Thermal Analysis of the Crotch Absorber in APS¹

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Abstract

A crotch absorber design for use in the Advanced Photon Source (APS) has been proposed and analyzed. The absorber is placed downstream of sectors S2 and S4 in the curved storage ring chamber and will be subjected to a peak power of $120 \frac{W}{mm^2}$ per 100mA synchrotron radiation. A beryllium ring is brazed on the GlidCop cooling cylinder in order to diffuse the concentrated bending magnet heating. One concentric water channel and two annular return water channels are arranged in the GlidCop cylinder to enhance the cooling. A Bodner-Partom thermoviscoplastic constitutive equation and a modified Manson-Coffin fatigue relation are proposed to simulate the cyclic thermal loading, as well as to predict the thermal fatigue life of the crotch absorber. Results of temperature and stress using finite element computations are displayed and series of e-beam welder tests and microstructure measurements are reported.

1 INTRODUCTION

The crotch absorber in the Advanced Photon Source (APS) is known to be an important device in stopping part of the synchrotron beam. As shown in Fig. 1, the crotch absorber is located at the downstream of sector 2 (S2) and sector 4 (S4) in the curved storage ring chamber. The proposed crotch absorber is composed of three parts—one cylindrical absorber and two inclined absorbers—which sit inside the 6-inch ConFlat flange (Fig. 2). The cylindrical absorber is placed in front of the inclined absorbers to prevent the edge from heating. Due to its short distance (72 to 138 inches) from the source, the absorber is subjected to high power heat flux. Since the maximum heat power striking the cylindrical absorber surface is about $120 \frac{W}{mm^2}$ per 100 mA, a 0.125-inch thick beryllium ring is brazed on the GlidCop tube in order to diffuse the heat. Because of the extremely localized temperature gradient, subsequent high temperature and stress fields result. Since the bending magnet will be shut off twice a day, the absorber will experience thermal cyclic heating which might lead to low cycle thermal fatigue². Therefore, thermomechanical analysis is needed to provide sufficient information in designing the crotch absorber.

Since beryllium allows intense synchrotron radiation to diffuse into the material volume, the heat flux density is thus deposited into a larger volume which can reduce the temperature considerably compared with that of surface heating. CESR has successfully used beryllium in conjunction with copper tubing to form a beryllium-Copper composite absorber (Mills, et al [2]) to diffuse the heat flux. Several other researchers have also reported comprehensive thermal analysis using beryllium materials applied to beam stops (e.g., Bedzyk, et al [3], Choi [4], and Khounsary [5]).

From the metallurgical point of view, high temperature may result in instability in the microstructure of a metal. Fatigue properties, in general, are dependent on the frequency of thermal loading, the amplitude, recovery, phase change, phase transformation, grain coarsening, recrystallization, reversion.

¹ Work supported by U.S. Department of Energy, Office of Basic Energy Sciences under Contract No. W-31-109-ENG-38.

² 0 to 10⁴ cycles is defined as *low cycle fatigue*. 10⁴ to 10⁷ is *high cycle fatigue* (Madayag [1]).

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diffusion, oxidation, and other corrosion and chemical changes (Madayag [1]). Hence, in order to qualitatively predict the thermomechanical events of the crotch absorber during cyclic heating, a temperature and strain-rate-dependent unified constitutive law is used. The mathematical model was constructed by Bodner and Partom [6] and has proven to be more appropriate and computationally less time consuming than other existing constitutive equations (James, et al [7]). In addition, a modified Manson–Coffin fatigue life relation is utilized to predict the thermal fatigue life cycle of the absorber. In this paper, only steady temperature and elastic stress of the cylindrical crotch absorber are reported.

An e-beam welder experiment has been carried out in order to study the thermal induced fatigue. A 1-kHz rastered electron beam is utilized to heat GlidCop and OFHC coupons, and 1 and 2 kW of electron power are used for the experiments. A transient temperature analytical solution based on a rastered Gaussian beam is also displayed to illustrate the trend of the thermal cycle.

2 POWER IMPLEMENTATION AND FINITE ELEMENT MODEL

The dimensions of the proposed cylindrical crotch absorber are shown in Fig. 3. The cylindrical crotch absorber is discretized into an 8 node brick element model as is shown in Fig. 4. Water cooling is supplied on the channels whereas at any other boundaries it is assumed to be adiabatic. A beryllium ring is placed outside the surface of the GlidCop cylinder where the bending magnet radiates. Due to the material character of the beryllium ($Z = 4$), the bending magnet radiation penetrates into the material and can be diffused. Thus, for beryllium, the heat input is assumed to be heat generation and can be written as [8] :

$$q'[\frac{kW}{mm^3}] = \frac{1.4107 \times 10^3 B[T] E^4[GeV] I[A] \sin(\delta)}{l^3[mm]} \int_0^\infty \alpha_t \eta^2 F(\eta, \gamma\psi) \exp(-\alpha_t \xi) d\eta, \quad (1)$$

where

$$\begin{aligned} F(\eta, \gamma\psi) &= F_{||}(\eta, \gamma\psi) + F_{\perp}(\eta, \gamma\psi), \\ F_{||}(\eta, \gamma\psi) &= \left(1 + (\gamma\psi)^2\right)^2 K_{\frac{2}{3}}^2 \left[\frac{\eta}{2} \left(1 + (\gamma\psi)^2\right)^{\frac{3}{2}}\right], \\ F_{\perp}(\eta, \gamma\psi) &= \gamma^2 \psi^2 \left(1 + (\gamma\psi)^2\right)^2 K_{\frac{1}{3}}^2 \left[\frac{\eta}{2} \left(1 + (\gamma\psi)^2\right)^{\frac{3}{2}}\right]. \end{aligned} \quad (2)$$

Here K_n is the modified Bessel function of the n kind; B , E , I , δ , and l are the magnetic field, positron beam energy, beam current, incident angle, and distance from the source, respectively; and $\gamma = \frac{E}{m_o c^2} = 1957 E$. α_t is the nondimensionalized absorption coefficient dependent on photon energy and can be evaluated in terms of series (Choi [8]). The value used in this analysis is obtained by executing the program TRANSMIT. ξ is the parameter defined as

$$\xi = \frac{h_t}{l}, \quad (3)$$

and h_t is the depth measured from the vacuum heating surface. The vertical angle ψ is written as

$$\psi = \frac{z}{l} = \frac{\pi}{2} - \cos^{-1} \left(\frac{z'_t}{\sqrt{x'^2_t + y'^2_t + z'^2_t}} \right), \quad (4)$$

x' , y' , and z' are in the new coordinate system defined as

$$\begin{bmatrix} x' \\ y' \\ z' \end{bmatrix} = \begin{bmatrix} 0 & 0 & 1 \\ -\sin(\theta_o) & \cos(\theta_o) & 0 \\ -\cos(\theta_o) & -\sin(\theta_o) & 0 \end{bmatrix} \begin{bmatrix} x - x_o \\ y - y_o \\ z - z_o \end{bmatrix} \quad (5)$$

if the center of the bending magnet beam travels only horizontally. The horizontal angle, θ_o , is determined as

$$\theta_o = \tan^{-1} \left(\frac{y_o - y_t}{x_o - x_t} \right), \quad (6)$$

where x_t, y_t, z_t represents the coordinate in the absorber which is to be heated by the bending magnet. x_o, y_o, z_o is the corresponding coordinate of the source point given as

$$\begin{aligned} x_o &= \frac{\rho^2 x_t - \rho y_t \sqrt{x_t^2 + y_t^2 - \rho^2}}{x_t^2 + y_t^2}, \\ y_o &= \frac{\rho^2 y_t + \rho x_t \sqrt{x_t^2 + y_t^2 - \rho^2}}{x_t^2 + y_t^2}, \\ z_o &= 0, \end{aligned} \quad (7)$$

where ρ is the radius of the curved chamber and is *1533.903 inches (= 38.9613 meters)* in the APS. Note that the incident angle δ is determined by utilizing the inner product of two unit vectors: one is the outward unit normal vector of the surface facing the bending magnet beam, the other is the vector from the source coordinate (x_o, y_o, z_o) to the target coordinate (x_t, y_t, z_t) . l is the distance from the source.

Assuming that the rest of the heat flux is deposited onto the beryllium-GlidCop interface, the power distribution due to the interfacial heating is in the form of

$$q' \left[\frac{kW}{mm^2} \right] = \frac{5.42 \sin(\delta)}{l^2 [mm]} B[T] E^4 [GeV] I[A] \int_0^\infty \eta^2 F(\eta, \gamma\psi) \exp(-\alpha_t \xi) d\eta. \quad (8)$$

A $2 \frac{W}{cm^2 \text{ } ^\circ C$ water convection coefficient is used, whereas the radiation effect is assumed to be negligible.

Fig. 4 shows the finite element model of the cylindrical absorber. An intensive element mesh is made along the photon fan plane due to its concentrated heating. Nodal power distributions are calculated individually using Eqs (1) and (8).

3 E-BEAM WELDER EXPERIMENT

If the absorber cooling is sufficient such that the risk of “burn-out” is eliminated, the next condition to be satisfied is to maintain the thermal stress within allowable limits. Since the absorber will be subjected to cyclic thermal loading, the allowable stress limits should be based on a fatigue criteria. Unfortunately, little information exists on the elevated temperature fatigue strength of many potential absorber materials [9]. The effects of the very localized nature of the thermal stress also need investigation.

An experiment was developed to study thermally-induced fatigue damage in potential absorber materials (see Fig. 5). The experiment utilizes a rastered electron beam to thermally cycle coupons of various materials mounted on a water-cooled base. Two coupons can be mounted on the base and the electron beam can be rastered across the face of both coupons. With rastering frequencies of 1 kHz , coupons can be subjected to 1 million thermal cycles in less than five minutes. The simple experimental setup can be used to compare the performance of two materials subjected to identical cyclic thermal loading. By comparing the results to thermal models, the experiment may yield information relating fatigue damage to peak oscillating temperature and number of thermal cycles.

When subjected to heating from a rastered electron beam the temperature profile of the coupons consists of two components: a quasi-steady state component and a transient component. The steady state component is the result of the time-averaged heating and can be easily predicted from a two-dimensional model. The transient component is more complex since it is the result of a circular heat source moving across the surface of a three-dimensional block. A model was developed to predict the transient temperature variation for the moving electron beam.

Fig. 8 shows a plot of the surface temperature contour for a 1 kW electron beam moving across the face of a copper coupon at a velocity of $200\frac{\text{m}}{\text{second}}$. As the plot indicates the surface temperature rises rapidly and then decreases rapidly to approximately the original value as the electron beam passes. Thus, an electron beam rastered at this velocity with a sufficiently large displacement amplitude will thermally cycle a coupon surface with each pass of the beam.

The electron beam used for the initial phase of the material testing was rastered at 1 kHz with a sinusoidal waveform and a displacement amplitude of 32 mm . As a result, the coupons were subjected to 2000 thermal cycles per second. The beam velocity during the center portion of the sweep was $200\frac{\text{m}}{\text{s}}$. Two coupons were mounted side-by-side; One coupon was made from GlidCop AL-15 and one was made from OFE copper.

The GlidCop coupon did not show any damage when heated with a 1 kW electron beam. The OFHC coupon was damaged with $60,000$ cycles (Fig. 6) and the damage increased as the number of cycles increased. With a 2 kW beam the GlidCop coupon was damaged with $20,000$ cycles and cracking was evident at $120,000$ cycles (Fig. 7). The OFHC sample was severely damaged by a 2 kW beam with $20,000$ cycles.

These are the initial results of an ongoing testing program. Further tests will be conducted to verify these results and study the effects of changing the peak temperature and number of cycles. Additional metallurgical examinations will also be conducted to determine if some of the observed damage is the result of excessive strain or if melting has occurred. Although these tests do not duplicate the line heating of a pan of radiation from a bending magnet, they provide a relatively inexpensive way to contribute to the currently undeveloped information base of thermal fatigue properties of absorber materials.

4 THERMAL STRESS

Due to inhomogeneous temperature distribution and boundary constraints, the absorber will experience

thermal stress. In general, a larger temperature gradient will result in higher stress. The material constants are listed in Table 1. On the other hand, due to different thermal expansion constraints, high thermal stress will be generated on the beryllium-GlidCop interface. During heating, high compressive stress is developed especially around the areas where the high temperature, as well as the high temperature gradient are located. The compressive stress will be attain its maximum magnitude when the steady state temperature is reached. When the beam shuts off and the absorber is cooled down, correspondly the stress state is decreased accordingly. Since the absorber is to be heated up and cooled down repeatedly, it is subjected to cyclic thermal loading. As a result, fatigue failure will be the subject of greatest concern.

In general there are two factors which result in failure. One is the fatigue due to cyclic loading in the material elastic range and the other is due to its plastic deformations. Thus, it is important to understand which failure mode is dominant in the absorber.

5 FATIGUE AND PLASTIC DEFORMATION

The GlidCop material is composed of reinforced metal (or intermetallic) inclusion in the copper matrix. and since the inclusion particle is much stiffer than that of the matrix, the mobility of dislocation is decreased which consequently increases the strength of the material. In addition, since the dimension of the beam profile is much larger than that of the inclusion particle in view of the neighborhood of a single particle, the temperature field can be assumed to be uniform. As a result, nearly constant stress appears inside the inclusion and drops off starting at the inclusion–matrix interface. Even though this constant stress could be lower than the yielding stress of the overall material, it is beneficial for the debonding between copper matrix and the inclusion; typically when a microcrack has been initiated at the interface. Therefore, in order to understand the failure mode of the GlidCop, thermomechanical analysis of the crack initiation problem in the two phase composite is desired and is under investigation. On the other hand, in view of the thermal energy activation on the material behavior, both loading frequency and the temperature gradient are interrelated. As was mentioned by Daniels and Dorn [1], the thermal failure cycle is directly related not only to temperature and frequency, but also to the activation energy and stress level. That is, the failure cycle N is expressed as

$$N = f(\omega e^{-\frac{\Delta H}{RT}}, \sigma_{ij}), \quad (9)$$

where R , H , and σ_{ij} are gas constant, activation energy, and stress tensor, respectively. More specifically, Udouchi and Wada [10] modified the well-known Manson–Coffin relation and proposed a new relation :

$$N^k \Delta \epsilon^{vp} \left[-\frac{Q_1}{T_{av}} + \left[1 + C_2 \Delta T \exp \left(-\frac{Q_1}{\Delta T} \right) \right] \right] = C_1, \quad (10)$$

where

$$\begin{aligned} \Delta T &= \text{amplitude of the temperature cycle,} \\ T_{av} &= \text{average temperature in the cycle,} \\ \Delta \epsilon^{vp} &= \text{plastic strain range in the cycle,} \end{aligned} \quad (11)$$

and material constants k , C_1 , C_2 , and Q_1 are to be determined from the experiment.

As to predicting the plastic deformation, a constitutive equation is introduced to describe the thermoviscoplastic behaviors of the absorber. However, the definition of the yielding surface may not be adequate since, in practice, micro plastic deformation may have been initiated in the early stage while the absorber was undergoing thermoelastic loading. By introducing internal state variables, Bodner and Partom [6], [11], [12] developed a unified constitutive equation which can fully describe the plastic behaviors during loading and unloading. The benefits of using the unified constitutive model are the following [12] :

1. It is free of yielding surface definition; therefore, there is no need to keep track of the yielding point during computation.
2. The model is strain rate sensitive and temperature dependent.
3. It is capable of determining isotropic and directional hardening, thermal recovery of hardening, creep and stress relaxation, cyclic hardening and softening, cyclic creep and relaxation, temperature and pressure dependence of the plastic flow.
4. It is capable of predicting inelastic response under complex loading histories containing either proportional and/or nonproportional loading paths.
5. The procedures for evaluating the material constants in the Bodner-Partom model are much simpler than other models.
6. It is easy to implement in the finite element model.

In our proposed model, the beryllium is assumed to behave thermoelastically whereas the GlidCop material behaves thermoviscoplastically. The plastic strain rate $\dot{\epsilon}_{ij}^{vp}$ of the GlidCop is proportional to the deviatoric stress S_{ij} and has the relation

$$\dot{\epsilon}_{ij}^{vp} = \Lambda(S_{ij})S_{ij}, \quad (12)$$

where

$$\Lambda(S_{ij}) = \frac{\sqrt{2}D_o}{\sqrt{S_{ij}S_{ij}}} \exp\left(-\frac{1}{2}\left(\frac{2Z^2}{3S_{ij}S_{ij}}\right)^n\right), \quad (13)$$

where Z is an internal state variable and is given by

$$Z = Z_1 - (Z_1 - Z_o) \exp(-m \int_0^t S_{ij} \dot{\epsilon}_{ij}^{vp} dt), \quad (14)$$

if the recovery effects are negligible (Merzer [13], [14]). Z_o denotes the initial value and Z_1 the limiting value of Z . D_o is the limiting strain rate. As suggested by Merzer and Bodner [14], the parameters n and Z_o are temperature-dependent material constants which have the order of $O(T^{-1})$ (Lindholm, et al [11]). The parameter m is given by

$$m = m_o + m_1 \exp(-\alpha_o \int_0^t S_{ij} \dot{\epsilon}_{ij}^{vp} dt), \quad (15)$$

where m_o , m_1 , and α_o are material constants.

As described above, the thermal fatigue life is directly related to the temperature and activation energy. It is worthwhile to mention that the Bodner-Partom model is typically appropriate since the temperature-dependent parameter n is a function of activation energy H and kT (Bodner [12]).

6 RESULTS AND DISCUSSION

In this paper, only temperature and elastic stress fields are reported. In the model, 34°C of ambient and water temperature is assumed. Fig. 9 shows the temperature contours of the cylindrical absorber. The maximum temperature is found to be 118°C at the beryllium-GlidCop interface where it is subjected to normal incident beam heating. After being diffused into beryllium, the rest of the bending magnet power is deposited onto the GlidCop surface. This results in the surface heating, which explains why the temperature reaches its maximum at the interface instead of on the vacuum surface. Along the radial direction of the beryllium disk, the temperature changes from around 100°C to 106°C . At about 60°C a high temperature contour is formed on the heating side of the absorber, which is expected to endure high thermal stress. On the cooling surface the highest temperature increase is only around 30°C . For the rest of the GlidCop, the average temperature increase is approximately 20°C .

High effective stress (maximum value of about 25 kpsi) appears on the beryllium vacuum surface (Fig. 10) while in the beryllium-GlidCop interface both high compressive radial stresses (Fig. 11) and circumferential stresses (Fig. 12) are observed. Since both areas are subjected to higher power input, the temperature gradients along these two areas are much higher than the surrounding materials; this generates high compressive stresses. However, due to the interface's geometry, the temperature gradients are nearly identical in all directions, resulting in a hydrostatic-like stress state. Thus, the effective stress in the interface is less than that on the vacuum surface.

During operation, the crotch absorber will be heated from ambient temperature to its steady-state temperature and cooled down. As a result, it will experience high compressive stress around the beryllium-GlidCop interface, and some of the areas will experience tensile stress. The magnitude of the tensile stress during heating is much smaller than the compressive stress because of extreme localized heating. During cooling, the effective stress follows the loading-unloading hysteresis loop in the stress-strain curve if the yielding point is reached and the material goes beyond elastic range. Having been subjected to high compressive stress, the interface area will experience tensile stress if the absorber reaches ambient temperature. The magnitude of the residual tensile stress is suspected to be much smaller than that of the compressive stress.

Since tensile stress, in general, is conducive to creation of a crack opening displacement (COD) on the crack tip, the accumulation of residual tensile stress can be used as an index of failure life prediction if thermally-induced plastic deformation is dominant and responsible for the fracture. The determination of tensile stress and the corresponding plastic strain require further investigation.

7 ACKNOWLEDGEMENTS

The authors would like to thank Mr. Bon Calayag for the e-beam welder test setup and Sushil Sharma for the valuable discussions. Editing by Catherine Eyberger is appreciated.

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Table 1: Material Properties

Properties	Symbols	Magnitudes
Magnetic Field	B	0.6 [T]
Photon Energy	E	7.0 [GeV]
Current	I	100 [mA]
GlidCop Thermal Conductivity	K_{GC}	$3.35 \left[\frac{W}{cm \text{ } ^\circ K} \right]$
Beryllium Thermal Conductivity	K_{BE}	$1.47 \left[\frac{W}{cm \text{ } ^\circ K} \right]$
GlidCop Thermal Expansion Coefficient	α_{GC}	$1.66 \times 10^{-6} \left[\frac{1}{^\circ K} \right]$
Beryllium Thermal Expansion Coefficient	α_{BE}	$1.12 \times 10^{-6} \left[\frac{1}{^\circ K} \right]$
GlidCop Young's Modulus	E_{GC}	$1.552 \times 10^7 \text{ } Psi$
GlidCop Young's Modulus	E_{BE}	$4.408 \times 10^7 \text{ } Psi$
Ambient Temperature	T_o	34 [°C]

Figure 1: Plane View Drawing of the APS Storage Ring Chambers

Figure 2: Plane View Drawing of the APS Crotch Absorber

Figure 3: Dimensioning of the Cylindrical Absorber

Figure 4: Finite Element Modeling of the Cylindrical Absorber

Figure 5: E-Beam Welder Experiment Setup

OFHC: P=1kW V=125kV i=8.0mA f=1.0kHz t=30sec M=700x

GCOP: P=2kW V=125kV i=16.0mA f=1.0kHz t=120sec M=700x

Figure 6: Microstructure of the OFHC, using 1 kW, Figure 7: Microstructure of the GlidCop, using 2 kW, moving at $200 \frac{m}{second}$; 240,000 cycles, 700X moving at $200 \frac{m}{second}$; 60,000 cycles, 700X

Figure 8: Analytical Solution of the Surface Temperature Heated by a Rastering Gaussian Power

Figure 9: Steady State Temperature Contour of the Cylindrical Absorber Cross Section

Figure 10: Effective Stress Contour of the Cylindrical Absorber Cross Section

Figure 11: Radial Stress Contour of the Cylindrical Absorber Cross Section

Figure 12: Circumferential Stress Contour of the Cylindrical Absorber Cross Section

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