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Fusion Energy Division

MATERIAL OPTIONS FOR A COMMERCIAL FUSION REACTOR FIRST WALL

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ABSTRACT

A study has been conducted to evaluate the potential of various materials for use as first walls in high-power-density commercial fusion reactors. Operating limits for each material were obtained based on a number of criteria, including maximum allowable structural temperatures, critical heat flux, ultimate tensile strength, and design-allowable stress. The results with water as a coolant indicate that a modified alloy similar to HT-9 may be a suitable candidate for low- and medium-power-density reactor first walls with neutron loads of up to 6 MW/m^2 . A vanadium or copper alloy must be used for high-power-density reactors. The neutron wall load limit for vanadium alloys is about 14 MW/m^2 , provided a suitable coating material is chosen. The extremely limited data base for radiation effects hinders any quantitative assessment of the limits for copper alloys.

Table 1. Important advantages and disadvantages of materials considered for high-power-density reactor

Name	Advantages	Disadvantages
PCA	<ul style="list-style-type: none"> • Well-developed technology with large data base • Low cost • Weldable 	<ul style="list-style-type: none"> • Poor thermal properties • High activation limits disposal • Limited radiation resistance
Mod HT-9	<ul style="list-style-type: none"> • Neutron swelling resistance superior to that of PCA • Liquid metal compatibility limits use to approximately 550°C with Li, 430°C with LiPb • Low long-term activation 	<ul style="list-style-type: none"> • DBTT will be shifted by irradiation with concern about operational temperature below DBTT
V-15 Cr-5 Ti	<ul style="list-style-type: none"> • Potential for use to approximately 750°C • Low long-term activation • Compatible with Li, LiPb 	<ul style="list-style-type: none"> • Reactivity with nonmetallic elements (C, N, O) • High tritium permeability impacts tritium containment and recovery
Titanium	<ul style="list-style-type: none"> • Technology available 	<ul style="list-style-type: none"> • Hydrogen embrittlement • High hydrogen solubility results in excessive tritium inventory
Mo/TZM	<ul style="list-style-type: none"> • Potential operating temperature above 1000°C • Excellent compatibility with liquid Li, LiPb 	<ul style="list-style-type: none"> • Difficult to fabricate • High cost • Hydrogen embrittlement • Irradiation shifts DBTT well above shutdown temperature • High activation limits disposal
Al/SiC	<ul style="list-style-type: none"> • High stiffness and strength-to-weight ratios • Extends useful temperature range for Al by $\approx 100^\circ\text{C}$ • Lower creep rate than Al • Weldable • Low-activation material 	<ul style="list-style-type: none"> • Maximum operational temperature is $\approx 250^\circ\text{C}$ • Very brittle under irradiation • Impurities will be dominant source of radioactivity

1. INTRODUCTION

Conceptual designs for fusion reactors involve devices that are generally physically large and, because of economy of scale, must produce a net electric power in the neighborhood of 1000 MW(e) to be viable. It may be possible to develop viable commercial reactor options that would reduce the reactor cost through increasing the plasma power density. This translates into higher first-wall surface heat and neutron loads than in the conventional fusion reactors. The choice of first-wall materials is limited, since the first wall must operate reliably in the severe thermal, radiation, stress, and electromagnetic environment of a fusion power core.

A study has been conducted to evaluate the potential of various materials for use as first walls in high-power-density commercial fusion reactors. Table 1 lists the materials considered and their advantages and disadvantages for this application. An austenitic stainless steel designated a prime candidate alloy (PCA) was investigated as a reference material.

Of the materials considered, modified HT-9 (Mod HT-9),¹ V-15 Cr-5 Ti, and two copper alloys were selected for further investigation. Nominal compositions of these materials are shown in Table 2.¹ Those of PCA and HT-9 are shown for comparison.

The alloy Mod HT-9 is a ferritic steel without the high-activation elements (molybdenum and nickel) found in unmodified HT-9 but with a similar structural strength. As shown in Table 2, tungsten replaces molybdenum in Mod HT-9 to maintain strength, and the levels of undesirable impurities (Cu, N, and Nb) are very low.

Of the various copper alloys that exhibit high thermal conductivity, the dispersion-stabilized (DS) copper alloy C15760 has the best high-temperature rupture strength, as illustrated in Fig. 1.² This alloy contains a very fine dispersion of Al_2O_3 particles. Its tensile strength increases with Al_2O_3 concentration, with little loss of ductility, and reaches about 550 MPa at a concentration of 2.7 vol %. Other copper alloys may also be useful in fusion reactor applications; for this study, a copper magnesium-zirconium-chromium (MZC) alloy was selected. This alloy has a creep strength comparable to (but slightly higher than) that of the alloy C15000, which is shown in Fig. 1.

Vanadium alloys are also considered to be promising for fusion reactor applications. The most complete data base available is that for V-15 Cr-5 Ti, which is used in this study.

The analysis method is described in Sect. 2. The issue of critical heat flux, which may limit the vanadium and copper alloy applications, is discussed in Sect. 3. Section 4 presents the operating limits for each material, based on a number of criteria. Section 5 discusses the effect of high-heat-flux first walls on thermal cycle efficiency of the power plant. Section 6 discusses additional limits on material selection. The conclusions are presented in Sect. 7.

Table 1 (continued)

Name	Advantages	Disadvantages
Graphite	<ul style="list-style-type: none"> • Low-activation material • High-temperature use 	<ul style="list-style-type: none"> • Chemical interactions with plasma • Swelling limits lifetime • In composite forms, differential swelling/shrinkage will lead to degradation at low fluences • Impurities will be dominant source of radioactivity
Copper alloy	<ul style="list-style-type: none"> • High thermal conductivity • Swells at a modest rate 	<ul style="list-style-type: none"> • Transmutation of copper to nickel and zinc reduces thermal conductivity • High radioactivity and afterheat • Sputters three times as much as PCA • Swelling peaks around 300 to 350°C

Table 2. Nominal composition of structural alloys

Element	Amount of element (wt %)					MZC copper	DS copper (C15760)
	PCA	HT-9	Mod HT-9	V-15	Cr-5 Ti		
C	0.005	0.2	0.15				
O							0.5
Mg						0.03	
Al							0.6
Si	0.5	0.35	0.2				
Ti	0.3	0.09	0.1	5.0			
V		0.3	0.3	80.0			
Cr	14.0	12.0	11.0	15.0		0.6	
Mn	2.0	0.55	0.53				
Fe	64.91	84.45	85.22				
Ni	16.00	0.5					
Mo	2.0	1.0					
Cu						99.27	98.9
Zr						0.10	
W		0.5	2.5

Fig. 1. Effect of test temperature on 100-h rupture strengths of DS copper and other high-conductivity copper alloys.

2. METHOD OF ANALYSIS

The reference first-wall configuration analyzed to find the operating limits for each material is shown in Fig. 2. The first wall is composed of channels with square cross sections, 3.8 mm on a side. The skin thickness on the top and bottom of the channel is set at 2.54 mm, and the distance between the coolant passage center lines is 6.35 mm. These dimensions are those used in the Blanket Comparison and Selection Study.³ This configuration is not suitable for pulse operation because of fatigue.

A 204-node, steady-state thermal model of the first wall was constructed using the finite difference heat transfer code TACO.⁴ The assumption of uniform heating along the channel length and the symmetry of the first wall permitted a model for a two-dimensional (2-D) analysis that consists of half of one channel. Plasma surface heating and nuclear heating of the bulk were input as fluxes on the front face and structure cross section, respectively. The rear face of the first wall was assumed to be adiabatic. In reality, there is some heat flux to the rear face from the blanket that tends to reduce the resultant thermal stress.

The temperatures calculated by the code were used in the stress analysis. The peak temperature was compared with the maximum allowable temperature of the corresponding material, which is set by helium embrittlement.⁵ Water has been selected as the coolant for this study. The outlet coolant temperature was assumed to be 320°C, with a coolant pressure of 15.2 MPa (2200 psia). For all materials except the copper alloy, the coolant velocity was assumed to be 7 m/s, which results in a film heat transfer coefficient of 5.4 W/cm²·K. The coolant velocity for the copper alloy was increased to 10 m/s in order to increase the critical heat flux limit. The film heat transfer coefficient in this case is about 7.5 W/cm²·K.

The NASTRAN finite element model⁶ was used to evaluate stress in the first wall. Mechanical stresses were calculated by representing the coolant pressure as a concentrated force applied to grid points on the inside surfaces of the channel. Thermal stresses were calculated by inputting the average temperature for each element obtained from the TACO code. Based on the symmetry of the coolant passage, only one-half of the passage and one-half of the vertical rib were modeled. The model was simplified by constructing 204 2-D elements. Effects of the third dimension were included by using effective material properties for the modulus of elasticity and Poisson's ratio. The constraints applied to the panel are shown in Fig. 2. The first wall should be designed to ensure that designated stress limits are not exceeded. Since at present there is no design stress code for fusion reactors, the ASME Boiler and Pressure Vessel Code⁵ was used as a guide to estimate the allowable stress and lifetime.

Fig. 2. First-wall configuration used in analysis.

3. CRITICAL HEAT FLUX ISSUES

The critical heat flux, beyond which tube burnout occurs, may be reached by the vanadium and copper alloys (due to their physical properties) before they reach other limits. Many experimental correlation data for water are available in literature dealing with critical heat flux limits in both subcooled and two-phase regions. The issue of compatibility of the materials with water has not been addressed here.

Almost all of the available data are for a uniformly heated tube or for rectangular channels. Although the heat flux is approximately uniform around the tube in a blanket, this is not the case in the first wall. Therefore, the first-wall critical heat flux could only be estimated based on the available correlation data. In the subcooled region, the result for most of the correlation for water shows little difference. The Westinghouse correlation⁷ was used here to estimate the critical heat flux $(q/A)_{crit}$.

$$(q/A)_{crit} = [(0.23 \times 10^6) + 0.094G](3 + 0.01\Delta T_S) \\ \times (0.435 + 1.23e^{-0.6293L/D})(1.7 - 1.4e^{-a}), \quad (1)$$

where

$$a = 0.532 \left(\frac{\rho_l}{\rho_v} \right)^{1/3} \left(\frac{h_{sat} - h_i}{h_{fg}} \right)^{3/4},$$

h_{sat} = saturation enthalpy of liquid,

h_i = inlet enthalpy,

h_{fg} = phase-change enthalpy,

ρ_l = saturation density of liquid,

ρ_v = saturation density of vapor,

G = mass flux,

L = tube length,

D = equivalent diameter,

ΔT_S = liquid subcooled level at the exit.

This equation correlates 95% of the water data to $\pm 20\%$ for the tube and rectangular channels for the following ranges of variables:

$$G = (1-35) \times 10^6 \text{ kg/h} \cdot \text{m}^2 [(0.2-8) \times 10^6 \text{ lb/h} \cdot \text{ft}^2]$$

$$P = 5.5 \text{ to } 18 \text{ MPa (800 to 2700 psi)}$$

$$L/D = 21 \text{ to } 365$$

$$h_i = 695 \text{ J/g (300 Btu/lb)}$$

$$\begin{aligned}\Delta T_s &= 0 \text{ to } 127^\circ\text{C} \text{ (0 to } 228^\circ\text{F)} \\ D &= 2.54 \text{ to } 13.7 \text{ mm (0.1 to 0.54 in.)} \\ (q/A)_{\text{crit}} &= 1.2 \text{ to } 12.5 \text{ MW/m}^2 \text{ [(0.4-4) } \times 10^6 \text{ Btu/lb}\cdot\text{ft}^2\text{]}\end{aligned}$$

The critical heat flux increases with coolant pressure, coolant velocity, and subcooled level. Figure 3 shows the variation of critical heat flux vs the ratio of tube length to diameter (L/D) for a coolant pressure of 15.2 MPa and a coolant velocity of 10 m/s. This plot was constructed by combining Eq. (1) with the energy equation for the tube. The critical heat flux is plotted for two different values of temperature drops across the first wall (or steam generator). The significance of this temperature drop is discussed in Sect. 5. The coolant exit temperature/exit subcooled level isotherms are also plotted in Fig. 3. According to this figure, lower L/D values are desirable to attain higher critical heat fluxes. Of course, fabrication could become a problem at very low values of L/D .

Only a few experimental results were found for nonuniform circumferential heat fluxes where one side of the tube was being heated. As Fig. 4 shows, the critical heat flux for this case is higher than the value for a uniformly heated tube at a given steam quality.⁸ This leads us to believe that experimental investigations of plasma/first-wall configurations are needed to establish the true critical heat flux limitations. Finally, it should be pointed out that there are methods by which the heat transfer rates can be improved with an acceptable penalty in pressure drop. This subject is beyond the scope of this report, and refs. 9 and 10 are suggested for more information.

Fig. 3. Critical heat flux vs tube length-to-diameter ratio.

Fig. 4. Comparison of critical heat flux for nonuniform and uniform circumferential heat flux.

4. RESULTS

Figures 5-7 present the operating limits for PCA, modified HT-9, and V-15 Cr-5 Ti, respectively, with water as a coolant, ignoring the first-wall sputtering (see Sect. 6 for sputtering effects). It should be noted that these plots are for a deuterium-tritium (DT) fusion reactor with a neutron wall load about four times the surface heat load. In these figures, the peak combined mechanical and thermal stresses are plotted against neutron wall loads and compared with design-allowable stress. The design-allowable for the total stress is taken as the ultimate strength of the material, from ref. 5. Peak operating temperatures of the first wall at some neutron wall loads are also shown on the combined stress curve. Thermal stresses are the main drivers in increasing peak stress intensities. The peak combined stress occurs on the plasma-side surface at the center line of the coolant channel. The maximum allowable operating temperature of each material (from ref. 5) is also shown in each figure. The first-wall lifetime was determined based on the maximum allowable displacements per atom (dpa) for the material⁵ (100 dpa for 25% CW PCA, 150 dpa for Mod HT-9, and 250 dpa for vanadium alloys) and a power plant availability of 0.75.

Fig. 5. First-wall combined stress and lifetime vs neutron wall load for PCA.

The primary stress in the first wall is a function of the configuration and coolant pressure, assumed to be the same for all material studied. At a coolant pressure of 15.2 MPa, the maximum primary stress is about 52 MPa, which should be lower than the general primary allowable stress S_{mt} . The general primary allowable stress at operating temperature and end-of-life radiation exposure, calculated in ref. 5, was 85 MPa for PCA, 115 MPa for Mod HT-9, and 80 MPa for the vanadium alloy.

The peak combined stress is also shown in Fig. 5 for the backside plate and occurs at the intersection of the vertical rib and the backside plate, where the weld joins the two plates. The peak combined stress is compared with the design-allowables for annealed conditions, assuming the weld joints have the annealed properties. The weld joint efficiency has been assumed to be 0.8. The result indicates that PCA could be used at up to a

Fig. 6. First-wall combined stress and lifetime vs neutron wall load for HT-9.

4-MW/m² neutron wall load or a 1-MW/m² surface heat load and is limited by the combined stress developed at the backside plate. However, if the whole channel has the CW material properties, then the neutron wall load can be increased to about 5.5 MW/m², and the limit will be the maximum allowable structural temperature of 550°C. At this limit the wall will last about two calendar years. These results are consistent with those presented for PCA in ref. 3. The permissible neutron wall load could be increased to about 6.5 MW/m² for Mod HT-9, as shown in Fig. 6, and the limit is the maximum allowable structural temperature, which is assumed to be the same as that of PCA. The corresponding lifetime will be around three calendar years.

For a higher neutron wall load, the material options would be limited to vanadium and copper alloys. A vanadium alloy, according to Fig. 7, could be used at a neutron wall load as high as 14.5 MW/m². This is limited again by the allowable structural temperature of 750°C. The first wall will last around two calendar years at this wall load. The

Fig. 7. First-wall combined stress and lifetime vs neutron wall load for V-15 Cr-5 Ti.

corresponding peak surface heat flux will be around 3.6 MW/m^2 . The critical heat flux will limit the L/D of the coolant channel for this first wall to 250–400 (see Fig. 3). This corresponds to a channel length on the order of 1 m, which might impose some fabrication problem.

Table 3 presents the effect of critical heat flux on peak first-wall and coolant temperature for a copper MZC alloy for 5- and 10-MW/m^2 surface heat loads. Due to the high thermal conductivity of the copper, about 12% of the incident thermal energy will be carried to the backside plate through the vertical ribs, thus reducing the heat flux from the plasma side to the coolant by 12%. Therefore, for a 5-MW/m^2 surface heat load, the maximum heat absorbed by the coolant (on the plasma side) will be 4.4 MW/m^2 . For this heat flux, the coolant temperature must be $\leq 290^\circ\text{C}$ to prevent tube burnout, according to Fig. 3. This is based on a 33°C temperature drop across the steam generator primary loop ΔT .

Table 3. Effect of critical heat flux on peak first wall and coolant temperatures for MZC copper

	5-MW/m ² thermal wall load, 4.4-MW/m ² heat flux to coolant		10-MW/m ² thermal wall load, 8.8-MW/m ² heat flux to coolant	
	$\Delta T = 33^{\circ}\text{C}$	$\Delta T = 20^{\circ}\text{C}$	$\Delta T = 33^{\circ}\text{C}$	$\Delta T = 20^{\circ}\text{C}$
Maximum outlet coolant tem- perature, ^a °C	290	305	255	280
Inlet temperature, °C	257	285	222	260
Maximum internal wall temperature, °C	356	359	355	355
Maximum surface temperature, °C	397	400	437	437

^aLimited by critical heat flux.

However, the maximum coolant temperature could be increased to 305°C if the temperature drop across the steam generator primary loop is 20°C. In other words, higher thermal-cycle efficiency is feasible, at the expense of a larger steam generator. In either case, the maximum first-wall temperature would be around 400°C. At this temperature, the peak combined mechanical and thermal stresses have been calculated to be 148 MPa, far below the ultimate strength of the MZC copper, which is 417 MPa for 84% CW, aged, unirradiated material. The peak primary stress is about 52 MPa, compared to a creep strength of 180 MPa for 80% CW, aged, unirradiated material for 10,000 h of operation at 400°C.¹¹ Higher tensile and creep strengths could be obtained by using DS copper. Its creep strength is compared to that of other copper alloys in Fig. 1. Variation of the strength with radiation is unknown. The copper alloys have an extremely limited radiation-effects data base, but some irradiation tests are under way. The results of these tests are crucial in determining the usefulness of copper alloys in high-heat-flux environments. If the swelling rate and the maximum dpa level are the same as those for PCA, the first-wall lifetime will be limited to only 0.5 calendar years at a 5-MW/m² surface wall load (approximately 20-MW/m² neutron wall load).

Obviously, the peak coolant temperature drops with increasing surface heat flux, as illustrated in Fig. 3 and Table 3. For a 10-MW/m² surface heat flux, the peak coolant temperature is 280°C, and the peak surface temperature increases to around 440°C for a 20°C drop across the steam generator primary loop. This temperature does not appear to

create any stress problem, but the first-wall lifetime would be expected to be extremely short, at least for DT-fueled reactors. On the other hand, the critical heat flux issue will limit L/D to between 80 and 130 (see Fig. 3). At this ratio, fabrication is expected to be a problem.

5. EFFECT OF A HIGH-HEAT-FLUX FIRST WALL ON THERMAL CYCLE EFFICIENCY OF POWER PLANTS

As mentioned before, the critical heat flux limits the coolant outlet temperature, which in turn affects the thermal efficiency of the power plant. In this section, the thermal efficiency of a power plant with surface heat loads of 5 and 10 MW/m² is compared to that of a typical pressurized-water reactor (PWR). Figure 8 shows a standard PWR heat exchanger with primary inlet pressure and temperature of 15.5 MPa (2250 psi) and 325°C, respectively, with a 33°C temperature drop across the primary loop. The average temperature difference between the primary and secondary loops is about 41°C. The outlet condition is a saturated steam at 6.9 MPa (1000 psi). Figure 9 shows a possible fusion reactor heat exchanger for a 5-MW/m² surface heat load. The top portion shows a temperature drop of 33°C across the primary loop, which is the standard PWR value. The bottom portion shows a 20°C temperature drop so that the inlet temperature to the primary loop can be increased from 290°C to 305°C.

For the same average temperature difference between primary and secondary loops as in the PWR reactor, the saturated outlet pressure would be reduced to 3.9 MPa (570 psi)

Fig. 8. Standard PWR heat exchanger pressure and temperatures.

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Fig. 9. Possible fusion reactor heat exchanger pressures and temperatures for 5-MW/m^2 surface heat load.

for the 33°C case and 5 MPa (725 psi) for the 20°C case. As a result, the power plant thermal efficiencies of both heat exchangers would be lower than that of the PWR reactor. However, if the average temperature difference between the primary and secondary loops is lowered to 20°C by increasing the heat exchanger size, then the outlet pressure increases to the PWR level for the 20°C case.

The thermal efficiencies corresponding to the saturated pressures have been computed using the ORCENT II computer code.¹² The result is shown in Table 4. For the same size heat exchanger as the PWR, the thermal efficiency drops from 35% to 31.8%, a drop of 9%. A trade-off study between the capital cost of the heat exchanger and the operating revenue will determine the optimum design point of the heat exchanger.

Figure 10 shows a possible fusion reactor heat exchanger for a 10-MW/m² surface heat load. The best operating point for this surface heat load corresponds to a saturated outlet pressure of 4.7 MPa (680 psi). This corresponds to a 20°C drop in the primary loop and an average 20°C drop between the primary and secondary loops. The corresponding thermal efficiency would be around 32.5%.

Table 4. Turbine generator net thermal efficiency vs saturated steam pressure

Pressure (MPa)	Net efficiency (%)
6.9	35.00
5.5	33.80
5	33.3
3.9	31.8

6. ADDITIONAL LIMITS ON MATERIAL SELECTION

The results presented so far neglect the effect of first-wall sputtering. The analysis was extended for a first wall coated with beryllium. The erosion rate was assumed to be 1 mm/year, consistent with the Blanket Comparison and Selection Study.⁵ It was also assumed that the erosion rate does not change with heat flux. The maximum operating temperature of 700°C for beryllium limits the allowable coating thickness. The following conclusions were drawn:

- For PCA and Mod HT-9, the lifetime remains limited by swelling rather than by erosion.
- For V-15Cr-5Ti, the lifetime is limited by swelling for neutron wall loads of up to 10 MW/m². At higher neutron wall loads, the lifetime drops sharply. It is estimated

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Fig. 10. Possible fusion reactor heat exchanger pressures and temperatures for 10-MW/m^2 surface heat load.

to be about 0.3 years at a 12-MW/m² wall load, set by erosion of the thin beryllium coating.

- For copper alloys, the lifetime remains limited by swelling.

The radioactive waste disposal of these materials has been studied.¹ The results indicate that PCA and the copper alloys would not qualify for near-surface burial. On the other hand, Mod HT-9 and the vanadium alloy will qualify for Class C near-surface burial. Other alloys might qualify for near-surface burial if they were diluted with inert or less active material from the reactor during waste packaging.

7. CONCLUSIONS

A study was conducted to evaluate the potential of various materials for application to high-power-density commercial fusion reactor first walls. Water was used as a coolant. The operating limits for each material were obtained based on a number of criteria, including maximum allowable structural temperature, critical heat flux, ultimate tensile strength, and design-allowable stress. The conclusions are as follows:

- Mod HT-9 may be a suitable candidate for low- and medium-power-density reactor first walls up to a 6-MW/m² neutron wall load.
- Vanadium or copper alloys must be used for high-power-density reactors.
- The neutron wall load limit for the vanadium alloy examined, V-15 Cr-5 Ti, is about 14 MW/m². With a suitable coating material, the first wall could last about two calendar years.
- Copper alloys seem to be most suitable for the high-heat-flux environments. The extremely limited radiation-effects data base hinders any quantitative assessment of the limits. Extensive data in this area are absolutely necessary before any concrete results can be achieved.
- Since it is argued that it is safer not to use water as a coolant for a first wall close to a liquid-metal breeding blanket, a similar study with other coolants, such as lithium-lead, should be undertaken.

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