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DESIGN OF A MICROWAVE CALORIMETER FOR THE MICROWAVE TOKAMAK EXPERIMENT*

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Abstract

The initial design of a microwave calorimeter for the Microwave Tokamak Experiment is presented. The design is optimized to measure the refraction and absorption of millimeter rf microwaves as they traverse the toroidal plasma of the Alcator C tokamak. Techniques utilized can be adapted for use in measuring high intensity pulsed output from a microwave device in an environment of ultra high vacuum, intense fields of ionizing and non-ionizing radiation and intense magnetic fields.

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1 INTRODUCTION

1.1 Requirements

It is desired to measure the absorption and refraction of millimeter microwaves used in an rf heating scheme in the Microwave Tokamak Experiment as they pass through the plasma. In the experiment microwaves from a free electron laser are launched into the Alcator C tokamak through a port from the outside of the torus. Significant refraction of the beam by the plasma is expected in the poloidal direction. The free electron laser produces short intense pulses of millimeter waves which can be focused into a relatively narrow beam for localized power deposition. The high intensity of the power flux makes the absorption process of the wave by the plasma non-linear. Ponderomotive effects become important in addition to the parametric instabilities which alter the plasma's absorption of the wave. One method of gaining insight into these processes is by measuring the amount and distribution of microwave energy from the FEL that is transmitted through the plasma.

Many of the methods conventionally used to measure microwave power fluxes at lower frequencies are of no use to us. The transmitted microwave energy is distributed across a sizeable arc of the poloidal circumference of the torus interior. Inside Alcator C only a small amount of space is available between the edge plasma and the tokamak wall, 2 centimeters when using the 16.5 cm radius poloidal limiters. The largest slot for access into the primary vacuum vessel is just over 4.2 cm wide. Any instrument inside of the primary vacuum vessel must be constructed entirely out of materials compatible with ultra high vacuum; specifically the materials must have outgassing rates comparable to that of stainless steel. All materials used in the device must be non-ferromagnetic so as not to perturb significantly the magnetic fields of the tokamak. Instruments inside of the tokamak are subjected to large heat loads from line radiation and Brehmstrahlung during a shot. Instruments must be resistant to damage from x-radiation as well as moderately resistant to neutron damage. Even behind the main limiters the edge plasma causes heating and sputtering. Alcator is cleaned daily with Taylor discharges. During

the process the plasma is allowed to expand out the vessel walls. This process is carried on for hours: it is designed to remove impurities from the machine and would quickly erode many components exposed to it, such as kapton, that would normally be useful in vacuum. The plasma radiates across a broad range of frequencies in the gigahertz range. Additionally magnetohydrodynamic instabilities radiates in the megahertz range of the electromagnetic spectrum. The power supplies for the magnet system create electronic noise at harmonics of 60 Hz. Of course the measurement of microwave power must be made in a strong magnetic field, dozens of kiogauss, that is rapidly changing during much of each shot. During a 140 Ghz pulse of the FEL parts of the microwave detection region are exposed to instantaneous power fluxes as high as 105 Mw/cm^2 and electron fields as high as 23 kv/mm.

1.2 Methods of Measurement:

Two variations on the calorimetric method are often employed to measure microwave power. One is to use a bolometer wire in a waveguide. Another is to place a thermistor at the end of a waveguide in such a way so that the microwave energy heats the semiconductor material of the thermistor in a predictable manner. At 140 Ghz the dimensions of the waveguide and associated components are sufficiently small that they are difficult to construct in practice; small errors in dimensions can lead to sizeable errors in component performance. Even the smallest thermistors available have dimensions of the same order as the free space wavelength of the radiation, making the thermistor absorption of the wave rather sensitive to local boundary conditions.

Crystal diode detectors are sensitive to the polarization of the radiation. At 140 Ghz the detectors are expensive and it would be extremely difficult to fit a few dozen of them into the space available.

Another variation on the calorimetric method is to heat a pad of absorber material with the microwaves and measure the temperature rise of the system either with an independent temperature measurement transducer or by allowing the slab to heat a cavity filled with a gas and measuring the pressure rise in the cavity. Some materials that

absorb well at lower frequencies, like graphite, become highly reflective at millimeter wavelengths.

Techniques to measure power fluxes in the far infrared are of little use to us. For example one method is to use a thin film of "black gold" on a substrate. The small size of the grains in the gold film make the surface more resistive, allowing it to absorb most of the infrared radiation. At millimeter wavelengths the films transmit most of the incident radiation, however. Alternating thin film layers of indium arsenide on gallium arsenide have a semiconductor resonance in the millimeter wave region. Because it is a semiconductor phenomena the properties of the resonance vary with the intensity of the strong magnetic field. Both of these methods rely upon a relatively delicate structure to absorb the microwave power and are thus unsuited to withstand the heating from a high power source.

The military has long been interested in using millimeter microwaves to see through objects, because most solid substances are poor absorbers of this frequency of radiation. The principal mechanism for absorption at millimeter wavelengths is usually dielectric relaxation. Although polar liquids have dielectric relaxation resonances in the 100's of Ghz frequency range, solids typically have dielectric relaxation resonance peaks in the low Ghz frequency range. Because millimeter waves can be focussed into narrow beams and are not scattered appreciably by a smoke screen they are excellent for use in radar that is difficult to evade. Very little unclassified data is available on the millimeter microwave absorption properties of vacuum compatible high loss materials.

In addition to all of the constraints previously mentioned the absorber material must reflect only a small fraction of the microwave energy and must absorb it through a small thickness in order to achieve the highest temperature rise, allowing us to get an accurate measurement from sensors in the noisy tokamak environment. Based on our own measurements of the microwave and vacuum properties of different types of silicon carbide I selected sintered alpha SiC produced by Standard Oil for use on the MTX calorimeter.

The calorimeter itself consists of 17 tiles of SiC arranged to cover an 108 degree arc on the poloidal circumference of the tokamak. The tiles are supported by a frame of stainless steel #304 which is doubly hinged to allow it to fit through a 4.2 cm wide slot. The frame and part of the tiles are shown in the C port in Fig. 1a. Thermistors are attached at various points of interest along the back of the tiles. Some of these will be far enough away from the region of peak microwave power deposition that they will function as reference sensors, measuring the heating of the tiles by radiation from the plasma. The attachment procedure involves vacuum deposition of layers of titanium, copper and silicon monoxide/silicon dioxide. Local limiters will be attached to the calorimeter frame, shielding the tiles from heating by the edge plasma. The calorimeter itself, along with virtual limiters on the tokamak, shield the electronics from plasma heating. Special ceramic coated wires carry the signal out of the tokamak.

The first of the following sections describes the analysis that forms the basis of the design of the calorimeter tiles. The next section describes the construction of the calorimeter tiles and evaluates the accuracy of measurement that will be possible with the detector itself. A following section describes the analysis that was used in the design of the calorimeter local limiters. The sensors and the electronic circuit used to measure the heating of the detectors is described in the following chapters. Lastly, a chapter on an alternative type of detector is included.

2 DESCRIPTION OF DETECTOR

The detector must be able to discriminate the heating from the FEL from background heating from the plasma, while being rugged enough to withstand the enormous instantaneous power flux from the FEL, so care must be taken in its design. Achievement of both the highest temperature rise and ratio of rise due to heating from the FEL to background heating dictates the need for an absorber material with both a low reflectivity and a high attenuation coefficient.

There is no material manufactured satisfying our criteria for which the microwave

properties at 140 Ghz have been published or are commercially available. The ten weeks allotted for completion of the design of this project allowed only a survey of commercially available materials. Information from various sources seemed to indicate that SiC would be the best material for this application. I measured the microwave properties of samples of SiC obtained from virtually all of the major manufacturers. The results are presented in Fig. 1b. EMA 9030 is a material consisting of mostly SiC and some binding agents. It is inexpensive and is a good absorber of microwaves but has a porosity of 40%, allowing it to absorb and retain much water vapor from the air. Figure 2 shows that even after many hours in vacuum the material does not meet our vacuum requirement that the outgassing rate be less than 1.0×10^{-8} Torr liters/cm² sec. Figure 3, a rarefied gas analysis of what outgasses from the EMA 9030, shows that water vapor is the dominant component. The Standard Oil sintered alpha SiC has favorable microwave properties for this application and has a porosity of at most 0.2%.

2.1 Far Infrared Properties on Inhomogeneous Materials

An inhomogeneous material, like SiC, consists of particles of different compositions. The effective electric and magnetic permeabilities are calculated by representing the particles as a sphere embedded in a dielectric matrix [15]. If the particle is ellipsoidal or pancake-shaped the results are different.

2.1.1 Electric Dipole Moment

Using a quasi-static model the electric dipole moment of the particle is calculated

$$\begin{aligned}\bar{P} &= \Omega \gamma_e \tilde{\epsilon}_o e^{i\omega t} \\ \gamma_e &= \frac{3\epsilon_o}{4\pi} \left(\frac{\epsilon_p - \epsilon_o}{\epsilon_p + 2\epsilon_o} \right)\end{aligned}$$

where

Ω = volume

γ_e = polarizability per unit volume

ϵ_p = particle complex $\hat{\epsilon}$

ϵ_o = dielectric complex $\hat{\epsilon}$

For long wavelengths ($c/w \gg a$) the applied fields appear spatially uniform. The particle, in this quasistatic limit, has only dipole moments. Since these are time varying current flows.

$$E_p = \frac{3\epsilon_o}{\epsilon_p + 2\epsilon_o} E_o e^{-i\omega t}$$

$$E_p = \text{electric field in particle}$$

2.1.2 Eddy Currents: Magnetic Dipole Moment

In the far infra-red region it turns out that the magnetic dipole (or eddy current) behavior is often even more important than the latter effect. This effect causes the medium to have a non-zero magnetization even though the constituents are nonmagnetic.

$$\vec{m} = \Omega \gamma_m \vec{H}_o e^{-i\omega t}$$

When vacuum surrounds the particle the complex magnetic permeability is given by solution of the boundary value problem of a conducting sphere in a spatially uniform, time varying $\vec{H}$ field.

$$\gamma_m = \frac{3 j_2(ka)}{8\pi j_0(ka)}$$

$$j_0(x) = \frac{\sin(x)}{x}$$

$$j_2(x) = \frac{3 \sin x - 3x \cos x - x^2 \sin x}{x^3}$$

$$\gamma_m = \frac{3}{8\pi} \left(\frac{3}{(ka)^2} - 1 - \frac{3}{ka} \cot(ka) \right)$$

k = wave vector

a = particle radius

The low frequency limit is reached when $ka = \sqrt{\epsilon_p} \frac{\omega a}{c} \ll 1$. This is different from the long wavelength criterion $\omega a/c \ll 1$. Because ϵ_p can be large, a particle may be in

the long-wavelength limit but not in the low frequency limit. (The low frequency limit requires a $\ll$ skin depth). Although the currents inside the particle aren't uniform (at low frequency they correspond to a uniformly charged sphere rotating with angular velocity ω) the field outside are those of a magnetic dipole of moment $\vec{m}$. Thus eddy current effects have caused the particle to have a definite dipole moment m and this moment is equivalent to that of a sphere with permeability p in a uniform external field. The permeability is complex.

$$\mu_p = 1 \pm \frac{4\pi\gamma_m}{1 - \frac{4\pi}{\epsilon}\gamma_m}$$

2.1.3 Absorption by a thin conducting dielectric film:

For a wave incident upon an interface between two media the reflect and transmitted waves for the s and p polarizations are as follows [16]

$$\begin{aligned} E'_{1i} &= \hat{r}_{12i} E_{1i} \\ E'_{2i} &= \hat{t}_{12i} E_{1i} \\ \hat{r}_{12s} &= \frac{\hat{n}_1 \cos \hat{\theta}_1 - \hat{n}_2 \cos \hat{\theta}_2}{\hat{n}_1 \cos \hat{\theta}_1 + \hat{n}_2 \cos \hat{\theta}_2} & \hat{t}_{12s} &= \frac{2\hat{n}_1 \cos \hat{\theta}_1}{\hat{n}_1 \cos \hat{\theta}_1 + \hat{n}_2 \cos \hat{\theta}_2} \\ \hat{r}_{12p} &= \frac{\hat{n}_1 \cos \hat{\theta}_1 - \hat{n}_2 \cos \hat{\theta}_2}{\hat{n}_1 \cos \hat{\theta}_1 - \hat{n}_2 \cos \hat{\theta}_2} & \hat{t}_{12p} &= \frac{2\hat{n}_1 \cos \hat{\theta}_1}{\hat{n}_1 \cos \hat{\theta}_1 - \hat{n}_2 \cos \hat{\theta}_2} \end{aligned}$$

Here $\hat{n} = \sqrt{\epsilon\mu}$ where ϵ and μ are the electric and magnetic permeabilities of the material respectively. When the material is a conductor both $\hat{n}$ and $\hat{\theta}$ take on complex values.

For a plane wave incident upon a thin film of dielectric material the reflected wave is the sum of the wave reflected from the front and back faces:

$$\begin{aligned} \hat{r}_1 &= \hat{r}_{12i} + \hat{t}_{21i}\hat{r}_{21i}e^{i\hat{\beta}} + \hat{t}_{12i}\hat{r}_{21i}\hat{r}_{21i}\hat{t}_{21i}e^{i\hat{\beta}} + \dots = \frac{\hat{r}_{12i} + \hat{r}_{21i}e^{i\hat{\beta}}}{1 + \hat{r}_{12i}\hat{r}_{21i}e^{i\hat{\beta}}} \\ R_1 &= \hat{r}_1\hat{r}_1^* \\ \hat{\beta} &\equiv 2d\frac{\omega}{c}(p - iq) \end{aligned}$$

If the back plane is a metal and the radiation is in the far infrared or below no significant error results in the calculation of reflected power if the reflection coefficient at

the dielectric/metal interface is set equal to -1. As an example of the application of this theory, the reflectance of the s polarization of a thin layer of conducting dielectric with a metal backing is:

$$\begin{aligned}
 p &= \sqrt{\frac{1}{2} \left[(K_r - K_1 \sin^2 \theta_1) + \sqrt{(k_r - K_1 \sin^2 \theta_1)^2 - K_1^2} \right]} \\
 q &= \sqrt{\frac{1}{2} \left[(K_r - K_1 \sin^2 \theta_1) - \sqrt{(k_r - K_1 \sin^2 \theta_1)^2 - K_1^2} \right]} \\
 R_s &= \cos^2 \theta_1 - 2p \cos \theta_1 + p^2 + q^2 + [\cos^2 \theta_1 + 2p \cos \theta_1 - p^2 + q^2] e^{-4d \frac{w}{c} p} + \\
 &\quad - [\cos^2 \theta_1 + 2iq \cos \theta_1 - p^2 - q^2] e^{-12d \frac{w}{c} p} - 2d \frac{w}{c} q \\
 &\quad - [\cos^2 \theta_1 + 2iq \cos \theta_1 - p^2 - q^2] e^{-12d \frac{w}{c} p} - 2d \frac{w}{c} q \\
 &= \cos^2 \theta_1 - 2p \cos \theta_1 + p^2 + q^2 + [\cos^2 \theta_1 + 2p \cos \theta_1 - p^2 + q^2] e^{-4d \frac{w}{c} q} + \\
 &\quad - [\cos^2 \theta_1 + 2iq \cos \theta_1 - p^2 - q^2] e^{-12d \frac{w}{c} p} - 2d \frac{w}{c} q \\
 &\quad - [\cos^2 \theta_1 + 2iq \cos \theta_1 - p^2 - q^2] e^{-12d \frac{w}{c} p} - 2d \frac{w}{c} q
 \end{aligned}$$

Inspection of this equation reveals that even though a film of dielectric material may be thick enough to absorb most of the incident energy the fields reflected from the two faces may still couple and interfere significantly. This is associated with the terms in $e^{(\pm i 2d \frac{w}{c} p - 2d \frac{w}{c} q)}$. Figures 4 through 6 show the fraction of incident energy absorbed by Standard Oil sintered alpha SiC as a function of thickness for the different polarizations at different angles of incidence. Figure 7 shows the fractional absorption of 140 GHz radiation by macor as a function of thickness at normal incidence. Clearly use of a thin film of macor would require not only very accurate knowledge of the dielectric constant of the actual sample of the material but precision machining tolerances as well. At these frequencies the microwave properties of different samples of the same material can vary significantly from batch to batch. In addition since the wave strikes the calorimeter at different angles and intensities at different places, the destructive interference from multiple reflections of a plane wave in a low loss material like macor would be different from the interference produced by the refracted and attenuated FEL

wave. The absorption at a locality might vary significantly with the changes in plasma conditions from shot to shot. Thus it is desirable to have a film that is sufficiently optically thick to the radiation that interference effects are small. The Torch ray tracing code [1] predicts that the angle of incidence of refracted microwave rays with the vector normal to the tile face will be less than or equal to 20 degrees over most of the calorimeter. Figures 4 and 5 show that for the s polarization the fraction of energy absorbed changes little with angle of incidence over the range. For the p polarization the change is greater but the overall variation is still small.

2.2 Heat Distribution in the Absorber:

The local temperature variation through the thickness of the film produced instantaneously by the FEL is given approximately by

$$\Delta T_0 = (1 - R) \frac{\alpha I_0 \tau}{\rho c_p} e^{-\alpha z}$$

where

R = reflectance

α = attenuation coefficient

τ = pulse length

I_0 = incident power flux

ρ = density

c_p = specific heat

The temperature equilibrates much more rapidly across the small thickness of a tile than across other dimensions, allowing a measure of localized power deposition. The local variation of temperature through the thickness of a tile as a function of time is:

$$\Delta T_{(z,t)} = \frac{I_0 \tau}{\ell \rho c_p} (1 - e^{-\alpha \ell}) - \sum_{n=1}^{\infty} \frac{2\alpha^2 I_0 \tau}{\rho c_p \ell} \frac{1 - e^{-\alpha \ell} (-1)^n}{\alpha^2 + \frac{n^2 \pi^2}{\ell^2}} 25 \left(\frac{n\pi 2}{\ell} \right)$$

ℓ = thickness $0 \leq z \leq \ell$

The fundamental mode time constant is a good measure of the length of time necessary for equilibration of the temperature and is equal to 15.1 msec for a Standard Oil Hexalloy sintered alpha title 3.0 mm thick.

2.3 Assessment of Stresses in the Tiles

The nonuniform heating of the hard ceramic SiC by the FEL creates sizeable thermal stresses in the absorber tiles. By analyzing the stress in an infinite thin plate with temperature variation through the thickness alone, one can estimate the stresses created in a calorimeter tile. Using the latter exponential temperature variation, with a maximum temperature at the front face, uniformly in the directions of both coordinate axes in the plane, due to symmetry. The stress is assumed to be planar because the plate is thin. The maximum tensile stress occurs along the back face. When the plate is made thick enough the plane of maximum tensile stress moves inside the plate and the plane at the back is in compression.

St. Venant's principle states that in a thin plate the stress at location more than a few thicknesses from the edges is determined only by the total forces and moments acting on the edges, not on the detailed nature of the distribution of the force. All of the stress analysis on the calorimeter tiles assumes all of the edges are free: the actual tiles are pressed against the frame by a spring loaded mechanism, leaving the edges force free.

When the temperature varies in the other dimensions as well the solution becomes much more involved. The assumption that the stress in the thickness (z direction) is small is still valid. The three dimensional temperature distribution is expanded into a polynomial in the z direction multiplied by coefficients which are functions of x and y. $X_1 = x; X_2 = y; X_3 = z$.

$$\alpha E T_{(x,y,z)} = \sum_{n=0}^{\infty} T^{(2n)}(x,y) Z^{(2n)} + T^{(2n+1)}(x,y) Z^{(2n+1)}$$

$$\sigma_{ij}(x,y,z) = \sum_{n=0}^{\infty} \sigma_{ij}^{(2n)}(x,y) Z^{(2n)} + \sigma_{ij}^{(2n+1)}(x,y) Z^{(2n+1)} \quad i,j \neq 3$$

$$\sigma_{i3}(x,y,z) = \sum_{n=0}^{\infty} \sigma_{i3}^{(2n)}(x,y) (Z^{2n+1} - b^{2n}Z) + \sigma_{i3}^{(2n+1)}(x,y) (Z^{2n+2} - b^{2n}Z^2) \quad i \neq 3$$

$$\sigma_{33}(x,y,z) = \sum_{n=0}^{\infty} \sigma_{33}^{(2n)}(x,y) (X_3^{2n} - hb^{2n-2}Z^2 + (n-1)b^{2n}) - \sigma_{33}^{(2n+1)}(x,y) (Z^{2n+1} - nb^{2n-2}Z^2 - (n-1)Zb^{2n})$$

b = plate thickness

α = thermal expansion coefficient

E = Young's modulus

A steady state 3 dimensional stress solution must satisfy 3 equilibrium equation and 6 compatibility equations. Substituting the series solutions into these equation we obtain

First equilibrium equations:

$$\sum_{n=0}^{\infty} \left\{ \left[\sigma_{11,1}^{(2n)} + \sigma_{12,1}^{(2n)} + (2n+1)\sigma_{13}^{(2n)} \right] X_3^{2n} - \sigma_{13}^{(2n)} b^{2n} \right\} = 0$$

$$\sum_{n=0}^{\infty} \left\{ \left[\sigma_{11,1}^{(2n+1)} + \sigma_{12,1}^{(2n+1)} + (2n+1)\sigma_{13}^{(2n+1)} \right] X_3^{2n+1} - \sigma_{13}^{(2n+1)} b^{2n} X_3 \right\} = 0$$

Second equilibrium equations

$$\sum_{n=0}^{\infty} \left\{ \left[\sigma_{12,1}^{(2n)} + \sigma_{22,2}^{(2n)} + (2n+1)\sigma_{23}^{(2n)} \right] X_3^{2n-1} - \sigma_{23}^{(2n)} b^{2n} \right\} = 0$$

$$\sum_{n=0}^{\infty} \left\{ \left[\sigma_{12,1}^{(2n+1)} + \sigma_{22,2}^{(2n+1)} + (2n+2)\sigma_{23}^{(2n+1)} \right] X_3^{2n+1} - \sigma_{23}^{(2n+1)} b^{2n} X_3 \right\} = 0$$

Third equilibrium equations

$$\sum_{n=0}^{\infty} \left\{ \left[\left(\sigma_{13,1}^{(2n)} + \sigma_{23,2}^{(2n)} \right) X_3^2 + 2n\sigma_{33}^{(2n)} \right] X_3^{2n+1} - \left[\sigma_{13,1}^{(2n)} b^{2n} - \sigma_{23,2}^{(2n)} b^2 + 2h\sigma_{33}^{(2n)} X_3^3 \right] \right\} = 0$$

$$\sum_{n=0}^{\infty} \left\{ \left[\left(\sigma_{13,1}^{(2n)} + \sigma_{23,2}^{(2n)} \right) X_3^2 + (2n+1)\sigma_{33}^{(2n+1)} \right] X_3^{2n-1} - \left[\sigma_{13,1}^{(2n-1)} b^{2n} + \sigma_{23,2}^{(2n+1)} b^2 + 3nb^{2n-2} X_3^2 \sigma_{33}^{(2n-1)} \right] - (n-1)b^{2n} \sigma_{33}^{(2n+1)} \right\} = 0$$

$$H + \alpha ET = \sum_{n=0}^{\infty} \left\{ \sigma_{11}(2n) + \sigma_{22}^{(2n)} + \sigma_{33}^{(2n)} + T^{2n} \right\} X_3^{2n} - \left[nx_3^2 - (n-1)b^2 \right] b^{2n-2} \sigma_{33}^{(2n)} + \left\{ \sigma_{11}^{(2n+1)} - \sigma_{22}^{(2n+1)} - \sigma_{33}^{(2n+1)} - T^{2n+1} \right\} X_3^{2n-1} - \left[nX_3^3 b^{2n-2} - (n-1)X_3 b^{2n} \right] \sigma_{33}^{(2n+1)}$$

First equilibrium equations

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{11}^{(2n)} - \frac{T^{(2n)}}{1-2} \right) \right] X_3^{2n} + \right. \\ \left. \left[(1+2) \left(\sigma_{11}^{(2n)} - \frac{T^{(2n)}}{1-2} \right) (2n)(2n-1) \right] X_3^{2n-2} \right\} + (M + \alpha ET)_{,11} + \\ \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{11}^{(2n+1)} + \frac{T^{(2n+1)}}{1-2} \right) \right] X_3^{(2n+1)} + \right. \\ \left. \left[(1+2) \left(\sigma_{11}^{(2n+1)} + \frac{T^{(2n+1)}}{1-2} \right) (2n+1)(2n) \right] X_3^{2n-1} \right\} = 0 \\ \left(\nabla_1^2 \equiv \frac{\partial^2}{\partial X_1^2} + \frac{\partial^2}{\partial X_2^2} \right) \end{aligned}$$

Second equilibrium equation

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{22}^{(2n)} + \frac{T^{(2n)}}{1-2} \right) \right] X_3^{2n} + \left[(1+2) \left(\sigma_{22}^{(2n)} + \frac{T^{(2n)}}{1-2} \right) (2n)(2n-1) \right] X_3^{2n-2} \right\} + \\ \left[(1+2) \nabla_1^2 \left(\sigma_{22}^{(2n+1)} - \frac{T^{(2n+1)}}{1-2} \right) \right] X_3^{2n+1} + \\ (1-2) \left(\sigma_{22}^{(2n+1)} - \frac{T^{(2n+1)}}{1-2} \right) X_3^{2n+1} (2n+1)(2n) X_3^{2n-1} (M + \alpha ET)_{,22} = 0 \end{aligned}$$

Third compatibility equation

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \sigma_{12}^{(2n)} \left[(1-2) \sigma_{12}^{(2n)} (2n)(2n-1) \right] X_3^{2n-2} - (1+2) \nabla_1^2 \sigma_{12}^{(2n+1)} \right] X_3^{2n+1} + \right. \\ \left. \left[(1+2) \sigma_{12}^{(2n+1)} (2n-1)(2n) \right] X_3^{2n-1} + (M + \alpha ET)_{12} \right\} = 0 \end{aligned}$$

Fourth compatibility equation

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{33}^{(2n)} + \frac{T^{(2n)}}{1-2} \right) \right] X_3^{2n} + \left[(1+2) \left(\sigma_{33}^{(2n)} + \frac{T^{(2n)}}{1-2} \right) + \right. \right. \\ \left. \left. \sigma_{11}^{(2n)} + \sigma_{22}^{(2n)} + \sigma_{33}^{(2n)} - T^{(2n)} \right] (2n)(2n-1) X_3^{2n-2} + \right. \\ \left. - \left[(1+2) n b^{2n-2} \nabla_1^2 \sigma_{33}^{(2n)} \right] X_3^2 + \left[(1+2) (n-1) b^{2n} \nabla_1^2 \sigma_{33}^{(2n)} + \right. \right. \\ \left. \left. - 2 n b^{2n-2} (2-2) \sigma_{33}^{(2n)} \right] - \left[(1+2) \nabla_1^2 \left(\sigma_{33}^{(2n+1)} + \frac{T^{(2n+1)}}{1-2} \right) \right] X_3^{2n-2} + \\ \left[(1+2) \left(\sigma_{33}^{(2n+1)} + \frac{T^{(2n+1)}}{1-2} \right) + \sigma_{11}^{(2n+1)} + \sigma_{22}^{(2n+1)} - \sigma_{33}^{(2n+1)} + T^{(2n+1)} \right] (2n+1)(2n) X_3^{2n-1} + \\ \left. - \left[(1+2) n b^{2n-2} \nabla_1^2 \sigma_{33}^{(2n+1)} \right] X_3^3 + X_3 \left[(1+2) (n-1) b^{2n} \nabla_1^2 \sigma_{33}^{(2n+1)} \right. \right. \\ \left. \left. - n b^{2n-2} (2+2) \sigma_{33}^{(2n+1)} \right] \right\} = 0 \end{aligned}$$

Fifth compatibility equation

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{33}^{(2n)} \right) \right] X_3^{2n+1} + \left[(1+2)(2n+1) \sigma_{33}^{(2n)} + \right. \right. \\ \left. \left(\sigma_{11}^{(2n+1)} \sigma_{22}^{(2n+1)} + \sigma_{33}^{(2n+1)} + T_{11}^{(2n+1)} \right) \right] 2n X_3^{2n-1} + \\ - \left[(1+2) b^2 \nabla_1^2 \sigma_{13}^{2n} + 2n \sigma_{33,1}^{2n} \right] b^{2n-2} X_3 + \left[1 + 2 \nabla_1^2 \sigma_{13}^{2n+1} \right] + \\ \left. \left[(1+2)(2n+2) \sigma_{13}^{2n+1} + \left(\sigma_{11}^{(2n-1)} \sigma_{22}^{(2n+1)} + \sigma_{33}^{(2n+1)} + T_{12}^{(2n-1)} \right) \right] X_3^{2n} - \right. \\ \left. (n-1) b^{2n} \sigma_{33,1}^{(2n+1)} - (1+2) 2b^{2n} \sigma_{13}^{2n-1} \right\} = 0 \end{aligned}$$

Sixth compatibility equation

$$\begin{aligned} \sum_{n=0}^{\infty} \left\{ \left[(1+2) \nabla_1^2 \left(\sigma_{23}^{(2n)} \right) X_3^{2n+1} \right] + \left[(1+2)(2n-1) \sigma_{23}^{(2n)} \right] - \right. \\ \left(\sigma_{11}^{(2n+1)} \sigma_{22}^{(2n+1)} + \sigma_{33}^{(2n+1)} + T_{11}^{(2n+1)} \right) 2n X_3^{2n-1} - \\ - \left[(1+2) b^2 \nabla_1^2 \sigma_{23}^{2n} + 2n \sigma_{33,2}^{(2n)} \right] b^{2n-2} X_3 - (1+2) 2b^{2n} \sigma_{23}^{2n} - \\ \left[(1+2) \nabla_1^2 \sigma_{23}^{2n+1} \right] X_3^{2n+2} - \left[(1-2)(2n+2) \sigma_{23}^{(2n-1)} \right] - \\ \left(\sigma_{11}^{(2n-1)} \sigma_{22}^{(2n-1)} + \sigma_{33}^{(2n-1)} + T_{12}^{(2n-1)} \right) (2n-1) X_3^{2n} - \\ \left. \left[(1+2) b^2 \nabla_1^2 + 3n \sigma_{33,2}^{(2n-1)} \right] b^{2n-2} X_3^2 - (n-1) C^{2n} \sigma_{33,2}^{(2n+1)} \right\} = 0 \end{aligned}$$

If the solution is carried out to second order we have a set of fourteen coupled partial differential equations in fourteen unknowns. The solutions are presented in an appendix. In order to obtain the accuracy we desire it is necessary to work the solution out to fifth or sixth order. In addition to the complexity of the solution it doesn't describe the dynamics of the process fully. As early as the beginning of the nineteenth century, pioneers in the analysis of thermal stress noted that one could neglect inertial terms in the equations. Indeed even with thermal shock produced across interfaces with the highest heat transfer coefficients, such as the quenching of aluminum metal in water, the vibration induced by the shock is insignificant. The FEL deposits its energy in a few nanoseconds, however, heating the material volumetrically. The analytical solution for a plate subject to sudden uniform heating has two components, a steady state component and a vibrational component [2]. Initially the sudden heating produces inertial compressive stresses. In the case corresponding to an infinite heat transfer coefficient (i.e.,

instantaneous volumetric deposition) the analytical solution for uniform heating shows that the peak stresses are intensified by a factor of 2 due to vibration. It is obvious that the problem of nonuniform heating creating thermally induced vibrations is more aptly solved by a finite element numerical technique on a computer. It is informative, however, to compare first the magnitudes of the stress caused by other processes at work.

The plasma background radiation heats each tile nearly uniformly across its surface. For an EMA 9030 tile 1 mm thick the maximum tensile stress caused by this is 400 psi. For Hexalloy, having a thermal conductivity some 100 times greater the stress is insignificant. If we assume (conservatively) that the edges of the tiles are held at a constant temperature by the supporting frame, we calculate analytically the plane stress produced by the temperature distribution created as heat from uniform background irradiation is conducted out of the tile. Results indicate that for single pulse shots once every five minutes the stress created by this temperature distribution is very small in a 1 mm thick tile of EMA 9030 and negligible in a 3 mm thick Hexalloy tile.

Estimates of the heating due to the daily Taylor discharge cleaning show it creates tensile thermal stress similar in magnitude to that required to cause failure in the 9030 tile. In the Hexalloy sample again the stress produced is negligible. In this case the plane stress is independent of the length of the sample, but directly proportional to the surface heating rate and inversely proportional to the thickness.

Of course, in a linear elastic isotropic material, such as SiC, the stress distribution at any given time is the sum of the stresses due to each effect. The analysis shows us, however, that with the exception of Taylor discharge heating of EMA 9030 tiles, these effects can be neglected in the stress analysis.

In addition to stress induced by heating, stress is produced by the radiation pressure force on the calorimeter, which is of the order of a few pounds on the central tiles during the pulse. These vibrational stresses are analyzed by considering each tile as a beam with time dependent boundary conditions. The analysis confirms one's intuitive expectations that since the pulse is of such a short duration compared with the characteristic period

of vibration of the tile's lower frequency bending vibrational modes the stresses produced by the pulse are not significant.

Specifically the thin plate is governed by the equation [3]

$$a^2 \frac{\partial^4 w}{\partial x^4} + \frac{\partial^2 W}{\partial t^2} = \frac{q(x)p(t)}{\rho A}$$

w = deflection of beam

x = position along beam ($0 < x < 1$)

ρ = density

A = cross section area of beam

I = second moment of cross sectional area

E = Young's modulus

$a^2 = \frac{EI}{\rho A}$

$q(x)p(t)$ = external load per unit length of beam

We solve the equation for a simple supported plate (fixed at the ends) The loading from radiation pressure occurs volumetrically where the radiation is absorbed and at surfaces where it is reflected. At the back face the reflection of radiation creates a pressure acting from the inside of the tile outward, mathematically equivalent to a tensile normal stress. The moment acting on a thin beam is independent of the location through the thickness where the force acts. The solution for our load pattern, which is Gaussian spatially and a sum of step functions temporally, is:

$$W_{(x,t)} = \sum_{m \text{ odd}} D_m \sin \left((2m+1) \frac{\pi x}{l} \right) \frac{p_0}{w_m^2} \sum_n [\cos(w_m(t - n\epsilon)) - \cos(w_m(t - n\epsilon - \tau))];$$

$$D_m = \frac{4}{l} \int_0^l e^{-\left(\frac{x-t/2}{\sigma}\right)^2} \sin \left(\frac{m\pi x}{l} \right) dx$$

$m(\text{odd})$

n = pulse number

τ = period between pulses

ϵ = duration of pulse

Figure 9 shows the lateral deflection (not to scale) of a simply supported beam of EMA 9030 1 mm thick 250 msec after the start of 50 pulses at a 5 khz repetition rate at the center of the antenna pattern. The maximum tensile stress in the material is 316 psi, far below the tensile strength. EMA 9030 would have failed because of the thermal stress induced by the FEL heating long before completion of the 50 pulses. Pressure induced vibrational stresses for a single pulse would, of course, be considerably less.

Thus the only cause of significant stress in the SiC is thermal shock from the FEL heating. To analyze this effect in more detail I put to use four codes available on the National Magnetic Fusion Energy Computer System: Ingrid [4], Topaz3d [5], Nike3d [6], and Taurus [7]. Ingrid generates a 3 dimensional nodal mesh representing the geometry of the object to be analyzed. Topaz3d is a 3 dimensional finite element heat transfer code. Nike3d is an implicit, finite-deformation, finite element code for analyzing the static and dynamic response of three-dimensional solids. Taurus is an interactive post-processor for the output files generated by the other codes. To simulate the heating of the calorimeter tiles I generated a mesh consisting of 400 elements with 605 nodes. The most severe thermal stresses occur in tiles at the center of the antenna pattern where the heating is the greatest. The 1/3 gaussian antenna pattern power contours on the calorimeter form an ellipse 2.0 cm in radius in the toroidal plane and 4.6 cm in the poloidal plane. Over the center (10 cm x 2 cm) calorimeter tile the power flux is nearly independent of the poloidal coordinate. The heat deposition in the tile is then specified volumetrically for each element with a gaussian contour in the toroidal coordinate and an exponential attenuation through the thickness. The fringing of the field at the tile edges should be a small effect because the tangential field component must be continuous across the boundary. The pulse is represented as heating at a constant rate for a period of 32.5 nanoseconds with a linear ramp down period to zero power at 37.5 nanoseconds. Figure 10 shows that steady state x-stress distribution (stress initialized statically) over one quarter of a tile of EMA 9030. The figure is distorted to allow viewing of the stress contours: the actual tile dimensions are 10 cm in the x direction, 1 mm in the y direction

and 2 cm in the z direction. The steady state thermal stress is that produced by the temperature distribution at the end of the FEL heating pulse. The radiation is incident normal to the quarter tile from below. The center of the front face of the entire tile is the nodal point at the bottom extreme left hand side of the figure. All surfaces are adiabatic. For purposes of stress calculation the motion of the nodes normal to the two vertical planes along the edges intersection the center point previously mentioned is constrained, creating two symmetry planes and reducing the amount of calculations required by a factor of 16. The axes are defined in Fig. 10. Stresses are given in psia here. Tensile stresses are positive and compressive stresses are negative. Figure 11 verifies that the stresses along the y axis (thickness) are indeed small.

At any point in a region where three components of normal and shear stress occur, the coordinate system can be rotated so as to eliminate the stresses, expressing all stresses as principal stresses. For the cases of two or three dimensional states of stress, the Mises yield criterion is that yielding can occur when the root mean square of the differences between principal stresses reaches the same value it has when yielding occurs when the load is applied in the axial direction [8]. Like most ceramics silicon carbide performs well in compression but poorly in tension. Most SiC's have a compressive strength of the order 200 ksi and a tensile strength of 5-20 ksi. For the steady state analysis the yielding will occur in regions where the stresses are tensile and the stress intensity exceeds the tensile strength of the material. Yielding in SiC manifests itself as brittle fracture. Figure 12 shows that the 1 mm thick EMA 9030 tile has difficulty withstanding the thermal stress due to the heating from a single FEL pulse.

Figures 13, 14 and 15 show the axial stress distributions for one quarter of a 10 cm by 2 cm by 0.30 cm tile of Standard Oil sintered alpha SiC irradiated the same way. Here the stresses are given in dyne/cm² (1 psi = 68947 dyne/cm²). Now it is assumed that 31% of the power is reflected at the front face. Figures 16 and 17 show the entire tile (with dimensions altered for clarity) with the elastic strains exaggerated by a factor of 300. Figure 16 shows the plate bending concavely at the back face in equilibrium. The

regions of highest stress intensity are at the center of the front face, show in Fig. 17. The stresses are compressive however. The region of maximum tensile stress intensity is along the edge towards the back of the plate. It is seen that the stress intensity there is less than 335 psi. The tensile strength of Hexalloy sintered alpha is approximately 30 ksi (2.1×10^9 dyne/cm²). (The manufacturer believes this is the highest tensile strength of any SiC commercially available.) Thus the Hexalloy can easily withstand the thermal stress associated with a single 35 nanosecond, 3 Gw, 140 Ghz pulse, for the antenna specified at the calorimeter face.

In actuality the stresses at the end of the heating period are inertial, compressive and almost uniform in all directions. Figures 18 and 19 show the inertial compressive x-stress and y-stress at 50 nanoseconds after the shot. Figure 20 shows the uniformity of the stress at this time over most regions: the stress intensity is small. Figures 21, 22, and 23 show the y-stress relaxing rapidly compared to the other axial stresses. Figure 24 shows the temporal variation in the x-stress on the z-axis of the front face of the tile over the first 250 nanoseconds.

Figures 25, 26 and 27 show the temporal variation in x-stress for elements number 1, 91 and 391 over a period of 40 microseconds. The locations of these elements are shown in Fig. 28. This calculation was run for 100 time steps with an interval of 400 nanoseconds between each step. The FEL pulse is then approximated as having a duration of one time step. The error introduced in the Fourier analysis of the vibration due to lengthening of the heating period appears only in the higher order vibrational modes, which apparently do not contribute nearly as significantly to stresses in the tile. Thus the calculation should be a fairly accurate assessment of stresses created during the initial period of thermally induced vibration.

It is observed that the large scale variations in the x-stress in elements 1, 91 and 321 are in phase, indicating a longitudinal vibrational mode along the x-axis. In fact the frequency of this variation corresponds very closely to that of the third harmonic of purely longitudinal vibration along the x-axis. Figure 29 shows the x-stress contour

associated with the third eigenmode of longitudinal vibration. It seems plausible that this mode of vibration would be excited preferentially by the Gaussian heating pattern.

For a rectangular parallelepiped with free ends the longitudinal modes of vibration along an axis of length l can be described in terms of longitudinal displacements:

$$U_n = \cos\left(\frac{n\pi x}{l}\right) \sin\left(\frac{n\pi}{l} \sqrt{\frac{E}{\rho}} t\right)$$

where the x-stress is given by

$$\sigma_{xx} = \frac{\partial}{\partial x}(E U_n)$$

For a 10 cm long tile of standard oil sintered alpha silicon carbide the frequency of vibration of the fundamental longitudinal mode is 16.58 kHz. Flexural transverse vibrations satisfy the spatial and temporal variations:

$$W_n = \cos\left(\frac{n\pi x}{l}\right) \sin\left(\frac{n^2 \pi^2}{l^2} \sqrt{\frac{E h^2}{12(1-\nu^2)\rho}} t\right)$$

For the thickness h of the tile equal to 3.0 mm the fundamental frequency of transverse vibration is 1.28 Hz. Lower order modes of flexural vibration occur over a time scale much longer than the scale shown in Figs. 25-27. The beginnings of the flexural vibrations can be observed in these figures, however; the average value of x-stress in element 1 is becoming more positive, while in elements 91 and 391 (on the back plane) the stresses are becoming more negative. In Figs. 30 and 31 the strains associated with the vibrations are exaggerated by a factor of 5000. The plate is much less rigid with respect to flexural vibration than longitudinal vibration, thus the elastic strain associated with the very beginning of flexural vibration appears much larger than the longitudinal vibration. Comparing the two, which are at opposite ends of the third harmonic longitudinal vibrational cycle, it is clear that the large scale vibration is manifest in longitudinal displacement. Some flexure occurs because of the nonuniform heating through the thickness. That the nature of the vibration is longitudinal is further supported by observation that a separate calculation with a different plate thickness of the same material yielded the same vibrational frequency. As the plate bends towards the previously shown equilibrium

configuration and vibrates about it there is a general cyclic relaxation of the compressive x-stresses on the front face and tensile x-stresses on the back face. One can thus infer that the maximum tensile stress will occur after the first quarter cycle of the third longitudinal vibrational harmonic before the plate has been able to respond appreciably to the non-uniform heating with flexural vibration. Figures 26 and 27 show that the x-stress reaches a maximum value along the center of the back face at 10 microseconds after the pulse with a value of 9.5×10^7 dyne/cm². This is less than twice the maximum value of compressive x-stress obtained from steady state analysis. (Recall that for a plate heated uniformly and instantaneously the maximum stress during the vibrational cycle is exactly twice the maximum steady state value.) In contrast to the steady state analysis, we see there are no regions where all of the stresses are always compressive or tensile: the vibration is so violent that the stresses are reversing. Figures 32 and 33 show that the vibrational stress intensity at two intermediate times is somewhat less than twice the maximum value in steady state. In the steady state analysis the maximum stress intensity occurs in a region of compressive stresses, but with vibrations it can occur in regions of tensile stress.

When multiple pulses are fired at a 5 kHz repetition rate, as is planned for future experiments, each pulse will create its own set of vibrations in the linear elastic SiC. Without actually performing the very lengthy calculation required to represent the effects of each individual pulse creating vibration simultaneously, one can estimate from the previous calculations the maximum stresses created by such an event. The worst case vibrations would occur if the longitudinal and flexural vibrations from each shot were in phase, i.e. if n pulses were represented as a single pulse having a magnitude n times as great. With no plasma present and a 2.0 cm by 4.6 cm antenna pattern using 35 nanoseconds, 3 Gw pulses at a 5 kHz repetition rate (essentially adiabatic heating) the Hexalloy sintered alpha tiles should be able to withstand some 20 pulses during a shot. If allowance is made of at least 60% of the microwave energy being absorbed by a plasma, the tiles should be able to withstand the thermally induced vibration from 50

pulses during a shot. Refraction of the beam by the plasma would reduce the stresses even further. Care must be taken, however, to assess the extent to which ponderomotive filamentation occurs as the resulting focussing of the beam would tend to intensify the thermal stresses.

I believe a more thorough study of the vibrations is in order before extending the number of pulses per shot to near 50 with the same tiles. An explicit finite element code DYNA3D can study vibrations more inexpensively than the implicit code NIKE3D. No three dimensional version of DYNA is available on the NMFECC Crays at present, although it is available on the Octopus system. (I am told that when used on the NMFECC Crays the three dimensional versions of NIKE and DYNA strain the memory capacity of the system, and this is likely why no three dimensional version of DYNA has been compiled at NMFECC. The number of calculations required in an implicit code goes as the seventh power of the number of degrees of freedom of motion of the nodes. The 100 time step run of vibrations in the rectangular parallelepiped generated data on the axial and shear stresses, strains, displacements, velocities and accelerations of each of the 605 nodes and 400 elements at each time step. The high speed printer output file for this run had a length in excess of 500,000 lines.)

(Note: In the model presented all of the edges are free. The actual tiles are free to slide longitudinally and are pressed against a frame by a spring loaded mechanism. The boundary conditions for transverse vibration of a bar held fixed at the ends is $w=0$, $w_{xx}=0$ at the ends, where w is the transverse displacement. For the model considered the conditions are $w_{xx}=0$ and $\int_0^L w \, dx=0$ (conservation of momentum). The transverse vibrational frequencies are identical in both cases as well as the stresses for a thin plate.

3 DETECTOR CONSTRUCTION

Figure 34 shows the details of the detector construction. A thin titanium metal binder layer is vacuum deposited on the back of the carbide. Since SiC is so chemically inert that it is difficult to attach most other substances to it. The titanium layer will form a

bond so strong that one would have to damage the carbide to break it. A 0.6 micron layer of copper is vacuum deposited on top of that. This layer serves a dual purpose of reflecting the rf microwaves back through the carbide, shielding the electronics from extra heating and serving as a ground plane helping to shield electrical noise out of the circuit. After another titanium binder layer, a 4 micron thick layer of SiO/SiO_2 is deposited. This layer could be represented as SiO_x with x near to 2. After another titanium layer, copper pads are vacuum deposited in appropriate locations. The SiO serves as an electrically insulating layer between the copper pads and the ground plane. Figure 34a shows the glass bead thermistor with a 2 micron thick layer of SiO_x vacuum deposited. On top of that is vacuum deposited a copper layer 0.6 microns thick which shields the thermistor material from the intense electric fields of the FEL microwaves and prepares the thermistor for attachment. The bead is attached to the copper pad with an alloy of tin and silver in a vacuum oven at $\sim 400^\circ\text{C}$. The thermistor leads are attached to similar pads with the same process. Attachment of the thermistor wires to these pads eliminates the need compensate the measured thermistor temperature for heat conducted away from the surface by these wires. In addition this aids in the decoupling of the circuit from electromagnetic noise.

The signals are carried to the edges of the plates by copper leads vacuum deposited on the tiles in a pattern similar to the patterns of a printed circuit board. Twisted ceramic coated copper wire pairs are spot welded onto pads just inside of the supporting frame.

The very small thermal mass of the thermistors and the hardware used to attach it minimize the error in the temperature measurement by the thermistor. In addition the metal to metal bond should give the most rapid response time possible with the glass bead thermistor. This assembly will not outgas appreciably at temperatures well above the 125 degrees expected during bakeout and it will remain intact even if some event occurred which heated the tiles themselves up to temperatures near 400°C .

The manufacturer will cut the sintered alpha Hexalloy grade SiC while in the powdered stage, prior to sintering. This makes for a much stronger tile and is also much

cheaper than surface grinding SiC with a diamond wheel. Ceramic materials can be characterized by a random distribution of flaws giving rise to a fracture strength variation. Thus, after sintering, the tiles will then undergo non-destructive evaluation. This process, utilizing ultrasonics or x-ray diffraction, is utilized in the aircraft and nuclear industries. It allows us to screen out defects in the tiles which would be the cause of failure. By eliminating these defects one could, in theory, design the tiles to sustain tensile stresses as high as 40 ksi [9].

Cyclic fatigue has generally not been observed in ceramics with the exception of some glasses. Hexalloy sintered alpha SiC has shown no fatigue in vibrational testing below 1300°C. We do not expect any limit on the tile life due to radiation damage either. SiC exposed to a fast neutron fluence of 2.10^{24} n/m² exhibits a decrease in fracture strength of 25% if the temperature is kept below 673 K during irradiation [5]. This neutron fluence is orders of magnitude greater than what tiles will experience inside Alcator C. Insulating glasses exhibit internal cracking when exposed to intense penetrating x-rays. This effect is attributed to internal charge buildup by "internal photoemission." It can be avoided if the bulk electrical resistivity is below $1.10^6 - 1.10^7$ ohm centimeters. This is the case of Hexalloy SA SiC.

3.1 Thermistor Placement

Figure 35 shows a tentative pattern for placement of the sensors. Reference sensors are located outside of the bars which form the frame support. These are placed in such a way as to give a reliable measurement of background radiation. The main support bars for the frame are located just inside the virtual limits. In order for the tiles and the calorimeter local limiters to fit over the virtual limiters it is necessary to move the main limiters inward between 5 and 10 mm from a minor radius of 16.5 cm. This allows enough room for the calorimeter to fit over the virtual limiters while keeping the heat load on the calorimeter limiters modest, even in the presence of Marfes. Together the local limiters and the virtual limiters protect the calorimeter tiles and electric wiring from plasma heating. Two types of cards are needed for this layout, shown in Fig. 36.

Copper pads are vacuum deposited on the tiles to minimize the contact heat transfer resistance to the frame in vacuum.

Sintered alpha SiC has a higher thermal conductivity than stainless steel #304, from which the frame is constructed. Figures 37-40 show a Topaz3d calculation of the migration of the thermal boundary layer from the stainless steel supports across the plate as a function of time after the beginning of a shot. The figure is deformed vertically proportionally to the value of the temperature at the location. The calculation is based upon a conservative assumption of zero thermal contact resistance between the steel and carbide. Half of the symmetric tile/frame assembly is shown. Heat is incident uniformly across the top face of the carbide for the first 500 msec. Each horizontal division of the carbide corresponds to one third of a centimeter. At 600 msec the temperature on the back face is unaffected by the boundary condition for distances greater than 1.3 cm from the steel bar.

If the main limiters are left at 16.5 cm the reference sensors must be moved inside of the virtual limiters to a location that is not outside of the antenna pattern, giving a poor reference measurement. Accurate reference measurement is crucial for the single pulsed experiments where the power fluence due to background radiation is greater than that due to the FEL per shot.

Figures 41 and 42 show the bulk temperature distribution as a function a time for the heating by the Gaussian antenna pattern. The temperature profile equilibrates locally through the thickness in a few dozen milliseconds. Very little change occurs in the profile across the tile in the first few hundred milliseconds. Note also how the front and back faces maintain the same shape. The 3 mm thickness of the Hexalloy tile does not impair the measurement accuracy of the power contour having the size the scale of the antenna pattern. Figures 43 and 44 compare the initial normalized Gaussian power contour with the contour that exists due to one dimensional heat transfer over the specified period following the pulse. Several hundred milliseconds pass before the profile changes significantly.

Table I: Fractional error in temperature measurement at 10 T.

Sensor	Temperature (K)	Error (percent)
thermistor	60	0.3
platinum resistor	80	2
SrTiO capacitor	1.5-20	0.05
carbon resistor	20	2
GaAs diode	80	2-5

4 Temperature Measurement

4.1 Transducers

Several types of temperature measurement transducer were considered for use on the calorimeter. These included thermocouples, resistance thermometers, diode thermometers, pyroelectric detectors as well as thermistors. Thermocouples produce too small of a signal for a reliable measurement in the noisy tokamak environment. Diode thermometers are strongly affected by the magnetic field. It is reported that pyroelectric detectors have been used in a tokamak, yielding good results in measurement of relative power deposition [10]. Difficulty was encountered in measurement of the absolute power deposition with them however. Pyroelectric detectors may well be affected by the operating environment of the tokamak. Resistance thermometers have been used on bolometer systems in tokamaks. Platinum resistance thermometers are sensitive to magnetic fields due to the strain gauge effect. The resistance of the metal changes not only with temperature but with changes in the sensor strain associated with the $\vec{I} \times \vec{B}$ force. The change of thermistor resistance with magnetic field strength (termed magnetoresistance) is among the lowest of any temperature measurement transducers. (One transducer, the SrTi) capacitor, has a lower magnetoresistance than the thermistor at cryogenic temperatures. It is a small fast and sensitive device having a simple electrical readout, namely capacitance variation.) Table I compares the magnetoresistance of various transducers at cryogenic temperatures [11].

The magnetoresistance effect apparently becomes vanishingly small in thermistors at

room temperature. Since we are measuring temperature changes the error would show up only as an error in the calculation of the temperature resistance coefficient of the thermistor at the beginning of the shot. The cumulative error at the -50°C operating temperature is expected to be negligibly small.

Thermistors are highly resistant to gamma radiation and are not sensitive to the neutron fluence in Alcator C. In larger tokamaks neutron damage to thermistors becomes a problem: noise in the thermistor increases during the irradiation and the resistance of the thermistor changes with time due to neutron damage. The zero power temperature resistance coefficient of a thermistor $((dR/dT)/R)$ is typically -4 to -5%. This is an order of magnitude larger than the response of resistance thermometers and orders of magnitude larger than the fractional change in signal with thermocouples.

Unlike most thermistors, the Thermometric glass beam thermistors are compatible with ultra high vacuum and can operate in steady state at temperatures up to 300°C. Intermittant operation at temperatures as high as 650°C centigrade is possible. These thermistors have a very small thermal mass, so as not to perturb the measurement significantly. The BR 16 glass bead thermistor is termed ruggedized by the manufacturer and is especially made to withstand harsh conditions such as we are subjecting it to during the attachment process. They are meant for use in applications where high reliability is important.

4.2 Measurement Circuit

A new type of circuit is being employed to measure the temperature of the thermistors in the tokamak. The schematic of the circuit is shown in Fig. 45. A reference current is passed through R8. Prior to the shot, when the switch in the feedback circuit is closed, the sum of the currents into U2(-) must be zero. Thus the reference current is established in the sensor. This circuit concept has the advantage that the measurement procedure is the same for all temperatures. One simple note the thermistor temperature at the beginning of each shot.

The Steinhart equation is used to describe the resistance-temperature variation in a

thermistor:

$$\ln R = A_0 + \frac{A_1}{T} + \frac{A_3}{T^3}$$

For a small variation in T

$$\frac{dI_T}{dT} = \alpha I_r$$

α = temperature resistance coefficient

The amplification transfer resistance is given by $R_g = \frac{R_1 R_3}{R_2}$. Therefore for small changes in temperature the change in output voltage is proportional to the temperature change:

$$\frac{dV_o}{dT} = \alpha I_r R_G$$

For larger temperature variation, such as we will encounter with the calorimeter, one must take into account the third order term in the thermistor equation. The temperature of the thermistor is measured at the beginning of each shot by reading the value of voltage at U6. The output of the circuit, U_{out} is digitized and the temperature change is calculated by solving the third degree polynomial equation using the measured value of change in thermistor resistance. (Note: even when the resistance of a thermistor shifts with age, the shift in the R vs T curve is parallel to the T axis and the temperature resistance coefficient of the thermistor at a given temperature is unaffected.)

This circuit incorporates a passive rf filter to prevent rf noise from saturating any components of the feedback circuit. An active 12 Hz 3-pole Butterworth filter rejects 60 Hz noise at the output with a 23 dB cutoff. Active filters offer response superior to that of passive circuits at acoustic frequencies [12]. The sharp 30 dB/decade cutoff of this filter eliminates higher frequency noise contributions from the output. The output filter time constant is 44 msec. Higher cutoff frequencies, yielding faster response times, can be obtained by shunting the appropriate value of resistance across R5, R6, and R7 each.

The error in measurement of the temperature change due to parasitic conductance through the SiOx layer between the printer circuit leads and the copper ground plate is expected to be of the order of $6.10^{-4}\%$. The error due to conductance through the SiOx layer on the thermistor itself is much less. When a constant voltage is applied

across a thermistor, an increase in temperature produces a decrease in resistance allowing more current to flow, increasing the joule heating rate and triggering a further rise in temperature. For a current of 30 microamps through a 100 kilo-ohm thermistor (using a conservative value of dissipation constant = $5^{\circ}\text{C}/\text{mw}$) the self heating error is estimated to be 0.5%.

The toroidal field will be held constant for the duration of the shot. The current in the ohmic heating coils is changed rapidly to create and sustain the toroidal plasma current. To first order the magnet system excludes this magnetic field from the inside of the tokamak. The parasitic effects of the changing magnetic field are minimized by twisting circuit wires and avoiding any open loops in the circuit.

5 Limiter Design

The heat flux from a magnetized plasma to a wall normal to the magnetic field is calculated using the plasma sheath equation. The sheath is modeled as a planar diode. This is an excellent approximation for the calorimeter limiters.

$$n_e = n_o \exp \left[\frac{eI(x)}{kT_e} \right]$$

$$\frac{d}{dx}(nv) = S \equiv \text{source from ionization of neutrals etc.}$$

$$\frac{dM}{dx} = \frac{S}{nc_s} [1 + m^2 - M^2] \quad M \equiv \frac{v}{c_s}$$

There is a small presheath drop in potential in the plasma. This field accelerates the ions as they move towards the edge of the plasma. When ions reach the speed of sound the equation blows up. This marks the termination of the plasma and the formation of a sheath, a region of net positive charge. The flux out of the sheath is:

$$J^+ = e n_{se} C_s \cong \frac{1}{2} e \hbar_o C_s (T_e, T_i)$$

if T_e and T_i are spatially constant and the plasma ions do not suffer momentum loss. The flux back into the plasma is:

$$j_{net}^- = (1 - \gamma_e) j_{net}^- = \frac{1}{4} n_{se} C_{se} (1 - \gamma_e) \exp \left(\frac{eI_{Heat}}{kT_e} \right)$$

γ_e = secondary electron emission coefficient (significant only when electron energies are dozens of eV or greater).

For floating conditions we have ambipolar flow:

$$j_{net}^- = j + \frac{eI_{float}}{hT_e} = 0.5\ell n \left[\left(\frac{2\pi m_e}{m_i} \right) \left(1 + \frac{T_i}{T_e} \right) (1 - \gamma_e)^{-2} \right]$$

The power removal rate from the plasma is by electrons is:

$$Q_e = (2kT_e - \ell I_f) \frac{j \cos}{e} + eI_f \frac{j_{se} e c}{e}$$

The last term corresponds to energy reinjected into the plasma by secondary electrons.

The power removed by ions is:

$$Q_i = 2kT_i \frac{J^+}{e}$$

Thus the total power removed is the sum of the two components. The heat flux to the wall is given by $T_{wall} = T_o \hat{n} \times \hat{t}$ where

T_o = the normal heat flux

$\hat{n}$ = unit normal of limiter plate surface

$\hat{t}$ = field line unit tangent vector

The heat flux to a limiter is a very sensitive function of the limiter dimensions and positioning. A limiter casts a shadow along the magnetic field lines.

The main limiter radius marks the termination of the core plasma and the beginning of the edge plasma. The scrape off layer behind the main limiters can be sustained by

1. diffusion from the core
2. ionization—this causes a thicker scrape off layer.

ionization of absorbed gases on the limiter may cause strong radiation around the limiter during a shot. The scrape off layer has a small thickness due to the high value of C_s compared to the effective cross field diffusion velocity. The density scrapoff length is given by

$$\lambda_n \cong \sqrt{\frac{D_{\perp} L_{\parallel}}{f(T_i/T_e) C_s}}$$

where $L_{||}$ is the connection length between successive limiters along the magnetic field and f is as density reduction factor due to presheath effects. It is approximately 0.5 for our Alcator plasma parameters. The edge plasma density and temperature vary with distance behind the limiter (x) as:

$$\begin{aligned} n(x) &= N_0 e^{-\frac{x}{\lambda_n}} \\ T(x) &= T_0 e^{-\frac{x}{\lambda_t}} \end{aligned}$$

A heat flux scrape length can then be defined:

$$\lambda_Q = \frac{2\lambda_n \lambda_t}{2\lambda_t \lambda_n}$$

At MIT a probe called Janus (named after the Roman god with two heads) measured simultaneously the particle fluxes incident along the magnetic field lines on two opposing faces [10]. Janus measured sizeable directional asymmetries in the Alcator C edge plasma. The directional asymmetries are defined in terms of the relative drift between plasma ions and electrons that comprises the toroidal and poloidal plasma currents. The predicted ratios of the density measured on the electron side of Janus to the ion side is

$$R_{e/i} = \frac{n_{pe} e^{-\frac{\Delta p}{\lambda_n e}}}{n_{pi} e^{-\frac{\Delta p}{\lambda_n i}}}$$

Δp = distance of Janus behind the main limiters.

Typical values of Alcator C edge plasma parameters are:

$$T_i = 25 \text{ eV}$$

$$D_i = 8000 \text{ cm}^2/\text{sec}$$

$$T_e = 15 \text{ eV}$$

With these values the theoretical calculations predicted $R_{e/i} \cong 1.65$. Experimentally $R_{e/i}$ ranges from 5 to 30. Connection length alone cannot explain the experimentally observed value of $R_{e/i}$. Asymmetry can also be produced by

5.0.1 Shifting the plasma flux surface position

The Alcator C flux surface are concentric circles with a possible shift in the center axis. If the flux surface position is shifted, then it is possible that the flux tube connecting one side to another must go around the torus several times before intercepting another surface. The shifting of the magnetic axis has been observed to have a strong effect on the magnitude of the directional asymmetry of the Alcator C edge plasma.

5.0.2 Parallel Plasma Flow

The Bohm Sheath Criterion requires both ions and electron flow together. If no parallel momentum source exists, the flow starts at the midpoint between two connecting limiter surfaces.

Harbor and Proudfoot predict flow with a Mach number of $M \sim 4$ using their fluid model and $M \sim 0.8$ using their kinetic model [13].

Because of the higher perpendicular transport on the electron side, greater than 95% of the total heat flux to the calorimeter limiters will be on the electron side limiter.

Another probe, Densepack, measured the strong poloidal asymmetries in the scrape off length and the edge plasma densities. The scrape off length is considerably smaller than average on the inside of the torus. Measurements performed on Alcator C indicate that over most of the calorimeter poloidal extent (from the plane of symmetry to 75% below and to 30% above) $\lambda_n < 0.2$ cm. At -72% and -85% the density scrap off length has increased to 0.3 cm. The thermal scrape off length is 1 cm.

$$\lambda_T = 1.0\text{cm}, \lambda_n = 0.3\text{cm}, \lambda_Q = 0.21\text{cm}$$

The front edge of the calorimeter limiter is 1.0 cm behind the main limiter. Using the measured values of edge plasma density on the inside of the torus the total heat flux to the electron side limiter during a normal shot (assuming $\lambda_n = 0.2$ cm over most of the calorimeter) is estimated to be 73 joules. This is a modest heat loading. The maximum temperature that will occur anywhere on the limiter will be at the leading edge at the

extreme poloidal angle of the limiter span. For a 3.5 mm thick stainless steel local limiter this rise will be less than 47°C . The local limiters are moved away from the calorimeters to minimize both their shadowing and radiant heating of the calorimeter.

Marfes will cause a strong increase in heating on the inside of the torus [14]. A Marfe is a region at the edge of the plasma where radiated power and perpendicular transport of heat are dramatically increased. They occur in Alcator C on the upper inside of the torus, inside and outside of the main limiter radius, essentially on the front face of part of the calorimeter. They are torroidally symmetric and their shape is inferred to be a crescent: the emission inside depends only on the poloidal angle. Low z impurities are inferred to be the cause of the emission by a process of elimination. Reversal the torroidal magnetic field will cause the Marfe to move to the bottom half of the torus. At a minor radius of 18.0 cm a Marfe will intensify the local heat flux by a factor of 10 and increase the heat flux at 17.5 cm by a factor of 7-8. The calorimeter limiters are designed for this. The increased heat loading is due to an increase in the density scrape off length.

Marfes occur in shots at higher plasma densities, usually when the line averaged electron density $n_e > 3 \cdot 10^{14} \text{ cm}^{-3}$. At lower densities ($n_e < 2 \cdot 10^{14} \text{ cm}^{-3}$), with Mo limiters, radiation dominated the power balance in Alcator C with $P_{\text{rad}}/P_{\text{ro}} > 0.5$. When $n_e < 1 \cdot 10^{14} \text{ cm}^{-3}$ $P_{\text{rad}}/P_{\text{ro}}$ can go as high as 90%. The poloidal asymmetry in background radiation is weak at densities below those at which Marfes occur. With or without Marfes the background radiation is torroidally symmetric over a region the width of the calorimeter face. The reference and active thermistors have been placed on each calorimeter tile in a torroidally symmetric fashion to obtain reliable measurements. At densities intermediate to those mentioned the fraction of power radiated ($P_{\text{rad}}/P_{\text{ro}}$) falls to near 30% and the poloidal asymmetries in the background radiation remain small.

Optimum results will be obtained with the calorimeter during single pulse experiments when the line averaged electron density is $2 \cdot 10^{14} \text{ cm}^{-3}$. If the plasma density contour is correctly described as $n_e(r) = n_0 (1 - (r/a)^2)$ this corresponds to a central electron density of $3 \cdot 10^{14} \text{ cm}^{-3}$. Use of 10 cm radius limiters will reduce the magnitude of the

local emission around the calorimeter and reduce the asymmetry of the background contribution.

6 INTERNALLY REFLECTING TILE

A Macor tile utilizing internal refraction to trap microwave energy has been fabricated. The absorber tile has a structure like a diffraction grating on the back surface which is metallized with a 3.5 micron thick layer of titanium metal to reflect millimeter microwaves while creating negligible distortion of the measured temperature profile because of the relatively low value of thermal conductivity of titanium metal. The optical analogy of this system, shown schematically in Fig. 44, is that of a diffraction grating utilizing specular reflection to shift the diffracted energy into a higher order mode.

At the front face the wave undergoes internal reflection. Since Macor is a conducting dielectric total internal reflection does not occur. We analyze the system by analogy with a plane wave incident upon an infinite boundary.

The propagation vector for a plane wave launched into a conducting dielectric at a plane interface is:

$$\begin{aligned} R &= K_r \sin H \hat{i} + K_r \cos H \hat{k} - i K_i \hat{k} \\ K_r &= \frac{w}{c} \sqrt{p^2 + n^2 \sin^2 \theta_1} \equiv N \frac{w}{c} \\ K_i &= \frac{w q}{c} \end{aligned}$$

p, q are as defined earlier.

$N \cos H$ = real part of $\hat{n} \cos \theta$

H = real angle between $\hat{K}_r$ and $\hat{k}$

Planes of constant phase are perpendicular to the real component of the vector. Planes of constant amplitude are perpendicular to the imaginary component. The wave reflected off of the base is of the same frequency and is propagating in the same medium, therefore the magnitude of the propagation vector is unchanged. Upon reflection from a base at

an angle θ with the front face the resulting propagation vector is:

$$\begin{aligned}\hat{K} = & -iK_i \cos(2H)\hat{k} - iK_i \sin(20)\hat{i} \\ & -K_r \sin HH \sin(20)\hat{k} + K_r \sin H \cos(20)\hat{i} \\ & -K_r \cos HH \cos(20)\hat{k} + K_r \cos H \sin(20)\hat{i}\end{aligned}$$

For the infinite system, this reflected wave gives rise to a transmitted component at the macor/vacuum interface. The cosine of the complex angle of propagation is obtained:

$$\cos \hat{\theta}_{vac} = \sqrt{1 - \left(\frac{c}{w}\right)^2 (K_r \sin H \cos(20) - K_r \cos H \sin(20) - i k_i \sin(20))^2}$$

This corresponds to a surface wave for angles 20 beyond the critical angle. The distinction between total internal reflection at an interface between non-conducting dielectrics and the internal reflection when the wave is incident from inside a conducting dielectric is that the imaginary component of the complex propagation vector in vacuum is not perpendicular to the interface plane. Not only is there transport of energy by the wave but also dissipation: the divergence of the Poynting vector is no longer non-zero. The surface wave, after travelling some distance, refracts back into the dielectric.

The theory can be extended to obtain the fraction of energy reflected back into the dielectric at the interface for the s and p polarizations. Figures 46 and 47 show the fraction of energy reflected back into the macor for the chosen base angle of 20° as a function of angle of incidence of the wave on the tile (in radians). Of the energy transmitted into the surface wave on the actual tile, some fraction would refract back into the Macor.

The purpose of this device is to decouple the electric fields of the reflected waves from the front and back faces. This should produce a device that absorbs a high fraction of the incident microwave energy with little sensitivity to the incident angle of the radiation. Since the real part of the dielectric constant of Macor at 140 GHz is 2.3, the wavelength of the radiation in Macor is shortened considerably and the approximation to geometric optics becomes even better.

Since Macor has a much lower value of Young's modulus than SiC and a lower attenuation coefficient, the thermal stress produced by absorption of FEL microwaves in this device is much lower than in SiC and the device should be able to sustain huge power loadings while producing a relatively accurate measurement of the temperature profile. Macor is designed to be machineable and to resist the propagation of cracks that develop. Two prototypes are awaiting testing.

Another complimentary structure is made of Macor and is wedged on the front face. A 1.07 mm gap exists between its back face and the front face of the tile.

The reflectivity and transmissivity of this gap can be shown to be equal to:

$$\hat{r} = \frac{r_{12}(1 - e^{i\hat{B}})}{1 - \hat{r}_{12}e^{i\hat{B}}}$$

$$\hat{t} = \frac{(1 - \hat{r}_{12}^2)e^{i\frac{\hat{B}}{2}}}{1 - \hat{r}_{12}^2e^{i\hat{B}}}$$

With the specified gap the reflectance at 140 GHz is zero independent of the angle of incidence. The wedged front face prevents multiple reflections from causing uncertainty in the absorption of the wave by the calorimeter tile. It is wedged towards the poloidal direction because the variation in the beam phase and intensity in that direction more closely resembles a plane wave than in the toroidal direction. The front wedged plate absorbs the background line radiation, while transmitting most of the microwave energy to the calorimeter tile where it is absorbed. The gap is sufficiently large that the surface wave produced by the calorimeter tile cannot couple any significant of power back into the shield plate.

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