Railgun Solid Armature Scale Modeling

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Abstract—This paper describes a methodology for using two finite element (FE) codes, MAGTRPL and DYNA, to perform electromechanic, thermal, and structural modeling of railgun solid armatures. We analyze subscale armature experiments and present results that begin to identify the important engineering limits for materials used in armature fabrication.

INTRODUCTION

During a railgun launch, a high-current pulse is injected into the rail-armature system for a very short period of time. The current rise time is typically less than 1 ms. As a result, the current flow in the rail-armature conductors is highly transient and the current (or magnetic flux) is not fully diffused into the conductors. Current distribution in the rails and armature conductor cross sections is highly non-uniform, as are the temperature, EM force, stress and strain distributions. This paper presents an analysis methodology that may be used to calculate these transient quantities. Analysis results may help engineers understand the armature's electric, thermal and structural requirements and design better rails and armatures.

Three-dimensional (3-D) modeling is critical for EM analysis of viable EM gun launch packages and rails. It enables the accurate geometric representation of the conductors as well as non-linear material properties, such as electrical conductivity-temperature relationships. The EM and thermal relations are strongly coupled and must be calculated together. Ohmic heating is generated due to the electrical resistivity, and the electrical resistivity is temperature dependent. The structural problem is only weakly coupled with EM/thermal calculation and may be solved separately. This is because rail and armature deflections are very small — on the order of 0.25 mm or less — and their effect on the EM/thermal performances is negligible, except for the rail-armature contact area, where a small deflection can cause a gap or contact pressure variations which can have a large effect on current distribution. Electrical performance at the rail-armature interface is assumed to be a galvanic, ideal contact. In reality, the sliding contact's electric performance is a function of the contact pressure, both magnetic and mechanical preload, and all the structural/EM/thermal problems should be solved together. The armature is assumed to be stationary relative to the rails.

APPROACH

A 3-D, transient, EM/thermal, finite element code was used for EM/thermal analysis. Maxwell's equations that describe the EM problem are expressed as:

\[ \nabla \times \mathbf{E} = -\frac{\partial \mathbf{B}}{\partial t} \]  \hspace{1cm} (1)

\[ \nabla \times \mathbf{H} = \mathbf{J} + \frac{\partial \mathbf{D}}{\partial t} \]

Constitutive relationships are expressed as:

\[ \mathbf{J} = \sigma \mathbf{E} \]  \hspace{1cm} (2)

\[ \mathbf{B} = \mu \mathbf{H} \]

The displacement current, \( \mathbf{D} \), may be neglected for our problem. The magnetic vector potential, \( \mathbf{A} \), and the electric scalar potential, \( \mathbf{V} \), are employed.
\[
B = \nabla \times A \\
J = -\sigma \left( \frac{\partial A}{\partial t} + \nabla V \right) \tag{3}
\]

Maxwell’s equations can be expressed in terms of \( A \) and \( V \) as:
\[
\nabla \times \left( \frac{1}{\mu} \nabla \times A \right) = -\sigma \left( \frac{\partial A}{\partial t} + \nabla V \right) \tag{4}
\]

There are three equations, one for each vector component of \( A \), and four unknowns \( A_x, A_y, A_z, \) and \( V \). The fourth equation comes from a condition that requires the divergence of \( A \) to be zero, a scalar equation.

EM analysis requires modeling both conductors and the non-conducting regions surrounding them. Because a large volume of space has to be modeled in the EM problem to establish the boundary condition at infinity, higher-order quadratic elements are used to insure the accuracy of the solution while employing a reasonable number of elements.

Armature material resistivity and specific action data used are shown in Fig. 1. The data was measured at CEM-UT and by Tucker and Toth.

The gun-current profile that was measured during the test was traced (stepwise linear approximation) in the analysis by a number of time-step calculations (6 time steps in the sample problems). Finite element size must be small enough to accurately represent the smallest magnetic flux diffusion process into the conductor body. The smallest diffusion depth in the armature happens at the first time step and is calculated as approximately 7.6 mm for the sample problem. The maximum armature element size in the direction of diffusion was only 2.5 mm for the sample problem.

The EM/thermal code returns \( A \) and \( V \) potential and temperature data at node points. The current density, \( J \), and the magnetic flux density, \( B \), are solved for at gauss points using equation (3) and the quadratic shape functions. The \( J \) and \( B \) values at gauss points are then used to calculate \( J \) and \( B \) values at node points. Nodal values which are calculated from neighboring elements (although calculated at the same node) may differ and therefore, their average values were used.

The finite element structural code DYNAT was used for structural analysis. Due to the combination of heating and loading above the elastic limit, structural analysis of armatures require a highly nonlinear material calculations. This can make the convergence of the material iteration loop very difficult. In such cases, using linear elements (8 node brick, 1 gauss point) can provide a convergence rate far superior to quadratic elements. A large number of linear elements were used to grid rails and armature. The non-conducting region no longer needs to be modeled.

The temperature-dependent elastic-plastic armature material behavior is represented by bi-linear stress-strain curves at a series of elevated temperatures (Fig. 2). Temperature-dependent Young’s modulus data were obtained from [3]. Temperature-dependent tensile and yield strengths and elongation data were from [1], [4].

![Image of resistivity vs. specific action data](3910.0297)

**Fig. 1.** Resistivity vs. specific action data for 7075 aluminum (armature material)

![Image of stress-strain curves](3910.0286)

**Fig. 2.** Temperature dependent mechanical properties for 7075-T651 used for armature structural analysis [1], [3], [4]
The $\mathbf{J} \times \mathbf{B}$ force and temperature information must be transferred from the EM/thermal grid to the structural analysis grid. Interpolation between the two different grids is necessary. In order to make the interpolation process easier, the EM/thermal quadratic mesh was subdivided into several smaller linear elements; thus interpolation for each element is confined within each quadratic element (Fig. 3). Quadratic element shape functions were used to interpolate $\mathbf{J}$, $\mathbf{B}$, and $T$ nodal data to linear element gauss points. The $\mathbf{J} \times \mathbf{B}$ force was then calculated from the interpolated $\mathbf{J}$ and $\mathbf{B}$ data. It is very important, especially in the high $B$-field regions, that the linear elements occupy exactly the same volume described by quadratic elements, as the total force resultant is the integral of the $\mathbf{J} \times \mathbf{B}$ force density over element volume. Accurate element volume calculation is equally important. In order to verify the interpolation results, we compared the total resultant force calculated using each grid. The agreement was within 2%, which is acceptable for this purpose.

In addition to the EM force, the launch package is also loaded with inertial forces in the direction opposite to the armature motion. The inertial load is in the form of a body force density, whose magnitude is the material mass density multiplied by the package acceleration. The package acceleration is simply the total armature driving force divided by the total package mass. Rail-armature sliding friction was neglected. Loss of armature mass at the rail interface due to sliding was neglected. The sum of the armature driving force is always equal and in the opposite direction of the inertial force sum, and they are in equilibrium in a quasi-static sense. All other dynamic effects were neglected, as the armature was assumed to be stationary relative to the rail. Payload-armature contact interface stress was also calculated.

**Example**

To demonstrate the utility of this approach, the method was used to provide insight into the material performance of subscale armatures. Two tests were selected: monolithic contact armature (MCA) test 17, in which the armature failed, and MCA test 19 in which the armature performed successfully. Fig. 4 presents the rail, armature and payload schematic of the test articles. These two tests were carefully selected because we were trying to gain insight into engineering guidelines for this material. Tests 19 and 17 are similar in radial and axial stress and current. Temperature is the parameter showing the largest change at the time of the failure. Load reversal can produce fatigue related events and loading rate can produce strain rate effects. Because there was no load reversal and the loading rate of the two experiments were similar, it is postulated that the instability and failure was due to the temperature rise. This allows some statement about material performance as a function of temperature. The conditions for these scale tests were selected to reproduce the temperatures and inertial loads that would be experienced by the armature material when used in a full-scale tactical launch package. The current waveforms used for the two tests are presented in Fig. 5. Also presented in the figure is a table listing the electrical action.

![Figure 3](image1.png)

Fig. 3. Methodology for converting from quadratic EM grid to linear structural grid

![Figure 4](image2.png)

Fig. 4. Sub-scale test rail, armature, and payload schematic (only one of the two rails is shown)
experienced by the armature at each calculated time step. The action provides an indication of the average temperature of the armature. Experimentally, the armature failure in test 17 was identified by a discontinuity in muzzle volts between time steps 5 and 6. By using the combined EM and structural codes, it was demonstrated that the body forces in the two armatures were nearly identical before the failure in test 17.

Fig. 6 presents the temperature, axial, and radial body force profiles of MCA 19 at time step 4 and MCA 17 at time step 5. To further understand the failure mechanism and the material behavior, strain plots for the armatures are presented in Fig. 6e. Notice that the maximum plastic strain in MCA 17 is migrating out of the root area of the armature and moving toward the contact arm. The predictions also show that the strain has not yet reached the failure strain level for this material at the given temperature. In a similar manner, axial and radial body force and current at time step 5 for MCA 19 and time step 6 for MCA 17 are seen to be similar. Now, the contours of plastic strain in Fig. 7e provides a graphic representation of the failure mechanism. A gap between the dummy payload and the rail has allowed the hot armature to strain to failure. Comparison of the deformed shape in Fig. 7e to pieces of the armature recovered after the test show good correlation.

CONCLUSION

A method of handshaking between EM/thermal and structural finite element codes has been developed and used to predict experimental results. The results are helping designers to understand engineering limits for materials at elevated temperatures and loads representative of those required for the operation of full scale tactical launch packages. The importance of the armature sabot interface has been demonstrated, and new insight gained into the design methodology for these components.

REFERENCES

Slk 19
Step 4
Current = 560 KA
Action = 180 x 10^6 A^2s
Avg/Spec. action = 11,490 A^2s/mm^4

Slk 17
Step 5
Current = 560 KA
Action = 198 x 10^6 A^2s
Avg/Spec. action = 12,640 A^2s/mm^4

A) Temperature Profile

B) Temperature Profile

373°C
423°C
473°C

383°C
443°C
503°C

C) J x B axial component

6.0 x 10^10 N/m^3
8.0 x 10^10 N/m^3
1.0 x 10^11 N/m^3

6.0 x 10^10 N/m^3
8.0 x 10^10 N/m^3
1.0 x 10^11 N/m^3

D) Radial component

4.0 x 10^10 N/m^3
6.0 x 10^10 N/m^3
1.0 x 10^10 N/m^3

4.0 x 10^10 N/m^3
6.0 x 10^10 N/m^3
8.0 x 10^10 N/m^3

E) Plastic Strain

7.14%
5.8%
6.5%

5.1%

9.68% (interior)
8.9%
7.9%
6.9%

Fig. 6. Armature temperature, J x B force density, and plastic strain profiles (shortly before failure of slk 17)
Fig. 7. Armature temperature, J x B force density, and plastic strain profiles (shortly after failure of silk 17)