42,43]. Earlier work has shown that restricting the joint to purely elastic behavior ($3S_m$) constrains the operating window too severely [46].
Figure 21. Joint temperature during fabrication and operating cycles

Our analysis of earlier joint designs indicated that the accumulation of plastic deformation would likely cause ratcheting [44]. Alternative designs were developed, leading to the final design concept depicted in Figure 20. This final design exhibited less plastic deformation in the joints as well as no ratcheting caused by warm and cold shutdown.

Figure 22 shows an example of a 2D elastic plastic evaluation of the plastic strains near the interface between the steel and Ta, using properties of Cu for the braze material (the actual braze material is still to be determined). Most of the strain accumulation occurs during fabrication for all the materials. The principal plastic strain remains steady after the first few cycles from operating temperature to cold shutdown, showing no tendency toward ratchetting. The principal plastic strain for all the materials are well below the design criterion. More details on both 2D and 3D analysis are published elsewhere [44, 45].

Figure 22. Maximum principal plastic strain due to fabrication and cycling to room temperature
IV. Structural Ring

The structural ring (SR) is a large helium-cooled double walled component measuring ~4 m wide and ~10 m high with ~30 cm overall thickness. Its primary functions are to secure the inboard and outboard blanket and divertor segments in position and to operate at a sufficiently high temperature to contribute heat to the power cycle. The current assumption is to operate with helium inlet and outlet temperatures of 650°C and 680°C respectively which would result in steel operating temperatures of 675-700°C. If necessary to ease materials requirements the operating temperature could be lowered to ~650°C maximum with a resulting loss of ~1% in the overall cycle efficiency.

Although each segment of the SR is removed as an integral part of the power core for a blanket and divertor replacement every 5 years, it is anticipated that the SR, being in a lower flux region, could be re-used and only replaced once if necessary during the machine lifetime. Essentially it is regarded as a plant lifetime component.

The neutron doses and helium generation levels in the SR have been calculated by El Guebaly et al. [4]. The requirement for periodic segment replacement necessitates assembly gaps which tend to close partially during operation and neutron streaming through the gaps contributes to the overall neutron dose [4]. For example, with an operating gap of 1 cm. between power core segments, the inboard section of the ring would sustain a 40 FPY lifetime dose of ~200 dpa combined with ~400 appm helium whereas for the outboard section these numbers are reduced to ~10 dpa and ~ 5 appm helium.

The requirement for prolonged operation at temperatures > 600°C essentially eliminates the current set of reduced activation ferritic-martensitic steels (F82H, EUROFER97, CLAM etc.) [47, 48] and it is therefore necessary to consider alternative structural alloys. In anticipation of the requirements for higher component operating temperatures in fusion energy systems beyond ITER, several international programs are pursuing the development of alternative materials such as ferritic-martensitic (FM) steels capable of operation in the range 600-650°C and nanostructured steels capable of operating at temperatures up to 750-800°C.

Driven by the needs of the fossil energy power generating industry to improve boiler efficiencies and to operate at increasingly higher water temperatures, global developments in the composition and microstructures of ferritic-martensitic steels coupled with advances in fabrication technologies have resulted in incremental improvements in high temperature operating capabilities as shown in (Fig. 23). In terms of this plot, the current set of RAFM steels for fusion applications are considered to be intermediate between the Gen1 and Gen2 materials. The possibility of applying the alloying principles embodied in the development of the Gen4 alloys to the development of a comparable set of reduced activation alloys which could be manufactured via conventional processing technologies, has been discussed by Klueh [49], who outlined a developmental approach based on a reduced activation FM steel (9Cr-1.0W-0.25V-0.1Ta-0.10C) with systematic variations in B11 and N concentrations within specific ranges derived from an analysis of the compositions of the Gen4 FM steels. These ideas were subsequently expanded by L. Tan et al. [50, 51] with a focus on controlling microstructures by means of compositional adjustment guided by computational thermodynamics and optimization of thermo-mechanical
treatments. Initial results have demonstrated that reduced activation alloys with tensile strength and high temperature creep properties exceeding those of the Gen3 alloys are attainable via this approach [51].

A second group of materials with attractive properties for SR applications are the oxide-dispersion-strengthened (ODS) alloys and the nano-structured ferritic alloys (NFAs) produced via mechanical alloying and consolidation by high temperature extrusion or HIPing. The incorporation of nano-scale oxide particles such as Y₂O₃ into the 9-11%Cr ferritic-martensitic steels leads to improved high temperature creep properties and a capacity for long-term operation at ~650°C (Fig. 24). A comprehensive review of this technology and its scale-up for fission reactor applications was recently presented by S. Ukai [52]; the current status of the development of reduced activation ODS alloys for fusion energy systems has been summarized by N. Baluc et al. [53]. In the RAFMs the ferrite-austenite transformation can be manipulated during processing to improve both strength and fracture toughness properties [54, 55]. However, the existence of this transformation limits the maximum temperature that could be sustained during a severe off-normal event (LOFA/LOCA) to ~800°C.

Increasing the Cr concentration into the 12-14 wt.% range has led to the development of a revolutionary set of new alloys designated as nanostructured ferritic alloys (NFAs). Precise control of impurity pick-up during mechanical alloying and refinements to the mechanical alloying and consolidation processes have led to the development of a transformation-free ferrite matrix containing ultra-high concentrations of Y-Ti-O rich nano-features coupled with a nano-scale grain structure. This unique combination imparts high tensile and creep strengths (Figs 23-24) combined with good toughness, outstanding thermal stability and the potential to mitigate radiation effects phenomena, including the damaging effects of helium at high temperatures [56, 57]. These materials open up the possibility for components operating temperatures up to ~800°C. Major impediments to the rapid development and deployment of these materials are the lack of manufacturing facilities capable of processing ~1000 kg quantities of mechanically-alloyed materials with sufficient homogeneity and reproducibility, and the challenging issues related to the development of joining technologies.
Fig. 23. Historical rate of improvement in the maximum operating temperature of four generations of ferritic martensitic steels for fossil-fired power plants. The reduced activation steels (RAFM’s) developed for fusion reactor applications fall between the Gen1 and Gen2 alloys (Based on data from [58]).

Fig. 24. Larson-Miller parameter plot illustrating the superior creep strength of two reduced activation ODS-ferritic/martensitic steels compared to RAFMs [55].
V. Vacuum Vessel

V.A Introduction

The main function of the vacuum vessel is to provide the high quality vacuum environment needed to initiate and maintain a fusion plasma. Owing to its hermeticity, it also acts as a primary containment barrier for radioactivity, including tritium, radioactive dust, and activated materials released in case of an accident. In some tokamak designs, such as ITER, the vessel is used as a primary structural element to which in-vessel components are attached and supported. In ARIES studies dating back as far as ARIES-RS, a full-sector maintenance approach was adopted in which in-vessel components are self-supporting, with gravity loads transferred vertically through the bottom of the vessel. The power core simply “sits” on its support structure within the vessel; this reduces loads on the vessel and simplifies the design considerably.

Two new features have been added to the ARIES-ACT1 vacuum vessel as compared with previous studies. (1) Due to concerns over tritium inventory and control, we decided to operate the vessel at an elevated temperature (>350°C) and avoid the use of water. Moderately low pressure (10 atm) He coolant is used in the space between the two face sheets for temperature control and to remove any tritium that permeates through the inner wall. (2) The need for a low-activation steel alloy that does not require post-weld heat treatment led us to adopt a 3Cr-3WV bainitic alloy. Austenitic steels suffer from high activation and radiation damage concerns (e.g. swelling or loss of fracture toughness) at fluence levels expected in a power plant. Ferritic-martensitic steels, such as those being developed for the first wall and blanket, contain microstructures that are disrupted by welding and would require post-weld heat treatment. Due to the size and complexity of the vessel, post-weld heat treatment was judged to be too difficult, if not impossible.

As a result of removing water (and thinning the structure as compared with earlier designs), the vessel alone is not able to provide adequate shielding of the superconducting magnets. In the present design, a water-bearing low-temperature shield is added between the vessel and the magnets, described elsewhere [4]. By eliminating the requirements on shielding and structural support of in-vessel components, the ARIES-ACT1 vessel becomes a thin (10 cm including two face sheets, internal ribs and He coolant), simple structure with modest loading conditions and relatively low cost.

V.B Design features and analysis

Figure 25 contains a CAD drawing of the entire vessel, including the large maintenance port extensions. Figure 26 shows a cutaway of one sector (1/16 of the vessel). The vessel is constructed of two thin face sheets with stiffening ribs located between them. The thickness of the ribs, their orientation, and other details of the geometry of the vessel were studied parametrically in order to find the thinnest structure capable of meeting the design requirements. We required the elastic stresses in all parts of the vessel, including the ribs, to remain below the yield strength.
Detailed 3D stress analysis was performed using ANSYS [28] in order to optimize the design and demonstrate that it meets its primary requirements. This includes static analysis of gravity and pressure loads, thermal stresses, off-normal pressurization in case of a helium loss-of-coolant accident in the power core, and electromagnetic loads due to disruptions. One full sector was analyzed, with symmetry conditions applied along the edges, free (frictionless) boundaries everywhere else, except a fully fixed boundary at the bottom (as shown in Figure 26). In the final design, the mesh included 377284 hexahedral, tetrahedral and wedge elements and 1983930 nodes. Five elements were used through the thickness of the walls and the ribs.
Table IX. Design parameters for the whole mixed ribbed structure VV

<table>
<thead>
<tr>
<th>Design Parameter</th>
<th>Size (cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Distance between vertical ribs</td>
<td>18-19</td>
</tr>
<tr>
<td>Distance between horizontal ribs</td>
<td>25</td>
</tr>
<tr>
<td>Rib cross section</td>
<td>4x4</td>
</tr>
<tr>
<td>Outer wall thickness</td>
<td>2</td>
</tr>
<tr>
<td>Inner wall thickness</td>
<td>4</td>
</tr>
<tr>
<td>Gap between inner and other sheets</td>
<td>4</td>
</tr>
<tr>
<td>Total vessel thickness</td>
<td>10</td>
</tr>
</tbody>
</table>

Figure 27. Stress distribution in the vacuum vessel under normal loads, highlighting (a) inner face sheets, (b) inside view with inner port wall removed, highlighting stresses in ribs, (c) top view with face sheets included, (d) top view with face sheets removed, highlighting stresses in ribs.

Table IX contains the final design parameters chosen for the vessel. Figure 27 shows an example of the stress distribution for a full sector. All walls and ribs remain within safe stress
limits, satisfying the \(3S_m\) ASME design criteria. In a complete LOCA, including the volume of He in the ex-vessel pipes and heat exchangers, the large volume of the vessel (including maintenance ports) prevents the overpressure from exceeding 1 atm. More details on the analysis are available elsewhere [59].

V.C Materials considerations

The design challenges and activation requirements for the ARIES-ACT1 vacuum vessel were evaluated by El-Guebaly et al. [60]. The requirements for the primary structural material may be summarized as follows: a) generates only low level radioactive waste, preferably Class A to reduce disposal cost, b) generates low levels of decay heat to ensure the vessel’s effectiveness as a heat sink during LOCA/LOFA events, c) develops a fracture-resistant microstructure with adequate strength and ductility during fabrication, d) maintains adequate fracture toughness, strength and uniform strain properties and creep resistance during the vessel lifetime of ~40 FPY, and e) exhibits high-quality weld properties without the need for a well-controlled post-weld heat treatment (PWHT). These requirements are more stringent than those pertaining to current fission reactor containment vessels or to the ITER vacuum vessel and necessitate a fresh look at potential structural materials.

An assessment of potential candidate alloys based on activation characteristics and mechanical properties was carried out by El-Guebaly et al. [60] and included the 8-9% Cr RAFS, low-alloy ferritic steels (such as Type 430), Mn-substituted austenitic stainless steels and the 2-3%Cr bainitic steels. It was concluded that the greatest potential to meet the requirements outlined above, resided in the new class of 3Cr-3WV bainitic steels. It was shown in order for the candidate steels to meet the requirements for classification as Class C low-level waste after 40 FPY of operation, it would be necessary to adopt the set of seventeen stringent impurity levels identified by Klueh et al. [61] based on careful chemical analysis of the lowest levels that have ever been achieved in large scale melting and fabrication processing of a variety of reduce activation and commercial steels. On this basis, the 3Cr-3WV bainitic steels were the only option that met the waste disposal criteria without presenting serious concerns regarding decay heat requirements [60]. These steels easily meet the general strength requirements in terms of \(S_m\), combined with excellent creep rupture properties in the 400-500°C operating temperature range.

Materials in this class were studied by the US Fusion Materials Program during the 1990’s as alternatives to the 8-9Cr RAFS for first wall and blanket applications [15, 62]. The subsequent realization of the potential applications for the 3Cr-3WV steels within the petroleum and chemical processing industries subsequently motivated the development and the production of 50-ton heats of several 3Cr-3WV alloys as higher strength replacements for the traditional 2⅓ Cr-1Mo steel (ASME T22). A significant outcome of this program was the submission of an ASME Code Case for one of the alloys, Grade 315, which contained 1.7wt.%W and 0.7wt.% Mo [63]. Earlier work in Japan on W-substituted low Cr alloy steels such as HCM2S resulted in the development of a code-qualified bainitic steel T23 (2.25 %Cr-0.25Mo-1.6W) and its subsequent commercialization and large-scale deployment in the chemical processing industry [64,65,66]. Subsequent work by Klueh et al. [67] demonstrated that the Mo-free, low-activation 3Cr-3WV steel developed superior stress rupture properties compared to the commercial alloys T22 and
T23 (Fig 28) and even compared favorably with those of the 9Cr-1Mo ferritic-martensitic steel T91. Another important finding of these programs was that the thermodynamics and kinetics of the bainite transformation in both the low activation and conventional versions of the 3Cr bainitic alloys enable high toughness microstructures to develop in the weld zone and heat affected zone without the need for a PWHT [63,64,66] (Fig 29).

In summary, the 3Cr-3WV class of bainitic steels represent a new class of low activation structural alloys with potential applications for fusion energy systems. The promising range of properties include, fabrication and tonnage-scale production by conventional metallurgical processing, good tensile strength, ductility and facture toughness, superior creep resistance and excellent welding behavior. Most importantly, with currently achievable levels of impurity control, they meet the requirements for Class C low-level waste while generating low decay heat. However, at present there is very little known regarding microstructural stability over projected plant lifetimes in the 400-500°C range or of the response of these steels to moderate levels of radiation damage at any temperatures in a degraded 14 Mev fusion neutron spectrum typical for a vacuum vessel.

![Figure 28. Larson-Miller parameter plot for reduced activation 3Cr-3WVTa bainitic steel compared with commercial low- Cr bainitic steels T22 and T23. [62]](image-url)
VI. Power conversion system

VI.A Power distribution

A primary requirement of the fusion power core is to transfer high-grade (i.e., high temperature) heat into a power conversion system for the production of electricity. Heat sources include surface heating from the plasma, volumetric nuclear heating and heat deposited into the coolants by friction or MHD losses. In ARIES-ACT1, every attempt was made to maximize the temperature into the power cycle for maximum thermal conversion efficiency. We have found in previous studies that high conversion efficiency is important to offset relatively high recirculating power and high capital cost in tokamak power plants. High conversion efficiency helps ease constraints on other parts of the system, opening up parameter space for a smaller machine with lower power density and less aggressive physics requirements.

We also strive to capture as much thermal power as is practical in the high-temperature components. The thickness of in-vessel components is determined in part by the desire to lose only a few percent of the total thermal power into components unable to contribute to the power cycle. As seen in Table XI [4], about 2.5% of the total available power is deposited in the vacuum vessel and low-temperature shield. Although the vacuum vessel in ARIES-ACT1 operates hot as compared with the vessel of ITER, it is not hot enough to make a significant impact on thermal conversion to electricity in the present study.
Table XI. Summary of thermal power in power core components (in MW) [4]

<table>
<thead>
<tr>
<th>Component</th>
<th>Nuclear heating</th>
<th>Surface heating</th>
<th>Pumping power</th>
<th>Total</th>
</tr>
</thead>
<tbody>
<tr>
<td>First wall and blanket</td>
<td>1347</td>
<td>130</td>
<td>4</td>
<td>1481</td>
</tr>
<tr>
<td>Stabilizing shells</td>
<td>52</td>
<td></td>
<td></td>
<td>52</td>
</tr>
<tr>
<td>Divertor plates and structure</td>
<td>176</td>
<td>275</td>
<td>9</td>
<td>460</td>
</tr>
<tr>
<td>Structural ring</td>
<td>76</td>
<td></td>
<td></td>
<td>76</td>
</tr>
<tr>
<td>Total at high temperature</td>
<td>1651</td>
<td>405</td>
<td>13</td>
<td>2069</td>
</tr>
<tr>
<td>Vacuum vessel</td>
<td>13</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Low temperature shield</td>
<td>42</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total at low temperature</td>
<td>55</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

VI.B Coolant conditions and heat exchangers

The adoption of He as coolant in the divertor and structural ring makes the power cycle more complicated than that of ARIES-AT, which used PbLi as the only high-temperature coolant. We need to combine the thermal power from several components into a single secondary power system. The inlet and outlet temperatures of each component must satisfy not only the material limits within that component, but also the overall power balance in the primary heat exchangers. In order to achieve this, we place the structural ring coolant first in series (due to the lower temperature limit of steel), followed by the divertor and then finally the blanket. Figure 30 provides a graphical depiction of the power flows and temperatures of the key components.

Figure 31 shows a plot of the temperature rise across the primary and secondary side of the heat exchanger. We assume the He and PbLi heat exchangers are separate components, although they could be easily integrated into one system. The heat exchanger temperature drop from He to He was assumed to be 50 °C, whereas the drop from PbLi to He could be lower (30 °C) due to better heat transfer in the heat exchanger. Inlet and outlet coolant temperatures must be chosen to account for the total power removed in each component, and constraints from multiple structural materials must be considered. This leads to mismatch at the divertor exit, as seen in Figure 31.
VI.C Cycle analysis

Several power cycles are available for use in a high-temperature system such as ARIES-ACT1. These include the helium Brayton cycle, steam Rankine cycle or supercritical CO₂ cycle. A closed helium Brayton cycle is well suited for the high temperature coolants used in ARIES-ACT1. This cycle has been applied to other ARIES power plant concepts in the past, including ARIES-ST and ARIES-AT [9,17]. It offers several distinct advantages, especially compared with steam cycles:
1. the inertness of He provides flexibility in materials selection
2. tritium permeation through the heat exchangers can more easily be recovered from He than from water
3. the absence of water in the power cycle avoids any possibility of a chemical reaction with hot PbLi
4. the absence of water also avoids a source of oxygen impurity in the He loops
5. the Brayton cycle offers performance advantages compared with a steam turbine cycle, especially in the temperature range anticipated for fusion blankets.

The characteristics of a Brayton cycle combined with a liquid metal fusion blanket have been described previously [68]. Thermal conversion efficiency approaching 60% can be attained with the 1000 °C outlet temperature from our PbLi-cooled SiC composite blanket. The exact value depends on the operating conditions of the power cycle, and the achievable efficiency of various components. An evaluation of near-term achievable performance parameters was performed previously [69], and we used those recommendations for our cycle analysis (see Table XII).

Table XII. Performance parameters used in the power cycle analysis

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of compression stages</td>
<td>3</td>
</tr>
<tr>
<td>Total compressor pressure ratio</td>
<td>2.8</td>
</tr>
<tr>
<td>Turbine inlet temperature (T₀)</td>
<td>1000 °C</td>
</tr>
<tr>
<td>Heat rejection temperature (Tₛ)</td>
<td>35 °C</td>
</tr>
<tr>
<td>Temperature ratio (T₀/Tₛ)</td>
<td>4.13</td>
</tr>
<tr>
<td>Sum of pressure ratios</td>
<td>5%</td>
</tr>
<tr>
<td>Pressure loss ratio (β)</td>
<td>1.02</td>
</tr>
<tr>
<td>Recuperator effectiveness</td>
<td>96%</td>
</tr>
<tr>
<td>Turbine efficiency</td>
<td>92%</td>
</tr>
<tr>
<td>Compressor efficiency</td>
<td>92%</td>
</tr>
<tr>
<td>Cycle gross efficiency</td>
<td>57.9%</td>
</tr>
</tbody>
</table>

Figure 32 shows a plot of the conversion efficiency and heat exchanger inlet temperature as a function of the total compression ratio. Note, this efficiency includes all of the pumping power requirements of the secondary components (turbine, heat exchangers, compressors), which are supplied directly from the turbine. However, it does not include pumping power on the primary side (within the power core). Electricity needs and energy deposited into the primary coolants are treated elsewhere in the system costing and power balance.

The optimum compression ratio results in an inlet temperature too high for the structural ring, so we chose to operate slightly off optimum, where the cycle efficiency is 57.9% and the primary heat exchanger secondary side inlet temperature is 600°C. This corresponds to a structural ring coolant inlet temperature of 650 °C. The figure also shows the effect of decreasing the inlet temperature to the structural ring. For example, a reduction of 50°C would decrease the gross cycle efficiency to around 57.1%, with a corresponding compression ratio of 3.4.
VII. Summary

The ARIES-ACT1 fusion power core provides a vision for an attractive commercial product with superior safety and environmental characteristics. The high temperature capability of the materials enables high thermal conversion efficiency, which reduces the size of the power core also provides flexibility in the choice of device parameters.

As compared with the earlier ARIES-AT design concept, we added features that are expected to make the design more robust, with minor impact on conversion efficiency and cost. These include an advanced steel backbone to the replacement sectors, simplified liquid metal flow paths and a new high-temperature vacuum vessel concept.

Many of the technologies adopted in this study require further R&D to translate concepts into reality. For the most part, the broad areas of R&D already have been identified and are being pursued in international fusion R&D programs. This study provides a specific set of goals, design features and operating parameters to focus future R&D.

Acknowledgements

Financial support was provided by the US Department of Energy under contract number DE-FG02-04ER54757.
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