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ABSTRACT—This paper reports on the design, and component testing of a dissipation circuit for a 560 MJ inductive storage supply. This storage supply is charged by a 5 MA battery power supply where both are part of a large pulse power test facility being upgraded under a U.S. Air Force contract. This battery upgraded supply (BUS) requires energy management at either the end of a test or as a means of aborting a test in the event of a fault. Energy management is accomplished by diverting the supply current into large dump resistors capable of dissipating the inductively stored energy through resistive heating. After the most of the energy has been resistively dissipated, a low residual current exists in the resistor circuit due to the applied battery voltage of the battery power supply. This current is interrupted with final interrupter switches in series with the dump resistors. Both the divert and final interrupt switches are explosive opening switches.

Details of BUS (a 5 MA battery power supply charging of pair of inductors to 560 MJ) are presented in companion papers. This paper reports on the details of the energy management components which include the high current divert switches, the energy dump resistors, the final interrupt switches, the switch containment vessels, and the robust buswork and clamping required to construct the high energy circuits.

Special design features of these components include low cost extruded aluminum switch elements for use in both the divert and final interrupt positions that have the designed in capability to open thermally in the event of an explosive switch failure. Other special design features include nonferrous containment vessels and compact stainless steel dump resistors whose geometry provides the required circuit resistance and also helps control the separation forces acting on the resistor plates.

INTRODUCTION

In designing an energy management scheme for the BUS program, one of the main design considerations was to arrange the power supply circuit in such a manner as to utilize proven circuit components. The primary components being the initiate switches, the divert switches and the final interrupt switches. By arranging the 5 MA battery power supply into two 2.5 MA circuits and further arranging these 2.5 MA circuits into the circuits seen in Fig. 1, the divert and final interrupt switch designs could draw heavily from existing opening switch technology developed by CEM-UT [1,2].

The 2.5 MA circuits are referred to as the high current switch (HCS) modules and are seen in Fig. 2. The two circuits are electrically isolated from each other but are

Fig. 1. BUS HCS circuit

Fig. 2. High current switch modules

configured to allow the two mechanical initiate switches [3] to be closed (connects inductors to battery supply for charging) by a single hydraulic actuator to provide virtually no jitter (~ 5 ms) as to the two separate switch closures.

Upon initiate switch closure, the current flows through the HCS module as per Fig. 3. At the end of a test or as a means of aborting a test, the explosive opening of these divert switches (Fig. 4) changes the return current path so that the current travels through the final interrupt switches and the stainless steel dump resistor plates before returning to the super bus. After dissipation of the energy in the dump resistors, the low residual current in the resistor circuit (applied battery voltage of the battery power supply) is interrupted by the explosive opening of final interrupt switches.

Details as to designs of each of the major components (HCS buswork, divert switches, dissipate resistors, final interrupt switches and switch containment) follows in this report.

HIGH CURRENT SWITCH BUSWORK

In doing the conceptual design work as to the HCS structural layouts, the overall BUS system resistive budgets drove the geometry of many of the buswork components. Buswork heating due to the electrical action of the BUS system currents (up to 5 MA) was calculated to be minor and no personnel hazard. Electrical action of the current did, however, help to dictate the geometry of the divert and final interrupt switches (described later in this report).

The geometry of these switch elements in turn dictated the buswork associated with the clamping of these elements into the buswork circuits. The length of a divert switch is 84 in. and much of the HCS buswork matches that height in that the element is clamped in a vertical orientation (Fig. 2).

With the conceptual design providing locations and dimensions as to the HCS modules relative to the superbus and the BUS inductors, an electromagnetic (EM) analysis was conducted to calculate the forces seen by HCS conductors as a result of both steady state and eddy currents. This analysis modeled two cases: (1) a catastrophic open circuit where the magnetic field of the inductor collapses and the full transport current in the buswork drops from 2.5 MA

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Fig. 3. HCS module circuit during charging

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Fig. 4. Dissipation circuit after divert switches opened

to zero in 10 ms and (2) the case of steady-state, full transport current (2.5 MA) in only one of the two 2.5 MA circuits that comprise the overall BUS system (somewhat asymmetric loads to buswork and clamping).

The analysis modeled both horizontal and vertical eddy circuits. The eddy circuit in the horizontal plane consisted of a loop formed by the two divert switch legs making up one module of HCS. This loop is closed by common points at the superbus/HCS interface joints and at the tee-section of the HCS center busbar where the final interrupt switches are mounted. The analysis predicts an eddy current which reaches 250 kA at the end of the catastrophic short circuit simulation. This eddy produces a current density of only 20% of that produced by the full transport current and it reaches that value only when the transport current has declined to zero.

A vertical eddy circuit was also investigated. This circuit was formed in the solid central conductor (connecting the tee-section end to the initiation switch) of one side of the HCS. The inductor field was mapped in detail in the vicinity of this plate and the mutual inductance was derived. The HCS vertical eddy circuit was modeled with the EM analysis codes LPRIME and TEXMAP developed at The University of Texas at Austin.

The TEXMAP model allowed the input of an even current distribution in the circuit and this resulted in a much higher self-inductance. The higher self-inductance produces a larger time constant and lower eddy currents. The analysis showed that the vertical eddy circuit in the HCS reaches 36 kA when the transport current has decayed to zero.

Because of these results and the results of other eddy current force analysis done on the superbus and inductors, the horizontal and vertical eddy currents in the HCS were assumed to be negligible and the maximum forces are assumed to occur during full transport current (2.5 MA per side).

A detailed current filament force analysis of the HCS and the superbus/HCS transition was also completed. Steady state, full-transport current in one side of the BUS (2.5 MA) was modeled. The model included one inductor, a section of the superbus between the inductor and the HCS, the superbus/HCS transition and one side of the HCS.

Because of the immense size of the computation matrix of this filament model, some streamlining procedures were utilized. Several variations of the model were used to provide the forces present on different components. Forces acting on superbus components were derived from a model in which the superbus was modeled finely (with 49 layers of filaments) and the HCS was modeled coarsely (5 layers). In this case, the coarse HCS model was an adequate representation because of the large relative distance between the superbus (where the elements of interest were) and the HCS. Forces acting on the HCS elements were derived with a fine HCS model (43 layers) and a coarse (5 layers) superbus.

In all cases, forces acting on particular conductors were conservatively calculated by multiplying the forces on the central filament by the number of layers in the conductor. Several cases of simple geometry were run to test this method, comparing the results derived from filament analysis with those derived from closed form solutions. These comparisons showed that the method was accurate. The fineness of filament layers was also checked by comparing forces derived from a 29 layer HCS with those from the 43 layer HCS. The forces from these two methods exhibited minimal differences (2%).

The methods above greatly facilitated the rapid calculation of forces. Typical runs of the filament code required approximately eight hours to produce the necessary forces. This computation time would have been 30 to 40 times longer if parts of the model were not coarsened and if the central filaments were not used as representative filaments.

The filament model was used to predict forces during full current (2.5 MA) in one side of the HCS and superbus. Although current will not simultaneously flow in all switches, the model simulated this situation and calculated maximum forces on all switch elements and the tee-section at the same time. The dissipate resistors were not included in the filament model; these components were modeled with closed-form solutions.

The results of the filament model were reduced to forces and force distributions on various parts of the superbus and HCS. Maximum pressure developed on any superbus conductor was 39.9 psi and the maximum pressure on any HCS conductor was 180 psi.

With a mapping out of the forces seen by the HCS buswork, clamping schemes were devised that utilize clamping methods used successfully on the various buswork at CEM-UT that make up the Balcones Homopolar (BHPG) power supply to the 9 MJ single shot gun programs 10-m long railgun. Portions of this buswork has been successfully tested to 3.4 MA [4].

DIVERT SWITCH DESIGN

The desired features for the explosive divert switch design were:

- (1) Highly reliable (proven technology)
- (2) Extremely safe operation
 - (a) use of insensitive and easily handable explosives (detonating cord)
 - (b) use of redundant switch elements and firing controls
 - (c) completely contained switch openings

- (3) Low cost switch consumables
 - (a) extruded aluminum elements
 - (b) commercially available explosives
 - (c) reusable element backing plates
- (4) Low cost manpower requirements as to turnaround
 - (a) quick clamping methods
 - (b) light weight elements and containment vessels

The divert explosive switch elements were an optimized version of switch elements used on the BHPG's. Those switch elements have been successfully tested with predictable performance under a variety of discharge current levels up to the full current duty (1.25 MA) the BUS divert switches would be required carry and open.

The metal cross-sectional area of the switch bus bar and the different switching interruption components are sized based on electrical action requirements. Because the discharge circuit is split into two parallel paths of equal current it can be shown that the required bus area for individual circuits is halved.

$$A_{1/2 \text{ current}} = \sqrt{\frac{\int [i/2]^2 dt}{gt}} = \sqrt{\frac{1/4 \int i^2 dt}{gt}} = \frac{1}{2} A_{\text{Tot. current}}$$

The metal interrupter elements in the switches are sized to be near melting at the end of a test. This will ensure passive thermal opening in the event explosive opening is not achieved. Full energy tests require an element 84 in. long where the minimum cross sectional thickness is 0.375 in.

The cross section view of the BUS divert switch elements is seen in Fig. 5. As seen from the drawing the use of stress concentrations (dovetail grooves and surface notches) are employed to insure an accurate and highly predictable shearing of the explosive gap material. The 3/8 in. thick elements have been successfully cut with 100 grain/ft detonating cord where the detonating cord is placed down the dovetail grooves. Given the aluminum switch elements are near melting at the end of a test, a close look at the thermal issues regarding the detonating cord was conducted.

A one dimensional (1-D), transient, conduction and convection heat transfer model of 100 grain/in. detonating cord was constructed (Fig. 6). In this model, the region from model center to $r_1 = 0.1175$ in. is the PETN explosive, with a melt temperature of 104°C . The subsequent regions are: $r_3 = 0.102$ in. (polypropylene), $r_5 = 0.117$ in. (thermal insulation to outer diameter of detonating cord, if required), and $r_6 = 0.128$ in. (radius of inscribed circle within switch dovetail). The radii r_2 and r_4 are mean radii (nodes). For the

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Fig. 5. BUS divert switch element

purpose of this model, the detonating cord was allowed to touch the aluminum surface in three locations. Total circumferential contact length was calculated to be 0.24 in. The analog electrical circuit of this model is also shown in Fig. 6.

Two parallel numerical computations were run using this model. The dynamic boundary temperature at circuit input (T_A) is generated using the electrical action in the narrow region of the explosive aluminum gap due to a full current (5 MA) for a normal test duration (3 s charge, 5 s operate). This temperature input is equivalent to forcing all the detonating cord circumferential length allowed to contact the switch, to do so at the hottest part of the aluminum switch (the explosive gap) which is conservative. With this boundary condition solved, temperatures in each material were calculated.

First simulations using this model assumed no thermal insulation on the detonating cord (i.e., the polypropylene extends to r_5). In this case, at time = 8.1 s, the aluminum has reached 696°C (very near melt), the outer polypropylene layer 186°C, the inner polypropylene layer 104°C, and the PETN core 78°C, which is comfortably below the melt temperature of 104°C. Actual core temperature could be still lower considering that the outer polypropylene layer is beginning to melt, which will clamp temperature in that region due to its latent heat of fusion. Based on the results of this model, it is evident that detonating cord performance will not be unduly affected by proximity with the hot switch material.

DISSIPATE RESISTORS

As previously mentioned, successful opening of the divert switches moves the circuit current over into the stainless steel

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Fig. 6. Detonating cord heat transfer analysis

dissipate resistors. The full rated design duty for this component per 2.5 MA circuit is the dissipation of 280 MJ of energy. To accomplish this resistively required a number of considerations. First the resistor hardware had to be made as compact as possible to fit within experimental lab space. It needed to be nonferrous due to the large EM fields present. After heating up the material should preferably not undergo annealing.

Stainless steel was chosen because of its nonferrous, strength and machinable properties. A calculation was done using $Q = mC_p\Delta T$ to calculate the mass of stainless required so as to not have the resistive heated upper temperature exceed a boundary set at 850°F. Stainless plate with the calculated mass required had a much too low resistance value so the decision was to up the resistance value by cutting the stainless steel plate into a serpentine pattern. Early analysis showed that the separation forces acting on the serpentine current pathways would be unmanageable. However, the forces were manageable if the plates were doubled with the current path effectively running opposite in the second plate. The side by side plates are separated in Fig. 7 to better show the current paths. An HCS module in Fig. 2, has two pair of stainless steel plates per 2.5 MA circuit leg.

Force analysis on the serpentine dissipate resistor plates assumed a worse case steady state condition of 2.5 MA per circuit. Forces acting on the plates include both the separation forces between serpentine paths within the same plate, and the separating forces between neighboring resistor plates. These calculations were made using a code based on the Biot-Savart law, and accounted for higher order terms such as attractive forces between non-nearest neighbor plates in which current flows in like directions. The overall result is that the forces tending to stretch out a single resistor plate are effectively cancelled.

By doubling the stainless plates the additional mass also lowered the final temperature to 448°F. Though considerably below the design limit this final temperature could pose a possible personnel or other component hazard. To look at the final resistor temperature effects, a 1-D, electrical analog transient conduction heat transfer analysis of the dissipate resistor assembly, surrounding insulation, and buswork was done to evaluate the temperature exposure of the initiate switch and other nearby components. This code predicted that with no forced convection, exposed busbar surfaces will reach and hold temperatures of 150°F for approximately three hours, presenting a potential personnel safety hazard. However, assuming forced convection by

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Fig. 7. Energy dump resistor plates

approximately 24 fps of standard condition air (easily obtainable with a 36 in. commercial fan), accessible outer busbar temperatures remain quite low, and the initiate switch never exceeds its end-of-test temperature of 134°F. These results indicate that management of dissipate resistor heat following tests does not represent a major safety or equipment exposure problem.

Another thermal issue was the thermal growth of the dissipate resistor. Because of its geometry the thermal growth of the dissipate resistor is difficult to analyze accurately. An 8.5:1 scale resistor was fabricated from 304 SS and evaluated as to its dimensional changes under mechanical load and while heated to 825°F (much hotter than actual service of the full-scale resistor plates). The measured thermal growth agreed closely with the calculated values assuming a monolithic plate of the same overall dimensions. The experimentally determined spring constant of the plate in the serpentine direction enabled calculation of the load required to constrain resistor growth in this direction (i.e., to force thermal expansion to accumulate within the interserpentine kerfs). In the vertical direction, where there are no critical connections at each end, expansion will be allowed, with extra clearance in the through bolt holes to protect insulation.

Also using four of the 8.5:1 scale 304 SS dissipate resistors and appropriate insulating materials and copper, a scale mockup of the HCS subsystem in the region of the divert/dissipate circuits was constructed in order to measure its resistance and inductance. Using a high precision LCR video bridge, the inductance of this subassembly was measured to be 68 nH. The inductance scales linearly with the scaling factor, so actual inductance will be approximately 0.58 µH.

Determination of the inductance is required to calculate accurately the magnetic energy generated by the divert switches while opening and commutating current into the dissipate circuit. This energy must be added to the explosive energy in designing the divert containment vessel. A detailed resistance analysis of this subsystem, using average elevated temperature resistivity values, gives a dissipate resistor circuit resistance of 0.590 µΩ.

A summary of the total dissipation resistors circuit (8 plates) is listed below:

- dissipates 560 MJ of energy over a 200 ms time frame
- 13,350 lb of stainless steel (8 plates)
- calculated resistance of 1.18 mΩ (total circuit)
- spring constant 1,250 lb/in.
- inductance of 590 nH
- maximum temperature reached after dissipation = 448°F

FINAL CURRENT INTERRUPT SWITCHES

Following dissipating of residual inductor energy, a low residual current continues in the resistor circuit due the applied voltage of the battery power supply. To interrupt this current single gap explosive switches (two per HCS module) are opened. The element is to be extruded with dovetail explosive grooves and should require no secondary machining other than cutting to length (28.5 in.). The interrupt switch duty should be no greater than opening

250 kA with stand off voltage requirements being several hundred volts.

SWITCH CONTAINMENT

Due to a number of constraints, an initial design decision was made to completely contain both the divert and the final interrupt switch openings. To do this has proven difficult because of large magnetic fields generated by the inductors. As a part of the EM analysis of HCS modules previously mentioned, an axisymmetric inductor model was produced with TEXMAP, with iron rings included around the inductor at the same radii as the containment vessels would appear. The model showed that all containment vessels will easily saturate in the inductor field present during full transport current. Therefore, the containment vessels would see large magnetic forces were the vessels fabricated using conventional ferrous steel.

Preliminary design work focused on the use of either aluminum or stainless steel as the material for the vessels. Aluminum was chosen because of reduced cost and less overall weight to provide ease of handling. As seen in Fig. 8, one vessel contains either two divert or two final interrupt switches. A divert switch containment vessel is a semicircular shell with 1 in. wall thickness and has a 42 in. internal diameter. The final interrupt containment is similar but smaller with a 33 in. internal diameter and also has a 1 in. wall thickness.

The containment vessels designs were analyzed to see if each could withstand the shock and quasistatic pressures generated each test by the exploding detonating cord and the magnetic energy released in the switches in the form of expanding gas caused by arcing [5]. This total energy was converted to an equivalent weight of TNT and divided into each strand based on the switch length. This weight was then added to the explosives weight of the cord to arrive at a total explosives weight per unit length. This weight was then multiplied by 20% to account for uncertainties in material properties and construction which is recommended by military design guides for explosives design.

Shock loads were predicted in two ways. The first was to lump 12 in. of cord at a single point on the length and predict peak reflected pressure and impulse at various points on the vessel wall using standard surface burst prediction curves for

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Fig. 8. Explosive switches containment

TNT. This massing of cord was done to account for the difference between 3-dimensional (3-D) expansion, which the curves were based on, and the 2-dimensional (2-D) expansion occurring in the containment. This 2-D expansion is caused by the confining effect of adjacent explosives along the cord length.

The pressures predicted at each selected point on the vessel wall were averaged and multiplied by 1.75 to account for response effects of multiple arrival times. Impulses at each point were summed and the result multiplied by 1.75. These values were used as the design shock load.

The second method involved use of theoretical curves for prediction of reflected pressures and impulses caused by line charges. The design unit explosive weight in each strand was doubled to account for the reflective surface of the switch for use with the airblast curves. Again peak reflected pressures were averaged and impulses were summed with the results being multiplied by 1.75. This method produced extremely high loads close to the switch but dropped off significantly as standoff increased. Because of the stiffness of the vessels, the response was sensitive to these high pressures making these load predictions controlling values.

Quasistatic pressures were predicted by taking the entire quantity of explosives in the detonating cord and the total magnetic energy released and converting both to an equivalent weight of TNT. This weight was increased by 20% as described above. It was divided by the total vessel volume to arrive at a weight to volume ratio. This ratio was used with an empirical curve for peak gas pressure produced by a TNT detonation in a partially contained chamber to determine the peak quasistatic pressure. Since the vessel will essentially be totally contained, the duration of this pressure is taken as infinite.

The containment vessels were analyzed for dynamic loads by computing single-degree-of-freedom properties and numerically integrating the equations of motion while applying transient loads. These properties were obtained by statically analyzing the vessel to obtain peak stress and deflection at a given internal pressure. This pressure was then scaled to arrive at a resistance and stiffness. Use of these static models was possible because the dynamic response of the vessel had to remain elastic, thus the stresses and deflections were scalable.

Two type of static analysis were performed. The first was finite element model of the shell quarter section and the flange. This model was used to determine the local stresses caused by the bolts, dowels, and gussets. This model used plate elements for the shell and gusset and 3-D solids for the flange. The second type of model was a 2-D analysis using frame (3 dof/node) elements for the shell and flange. This

model showed bending stresses occurring near the shell/flange interface which controlled the ultimate resistance of the vessel.

Since the vessels are inherently stiff, the maximum response of the structure occurred very early in the load-time history. This meant that the vessel was responding primarily to the shock loads with the quasistatic loads having less effect. The high pressures occurring at close standoffs tended to drive the design of the shells. The loads occurring over the circumference of the shell was averaged to avoid too much conservatism in the analysis and allow a reasonable design. The containment design will be fully fabricated and tested using high explosives to simulate operational explosive and magnetic loads.

CONCLUSIONS

The battery upgraded system, when fully operational, will provide EML researchers with an extremely high energy pulsed power supply. The high current switching and dissipation subsystems have been designed to be highly reliable yet very functional as to routine testing operations.

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