Low-Velocity Impact to High-Temperature Low-Sag Overhead Conductors

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Low-Velocity Impact to High-Temperature Low-Sag Overhead Conductors

A Thesis

Presented to

the Faculty of the Daniel Felix Ritchie School of Engineering and Computer Science

University of Denver

In Partial Fulfillment

of the Requirements for the Degree

Master of Science

by

Daniel H. Waters

June 2016

Advisor: Dr. Maciej Kumosa
ABSTRACT

High-Temperature Low-Sag (HTLS) conductors, such as Aluminum Conductor Composite Core (ACCC), improve infrastructure to support the delivery of power to meet the nation’s increasing demand for electricity. Their response to low-velocity impacts during transportation, installation or in service, however, has not been addressed in the past. Therefore, this study investigates both experimentally and numerically mechanical effects associated with transverse low-velocity impacts on energy dissipation by the conductors subjected to either free or constrained end conditions and large axial tensile loads. Impact experiments were conducted using a newly designed and manufactured testing apparatus. The experimental work was strongly supported by non-linear static and dynamic finite element analysis. It has been determined that ACCC exhibited very good resistance to impact under constrained end conditions with and without axial tension. It was also identified that the most damaging condition to the conductors under impact is the free end situation when conductors were allowed to develop severe bending.
ACKNOWLEDGMENTS

Finally at the culmination of my Masters work, there are many people I am obliged to thank. By many days, and countless hours, their assistance gave rise to this research and my thesis, and for that they will ever have my deepest gratitude.

This work would not have been possible without the support of the NSF Center for Novel High Voltage/Temperature Materials and Structures. In particular, I am most grateful to the Western Area Power Administration (WAPA), the Bonneville Power Administration (BPA), the Tri-State Generation and Transmission Association and CTC Global for providing materials, expertise, and guidance throughout the entire study.

Many thanks to my advisor, Dr. Maciej Kumosa, for his overall support, and for continuously believing I could aspire to this level of graduate education and science.

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I express my appreciation towards Mr. Jon Buckley, for his friendship, teaching, valuable insight, and extensive knowledge of manufacturing, without whom I would have not been able to manufacture a unique impact fixture so critical for my research.
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1.0 INTRODUCTION

The increasing consumption of energy and demand for electricity has put a significant strain on the current electrical grid, and utilities must meet the ever increasing demand for electricity by upgrading or replacing the existing infrastructure. The grid, shown in Figure 1.1, is a vast network of overhead transmission lines, substations, and power generation plants. As it currently exists the electric grid can be classified into three categories (Figure 1.2):

- Generation, with facilities that generate power such as coal fired power plants, wind turbines, and hydroelectric stations.
- Transmission, with high-voltage (HV) overhead conductors to transmit the power from the generation stations over long distances.
- Distribution, where power is distributed across more densely populated areas to the end user.

The scope of this thesis focuses on the resistance to mechanical damage of the HV overhead conductors used as transmission lines, a main component of the power transmission network.
There are four major overhead high voltage conductor designs currently used as transmission lines (Figure 1.3).

- Aluminum Conductor Steel Reinforced (ACSR)
- Aluminum Conductor Steel Supported (ACSS)
- Aluminum Conductor Composite Reinforced (ACCR)
- Aluminum Conductor Composite Core (ACCC)

The conventional conductor designs of ACSR and ACSS using steel and aluminum are limited in the amount of power they can transmit by operating temperatures. ACSR is a traditional design which has been standard in the industry since the early 1900’s (Thrash 2013). As the transmission line temperature increases with an increase of power, thermal expansion of the conductor can cause line-sag to exceed safe limits leading to potential short-circuits with trees or surrounding structures. In August of 2003, sagging lines caused a blackout across much of Ontario and the eastern United States (Wald 2004). One method considered by the power transmission industry to keep up with the ever increasing demand, is to re-conductor existing rights of way with a new class of High-Temperature Low-Sag (HTLS) transmission lines (Jones 2006). According to Clairmont (2008), and Lancaster (2011), the cost of HTLS conductors are 2-12 times more expensive than that of conventional conductors, but the same diameter and weight HTLS conductor can carry twice the current without the need to replace existing towers. Two such HTLS conductors are ACCC and ACCR, both of which utilize composite load-bearing components that are lighter, stronger, and allow increased operating temperatures over ACSR without losing strength to annealing or exceeding the minimum sag clearances.
ACCC, shown in Figure 1.4, is manufactured by CTC Global and has a hybrid Polymer Matrix Composite (PMC) core consisting of carbon fibers with a high temperature epoxy resin surrounded by a galvanic barrier of glass fibers with the same resin (Alawar, Bosze and Nutt 2005). The conducting strands consist of extremely soft 1350-O aluminum with a trapezoidal cross-section. The O temper, in accordance with ANSI H35.1, indicates the material is annealed to attain its lowest strength temper. Despite the potential benefits, some utilities are reluctant to use these novel HTLS conductors, owing to a combination of unfamiliarity in material behavior of the composites under long term in service conditions and a poor knowledge of the reliability of the composite design (Wald 2004). Several past incidents have occurred where ACCC conductors failed during or after installation:

- In 2005/2006, three failures during the installation of a 15.7 mile (25 km) line in Texas where the PMC core failed during installation operations (Berger 2006).
• In 2008, three failures of a 220 kV line outside Warsaw, Poland where the lines started dropping within six months of being energized. An example of the failure is in Figure 1.6 (Knych August 2009).

• A November 2010 breakage in Indonesia 10 days after completion of installation shown in Figure 1.5 (CTC Global Litigations 2013).

• Failure of a 345 kV line during stringing operations in Salt Lake City in 2011. Damage is shown in Figure 1.7 and Figure 1.8 (Kumosa 2011).

The ACCC failures that occurred in service and during installation are suspected to be a result of damage incurred at the time of installation. These failures were unexpected, and the cause of most was unknown. Noticeable damage to the aluminum stranding was evident, but analysis of the composite core near the fracture locations yielded little to no decrease of flexural strength (Kumosa 2011).

![Components of ACCC Conductor](image)

Figure 1.4: Components of ACCC Conductor
Figure 1.5: Example of 2010 Indonesia failure (CTC Global Litigations 2013)

Figure 1.6: Two examples of 2008 ACCC failures in Poland (Knych August 2009)
Several possible scenarios have been identified which have the potential to cause similar damage to HV transmission lines and contribute to similar failures:

1. Loss of control of the conductor tension leaving a spool and passing over travelers from tower to tower during installation could cause the conductor to bounce off a traveler and collide with either the traveler or the support structure of the transmission tower.
2. Installation tools, rocks, or other objects might be dropped or fall on the conductor.

3. Trees or tree limbs may inadvertently fall on conductors, both in-service and during installation.

4. Poor installation procedures could result in unnecessary bouncing of the conductor during installation.

5. Mishandling during transportation or installation could cause excessive bending, torsion, or other mechanical stresses.

These possible damaging scenarios can all be characterized as low-velocity impacts: \(< 10 \, \text{m/s} \, (32.8 \, \text{ft/s})\) of varying degrees of severity. Damage from high velocity impacts, such as ballistic projectiles, is outside the scope of this research.

Traditional conductors, like ACSR, are based on steel and aluminum, metals that deform plastically prior to failure when stressed beyond their yield strength. This plastic behavior increases the material toughness and makes ACSR more resilient to mishandling and damage from low-velocity impacts because it can dissipate more energy up to fracture and the point of failure. Fiber-reinforced composites, such as the PMC core of ACCC, exhibit high strength to weight ratios, but as brittle composites, they can be more susceptible to impact damage during manufacture, transportation, installation, and use in service. The hybrid epoxy glass/carbon fiber core of ACCC exhibits no plastic behavior prior to failure when loaded in tension and therefore, has less ability to absorb energy before fracture.

PMCs also exhibit damage characteristics very different from metals, and when exposed to impact loading can incur internal damage with little to no indication on the
surface, or Barely Visible Impact Damage (BVID) (Zhang and Richardson 2007) (Cantwell and Morton 1992) (Mitrevski, et al. 2005) (Poon, Benak and Gould 1990) (Agrawal and Singh 2014). This BVID can lead to catastrophic failure as happened with the dropped ACCC conductors; thus the power transmission industry wants conductors such as ACCC to be evaluated for damage resistance to possible impacts during installation.

Extensive research has been conducted on the impact resistance of fiber-reinforced PMC laminates exposed to impact (Agrawal and Singh 2014) (Cantwell and Morton 1991) (Cantwell and Morton 1992) (Poon, Benak and Gould 1990) (Zhang and Richardson 2007), where damage from low-velocity and high-velocity projectiles was investigated given various properties of fibers and matrix. The results, however, are limited to flat laminate plates typically used in aerospace and ballistic protection applications. No research has been found pertaining to any low-velocity impacts in stranded overhead conductors, or HTLS conductors, or more specifically ACCC.

Limited work has been conducted to help characterize the mechanical properties of ACCC under static loading conditions (B. M. Burks, et al. 2009) (Burks, et al. 2010) (Alawar, Bosze and Nutt 2005) (Burks, Armentrout and Kumosa 2010); research specific to the bending strength of the PMC rod was conducted by Burks, et al. to determine the minimum static bend radius to initiate damage at the Glass/Carbon interface or at any point within the composite rod (B. M. Burks, et al. 2009). Burks also quantified the residual tensile strength of the hybrid composite rod after excessive static bending (Burks, et al. 2010), a result that can only be applied to static loading conditions of the composite rod. The lack of any work to evaluate ACCC for its resistance or response to
impact leaves an opportunity for improvement in understanding both the damage mechanisms in composite core conductors and the conditions that can be most damaging and should be avoided. Work pertaining to the impact resistance of ACCC will allow utility companies to better understand the advantages and limitations of using HTLS conductors.

Currently, any low-velocity impact damage tolerance of ACCC conductor is unknown and a standard practice for determining a conductor’s resistance to impact does not exist. Several ASTM standard test methods exist for impact testing of standardized samples of polymers and metallic materials, including but not limited to:

These standards utilize either a falling weight impact device, as in the Garner Impact to determine energy required for breaking flat, rigid plastic specimens, or a pendulum impact machine to measure the energy to break samples of a specified geometry with a given support mounting as for the Charpy and Izod impact resistance. All methods require failure to occur with one impact and have a specified impact velocity. Charpy impact specimens must be $55 \times 10 \times 10 \, mm$ with a notch machined in the middle; they are then simply supported for the pendulum to swing through. Izod impact specimens must be $75 \times 10 \times 10 \, mm$ with a machined notch $28 \, mm$ from the end and are supported as a cantilever beam. These methods as published are not suitable to determine the full-scale impact resistance or subsequent structural integrity of conductors subjected to various impact scenarios and boundary conditions.

From a numerical analysis perspective, the dynamic response of materials and structures subjected to impact conditions can be modeled using Finite Element Analysis (FEA). FEA involves discretizing a mathematical model into a mesh of finite elements connected at nodes with field quantities, such as stress, displacement, and temperature approximated across elements by simpler mathematical approximations. The mesh which is a simulation of a real structure is numerically represented by a system of equations simultaneously solved for unknown field values at the nodal locations. FEA has advantages over other numerical analysis techniques in that modeled geometry has no restrictions, material properties can be anisotropic, boundary and loading conditions can be applied at any point in the mesh, and a model can consist of multiple components having different properties. Many commercial software packages exist that make FEA efficient to apply.
2.0 PROBLEM IDENTIFICATION

The unexpected failures of ACCC in service described in the previous chapter suggest there are circumstances that can cause damage to the composite core. Not knowing what conditions may be damaging to ACCC, preliminary tests of transverse impact to ACCC conductors were carried out at the beginning of the project to identify the potential for damage under particular conditions.

2.1 Experimental Setup

Two initial tests were carried out that involved dropping lead bricks from a fixed height onto conductor sections supported across concrete blocks as in Figure 2.1. Each brick was held by hand from a ladder at approximately 7.5 ft (2.3 m). The dimensions for $a$, $b$, $c$, and $m$ for the two tests can be found in Table 2.1. For both drop tests, video footage was recorded at a resolution of 640x480 and 100 frames per second and the resulting conductor damage was photographed.

Using the principle of conservation of energy, the final kinetic energy in each brick at impact was determined from the initial potential energy prior to release, neglecting any losses. $PE_i = KE_f$, Where $PE_i = mgh$ and therefore, $KE_f = mgh$. Energy stored in the bricks for tests 1 and 2 was calculated as $375 \text{ lbf} \cdot \text{ft}(508 \text{ Nm})$ and $202.5 \text{ lbf} \cdot \text{ft}(274.6 \text{ Nm})$, respectively.
Table 2.1: Brick Drop Tests Quantities

<table>
<thead>
<tr>
<th></th>
<th>Brick Drop Test 1</th>
<th>Brick Drop Test 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>( a )</td>
<td>7.5 ft (2.3 m)</td>
<td>7.5 ft (2.3 m)</td>
</tr>
<tr>
<td>( b )</td>
<td>5.0 in (0.127 m)</td>
<td>10.0 in (0.254 m)</td>
</tr>
<tr>
<td>( c )</td>
<td>18.0 in (0.457 m)</td>
<td>18.0 in (0.457 m)</td>
</tr>
<tr>
<td>( m )</td>
<td>50 lbm (22.7 kg)</td>
<td>27 lbm (12.2 kg)</td>
</tr>
<tr>
<td>( KE_f )</td>
<td>375 lbf-ft (508 Nm)</td>
<td>202.5 lbf-ft (274.6 Nm)</td>
</tr>
</tbody>
</table>

2.2 Experimental Results and Discussion

The camera used to record video had a limited frame rate of 30 frames per second and resulted in observed discontinuities in the frame by frame playback represented by Figure 2.2 and Figure 2.3. In Test 1, a 375 lbf \( \cdot \) ft (508 Nm) impact of the brick caused permanent deformation of the ACCC sample, which retained almost a 90° bend, and upon removal of the Al strands, the composite core showed obvious failure (Figure 2.4). In Test 2, a 202.5 lbf \( \cdot \) ft (274.6 Nm) impact of the brick caused moderate displacement of the outer aluminum strands but no evidence of fracture in the composite core (Figure 2.5).
Figure 2.2: Brick Drop Test 1 Video footage

Figure 2.3: Brick Drop Test 2 Video footage
2.3 Conclusions

The results from this initial evaluation of impact response of ACCC show that situations exist where ACCC can be damaged, and that the failure process can strongly depend on the type of impact. It was immediately realized that a more systematic and repeatable experiment was required to evaluate the response of this system to impact depending on boundary conditions (tension/free-bending), shape of impacting object,
energy of impact, velocity of impact, and angle of impact. This research, due to time constraints and complexity of the problem, focuses initially on the variables of boundary conditions.
3.0 EXPERIMENTAL METHODS

3.1 Tested Conductors and Sample Preparation

The Aluminum Conductor Composite Core/Trapezoidal Wire (ACCC/TW) conductor evaluated in this research was received from the Western Area Power Administration (WAPA) and had Drake geometry with an approximate overall outer diameter of 1.11 in (28.2 mm). ACCC/TW Drake consists of the hybrid epoxy PMC core surrounded by an inner layer of 8 and outer layer of 14 helically wound trapezoidal cross-section 1350-O aluminum strands.

Samples were shipped as 53 in (1.34 m) length sections cut from a continuous spool. For the experimental test setup, samples were reduced to 43.25 in (1.10 m) using a diamond blade saw and horizontal band saw for the composite core and aluminum strands, respectively. The aluminum strands were cut first by sliding the composite core partially out from the strands so a flood-coolant horizontal band saw could cut the aluminum independently. Cable ties were fastened around the aluminum circumference approximately 1.0 in (25.4 mm) from the cut to keep strands together during cutting. After cutting, the aluminum ends were de-burred using a 3M Scotch-Brite™ wheel to provide a clean finish with minimal tool marks and free of burrs. The composite core rod was then partially exposed from the aluminum strands to allow cutting with a water-
cooled diamond grit blade on a bench-top band saw to match the cut length of aluminum.

An example of the prepared ACCC samples is given in Figure 3.1.

![Figure 3.1: Example of prepared sample](image)

3.1.1 Installation of Strain Gauges on Select Samples

For verification of the method used to measure conductor tension, several samples had SR-4 type A-7 lot# B-31 strain gauges bonded to the exterior of the composite rod in the axial direction using Micro Measurements M-Bond 200. Strain gauges were positioned 8.1 in (20.6 cm) from the end of the sample. 42 in (1.07 m) of 28 AWG lead wires were then soldered to the strain gauge leads to facilitate connection with a Vishay P-3500 strain indicator unit in a quarter bridge configuration.

3.2 Impact Fixture Design

The specialized impact test fixture was designed and constructed with the assistance of an undergraduate senior design team. The document titled “High Voltage Conductor Impact Testing Device” describes and documents the device as delivered for this project (Figure 3.2) (Mobley, O'Brien and Platisa 2014). To significantly improve the
operation and functionality of the test fixture several additional components were subsequently designed and fabricated as part of this work.

Figure 3.2: Apparatus as delivered by the undergraduate design team

3.2.1 Additional Part Design

3.2.1.1 Floor Brackets

To minimize the undesired dissipation of energy during impact testing by the movement of the fixture across the floor, four floor brackets were fabricated using low-carbon steel 3”x3” angle with a 3/16” wall thickness, as shown in Figure 3.3. The brackets were fastened to the concrete floor using 3.75” long ½” diameter concrete stud anchors at each of the test frame’s four corners.
3.2.1.2 Pendulum Impactor Assembly

The undergraduate design team supplied a concept design for the impacting mechanism that involved a sophisticated spring loaded impactor with a short travel distance (Figure 3.4). A weighted pendulum was deemed simpler and easier to implement (Figure 3.5), where the energy transfer could be measured by angular velocity or position in a similar way to a Charpy-Izod impact tester.
Figure 3.4: Undergraduate design concept for impacting system
Implementation of the pendulum impacting subsystem required additional thru holes in the two Width Beam-Top components as shown by DWG. NO.: Width Beam – Top, rev. A (Figure 3.6) and fabrication of additional components: brackets and Depth Beam – Middles. The additional components and modifications provide a rail-system where the pendulum support frame can rest, as shown in Figure 3.7.
Figure 3.6: Modifications to Width Beam – Top components for pendulum assembly
Pendulum Support Frame

The pendulum support frame includes brackets for the attachment of two stamped steel mounted bearings where the pendulum axle is constrained (Figure 3.8). The square support frame is meant to rest atop the Depth Beam – Middles and be fastened using four 3” C-clamps at each corner of support frame to allow for variation of impact angle and angle of swing where contact is made with the conductor (Figure 3.8).
Swing Arm Assembly

The swing arm assembly consists of the pendulum axle and axle stiffener, pendulum arm and arm backer, weight backer and weight hangers secured with weld beads into a single subassembly, as shown in Figure 3.9. With the cylinder impactor, striker mount, and addition of four lead bricks for added mass, the pendulum impacting assembly is mounted via the axle to the pendulum support frame bearings. The additional mass is secured to the weight backer by two 1.5” wide webbing ratchet straps passed around the lead bricks and front of the weight backer.

Figure 3.8: Pendulum support frame resting on Depth Beam – Middles
Pendulum Release Support

To vary the height and subsequently the energy and velocity imparted at impact, the initial pendulum height is set by altering the length of a chain (¼ chain size) attached to a 1.5” square steel tube on top of the test frame as in Figure 3.10. A panic snap on the opposing chain end holds the pendulum at a U-bolt on the pendulum arm and provides a consistent release mechanism to initiate each test.
3.2.1.3 Height Extensions and Extension Support Straps

In order for the pendulum swing to generate enough energy to damage sections of conductors, the height of the initial fixture design delivered by the undergraduate design team had to be increased to accommodate the long pendulum arm. Height extension is achieved by the use of four square height extension tubes with hole patterns identical to the tops of the original vertical support posts that attach to the existing top structure components. Simple brackets called extension straps are fabricated to fasten the height extenders to the tops of the vertical support posts. The use of the height extensions and extension straps can be seen in Figure 3.5, with the designs shown in Figure 3.11 and Figure 3.12, respectively.
Figure 3.11: Height extensions
3.2.1.4 Conductor Clamps

To constrain 43.25” length sections of Drake conductors in tension, clamps were designed to grip the ACCC conductors through the outer aluminum stranding and transfer the loading to the core. The profile of the gripping surface was chosen to duplicate the radius from a set of Chicago style sliding jaw round contour grips (Figure 3.13) received from the Bonneville Power Administration (BPA) for use with Drake sized aluminum stranded conductors during installation. Two halves of the grips are fastened to the conductor ends with the clamping force of eight grade-8 1/2” hex bolts. Tension loading from the test fixture tension bars is transferred by a double-clevis connector and two links of Grade-80 alloy 1/2” chain into the conductor clamps, as shown in Figure 3.14.
Clamps are secured to the ends of each conductor sample using a ½” drive clicker-style CDI torque wrench, part# 2503MFRMH-CDI, in steps of 50, 80 and 100 ft-lb. A uniformly repeating pattern is used to achieve a uniform distance between each clamp. Grade 8, ½”-13 x 3” zinc plated hex bolts are lightly lubricated and used in combination with lock-washers and hex nuts. The conductor clamps are attached to the ½” chain with a Grade 8, 5/8”-11 x 3” hex bolt and hex nut which transfers the tensile load in double-shear.

Figure 3.13: Chicago style sliding round jaw grip received from BPA
Figure 3.14: Conductor clamp and cable tensioning
3.2.2  Fixture Calibration and Verification

3.2.2.1  Measuring Tension in Conductor

Tension in the sample is initially approximated by assuming a frictionless first-class lever having a mechanical advantage (ma) of $\frac{29.0 \text{ in}}{4.5 \text{ in}} \approx 6.4$. A 0.75 ton ratcheting chain puller and a Dillion 2,000lb Mechanical AP Dynamometer are used to increase, hold, and measure the tension on the input side of the tensioning lever arms. The measured input tension is then multiplied by the mechanical advantage to approximate sample tension. Once the desired tension is reached, a reference distance is measured on
the lever input arms for each sample and the dynamometer and chain-hoist are replaced by the turnbuckle tensioning device to fix the displacement of lever input arms at the measured reference distance.

To verify the method used to measure conductor tension in each test, an ACCC rod with aluminum stranding removed and strain gauge attached, as described in section Installation of Strain Gauges on Select Samples, was mounted in the test fixture. The sample was gripped in the conductor clamps with 4.5 in (11.4 cm) of aluminum stranding at either end of the sample. The strain gauge was then connected to the P-3500 Strain Indicator in a quarter-bridge configuration to measure indicated microstrains in the axial direction of the composite rod surface. The sample was preloaded to an initial input tension of 200 lbf (890 N) to allow load distribution and settling of the composite rod in the grips. Input tension was subsequently increased from 200 lbf (890 N) to 1100 lbf (4893 N) and back to 200 lbf (890 N) in 100 lbf (445 N) increments using the Dillon AP Dynamometer and chain-puller. Axial strain was recorded for each input tension and is plotted with the frictionless approximations of rod axial stress and rod tension.

3.2.2.2 Measuring Pendulum Position During Impact

Angular position of the pendulum arm is measured using a rotary pulse generator attached to the pendulum axle. The angle encoder generates 2,100 digital pulses per revolution, and, when used with an Arduino Uno board and personal computer, indicates both magnitude and direction of the angular displacement with respect to time. Implementation of the microprocessor and computer code with the angle encoder was
accomplished as part of the design portion for the PhD qualifying exam of Eva Hakansson.

To verify the calibration of the angular displacement measurement, the pendulum was loaded with four lead bricks, the cylinder impact head, and ratchet straps and released from an initial angle of -55° from the horizontal reference plane. Data was recorded for nine complete swing cycles of the pendulum at which point the pendulum was stopped by hand to rest at its equilibrium point.

3.2.2.3 Calculating Energy in Pendulum

Energies of the rotating pendulum are calculated using the equation of kinetic energy in a rotating system, \( KE = \frac{1}{2} I \omega^2 \), where, \( \omega \) is the instantaneous angular velocity of the pendulum and \( I \) is the moment of inertia of the experimental test fixture pendulum. \( I \) is experimentally determined using the equation for moment of inertia about a fixed pivot, \( I_{zz} = Wr \left( \frac{\tau}{2\pi} \right)^2 \). The period, \( \tau \), weight, \( W \), and radius to center of mass, \( r \), are measured for the entire assembly of pendulum, four lead bricks, cylinder impactor, and ratchet straps. The radius to the center of mass was determined from the pendulum axle to the location where the entire assembly would balance on the vertex of a short piece of angle aluminum.

3.2.3 Impact Experiments

Experiments were conducted for boundary conditions of fixed displacement ends at initial tensions of 258 lbf (1.15 kN), 1031 lbf (4.586 kN), 2578 lbf (11.47 kN), 4511 lbf (20.07 kN) and 6573 lbf (29.24 kN) and a 3-point impact condition where the conductor was supported across posts. The pendulum was loaded with the same
105 lbm (47.6 kg) of lead bricks used to determine the moment of inertia. Initial height for release was set at 45° below the horizontal plane and verified for each test using a magnetic angle finder placed on the pendulum just below the U-bolt attachment. A cylindrical steel impacting geometry of diameter 1.625 in (4.13 cm) attached to the end of the pendulum as shown in Figure 3.9 would strike transverse to the axial direction of the conductor for all tests. All conductor samples had Pinch-type hose clamps fastened at 5.0 in (12.7 cm) from each end of the conductor to constrain outer aluminum strands. Time history of the angular position of the pendulum was recorded for each test and analyzed with MATLAB to calculate angular velocity and kinetic energy stored in the pendulum. Three samples were tested in the 3-point impact condition of no axial constraints with 1.0 in (2.54 cm) square support posts separated 18.0 in (45.7 cm); conductor impact occurred midway between the supports. For the fixed-displacement boundary conditions two samples were tested at each of the five initial tensions with tension set as described in Measuring Tension in Conductor section and conductor clamps were installed as described in Conductor Clamps section.

3.2.4 Tensile Testing of Aluminum Strands

In order to model the conductor with non-linear material behavior of the aluminum strands, it was necessary to characterize the mechanical properties of the as received 1350-O Aluminum strands with several tensile tests on an MTS 858 Mini Bionix II load frame. Strain rates of 2.0 in/min (50.8 mm/min), 10 in/min (254 mm/min), and 20 in/min (508 mm/min) were used to identify any strain rate dependencies and elongation was measured by an MTS extensometer model 632.13E-20. The
trapezoidal strands were taken from an 8.25 in (21.0 cm) section of ACCC conductor and straightened just enough to be held in the grips of the test frame. Cross-sectional area for all strands was determined to be $A = 0.036 \text{ in}^2 (23.6 \text{ mm}^2)$. The extensometer was attached to the middle of the strands with standard knife edges and anchor springs. Elongation and load data was collected at a sampling rate of 49.51 Hz.
4.0 NUMERICAL METHODS

Numerical modeling was done to help understand the transfer of energy into the ACCC conductor when exposed to low-velocity impact. Quantities not measured in the experiment, such as friction, plastic strain, and elastic strain, may depend on the boundary conditions and can help show how the conductor is storing or dissipating energy. The models presented do not represent the actual response of the conductor to the experimental conditions or the complete geometry of the real conductor. They are simplified in their geometry, loading, and analysis with the attempt to indicate possible trends and contribute to the understanding of the actual experiment.

4.1 Modeled Geometry

To demonstrate the dependencies of energy transfer on boundary conditions, a complete model of all components in the conductor is not necessary. This would be computationally costly and produce extremely large output data files. Instead, a simplified ACCC conductor geometry was created (Figure 4.1 and Figure 4.2). Non-linear geometries were taken into account to accommodate contact interactions and plastic strains. 8-Noded hexahedral reduced-integration continuum elements (C3D8R) were used to save on computation time with a minimal effect on accuracy of the results. The model consisted of four trapezoidal aluminum strands wound helically around a composite rod core.
The cross section of the simplified ACCC model is shown in Figure 4.2, colors indicate individual components. The outer diameter of the wound aluminum strands (A) is 1.0 in (25.4 mm), and the hybrid composite core has ø0.25in (6.35 mm) of Carbon/Epoxy (C) surrounded by an additional 0.0625 in (1.60 mm) of ECR-Glass/Epoxy (B) for an overall core ø0.375 in (9.53 mm). The interface between Glass and Carbon is modeled as perfectly bonded by making their coincident nodes equivalent. The outer strands follow a helical spiral path having a pitch, p = 6.0 in (152.4 mm) and height, h = 6.0in (152.4 mm) as shown in Figure 4.1.

Surface interactions in the model use surface-to-surface contact pairs. Each of the four outer strands (A) interacts with adjacent strands with small sliding contact formulation and friction coefficient of aluminum on aluminum, \( \mu_{Al-Al} = 1.1 \). Contact between Strands (A) and Glass/Epoxy (B) is also defined with a small sliding formulation with friction coefficient between aluminum and Glass/Epoxy composite, \( \mu_{Al-ECR} = 0.5 \) (Bowden and Tabor 1950). The small sliding formulation was chosen because it allows contacting surfaces to undergo only relatively small sliding in relation to each other, but permits arbitrary rotation of the bodies, while being less computationally expensive than other contact formulations.
Orthotropic material properties of the hybrid composite rod were calculated using the elastic transversely isotropic Eshelby method with a fiber fraction of \( f = 0.6 \), and the constituent properties of Table 4.1 with orthotropic properties of the fibers (Eshelby...
1957). An isotropic elastic-plastic with isotropic strain hardening material model was used for the 1350-O Al. Material properties used for the aluminum were determined experimentally with the values given in Table 5.1, with elastic-plastic behavior defined by the experimental true stress/strain curve of Figure 5.4.

Table 4.1: Constituent material properties used to determine composite material properties (B. M. Burks, et al. 2009)

<table>
<thead>
<tr>
<th>Property</th>
<th>Carbon fiber</th>
<th>ECR-Glass fiber</th>
<th>Epoxy resin</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axial Young’s modulus, GPa[Msi]</td>
<td>230[33.4]</td>
<td>76[11.0]</td>
<td>3.6[0.52]</td>
</tr>
<tr>
<td>Transverse Young’s modulus, GPa[Msi]</td>
<td>12[1.74]</td>
<td>76[11.0]</td>
<td>3.3[0.48]</td>
</tr>
<tr>
<td>Longitudinal Poisson’s ratio</td>
<td>0.3</td>
<td>0.22</td>
<td>0.2</td>
</tr>
<tr>
<td>Transverse Poisson’s ratio</td>
<td>0.2</td>
<td>0.22</td>
<td>0.2</td>
</tr>
<tr>
<td>Longitudinal shear modulus, GPa[Msi]</td>
<td>5.0[0.73]</td>
<td>6.9[1.0]</td>
<td>1.2[0.74]</td>
</tr>
</tbody>
</table>

4.2 Static Analysis

The implicit finite element method was utilized in the Abaqus/Standard® v.6.11.3 finite element solver to model the simplified ACCC conductor piece subjected to axial tension and transverse compression. Analyses were performed to investigate conductor response under different boundary conditions of prescribed displacements and prescribed tension, as shown by Figure 4.3. Each analysis consists of 4 steps: Step 1 applies a ramped load in the global z-direction (longitudinal) to create axial tension in the conductor model. In this tension step, all models are constrained with a single node rigidly beamed (MPC) to each node on the bottom face of the geometry. This node was
then constrained in all translational degrees of freedom (dof) and in rotations about the z-axis for stability. Nodes on the opposing face were rigidly linked (MPC) to a single node constrained in translational dof’s along the global x- and y- directions; this node was then displaced or loaded with a concentrated force to achieve the desired tension.

Step 2 moves a cylindrical analytical rigid surface of Ø 1.625 in (41.30 mm), oriented orthogonal to the conductor into contact with the outer strands. Step 3 ramps a concentrated force of 350 lbf (1556.9 N) on the reference node of the rigid surface to apply a static transverse load at the center of the conductor model. Finally, step 4 simply removes the transverse load on the conductor to show the permanent resulting deformation of the model. This procedure was applied for tensions of 500 lbf (2224 N), 1000 lbf (4448 N), 2000 lbf (8896 N), and 3000 lbf (13345 N).

![Diagram of installation and service boundary conditions](image)

**Figure 4.3:** Boundary and load conditions for static analysis

### 4.3 Dynamic Analysis

To show the energy dependencies on loading rate, the numerical model was expanded into an explicit dynamic analysis of the simplified conductor response to low-
velocity impacts. Energies of the entire model were calculated for frictional dissipation, plastic dissipation, recoverable strain energy, and total strain energy using the Abaqus 6.11.3 numerical solver in an explicit dynamic analysis. In the dynamic analysis, ends of the generalized conductor segment were fixed in translational displacement with zero preload tension, and an impacting tool of fixed mass is given initial velocities of $25 \text{ in/s} (0.64 \text{ m/s})$, $50 \text{ in/s} (1.27 \text{ m/s})$, and $75 \text{ in/s} (1.91 \text{ m/s})$. A representative equivalent static result was calculated for each impact velocity by applying the maximum displacement from each dynamic analysis in an implicit static step. In addition, the dominant variable (energy or velocity) can be shown by comparing the static and dynamic results.
5.0 EXPERIMENTAL RESULTS AND DISCUSSION

5.1 Tensile Testing of Aluminum Strands

The resulting engineering stress and strain for the aluminum strands pulled at each load rate exhibit similar elastic-plastic behavior and are given in Figure 5.1, Figure 5.2, and Figure 5.3. The plots suggest there is no rate dependency for the as received aluminum for load rates between 2.0 in/min (50.8 mm/min) and 20 in/min (508 mm/min). As such, the elastic-plastic properties up to the onset of necking are calculated with the 20 in/min (508 mm/min) results and converted to the true stress and strain plot given in Figure 5.4. The calculated Young’s modulus, yield strength (offset = 0.2%), and Poisson’s ratio are given in Table 5.1, with the yield strength falling within the range designated by ASTM B609/B609M – 12e1 – Standard Specification of Aluminum 1350 Round Wire, Annealed and Intermediate Tempers, for Electrical Purposes.
Figure 5.1: Aluminum engineering stress/strain for 2.0 in/min (50.8 mm/min) strain rate
Figure 5.2: Aluminum engineering stress/strain for 10 in/min (254 mm/min) strain rate
Figure 5.3: Aluminum engineering stress/strain for 20 in/min (508 mm/min) strain rate

Table 5.1: Measured aluminum tensile properties of ACCC conductor strands

<table>
<thead>
<tr>
<th>Property</th>
<th>1350-O Al</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus, $E$ (ksi[kPa])</td>
<td>7.03 [48.50]</td>
</tr>
<tr>
<td>Poisson’s ratio, $\nu$</td>
<td>0.33</td>
</tr>
<tr>
<td>Yield stress, $\sigma_y$ (ksi[kPa])</td>
<td>8.8 [60.67]</td>
</tr>
</tbody>
</table>
5.2 Elastic Modulus of ACCC Rods Under Tension

The approximate axial tension and corresponding measured strain of the ACCC rod is shown in Figure 5.5. Likewise, the approximate average stress and measured strain are presented in Figure 5.6. Using a linear regression of the engineering stress and strain data in Figure 5.6, an experimental elastic modulus was determined as $E = 17,679 \text{ ksi (121.9 MPa)}$ with an error of 8.2% in comparison with the values published in (Alawar, Bosze and Nutt 2005). Note, there was a small difference in the measured strain before and after loading, which can be attributed to measurement error in the Dillion Dynamometer.
Figure 5.5: Frictionless approximation of tension and measured strain in ACCC rod
Figure 5.6: Frictionless approximation of stress and measured strain in ACCC rod

5.3 Verification of Measured Pendulum Position

Figure 5.7 shows the pulse count of the loaded pendulum arm through nine complete cycles as reported from the shaft angle encoder. Pulse counts are converted to angular displacement in radians with the relation of $2100 \text{ pulses} = 2\pi \text{ radians}$ shown in Figure 5.8. Angular velocity of the free swinging pendulum is determined by the numerical differentiation of angular position with respect to time and subsequently smoothed using a Savitzky-Golay filter in Matlab to produce the results of Figure 5.9. To check for error in the numerical differentiation of angular velocity, the maximum velocity of the first return swing is calculated by a linear regression of twenty data points centered
on the equilibrium angle. A difference of 2.0% was found in the values for the maximum angular velocity of the first return swing.

Figure 5.7: Pulse count of pendulum free swing
Figure 5.8: Angular displacement of free swing pendulum
Figure 5.9: Angular velocity of free swinging pendulum

5.4 Calculating Energy in Pendulum

The period, $\tau$, of the free swinging pendulum was averaged over the first four cycles of Figure 5.8 to be $\tau = 2.325 \text{ s}$. The total weight of the assembled pendulum, $W$, consisting of four lead bricks, cylinder impactor, and ratchet straps was measured to be 125.5 $lbm$ (56.9 $kg$). The radius to the center of mass, $r$, was measured as $r = 48.0 \text{ in} \ (1.22 \text{ m})$. The resulting mass moment of inertia, $I_{zz}$ for the instrumented pendulum is determined to be $I_{zz} = 68.73 \text{ lb} \cdot \text{ft} \cdot \text{s}^2 \ (93.2 \text{ N} \cdot \text{m} \cdot \text{s}^2)$
5.5 Impact Experiment Results

To reduce noise in the calculated angular position and velocity, the data for all impact experiments utilized a Savitzky-Golay polynomial smoothing filter on the raw data of time and the calculated angular position. The Savitzky-Golay filter was chosen because the sampling rate of the angle encoder changes with the velocity of the pendulum, resulting in a variable sampling rate of data. Savitky-Golay filters fit a polynomial to a specified frame of data to reduce noise. For this application, a 2nd order polynomial was used for frames of 13 data points.

Because of remaining noise in the calculated angular velocities, the maximum kinetic energy of the pendulum was calculated from the slope of angular position starting 5° prior to contact with the conductor and assumed to be entirely transferred into the conductor. Residual kinetic energy of the pendulum was calculated from the slope of angular position data for 5° after the point where contact no longer exists, taken as the inflection point in the angular position curves. For the constrained impact condition, calculated initial tension, maximum kinetic energy before impact, measured change in energy, percent of total energy lost, and linear velocity of impact are given in Table 5.3. Similarly for the 3-point impact experiments, maximum kinetic energy before impact, measured change in energy, percent of total energy lost, and linear velocity of impact are given in Table 5.2. The dissipated energy for each test is plotted as a percentage of the initial kinetic energy in the pendulum with respect to the initial axial tension from all tests in Figure 5.10. The resulting permanent curvature of the 3-point impact and
tensioned conductor samples after removal of conductor clamps is shown in Figure 5.11, with the 3-point impact specimens on the right and tension decreasing from left to right.

Table 5.2: ACCC 3-point impact experiment results (no tension)

<table>
<thead>
<tr>
<th>Test</th>
<th>9</th>
<th>10</th>
<th>13</th>
</tr>
</thead>
<tbody>
<tr>
<td>KE_{max} (lbf-ft[Nm])</td>
<td>195.3[264.8]</td>
<td>204.3[277.0]</td>
<td>183.8[249.2]</td>
</tr>
<tr>
<td>ΔKE (lbf-ft[Nm])</td>
<td>130.0[176.3]</td>
<td>195.6[265.2]</td>
<td>123.1[166.9]</td>
</tr>
<tr>
<td>ΔKE (%)</td>
<td>66.6</td>
<td>95.7</td>
<td>67.0</td>
</tr>
<tr>
<td>Velocity (ft/s[m/s])</td>
<td>10.9[3.3]</td>
<td>11.2[3.4]</td>
<td>10.6[3.2]</td>
</tr>
</tbody>
</table>

Table 5.3: Tensioned ACCC conductor impact test results

<table>
<thead>
<tr>
<th>Test</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
</tr>
</thead>
<tbody>
<tr>
<td>KE_{max} (lbf-ft[Nm])</td>
<td>216.1[293]</td>
<td>220.8[299]</td>
<td>223.9[304]</td>
<td>225.8[306]</td>
<td>229.2[311]</td>
</tr>
<tr>
<td>ΔKE (lbf-ft[Nm])</td>
<td>103.6[140]</td>
<td>99.2[134]</td>
<td>87.8[119]</td>
<td>79.7[108]</td>
<td>90.7[123]</td>
</tr>
<tr>
<td>ΔKE (%)</td>
<td>47.9</td>
<td>45.0</td>
<td>39.2</td>
<td>35.3</td>
<td>39.6</td>
</tr>
<tr>
<td>Velocity (ft/s[m/s])</td>
<td>11.5[3.51]</td>
<td>11.6[3.54]</td>
<td>11.7[3.57]</td>
<td>11.7[3.57]</td>
<td>11.8[3.60]</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Test</th>
<th>8</th>
<th>11</th>
<th>12</th>
<th>14</th>
<th>15</th>
</tr>
</thead>
<tbody>
<tr>
<td>KE_{max} (lbf-ft[Nm])</td>
<td>227.3[308]</td>
<td>219.1[297]</td>
<td>217.0[294]</td>
<td>217.7[295]</td>
<td>222.1[301]</td>
</tr>
<tr>
<td>ΔKE (lbf-ft[Nm])</td>
<td>84.6[115]</td>
<td>77.9[106]</td>
<td>73.9[100]</td>
<td>72.5[98]</td>
<td>81.6[111]</td>
</tr>
<tr>
<td>ΔKE (%)</td>
<td>37.2</td>
<td>35.6</td>
<td>34.1</td>
<td>33.3</td>
<td>36.7</td>
</tr>
<tr>
<td>Velocity (ft/s[m/s])</td>
<td>11.8[3.60]</td>
<td>11.6[3.54]</td>
<td>11.5[3.51]</td>
<td>11.5[3.51]</td>
<td>11.7[3.57]</td>
</tr>
</tbody>
</table>
Figure 5.10: Percentage of energy lost after impact
5.5.1 3-Point Impact Experiments

Test 9, 10, and 13 had the conductor supported in a 3-Point impact condition with no displacement constraints or tension at conductor ends. The impacting surface left a barely noticeable indentation at the point of contact, but a significant amount of separation and deformation in the outer aluminum strands, represented by the samples in Test 9 and 13 in Figure 5.12 and Figure 5.14, respectively. The sample of Test 10
exhibited a significant degree of permanent curvature (about 90 deg) after impact shown in Figure 5.13. Closer examination of that sample revealed severe damage of the composite rod evident through the aluminum stranding. The measured angular displacement with respect to time of the pendulum in Test 9 and 13 match closely, and differ from Test 10 in Figure 5.15.

The computed angular velocities of all tests in Figure 5.16, exhibit trends as expected from pendulum release to contact with the conductor at time, \( t = 0.52 \, s \): the velocity magnitude increases as expected for a free swinging pendulum. In Test 9 and 13, where the composite core did not fracture, the angular velocity magnitude decreases to zero while the pendulum kinetic energy is transferred into the conductor as elastic strain energy of the composite rod, elastic and plastic strain energy in the aluminum strands, frictional dissipation energy between the strands and core, and frictional dissipation energy of the conductor sliding on the support posts. In addition, the energy also goes into vibration of the pendulum arm and test frame, friction in the pendulum bearings, air resistance of the pendulum, and the motion of the conductor sample through the air after impact.

For Test 9 and 13, the conductor stores a significant amount of recoverable strain energy in the bending of the composite core which upon straightening is transferred back into the pendulum to change its direction and overcome the friction of the support posts. Once the pendulum is no longer in contact with the conductor the angular velocity again varies as would be expected. In Test 10, failure of the composite core provides an unrecoverable transference of energy in fracture, the remaining energy that would have
gone to the rod likely goes into elastic and plastic strain energy of the aluminum strands, evident in its significantly greater permanent curvature.

Figure 5.12: Impact zone images in Test 9

Figure 5.13: Impact zone images in Test 10
Figure 5.14: Impact zone images in Test 13

Figure 5.15: Angular displacement for 3-point impact experiments in Tests 9, 10 and 13
5.5.2 Tensioned Impact Experiments

In Tests 3 and 4 the conductors were fixed in the clamps with an initial
258 lbf (1.15 kN) axial tension. The impacting surface left a noticeable indentation and
a small amount of separation visible in the outer aluminum strands, as shown in Figure 5.17
and Figure 5.18. For both tests there was no measurable loss of tension after impact,
indicating that no measurable slipping of the conductor occurred in the grips.

The measured angular displacements of the pendulum in both tests agree well, as
shown in Figure 5.19. The computed angular velocities in Figure 5.20 exhibit the same
expected trends from pendulum release to contact with the conductor at time, \( t = 0.54 \text{ s} \):
increase of velocity magnitude. Contact with the sample occurs and the angular velocity decreases to zero while the pendulum’s kinetic energy is transferred into the sample, as elastic strain energy of the composite rod, elastic and plastic strain energies in the aluminum, and frictional dissipation energy between all components. Energy also goes into vibration of the pendulum arm and test frame, friction in the pendulum bearings, air resistance of the pendulum, and elastic strain in the tensioning mechanism. Additionally if there is a measurable loss of sample tension after impact, energy will be dissipated due to friction of the sample sliding in the grips.

 Starting at the point where the pendulum angular velocity is zero, the recoverable strain energy of the sample straightens the conductor and transfers back into the pendulum reversing its direction and increasing its magnitude. Once the pendulum is no longer in contact with the sample the angular velocity again varies as expected.
Figure 5.18: Impact zone images, Test 4
Figure 5.19: Angular displacement for tensioned impact experiments in Test 3 and 4 (overlay)
Tests 5 and 6 had the conductor fixed with an initial 2578 lb (11.47 kN) of axial tension. Similar to Tests 3 and 4, the impacting surface left a noticeable indentation and a small amount of separation visible in the outer aluminum strands as shown in Figure 5.21 and Figure 5.22. In both tests there was no measurable loss of tension after impact and the measured pendulum angular displacements agree well in Figure 5.23. The computed angular velocities in Figure 5.24 exhibit the same trends as Tests 3 and 4 with the same transference of energy.
Figure 5.21: Impact zone images, Test 5

Figure 5.22: Impact zone images, Test 6
Figure 5.23: Angular displacement for tensioned impact experiments in Test 5 and 6 (overlay)
The samples of Tests 7 and 8 had an initial 1031 lbf (4.586 kN) of tension. The impacting surface imparted an indentation and separation of the outer strands as shown in Figure 5.25 and Figure 5.26, no different than seen in previous tension tests. After impact there was no measurable loss of tension for both samples. The angular displacements of both tests agree in Figure 5.27. The computed angular velocities in Figure 5.28 exhibit the same trends as previous tests with the same transference of energy.
Figure 5.25: Impact zone images, Test 7

Figure 5.26: Impact zone images, Test 8
Figure 5.27: Angular displacement for tensioned impact experiments in Test 7 and 8 (overlay)
Test 11 and 12 had an initial 6573 lbf (29.24 kN) of axial tension. The impacting surface left a noticeable indentation and almost no visible separation of the outer aluminum strands when still held under tension as shown in Figure 5.29 and Figure 5.30. In Test 11 there was a measured decrease of tension in the conductor to 6058 lbf (26.95 kN), a reduction of 515 lbf (2.29 kN) or 7.8%. Test 12 had a measured tension after impact of 5864 lbf (26.08 kN), a reduction of 709 lbf (3.15 kN) or 10.7%. The loss of tension in both tests suggests the composite core exhibited a finite amount of slip in the grips. The measured pendulum angular displacements for both tests agree well in Figure 5.31. The computed angular velocities

Figure 5.28: Angular velocity for tensioned impact experiments in Test 7 and 8 (overaly)
in Figure 5.32 exhibit the same trends as previous tests with additional energy lost to grip slip.

To better quantify the amount of strain energy that may have been dissipated by slipping of the composite core inside the aluminum, an approximate value is determined using the principle of conservation of energy. Assuming the change in internal strain energy of a constant axial load, $\Delta U_i$, equals the energy lost to grip slip, $U_e$, as in $U_e = \Delta U_i$. $\Delta U_i$ is then given as:

$$U_i = \frac{1}{2AE}(P_1^2L_1 - P_2^2L_2)$$

Where $P$ is the axial load, $L$ is the length between the grips, $A$ is the cross sectional area of the whole conductor, $E$ is the Young’s modulus of the whole conductor and, the subscripts 1 and 2 denote before impact and after, respectively. The change in length is assumed to be small and the length of the sample is taken as $L_1 \approx L_2 = 43.25 \text{ in (1.10 m)}$, area taken as $A = 0.964 \text{ in}^2 (6.22E \text{ in}^2 - 4 \text{ m}^2)$, and elastic modulus, $E = 8300 \text{ ksi (57.22 GPa)}$. The approximate energy lost to grip slip in Tests 11 and 12 is then calculated as $1.2 \text{ lbf } \cdot \text{ ft (1.6 Nm)}$ and $1.6 \text{ lbf } \cdot \text{ ft (2.2 Nm)}$, respectively.

This calculation underestimates the impact energy dissipated through impact. It only approximates the energy dissipated by friction in the process of slipping and assumes slip initiates at the initial sample tension. In actuality, slip initiates at a much higher tension due to the impulse loading from impact. An accurate determination of the total additional energy dissipated by slipping in the grips is difficult. Energy is not only lost to friction in the pull-out process, but with displacements fixed an increase in gauge
length of the conductor permits more deflection, and subsequent strain in the aluminum
during impact loading.

Figure 5.29: Impact zone images, Test 11

Figure 5.30: Impact zone images, Test 12
Figure 5.31: Angular displacement for tensioned impact experiments in Test 11 and 12 (overaly)
Test 14 and 15 had the sample mounted with an initial tension of 4511 lbf (20.07 kN). The impacting surface left a noticeable indentation at the point of impact and almost no visible separation of the outer aluminum strands when still held under tension as shown in Figure 5.33 and Figure 5.34. In Test 15 there was a measured decrease of tension in the conductor to 3480 lbf (15.48 kN), a reduction of 1031 lbf (4.586 kN) or 23%. Test 14 also had a measured decrease of tension after impact to 3351 lbf (14.91 kN), a reduction of 1160 lbf (5.160 kN) or 26%. As in Tests 11 and 12, the approximate energy dissipated by the measured loss of tension was

Figure 5.32: Angular velocity for tensioned impact experiments in Test 11 and 12 (overlay)
found to be $1.6 \text{lbf} \cdot \text{ft} (2.2 \text{Nm})$ in Test 14 and $1.5 \text{lbf} \cdot \text{ft} (2.0 \text{Nm})$ in Test 15. Even with grip slip, the pendulum angular displacements for both tests agree well in Figure 5.35. The computed angular velocities in Figure 5.36 exhibit the same trends and transference of energy as in Tests 11 and 12.

Figure 5.33: Impact zone images, Test 14
Figure 5.34: Impact zone images, Test 15

Figure 5.35: Angular displacement for tensioned impact experiments in Test 14 and 15 (overlay)
At each of the tested conditions the experimental test fixture produced repeatable results. However, the impact of the pendulum with each conductor produced significant vibration of the pendulum and support structure of the test fixture. The design of the test frame tensioning system provides only 2.5 in (63.85 mm) below the conductors during a test; because of this, the center of percussion of the pendulum is 6.0 in (152.4 mm) above the conductor at impact. This offset creates a significant moment to induce vibrations in the pendulum. A better pendulum design would place the center of percussion at the point of contact.
6.0 NUMERICAL RESULTS AND DISCUSSION

6.1 Static Analysis

6.1.1 Prescribed Tension

Static load analysis of the simplified numerical model investigates the transfer of energy when conductor ends are constrained with fixed displacement and fixed tension. The quantities of plastic dissipation energy (PD) and frictional dissipation energy (FD) in the entire model are compared through the analysis with different prescribed boundary conditions.

With a constant axial tensile force applied at the model ends, the plastic dissipation energy and frictional dissipation energy are given in Figure 6.1 and Figure 6.2, respectively. Energy dissipated by the plastic deformation of aluminum for all tensions is negligible in the tension and contact steps, indicating that strain in the aluminum is entirely elastic until the transverse compression load is applied. As transverse loading is applied, the plastic dissipation energy increases with the magnitude of the applied load as the model deflects more. In addition, the aluminum exhibits more plastic strain at lower tension, and as axial tension increases the value of plastic dissipation energy appears to converge in Figure 6.1 to $PD = 8.36 \, \text{lb} \cdot \text{in} \approx (0.945 \, \text{Nm})$.

Upon removal of the transverse load in the unload step there is a negligible increase of the plastic dissipation energy, indicating that as the conductor straightens there is only a negligible additional amount of plastic strain in the aluminum.
Frictional dissipation energy from the fixed tension analysis shown in Figure 6.2 remains low for the tension and contact steps, suggesting that relative motion between the components in the model is small. Upon loading, the frictional energy increases with more friction at lower applied axial tension. This means that there is a greater degree of overall motion and/or the contact forces between surfaces are greater. As the transverse load is removed in the unload step, there is a slight increase in the energy lost to friction as the conductor recovers elastic strain to reach its final curvature.

Permanent deformation of the model is shown in Figure 6.3 and Figure 6.4 for the axial tensions of 500 lbf (2.22 kN) and 3000 lbf (13.34 kN), respectively. In both figures the shadow of the geometry shows the model prior to loading with the resulting displacements given with a deformation scale factor of 5.0. The resulting deformation after loading shows that for the load conditions modeled, an axial tension of 500 lbf (2.22 kN) allows more curvature in the conductor when compared with tension of 3000 lbf (13.34 kN).
Figure 6.1: Total plastic dissipation energy under prescribed tension; static model
Figure 6.2: Total frictional dissipation energy under prescribed tension; static model
Figure 6.3: Permanent deformation after unloading under 500 lbf (2.22 kN) prescribed tension

Figure 6.4: Permanent deformation after unloading under 3000 lbf (13.34 kN) prescribed tension
6.1.2 Prescribed Displacement

The resulting plastic dissipation energy and frictional dissipation energy when fixed displacements are applied at the model ends to create an initial axial tension, are given in Figure 6.5 and Figure 6.6, respectively. Energy dissipated by the plastic deformation of aluminum for all tensions is negligible in the tension and contact steps, indicating that strain in the aluminum is entirely elastic until the transverse compression load is applied. As transverse loading is applied in the load step the plastic dissipation energy increases independently of the initial tension to an average final value of \( PD = 8.7 \text{ lbf} \cdot \text{in} (0.98 \text{ Nm}) \). Upon removal of the transverse load in the unload step there is a negligible change of the plastic dissipation energy, indicating that as the conductor straightens there is a negligible additional amount of plastic strain in the aluminum.

Frictional dissipation energy shown in Figure 6.6 remains low for the tension and contact steps, suggesting that relative motion between the components in the model is small. Upon loading, the energy lost to friction increases with little dependence on the initial axial tension when compared to the fixed tension analysis. As the transverse load is removed in the unload step there is a slight increase in the energy lost to friction as the conductor recovers elastic strain to reach its final curvature and a final average value in the frictional dissipation of \( FD = 3.3 \text{ lbf} \cdot \text{in} (0.37 \text{ Nm}) \).

Permanent deformation of the fixed displacement model is shown in Figure 6.7 and Figure 6.8 for the initial axial tensions of 500 lbf (2.22 kN) and 3000 lbf (13.34 kN), respectively. In both figures the shadow of the geometry shows the model prior to loading with the resulting displacements given with a deformation
scale factor of 5.0. The resulting deformations after loading show that for the load conditions modeled with fixed end displacements, initial axial tensions of 500 lbf (2.22 kN) and 3000 lbf (13.34 kN) result in similar deformations after loading. Contrary to the prescribed tension boundary condition, fixed displacements result in significantly less model deflection.

Figure 6.5: Total plastic dissipation energy under prescribed displacement; static model
Figure 6.6: Total frictional dissipation energy under prescribed displacement; static
Figure 6.7: Permanent deformation after unloading under 500 lbf (2.22 kN) prescribed displacement

Figure 6.8: Permanent deformation after unloading under 3000 lbf (13.34 kN) prescribed displacement
6.2 Dynamic Analysis

The dynamic analysis of the numerical model investigates the values of total internal strain energy, recoverable elastic strain energy, plastic dissipation energy, and frictional dissipation energy as a function of impact velocity, and compares the dynamic analysis results with a similar static loading analysis.

The model values of internal energy, recoverable elastic strain energy, plastic dissipation energy, and frictional dissipation energy of the static (red) and dynamic (black) solutions are plotted as a function of tool displacement in Figure 6.9, Figure 6.10, Figure 6.11, and Figure 6.12, respectively. Total internal strain energy is the summation of plastic dissipation energy and recoverable strain energy, and shows good agreement between the velocity controlled explicit results and the displacement controlled static results. With increasing velocity of the tool surface the energy of the impact increases, this increase of energy causes more deflection in the modeled geometry. For the impact velocities simulated, total strain energy appears to depend on the energy of impact and not velocity; evident in the total strain energy following the same path upon loading of all velocities.

Recoverable strain energy is largely dependent on the energy of the impact, shown by the similar paths of loading and unloading. Plastic dissipation energy appears independent of impact velocity as the static and dynamic results follow similar paths for the same magnitude of deflections and only slight increases as the load of impact is reduced. The frictional dissipation energy shows a significant dependence on both impact energy and impact velocity. In the dynamic analysis, frictional dissipation energy
changes significantly with the velocity of impact; this dependence is evident by the
different values of frictional dissipation for each impact velocity at the same magnitude
of deflection.

Figure 6.13 compares the static and dynamic results for each of the modeled
impact velocities at the point of contact release of the plastic dissipation and total strain
ergy of Figure 6.9 and Figure 6.11, respectively. The close relation of these results
suggests that the values of plastic dissipation and internal strain energy are dependent on
impact energy and not impact velocity. Figure 6.12 shows that the frictional dissipation
energy increases with both velocity and energy and cannot be well represented by
previous results from the static loading condition which have much lower magnitudes.

It should be noted that the highest velocity modeled of 75 in/s (1.91 m/s) is
lower than the experimental impact velocity of an average 138 in/s (3.5 m/s).
Figure 6.9: Total internal strain energy, dynamic (black) and static (red)
Figure 6.10: Recoverable strain energy, dynamic (black) and static (red)
Figure 6.11: Plastic dissipation energy, dynamic (black) and static (red)
Figure 6.12: Frictional dissipation energy, dynamic (black) and static (red)
Figure 6.13: Comparison of static and dynamic plastic dissipation and internal strain energy at point of contact release
7.0 CONCLUSIONS

7.1 Experimental

The experimental work conducted for this thesis represent the first laboratory testing of HV transmission line conductors exposed to low velocity transverse impact. It created a unique set of data which should help the manufacturers and users understand the response of standard and novel overhead conductors to transverse impact.

The specially designed and built impact test fixture produced repeatable results for impacting energies up to 230 lb f · ft (312 Nm) with a cylindrical impacting surface having a velocity of 11 ft/s (3.4 m/s) and boundary conditions of 3-point impact or initial axial tension from 258 lb f (1.15 N) to 6573 lb f (29.2 kN).

All impact experiments performed on ACCC samples under the above conditions were successful. They resulted in dissipated energies ranging from 195.6 lb f · ft (265.2 Nm) (3-point impact and no axial tension) to 77.9 ft · lb f (106 Nm) (constrained ends with axial tension of 6573 lb f (29.2 kN). With an increase in axial tension, the dissipated energy was observed to gradually decrease.

The tested samples of ACCC showed more resistance to low velocity transverse impact under constrained end conditions with and without initial axial tension. Not a single ACCC sample tested in the fixture under such conditions resulted in rod failure. It was also observed that the ACCC design is more susceptible to severe damage of the
composite core if the ends of the conductor are left free in the fixture. One out of three samples tested with unconstrained ends resulted in composite rod collapse. This could indicate that ACCC conductors with relatively moderate constraints during installations could develop severe rod damage by excessive bending if exposed to transverse impacts. This effect, of course, is strongly dependent on the length of the conductor subjected to impact, which was not investigated in this research.

Under the 3-point impact condition (unconstrained ends), two tests resulted in $130.0 \, \text{lbf} \cdot \text{ft} (176.3 \, \text{Nm})$ and $123.1 \, \text{lbf} \cdot \text{ft} (166.9 \, \text{Nm})$ of dissipated energy (no discernible rod collapse) and one $195.6 \, \text{lbf} \cdot \text{ft} (265.2 \, \text{Nm})$ (apparent rod collapse). Therefore, for the assumed impact energy and the length of the samples, this situation could be a transition for ACCC to fail by excessive bending and rod collapse, or by less severe failure modes observed under the constrained ends with tension. It can be expected that this transition range will be dependent on the length of the span and impact situations.

Visible permanent damage to aluminum stranding is more evident after 3-point impact (no end constraints) with a greater separation of strands caused by plastic strain and a greater degree of permanent curvature than under axial constrain. For fixed end constraints, ACCC exhibits significantly less permanent deformation of aluminum strands in comparison with the free end condition, and the damage to the strands seems to get less noticeable as the initial tension increases. This could suggest that transversely impacting objects could generate much less visible damage to the strands if the conductor is under large axial tension. It should also be stated here that under large axial loads and
constrained ends, at large impact energies, the conductors could develop severe damage to their rods, not visible from the outside. However, this effect was not evaluated in this work due to the limitations of the fixture.

The method utilized for gripping the ACCC conductor ends in the fixture was sufficient for 230 lbf \( \cdot \) ft (312 Nm) impacts with initial tensions up to 2578 lbf (11.5 kN). Experiments with tensions above that exhibited slipping of the composite rod within the aluminum. The slip experienced in the clamps of the two highest tension test conditions makes the measured value of energy dissipated through impact overestimated. It was also observed that fixture vibrations under impact could also contribute to the accuracy of the energy dissipation measurements. More work is still required to improve the fixture to handle much higher impact energies and applied loads without the detrimental effect of end slippage and vibrations.

7.2 Numerical

The numerical analyses presented in this study only approximate the experimental setup to help understand how energy dissipation depends on boundary conditions, and how the dissipated energy is distributed across the frictional, plastic and recoverable energies. The overall modeled geometry represents that of the experimental samples, but was simplified by the number of strands and the length of the specimens. Therefore, the numerical and experimental results are expected to illustrate similar trends and dependencies, but not match exactly.

For the properties and conditions modeled using the static assumptions, the numerical results suggest that energy dissipated by plastic deformation and friction
increase with the maximum curvature of the conductor. When the conductor is fixed at
the ends, initial tension has little effect on the unrecoverable mechanisms of plasticity and
friction under the same loading conditions.

Despite the fact that most of the finite element modeling was performed under
static conditions, the limited dynamic calculations show that the effect of impact velocity
could be ignored for the impact situations considered in this research. Therefore, it can be
assumed that the numerical research on the effect of boundary conditions on energy
dissipation in the ACCC conductors under static assumptions, contributed well to the
understanding of the behavior of the conductor subjected to impact in the newly designed
fixture.

7.3 Application

This research was initiated in an attempt to identify the most damaging conditions
to HTLS conductors subjected to low-velocity impact and determine their impact damage
tolerance. For the impact energies tested, ACCC showed greater resistance to damage
when constrained under tension. It has also shown that no ACCC can resist quite well
low velocity impacts which could occur either during installation or in service. The only
catastrophic rod failures observed in this work occurred under conditions of zero tension
and no axial constraint; this type of predictable failure is caused by excessive bending. At
the same time, no catastrophic rod failures that might occur under higher tension were
observed for the considered impact condition. Identification of such abrupt failure modes
were sought in this research, however, due to the limitation of the impact apparatus and
time constrains, the research has not yet accomplished this. Clearly more research is still required to understand the behavior of the conductors under more severe load conditions.

The experimental and numerical results evaluated ACCCs response to low-velocity transverse impact from a cylindrical surface. The conductor should behave very different when exposed to other low and high-velocity impacts, and this type of response cannot be predicted from the current work. To evaluate such cases, more complex and comprehensive FE modeling would be necessary.
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