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MATERIAL SELECTION FOR TFTR LIMITERS

BY

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Material Selection for TFTR Limiters

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The requirement for the material to be used as the first surface of limiters in TFTR are that it: 1) withstand a heat flux of 1 kw/cm^2 for a pulse length of 1.5s and a duty cycle of $1/200$ for 10^5 cycles, 2) withstand the thermal and electro-magnetic loads from 10^4 plasma current disruptions lasting about 200 μs , 3) generate impurities at a rate low enough to meet impurity control requirements (which depend on the atomic number of the material) for TFTR, and 4) have tritium retention characteristics consistent with tritium inventory requirements for TFTR. An extensive set of material tests using electron beams, neutral beams, and plasma bombardment have been carried out to identify materials which can meet the thermal requirements of 1 and 2 above. In most cases these tests have been combined with calculations of the expected thermo-mechanical

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response of the material to determine the reasons for the result observed. These studies have identified two major classes of materials which are acceptable, graphite which is: 1) coated to control hydrocarbon formation and 2) copper clad with a lower sputtering yield material.

1. INTRODUCTION

In the Tokamak Fusion Test Reactor (TFTR) the combination of high neutral beam power and long pulse length makes the selection of the material for the first surface of limiters particularly important. The importance of the material selection is further enhanced by the extensive impurity control effort being planned for TFTR, since the greater the success of the impurity control program the greater the fraction of the input power which goes to the limiter. The choice of limiter material is also closely coupled to the impurity control effort because the limiter can be one of the major sources of impurities. In addition, the electromagnetic forces due to the plasma current disruption induced eddy currents and the tritium inventory requirements for TFTR have an impact on the material selection.

The arrangement of limiters planned for TFTR is shown in Figure 1. The bumper limiter is toroidally symmetric and extends 60° above and below the midplane of the machine. It has an area of about 20m^2 and is intended to serve as the main limiter during

all of the high power neutral beam injection pulses. With 4 neutral beam operation, the requirements for the material on the plasma surface of the bumper limiter are: 1) withstand a heat flux of 1 kW/cm^2 for a pulse length of 1.5s and a duty cycle of 1/200 for 10^5 cycles, 2) withstand the thermal and electro-magnetic loads from 10^4 plasma disruptions lasting a minimum of 200 μs , 3) generate impurities at a rate consistent with impurity control requirements for TFTR, and 4) have tritium retention characteristics consistent with tritium inventory requirements for TFTR. The conceptual structure of the bumper limiter during this phase is a support plate covered with tiles. With ohmic heating only, the requirements for the movable limiters are very similar to those for the bumper limiter. With strong neutral beam heating, the time the plasma will be allowed to contact the movable limiter is to be limited to protect the first surface material. This is necessary because the area of the movable limiters is too small to take the heat load for the full pulse length without exceeding the capabilities of any known material.

An extensive set of experiments to select the material for the limiters has been performed under the impurity control program of the TFTR Flexibility Modification Project (TFM). These experiments included a study of base materials and a study of coatings and/or claddings to be applied to the base materials to improve their surface properties. The desired improvements in surface properties are reductions in sputtering, chemical

erosion, sublimation, and outgassing, as well as measurements of the hydrogen retention characteristics of the coatings/claddings. The studies in the second phase of this program are a joint effort by Princeton, Sandia National Laboratories in Albuquerque¹⁻⁶ and Livermore,⁷ and Argonne National Laboratory.⁸⁻¹⁰ Other data relating to limiter material selection are to be found in References 11 and 12.

The results of the experiments carried out at Princeton have been previously published,¹³⁻¹⁵ but a brief summary of those tests is included in Section 2 for completeness. The thermal and mechanical models used for interpretation of the data are presented in Section 3. The application of these models to the data are presented in Section 4. Section 5 contains recommendations for limiter material to be used in TFTR.

2. EXPERIMENTAL TECHNIQUES

The material tests were performed using three different types of heat source: 1) electron beam, 2) neutral beam, and 3) plasma bombardment in the PDX tokamak. Two types of tests using electron beams have been used to determine the suitability of all material candidates; thermal shock where the heat flux required to cause failure (melting or fracture) of the material from a single heat pulse is determined, and thermal fatigue where up to 5000 heating cycles are applied to the material at flux levels less than the shock limit to determine long term behavior under realistic conditions. The neutral beam exposures have been limited to those material candidates which performed well in the

electron beam tests. The neutral beam tests have been mostly of the thermal fatigue type but for less than 1000 cycles. Only one material (TiC on POCO Graphite) has been exposed to plasma bombardment for about 100 pulses (some with ohmic heating only, some with about 2 MW neutral beam heating). The results of these tests are reported in References 13 and 14. A list of the materials or material combinations which have been evaluated is shown in Table I.

3. ANALYSIS MODELS

The electron beam spot size ($\sim 1 \text{ cm}^2$) is such that the radius of the heated area is less than or equal to the thermal diffusion length (for the pulse lengths of interest, 0.5-1.0s) in many of the materials tested. This means that the heat diffuses radially out of the heated zone as well as into the depth of the material. This results in an apparent capability to withstand higher heat fluxes than would be the case if the heated area were much larger than the diffusion length (which is the case in TFTR). This effect was corrected for through the use of a two dimensional thermal transport model. Only two dimensions were necessary in the thermal model because the electron beam spot is nearly axisymmetric. In cylindrical coordinates the heat diffusion equation can be written as

$$\begin{aligned} \rho(T) c(T) \frac{dT}{dt} = k(T) & \left(\frac{2}{r} \frac{\partial T}{\partial r} + \frac{\partial^2 T}{\partial r^2} \right. \\ & + \frac{\partial^2 T}{\partial z^2} \Big) + \frac{\partial k}{\partial T} \left(\frac{T}{r} \frac{\partial T}{\partial r} \right. \\ & \left. \left. + \left(\frac{\partial T}{\partial r} \right)^2 + \left(\frac{\partial T}{\partial z} \right)^2 \right) \right) \end{aligned} \quad (1)$$

where k , ρ , and C are the thermal conductivity, density, and specific heat of the material, and the temperature variation of the properties has been included explicitly. The variation of thermal properties had to be included to properly fit the data. The material thermal properties were taken from References 16-27. The above equation was reduced to a set of finite difference equations and the resulting set of equations was solved using a third order Runge-Kutta scheme.²⁸ The electron beam heating as well as radiation and/or active cooling of the sample were included as boundary conditions in the manner suggested in Reference 29. The coated/cladded materials were analyzed by making the thermal properties depend on position as well as temperature and including additional boundary conditions at each material interface. The corrections resulting from the thermal analysis were as large as a factor of 2 in many cases. A one dimensional thermal transport model similar to the 2-D model was also developed for analysis of data from the neutral beam tests where the heat deposition is uniform over the typical 10cm $\times$ 10cm area of the test piece.

The mechanical response of the candidate materials has been analyzed using two different models. The first model assumes that the mechanical and thermal properties are independent of temperature and independent of position in the material. This model cannot be used for coated/cladded materials. The second model allows for the temperature variation of the mechanical and thermal properties as well as allowing them to vary spatially.

This model could be used for coated/cladded materials. Neither model includes any plastic effects.

If the material thermal properties are independent of temperature, and a plate of thickness $2h$ is heated by a heat flux F_0 on the face $z=h$ with no heat flow across $z = -h$ or the edges, and the starting temperature is 0, then the temperature is given by

$$T(z, t) = \frac{F_0 h}{12k} \left\{ \frac{24}{\pi^2} \frac{t}{\tau} + 3 \left(\frac{z}{h} \right)^2 + 6 \left(\frac{z}{h} \right) - 1 \right. \\ \left. - \frac{48}{\tau^2} \sum_{n=1}^{\infty} \frac{(-1)^n}{n^2} e^{-n^2 t / \tau} \cos \left(\frac{n\pi (z+h)}{2h} \right) \right\} \quad (2)$$

where $\tau = 4h^2 / \pi^2 \kappa$ and $\kappa = k / \rho c$ with k , ρ , and C as above.

If the length and width of the plate are much larger than its thickness, the heat flux uniform over the surface, and the material mechanical properties are independent of temperature, then the stress away from the edge is given³¹ by

$$\sigma_{xx} = \sigma_{yy} = \sigma_o(z) \quad , \quad (3)$$

$$\sigma_{zz} = \sigma_{xz} = \sigma_{xy} = \sigma_{yz} = 0 \quad , \quad (4)$$

with

$$\sigma_o(z) = - \frac{\alpha E}{1-\nu} \left[T - \frac{1}{2h} \int_{-h}^h T \, dz \right. \\ \left. - \frac{3z}{2h^3} \int_{-h}^h T \, z \, dz \right] \quad (5)$$

where α, E , and ν are the thermal expansion, elastic (Young's) modules, and Poisson's ratio. Evaluating the integrals using the temperature distribution above, results in the following:

$$\sigma_o(z, t) = \frac{\alpha E}{1-\nu} \frac{F_o h}{k} \left[-\frac{1}{4} \left(\frac{z}{h}\right)^2 + \frac{1}{12} + \frac{4}{\pi^2} \sum_{n=1}^{\infty} \frac{(-1)^n}{n^2} e^{-n^2 t/\tau} \left\{ \cos \frac{n\pi (z+h)}{2h} - \frac{6}{n^2 \pi^2} \frac{z}{h} \left((-1)^n - 1 \right) \right\} \right] \quad (6)$$

A second case where the face $z = -h$ is held at $T = 0$ has been previously evaluated³² with the results

$$T(z, t) = \frac{F_o h}{k} \left[\frac{z}{h} + 1 - \frac{16}{\pi^2} \sum_{n=0}^{\infty} \frac{(-1)^n}{(2n+1)^2} e^{-(2n+1)^2 t/\tau} \sin \left(\frac{(2n+1)\pi (z+h)}{4h} \right) \right] \quad (7)$$

where $\tau = 16h^2/\pi^2 K$ and the stress is given by

$$\sigma_o(z, t) = \frac{\alpha E}{1-\nu} \frac{F_o h}{k} \left[\frac{16}{\pi^2} \sum_{n=0}^{\infty} \frac{(-1)^n}{(2n+1)^2} e^{-(2n+1)^2 t/\tau} \left\{ \sin \left(\frac{(2n+1)\pi (z+h)}{4h} \right) - \frac{6z}{h} \left(\frac{(4(-1)^n - (2n+1)\pi)}{(2n+1)^2 \pi^2} \right) - \frac{2}{(2n+1)\pi} \right\} \right] \quad (8)$$

In both cases the stress is of the form

$$\sigma_o(z,t) = \frac{\alpha E}{1-\nu} \frac{F_o h}{k} f(t/\tau, Z) . \quad (9)$$

Figure 2 is a plot of f for the two cases as a function of t/τ for three different values of Z (front face $Z=h$, back face $Z=-h$, and center $Z=0$). Examination of the plots reveals that a maximum compressive stress occurs on the $Z=h$ face if $F_o > 0$ for both cases. This implies that a stress limited heat flux can be defined as

$$F_o^S = \frac{(1-\nu) k \sigma_{\max}}{\alpha E h f_{\max}(t/\tau, h)} \quad (10)$$

where $\sigma_{\max}$ is the compressive stress limit for the material. Examination of equations (2) and (7) reveals that the temperature distributions are of the form

$$T(z,t) = \frac{F_o h}{k} g(t/\tau, Z) . \quad (11)$$

The form of g is such that the maximum temperature always occurs at $Z=h$. This implies that a melting flux limit can be defined for the same surface and value of t/τ as the stress flux limit.

The melting flux limit is given by

$$F_o^M = \frac{k T_{\text{melt}}}{h g((t/\tau)_o, h)} \quad (12)$$

where $(t/\tau)_0$ is the value of t/τ for which the maximum compressive stress is achieved. The ratio of the stress flux limit to the melting flux limit is then

$$R = g(t/\tau)_0, h) (1-\nu) \sigma_{\max}/f_{\max} \alpha E T_{\text{melt}} \quad (13)$$

If $R > 1$ the material should melt before cracking whereas if $R < 1$ the material will crack before melting. For $R = 1$ a more detailed analysis is required.

The necessity to analyze coated/cladded materials plus the restrictions imposed by the assumptions required by the analytic solutions above prompted the development of a model which allowed the material properties to vary both spatially and thermally. The model consists of using the one dimensional thermal model described above to calculate the temperature distribution. Equation (5) is then recast in the following form

$$\begin{aligned} \sigma_o(Z) = & \frac{-1}{(1-\nu)} \left(\alpha(T) E(T) T \right. \\ & - \frac{1}{2h} \int_{-h}^h \alpha(T) E(T) T dz \\ & \left. - \frac{3z}{2h^3} \int_{-h}^h \alpha(T) E(T) T z dz \right) \end{aligned} \quad (14)$$

to include the variation of mechanical properties with temperature. The integrals are evaluated using Simpson Rule³³ and/or Newton's 3/8 Rule.³³ The variation of material properties in

coated/cladded materials is easily included by performing the numerical integrals over each layer and summing the results. The results are still only applicable to plates that are large compared to their thickness and uniformly heated. This is approximately true for the 10cm x 10cm x 1.3 cm tiles being considered for the first surface of TFTR limiters. Finite element codes must be used for more detailed analysis.

5. RESULTS AND DISCUSSION

A. Experimental Results

The results of the base materials study indicated graphite and copper as the most interesting candidates to use as substrates for the coatings/claddings study. The choice was based on the best combination of thermal shock and thermal fatigue behavior. There is some emphasis on the fatigue behavior because fatigue is becoming more important since maintenance is more difficult in a tritium machine. The atomic number (Z) of the material also played a role in this selection, i.e., low or medium Z materials were given strong preference over high Z materials.

The results of the coatings/claddings study showed TiC/POCO graphite and vanadium cladded copper to be the two most interesting combinations. The need to prevent melting of the copper under the vanadium led to the consideration of the three layer combinations discussed below. The inspection procedures for the explosion bonded material have also been improved so that areas of poor bonding are avoided.

Two new explosion cladded material samples have been subjected to electron beam tests recently. The thermal shock limit for a sample consisting of 0.051 cm Vanadium on 0.635 cm of Niobium on 1.27 cm of copper was determined to be 3.22 kW/cm^2 for an 0.5s pulse and 1.94 kW/cm^2 for a 1.5s pulse both with a 20C starting temperature. The thermal shock limit for a sample consisting of 0.051cm Vanadium on 0.089cm type 304 stainless steel on 1.27cm copper was determined to be 3.25 kW/cm^2 for an 0.5s pulse, and 2.19 kW/cm^2 for a 1.5s pulse both starting at 20C. In addition, a 1000 cycle fatigue test of the V/SS/Cu sample was carried out at 1.0 kW/cm^2 with 1.5s pulses. Slight cracking of the vanadium was observed. The damage was very similar to that observed on copper and vanadium copper samples under the same conditions.

A further exposure of the TiC/POCO graphite test limiter has been done on the PDX tokamak with about 2 MW of neutral beam heating lasting about 200 ms. The test limiter had been sitting in the shadow of the main titanium limiters on PDX for about 5 months before this test. A substantial number of disruptions occurred during the exposure. When the limiter was removed from PDX it was discovered that the TiC coating had been covered with a layer of another unknown material which the plasma had then cleaned off the surface. The removal of this dirt was probably the cause of the disruptions.

Figure 3 shows the limiter as it looked immediately after removal. The areas where the surface contamination was removed

are clearing visible. The TiC coating was undamaged. The power flow to the limiter during the neutral beam heating part of the discharge is estimated to be about 2 kW/cm^2 . The exposure lasted for about 50 pulses during this run.

B. Results of the thermal models

The results of using the one dimensional thermal analysis model on four candidate materials are shown in Figure 4. The starting temperature was 200°C and the heat flux 1 kW/cm^2 in each case. The effect of adding explosion bonded layers to the surface of copper is to increase the surface temperature of the composite because of the lower thermal conductivity of the added layers. It is evident that the addition of the stainless steel layer does cause a large temperature gradient across a thin layer which can cause stress problems, but the reason for including the steel layer was to prevent melting of the vanadium copper interface and this was accomplished. The temperature drop across the 20μ TiC coating on POCO graphite is only 6.5°C and no curves are shown for this case or the TiB_2 on POCO which is similar.

The one dimensional thermal model has also been used to calculate melting flux limits versus heat deposition time and flux limits based on impurity release due to vaporization of the material. The impurity flux limit is determined from the allowed impurity concentrations¹⁵ in TFTR, the power distribution on the limiter,³⁴ the vapor pressure of the material,^{18,19} and an assumed particle confinement time of 50 ms. The resulting temperature limits are: TiC - 1805°C , TiB_2 - 1952°C , C-2145C, and V - 1498°C . Figure 5

shows the flux limits as a function of heat pulse length assuming a starting temperature of 200C. The 200C starting temperature corresponds to a fluid cooled limiter. If the limiter is radiation cooled, then a 550C starting temperature is appropriate. This reduces the flux limit by about 0.3 kW/cm^2 for 1.5s pulse for all combinations except vanadium where the reduction is about 0.5 kW/cm^2 . This reduction in flux limit reduces the impurity flux limits to about the same level as the anticipated heat flux which strongly suggests a need to cool the limiter in TFTR.

C. Results of the Stress Models

Evaluation of the function f defined in Equations 6, 8 and 9 at the time of maximum stress gives the following results

$$f_{\max} = -0.2057 \text{ at } t/\tau = 0.5315 \text{ (from (6))}$$

$$f_{\max} = -0.2056 \text{ at } t/\tau = 0.1310 \text{ (from (8))}$$

The values for the two cases are nearly equal because the maximum stress occurs at a time which is short compared to the thermal time constant of the plate which means that the amount of cooling on the back of the plate has not affected the temperature distribution. However, the first case of no flow across the back of the plate is a better approximation to the TFTR design than the $T = 0$ case, and it was used to prepare Table II. The value of the function g (Equation 11) at $T/\tau = 0.5315$ is $g = 0.5327$.

Using the values of f and g above, the mechanical properties from References 35-38 with thermal properties from References 16-27, and taking the half thickness of the plate to be $h = 0.635 \text{ cm}$, the results of evaluating Equations 10, 12 and 13 are shown in Table II.

The values of material properties were taken at 20C. The compressive ultimate strength was used for $\sigma_{\max}$ for the graphites, carbides and diboride. The yield strength was used for $\sigma_{\max}$ for the metals because the model does not include plastic effects. The values of the yield strength were for fully hard material. If annealed values are used, then the values of R are at least a factor of 2 lower. The use of material properties at temperatures above 20C decreases the value of R slightly for all materials in the table except the graphites for which the ratio increases. The results indicate that the graphites are the only materials in the table which should not exhibit a stress type failure before melting occurs. Since $R = 0.88$ for POCO graphite, a more detailed analysis with fewer approximations is required to predict behavior of this material. The POCO graphite is being used because of its compatibility with certain coatings, not because it is necessarily the best graphite. The experimental results show that no fracture of graphite has occurred in tests conducted here. For the carbides, the prediction is for fracture before melting. This has been observed for all carbides tested. For the metals the results indicate the elastic limit is exceeded before the material melts. Since the material is in compression when it yields, a prediction of the exact mode of failure is beyond the capability of this model. The experimental results show that cracking does not occur before melting on a single pulse, but cyclic heating does cause cracking of all metals tested. The severity of the cracking is not correlated with the value of R . Thus, this simple model predicts the behavior of those materials having very little plastic behavior (graphites and carbides, etc.) very well, but it does not correlate well for materials exhibiting plasticity and fatigue effects.

The results of using Equation 14 to predict the stresses in coated and cladded materials are shown in Figures 6, 7 and 8. The heat flux used in the calculation was 1.0 kW/cm^2 and the pulse length was 1.5s. Figure 6 shows the stress distribution at the end of the heat pulse through the thickness of the plate for two substrate materials and two cladded materials. It is evident from this figure that the effect of adding the relatively thick layers of cladding to the copper substrate causes dramatic changes in the stress distribution in the copper as well as large stresses in the cladding. The effect on the POCO graphite is not shown but **is equally dramatic as** can be seen from comparison of Figure 7A, 7B, and 7C. The time history of the stress at the front, middle, and back of the substrate and the front and back of each coating/cladding layer are shown in Figures 7 and 8. Comparison of the time behavior of the substrate materials with that predicted by the simple model in Figure 2 shows that the agreement is quite good for copper and very poor for graphite. This is because the value of $(1-\nu)k/\alpha F$ is nearly constant with temperature for copper but varies substantially for graphite. The good agreement of the simple model prediction and the experimental results is because the magnitude of the stress predicted by the model is correct even though the time behavior is not. For the cladded materials and the bare copper substrate, the yield stress is exceeded in the copper at $t \leq 50 \text{ ms}$ if the value of annealed copper is used. This implies that plastic effects are important for pulse lengths greater than 50 ms. The behavior of the material is then determined by

fatigue effects. The observations show that cracking does occur during a fatigue test but not from a single pulse. For the coated material the compressive ultimate strength of the coating is exceeded at about 100 ms which would predict failure of the coating. Failure was observed for some TiB_2 samples but none of the TiC samples showed this behavior. This failure of the model is probably due to the fact that the coatings are slightly porous and not continuous as in the model. The calculations do agree well with 2-D finite element results³⁹ for the same material combinations and heat fluxes. It is apparent, however, that this type of calculation is not sufficient to predict the behavior of coated or clad samples, and their behavior must be determined by experiment.

D. Eddy Current Forces

One additional fact which strongly influences the selection of materials for limiters in TFTR is the effect of eddy currents on the material. For tiles of the same basic design, the relative response of a graphite tile as compared to a copper tile is the ratio of their electrical resistivities. For POCO graphite and OFHC copper, this ratio is $\rho_{POCO}/\rho_{Cu} = 759$ which means the time constant of a copper tile is almost 3 orders of magnitude longer than that of a similar graphite tile. This can have a substantial impact on the eddy current induced forces on the tile. In fact, the eddy current forces on copper tiles far exceed the allowables for all designs considered for TFTR limiters.

5. CONCLUSIONS

Based on the results of the tests performed thus far, we can conclude that both the TiC coated POCO graphite and the V/SS/Cu clad material can meet the thermal shock, thermal fatigue, and vapor pressure limits for TFTR. Both materials can also meet⁷ the tritium inventory requirements for TFTR. The data on the physical and/or chemical sputtering of the coated graphite is not yet complete and remains a potential source of difficulty for this material. We have yet to find a method of supporting the large disruption induced eddy current forces on the copper based tile while these forces are easily managed for the graphite tile. For this reason a graphite based tile is considered our prime candidate material for TFTR limiters. The exact choice of a coating for the graphite tile will depend on the results of the physical and chemical sputtering work and possibly the development of new coatings before limiters need to be installed in TFTR. Work is continuing on the clad copper tiles because they are potentially easier to cool for future long pulse machines and they have potential applications as neutral beam dumps where eddy current forces are not important.

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³⁹Budny, R., private communication.

TABLE I. Materials examined for use as TTR limiters.

Coating(s)/ Claddings	Thickness (mm)	Substrate	Production Method*	Bare Substrates
TiC	0.02	POCO Graphite	CVD	Pyrolitic Graphite
TiB ₂	0.03	POCO Graphite	CVD	Tungsten
B	0.02	POCO Graphite	CVD	W 2 ThO ₂
TiB ₂	0.10	Copper	PS	Molybdenum
V	0.5	Copper	EB	T2M
V/Mo	0.5/1.6	Copper	EB	ATJS Graphite
V/Nb	0.5/6.4	Copper	EB	Graphnol N3M Graphite
V/SS 304	0.5/0.9	Copper	EB	ATJ Graphite
VF ₂ /V	0.02/0.5	Copper	PB/EB	UT6-ST Graphite
Ti	0.5	Copper	EB	TaC-C Composite
B ₄ C	0.12	ATJ Graphite	PS	Carbon-Carbon Composite
C	0.02	Copper	IVD	OFHC Copper
C	0.01	Copper	GD	Tantalum
W	0.12	Copper	PS	Beryllium
Al	0.05	Copper	PS	70 Mo3DW
Ni	0.07	Copper	CP	Ta 10W
Fe	0.07	Copper	CP	Aluminum
Au	0.01	Copper	CP	Titanium
W/YO+ZrO	0.12/0.22	Copper	PS	Stainless Steel 304
YO+ZrO	0.21	Copper	PS	Silicon Carbide
Al ₂ O ₃ /YO+ZrO	0.02/0.21	Copper	PS	Boron Carbide
*Production Methods CVD - chemical vapor deposition PS - plasma spray EB - explosion bond PB - pack boriding IVD - ion vapor deposition GD - glow discharge (methane cracking) CP - contact plating				Titanium Carbide
				Zirconium Carbide
				Chromium Carbide
				Vanadium Carbide
				ZrB ₂ SiC

TABLE II. Calculated stress and melting flux limits for base materials.

Material	F_{O}^{S} (kW/cm ²)	F_{O}^{M} (kW/cm ²)	R
Tungsten	7.83	18.20	0.43
Molybdenum	4.83	11.78	0.41
Copper	3.90	13.00	0.30
UCAR Graphite Grade ATJS	66.88	20.64	3.24
UCAR Graphite Grade ATJ	22.00	13.72	1.60
POCO Graphite Grade AXF5Q	11.64	13.28	0.88
Niobium	1.60	4.00	0.40
Tantalum	1.65	5.16	0.32
Beryllium	1.93	8.04	0.24
Aluminum	1.13	4.72	0.24
Titanium	0.46	1.10	0.42
Stainless steel type 304	0.17	0.64	3.27
Inconel X-750	0.17	0.51	0.34
Silicon Carbide	5.22	10.54	0.50
Titanium Carbide	0.88	3.40	0.26
Titanium Diboride	0.74	2.32	0.32

FIGURE CAPTIONS

Fig. 1. A schematic cross-section of TFTR showing the planned location of limiters.

Fig. 2. The value of the stress function f versus t/τ for a) no heat flow across the face $z = -h$ and, b) $T = 0$ on the face $z = -h$.

Fig. 3. A photograph of the TiC coated POCO graphite limiter after exposure in PDX during neutral beam injection. The area where the surface contamination was removed is clearly seen in the center of the limiter.

Fig. 4. Temperature distribution versus time for a heat flux of 1 kW/cm^2 on a) vanadium on copper, b) copper, c) vanadium on stainless steel on copper, and d) POCO graphite. The curves correspond to front (1) and back (2) face for b and d; V front face (1), V/Cu interface (2), and Cu back face (3) in a; and V front face (1), V/SS interface (2), SS/cu interface (3), Cu back face (4) in c.

Fig. 5. Heat flux limit versus pulse length for several candidate limiter materials. The solid curves correspond to melting and the dashed curves are vapor pressure limits (see text). The starting temperature was 200°C .

Fig. 6. Thermal stress due to 1 kW/cm^2 heat flux for 1.5s on a) copper, b) POCO graphite, c) V/SS/Cu, d) V/Cu. The dimension z is through the thickness of the heated plate.

Fig. 7. The time dependance of the thermal stress in A) POCO graphite, B) TiC/POCO, and C) TiB_2 /POCO for 1 kW/cm^2 heat flux. The curves in A correspond to heated face (a), center (b) and back face (c). The curves in B correspond to TiC (a), POCO front face (d), POCO center (c), POCO back face (b). The curves in C correspond to TiB_2 (a), POCO front face (b), POCO center (c), POCO back face (d).

Fig. 8. Time dependance of the thermal stress in A) copper, B) vanadium/copper, and C) vanadium/stainless steel/copper for 1 kW/cm^2 heat flux. The curves in A correspond to front face (1), center (2), and back face (3). The curves in B correspond to vanadium front face (1), vanadium back face (2), copper front face (5), copper center (3), copper back face (4). The curves in C correspond to vanadium (1), stainless front face (5), stainless back face (4), copper front face and center (2), copper back face (3).

Fig. 1. (PPPL-803696)

Fig. 2. (PPPL-804727)

Fig. 3. (PPPL-804606)

Fig. 4. (PPPL-804735)

Fig. 5. (PPPL-804725)

FIG. 6. (PPPL-804726)

Fig. 7A. (PPPL-804728)

Fig. 7B. (PPPL-804733)

Fig. 7C. (PPPL-804732)

Fig. 8A. (PPPL-804731)

Fig. 8B. (PPPL-804729)

Fig. 8C. (PPPL-80473G)