DESIGN, FABRICATION, AND TESTING OF AN ULTRASONIC DE-ICING SYSTEM FOR HELICOPTER ROTOR BLADES

A Thesis in
Aerospace Engineering
by

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A low-power, non-thermal ultrasonic de-icing system is introduced as a possible substitute for current electro-thermal systems. The system generates delaminating ultrasonic transverse shear stresses at the interface of accreted ice. A PZT-4 disk driven at 28.5 KHz (radial resonance of the disk) instantaneously de-bonds 2 mm thick freezer ice layers. The ice layers are accreted to a 0.7 mm thick, 30.4 cm x 30.4 cm steel plate at an environment temperature of -20°C. A power input of 50 Watts is applied to the actuator (50 V, 19.6 KV/m), which translates to a de-icing power of 0.07 W/cm². A finite element model of the actuator bonded to the isotropic plate is used to guide the design of the system, and predicts the transverse shear stresses at the ice interface. Wind tunnel icing tests were conducted to demonstrate the potential use of the proposed system under impact icing conditions. Both glaze ice and rime ice were generated on steel and composite plates by changing the cloud conditions of the wind tunnel. Continuous ultrasonic vibration prevented impact ice formation around the actuator location at an input power not exceeding 0.18 W/cm² (1.2 W/in²). As ice thickness reached a critical thickness of approximately 1.2 mm, shedding occurred on those locations where ultrasonic transverse shear stresses exceeded the shear adhesion strength of the ice. Finite element transverse shear stress predictions correlate with observed experimental impact ice de-bonding behavior. To increase the traveling distance of propagating ultrasonic waves, ultrasonic shear horizontal wave modes are studied. Wave modes providing large modal interface transverse shear stress concentration coefficients (ISCC) between the host structure (0.7 mm thick steel plate) and accreted ice (2.5 mm thick ice layer) are
identified and investigated for a potential increase in the wave propagation distance. Ultrasonic actuators able to trigger these optimum wave modes are designed and fabricated. Despite exciting wave modes with high ISCC values, instantaneous ice de-bonding is not observed at input powers under 100 Watts. The two triggered ultrasonic wave modes of the structure occur at high excitation frequencies, 202 KHz and 500 KHz respectively. At these frequencies, the ultrasonic actuators do not provide large enough transverse shear stresses to exceed the shear adhesion strength of the ice layer. Neither the actuator exciting the $SH_1$ mode (202 KHz), nor the actuator triggering the $SH_2$ mode (500 KHz) instantaneously de-bonds ice layers with an input power under 100 Watts.
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Chapter 1

INTRODUCTION

The effects of aircraft icing were first noticed between the 1920’s and the 1940’s as aircraft flew over longer ranges and higher altitudes in changing weather conditions. The earliest icing protection systems were investigated and implemented to fixed wing aircraft between 1935 and 1949, particularly on DC-3’s, B-29’s and other aircraft during World War II [58]. The advances accomplished in airplane electro-thermal ice protection systems were not investigated for rotorcraft application until 1956 [1, 2]. Aircraft icing continues to be a concern since it has caused 583 (civil) aviation accidents and over 800 deaths in the U.S. alone between 1982 and 2000 [3].

The altitude range of operation of helicopters makes them susceptible to encounter icing conditions. Helicopter and tilt-rotor blades operating at temperatures below -10º C tend to collect ice along the majority of the blade’s leading edge [4]. Water can freeze on impact with the leading edge of the rotor blade when a combination of temperatures close to freezing, high speeds, and high cloud water concentrations occur [5]. As ice accumulation alters the stagnation point geometry of the blade, the performance of the vehicle decreases. High vibration levels are also introduced due to unevenly distributed rotor ice adhesion.

Eighty percent of worldwide rotorcraft and ninety-five percent of U.S. rotorcraft currently operate without rotor ice protection due to economical drawbacks related to power consumption and manufacturing complexity [6]. Because of the lack of effective
rotor de-icing, the general rule is to avoid icing conditions (forecasted or otherwise) in order to eliminate the possibility of icing related accidents (Figure 1.1). These limitations reduce the versatility of rotorcraft to achieve operational parity with their fixed-wing counterparts [6].

Unevenly shaped and distributed accreted ice considerably modifies the flow field around the airfoil, changing the lift and drag characteristics of the blade. Premature transition, vibration due to mass unbalance, and flow separation become significant safety concerns for the vehicle and its crew. The increase in drag generated by accreted ice makes necessary an increase in torque to maintain the lift conditions [5]. The high fluctuating drag over very short periods of time results in sudden large torque increases. The transmission or engine limits can be reached in this dangerous fluctuating environment, making maintaining a given flying condition impossible for the pilot [5].

Ice shedding is another major problem introduced by ice accretion on rotating blades. Scavuzzo et al. [8] demonstrated how the shear stresses created by centrifugal forces at the interface between ice and the leading edge of the airfoil increases linearly with ice thickness. When the increasing shear stresses exceed the ultimate adhesive shear strength, the ice will detach, releasing shards of ice that could cause serious damage to the aircraft. As ice sheds unevenly, rotor mass unbalance introduces undesired vibrations and changes in the handling of the vehicle.

To avoid critical ice formation on the rotor, industry has adopted a standard de-icing system for a limited number of helicopter models. This de-icing system is based on thermal energy to melt accreted ice. Such a system requires large amounts of energy and contributes to an undesired increase in the blade’s overall weight and cost. Due to these
drawbacks, many civil helicopters do not employ any de-icing capabilities, limiting the operations of these vehicles under adverse conditions. The thermal de-icing mechanism is only run periodically in order to avoid large power consumption or excessive heating of the leading edge structure.

![Image of helicopter and ice accretion](image)

**Figure 1.1: Vehicle Grounded due to Icing Conditions (Top Left), Ice Accretion Fuselage Antenna (Top Right), Rotor Leading Edge (Bottom) [Colyer et al., Ref. 7]**

The discontinuous “pulsing” of heater blankets results in ice accretion on the rotor. Melted ice has the potential to flow along the chord direction and refreeze further aft. De-bonded pieces of ice (typically at least 7.5 mm thickness) can possibly impact sensitive parts of the vehicle, such as the tail rotor, or be ingested by the vehicle’s engines, thus causing catastrophic effects [4, 5, 9].
Currently, leading edge protection caps are typically made of metallic materials to prevent erosion. These materials are being replaced by high erosion resistant polymer materials that perform better than metallic leading edges. Thermal de-icing systems are not suitable for the new generation polymer based leading edge protection caps, since the low thermal conductivity of the polymer materials promotes their melting when overheated. Under icing conditions, ice accretes in the region protected by the erosion cap. Thermal de-icing mechanisms would conflict with the implementation of low thermal conductivity polymer based protection caps.

The ideal ice protection system would actually be an anti-icing system; that is, the surface would remain clear of any ice formation [4, 10]. New, low-power consumption, and non-thermal anti-icing mechanisms would solve all previously discussed problems caused by icing. Such a system has not yet been conceived. In addition, an ice-phobic surface that can also withstand erosive effects has not yet been engineered [10]. Even though many silicon based ice-phobic materials have been identified, none of them have demonstrated the survivability required for a helicopter leading edge erosion cap [56].

1.1 Ice Accretion Effects on Rotor Performance

During the past 20 years, the number of agencies studying rotorcraft icing has increased considerably [5]. Efforts towards solving rotorcraft icing are being conducted by all the major helicopter companies in the United States (Boeing [6], Sikorsky [11], Bell Helicopters [4]). Collaboration between international agencies, such the joint effort
performed by CIRA (Italy) and Boeing (USA), is being conducted as an attempt to understand ice accretion and to solve its related problems [6].

Rotorcraft icing is defined as “flight in cloud at temperatures at or below the freezing point when super cooled water droplets impinge and freeze on the unprotected areas on which they impact” [5]. Two basic types of ice appear on the surface of rotorcraft blades in a subfreezing environment: rime and glaze ice (Figure 1.2). Each type of ice occurs under different flight conditions, and is affected by the following variable factors: induced and forward velocity, liquid water content in the atmosphere, droplet size distribution, and encountered temperature. Droplet size and temperature have the greatest influence on the type of ice that accretes to the blade. As previously stated, both types of ice negatively influence the performance of the rotor’s airfoil by altering their aerodynamic shape and changing their weight distribution. Under adverse icing conditions, ice forms up to 15 percent of the mean aerodynamic chord on the top surface of most rotorcraft blade airfoils, and up to 25 percent on the lower surface [4].

![Figure 1.2: Types of Ice Accretion](image)

Rime ice occurs at low blade velocities (closer to the blade root), low water vapor concentration (0.5 – 1.0 g/m$^3$), and temperatures below the freezing point of water [5, 9]. The very low temperatures cause the water droplets to freeze on impact, resulting in a
smooth layer of rime ice on the leading edge of the airfoil. This layer tends to be a white-opaque, streamlined accretion, but with a surface roughness much higher than that of the airfoil.

The crystalline structure of glaze ice is different from rime ice because it forms at higher blade velocities, higher water concentrations ($1.5 - 3.0 \text{ g/m}^3$), and at temperatures just below the freezing point. Higher temperatures than those encountered in the formation of rime ice do not make water droplets freeze on contact with the airfoil. Instead, the water droplets travel back on the chord’s airfoil direction and over existing ice before they freeze and attach to the surface [9]. This effect is known as “runback effect”. Glaze ice typically has a stronger influence than rime ice on the lift and drag characteristics of the airfoil due to irregular ice “horn” structures created on the leading edge of the airfoil.

Due to the variation in local velocity along the span of a helicopter rotor, it is possible to encounter both rime and glaze ice accretion on the same blade. Rime ice will tend to form on inboard locations, while glaze ice will tend to form on outboard positions.

A third, but relatively uncommon kind of ice, known as beak ice, can also be encountered. This type of ice accretion occurs in the very far outboard location of propellers (could be found in the V-22 rotors in forward flight configuration). Due to the high centrifugal forces encountered by rotor blades, this type of ice is not commonly observed on helicopter rotors. Compression of the air at the stagnation point on the leading edge of the blade raises its temperature, reaching temperatures as high as $22^\circ \text{C}$ at the very tip of the rotating blade. For this reason, at temperatures close to freezing, ice is
usually observed on the inboard sections of the blade, while the more outboard sections remain free of beak ice [10]. Higher centrifugal forces on rotorcraft blade outboard sections also prevent ice from forming at those locations.

The rotor blade is the component of the rotorcraft that collects ice at a faster rate than other parts of the vehicle. Since the blades of the helicopter are traveling at faster speeds relative to the helicopter fuselage, they encounter more droplets of water per second, which, under icing conditions freeze on impact on the leading edge. The higher collection efficiency of the blades also induces a faster rate of ice accretion than on the rest of the vehicle [5].

Ice accretion also introduces vibrations due to mass unbalance, premature transition, and separation of the flow around the blade. All these negative effects of ice formation combine to make reaching a stable flight condition unfeasible. As ice accumulation increases, the collective input required also increases, elevating the required engine torque [12].

The most significant outcome attributed to ice accretion is the change in the profile drag of the sections of the rotor blade. The great increases in drag over very short periods of time result in large torque increases. This torque increase could rapidly reach the transmission or the engine limits, making the flight condition unviable and unsustainable [5]. This effect, as previously mentioned, is accompanied by undesired vibrations and changes in the handling of the vehicle, making flight conditions in inclement weather dangerous.

The impairment of the aerodynamic characteristics of a rotor airfoil due to ice accretion produces an increase in drag and a decrease in maximum lift capabilities. The
irregular shapes formed by the accreted ice on the leading edge are responsible for sudden changes in aerodynamic rotor ability [12]. Accreted ice is eventually shed due to acting centrifugal forces, blade flexing, air loads, or warmer flight conditions. This introduces the possibility of asymmetric shedding, resulting in unbalanced and out-of-track rotors. This can lead to high vibrations in the cockpit and damaging cyclic loads in the support structures of the rotors.

Catastrophic effects could occur when the shed ice from the main rotor impacts the tail rotor or vice versa. In previous research done at the University of Akron by Scavuzzo et al., it was found that the centrifugally induced shear stress at the interface between ice and the leading edge of the airfoil increases linearly with ice thickness [8]. When the increasing shear stresses exceed the ultimate adhesive strength, the ice will detach, releasing shards of ice that have the potential to seriously damage the aircraft.

Helicopter rotor ice accretion also affects the emergency landing capabilities of the vehicle, as it is illustrated in Figure 1.3 and Figure 1.4. The degradation of the rotor aerodynamics increases the rotor speeds required for autorotation, which consequently increases the descending speed to a level that is deemed unsafe for landing [10].
Figure 1.3: Negative Effects Introduced by Ice Accretion Related to Autorotation

Figure 1.4: Autorotative Speeds Trends [Prouty, Ref. 10]
1.2 Ice Protection Mechanisms for Rotor Blades

There are six different systems that have been designed and applied to rotorcraft anti/de-icing. The most commonly used de-icing system for helicopter rotors are heating coils that run along the span of the blade. This de-icing method is characterized by high-energy consumption and slow ice melting due to the slow thermal diffusion of the heat over the blade materials. In addition, blades made out of composite materials undergo delaminating processes when ohmic mats or hot air systems are applied, because they have much lower thermal conductivity than metal blades. De-icing systems increase the weight of the blade, have running time limitations, and introduce other problems such as a refreeze of the melted ice or impact of detached pieces of ice on critical parts of the vehicle, such as the tail rotor or the engine. In addition to the electro-thermal de-icing system, there are five other main anti/de-icing methods that have been explored for application to rotorcraft: fluid anti-icing, pneumatic boot de-icing, electro-impulse de-icing, low frequency vibratory de-icing and high frequency microwave de-icing.

1.2.1 Electro-Thermal De-Icing

The main helicopter de-icing method used today is the electro-thermal method [4, 11, 13]. It is the only one approved for commercial and military helicopters by the Federal Aviation Administration and Department of Defense. New main blade electro-thermal systems, such as the Rotor Ice Protection System (RIPS) for the S-92 are currently under certification process [59].
The electro-thermal system is heavy and requires large electrical power sources. The electrical power needed substantially exceeds the normal helicopter electrical system capacity, and therefore, electro-thermal methods can only be run periodically [13], allowing ice to be formed (up to 7.5 mm) while the system is not in use [4]. The electro-thermal de-icing mechanism is also one of the most expensive systems because relatively large electric power supplies are required to activate the mechanism. In recent studies Botura et al. demonstrated that high efficiency, low power electro-thermal de-icing systems that reduce the required power for conventional electro-thermal de-icing from 26 KW to 1.9 KW are feasible [14]. As long as the thermal properties of the materials forming the blade withstand the high temperature conditions, this improved system could be run continuously. Thus, the low-power electro-thermal system remains under investigation.

1.2.2 Fluid Anti-Icing

With the exception of continuous electro-thermal devices (systems with related drawbacks such as high weight penalties, and large power consumption) used in large helicopters, such as the Super PUMA or the Eurocopter, fluid anti-icing is the only system that has been applied to flying rotorcraft and that can keep the blades clear of ice at all times [15]. This system was implemented in the UH-1 Bell Helicopter and tested during the winters of 1960 and 1961.

The fluid anti-icing system consisted of an alcohol/glycerin reservoir, and a distribution system that controlled the flow to the main and tail rotor. An electric fuel
pump supplied fluid from the reservoir. The fluid was transferred from a fixed nozzle to the rotating blades via a slinger ring that used the centrifugal force from the rotational velocity of the blades. Grooves that are milled into the forward and aft surfaces of the blade nose and holes that are drilled into the leading edge, allow the fluid to escape through the holes and to flow over the blade surface [4]. Tests performed showed that the system was effective for 1 hour and 24 minutes while there was fluid available. The system was effective for temperatures below $-20^0$ C and liquid water contents (LWC) of $0.8\text{ gr/m}^3$. A small engine torque of two percent was required to effectively distribute the anti-icing fluid over the 83.8 cm (33 inches) chord blade. A ten percent penalty in engine torque was measured for the four-bladed Bell Model 412 due to the shorter chord length of the blades (43.8 mm). The electrical power consumption of the system was negligible, and runback effects were not encountered since heating was not necessary.

The major disadvantages discovered for this system its weight and the maximum time allowed in icing conditions (limited by the amount of fluid carried on: 11 gallon reservoir of alcohol/glycerin, designed by Bell Helicopter). In addition, the holes that allowed the fluid mixture to flow out became obstructed by dirt particles, thus handicapping the anti-icing system.

### 1.2.3 Pneumatic Boot De-Icing

Pneumatic de-icing (Figure 1.5) is another system that has been implemented in rotorcraft vehicles. Localized rubber boots inflate to crack-off accreted ice on the helicopter leading edge. It is the lightest and least expensive of all six methods, with an
almost negligible electrical power requirement. This system also eliminates the runback effect problem since it uses mechanical principles to remove accumulated ice [23, 67]. A major disadvantage, however, is the requirement of very high engine torque during boot inflation. Also, the rain and particle erosion resistance of the blade’s leading edge boots need to be addressed [4]. This de-icing system has satisfactorily removed ice from blades at a severity of 0.8 liquid water content (LWC) and -20°C [4, 23]. Despite the effectiveness of the system as a de-icing device, ice accumulated up to a thickness of 7.5 mm prior to removal. It is at this thickness that the device was shown to be most effective. The boots took 30 seconds to inflate, and with the required power increase, minor roll, yaw, and pitch trim changes were observed [4]. An additional potential drawback of the system is that removed ice could impact against critical parts of the vehicle such as the tail rotor or the engine.

![Image](image.png)

Figure 1.5: Pneumatic De-Icing Mechanism Detail [Norbert et al., Ref. 16]
1.2.4 Electro-Impulse De-Icing

Electro-impulse rotor de-icing is another de-icing system that has been investigated for application on rotorcraft [17, 18]. Flat-wound coils made of copper ribbon wire are placed just inside the leading edge of the skin of the blade with a small gap separating the skin and the coil. The coils are connected to a high-voltage capacitor bank. A discharge of the capacitor through the coil creates a rapidly forming and collapsing electro-magnetic field, which includes eddy currents in the metal skin. For a fraction of a millisecond, forces of several hundred pounds can be obtained. These skin movements help to shatter, de-bond, and to expel the ice.

Energy requirements for the electro-impulse de-icing system are low at 3 KW for a medium sized helicopter (10,000 to 15,000 lbs.). The weight of the de-icing system for a medium size helicopter would be 120 lbs. for both the main and tail rotors. Drawbacks include an increase in skin fatigue failure and electromagnetic interference with avionics [18].

1.2.5 Low Frequency Electro-Vibratory De-Icing

In 1978, Bell Helicopter carried out a feasibility study on a method that introduced mechanical vibration on the rotor blade as a means to eliminate ice accretion. In this de-icing method, the blades of the main rotor were excited at frequencies matching the major natural frequencies of the blade [4]. Electronic motors vibrated the blade at different resonating modes (bending and shear). Icing tests were conducted for UH-1D blades to determine if the low frequency vibrations would successfully de-ice the blades.
The results showed that for vibrations creating forces between 25 to 30 g-forces from 0 to 47 Hz, the blade was de-iced, except for the tip.

The fatigue loads generated were acceptable for the metallic UH-1D blade. Ice shedding impact, and the necessity of having an additional ice removal system in the tip regions, tended to discard this simple system when compared to other icing protection systems.

1.2.6 High Frequency Microwave De-Icing

High Frequency Microwave anti/de-icing systems are the last of the main mechanisms currently under study for ice protection. It was first demonstrated in 1978 that microwave guided waves effectively shed accreted ice [19]. Composite blades have an inferior thermal conductivity than metal materials and could suffer delamination when ohmic resistance heating mats are used (electro-thermal systems). A novel High Frequency Microwave anti/de-icing mechanism for Glass Fiber and Carbon Fiber Reinforced leading edges was designed and tested [20]. An electromagnetic field pattern volumetrically heats Carbon Fiber Reinforced Composite blades (application of 30 GHz millimeter-wave) and generates a thermal heat flux towards the cold, outside surface of the blade, leaving it free from accreting droplets or melting formed ice. Carbon Fiber Reinforced Composites have high electrical conductivity, and therefore, the high frequency microwaves are absorbed by the structure and the accreted ice is removed by thermal contact with the surface of the structure. Glass Fiber Reinforced Composites have low electrical conductivity, and the high frequency microwaves are transmitted
through the structure, creating an anti-icing system that radiates energy to the water/ice on the outside of the airfoil. Since the millimeter-wave power levels are too high according to High Intensity Radiated Fields standards, a coated resonator structure would be required to avoid avionic electronic interference from the microwaves.

1.2.7 Qualitative Summary Rotor Blade De/Anti-Icing Systems

A qualitative comparison based on industry experience between five described anti/de-icing systems is reported in Reference 4 (Table 1-1). High Frequency Microwave anti/de-icing mechanisms were also included in this table for completeness. The values in Table 1-1 were calculated for a water content of 0.5 gr/m$^3$ [4]. All the ice protection mechanisms were assumed to be operating on a four-bladed Bell Model 412.

Table 1-1: Comparison Blade Icing Protection Systems For Bell 412 [4, 20]

<table>
<thead>
<tr>
<th>De/Anti-Icing System</th>
<th>Electro-Thermal</th>
<th>Fluid</th>
<th>Pneumatic</th>
<th>Electro-Impulse</th>
<th>Vibratory</th>
<th>Microwave</th>
</tr>
</thead>
<tbody>
<tr>
<td>Application to Date</td>
<td>In production</td>
<td>Operational</td>
<td>Operational</td>
<td>Being Evaluated</td>
<td>Feasibility Study</td>
<td>Being Developed</td>
</tr>
<tr>
<td>Weight (lbs.)</td>
<td>162</td>
<td>194</td>
<td>54</td>
<td>120</td>
<td>120</td>
<td>To be determined</td>
</tr>
<tr>
<td>Ice accretion</td>
<td>Yes</td>
<td>No</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
<td>No</td>
</tr>
<tr>
<td>Electrical Power</td>
<td>26</td>
<td>Negligible</td>
<td>Negligible</td>
<td>3.0</td>
<td>1.3</td>
<td>15</td>
</tr>
<tr>
<td>Required (kW)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Performance Effects</td>
<td>10% torque rise</td>
<td>No penalty</td>
<td>10% torque rise</td>
<td>10% torque rise</td>
<td>10% torque rise</td>
<td>To be determined</td>
</tr>
<tr>
<td>Runback Potential</td>
<td>Yes</td>
<td>No</td>
<td>No</td>
<td>No</td>
<td>No</td>
<td>No</td>
</tr>
<tr>
<td>Detached Ice Impacts</td>
<td>Yes</td>
<td>No</td>
<td>Yes</td>
<td>Yes</td>
<td>Yes</td>
<td>No</td>
</tr>
<tr>
<td>Interference with Avionics</td>
<td>No</td>
<td>No</td>
<td>No</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
</tr>
</tbody>
</table>
1.2.8 Prediction of In-Flight Ice Accretion

Another system to consider in this introduction is not a de-icing system but rather a system to avoid flying into icing conditions. Ice accretion models have been implemented since the early 1980’s [21]. Another ice protection approach currently under study is a simplified in-flight, real-time method to predict airfoil performance degradation [6, 22, 23, 57]. The system alerts the pilot when critical icing conditions are expected during flight. The concept envisions future helicopters providing warning signals to the pilot with information about the safe time allowed to operate in a given icing environment.

The helicopter will be equipped with a probe that can look forward to determine the liquid water content and droplet size of super-cooled water droplets. As the aircraft enters icing conditions, the mission computer will warn the pilot of the performance reduction that will be incurred if the aircraft proceeds (Figure 1.6). In order to perform that calculation, the mission computer will have to compute the changes in lift, drag, and stall margin that occurs on the rotors for a given icing condition and duration. As this is a real-time process, the performance decline algorithm will be empirically based [6].

![Blade Leading Edge Predicted Ice Accretion](image)

**Figure 1.6: Example of Ice Accretion Modeling using CFL3D Software Used for Performance Degradation Predictions [Hartman et al., Ref. 6]**
1.3 Ice Adhesion Physics

All possible physical mechanisms responsible for ice adhesion can be categorized into one of the following four groups: a chemical bonding mechanism, a fluctuation in electromagnetic interaction (Van der Waals forces), a direct electrostatic interaction [24], or a mechanical clamping [25].

Chemical or covalent bonding corresponds to the adhesion due to chemical reactions and the formation of interfacial compounds. Chemical bonds are interactions of electrons leading to strong forces of attraction which hold atoms together in molecules and compounds. Atoms may transfer or share electrons, and either process may provide for a stable arrangement of electrons between the atoms that results in the formation of molecules. The force holding the atoms together is due to the attraction of each atom to another by the electrons that they are sharing. Such an attraction occurs in the range of 0.1 to 0.2 nanometers. This mechanism can be considered the lowest value of the overall adhesion energy of ice [24].

In compounds such as water, the hydrogen atoms are bonded to small atoms of high electronegativity. The hydrogen atom has only a very small share of the electron pair that forms the bond. Such molecules are highly polar and each hydrogen atom acts largely as an exposed proton. It can be attracted to, and form a weak bond with, the highly electronegative atom of a neighboring molecule (these bonds are known as hydrogen bonds).

Hydrogen bonding occurs between water molecules. Water must therefore be raised to a much higher temperature before the kinetic energy of its molecules becomes
great enough to break the hydrogen bonds between the molecules. Breaking these hydrogen bonds is necessary to boil water. Three-dimensional structures caused by hydrogen bonding give ice crystals a crystalline arrangement with many hexagonal openings. This open structure accounts for the low density of ice.

Van der Waals forces act between all surfaces. These forces depend on macroscopic characteristics of the solid such as dielectric molecular frequencies [24, 26]. Attractions are electrical in nature; however, in a symmetrical molecule like hydrogen, on average, there is no electrical distortion to produce positive or negative parts. In a water molecule, the electrons are mobile, and at any one instant they might find themselves towards one end of the molecule, making that end negatively charged. The other end is temporarily short of electrons, and so it becomes positively charged. A constant shifting of the electrons in the molecule creates fluctuating dipoles even in a symmetrical molecule such water. As molecules with different polarities approach each other, an induced dipole occurs between them. The polarity of both molecules might reverse an instant later, but as long as the molecules stay close to one another, the polarities will continue to fluctuate in synchronisation so that the attraction is always maintained [69].

Two solids that contain non-compensated or spatially separated charges will also generate electrostatic forces. It has been proven that electrostatic forces account for the larger energy component involved in ice adhesion. The electrostatic interaction between ice and other surfaces supplies energy that is significantly higher than chemical bonding energy and Van der Waals forces at distances greater than intermolecular ones. Electrostatic forces also help explain time and temperature dependence on ice adhesion [24].
Finally, adhesive ice sticks to a surface because it flows into microscopic pores of the substrate and freezes, expanding and, thereby, forming an interlocking system [25]. This mechanism contributes largely to the overall ice adhesion energy.

The adhesion strength of ice to a substrate results from tensile and shear components of the adhesion force at the interface between the ice and the substrate [27]. Shear is the weakest of the two adhesion force components [28]. In order to break off adhesive bonds between ice and a given surface, the shear adhesive forces encountered must be overcome [28, 27]. Bascom et al. [29] demonstrated by the inspection of failed accreted ice that dislocations and other defects occurred parallel to the shear force direction, implying weak shear adhesion compared to tensile adhesion strength [27]. The shear stresses encountered on impacting ice to a rotating airfoil is linearly dependent to the ice thickness. As the shear stresses exceed the ultimate strength of the ice, shear cracks propagate, and tensile failure occurs at the crack location [8].

The ice adhesion strength to various types of solids has been extensively studied [25, 30, 31]. When water is frozen on a clean metal surface, the interface is stronger than the ice, and fracture is expected to occur within the ice itself [30]. When tensile stresses are high, the failure is brittle, and the breaking stress is independent of temperature. On the other hand, ductile failure occurs when the tensile stresses are below the critical limit. The required de-bonding stress increases linearly as temperature reduces below 0°C [30]. Surface contaminants decrease the adhesion by large factors since the contact area between impacting ice and a clean metal surface –where strong bonding occurs– is reduced. The adhesion to polymers is less than that of metals [30, 31], and failure occurs at the interface between the accreted ice and the material surface. However, extensive
studies performed by Scavuzzo and Chu [28, 8] demonstrate that adhesive shear strength of ice is independent of the substrate material. The difference in performance between icephobic polymers and metals is not related to the material hydrophobic properties, but to the surface roughness of the material [32]. Still, no coating is perfectly icephobic, as ice can stick to anything at negative temperatures [33].

1.4 Ice Bonding Measurement

Any feasible method to perform adhesion tests has to effectively measure the ice removal force (adhesive failure). Tests used to measure ice adhesion have used traction or compressive forces to measure ice failure [25, 30, 34, 35]. The adhesion bond strength of ice measured during tests performed in compression, tension, and shear forces provides equal results; therefore they can be used at convenience [36]. On the other hand, these experiments require freezing water to be at a constant shape, because variations lead to highly variable results when the shape or conditions of the freezing water even slightly change [33].

To solve these limitations, researchers have adopted the use of centrifugal forces to perform adhesion tests [31, 33]. Rotating beams under controlled icing conditions are allowed to accrete ice. Once ice is accreted, the system is balanced and rotated. From the known revolution frequency of the system and the ice location, shear stresses are calculated (Figure 1.7).
The observations obtained during different ice bonding experiments can be summarized as follows:

- The adhesive shear strength of ice is independent of the substrate material and the ice thickness \[8, 28, 36\].
- The adhesive shear strength increases with slightly increasing droplet momentum \[8\].
- An increase in the substrate’s roughness highly increases the interfacial adhesive shear strength \[8, 28, 33, 36\].
- The shear strength linearly decreases with increasing interface temperature between the accreted ice and the substrate. This bonding strength decrease is especially noticeable for temperatures between \(0^0\) C and \(-3.9^0\) C \[28\].

The adhesive shear strength of rime and glaze ice (the two types typically encountered in helicopter rotors) is 0.12 MPa and 0.4 MPa respectively \[27\]. The shear strength of rime ice is lower than that of glaze ice because it shears cohesively rather than at the ice-substrate interface \[8\]. Venna et al. \[37\], provides a summary of the most relevant ice adhesion shear strength measurements to isotropic metals performed to date. This compilation is presented in Table 1-2.
1.5 High Frequency Shear Vibration Effects on Ice Bonding

Ultrasonic ice adhesion detection can be performed over large surface areas during flight by using ultrasonic guided wave sensors [38, 40]. Shifts in the ultrasonic wave propagation modes due to ice loading is captured by ultrasonic sensors and processed by a computer. Ultrasonic guided waves have also been proven to be able to measure bond strength between structures, but have never been implemented to measure ice adhesion strength [41].

Few studies have been performed on the effects of ultrasonic waves on ice adhesion. Experiments performed by Chow et al. [42] showed that fragmentation of ice crystals is obtained when flow patterns around cavitation bubbles produced by ultrasonic vibration is applied to a super-cooled liquid. Experimental suppression of frosting on metal surfaces was also accomplished by the application of ultrasonic vibration [43]. Furthermore, melting of Helium, $^4$He, at 1.20 K was accomplished by pulsing ultrasound

<table>
<thead>
<tr>
<th>Author</th>
<th>Substrate</th>
<th>Shear Strength (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Wind-Tunnel Ice</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stallbrass and Price [31]</td>
<td>Al</td>
<td>0.026-0.127</td>
</tr>
<tr>
<td>Chu and Scavuzzo [28, 68]</td>
<td>Al</td>
<td>0.2-1.03</td>
</tr>
<tr>
<td>Refrigerated Ice</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Loughborough [39]</td>
<td>Al</td>
<td>1.52</td>
</tr>
<tr>
<td>Hass</td>
<td>Cu</td>
<td>0.85</td>
</tr>
<tr>
<td>Raraty and Tabor [30]</td>
<td>Stainless Steel</td>
<td>1.66</td>
</tr>
<tr>
<td>Bascom et al. [29]</td>
<td>Polished Stainless Steel</td>
<td>1.63</td>
</tr>
<tr>
<td>Ford and Nichols</td>
<td>Polished Stainless Steel</td>
<td>0.24</td>
</tr>
</tbody>
</table>
waves to the surface of the solid crystals [44]. The melting was attributed to the radiating pressure of the acoustic waves.

Ramanathan et al. [27] introduced a novel approach of “breaking ice-substrate bonds” by exceeding their adhesive strength using shear horizontal waves (Figure 1.8). Ice breaking or instantaneous ice delaminataion was not observed during the experiments conducted by the author. Melting of ice patches accreted to an aluminum plate was observed after 145 seconds, pointing towards heat propagation from the actuator. A clear melting pattern propagating from the actuator source to a closely located ice patch is observed in photos provided in his published research (Figure 1.8 [27]). The actuator was driven at the high admittance peak frequency of the patches (1 MHz).

Parallel studies performed at Akron University showed similar results when shear and out-of-plane vibration generated by piezoelectric patches was introduced on an aluminum plate with accreted ice patches formed in a freezer environment. In this research, the vibration frequency (1 KHz) matched a structural modal resonance of an aluminum airfoil structure [37]. Transverse shear stresses up to 7.5 MPa and normal stresses of 25 MPa were calculated at the ice interface at this resonating mode. The accreted patch was delaminated after 130 seconds. The combination of shear and impulse stresses reduced ice de-bonding time by 40% with respect to imparting only shear forces. The system considerably reduced the power required to de-bond the accreted glaze ice as compared to current ice protection systems (100 W vs. 2,000 W). In both Ramanathan work and the results published by Akron University, it took over 100 seconds for well formed ice to delaminate from the host structure. Clear melting is observed during the process.
Palacios et al. conducted proof-of-concept experiments in which thin aluminum tubes under torsional stresses generated by shear tube actuators prevented ice formation on the surface of the aluminum tubes when the first resonance (500 – 1000 Hz) mode of the structure was triggered (Figure 1.9, Figure 1.10). An input power of 70 Watts was applied to the actuator (350 V, 0.2 A). Water sprayed onto the thin walled structure froze on contact and was instantaneously removed by the system. The system did not delaminate well-accreted, slow forming ice. Large input power (200 W) was required to provide vibration stresses able to detach the slow freezing ice, jeopardizing the integrity of the structure [45, 46].

Figure 1.8: 6.35 x 3.81 x 0.15 cm Ice Patch; De-bonding at t = 115 and 145 seconds [Ramanathan et al., Ref. 27]
Figure 1.9: Schematic Coil Radiator – Liquid Nitrogen Bath Icing Experiment and Detail of Accreted Ice on Aluminum Tube [Palacios et al., Ref. 46]

Figure 1.10: Qualitative Ice Thickness vs. Time with Shear Actuator On and Off and Frequency Response Function for the Actuator – Thin Walled Structure System [Palacios et al., Ref. 45]
1.6 Proposed De-Icing System

Ultrasonic vibration presents a novel non-thermal approach for generating waves able to de-bond thin layers of ice (under 3 mm thick). Currently, electro-thermal de-icing systems allow for ice accretion of up to 7 mm in thickness. The proposed de-icing system takes advantage of actuator ultrasonic modes that generate high transverse shear stresses at the interface of an accreted ice layer (Figure 1.11).

Figure 1.11: Schematic of Proposed Leading Edge De-Icing System

Ultrasonic vibration has the potential to create sufficient stresses to promote shear cracks and de-bonding of accreted ice layers. The de-bonded ice layers are consequently removed from a helicopter blade under centrifugal forces. A reduction of the de-bonded ice layer thickness reduces the potential for damage due to ballistic impacts. The ultrasonic actuator system does not increase the weight to the blade since it is placed in
the leading edge of the airfoil, replacing leading edge masses that induce rotor inertia and move the center of gravity of the airfoil in the forward direction.

1.7 Objectives

The goal of this research is to predict and experimentally demonstrate that low-power ultrasonic waves can instantaneously de-bond thin layers of accreted ice. The present work involves the design, fabrication, bench top testing and wind tunnel testing of isotropic surfaces actuated by in-plane displacement ultrasonic piezoelectric actuators. The power required to de-bond an ice layer of a given thickness is compared to the power consumption of state-of-the-art electro-thermal de-icing.

To achieve this goal, the objectives of the present work are to: 1) use finite element modeling to predict interface shear stresses created by in-plane displacement piezoelectric actuators continuously driven at ultrasonic frequencies (\(d_{13}\) radial mode disks, and \(d_{11}\) bar elements); 2) investigate theoretical wave propagation modes that provide high transverse shear stresses at a given ice interface and that could potentially increase wave propagation distance; 3) design, fabricate and test resonating ultrasonic actuators that provide transverse shear stresses at an ice interface that exceed the shear adhesion strength of ice; 4) conduct a series of proof-of-concept experiments (freezer ice and wind tunnel impact ice) in order to investigate the effects of ultrasonic transverse shear stresses on ice adhesion; 5) correlate predicted transverse shear stresses at a given ice interface with experimentally observed ice de-bonding; 6) design an actuator that excites a high interface shear stress concentration ultrasonic wave mode; 7) fabricate and
test an actuator that triggers a high interface shear stress concentration ultrasonic wave mode and compare the required power consumption with continuously driven ultrasonic actuators; and 8) compare the power consumption of the proposed ultrasonic de-icing system to electro-thermal state-of-the-art de-icing.

1.8 Thesis Overview

The thesis is organized into the following remaining chapters:

**Chapter 2: Actuator Modeling;** the finite element analysis performed to model in-plane displacement piezoelectric actuators is presented. PZT thermal analysis tools are also presented. A third set of tools to predict shear wave modes is also described. To generate modes that provide large wave propagation distances, ultrasonic wave modes (dispersion curves) that provide high interface shear transverse stresses concentrations between an ice layer and a host structure are theoretically predicted. The formulation of the analytical model used to characterize the dispersion curves of an isotropic plate covered with an accreted ice layer is described.

**Chapter 3: Modeling Tools Validation;** the predicted impedance behavior of the actuators is compared to experimental impedance measurements to validate the finite element model. Temperature predictions on the PZT actuators are also experimentally validated. To validate the analytical wave propagation modes of a host structure with an accreted ice layer, experimental dispersion curves are experimentally generated and compared to theoretical wave propagation modes.
Chapter 4: Ultrasonic Actuator Design; the design process followed to fabricate actuators able to trigger ultrasonic shear wave modes is discussed. The rationale behind the selection of the ultrasonic actuators used to demonstrate instantaneous ice de-bonding is also described.

Chapter 5: Ultrasonic Icing Experiments; freezer icing experiments are conducted on isotropic plates that are continuously driven by in-plane displacement ultrasonic actuators. The actuators generate transverse shear stresses on the interface of accreted ice layers that exceed the shear adhesion strength of ice. The experimental results are compared to finite element predictions. The results of experimental bench top tests of actuators triggering ultrasonic shear wave modes, with high shear stress concentration coefficients, are also presented in this chapter.

Chapter 6: Wind Tunnel Icing Test; the performance of a continuous wave ultrasonic de-icing device is tested under impact ice conditions in a wind tunnel. Experimental results are presented, and correlated to predicted de-icing capabilities of the system. The actuator power consumption during these tests is compared to the power consumption of electro-thermal systems.

Chapter 7: Conclusions and Recommendations; the final chapter summarizes the conclusions of the present work in designing, fabricating, and experimentally testing a non-thermal, ultrasonic anti-icing system for helicopter rotor blades. Suggestions for future work are also presented.
Chapter 2

ACTUATOR MODELING

The modeling tools used to select a de-icing actuator able to demonstrate instantaneous ice de-bonding are presented. The finite element modeling performed to analyze piezoelectric actuators, and the overall isotropic plate–actuator system, is described in this chapter. Radial resonance PZT-4 disk actuators are studied as candidates to create high transverse shear stresses at the interface between an isotropic plate and an accreted ice patch. PZT thermal analysis tools to determine the temperature change of the PZT material at a given input power are also presented. This tool is used to select an actuator that does not introduce major thermal stresses that could promote ice de-bonding. A final set of theoretical tools to determine ultrasonic shear wave modes are also introduced in this chapter. Ultrasonic shear wave modes with high transverse shear stress concentrations present the potential to increase the wave propagation traveling distance that affects ice bonding. These wave modes are identified and compared for different ice layer thicknesses.

2.1 Shear Stress Definition

It is important to differentiate between ZX transverse shear stresses and XY shear stresses that are created on the host structure under the actuation of a piezoelectric material (Figure 2.1). ZX transverse stresses are surface stresses, and are zero at free
traction surfaces [47], while XY shear stresses can be non-zero throughout a finite element thickness. When ice accretes to a surface, such surface is no longer free of tractions, and both XY stresses and ZX transverse shear stresses are present at the ice interface. ZX transverse shear stresses introduced at the ice–host structure interface are responsible for delamination processes between layers [64], while XY shear stresses contribute to ice cracking. As the transverse shear stresses exceed the shear adhesion strength of ice, ice delamination is predicted (Table 1-2).

Figure 2.1: Infinitesimal Element Stresses: $\sigma_{zx}$ is Zero at the Elements Free Traction Surfaces

When the XY shear stresses exceed the shear strength of ice, ice cracking occurs. The XY shear strength of ice under static loading at different strain rates is well documented (Figure 2.2). Determining the shear strength of ice under ultrasonic strains is beyond the scope of this work, since ice de-bonding under ultrasonic vibration (ZX shear stresses) is the main objective of the research.
2.2 Finite Element Modeling

Finite element predictions are obtained by using quadratic piezoelectric and isotropic elements. Convergence of the results is ensured by comparing results provided by different size finite element meshes. 20-node three dimensional continuum finite elements coupled with 20-node three dimensional piezoelectric elements are used to model piezoelectric actuators, as well as actuators attached to a host structure [74, 75, 76]. The host structure is an isotropic plate. A schematic of the 20-node 3 dimensional quadratic finite element used is presented in Figure 2.3.

The formulation of the piezoelectric and isotropic structure equations modeling a PZT actuator bonded to a host structure is defined in Equation 2.1 [74].

\[
\vec{F} = \left( [K_{uu}] - \omega^2 [M] - j \omega [R] \right) \vec{U} + \left[ K_{\psi} \Phi \right] \begin{pmatrix} \frac{i}{j} \omega \\ \Phi \end{pmatrix} = \left[ K_{\psi} \right] \vec{U} + \left[ K_{\psi} \right] \Phi \tag{2.1}
\]

where

\[\Phi = \begin{bmatrix} \Phi_1 \\ \Phi_2 \\ \Phi_3 \end{bmatrix} \]

\[k_{\psi} = \begin{bmatrix} \tilde{k}_{\psi_{xx}} \\ \tilde{k}_{\psi_{yy}} \\ \tilde{k}_{\psi_{zz}} \\ \tilde{k}_{\psi_{xy}} \\ \tilde{k}_{\psi_{xz}} \\ \tilde{k}_{\psi_{yz}} \end{bmatrix} \]

\[K_{\psi_{ij}} = \begin{bmatrix} k_{\psi_{xx}} & k_{\psi_{xy}} & k_{\psi_{xz}} \\ k_{\psi_{yx}} & k_{\psi_{yy}} & k_{\psi_{yz}} \\ k_{\psi_{zx}} & k_{\psi_{zy}} & k_{\psi_{zz}} \end{bmatrix} \]

\[K_{\psi} = \begin{bmatrix} k_{\psi_{xx}} & k_{\psi_{xy}} & k_{\psi_{xz}} \\ k_{\psi_{yx}} & k_{\psi_{yy}} & k_{\psi_{yz}} \\ k_{\psi_{zx}} & k_{\psi_{zy}} & k_{\psi_{zz}} \end{bmatrix} \]

\[M_{ij} = \begin{bmatrix} m_{xx} & m_{xy} & m_{xz} \\ m_{yx} & m_{yy} & m_{yz} \\ m_{zx} & m_{zy} & m_{zz} \end{bmatrix} \]

\[R_{ij} = \begin{bmatrix} r_{xx} & r_{xy} & r_{xz} \\ r_{yx} & r_{yy} & r_{yz} \\ r_{zx} & r_{zy} & r_{zz} \end{bmatrix} \]

\[U_{ij} = \begin{bmatrix} u_{xx} & u_{xy} & u_{xz} \\ u_{yx} & u_{yy} & u_{yz} \\ u_{zx} & u_{zy} & u_{zz} \end{bmatrix} \]

\[\omega = \text{frequency}\]
\( \vec{F} \)  Applied Mechanical Force;
\( \vec{I} \)  External Electrical Current;
\( \vec{U} \)  Elastic Displacement;
\( \Phi \)  Electric Potential;
\( [K_{uu}] \)  Elastic Stiffness Matrix;
\( [K_{\Phi u}] \)  Piezoelectric Stiffness Matrix;
\( [K_{\Phi \Phi}] \)  Dielectric Matrix;
\( M \)  Mass Matrix;
\( R \)  Dissipation Matrix;
\( \Omega \)  Angular Frequency.

The matrix equations may be adapted to different analyses types, such as static, modal, harmonic, and transient. To predict the standing wave condition of a structure,
harmonic calculations are used, while to predict wave propagation, transient calculations are performed.

Harmonic analysis is implemented to calculate the impedance of a system at different frequencies. The frequency dependent impedance values are used to determine the actuator-structure resonance frequency. At any selected driving frequency, the displacement and stress fields in the structure are calculated. Dielectric and mechanical losses of the PZT material are taken into account in the finite element model. The finite element computation matrix for harmonic analysis is presented in Equation 2.2.

\[
\begin{bmatrix}
K_{uu} - \omega^2 M & K_{u\Phi} \\
K_{u\Phi} & K_{\Phi\Phi}
\end{bmatrix}
\begin{bmatrix}
U \\
\Phi
\end{bmatrix}
= 
\begin{bmatrix}
0 \\
- Q
\end{bmatrix}
\] (2.2)

The vector of the nodal charges, \( Q \), is such that all the nodes \( i \) that belong to an electrode \( p \) with potential \( \Phi_p \), the sum of the charges \( Q_i \) is equal to \( \Phi_p \). For all the other nodes that do not belong to an electrode, \( Q \) is equal to zero.

### 2.2.1 Harmonic FEM Ultrasonic Disk Actuator

Harmonic response of piezoelectric disk actuators made out of PZT-4 is predicted. The electrical and mechanical properties of the material are described in Table 2-1. The material constitutive equations and the definition of all the material constants are well documented in the literature, and presented in Appendix A. The actuators have a 38.1 mm (1.5 inch) radius and a thickness of 2.54 mm (0.1 inch). They are poled along their thickness direction, and electrodes are placed on the top and bottom surfaces of the disk. A schematic of the PZT-4 disk is illustrated in Figure 2.4.
PZT-4 disks were selected as de-icing actuators because of the large in-plane stresses generated by the radial resonating mode of the proposed shape and material. PZT materials are classified as “hard” and “soft” depending on the materials used to dope the piezoelectric elements. PZT-4, also known as Navy Type I, is a hard material with low piezoelectric coefficients, low permittivity, low mechanical losses, low electric resistivity, and high mechanical quality factors and coercive fields. These properties make PZT-4 very resistant to de-poling, and ideal candidates to drive structures where high power and low losses are dictated by design [50, 70]. Hard PZT material is preferred for the proposed de-icing actuator because the stresses generated by constrained hard PZT elements are larger than those created by softer piezoelectric materials [27].

![Diagram of FE Mesh and Poling and Electrode Schematic](image)

**Figure 2.4:** a) Sample Converging Mesh used for the Modeling of the Disk Actuator Poling and b) Electrodes Schematic of a PZT-4 Disk
The radial resonance of the actuator, \( f_s \), was estimated from the planar frequency constant of the material, \( N_p \), defined by Equation 2.3. The calculated radial resonance of the 38.1 mm disk is 28.6 KHz.
FE calculations also predicted the radial resonance of the disk. The impedance of the actuator, \( Z \), is calculated for different frequencies. The impedance of the system is defined as the ratio of the voltage, \( V \), to the current, \( I \), in the alternating-current circuit (Equation 2.4). A low peak of the impedance implies that the input voltage becomes a minimum at the same time as the current becomes a maximum. The frequency corresponding to an impedance low is that of a resonance mode, \( f_s \). Similarly, and since the impedance of the system is defined as the inverse of the admittance (Equation 2.5), the frequency at which an admittance low occurs corresponds to an anti-resonance mode.

\[
Z = \frac{V}{I} \quad \text{(2.4)}
\]

\[
Z = \frac{1}{Y} \quad \text{(2.5)}
\]

The impedance low frequency of the actuator, \( f_s \), presented in Figure 2.5, corresponds to the radial resonance of the disk (28.6 KHz). The radial mode resonance frequency of the actuator predicted using finite elements matches that frequency calculated from the frequency constant of the material, with an error of less than 1%. At the radial resonance frequency, the impedance value of the actuator is 9 Ohms. In order to efficiently excite a given ultrasonic mode, impedance matching between the source (amplifier) and the load (actuator) must be enforced (see Appendix B).

The disks displacement when driven by 50 V (19.6 KV/m) at its radial resonance frequency is illustrated in Figure 2.6.
Figure 2.5: FEM Prediction of Disk Actuator Impedance (Resonance 28.5 KHz)

Figure 2.6: Radial Resonance Free Displacement PZT-4 Disk (28.6 KHz, 50 V)
2.2.2 Harmonic FEM Actuator Disk-Steel Plate

In-plane disk actuators continuously driven at a given frequency create shear stresses at the surface of a steel plate. A schematic of the finite element model is depicted in Figure 2.7. The predicted ultrasonic XY stresses generated on a 30.48 cm x 30.48 cm x 0.711 mm steel plate with a 2.54 mm thick ice layer are presented in Figure 2.8. The ZX transverse shear stresses at the interface between the ice layer and the steel plate surface are also calculated and illustrated in Figure 2.9. Ice accreted to the host structure is assumed to be isotropic, with material properties described in Table 2-2.

<table>
<thead>
<tr>
<th>Young’s Modulus</th>
<th>Poisson’s Ratio</th>
<th>Density</th>
</tr>
</thead>
<tbody>
<tr>
<td>9.33 GPa</td>
<td>0.325</td>
<td>0.9197 g/cm³</td>
</tr>
</tbody>
</table>

At the driving conditions (50 V, 19.6 KV/m, 28.5 KHz), the ZX shear stresses at the ice interface exceed 1.66 MPa, which is the maximum adhesion shear strength of refrigerated ice to steel found in literature (Table 1-2). Ice de-bonding is predicted to occur at the radial resonance of the actuator when 50 V are applied (28.5 KHz continuous excitation).
Figure 2.7: FE Model PZT-4 Disk and 2.5 mm Thick Ice Layer Bonded to a 0.711 mm Thick Steel Plate

Figure 2.8: Predicted XY Shear Stresses Generated by a PZT-4 Disk on a 0.7 mm Thick Steel Plate with a 2.5 mm Accreted Ice Patch (50 V, 28 KHz)
2.2.3 Harmonic FEM Triple Disk Actuator - Clamped Steel Plate

A second prototype was modeled. The system had three PZT-4 disk actuators exciting a 30.4 x 30.4 cm steel plate. A layer of 1.2 mm was modeled as being perfectly bonded to the plate. To take into account the placement of the plate into a wind tunnel test section, the edges of the plate were assumed to be clamped Figure 2.10. The impedance of the system was predicted, and it is depicted in Figure 2.11. The system was modeled with an input voltage of 150 V (83.5 KV/m) and at a frequency of 31.6 KHz. The transverse shear stresses at the steel – ice interface are depicted in Figure 2.12.
Transverse shear stresses of up to 10 MPa are predicted at the 1.2 mm thick ice layer interface.

Figure 2.10: Steel Plate, PZT Disk, Ice Layer FEM Model

Figure 2.11: Triple Disk Actuator Predicted Impedance
2.3 PZT Thermal Estimates

Elastic, dielectric and piezoelectric losses degrade the performance of the PZT actuators [54, 61] (see Appendix A). Electrical energy supplied to the PZT actuators to drive the host structure is not only transformed into mechanical motion (dictated by the coupling factors of the material) but also into internal heat that is transferred to the host structure [55]. Damping in the actuator and dielectric losses create distributed heat generation in the PZT material and the host structure. Temperature increases in the system facilitate ice melting and generate additional thermal stresses. The following heat generation estimates are performed to calculate the temperature increase of the PZT
material. The theoretical tool guides the selection of a de-icing actuator that does not generate melting due to temperature changes in the PZT.

A one-dimensional, stationary, heat conduction model is assumed. Three dimensional and transient effects are ignored. This heat conduction model is described in references 52, 53, 54, and followed in this thesis. The symbols used during the derivation of this model are presented in Table 2-3. The PZT actuator and the host structure are assumed to be perfectly bonded to one another.

The governing differential equation of the temperature distribution in the PZT actuator is defined by Equation 2.6, where the subscript $p$ refers to the piezoelectric material, and $\alpha$ (W/m K) is the thermal conductivity of the material.

$$\frac{d^2 T}{d^2 z} + \frac{Q_p}{\alpha_p} = 0$$

(2.6)

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$H$</td>
<td>Ratio of heat flow to surface area</td>
</tr>
<tr>
<td>$P_p$</td>
<td>Dissipative Power Consumption of the PZT (Watts)</td>
</tr>
<tr>
<td>$Q$</td>
<td>Heat generation per unit volume (W/m$^3$)</td>
</tr>
<tr>
<td>$q$</td>
<td>Heat flow</td>
</tr>
<tr>
<td>$s$</td>
<td>Surface area (m$^2$)</td>
</tr>
<tr>
<td>$T$</td>
<td>Temperature ($^\circ$ C)</td>
</tr>
<tr>
<td>$t$</td>
<td>Thickness (m)</td>
</tr>
<tr>
<td>$T_0$</td>
<td>Bottom Surface Host Structure Temperature ($^\circ$ C)</td>
</tr>
<tr>
<td>$T_I$</td>
<td>Interface Temperature ($^\circ$ C)</td>
</tr>
<tr>
<td>$T_2$</td>
<td>PZT Surface Temperature ($^\circ$ C)</td>
</tr>
<tr>
<td>$t_p$</td>
<td>Thickness of the PZT (m)</td>
</tr>
<tr>
<td>$V$</td>
<td>Actuator Volume (m$^3$)</td>
</tr>
<tr>
<td>$Y$</td>
<td>Admittance (1/Ohms)</td>
</tr>
<tr>
<td>$\alpha$</td>
<td>Thermal conductivity (W/m K)</td>
</tr>
</tbody>
</table>
The temperature of the top surface of the PZT element is $T_2$ and corresponds to the freezing environment temperature (Figure 2.13). The heat generation rate per unit volume, $Q$ (W/m$^3$), is assumed to be uniform in the PZT material. The bottom of the plate, $z = 0$, is assumed to be insulated, making the change in the temperature gradient zero.

![Figure 2.13: Heat Conduction Model of a PZT Segment Driving Isotropic Plate](Zhou et al. Ref. 55)

The boundary conditions of the system are:

1) $\frac{dT}{dz} = 0$

2) $T = T_1$ at $z = z_1$

3) $T_2 = -10^0 \text{C}$ to $-20^0 \text{C}$ at $z = z_2$ (depending on freezer temperature environment)

Solving for the temperature, $T$, from Equation 2.6, the PZT segment temperature distribution is defined by [55]:

$$T = T_1 + (T_2 - T_1) \left( \frac{z - z_1}{z_2 - z_1} + \frac{Q_p (z_2 - z_1)}{2\alpha_p} \left[ \frac{z - z_1}{z_2 - z_1} - \left( \frac{z - z_1}{z_2 - z_1} \right)^2 \right] \right) \quad (2.7)$$
The heat generation rate per unit volume in the PZT actuator, $Q_p$, is

$$Q_p = \frac{\text{Power}}{\text{Volume}} = \frac{P_p}{V} \quad (2.8)$$

where the dissipative power consumption of the material, is [55]

$$P_p = \frac{V^2 \text{Re}(Y)}{2} \quad (2.9)$$

$V$ is the applied voltage and $Y$ is the admittance of the system.

To solve for the temperature at the PZT–host structure interface, $T_1$, additional boundary conditions are required. Further boundary conditions are identified by defining $H$ as the ratio of the rate of heat flow, $q$, to the surface area of the structure, $s$ (Equation 2.10). The heat flow, $q$, is defined by Equation 2.11.

$$H = \frac{Q}{s} \quad (2.10)$$

$$q = -\alpha \cdot s \frac{dT}{dz} \quad (2.11)$$

The ratio of the rate of heat flow, $q$, to the surface area of the structure, $s$ at the PZT- host structure interface is:

1) $H_p = H$ at $z = z_I$

where The same governing equation described in Equation 2.6 applies, with boundary conditions:

2) $T = T_i$ at $z = z_I$

3) $\frac{dT}{dz} = 0$ at $z = z_0$
The solution to the governing equation provides the value of $H$ at $z = z_f$ and $z = 0$:

$$H_{z_1} = (T_0 - T_i) \frac{\alpha}{z_1} + \frac{z_1}{2} \frac{Q}{z_1} \quad (2.12)$$

$$H_0 = (T_0 - T_i) \frac{\alpha}{z_1} - \frac{z_1}{2} \frac{Q}{z_1} \quad (2.13)$$

Substituting Equation 2.7 into Equation 2.11, $H_p$ of the PZT segment is obtained to be at $z_f$:

$$\left( H_p \right)_{z_f} = (T_0 - T_2) \frac{\alpha_p}{z_2 - z_1} + \frac{(z_2 - z_1)}{2} \frac{Q_p}{z_2 - z_1} \quad (2.14)$$

Equating the ratio of the rate of heat flow from the plate structure and the piezoelectric material (Equation 2.14 with Equation 2.12) the temperature at the PZT–host structure interface is obtained:

$$T_1 = \left( \frac{z_2 - z_1}{2} \frac{Q_p}{z_2 - z_1} + \frac{z_1}{2} \frac{Q}{z_2 - z_1} + \frac{T_0 \alpha}{z_1} + \frac{T_2 \alpha_p}{z_2 - z_1} \right) / \left( \frac{\alpha}{z_1} + \frac{\alpha_p}{z_2 - z_1} \right) \quad (2.15)$$

$T_{\text{max}}$ is the maximum temperature in the PZT material, is derived from Equation 2.7:

$$T_{\text{max}} = T_1 + \left[ \frac{1}{2} \frac{\alpha_p \frac{T_2 - T_1}{Q_p (z_2 - z_1)^2}}{2} + \frac{1}{2} \frac{(T_2 - T_1) + \frac{Q_p (z_2 - z_1)^2}{\alpha_p}}{4} \right] \quad (2.16)$$
2.4 Dispersion Curve Modeling

The formulation of the analytical shear dispersion curves of a metallic host structure covered with an accreted ice layer is described. The model provides interface transverse shear stress concentration coefficients (ISCC) between the accreted ice layer and the isotropic host structure. The ISCC concept was first presented by Huidong Gao, at the Pennsylvania State University [77]. These stress concentrations guide the selection of ultrasonic wave de-icing modes.

Ultrasonic wave modes have the potential to increase the performance of the ultrasonic de-icing system. An optimized actuator could be obtained by matching the actuator resonance frequency with an ultrasonic shear wave mode of the structure, which is comprised of an isotropic host structure with an accreted ice layer. The triggered wave mode provides wave structures with maximum ice interface transverse shear stresses. To minimize required power, the actuation frequency of the wave mode, \( f \), should coincide with that of the actuator resonance, \( f_s \).

2.4.1 Theoretical Model of Ultrasonic Shear Horizontal Waves

The following mathematical model was used to predict shear horizontal waves (SHW) in a multilayer system (metal-ice layers). The theoretical approach and applications to non-destructive evaluation techniques is described in detail by Rose [47]. From this model, the Interface Shear Concentration Coefficient (ISCC) can be calculated. The ISCC quantity was developed by Huidong Gao at the Pennsylvania State University, and publication of the theoretical approach is pending. The ISCC value is a quantity;
independent of actuator characteristics and therefore, finite element analysis of the actuator and actuator–structure system has to be performed to obtain a physical interface stress quantity generated by a traveling wave.

The layer system, formed by a thin layer of ice and a metallic skin surface under the effects of shear horizontal waves, can be modeled using the governing equation for waves in solid media [47], defined by Equation 2.17, where \( \rho \) is the density of the metal, \( C_{ijkl} \) is the stiffness matrix, and \( u_i \) is the displacement field.

\[
\rho \frac{\partial^2 u_i}{\partial t^2} = C_{ijkl} \frac{\partial^2 u_i}{\partial x_j \partial x_k}\tag{2.17}
\]

\( C_{ijkl} \) can be populated with the elements of the two dimensional stiffness tensor \( C_{nm} \) as follows:

- if \( i = j \) \( \rightarrow n = i \);
- if \( i \neq j \) \( \rightarrow n = 9 - (i+j) \)
- if \( k = l \) \( \rightarrow m = k \);
- if \( k \neq 1 \) \( \rightarrow m = 9 - (k+l) \)

Example: \( \begin{cases} C_{111} = C_{11} \\ C_{1321} = C_{56} \end{cases} \) as so on

For an isotropic material, \( C_{nm} \) is defined by Equation 2.18.

A schematic of the isotropic host plate with an accreted ice layer is depicted in Figure 2.14.
\[ C_{mn} = \begin{bmatrix} \lambda + 2\mu & \lambda & \lambda & 0 & 0 & 0 \\ \lambda & \lambda + 2\mu & \lambda & 0 & 0 & 0 \\ \lambda & \lambda & \lambda + 2\mu & 0 & 0 & 0 \\ 0 & 0 & 0 & \mu & 0 & 0 \\ 0 & 0 & 0 & 0 & \mu & 0 \\ 0 & 0 & 0 & 0 & 0 & \mu \end{bmatrix} \]

where

\[ \lambda_i = \frac{\nu E}{(1 + \nu)(1 - 2\nu)} \]  
(2.19)

\[ \mu = \frac{E}{2(1 + \nu)} \]  
(2.20)

\[ \nu = \frac{\lambda}{2(\lambda + \mu)} \]  
(2.21)

and where \( \lambda_i \) and \( \mu \) are the Lame Constants and \( \nu \) is the Poisson’s Ratio of the materials.
The following general trial solution is assumed:

\[ u_i = U_i e^{ik(x_1 + \alpha x_3 - ct)} \]  \hspace{1cm} (2.22)

where \( U_i \) is the polarization vector that represents the displacement vector in each direction, \( k \) is the wave number along the \( x_i \) direction, \( c \) is the phase velocity along \( x_i \) direction. A graphical schematic of the definitions of the wave number and the phase velocity are presented in Figure 2.15, Figure 2.16, and \( \alpha \) is the ratio of wave number in \( x_3 \) direction with respect to wave number in the \( x_1 \) direction. The wave is assumed to be propagating along the \( x_1-x_3 \) plane, independently from the \( x_2 \) direction. The longitudinal waves propagated in the \( x_2 \) direction are not considered in this model, since only shear horizontal waves generating ZX transverse shear stresses between the two layers are of interest.

The wave number, \( k \), is defined by Equation 2.23, where \( \omega \) is the applied frequency:

\[ k = \frac{\omega}{c} \]  \hspace{1cm} (2.23)

Solving the governing equation (Equation 2.17) with the assumed solution, Equation 2.22, Christoffel’s equation is obtained:

\[
\begin{bmatrix}
(\lambda + 2\mu) + \mu \alpha^2 - \rho \omega^2 & 0 & (\lambda + \mu)\alpha \\
0 & \mu(1 + \alpha^2) - \rho \omega^2 & 0 \\
(\lambda + \mu)\alpha & 0 & (\lambda + 2\mu)\alpha^2 + \mu - \rho \omega^2
\end{bmatrix}
\begin{bmatrix}
U_1 \\
U_2 \\
U_3
\end{bmatrix}
=
\begin{bmatrix}
0 \\
0 \\
0
\end{bmatrix}
\]  \hspace{1cm} (2.24)

where the subscript \( p \) refers to the layer number. Different Christoffel’s equations \([47]\) are generated for each different layer.
In the case of having shear horizontal waves, the polarization vector, \( U_2 \), is set to be unity in order to define the wave oscillation along \( x_2 \) direction. From Equation 2.24, the characteristic equation becomes:

\[
\mu(1 + \alpha^2) - \rho c^2 = 0
\]  

(2.25)
Solving for the ratio of wave number in $x_3$ direction with respect to wave number in $x_1$ direction, $\alpha$, Equation 2.26 is obtained, which relates $\alpha$ and the phase velocity (Figure 2.15). Since multiple Christoffel’s equations are generated (one for each layer), two eigenvalues result for each layer.

$$\alpha^2 \bigg|_{l,2} = \frac{\rho c^2}{\mu} - 1$$  \hspace{1cm} (2.26)

The displacement field solution to the wave governing equation becomes:

$$u_2 = \sum_{j=1}^{2} \nu_j e^{ik(x_1 + \alpha x_3 - ct)}$$  \hspace{1cm} (2.27)

$$\sigma_{32} = 2\mu \frac{\partial u_2}{\partial x_3} = 2\sum_{j=1}^{2} \nu_j \alpha_j \mu ik e^{ik(x_1 + \alpha x_3 - ct)}$$  \hspace{1cm} (2.28)

where $\nu_j$ are the weighting coefficients of the partial waves. In order to solve for the weighting coefficient vector, the equations describing the boundary conditions of the layer were assembled using the global matrix method [47]. For shear horizontal waves on a two layered system, four boundary conditions are required. The imposed boundary conditions account for free tractions at the surface of the top layer ($x_3 = 0$) and bottom layer ($x_3 = h_2$). Continuity of the displacement and the stress is forced in the interface between layers.

A global matrix system with all the boundary conditions is created by letting $j = 1, 2$ refer to the partial wave in the top layer, and $j = 3, 4$ refer to the partial waves in the second layer. The final eigenvalue problem is expressed in Equation 2.29, where $D$ is the global matrix containing all the boundary condition equations.
The boundary conditions for a system of two layers without a substrate become:

1) Free stress at the top layer surface:

\[ \alpha_1 \mu_1 (ik)e^{ik(x_1-x)}v_1 + \alpha_2 \mu_1 (ik)e^{ik(x_1-x)}v_2 = 0 \]  \hspace{1cm} (2.30)

2) Continuity in stress and displacement in the interface between layers:

- Displacement continuity:

\[ e^{ik(x_1+\alpha_1 h_1-x)}v_1 + e^{ik(x_1+\alpha_2 h_2-x)}v_2 = e^{ik(x_1+\alpha_1 0-x)}v_3 + e^{ik(x_1+\alpha_2 0-x)}v_4 \]  \hspace{1cm} (2.31)

- Stress continuity:

\[ \alpha_1 \mu_1 (ik)e^{ik(x_1+\alpha_1 h_1-x)}v_1 + \alpha_2 \mu_1 (ik)e^{ik(x_1+\alpha_2 h_2-x)}v_2 = \\
= \alpha_3 \mu_2 (ik)e^{ik(x_1+\alpha_3 h_1-x)}v_3 + \alpha_4 \mu_2 (ik)e^{ik(x_1+\alpha_4 h_2-x)}v_4 \]  \hspace{1cm} (2.32)

3) Free stress at the bottom layer surface:

\[ \alpha_3 \mu_2 (ik)e^{ik(x_1+\alpha_1 h_2-x)}v_3 + \alpha_4 \mu_2 (ik)e^{ik(x_1+\alpha_1 h_2-x)}v_4 = 0 \]  \hspace{1cm} (2.33)

By assembling and simplifying Equations 2.30, 2.31, 2.32, and 2.33 into a matrix, the following matrix is obtained:

\[ D_m v_j = 0 \quad m = 1...4; \quad j = 1...4 \]  \hspace{1cm} (2.29)

To obtain a non-trivial eigenvector solution for the weighting coefficients of the partial waves, the determinant of the \( D \) matrix should disappear. A relationship for the dispersion relations of the structure can be derived. This process could be thought of as a 2 dimensional eigenvalue problem similar to the one performed in standard structural
dynamics. In this case, not only the frequency of the vibration provides information about the systems response, but also the wave package velocity of propagation (phase velocity). The set of frequency and phase velocity values that provide a solution to the governing equation and boundary conditions of the system are known as “dispersion curves.” The dispersion relation could be represented in several spaces, such as \((k, c)\), \((\omega, c)\), or \((\omega, k)\) by the definition of the wave number, \(k\), defined in Equation 2.23. From the derivation above, the most direct representation is \((k, c)\). The most easily applicable presentation in an experiment is \((\omega, c)\). A sample of dispersion curves for a 1 mm thick steel plate with a 2 mm thick ice layer is presented in Figure 2.17.

The solutions of the weighting coefficient of the partial wave, \(\nu\), can be obtained by inserting the mode value of \(k\) and \(c\) into the equation and solving for the eigenvector of the matrix. The displacement and stress wave structure for a given mode can be calculated. When an “imperfect singularity” occurs for the \(D\) matrix, the least square method can be used to obtain the solutions of the vector \(\nu\). When the given characteristic

![Figure 2.17: Example of Dispersion Curves for a 1 mm Steel Plate with a 2 mm Accreted Ice Layer](image_url)
pair is not the exact value to make the $D$ matrix singular, the pair is said to be an imperfect singularity. The least square error method [73] can be used to find the approximate eigenvector with minimum error.

If we assume $\nu_1 = 1$, Equation 2.29 can be rearranged as shown in Equation 2.35:

$$D_{\text{rest}} \nu_{\text{rest}} = -D_1$$

(2.35)

where $D_1$ is the first column of the $D$ matrix, $D_{\text{rest}}$ is the remaining columns of the matrix, and $\nu_{\text{rest}}$ is the rest of vector $\nu$ other than $\nu_1$. The resulting Equation 2.35 is an over-determined equation system with the row number greater than the column number by one. Least square solution of $\nu_{\text{rest}}$ (Equation 2.36) can be obtained by solving this equation with the pseudo-inverse technique, defined by Equation 2.37 [73].

$$\nu_{\text{rest}} = D_{\text{rest}}^+ D_1$$

(2.36)

$$D_{\text{rest}}^+ = (D^T D)^{-1} D^T$$

(2.37)

By substituting the values of $\nu$ into Equation 2.27 and Equation 2.28, the displacement and stress profiles along the thickness of the structure, also known as wave structure plots, can be obtained. The displacement and shear stress wave structure plots for the $SH_1$, $SH_2$, and $SH_3$ modes are presented in Figures 2.18, 2.19, and 2.20.

A decrease in accreted ice thickness make shear ultrasonic modes in the dispersion curves to shift towards higher frequencies, as it is illustrated in Figure 2.21.
Figure 2.18: Wave Structure $SH_1$ Mode: 0.3 MHz, 8 Km/sec

Figure 2.19: Wave Structure $SH_2$ Mode: 0.8 MHz, 8 Km/sec

Figure 2.20: Wave Structure $SH_3$ Mode: 1.25 MHz, 8 Km/sec
2.4.2 Interface Shear Stress Concentration Coefficient Calculation

Gao et al. [77] introduced a theoretical approach to calculate the interface shear stress concentration coefficient (ISCC) of ultrasonic wave modes. The calculation evaluates the ability of wave modes to direct transverse shear stress into a layer interface with a given amount of energy supply. The stress fields and displacement field are normalized with respect to the total amount of power transmitted through a unit width of the structure. The complex acoustic Poynting’s vector, $\vec{P}$, also called the power flow, is given by Equation 2.38. Poynting’s Theorem states that the power that leaves a region is
equal to the temporal decay in the energy that is stored within the volume minus the power that is dissipated as heat within [48]. Poynting’s vector defines the power flow on the structure:

$$\vec{P} = -\vec{v}^* \cdot \sigma$$  \hspace{1cm} (2.38)

$\vec{v}$ is particle velocity, $^*$ is the complex conjugate, and $\sigma$ is the stress tensor. For the shear horizontal waves, $\vec{v}$ and $\sigma$ are defined by:

$$\vec{v} = \begin{bmatrix} 0 & \frac{\partial u_2}{\partial t} & 0 \end{bmatrix}$$  \hspace{1cm} (2.39)

When substituting Equations 2.27, 2.39, and 2.40 into Equation 2.38, Poynting’s vector is obtained (Equation 2.41).

$$\sigma = \begin{bmatrix} 0 & \mu \frac{\partial u_2}{\partial x_1} & 0 \\ \mu \frac{\partial u_2}{\partial x_1} & 0 & \mu \frac{\partial u_2}{\partial x_3} \\ 0 & \mu \frac{\partial u_2}{\partial x_3} & 0 \end{bmatrix}$$  \hspace{1cm} (2.40)

$$\vec{P} = P_{x_1} \cdot \hat{x}_1 + P_{x_3} \cdot \hat{x}_3$$  \hspace{1cm} (2.41)

where

$$P_{x_1} = -\frac{1}{2} \left( \frac{\partial u_2}{\partial t} \right)^* \mu \left( \frac{\partial u_2}{\partial x_1} \right) = \frac{1}{2} k \omega \mu \left\| \sum_{j=1}^{2} v_j e^{i \alpha_2 x_3} \right\|^2$$  \hspace{1cm} (2.42)

$$P_{x_3} = -\frac{1}{2} \left( \frac{\partial u_2}{\partial t} \right)^* \mu \left( \frac{\partial u_2}{\partial x_3} \right) = \frac{1}{2} k \omega \mu \left[ \sum_{j=1}^{2} \left( v_j e^{i \alpha_2 x_3} \right)^* \right] \left[ \sum_{j=1}^{2} \left( v_j e^{i \alpha_2 x_3} \right) \alpha_j \right]$$  \hspace{1cm} (2.43)
The power flow can be integrated along the whole thickness of the structure to determine the power transmitted with the ultrasonic wave through a cross section perpendicular to the $x_1$ direction with unit width (Equation 2.44).

$$\text{Power} = \int_{\text{Thickness}} \tilde{P} \cdot \hat{x}_3 dx_3 = \int_{\text{Thickness}} P_{31} dx_3$$  \hspace{1cm} (2.44)

Normalizing the interface stress with respect to this total average power through the cross section of a wave beam of unit width, provides the ISCC, defined by Equation 2.45.

$$\text{ISCC} = \frac{\sigma_{23}}{\sqrt{\text{Power}}} \bigg|_{\text{Layer/Interface}}$$  \hspace{1cm} (2.45)

The Interface Shear Stress Concentration Coefficient (ISCC) is a measurement of how much modal shear stress can be produced at the interface between the two layers for a given produced power per meter. When the interface transverse shear stress, $\sigma_{23}$ or $\sigma_{2x}$, exceeds the adhesive shear strength of ice to a metal surface, the de-icing system will be effective. The larger the ISCC, the less wave power is required to generate a given shear stress. The ZX shear stress produced is dependent on the actuator characteristics. Large ISCC points on the dispersion curves do not guarantee large physical stresses at the ice interface, but present large wave structure modal stress at the ice interface. A minimization in the required actuation power could be obtained by combining a point on the dispersion curves that provides a large ISCC value for the structure, and an actuator resonating at the same frequency (provided the right phase velocity is used to trigger the selected ultrasonic mode). The actuation frequency, $f$, is controlled by the AC input signal to the piezoelectric actuator, while the phase velocity, $c$, can be controlled by the
wavelength provided by the actuator physical dimensions and/or arrangement (Equation 2.46). As an example, the phase velocity versus frequency line for a wavelength of 7.8 mm is depicted in Figure 2.22.

\[ c = \lambda f \]  

(2.46)

![High ISCC Point](image)

**Figure 2.22: ISCC Example: 1 mm Steel Plate with a 2 mm Thick Ice Layer**

### 2.4.3 Lamb Wave ISCC Calculations

Similarly to shear horizontal waves, the ISCC generated by Lamb waves can be calculated. Lamb waves are created by ultrasonic vibration on the \( x_3 \) (perpendicular motion to the surface of the structure). The displacement field solution for Lamb Waves
to the wave governing equation (Equation 2.47) is defined by Equation 2.48. The transverse shear stress component of the wave is defined by Equation 2.49.

\[
\rho \frac{\partial^2 u_i}{\partial t^2} = C_{ijkl} \frac{\partial^2 u_l}{\partial x_j \partial x_k} \tag{2.47}
\]

\[
u_3 = \sum_{j=1}^{2} \nu_j e^{ik(x_j+ux_j-ct)} \tag{2.48}
\]

\[
\sigma_{31} = \mu \frac{\partial u_3}{\partial x_1} \tag{2.49}
\]

Poynting’s vector defines the power flow on the structure:

\[
\vec{P} = -\vec{v}^* \cdot \sigma \tag{2.50}
\]

\(\vec{v}\) is particle velocity, * is the complex conjugate, and \(\sigma\) is the stress tensor. For the Lamb waves, \(\vec{v}\) and \(\sigma\) are defined by:

\[
\vec{v} = \begin{bmatrix} 0 & 0 & \frac{\partial u_3}{\partial t} \end{bmatrix} \tag{2.51}
\]

When substituting Equation 2.50, and 2.51 into Equation 2.49, Poynting’s vector is obtained (Equation 2.52).

\[
\sigma = \begin{bmatrix} \lambda \frac{\partial u_3}{\partial x_3} & 0 & \mu \frac{\partial u_3}{\partial x_1} \\ 0 & \lambda \frac{\partial u_3}{\partial x_3} & 0 \\ \mu \frac{\partial u_3}{\partial x_1} & 0 & (\lambda + 2\mu) \frac{\partial u_3}{\partial x_3} \end{bmatrix} \tag{2.52}
\]
\[ \vec{P} = P_{x1} \cdot \hat{x}_1 + P_{x3} \cdot \hat{x}_3 \]  
(2.53)

where

\[ P_{x1} = -\frac{1}{2} \left( \frac{\partial u_3}{\partial t} \right)^* \left( \lambda + 2\mu \left( \frac{\partial u_3}{\partial x_3} \right) \right) \]  
(2.54)

The power flow can be integrated along the whole thickness of the structure to determine the power transmitted with the ultrasonic wave through a cross section perpendicular to the \( x_1 \) direction with unit width (Equation 2.54).

\[ \text{Power} = \int_{\text{Thickness}} \vec{P} \cdot \hat{x}_1 \, dx_3 = \int_{\text{Thickness}} P_{x1} \, dx_3 \]  
(2.55)

Normalizing the interface stress with respect to this total average power through the cross section of a wave beam of unit width, provides the interface shear stress concentration coefficient provided by Lamb Waves, ISCC\(_L\), defined by Equation 2.55.

\[ ISCC_L = \frac{\sigma_{13}}{\sqrt{\text{Power}}} \]  
(2.56)

2.5 Summary

In this chapter piezoelectric actuator characterization tools and ultrasonic wave mode theory were described. Finite element tools were used to model piezoelectric actuators, to calculate impedance of ultrasonic actuators to identify resonance frequencies, and to predict the shear stresses created by ultrasonic actuators on isotropic plates.
Theoretical thermal analysis for PZT materials driven at a given electric field and impedance was also described.

Shear horizontal wave propagation theory was implemented to predict wave modes and wave structures on isotropic plates with a given accreted ice layer. The ice interface transverse shear stresses concentration coefficients for wave modes of isotropic-ice layered structures were calculated, and those modes providing larger concentrations were identified.
Chapter 3

MODELING TOOLS VALIDATION

In order to validate the theoretical tools used in this work, experimental results were compared to predicted values. To validate the finite element model of PZT de-icing actuators, impedance tests were conducted and the results compared to predictions. Validation of the thermal analysis of bonded actuators to a host plate was also conducted. The experimental temperature of the PZT material bonded to a steel host structure was compared to predicted temperatures at a given frequency and input voltage. Lastly, to validate the wave propagation model, theoretical dispersion curves were contrasted to experimental results obtained using electromagnetic transducers (EMATS).

3.1 FEM Model Validation

The characterization of ultrasonic devices is typically done by measuring electrical impedance versus applied frequency. To validate the finite element analysis performed on the actuators, experimental impedance results obtained for different proposed actuators is compared to finite element model predictions. An impedance analyzer measured the experimental impedance of the actuators.

To validate the proposed FEM approach, the experimental impedance of a PZT-4 disk actuator was compared to its predicted impedance, and the results are shown in Figure 3.1. These PZT disks were purchased from Piezo-Kinetics, Inc. For the first radial
mode resonance of the actuator, there is an error of less than 1% between experimental and predicted impedance, validating the analytical tools used to model the actuators.

The theoretical impedance of the disk actuator attached to a 0.711 mm thick, 30.48 x 30.48 cm (12 x 12 inch) steel plate was also compared to experimental results. A photograph of the actuator bonded to the steel plate is presented in Figure 3.2. Eccobond 286, a bonding layer commonly used in ultrasonic applications, was used to attach the actuators to the steel host structure. The epoxy properties are described in Table 3-1.

**Table 3-1: Eccobond 286 Material Properties After Application**

<table>
<thead>
<tr>
<th>Property</th>
<th>Test Method</th>
<th>Units</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flexural Strength</td>
<td>ASTM-D-790</td>
<td>MPa</td>
<td>83</td>
</tr>
<tr>
<td>Tensile Lap Shear Strength AL-AL @ 25°C</td>
<td>ASTM-D-1002</td>
<td>MPa</td>
<td>15.2</td>
</tr>
<tr>
<td>Thermal Conductivity</td>
<td>ASTM-D-2214</td>
<td>W/m.K</td>
<td>1.04</td>
</tr>
<tr>
<td>Coefficient of Thermal Expansion</td>
<td>ASTM-D-3386</td>
<td>10⁻⁵ /°C</td>
<td>36</td>
</tr>
<tr>
<td>Temperature Range</td>
<td>----</td>
<td>°C</td>
<td>-55 to 105</td>
</tr>
<tr>
<td>Volume Resistivity @25°C</td>
<td>ASTM-D-257</td>
<td>Ohm-cm</td>
<td>&gt;10¹⁵</td>
</tr>
</tbody>
</table>

The trend of the theoretical and experimental impedance agrees. The experimental frequency of the radial mode of the actuator matches the finite element predicted frequency with an error of less than 2%, as it is illustrated in Figure 3.3. The impedance peak magnitudes present a maximum error of 30% at the anti-resonance frequencies, \( f_a \), and a 3% discrepancy at the resonance peaks, \( f_s \).
Figure 3.1: Experimental and Predicted Impedance Curves of the 38.1 mm PZT-4 Disk

Figure 3.2: Photo and Schematic of the In-Plane Radial Disk (38.1 mm Radius) Bonded to a 30.48 x 30.48 cm Steel Plate
3.2 Thermal Analysis Validation

The interface temperature and the maximum actuator temperature of a PZT-4 disk bonded to a 0.711 mm thick steel plate was theoretically calculated and compared to experimental results. The environment temperature was maintained at -10°C. The temperature of the surface of the plate and disk was measured prior turning the actuator on. The recorded temperatures were used as boundary conditions for the thermal governing equation. Different input ultrasonic frequencies excited the 45.6 cm² disk actuator. The input voltage was 50 V (27.8 KV/m). The impedance at each different frequency was experimentally measured (Figure 3.3). From the impedance of the actuator and the input voltage, the power dissipation was calculated.
The PZT disk theoretical and experimental steady state temperatures are presented in Table 3-2. To ensure steady state temperatures, the actuator was driven for 10 minutes before measuring its surface temperature.

Table 3-2: Temperature of a 2.54 mm Thick PZT-4 Disk Actuator Attached to 0.711 mm Thick Steel Plate. Environment Temperature -10°C

<table>
<thead>
<tr>
<th>Frequency</th>
<th>Impedance</th>
<th>Max. Theo. Temp.</th>
<th>Exp. Temp.</th>
<th>% Error</th>
</tr>
</thead>
<tbody>
<tr>
<td>18 KHz</td>
<td>480 Ohms</td>
<td>-9.0°C</td>
<td>-9.1°C</td>
<td>1%</td>
</tr>
<tr>
<td>28.5 KHz</td>
<td>65 Ohms</td>
<td>-8.4°C</td>
<td>-3°C</td>
<td>65%</td>
</tr>
<tr>
<td>33 KHz</td>
<td>70 Ohms</td>
<td>-8.5°C</td>
<td>-6.8°C</td>
<td>20%</td>
</tr>
<tr>
<td>37 KHz</td>
<td>56 Ohms</td>
<td>-8.3°C</td>
<td>-6.9°C</td>
<td>17%</td>
</tr>
</tbody>
</table>

For those frequencies not matching the radial resonance frequency of the actuator, the theoretical model predicts the actuator temperature with a maximum error of 20%.

At the radial resonance frequency of the PZT disk (28.5 KHz), the mechanical strains in the PZT material increase. Internal friction in the PZT material and generates heat energy not captured in the theoretical model. Errors of up to 65% are recorded when the PZT disk is driven at its resonance frequency and with an input voltage of 50 V.

Despite the large errors between the predicted and experimental temperature at the radial frequency of the actuator, the disk remains at a negative temperature when 50 V are applied. The actuator rise in temperature does not contribute to ice interface melting.

3.3 Experimental Validation of Shear Horizontal Waves Dispersion Curves

Experimental shear dispersion curves for both a 9 mm thick steel plate and a 9 mm thick steel plate covered with a 3.5 mm thick ice layer were acquired. A shear
horizontal electromagnetic transducer (EMAT) generated traveling waves on a steel plate. An EMAT receiver recorded the waveform in a “wave through transmission” configuration (Figure 3.4).

The transducer, on the left in Figure 3.4, acted as the wave source. The right transducer received the signal at 64 locations as it was moved along the plate and away from the transmitter. This process was repeated multiple times for different frequencies in increments of 0.1 MHz over the range of 0.1 MHz to 1 MHz. The transducer produced a tone burst (a wave train consisting of several cycles of the same frequency) corresponding to the desired frequency. A MATEC computer recorded the waveform at each location. The experimental set-up and a data acquisition process flow-chart is depicted in Figure 3.5.

A time-space matrix of 64 by 16,384 was populated from the spatial information (64 spatial points due to the translation of the receiving transducer) and time information (16,384 time points corresponding to each waveform). A two-dimensional Fourier Transform analysis, Equation 3.1, is carried out to calculate the experimental frequency domain dispersion curves of the structure, \( H \). The experimental results were compared to the theoretical predictions in order to validate the theoretical approach.
In Equation 3.1, $k$ is the wave number, $f$ is actuation frequency, and $u$ is transverse displacement in the $x_2$ direction. The wave number, $k$, relationship to the phase velocity of the wave, $c$, is defined by Equation 3.2 and Equation 3.3, where $N$ is the number of waveforms recorded (64 locations).
Theoretical shear dispersion curves for a 9 mm thick steel plate, illustrated in Figure 3.6, were compared to obtained experimental dispersion curves, presented in Figure 3.7.

Both results agree within 4%, validating the analytical tool developed. In Figure 3.8, the location where the white line intersects the dispersion curves is the triggered mode at a given frequency. The slope of this line is proportional to the spacing between multiple shear elements inside the EMAT and dictates the wave phase velocity excitation capability of the transducer (Equation 3.3).

\[
\Delta k = \frac{\left( \frac{2\pi}{\Delta x} \right)}{N}
\]  

(3.2)

\[
c = \frac{2\pi f}{k}
\]  

(3.3)

Theoretical shear dispersion curves for a 9 mm thick steel plate, illustrated in Figure 3.6, were compared to obtained experimental dispersion curves, presented in Figure 3.7.

Both results agree within 4%, validating the analytical tool developed. In Figure 3.8, the location where the white line intersects the dispersion curves is the triggered mode at a given frequency. The slope of this line is proportional to the spacing between multiple shear elements inside the EMAT and dictates the wave phase velocity excitation capability of the transducer (Equation 3.3).

Figure 3.6: Theoretical Dispersion Curves for a 9 mm Thick Steel Plate
Similarly, experimental and theoretical shear dispersion curves for a 9 mm thick steel plate covered with a 3.5 mm thick layer of ice were compared. The theoretical wave modes are depicted in Figure 3.8, while the experimentally acquired ones are presented in Figure 3.9. In this case, theoretical and experimental non-dispersive modes agree within 8%. Non-dispersive modes are those wave modes with small changes in phase velocity with changing frequency. The ultrasonic energy is dissipated on the viscoelastic ice layer, avoiding energy leakage to excite dispersive modes. The EMAT receiver only captures triggered non-dispersive modes. Due to the dispersion of the wave energy on the accreted ice layer, the experimental results do not show high wave displacement at phase velocities and frequencies corresponding to dispersive modes. The larger discrepancy between theoretical prediction and experimental values are also attributed to the uneven thickness of the accreted ice.
Figure 3.8: Theoretical Shear Dispersion Curves for a 9 mm Thick Steel Plate Covered with a 3.5 mm Thick Ice Layer

Figure 3.9: Experimental and Theoretical Shear Dispersion Curves for a 9 mm Thick Steel Plate with a 3.5 mm Thick Accreted Ice Layer
The theoretical dispersion curves for the 9 mm thick steel plate and the same plate covered with a 3.5 mm ice layer are compared to each other in Figure 3.10.

### 3.4 Summary

Validation of finite element predictions of piezoelectric actuator systems was performed. The trend of impedance curves of piezoelectric actuator systems was predicted accurately. Experimental ultrasonic resonance frequencies of piezoelectric actuators match predicted values with errors of less than 3%.

Thermal analysis captured temperature rises on PZT material bonded to isotropic plates. Predicted temperatures agreed with experimental ones with a maximum error of 20% when driven at off resonance frequencies of the actuator. At resonance frequencies
of the actuator, errors of up to 65% were recorded. The discrepancies are attributed to internal friction not captured by the model.

Theoretical dispersion curves were also validated by comparing the predictions to experimentally obtained results. The theoretical trends of the ultrasonic modes matched experimental ones with errors of less than 8%. Interface shear stress concentration coefficient (ISCC) theory was experimentally demonstrated.
Chapter 4

ULTRASONIC ACTUATOR DESIGN

Off-the-shelf ultrasonic in-plane displacement actuators able to generate transverse shear stresses large enough to de-bond accreted ice layers were not available. This chapter describes a methodology for the design of resonating piezoelectric actuators matching de-icing ultrasonic wave modes. The design goal is guided by theoretical dispersion curves of the structure. The goal of the design is to provide ultrasonic actuators triggering wave modes with high interface shear stress for the thinnest ice layer possible.

Guided by finite element results, a low-power ultrasonic de-icing actuators able to instantaneously de-bond thin ice layers is selected.

4.1 Ultrasonic Wave Mode Actuators

To trigger a desired ultrasonic mode on the dispersion curves of a structure, the wave phase velocity generated by the actuator, $c$, and its driving frequency, $f$, have to be selected. In order to activate a selected ultrasonic mode with the maximum available wave amplitude, the resonance of the actuator, $f_s$, must coincide with the wave mode frequency, $f$. Several configurations to impart pure shear horizontal waves (SHW) were studied, and are described in the following sections. Shear segments ($d_{15}$ pure shear), despite providing larger shear strains than other configurations, were not suitable to
trigger the phase velocity of wave modes providing large ice interface shear stresses. An arrangement of bar elements was chosen to provide the desired ultrasonic wave modes with potential de-icing qualities. The ultrasonic wave phase velocity of propagation, $c$, can be tuned by using several bar actuators. The spacing between the actuators controls the wavelength, $\lambda$, which provides the desired phase velocity, $c$, at the operating frequency, $f$. Two bar element actuators (202 KHz, and 500 KHz) exciting two wave modes with high ISCC respectively were tested.

### 4.1.1 SHW from a Patch formed by $d_{15}$ Shear Segments

Shear segments poled along their longitudinal direction have a limiting phase velocity at the resonance frequency of the segment equal to twice the shear frequency constant of the material (Equation 4.1). The limiting phase velocity that can be triggered by a given PZT shear segment lies between 2 and 3 km/sec, depending on the piezoelectric material. The ideal phase velocity for de-icing using shear horizontal waves lies at higher phase velocities ($c >5$ Km/sec). Therefore, shear segments are not suitable piezoelectric configurations to trigger high interface shear stress ultrasonic modes.

As is described in Equation 4.1, the width of the shear patch, $w$, dictates the resonance frequency of the actuator as well as the phase velocity of the propagating ultrasonic wave, which in this configuration is twice the width of the shear patch. The estimated resonance frequency of four different segment widths is presented in Table 4-1. The presented resonance frequencies were calculated from the shear frequency constant
of the material (See Appendix A). The phase velocity vs. frequency lines for four different widths of PZT-5 are presented in Figure 4.1.

The phase velocity is equal to twice the shear frequency constant of the material, a value smaller than the range where ultrasonic modes with possible de-icing properties occur, as presented in Figure 4.3. The \( d_{15} \) shear patch configuration does not provide a structural way to simultaneously control the wave phase velocity and frequency for ultrasonic modes.

\[
C_p = \lambda f = 2w \frac{f_c}{w} = 2f_c
\]

(4.1)

![Figure 4.1: Phase Velocity vs. Frequency for Four PZT-5 Shear Actuator Widths](image)
4.1.2 SHW from Bar Piezoelectric Segments

Configurations of shear segments did not provide a way to control phase velocity and frequency with structural positioning of the actuators. In this section, piezoelectric bar elements are introduced as candidates to generate shear horizontal waves on a plate. Bar element geometrical positioning can be used to control the wave phase velocity produced by the actuators. The resonance frequency of the actuator is dependent on the length of the bar elements (see Appendix A); the resonance frequency is independent of the positioning of the elements in the plate, which controls the phase velocity of the actuator.

The objective of the design process is to select an actuator configuration in which the actuator low impedance frequency point, $f_s$, matches that frequency of a suitable de-icing ultrasonic mode. A plate thickness, material, and ice thickness have to be selected. A 0.711 mm thick steel plate, representative of a leading edge protection cap, is selected as a constant in the design process. The design variable is the thickness of an accreted ice layer, which should be minimized.

<table>
<thead>
<tr>
<th>Width (mm)</th>
<th>PZT 5</th>
<th>PZT 4</th>
<th>PZT 8</th>
</tr>
</thead>
<tbody>
<tr>
<td>7.62</td>
<td>158.0</td>
<td>173.8</td>
<td>191.6</td>
</tr>
<tr>
<td>3.175</td>
<td>379.2</td>
<td>417.3</td>
<td>459.8</td>
</tr>
<tr>
<td>1.905</td>
<td>632.0</td>
<td>695.5</td>
<td>766.4</td>
</tr>
<tr>
<td>1.27</td>
<td>948.0</td>
<td>1,043.3</td>
<td>1,149.6</td>
</tr>
</tbody>
</table>
A second constant in the design process is the minimum thickness of a bar element. The minimum thickness that can be manufactured in house is 4 mm. A 4 mm thick PZT-4 bar would have a resonance frequency of 512 KHz, which is the upper frequency constraint of the design process. From the plate thickness and the upper frequency constraint constants, a minimum ice for the design can be determined.

Thinner ice thicknesses make shear ultrasonic modes in the dispersion curves to shift towards higher frequencies, as it is illustrated in Figure 4.2. Triggering higher frequency modes would require higher actuator resonance frequencies, which at the same time would require smaller length, \( L \), bar elements, since the length of the bar elements dictates their resonance frequency. Higher frequencies provide smaller stresses, at the same time that smaller elements are harder to fabricate.

![Figure 4.2: Theoretical Dispersion Curves for a 0.711 mm Steel Plate with 3 Different Ice Layers (1, 3 and 5 mm Thick)](image-url)
The minimum ice thickness to generate a high ISCC wave mode of a 0.711 mm thick still plate with frequency less than 512 KHz is 2.54 mm, which is \(\frac{1}{3}\) that of the maximum allowed ice thickness encountered in helicopter blades before electrothermal de-icing systems are turned on. The wave modes with superimposed ISCC values for the selected structure (steel plate – ice layer) are presented in Figure 4.3. The \(SH_1\) and the \(SH_2\) modes are the selected modes to be triggered at a phase velocity of 8 Km/sec. These two modes present the higher ISCC values under the design constraint frequency of 512 KHz.

![Figure 4.3: Dispersion Curves for a 0.711 mm Thick Steel Plate Covered with a 2.54 mm Ice Layer. The Two Points Depicted on the Curves Mark the \(c\) and \(f\) Used to Design Two Different Shear Actuators](image)

To effectively control phase velocity and resonance frequency, piezoelectric segments poled along a given direction and with an electric field applied in the same direction are presented in Figure 4.4. The resonance frequency of these bar elements can be estimated from the longitudinal frequency constant, \(N_3\), of the material (2,050 Hz-m
for PZT-4), and predicted using finite element techniques. The desired phase velocity of propagating shear waves, \( c \), can be controlled by the spacing of the elements as defined by Equation 4.2. The wavelength, \( \lambda \), coincides with the spacing of bar element driven in phase at a given frequency, \( f \), and phase velocity, \( c \).

\[
c = \lambda f
\]  

(4.2)

A set of bar elements can be arranged to control the phase velocity of the shear horizontal waves propagating in the \( x_2 \) direction, as illustrated in Figure 4.5. The separation between elements in the \( x_2 \) direction dictates the wavelength of the shear horizontal wave. The separation between elements in the \( x_1 \) direction, \( d \), is chosen so that the longitudinal propagating waves in that direction constructively interfere. The longitudinal wave propagation velocity of the isotropic plate, \( c_{\text{plate}} \) is defined in Equation 4.3. The frequency of vibration of the bar elements, \( f_s \), and the \( c_{\text{plate}} \) provide information about the wavelength in the \( x_1 \) direction, \( d \) (Equation 4.3). To ensure constructive interference, the bar elements should be placed at a multiple distance of the generated wavelength of the wave.

\[
c_{\text{plate}} = \sqrt{\frac{E}{\rho(1-v^2)}}
\]  

(4.3)

\[
c_{\text{plate}} = 2df
\]  

(4.4)
Two actuators, one triggering the first shear horizontal mode, $SH_1$, at 202 KHz and 8 Km/sec, and a second triggering the second shear horizontal mode, $SH_2$, at 505 KHz and 8 Km/sec, were designed and fabricated. The actuator system is designed so that the resonance of the actuator bar elements matches the desired triggered frequency of the shear horizontal mode. The phase velocity of the wave is controlled by placing the
actuator bar elements at a distance $\lambda$ from each other. This distance is proportional to the phase velocity to be triggered (8 Km/sec) and to the resonance frequency of the actuator.

The dimensions, as well as the resonance of the PZT-4 bar elements used for the 202 KHz actuator system are presented in Table 4-2. The experimental and theoretical impedance of a single bar element is shown in Figure 4.6.

**Table 4-2: Test Data 12.7 x 12.7 x 10 mm PZT-4 Bar Elements**

<table>
<thead>
<tr>
<th>Length (mm, in.)</th>
<th>Width (in.)</th>
<th>Thickness (mm, in.)</th>
<th>$f_s$ (KHz)</th>
<th>$Z_m$ (Ohms)</th>
<th>$d_{33} \times 10^{-12}$ (m/V)</th>
</tr>
</thead>
<tbody>
<tr>
<td>12.7, 0.5</td>
<td>12.7, 0.5</td>
<td>10, 0.390</td>
<td>202.5</td>
<td>211.4</td>
<td>300</td>
</tr>
</tbody>
</table>

**Figure 4.6: Bar Element Experimental vs. Theoretical Impedance (202 KHz Actuator)**
Two sets of three bar elements were mounted on a 0.711 mm thick, 20.32 x 61 cm steel plate. The ultrasonic shear horizontal wave actuator photograph can be seen in Figure 4.7. Six bar elements were used to keep the actuator volume similar to that of the radial in-plane disks tested in Section 4.1, and to make power density comparisons feasible. The phase velocity of the propagating wave is controlled by placing the actuators at, $\lambda = 38.9$ mm from each other in the y direction.

![Figure 4.7: Photo of 8 Km/sec, 202 KHz Shear Horizontal Wave Actuator](image)

In the $x$ direction, the plate velocity of the steel host structure was calculated and used to determine the separation between the two horizontal shear actuators to constructively interfere at the desired excitation frequency (Equation 4.3). By placing the two actuator sets at a wavelength distance from each other, constructive interference between the longitudinal waves traveling in that direction occurs, see Table 4-3.

<table>
<thead>
<tr>
<th>$c$ (Km/sec)</th>
<th>$C_{Plate}$ (Km/sec)</th>
<th>$f_s$ (KHz)</th>
<th>Wavelength $x_2\lambda$ (mm)</th>
<th>Wavelength $x_1d$ (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>4.965</td>
<td>205.5</td>
<td>38.92</td>
<td>24.16</td>
</tr>
</tbody>
</table>

Table 4-3: 205 KHz Shear Horizontal Wave Actuator Configuration
The same design approach was followed to fabricate a 500 KHz shear actuator (Figure 4.8) that triggers the third shear horizontal mode of the structure, presented in Figure 4.3. The experimental and theoretical impedance of the 500 KHz bar elements is presented in Figure 4.9. The dimensions, resonance, and anti-resonance frequencies of the PZT-4 bar elements used for the 500 KHz actuator system are presented in Table 4-4. Twenty-five bar elements were used to maintain the total piezoelectric volume similar to that of the 200 KHz SHW actuator and the PZT-4 disk actuator.

![Figure 4.8: 500 KHz Shear Horizontal Wave Actuator](image)

<table>
<thead>
<tr>
<th>$c$ (Km/sec)</th>
<th>$C_{Plate}$ (Km/sec)</th>
<th>$f_s$ (KHz)</th>
<th>Wavelength $x_2$</th>
<th>Wavelength $x_1$</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>4.965</td>
<td>500</td>
<td>16.16</td>
<td>10.70</td>
</tr>
</tbody>
</table>

A summary of the design objective, constant, and variables is presented in Table 4-5.
4.1.3 Experimental Ultrasonic Mode Detection

The theoretically predicted ultrasonic modes for the 0.711 mm thick steel plate with a 2.51 mm thick ice layer are experimentally determined by using two variable
wedge transducers (Figure 4.10). Snell’s Law (Equation 4.5) is a formula used to describe the relationship between the angles of incidence and refraction (when referring to waves) passing through a boundary between two different isotropic media, such as steel and Plexiglas. The law states that the ratio of the sines of the angles of incidence and of refraction is a constant that depends on the media [65, 47]. The shear phase velocity is defined by $C_s$ and the longitudinal phase velocity is defined by $C_L$.

\[
C_s \sin(\theta_s) = C_L \sin(\theta_L)
\]

An ultrasonic transducer mounted on a variable angle wedge made out of Plexiglas is used to propagate a horizontal shear wave on the steel plate. The actuator creates a longitudinal wave on the Plexiglas. This wave, by Snell’s Law, becomes a shear horizontal wave when it encounters the steel plate, and if the angle of incidence corresponds to the second critical angle between the materials. The shear horizontal wave generated by the actuator travels on the steel plate. The shear horizontal wave is converted to a longitudinal wave as it interacts with the Plexiglas wedge of the sensor.

A MATEC computer was used to generate a tone burst and collect the response of the sensor. As the excitation frequency of the piezoelectric actuator is changed, the amplitude of the signal displayed also changes. The excitation frequency of the actuator is swept until maximum amplitude is detected on the receiver. This frequency corresponds to the location of an ultrasonic shear mode.

The shear wave phase velocity, $C_s$, used to calculate the angle of the wedges (Equation 4.5) is 8 Km/sec, and corresponds to the locations of the modes of interest (Figure 4.11). The longitudinal wave velocity of Plexiglas is known to be 2,670 m/sec.
The experimental shear horizontal mode location of the ice-steel structure agrees with theoretical predictions with a maximum error of 4%. The error is attributed to an uneven ice layer distribution.

Figure 4.10: Schematic Variable Wedge Transducer Receiving a 200 KHz Horizontal Shear Wave

Figure 4.11: Dispersion Curves of a 0.711 mm Thick Steel Plate – 2.54 mm Ice Layer. Triggered Modes with Angle Wedge Transducers
4.2 Ultrasonic Disk Actuator

A continuous wave actuator able to produce ice interface transverse shear stresses in excess of the shear adhesion strength of ice was also selected. Guided by finite element tools, the transverse shear stresses of several ultrasonic actuator configurations were studied. In-plane displacement actuators were selected as potential candidates to impart transverse shear stresses at an accreted ice layer interface.

The impedance of the actuator needed to be such that when bonded a selected steel plate, the impedance low of the system, $Z_m$, would not exceed 400 Ohms. Impedance matching between the amplifier and the system had to be met in order to efficiently drive the actuator (See Appendix B). 400 Ohms is the maximum impedance of the impedance matching network of the amplifier.

To eliminate uncertainty due to heat generation, an actuator with temperatures not exceeding freezing when driven at resonance, and at the required input voltage, is sought. Temperature increases in the PZT material not exceeding $0^\circ$ C reduce the potential of ice interface melting due to heat propagation.

PZT-4 disks were selected to demonstrate low-power ice de-bonding due to ultrasonic shear transverse stresses. The selected disks had a diameter of 76.2 mm (3 in) and a thickness of 2.54 mm (0.1 in). The radial mode of the actuator (28.5 KHz) produced in-plane displacements when bonded to a 0.711 mm thick, 30.48 cm x 30.48 cm steel plate. The in-plane displacement translated to transverse shear stresses at the interface of a 2 mm thick ice layer. The transverse shear stresses exceeded the shear
adhesion strength of ice to steel (1.66 Mpa) when the actuator was driven by 50 V (27.8 KV/m).

Stationary thermal calculations predicted an equilibrium temperature for a 2.54 mm thick PZT-4 disk of -8.4°C when driven at its radial resonance with a 50 V input. A temperature of -8.6°C is predicted at the steel-PZT interface.

The impedance of the radial resonating mode of the PZT disk bonded to the steel plate did not exceed 100 Ohms. This impedance value is less than the maximum value of the impedance matching network of the amplifier, allowing driving the desired frequency with minimal reversed power.

4.3 Summary

PZT-4 disks were selected to demonstrate instantaneous ice de-bonding of ice layers 2.54 mm thick under the effects of ultrasonic transverse shear stresses. The actuator provides transverse shear stresses in excess of ice shear adhesion at low input voltages (50 V). Thermal analysis predicted negative temperatures on the PZT material when driven at its radial resonance frequency with an input voltage of 50 V. The negative temperature of the actuator eliminates potential uncertainty during ice de-bonding experiments, since ice interface melting due to thermal propagation does not occur.

A second set of actuators able to trigger ultrasonic wave modes with high interface shear stress concentration coefficients were designed, and fabricated. Actuator positioning is used to control the wave mode phase velocity. The actuator is designed
such that the frequency of the wave mode with high interface shear stress concentrations matches the resonance frequency of the actuator.
Chapter 5

ULTRASONIC ICING EXPERIMENTS

Experiments to visualize the effects of ultrasound vibration on the interface between ice and a metallic host structure were conducted. Distillated water on isotropic plates froze to form freezer ice at temperatures ranging between -10\(^{0}\) C and -20\(^{0}\) C. An in-house fabricated ice adhesion rig was used to measure the adhesion strength of formed ice patches. Disk actuators de-bonded 2.54 mm thick ice layers instantaneously from a 0.711 mm thick steel plate when driven at their radial mode (28.5 KHz) with an input power of 50 Watts (27.8 KV/m).

A second set of actuators, triggering shear horizontal wave modes with high ISCC values, were also tested. A 202 KHz actuator triggered the \(SH_1\) wave mode of a steel plate with a 2.5 mm thick ice layer at a phase velocity of 8 Km/sec. A second actuator (500 KHz) excited the \(SH_2\) wave mode of the same structure at the same phase velocity. The wave modes provided theoretical ultrasonic modes with high interface shear stress concentrations at the 2.54 mm ice layer interface. The ice layer was accreted to a 0.711 mm thick steel plate. Despite the larger theoretical stress concentrations provided by the excited wave modes, the higher frequency actuators did not provide sufficient transverse shear stress able to delaminate ice with an input power of less than 50 Watts.

The effects of a third actuator providing Lamb waves were studied. The actuator cracked and de-bonded ice layers as thin as 1.5 mm with an input power of 160 Watts.
5.1 Ice Bonding Strength Measurement Rig

A shear adhesion strength rig (similar to the one described in Reference 49) was fabricated. The rig, depicted in Figure 5.1, is used to measure the adhesive shear strength of ice to isotropic plates. A known stress is applied by a shear force to a known surface ice patch. A high traction surface is frozen on top of the ice patch accreted to the surface of the host structure. The high traction surface is connected to a cable-pulley mechanism that transforms vertical weight forces to horizontal shear forces on the ice patch.

To ensure steady state conditions on the ice adhesion strength during each test, the ice patches were allowed to freeze for 5 hours (Figure 5.2). The surface to which the ice adheres was polished with a 10 micron grain sand paper, followed by anti-grease and acetone to avoid surface contamination and to ensure proper ice adhesion. For a steel plate temperature of $-10^0\text{ C}$, the average adhesion shear strength of ice to steel was measured to be 1.5 MPa. The maximum shear adhesion strength of ice to steel found by Raraty et al. is 1.66 Mpa [30].

![Figure 5.1: Schematic and Photo of the Ice Adhesion Measurement Rig and Detail of High Traction Surface](image)
5.2 PZT-4 Disks Static Icing Experiments

Theoretical transverse shear stress predictions at an ice interface (2.54 mm thick) accreted to a steel plate (0.71 mm thick) showed to exceed the shear adhesion strength of the ice layer under the effects of ultrasonic vibrations generated by a PZT-4 radial displacement disks. The described actuator is used to demonstrate instantaneous ice debonding under ultrasonic transverse shear stresses exceeding ice shear adhesion strength.

A disk-shaped PZT-4 imparted in-plane displacements on a steel structure. A photograph of the actuator is presented in Figure 5.3. Test data obtained for a free-free disk actuator is described in Table 5-1. The actuator was bonded to a 30.48 x 30.48 cm x 0.711 mm steel plate, representative of a leading edge protection cap area of a helicopter blade, where ice accretes in flight.
An 800 Watt amplifier (AR-800A3) was used to amplify the signal proceeding from a signal generator (Figure 5.4). The host structure plates were inserted into a freezer that provided temperatures ranging from 0°C to -25°C.

The impedance of the actuator was calculated and compared to experimental results (Figure 5.5). The PZT-4 disks were bonded to the steel plate structure using Eccobond 286, which is an epoxy used for high frequency applications with a thermal bandwidth ranging from -40°C up to 200°C. The impedance of the actuator–plate system was also predicted and experimentally measured. The predicted impedance low points, $Z_m$, agree with an error of less than 3% with experimentally measured impedances, as it is shown in Figure 5.6.

Table 5-1: Test Data 38.1 mm Radius, 2.54 mm Thick Disk (Radial Mode)

<table>
<thead>
<tr>
<th>Outer Dia. (mm, in.)</th>
<th>Thickness (mm, in.)</th>
<th>$f_s$ (KHz)</th>
<th>$f_a$ (KHz)</th>
<th>$Z_m$ (Ohms)</th>
<th>$d_{33} \times 10^{-12}$ (m/V)</th>
</tr>
</thead>
<tbody>
<tr>
<td>76.2, 3</td>
<td>2.54 ,0.1</td>
<td>28.5</td>
<td>33</td>
<td>5</td>
<td>340</td>
</tr>
</tbody>
</table>

Figure 5.3: Photo of 38.1 mm Radius (2.54 mm Thick) PZT-4 Disk Attached to a 30.48 x 30.48 cm Steel Plate
Figure 5.4: Photo of Amplifier and Signal Generator used to Drive the Actuators

Figure 5.5: Experimental and Predicted Impedance Curves of the 38.1 mm PZT-4 Disk
An impedance matching network was used to modify the amplifier impedance to that of the actuator system. The actuator was driven at the system impedance low point that corresponded with the ultrasonic radial resonance of the actuator (28.5 KHz, 65 Ω).

FEM predictions of the stresses generated on the steel plate surface when actuated by the PZT-4 disk provided transverse shear stresses values of up to 3.6 MPa at the interface of a 2.54 mm thick ice layer. These transverse shear stresses exceed the shear adhesion strength of the accreted ice layer. These results are shown in Figure 5.7.

**Figure 5.6: Experimental Impedance Curves of the 38.1 mm PZT-4 Disks and Amplifier Impedance Maximum (400 Ohms)**

An impedance matching network was used to modify the amplifier impedance to that of the actuator system. The actuator was driven at the system impedance low point that corresponded with the ultrasonic radial resonance of the actuator (28.5 KHz, 65 Ω).

FEM predictions of the stresses generated on the steel plate surface when actuated by the PZT-4 disk provided transverse shear stresses values of up to 3.6 MPa at the interface of a 2.54 mm thick ice layer. These transverse shear stresses exceed the shear adhesion strength of the accreted ice layer. These results are shown in Figure 5.7.
As it is presented in Figure 5.8, the predicted voltage required for the PZT-4 disk actuator to generate interface ZX transverse shear stresses on a 2.54 mm thick ice layer exceeding the shear adhesion strength of ice to steel (1.5 MPa) is 49 Volts. Ice debonding is predicted when 28.5 KHz and 49 V are applied to the disk actuator.

An ice layer 2.54 mm thick was allowed to accrete to the surface of the steel plate for 3 hours at a temperature of -20°C. Still photographs of the actuator before was turned on, and in the instant when it was turned on are presented in Figure 5.9. The actuator
were excited at their radial resonance (28.5 KHz), and accreted ice was instantaneously removed for input voltages exceeding 43 V. Thus, the ultrasonic shear de-icing method is experimentally validated. Thermocouples placed on both the actuator and the steel plate showed temperatures of -18$^\circ$ C at the instant of ice de-bonding, eliminating any concern about thermal propagation being a major effect on the ice bonding.

The actuator power consumption required to de-bond the ice layer was measured to be 50 Watts or 0.077 W/cm$^2$ (0.5 W/in$^2$).

Figure 5.8: Theoretical Transverse Shear Stress vs. Applied Voltage at the Interface of a 2.54 mm Thick Ice on a 30.48 x 30.48 cm x 0.711 mm Steel Plate driven by a 38.1 mm Radius 2.54 mm Thick PZT-4 Disk (28.5 KHz)

When driven continuously, the disk actuator prevented ice formation on the entire steel plate surface. The actuator steady state temperature was measured to be -3$^\circ$ C when driven at its radial resonance frequency and 50 V were applied.
5.2.1 Ultrasonic In-Plane Displacement Effects on Variable Ice Thickness

Predicted ice interface transverse shear stresses vary with ice thickness. To further validate the used finite element method, experimental ice de-bonding of three different thickness layers is compared to predicted interface stresses.
Ice layers of three different thicknesses froze on the surface of the 30.48 x 30.48 cm steel plate. A photograph of the three different thickness ice layers (1.77, 2.28, and 3.81 mm) is presented in Figure 5.10.

The actuator, steel plate, and accreted ice layer system was modeled using finite element methods. A schematic of the system is illustrated in Figure 5.11.

Figure 5.10: 30.48 x 30.48 cm Steel Plate with Ice Layers of 1.77, 2.28, and 3.81 mm in Thickness

Figure 5.11: Steel Plate, Ice Layers, Piezoelectric Disk, FEM Model
The XY shear stresses at the interface of the ice layers were calculated. These shear stresses are responsible for ice cracking, but not de-bonding. De-bonding processes are due to ZX transverse shear stresses. The thinnest ice layer (1.77 mm thick) experiences average XY shear stresses values as compared to the other two ice thickness layers. These predicted XY shear values are shown in Figure 5.12. The 1.77 mm thick ice layer experiences the lowest theoretical ZX transverse shear stresses concentration, depicted in Figure 5.13. Experimental results show that this ice layer (1.77 mm) does not completely delaminate after the actuator is turned on for 5 seconds. The 1.77 mm layer also presents a larger crack distribution than the 2.28 and the 3.81 mm thick ice layers. This is due to the effects of XY shear stresses, and because the patch remains bonded to the host structure, since the ZX stresses are not large enough to immediately delaminate the patch (Figure 5.14). The 2.28 and the 3.81 mm ice layers delaminate under 5 seconds of PZT disk operation at 50 Watts (Figure 5.15). All three layers became delaminated in under 15 seconds of operation. Non-instantaneous delamination of ice layers is attributed to dynamic fatigue of the ice bonding. These experimental results demonstrate that the transverse shear stresses generated by continuous ultrasonic vibration are responsible for ice delamination while XY shear stresses contribute to ice cracking. They also demonstrate that larger ZX transverse shear stresses are generated at thicker ice layers.

A qualitative summary of the variable ice thickness experimental results is presented in Figure 5.16.
Figure 5.12: Contour Plot XY Shear Stresses: Top View and Ice-Steel Plate Interface

XY Transverse Stresses: Top View

1.77 mm Thick Ice Layer

2.28 mm Thick Ice Layer

3.81 mm Thick Ice Layer

XY Transverse Stresses at Ice and Actuator Interface

Frequency: 28.5 KHz, Amplitude: 50 V

Pascals

8.6268e+06
6.7097e+06
4.7926e+06
2.8755e+06
9.5046e+05
-9.5862e+05
-2.8757e+06
-4.7928e+06
-6.7099e+06
-8.6268e+06
Contour Plot of Points Located at Ice Interface

Figure 5.13: Contour Plot ZX Transverse Shear Stresses at the Interface of the Ice Layers and the Actuator

1.77 mm Thick Ice Layer: Low Transverse Shear Stresses Distribution

2.28 mm Thick Ice Layer

3.81 mm Thick Ice Layer: Maximum Transverse Shear Stresses

Contour Plot of Points Located at Ice Interface

Figure 5.14: 1.77 mm Ice Layer Complete Delamination Did Not Occur after 4 Seconds of Operation, 50 Watts, 28.5 KHz
Figure 5.15: Completely Delaminated 3.81 and 2.28 mm Ice Layers Under In-Plane Ultrasonic Vibration (5 Seconds of Operation, 50 Watts, 28.5 KHz)

Figure 5.16: Qualitative Delamination and Cracking of 3 Different Ice Layer Thicknesses Accreted to a Steel Plate, Driven by a Resonating PZT-4 Disk Actuator (50 W, 28.5 KHz)
5.3 Ultrasonic Wave High ISCC Mode Actuators

The ice de-bonding effects of a 202 KHz shear actuator triggering the $SH_1$ wave mode of a 0.711 mm thick steel plate with a 2.54 mm thick ice layer was tested. The effects on ice adhesion of shear waves provided by a second actuator triggering the $SH_2$ wave mode of the same structure was also experimentally studied. The 202 KHz and the 500 KHz shear wave modes corresponded with high ISCC modes of the structure (Figure 5.17).

![Dispersion Curves for a 0.711 mm Steel Plate Covered with a 2.54 mm Ice Layer. The Two Points Depicted on the Curves Mark the $C_p$ and $f$ Used to Design Two Different Shear Actuators](image)

The theoretical ISCC value at a given dispersion curve ultrasonic mode, illustrated in Figure 5.17, is independent of the actuator characteristics. The modal quantity assumes constant wave power for all the modes. For that reason, the physical stresses created at the ice interface can only be predicted by modeling the entire system,
including the actuator output. FE methods are used to predict the behavior of the system. The predictions are compared to experimental results.

A third actuator (42 KHz resonance) exciting the $A_0$ Lamb of an isotropic plate with an accreted ice layer was studied. The actuator instantaneously de-bonded and cracked accreted ice. A study of ice removal time changes with ice thickness and input power is presented.

### 5.3.1 202 KHz Actuator Modeling

Finite element modeling tools were used to model the 0.711 mm thick steel plate with a 2.54 mm thick ice layer. Six 202 KHz PZT-4 resonance bar elements excited the steel plate. The experimental impedance of the overall system shows a resonance peak at 114 KHz, corresponding to a longitudinal mode of the actuator. The resonance peak corresponding to the horizontal shear motion of the actuators is located at 202 KHz (Figure 5.18). An impedance matching network with impedance values up to 1,800 Ohms had to be placed in series with the amplifier in order to activate the 202 KHz, 1,100 Ohms shear mode of the actuator.
The transverse shear stresses at the ice interface were calculated for the two frequencies (114 KHz and 202 KHz). The maximum theoretical transverse shear stresses generated at the ice interface did not exceed the shear adhesion strength of ice. The maximum transverse shear stress at the ice interface is calculated to be 0.2 MPa, over 85% less than the shear adhesion strength of ice to steel (Figure 5.19). Instantaneously ice de-bonding of well accreted ice it is not predicted when the actuator is driven at 202 KHz and with an input power of 50 Watts (42 V, 4.2 KV/m). Larger power could not be applied to the actuator since an increase in input voltages would generate internal tensile stresses in the actuator exceeding the tensile strength of the material.

Figure 5.18: Steel Plate-Shear Actuator (202 KHz Resonance) Experimental Impedance
The longitudinal mode of the actuator (114 KHz) did not provide ice interface transverse shear stresses exceeding the shear adhesion strength of ice either. The transverse shear stresses generated by the longitudinal mode of the actuator reached up to 0.8 MPa at the ice interface, as depicted in Figure 5.20. At the described driving conditions, the maximum transverse shear stress value is 50% less than the shear adhesion strength of ice to steel.

Figure 5.19: Ice Interface Transverse Shear Stresses at 202 KHz, 50 Watts
Despite triggering a high ISCC ultrasonic mode, the vibration introduced by the actuator at 202 KHz was not enough to generate transverse stresses able to delaminate well formed ice (Figure 5.19). As the frequency increases, the actuator strains decrease, thus being unable to provide the required stresses to delaminate well accreted ice.

When the lower resonance mode of the actuator was triggered (114 KHz, 50 V), the 2.54 mm thick ice layer was delaminated 80 seconds after the actuator was turned on (Figure 5.21). Theoretical calculations show interface transverse stresses of up to 0.79 MPa at the center of the accreted ice where the shear wave propagates (Figure 5.20). The
maximum theoretical transverse stress are lower (0.79 MPa) than the average experimentally measured adhesion strength of ice to steel (1.5 MPa). A thermocouple placed on the surface of the steel plate read temperatures of -10°C at the time of delamination.

To further demonstrate the effects of transverse shear ultrasonic waves on ice accretion, and to validate theoretical predictions, ice patches of different thicknesses were allowed to accrete to the plate. 114 KHz ultrasonic vibration was applied to the actuator. Ice patches with thicknesses of 2.54 mm, and located in the predicted high transverse stress regions, delaminated as quickly as 14 seconds after the actuator was turned on. These ice patches were placed at locations farther away from the actuator than thinner layers (1 mm thick). Thinner ice layers, and layers not located in high stress regions, took up to 110 seconds to delaminate (Figure 5.22). This experiment validates theoretical predictions pointing towards larger transverse shear stresses at the interface of thicker ice

Figure 5.21: Delaminated 2.54 mm Ice Layers under the effects of 114 KHz Shear Waves. Complete Delamination after 80 Seconds

To further demonstrate the effects of transverse shear ultrasonic waves on ice accretion, and to validate theoretical predictions, ice patches of different thicknesses were allowed to accrete to the plate. 114 KHz ultrasonic vibration was applied to the actuator. Ice patches with thicknesses of 2.54 mm, and located in the predicted high transverse stress regions, delaminated as quickly as 14 seconds after the actuator was turned on. These ice patches were placed at locations farther away from the actuator than thinner layers (1 mm thick). Thinner ice layers, and layers not located in high stress regions, took up to 110 seconds to delaminate (Figure 5.22). This experiment validates theoretical predictions pointing towards larger transverse shear stresses at the interface of thicker ice
layers. These experimental results also eliminate thermal propagation as the main source of ice adhesion failure, since accreted ice layers farther away from the actuator source delaminated faster than thinner layers close to the actuator.

5.3.3 500 KHz Actuator Testing

Finite element model tools were used to model the 500 KHz shear horizontal actuator (Figure 5.23). The 500 KHz actuator introduced transverse shear stresses that were not sufficient to instantaneously de-bond accreted ice layers under 3 mm thick with
an input power of 50 Watts (38 V). Larger input voltages could not be applied due to actuator overdriving conditions. These experimental results are presented in Figure 5.24.

![Figure 5.23: 500 KHz Actuator-Steel Plate-Ice Layer FEM Model](image)

The high frequency vibration and related losses made the actuator reach temperatures close to 90\(^0\) C. Even though local de-icing and anti-icing was obtained at 50 Watts around the actuator region, the strains quickly decayed at locations further away from the actuator. Ice melting was observed 60 seconds after the actuator was turned on at those locations where predicted transverse stresses were maximum (0.1 MPa, less than 10% of the static adhesive strength of freezer ice). Complete ice de-bonding was not observed on the ice layer.
5.3.4 Lamb Wave Actuator

A 42 KHz, 800 Watt transducer applied Lamb waves (vertical out-of-plane displacement instead of shear motion) to a 2 mm (0.078 inch) thick aluminum plate in order to experimentally demonstrate the theoretical concept of ultrasonic modes with high ISCC values. A schematic of the experiment set-up is illustrated in Figure 5.25. Even though Lamb waves are initialized with out-of-plane displacements, shear stresses are also generated at the interface between two layers.
A 1.5 mm (0.06 inch) thick, 38.7 cm² (6 inch²) ice patch was allowed to freeze on the aluminum plate surface. A shear load of 1.15 KPa was applied to the ice patch, corresponding to less than 1% of the required load to de-bond the accreted ice patch without any ultrasonic waves. The Lamb wave transducer was activated up to 80 cm away from the ice patch. The Lamb wave dispersion curves with superimposed ISCC values were theoretically calculated for the 1.5 mm thick aluminum plate with a 1 mm thick accreted ice layer. The theoretical results are presented in Figure 5.26. The 42 KHz Lamb wave transducer triggers an ultrasonic mode with high ISCC value.

When the Lamb waves were applied to the 1.5 mm thick aluminum plate host structure, instantaneous cracks developed on the accreted ice patch. Ice adhesion failure also occurred instantaneously when the Lamb transducer had full power. The recorded ice removal time for a 1 mm thick ice patch at different power input Lamb waves is described in Figure 5.27. An instantaneous photograph of the cracked ice and the removed high traction surface is shown in Figure 5.28.
Figure 5.26: Lamb Wave Dispersion Curves for a 1.5 mm Thick Aluminum Plate with 1 mm Thick Ice Layer

Figure 5.27: Ice Removal Time vs. Lamb Wave Power, 1.5 mm Ice Thickness
Ice removal time for ice thicknesses ranging from 1 to 4 mm was also measured (Figure 5.29). A constant power of 320 Watts was applied to the Lamb transducer for all the different ice thicknesses.

The faster ice removal of thicker ice layers is physically explained by analyzing the wave structure of the ultrasonic mode triggered by the 42 KHz Lamb wave actuator for each different ice thickness. An increase in the thickness of the ice layer creates an
increase in relative inertia at the ice interface, as well as larger ISCC values at the excited ultrasonic mode, thus explaining the observed phenomena (Figure 5.30).

![Figure 5.30: Increase in Modal ISCC Value with Ice Thickness (1 - 4 mm) for the A₀ Lamb Wave Mode (42 KHz)](image)

Aluminum host structures of different thicknesses, each with a 3 mm thick ice patch were also tested under the effects of Lamb waves. As the plate thickness increased up to 9 mm, the effects of the Lamb waves became localized, to the point where ice cracking could not be observed and only local melting occurred.
Theoretical dispersion curves with superimposed ISCC values of a 9 mm thick aluminum plate with a 3 mm thick ice layer show a shift in both the ultrasonic modes and the maximum ISCC towards frequencies not corresponding to the 42 KHz ultrasonic vibration of the Lamb transducer. This explains why global ice adhesion failure does not occur on aluminum plates over 4 mm thick when a 42 KHz Lamb wave is applied to the structure (Figure 5.31). An ultrasonic mode with high ISCC values is not triggered.

Figure 5.31: Lamb Dispersion Curves for a 9 mm Aluminum Plate with 3.5 mm Thick Ice Layer

5.4 Summary

Freezer ice experiments were conducted on a steel plate actuated by an ultrasonic PZT-4 disk. The PZT-4 disk driven at 28.5 KHz (radial resonance of the disk) created transverse shear stresses able to instantaneously de-bond 2 mm thick ice layers when 50 Watts were applied. Thicker ice layers delaminated faster from the surface of the steel.
plate than thinner ice layers. Transverse shear stress predictions point toward higher stress concentrations at the interface of thicker ice layers, explaining the phenomena. The actuator power consumption required to de-bond the 2 mm thick ice layer was measured to be 50 Watts, 0.077 W/cm$^2$ (0.5 W/in$^2$). The main objective of this research was accomplished in this chapter, through the demonstration of instantaneous, low-power, non-thermal ice de-bonding due to ultrasonic transverse shear stresses.

A second actuator system triggering an ultrasonic wave mode of the layered structure was experimentally evaluated. The actuator resonance matched the frequency of a shear wave mode with a high interface shear stress concentration coefficient. The actuator, despite exciting ultrasonic modes with high modal stress wave structures, did not instantaneously de-bond ice layers under 3 mm in thickness with an input power of 50 Watts. The higher frequencies of excitation (202 KHz and 500 KHz) did not provide transverse shear stresses large enough to exceed the shear adhesion strength of ice.

A third actuator, a Lamb wave transducer that excited a high ISCC wave mode, and that provide large enough transverse shear stresses to delaminate accreted ice layers was evaluated. When exciting structures with high ISCC wave modes matching the operational frequency of the actuator, instantaneous ice removal was observed. When wave modes of a steel plate – accreted ice layer system that did not have high ISCC values at the actuating frequency, ice removal was not observed, validating the proposed theoretical modal interface stress results.

All the actuators tested for freezer ice de-icing capabilities and their results are summarized in Table 5-2.
Table 5-2: Actuator Testing and Results Summary Freezer Ice

<table>
<thead>
<tr>
<th>Actuator Description</th>
<th>Frequency (Hz)</th>
<th>De-Icing Power</th>
<th>Impedance Matching</th>
<th>Ultrasonic Mode</th>
<th>Local Anti-Icing</th>
<th>De-Icing</th>
</tr>
</thead>
<tbody>
<tr>
<td>Radial Disp. Disk</td>
<td>28,500</td>
<td>0.077 W/cm²</td>
<td>Yes</td>
<td>SH₀</td>
<td>Yes, 0 Sec.</td>
<td>Yes, 0 sec.</td>
</tr>
<tr>
<td>SH₁ Comb Array</td>
<td>202,000</td>
<td>---</td>
<td>Yes</td>
<td>SH₁</td>
<td>Yes, 40 Sec.</td>
<td>No</td>
</tr>
<tr>
<td>SH₁ Comb Array</td>
<td>114,000</td>
<td>0.03 W/cm²</td>
<td>Yes</td>
<td>SH₀</td>
<td>Yes, 14 sec.</td>
<td>Yes, 32 sec.</td>
</tr>
<tr>
<td>SH₂ Comb Array</td>
<td>500,000</td>
<td>---</td>
<td>Yes</td>
<td>SH₂</td>
<td>Yes, 20 sec.</td>
<td>No</td>
</tr>
<tr>
<td>Lamb Actuator</td>
<td>48,000</td>
<td>0.12 W/cm²</td>
<td>Yes</td>
<td>A₀</td>
<td>Yes, 0 Sec.</td>
<td>Yes, 4 Sec.</td>
</tr>
</tbody>
</table>
Chapter 6

WIND TUNNEL ICING TEST

Wind tunnel impact ice presents challenges that are not encountered when removing “freezer” ice. Ultrasonic in-plane strains generated by disk actuators proved to instantaneously delaminate freezer ice when transverse shear stresses exceeded the adhesion shear strength of the ice. Due to the different freezing physics involved in freezer ice versus impact ice, proof-of-concept icing wind tunnel experiments were conducted on isotropic plates actuated by ultrasonic actuators. Two specimens were tested: a 30.48 x 30.48 cm steel plate actuated by 2 PZT disks and a second plate actuated by 3 PZT disks.

The ultrasonic vibration prevented ice formation on top of the actuator locations for a fraction of the power required with electro-thermal systems (0.18 W/cm$^2$ vs. 3.8 W/cm$^2$). Experiments also showed ice delamination in certain areas of the plates as ice thicknesses reached a critical value of approximately 1.2 mm. A model of the 3 disk actuated steel plate, taking into account the clamping system, was created and compared to experimental results observed during impact icing test experiments. Both the theoretical ultrasonic modes of the system, and the predicted ice shedding areas, agreed with experimental results.
6.1 Double Disk Actuator Impact Icing Experimental Results

A 30.48 x 30.48 cm (12 x 12 inch) steel plate was tested under the effects of impact icing in the Goodrich Icing Tunnel, located in Uniontown, OH. The steel plate had two 76.2 mm diameter disks attached to it. The plate was mounted on two 2.54 cm diameter solid aluminum bars that connected the 55.8 cm (22 inch) wide cross-section of the tunnel test section. A schematic of the test specimen mounted on the clamping bars of the test section is illustrated in Figure 6.1. A photograph of the cross-section and the mounted steel plate is presented in Figure 6.2. To avoid flutter, two brass reinforcement bars (4 mm thick) were bolted at the edges of the steel plate.

![Figure 6.1: Schematic of 30.48 x 30.48 cm, 0.711 mm Thick Steel Plate Mounted to Test Section](image)

The test matrix for the system is presented in Table 6-1. The actuator remained off during the spray time. The actuator was turned on at a frequency matching the impedance low of the system after the spraying time. In addition, different shapes of accreted ice also changed the impedance of the system. The impedance of the system was monitored during the test with an impedance analyzer. The input frequency was manually changed in order to trigger an impedance low corresponding to a radial ultrasonic
frequency of the actuators. The test was repeated for the same flow conditions at $3^0$ and $30^0$, nose down angle of attack. In a third test, the actuators were also tested as an anti-icing system, being driven continuously while spraying.

Figure 6.2: After-Test Photo of 30.48 x 30.48 cm, 0.711 mm Thick Steel Plate with a Triple Disk Actuator Mounted (Test T2)

Table 6-1: Double Actuator Steel Plate Test Matrix

<table>
<thead>
<tr>
<th>Test</th>
<th>AOA</th>
<th>Airspeed</th>
<th>LWC</th>
<th>Air Temp</th>
<th>MVD</th>
<th>Spray Time Actuator Off</th>
<th>Power</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>-3 deg.</td>
<td>150 mph</td>
<td>1 gr/m$^3$</td>
<td>-4.5$^0$ C</td>
<td>40 micron</td>
<td>5 min</td>
<td>100 Watts</td>
</tr>
<tr>
<td>D2</td>
<td>-30 deg.</td>
<td>150 mph</td>
<td>1 gr/m$^3$</td>
<td>-4.5$^0$ C</td>
<td>40 micron</td>
<td>2 min 17 sec</td>
<td>100 Watts</td>
</tr>
<tr>
<td>D3</td>
<td>-30 deg.</td>
<td>150 mph</td>
<td>0.5 gr/m$^3$</td>
<td>-4.5$^0$ C</td>
<td>20 micron</td>
<td>0 min 0 sec</td>
<td>80 Watts</td>
</tr>
</tbody>
</table>
Photos of the steel plate before the actuator was turned on, and after the actuator was driven, are presented in Figure 6.3 for tests D1 and D2. Spraying continued while the actuators were turned on.

While the actuator was turned on, the accreted ice thickness remained under a critical value of 1.2 mm. Once the ice thickness exceeded the value at which the transverse shear stresses exceeded the adhesion strength of the ice, ice shedding occurred, especially in areas around the leading and trailing edges.

In a third test, D3, the actuator remained on during spraying. Ice accretion was prevented directly on top of the actuator region, and constant ice shedding was observed on the plate’s surface and leading edge. The leading edge remained clear of ice during the
entire experiment, and the accreted ice on the rest of the plate surface never exceeded a
critical thickness value (Figure 6.4). The absorption of the high mechanical vibration
generated on the actuator regions introduced thermal rises able to melt accreted ice [78].
The melted ice traveled in the aft location to refreeze.

The ultrasonic disks cracked after approximately 20 minutes of testing due to
tensile stresses exceeding the tensile strength of the material [79, 80]. The PZT-4 actuator
disks cannot withstand significant tensile loading. The dynamic tensile strength of the
actuator disk is approximately 20 MPa. The PZT actuators are extremely brittle and
-cracked while being excited at levels exceeding 90 W. To mitigate the effects of tensile
strength cracking in PZT materials, pre-compressive forces are usually introduced in the
design. The PZT disk actuators were bonded to the steel plate without any pre-
-compressive forces, limiting their input power by manufacture standards to 50 W.

Figure 6.4: Test D3; Double Actuator
6.2 Triple Disk Actuator Impact Icing Experimental Results

A steel plate similar to the one described in the prior section was tested under the effects of three 76.2 mm diameter piezoelectric disks. The tests conducted on this specimen are described in Table 6-2.

### Table 6-2: Triple Actuated Steel Plate Test Matrix

<table>
<thead>
<tr>
<th>Test</th>
<th>AOA</th>
<th>Airspeed</th>
<th>LWC</th>
<th>Air Temp</th>
<th>MVD</th>
<th>Spray Time</th>
<th>Actuator Off</th>
<th>Power</th>
</tr>
</thead>
<tbody>
<tr>
<td>T1</td>
<td>30 deg.</td>
<td>150 mph</td>
<td>1 gr/m³</td>
<td>-4.5°C</td>
<td>40 micron</td>
<td>0 sec.</td>
<td>100 Watts</td>
<td></td>
</tr>
<tr>
<td>T2</td>
<td>30 deg.</td>
<td>150 mph</td>
<td>0.5 gr/m³</td>
<td>-4.5°C</td>
<td>20 micron</td>
<td>0 sec.</td>
<td>100 Watts</td>
<td></td>
</tr>
<tr>
<td>T3</td>
<td>30 deg.</td>
<td>115 mph</td>
<td>0.5 gr/m³</td>
<td>-4.5°C</td>
<td>20 micron</td>
<td>0 sec.</td>
<td>150 Watts</td>
<td></td>
</tr>
</tbody>
</table>

The actuators were driven at 32 KHz, a frequency corresponding to a radial mode of the actuators bonded to the steel plate and with an impedance value of 100 Ohms. The 100 Ohms impedance point of the actuator load was matched with the impedance of the amplifier. During test T1, ice was prevented from forming around the actuator locations at a maximum input power of 1.2 W/in². Clear ice shedding was observed on the leading edge, trailing edge, and on top of the reinforcing brass bars once ice accumulation reached a critical thickness of approximately 1.2 mm in thickness (Figure 6.5). Similar results were observed for the second test performed with the triple actuator system, the results of which are depicted in Figure 6.6.
Figure 6.5: Triple Actuator Steel Plate After 5 Minutes of Icing Exposure (Test T1 Conditions)

Figure 6.6: Triple Actuator Steel Plate After 4 Minutes of Icing Exposure (Test T2 Conditions)
During the third test on this specimen (T3), the front actuator delaminated itself from the steel plate (Figure 6.7). At the higher input power (100 W), the transverse shear stresses at the actuator interface exceeded the adhesion shear strength of the ultrasonic bonding layer used (15.2 MPa). Despite the leading edge actuator being turned off, leading edge ice was shed, and the area above the two remaining actuators remained free of ice at all times (Figure 6.8).

The higher input voltages (150 V) generated tensile stresses in the PZT actuator exceeding the fracture tensile strength of the material, which ranges between 60 and 80 MPa [85]. Actuator cracking occurred.
6.3 Triple Disk Actuator Modeling

After the experiments were conducted, the impedance of a 30.48 x 30.48 cm steel plate covered by a 1.2 mm thick ice layer was calculated (Figure 6.9). The impedance of the system is defined as the ratio of the voltage, $V$, to the current, $I$, in the alternating-current circuit. A low peak of the impedance implies that the input voltage becomes a minimum at the same time as the current becomes a maximum. The frequency corresponding to an impedance low is that of a resonance mode, $f_s$.

The ice layer thickness used during the modeling was determined by the critical ice thickness shed during impact icing tests, which were presented in the prior section. The test section clamping bars that hold the plate in place were modeled by clamping the surfaces of the plate edge, as it is shown in Figure 6.10.
The transverse shear stresses generated by the PZT disks (100 Watt input) on the accreted ice layer were calculated for the ultrasonic mode with 100 Ohms impedance. Theoretical predictions were compared to the ice shedding patterns observed during experiment T1 (Figure 6.11). The frequency of the predicted mode agreed with the experimental driven mode during tests T1, T2 and T3 with an error of 1.25%. The high transverse shear stress areas predicted by the finite element model also agree with the shedding areas observed during the impact icing experiments. The shear adhesion strength of impact ice proved to be higher than that of freezer ice (1.5 MPa) since higher input power was required to instantaneously de-bond accreted ice. The shear adhesion strength of the ice was not directly measured, but FE modeling of the system predicted up to 5 MPa at the interface of a 1.2 mm thick ice layer on areas where experimental de-bonding was observed, and when 100 W input power was applied to the actuators.

**Figure 6.9: Triple Disk Actuator Predicted Impedance**

The transverse shear stresses generated by the PZT disks (100 Watt input) on the accreted ice layer were calculated for the ultrasonic mode with 100 Ohms impedance. Theoretical predictions were compared to the ice shedding patterns observed during experiment T1 (Figure 6.11). The frequency of the predicted mode agreed with the experimental driven mode during tests T1, T2 and T3 with an error of 1.25%. The high transverse shear stress areas predicted by the finite element model also agree with the shedding areas observed during the impact icing experiments. The shear adhesion strength of impact ice proved to be higher than that of freezer ice (1.5 MPa) since higher input power was required to instantaneously de-bond accreted ice. The shear adhesion strength of the ice was not directly measured, but FE modeling of the system predicted up to 5 MPa at the interface of a 1.2 mm thick ice layer on areas where experimental de-bonding was observed, and when 100 W input power was applied to the actuators.
6.4 Composite Leading Edge Coupon Impact Icing Experimental Results

The next set of tests was performed on a composite leading edge coupon provided by Bell Helicopter. The proof-of-concept test was performed to demonstrate that
actuation of composite structures, representative of a leading edge helicopter blades, is possible. A 76.2 mm diameter, 2.54 mm thick PZT-4 disk was bonded directly to the stainless steel protection cap. The plate was placed at a 3 degree nose down angle of attack. During the test the actuator remained turned on during spraying with a net input power of 60 Watts. At wind speeds of 150 mph, LWC of 1 gr/m$^3$, water particle medium volume diameters of 40 microns and a temperature of -4.5$^0$ C, the actuator continuously shed off accreted ice on the plate’s leading edge, maintaining the area clean of ice exceeding a critical thickness. A second run at the same wind tunnel conditions was performed. During this second run, the actuator remained turned off. A clear leading edge ice layer is observed after 5 minutes of spraying (Figure 6.12).

![Leading Edge Ice Shedding](image1)

![Plate Leading Edge Ice Accretion](image2)

Figure 6.12: Composite Coupon Impact Icing Test, Actuator On vs. Actuator Off Results
6.5 Summary

Isotropic plates excited by ultrasonic PZT-4 disk actuators were tested under impact icing wind tunnel conditions. The actuators de-bonded 1.2 mm thick ice layers in areas where predicted transverse shear stresses were maximum. The predicted transverse shear stresses required to de-bond ice layers exceeded the shear adhesion strength of freezer ice, since higher input power was required to instantaneously de-bond accreted ice. Despite, not having measure the actual shear adhesion strength of impact ice, predicted transverse shear stress values of up to 5 MPa were calculated on areas where experimental ice de-bonding was observed. Ice accretion was completely prevented on those areas where maximum transverse shear stresses were calculated, specifically, on top of the actuator region. In those areas where transverse shear stresses were large enough to de-bond ice layers, de-icing was accomplished with a power consumption of 1.2 W/cm². Proof-of-concept experiments on a composite coupon proved that instantaneous ice removal on structures with a stiffness representative of leading edge helicopter blades is possible using ultrasonic excitation.

All the actuators tested for impact ice de-icing capabilities and their results are summarized in Table 6-3.

<table>
<thead>
<tr>
<th>Actuator Description</th>
<th>Frequency (Hz)</th>
<th>De-Icing Power</th>
<th>Impedance Matching</th>
<th>Ultrasonic Mode</th>
<th>Local Anti-Icing</th>
<th>De-Icing</th>
</tr>
</thead>
<tbody>
<tr>
<td>Double Actuator</td>
<td>28,500</td>
<td>0.15 – 0.2 W/cm²</td>
<td>Yes</td>
<td>SH₀</td>
<td>Yes,</td>
<td>Yes,</td>
</tr>
<tr>
<td>Triple Actuator</td>
<td>32,000</td>
<td>0.18 W/cm²</td>
<td>Yes</td>
<td>SH₀</td>
<td>Yes,</td>
<td>Yes</td>
</tr>
</tbody>
</table>
Chapter 7

CONCLUSIONS AND RECOMMENDATIONS

Industry has studied several helicopter leading edge de/anti-icing methods, none of which show the capability to eliminate all the safety concerns related to ice adhesion. Ice shedding, runback effects, excess power consumption, and rotor unbalance due to unsymmetrical ice shedding are the main drawbacks related to current electro-thermal de-icing systems implemented in helicopter rotor blades.

In this thesis, a novel de-icing system based on ultrasonic actuators that create large ice interface transverse shear stresses has been designed, fabricated and tested on isotropic materials representative of helicopter leading edge erosion caps. Piezoelectric actuators were used to impart the ultrasonic vibrations to the host structure. The system was able to instantaneously delaminate thin ice layers (1-3 mm thick) at those ultrasonic frequencies that provided interface transverse shear stresses in excess of the shear adhesion strength of the accreted ice layer. The system affected freezer ice bonding with a power consumption of 0.05 W/cm\(^2\) (0.34 W/in\(^2\)). During impact icing tests conducted on a 30.48 x 30.48 cm steel plate, ultrasonic vibration prevented ice formation around the actuator location area and continuously shed off the accreted ice that reached a critical thickness of 1.2 mm. During these experiments, ice accretion was prevented around the actuators at a net power consumption of 1.2 Watts/in\(^2\). This novel approach to affect ice adhesion has the potential to considerably reduce the required power for de-icing systems of helicopter rotor blades, which currently consume an average of 3.8 W/cm\(^2\) (25 W/in\(^2\)).
Furthermore, at the lower power consumption of ultrasonic de-icing, the system could operate continuously, thus preventing ice accretion altogether.

7.1 Piezoelectric Actuator Modeling

Several piezoelectric actuators exciting isotropic structures (representative of rotor blade leading edge protection caps) were modeled to determine the ultrasonic resonance frequency of the system and the stresses created at the interface of an accreted ice layer at that frequency. In order to validate the tools used to model the actuators, experimental impedance values at a given range of frequencies were compared to those values obtained theoretically. The impedance resonance frequencies were predicted with errors not exceeding 2%. The trend of predicted impedance curves matched those measured experimentally, and proved that the finite element model implemented to analyze the actuator was a useful tool to design and fabricate ultrasonic de-icing systems.

7.2 Ultrasonic Dispersion Curves

To enhance wave propagation along large areas the excitation of ultrasonic wave modes of the steel-ice system was attempted. Certain shear wave modes of the structure provide large modal interface transverse shear stresses between the steel plate and an accreted ice layer. The ultrasonic shear wave modes of the dual layered structure were calculated and experimentally validated. The interface shear stress coefficient values for
all the ultrasonic modes were also calculated and superimposed on the dispersion curves of the structure.

To validate the proposed de-icing concept of exciting ultrasonic wave modes that provide large shear stresses at the ice interface, ultrasonic transducers actuated a 0.711 mm thick, 20.32 x 60.96 cm steel plate. The actuators were designed to have resonance frequencies that matched those wave modes that provided the desired large modal shear stresses at the ice interface. The actuators were spaced to trigger the phase velocity corresponding to the desired ultrasonic wave mode. To validate the existence of an ultrasonic mode on the structure, variable wedge transducers were used. The transducers were mounted on a Plexiglas coupler. By Snell’s Law of refraction, the incoming horizontal shear wave propagating on the steel plate became a longitudinal wave as it traveled on the Plexiglas. A MATEC computer displayed the amplitude of the longitudinal wave measured on the Plexiglas. The transducer incident angle was selected to generate a phase velocity of 8 Km/second, corresponding to that of the modes triggered by the actuators. As the actuation frequency was changed, the recorded longitudinal wave amplitude also changed. The frequency corresponding to ultrasonic structural modes coincided with the maximum wave amplitudes recorded. The theoretically predicted wave modes were experimentally validated with an error of less than 4%.
7.3 Freezer Ice Proof-of-Concept Experiments

The effects on freezer ice bonding under continuously driven ultrasonic actuators were experimentally studied. A disk-shaped actuator (76.2 mm diameter and a thickness of 2.54 mm) was selected as a suitable candidate to impart transverse shear stresses exceeding the shear adhesion strength of ice. The radial mode of the actuator (28.5 KHz) was triggered to create in-plane stresses on a 0.711 mm thick, 30.48 x 30.48 cm steel plate. Instantaneous ice delamination was experimentally observed when the actuator was driven at its radial resonance and with an input power of 50 Watts. Theoretical interface stresses were dependent on the ice layer thickness. As the ice layer thickness increased, larger interface transverse shear stresses were calculated at the ice interface. Theoretical predictions correlated with experimental observations on different ice layers. Ice formation was prevented as the actuator remained turned on at 50 Watts. In a steady state, the plate temperature did not exceed -3°C at an environment temperature of -20°C.

A second actuator with a longitudinal frequency of 114 KHz was also tested. The higher frequency mode generated smaller shear stresses, and ice delamination occurred under fatigue 14 seconds after the actuator was turned on at locations where high transverse stresses were predicted. Thicker ice layers delaminated faster than thinner ones, as the interface stresses increased with ice thickness.

7.4 Ultrasonic High ISCC – Shear Horizontal Mode Experimental Results

Despite triggering an ultrasonic shear mode with high interface shear stress concentration coefficients (ISCC), instantaneous ice delamination was not possible at
frequencies corresponding to these wave modes. The larger vibration frequencies caused smaller actuator strain and corresponding stresses. At input powers under 100 Watts, the created transverse stresses did not exceed the adhesion strength of ice. While instantaneous de-icing did not occur, the ultrasonic vibration introduced mechanical friction that melted ice around the actuator region in less than 20 seconds. At locations further away from the actuator (18 cm away) where the mechanical strains were smaller, it took up to 200 seconds for ice interfaces to melt.

7.5 Icing Wind Tunnel Results

Proof-of-concept experiments were conducted in an icing wind tunnel to investigate the potential de-icing capabilities of the technology under impact icing. The steel plates that were tested under the effects of ultrasonic vibration did not show ice formation around the actuator location at a maximum input power of 0.18 W/cm² (1.2 W/in²). Ice accretion on the surface of the plate never exceeded a critical ice thickness of 1.2 mm on those locations where transverse shear stresses exceeded 5 MPa. Ice shed from the plate once it reached said thickness. The power consumption required to affect impact ice accretion encountered during icing tests, increased with respect to the power required to de-bond freezer ice. These results pointed towards higher shear adhesion strength of impact ice as compared to freezer ice. The input power increase caused actuator integrity failure due to arcing and to excess internal tensile stresses that reached the tensile strength of the material. Actuator delamination also occurred due to excess input power and poor actuator bonding.
7.6 Research Summary

Finite element analysis to analyze ultrasonic piezoelectric actuators continuously driven at ultrasonic frequencies was implemented and validated. The predictions guided the design of an ultrasonic actuator able to instantaneously de-bond ice layers less than 3 mm thick. Experiments were conducted in freezer ice and wind tunnel impact ice environments to demonstrate the ice delamination capabilities of ultrasonic transverse shear stresses. Predicted transverse shear stresses at a given ice interface correlated with experimental results. Ultrasonic de-icing wind tunnel experiments showed the potential of a 90% reduction in power consumption with respect to currently used electro-thermal de-icing systems. Theoretical wave propagation modes that provide high transverse shear stresses at a given ice interface were investigated. An actuator system exciting such modes was designed, fabricated and tested. These wave modes, despite providing large modal transverse shear stresses at the ice interface, did not generate physical shear stresses able to de-bond thin ice layers when excited by resonating ultrasonic piezoelectric actuators.

7.7 Research Contributions

Theoretical and experimental results presented the following contributions:

1) It was predicted and validated experimentally that instantaneous delamination between ice and steel plates representative of helicopter leading edge protection caps is possible using ultrasonic waves, as transverse shear stresses generated at the ice interface exceed the adhesion strength of the ice.
2) Experimental results show that ultrasonic dynamic stresses and fatigue processes are able to delaminate ice layers when transverse shear stresses lower than the static adhesive strength of ice are introduced.

3) Theoretical and experimental results show that thermal energy propagation due to dielectric and mechanical losses on the de-icing actuators is not the main factor affecting ice bonding.

4) Ice interface transverse shear stresses can be predicted using finite element methods that model the actuator, host structure, and accreted ice layer.

5) Ultrasonic ice interface transverse shear stresses are dependent on the strains induced by the actuators and the accreted ice thickness. At a constant actuation input, thicker ice layers undergo larger transverse shear stresses.

6) A minimum stress is required to provide de-bonding transverse shear stresses at an ice interface. As the actuation frequency increases, induced actuator strains decrease. Ultrasonic actuators with resonating frequencies lower than 100 KHz are recommended for ice protection applications.

7) Resonating PZT actuators triggering high ISCC shear horizontal ultrasonic modes, despite presenting high modal ice interface stresses, are not ideal for ice protection on thin isotropic plates, since greater input power is required to reach ice de-bonding transverse shear stresses. The frequencies at which these modes occur for thin (<1.2 mm) isotropic plates are over 200 KHz, where the induced stresses of PZT materials are not sufficient to instantaneously delaminate accreted ice layers.
8) Impedance matching is required to effectively drive the actuator at ultrasonic frequencies. Failure to match load and source impedances reflects the input power back to the source.

9) For all the actuators where impedance matching was enforced, freezer ice formation (-10°C) was prevented near the actuator location at a minimum input power of 0.1 W/cm² (0.7 W/in²). Impact icing conditions encountered during wind tunnel testing required up to 0.18 W/cm² (1.2 W/in²) to avoid ice formation around the actuator location.

A comparison of all ice protection systems, including the proposed ultrasonic method is presented in Table 7-1. The values in the table were calculated for a water content of 0.5 gr/m³ [4]. All the ice protection mechanisms were assumed to be operating on a four-bladed Bell Model 412.

Table 7-1: Comparison Blade Icing Protection Systems For Bell 412 [4, 20]

<table>
<thead>
<tr>
<th>De/Anti-Icing System</th>
<th>Electro-Thermal</th>
<th>Fluid</th>
<th>Pneumatic</th>
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7.8 Recommendations

The proposed ultrasonic de-icing actuator remains conceptual and will need to undergo further tests to fully demonstrate its effectiveness on full-scale helicopter blades under rotor icing environments. In the following sections, the main tasks for complete validation of the system are described:

7.8.1 Transient Wave Propagation

In this research, ultrasonic interface stresses generated by harmonic excitation of PZT actuators were predicted using finite element tools. Dispersion curve theory was also implemented to study propagating wave modes in the structure able to generate large modal interface stresses between an accreted ice layer and a host structure. It is recommended to predict the physical stresses of a given transient wave using commercial finite element computation (Atila Software), as described in Equation 7.1. Integration of Equation 7.1 provides displacement, and electric potential information of a traveling wave generated by PZT actuator with tone burst excitation.

$$\begin{bmatrix} M & 0 \\ 0 & 0 \end{bmatrix} \begin{bmatrix} \ddot{U} \\ \ddot{\Phi} \end{bmatrix} + \frac{1}{\omega} \begin{bmatrix} K_{uu} & K_{u\Phi} \\ K_{u\Phi} & K_{\Phi\Phi} \end{bmatrix} \begin{bmatrix} \dot{U} \\ \dot{\Phi} \end{bmatrix} + \begin{bmatrix} K_{uu} & K_{u\Phi} \\ K_{u\Phi} & K_{\Phi\Phi} \end{bmatrix} \begin{bmatrix} U \\ \Phi \end{bmatrix} = \begin{bmatrix} F \\ -Q \end{bmatrix}$$  \hspace{1cm} (7.1)

In addition to predicting the physical stresses provided by transient wave propagation, transient heat propagation analysis is also recommended. In this research, stationary one dimensional heat equations were used to estimate the steady state temperature of ultrasonic PZT actuators. Three dimensional and transient heat propagation effects were ignored. Actuator temperature estimates and experimental
results demonstrated that thermal propagation is not the main factor affecting ice bonding. Transient and three dimensional thermal propagation predictions will further support the experimental observations described in this work.

7.8.2 Composite Host Structures

The effects of ultrasonic vibration on composite structures are currently being conducted at Penn State by the Vertical Lift Research Center of Excellence. Proof-of-concept experiments have been conducted on a composite coupon provided by Bell Helicopters. The 30.48 x 30.48 cm composite plate is formed by the same layers that form the leading edge of some Bell vehicles.

An in-plane disk actuator (76.2 mm diameter, 2.54 mm thick) was bonded to the steel protection cap of the composite coupon. The impedance of the system was experimentally measured (Figure 7.1), and the ultrasonic resonance of the system was driven with an input power of 60 Watts.

A thermal heater with a 78.7 mm diameter was placed on a second composite coupon. 60 Watts were also applied to the heater. An ice layer of 38.1 x 38.1 x 2.54 mm was accreted on both composite plates at a distance of 45.72 mm away from the actuators.

The thermal system reached temperatures of up to 55\(^\circ\) C, while the PZT actuator reached temperatures of 7\(^\circ\) C. The thermal energy was not sufficient to melt the accreted ice layer at a freezer temperature of -10\(^\circ\) C, while the ultrasonic vibration generated by the PZT disk melted the ice patch in 55 seconds, as depicted in Figure 7.2
This proof-of-concept experiment further demonstrates that thermal effects are not the key source of energy melting the ice interface. It also demonstrates that de-icing is possible on composite structures. Further studies are recommended to optimize the actuator design on composite structures.

Figure 7.1: Experimental Impedance Composite Plate with Ultrasonic Disk

Figure 7.2: Photo of Composite Coupons with Thermal Source and Piezoelectric Actuator and Photo of Melted Ice Patch Interface on a Composite Plate
7.8.3 Actuation Improvement

Radial resonance disk actuators, despite providing sufficient transverse shear stresses to instantaneously de-bond accreted ice layers over 2 mm thick, might not be the optimal actuator to generated transverse shear stresses on a structure. The study of different actuator configurations, such as $d_{13}$, $d_{33}$ plates and $d_{15}$ shear plate actuators is recommended in order to optimize transverse shear stresses at a desired area.

The study of traveling waves to affect ice accretion at locations far away from the actuator should be further studied. Piezoelectric transducers showed that stresses large enough to instantaneously delaminate accreted ice could not be reached using PZT actuators at frequencies exceeding 100 KHz. Electromagnetic transducers could be investigated as potential actuators to trigger high frequency wave modes.

High ISCC wave modes could be shifted to lower frequencies where PZT actuators showed to be effective by adding layers with low Young’s modulus to the host structure. Backing material, such as rubber, could be used to reduce the frequency of optimum ISCC wave modes and to prevent energy leakage to other layers forming the helicopter leading edge.

7.8.4 Rotor Environment Impact Icing

The effects of ultrasonic transverse shear waves on freezer ice and wind tunnel impact ice have been investigated in this thesis. To validate rotor blade applications, rotating icing tests are recommended. Tests under different angles of attack, water concentrations, water particle size, chamber temperature, and particle impacting
velocities should be conducted under the centrifugal forces provided by the rotating environment.

The Vertical Lift Research Center of Excellence at Penn State is in the process of creating a rotor rig icing chamber. An ice ballistic protection wall was designed and built for the rig chamber. The rotor rig was spun up to 600 RPM with 4 foot long, non-aerodynamic blades. Wireless cameras were installed in the hub to visualize ice adhesion, and strain gauges were mounted to measure potential rotor imbalance due to ice accretion. The facility is under construction pending the installation of icing nozzles to control water particle size and liquid water concentration (Figure 7.3). The expected completion date is in August 2008.

7.8.5 Actuator Survivability

To validate the reliability of the ultrasonic de-icing actuator, structural integrity tests should be conducted. Integrity tests should include fatigue testing under ultrasonic
loading of the piezoelectric actuators and the host structures, as well as experimental validation of actuator performance under centrifugal loadings encountered under rotation.

Arcing between the input power leads should be studied in order to be avoided in the future. Standard procedures on ultrasonic actuator fabrication should be investigated to eliminate some of the survivability issues encountered during impact icing tests.

To increase input voltages to the material, while avoiding exceeding the tensile strength of the actuator, pre-compressive forces on the actuators are recommended. During impact icing testing, actuator cracking was observed when input voltages exceeding 150 V were applied. The fracture tensile strength of hard PZT ceramics, such as PZT-4 can be expected to range between 60 MPa and 80 MPa, depending on the fabrication conditions of the material [85].

To demonstrate actuator cracking due to internal tensile stresses exceeding the tensile strength of the material, 2.54 cm diameter PZT-4 disks with a thickness of 0.254 mm were tested in a free-free condition. The PZT-4 disks cracked at input voltage amplitude of 100 V. Predicted tensile stresses of the free-free PZT-4 disks were compared to experimental crack observations. When the actuators were driven at their radial resonance (45 KHz) cracking occurred as the tensile strength of the material was exceeded, as it is illustrated in Figure 7.4.
Brückner-Foit et al. provided a numerical simulation of crack initiation on steel under low frequency fatigue loading [83]. Up to 800 MPa where calculated to be required to promote crack propagation. These fatigue stresses are two orders of magnitude larger than the fatigue stresses required to promote ice delamination (up to 5 MPa calculated for impact icing).

Kikukawa et al. demonstrated that at a frequency loading of 22 KHz, the fatigue strength of carbon steel (S10C, S20C) increases between 40 and 47% as compared with fatigue limits of these steels obtained at a frequency of 40 Hz [81]. More recently, these results are also observed by Ryuichiro et al [82]. Experimental S-N curves for SKD61 steel indicated maximum amplitude stresses requirements ranging between 700 and 900
MPa when excited at 20 KHz, while stresses between 550 and 750 MPa where required at an excitation frequency of 20 Hz. This frequency effect has not been theoretically clarified and should be further studied.

Despite the larger stresses required to promote crack and fretting [84] fatigue in steel as compared to the required stresses to de-bond accreted ice, a study of the effects of the high power de-icing ultrasonic vibration on crack propagation and other fatigue processes should be conducted. This study should expand to the effects of the ultrasonic excitation on the integrity of other composite materials forming the blade of the helicopter.

7.8.7 Driving Frequency and Impedance Matching

Impedance and frequency matching was cumbersome due to impedance changes encountered at different loadings introduced by ice accretion and changes in actuator temperature. In addition, a shift in the impedance curves of a system is expected at high input voltages due to the non-linear nature of PZT material.

A feedback control frequency and/or impedance sweep method is recommended to ensure triggering a desired ultrasonic mode while matching load and source impedances.
7.8.8 Other Applications

Ultrasonic shear stresses have shown to de-ice and avoid ice accretion of isotropic structures. Engine inlets and other isotropic surfaces on the fuselage could be protected from ice using the technology described in this thesis. For example, the nose of the V22 Osprey is collecting ice during flight tests in icing conditions. The accreted ice sheds off once it reaches a certain thickness. The detached ice impacts with the rotors, a concern that could be prevented by implementing ultrasonic shear de-icing concepts.

In addition, the study of the effects of the technology described in this research on glass and other windshield structures is recommended.
Bibliography


Appendix A

Piezoelectricity and Electromechanical Coupling

Symbols List:

\[
\begin{align*}
    d & \quad \text{Piezoelectric charge constant (C / N)} \\
    e_0 & \quad \text{Permittivity of free space (8.85 x 10^{-12} \, \text{farad / m})} \\
    e^T & \quad \text{Permittivity of ceramic material (farad / m) (at constant stress)} \\
    f_s & \quad \text{Minimum impedance frequency (resonance frequency) (Hz)} \\
    f_a & \quad \text{Maximum impedance frequency (anti-resonance frequency) (Hz)} \\
    G & \quad \text{Piezoelectric voltage constant (Vm / N)} \\
    k & \quad \text{Electromechanical coupling factor} \\
    k_{\text{eff}} & \quad \text{Effective coupling factor} \\
    k^T & \quad \text{Relative dielectric constant (at constant stress)} \\
    N & \quad \text{Frequency constant (Hz-m)} \\
    Q_m & \quad \text{Mechanical quality factor} \\
    S & \quad \text{Elastic compliance (m^2/N)} \\
    S & \quad \text{Strain} \\
    T & \quad \text{Stress} \\
    \tan \delta & \quad \text{Dielectric dissipation factor} \\
    Y_e & \quad \text{Young's modulus (N/m}^2) \\
    Z & \quad \text{Impedance at } f_m \text{ (ohm)} \\
    v & \quad \text{Velocity of sound in the ceramic material (m / s)} \\
    \rho & \quad \text{Density of ceramic (kg/m}^3) \\
\end{align*}
\]

Piezoelectric effects occur in noncentosymmetric crystals, such as PZT and quartz. Electric dipoles are generated due to mechanical deformations. The converse effect (reverse piezoelectric effect) has been used in this thesis to generate transverse shear stresses between a host structure and ice in order to de-bond the accreted ice.

The constitutive equations for piezoelectric materials under small field conditions are defined in Equation A.1 [72].
\[
\begin{bmatrix}
D \\
S
\end{bmatrix} =
\begin{bmatrix}
\bar{\varepsilon}^T & d^d \\
d^c & \bar{\varepsilon}^E
\end{bmatrix}
\begin{bmatrix}
E \\
T
\end{bmatrix}
\] (A.1)

Where \( D \) is the electric displacement vector (C/m\(^2\)), \( S \) is the strain vector, \( E \) is the applied external electric field vector (V/m), and \( T \) is the stress vector (N/m\(^2\)).

\( \bar{\varepsilon}^T = e^T (1 - \delta) \) is the complex dielectric permittivity at a constant stress, \( d^d_{im} \) and \( d^c_{jk} \) are the matrices of the piezoelectric strain coefficients, and \( \bar{\varepsilon}^E_{km} = e^E_{km} (1 - \eta) \) is the matrix of the complex elastic compliance at constant electric field. \( \delta \) is the dielectric loss factor and \( \eta \) is the mechanical loss factor of the material. The subscripts \( T \) and \( E \) denote that the material property has been measured at constant stress and electric field respectively.

**Piezoelectric Constant [70]**

The piezoelectric constant, \( d \), is the polarization generated per unit of mechanical stress (\( T \)) applied to a piezoelectric material. Alternatively it is the mechanical strain (\( S \)) experienced by a piezoelectric material per unit of electric field applied. The first subscript to \( d \) is the direction of the applied field strength. The second subscript is the direction of the induced strain. The strain induced in a piezoelectric material by an applied electric field is the product of the value for the electric field and the value for \( d \).

**Piezoelectric Voltage Constant [70]**

The piezoelectric voltage constant, \( g \), is the mechanical strain experienced by a piezoelectric material per unit of electric displacement applied. The first subscript to \( g \) indicates the direction of the applied electric displacement. The second subscript is the direction of the induced strain (Refer to Figure A.1).
The dielectric permittivity, \(e\), for a piezoelectric ceramic material is the dielectric displacement per unit electric field. \(e^T\) is the permittivity at constant stress, \(e^S\) is the permittivity at constant strain. The first subscript to \(e\) indicates the direction of the dielectric displacement; the second is the direction of the electric field.

The relative dielectric constant, \(K\), is the ratio of \(e\), the amount of charge that an element constructed from the ceramic material can store, relative to the absolute dielectric constant, \(e_0\), the charge that can be stored by the same electrodes when separated by a vacuum, at equal voltage (\(e_0 = 8.85 \times 10^{-12}\) farad / meter).

Elastic Compliance [70]

Elastic compliance, \(s\), is the strain produced in a piezoelectric material per unit of stress applied and, for the 11 and 33 directions, is the reciprocal of the modulus of elasticity (Young's modulus, \(Y_e\)). \(s^D\) is the compliance under a constant electric displacement (open circuit); \(s^E\) is the compliance under a constant electric field (short
circuit). The first subscript indicates the direction of strain; the second is the direction of stress.

**Electromechanical Coupling Factor [70]**

The electromechanical coupling factor, $k$, indicates the effectiveness with which a piezoelectric material converts electrical energy into mechanical energy, or vice versa. The first subscript to $k$ denotes the direction along which the electrodes are applied; the second denotes the direction along which the mechanical energy is applied, or developed.

Coupling factor values quoted in ceramic the specifications of suppliers typically are theoretical maximum values. At low input frequencies, a typical piezoelectric ceramic can convert 30 - 75% of the energy delivered to it in one form into the other form, depending on the formulation of the ceramic and the directions of the forces involved.

A high $k$ usually is desirable for efficient energy conversion, but $k$ does not account for dielectric losses or mechanical losses, nor for recovery of unconverted energy. The accurate measure of efficiency is the ratio of converted, useable energy delivered by the piezoelectric element to the total energy taken up by the element. By this measure, piezoelectric ceramic elements in well designed systems can exhibit efficiencies that exceed 90%.

The dimensions and shape of a ceramic element can dictate expressions of $k$:

- $k_{33}$ factor for electric field in direction 3 (parallel to direction in which ceramic element is polarized) and longitudinal vibrations in direction 3 (ceramic rod, length >10x diameter)
- $k_t$ factor for electric field in direction 3 and vibrations in direction 3 (thin disc, surface dimensions large relative to thickness; $k_t < k_{33}$)
Dielectric Dissipation Factor [70]

The dielectric dissipation factor, tangent $\delta$, for a ceramic material is the tangent of the dielectric loss angle. $\tan \delta$ is determined by the ratio of effective conductance to effective susceptance in a parallel circuit, measured by using an impedance bridge. Values for $\tan \delta$ typically are determined at 1 kHz.

Frequency Constant [70]

When an unrestrained piezoelectric ceramic element is exposed to a high frequency alternating electric field, an impedance minimum, the planar or radial resonance frequency, coincides with the series resonance frequency, $f_s$. The relationship between the radial mode resonance frequency constant, $N_r$, and the diameter of the ceramic element, $D\Phi$, is expressed by: $N_r = f_s D\Phi$.

At higher resonance, another impedance minimum, the axial resonance frequency (used in the “bar elements” in Chapters 6 -7), is encountered. The thickness mode frequency constant, $N_t$, is related to the thickness of the ceramic element, $h$, by: $N_t = f_s h$.

A third frequency constant, the longitudinal mode frequency constant, is related to the length of the element, $l$: $N_3 = f_s l$. 

$k_{31}$ factor for electric field in direction 3 (parallel to direction in which ceramic element is polarized) and longitudinal vibrations in direction 1 (perpendicular to direction in which ceramic element is polarized, ceramic rod configuration)

$k_p$ factor for electric field in direction 3 (parallel to direction in which ceramic element is polarized) and radial vibrations in direction 1 and direction 2 (both perpendicular to direction in which ceramic element is polarized, thin disc configuration)
Constant Definitions [70]

Relative Dielectric Constant:

\[ K^T = \varepsilon^T / \varepsilon_0 \]

Elastic Compliance:

\[ s = 1/\sqrt{2} \]

\[ s^D_{33} = 1/\sqrt{D_{33}} \]

\[ s^E_{33} = 1/\sqrt{E_{33}} \]

\[ s^D_{11} = 1/\sqrt{D_{11}} \]

\[ s^E_{11} = 1/\sqrt{E_{11}} \]

Electromechanical Coupling Factor (electric energy converted / mechanical energy input)

Low Frequency

- Ceramic disc: \( k_p^2 = 2d_{31}^2 / ((s^E_{11} + s^E_{12})\varepsilon^T_{33}) \)

High frequency

- Ceramic disc: \( K_p \equiv \sqrt{2.51(f_a - f_s) / f_a - ((f_a - f_s)/f_a^2)} \)

- Any shape: \( k_{\text{eff}}^2 = (f_a^2 - f_s^2)/f_a^2 \)

Frequency Constant

\( N_L \) (longitudinal mode) = \( f_s l \)

\( N_P \) (radial mode) = \( f_s D \)

\( N_T \) (thickness mode) = \( f_s h \)

Mechanical Quality Factor

\[ Q_m = f_a^2 / (2\pi f_c C_z m (f_a^2 - f_s^2)) \]

Piezoelectric Charge Constant
\[ d = k \sqrt{S E^T \varepsilon^T} \]

\[ d_{31} = k_{31} \sqrt{S_{11} E_{33}^T \varepsilon_{33}^T} \]

\[ d_{33} = k_{33} \sqrt{S_{33} E_{33}^T \varepsilon_{33}^T} \]

\[ d_{15} = k_{15} \sqrt{S_{55} E_{11}^T \varepsilon_{11}^T} \]

Piezoelectric Voltage Constant

\[ g = \frac{d}{\varepsilon T} \]

\[ g_{31} = \frac{d_{31}}{\varepsilon_{33}^T} \]

\[ g_{33} = \frac{d_{33}}{\varepsilon_{33}^T} \]

\[ g_{15} = \frac{d_{15}}{\varepsilon_{11}^T} \]

Young's Modulus

\[ Y = \frac{(F/A)/(\Delta l/l)}{T/S} \]

Relationship among \( d \), \( \varepsilon^T \), and \( g \)

\[ g = \frac{d}{\varepsilon^T} \text{ or } d = g \varepsilon^T \]
Appendix B

Impedance Matching Requirements

Even though theoretical shear stresses generated by the shear segments exceed the adhesion strength of ice, instantaneous ice de-bonding was not observed. The lack of instantaneous ice de-bonding can be explained by observing the “forward” and “reversed” power sent and received by the amplifier. The forward power is the power sent to the actuator systems, while the backward power is the power reflected from the system. To ensure that the voltage reaching the actuator is being converted into mechanical energy, minimum backward, or reflected power, is expected. To create this condition, impedance matching between the source (amplifier) and the load (actuator system) must occur at these ultrasonic frequencies.

Impedance matching is the practice of attempting to make the output impedance of an amplifier equal to the input impedance of the actuator to which it is ultimately connected. This practice maximizes the power transfer and minimizes reflections from the load. In low-frequency or direct-current power transmission cases, the reactance is negligible or zero and the impedance can be considered a pure resistance, expressed as a real number. At ultrasonic frequencies, impedance matching is required to ensure power transfer to the actuator.
**Minimum Losses: Impedance Matching Proof**

The impedance matching condition requirement is well known, and can be proven from Ohm’s Law. Impedance matching condition requirements in ultrasonic actuators is well documented in literature [60] and is reproduced in this thesis.

In a physically realizable system, the source and load are not completely resistive and have some inductive or capacitive components (Figure B.1).  

![Figure B.1: Schematic AC Amplifier – Actuator Electrical System](image)

In the electrical system, AC power is being transferred from the amplifier, with magnitude voltage $|V_S|$ (peak voltage) and fixed source impedance, $Z_S$, to a piezoelectric actuator with impedance $Z_P$, resulting in a magnitude current $|I|$. $|V|$ is the source voltage divided by the total circuit impedance (Equation B.1):

$$|I| = \frac{|V_S|}{|Z_S + Z_L|}$$  \hspace{1cm} (B.1)

The average power, $P_P$, dissipated in the load is the square of the current, multiplied by the resistive portion, $R_P$, of the load impedance (Equation B.2).
where the resistance, $R_S$, and reactance, $X_S$, are the real and imaginary parts of $Z_S$, and $X_P$ is the imaginary part of $Z_P$.

In order to determine the values of $R_P$ and $X_P$ (since $V_S$, $R_S$, and $X_S$ are fixed) for which this expression is a maximum, first the value of the reactive term $X_L$, for which the denominator of Equation B.2 is a minimum, must be found. Since reactances can be negative, this denominator is minimized by making:

$$X_P = -X_S$$

(B.3)

Thus, Equation B.2 is reduced to:

$$P_P = \frac{1}{2} \frac{|V_S|^2 R_P}{(R_S + R_P)^2 + (X_S + X_P)^2} = \frac{1}{2} \frac{|V_S|^2}{R_S^2 / R_L + 2R_S + R_P}$$

(B.4)

and it remains to find the value of $R_P$ which maximizes this expression. As the denominator of Equation B.4 becomes a minimum, the power dissipated in the piezo is a maximum. Differentiating the denominator of Equation B.4, a maximum power dissipated in the piezoelectric actuator is obtained. This occurs when the resistance of the load, $R_P$, is equal to the resistance of the amplifier, $R_S$. The combination of conditions described in Equation B.5 can be concisely written with a complex conjugate Equation B.6.
Any reactive components of source and load should be of equal magnitude, but opposite phase. This means that the source and load impedances should be complex conjugates of each other.

\[ R_P = R_S \quad (a) \]

\[ X_P = -X_S \quad (b) \quad (B.5) \]

\[ Z_P^* = Z_S \quad (B.6) \]
Appendix C

Shear Segment Actuators

Shear segments were the first actuation system adopted to investigate the effects of ultrasonic shear waves on ice bonding (the constitutive equations of PZT materials and the definitions of the piezoelectric and mechanical constants are defined in Appendix A). The shear segments are continuously poled along their length using a poling machine designed and fabricated in-house. The electric field is applied along their width, to generate a pure surface shear force, as it is shown in Figure C.1 and Figure C.2.

Figure C.1: Photo and Schematic Shear Segment

Figure C.2: Schematic of Shear Segment Deformation
Shear $d_{15}$ piezoelectric segments were considered as a possible candidate to trigger high interface shear stress ultrasonic modes. The fabrication of these shear segments, as well as their modeling, is described in this section.

Palacios et al. designed, fabricated, and tested a continuous piezoelectric poling machine to pole PZT material along its length [46]. If an electric field is applied across the width of a poled element, pure shear is obtained. Electric field restrictions introduced by the high coercive forces of hard PZT materials limit the continuous poling machine to poling soft PZT materials only, with maximum dimensions of 152.4 x 7.62 x 3.175 mm. PZT-5 segments of 101.6 x 7.62 x 3.175 mm (4 x 0.3 x 0.125 inches) were selected as the raw material to create the shear actuators (dimensions dictated by poling machine fixtures). The properties of the material are listed in Table C-1. The constitutive equations of the material, as well as definitions of all the electric and mechanical properties of the material are described in the Appendix of this thesis. The distance between the applied voltage (the width of the segment in this case, $w$) and the shear frequency constant of the material, $N_5$, dictates the resonance frequency of the shear patch (Equation C.1).

$$f_s = N_5 w$$  \hspace{1cm} (C.1)

One shear segment might not provide sufficient displacement and stresses to affect ice bonding. Several shear segments can be assembled to create a shear actuator. The shear segments can be glued together with conductive epoxy (Figure C.3). Several shear segments can also be placed at a distance multiple of their width to create constructive interference of the generated waves. When the segments are placed together,
the poling direction of adjacent segments must alternate so that when an electric field with sharing electrodes between the segments is applied, a constructive shear motion occurs (Figure C.3). The piezoelectric constitutive equation for a piezoelectric segment poled along its length is [71]:

\[
\begin{bmatrix}
\varepsilon_1 \\
\varepsilon_2 \\
\varepsilon_3 \\
\gamma_{23} \\
\gamma_{31} \\
\gamma_{12}
\end{bmatrix} =
\begin{bmatrix}
0 & d_{31} & 0 \\
0 & d_{33} & 0 \\
0 & d_{32} & 0 \\
0 & 0 & d_{15} \\
0 & 0 & 0 \\
d_{15} & 0 & 0
\end{bmatrix}
\begin{bmatrix}
E_1 \\
E_2 \\
E_3
\end{bmatrix}
\]  
(C.2)

The electric field is applied perpendicular to the poling direction, generating strains, \(\gamma_{12}\), linearly dependent to the \(d_{15}\) piezoelectric coefficient of the material and the applied electric field, \(E_3\) (Equation C.3, Appendix A).

\[
\gamma_{12} = d_{15}E_3
\]  
(C.3)

From the shear frequency constant, \(N_5\), listed in Table C-1, the estimated resonance frequency for a 7.62 mm wide shear segment (maximum width allowed by the continuous poling machine [46]) is calculated to be 160 KHz. If two shear segments are glued together to form a 15.24 mm shear segment, the shear resonance frequency is calculated to be 80 KHz. The resonance frequency calculated from the frequency constant provides a starting point to analyze a frequency range where the response of the shear segment is maximum. The experimental resonance frequency might vary depending on the poling conditions of the material.
Table C-1: PZT 5 Electromechanical Material Properties [50]

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<td>Mechanical Qm (°)</td>
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<td>Maximum Operating Temperature (°C)</td>
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<td>Planar Coupling Factor k_p (-)</td>
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Continuation Table C-1

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<td>Longitudinal Frequency Constant Nl (Hz-m)</td>
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<td>AC (KV/m [V/mil])</td>
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</tr>
<tr>
<td>DC - forward (KV/m [V/mil])</td>
<td>600 [15]</td>
</tr>
<tr>
<td>DC - reverse (KV/m [V/mil])</td>
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</tr>
</tbody>
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C.1 Material Selection

PZT-5, also known as Navy Type II, is a soft piezoelectric material designed for applications where high electromechanical couplings are required. This material is mainly used as a receiver since it generates larger amounts of voltage for a given strain. Conversely, it generates larger displacements as a given electric field is applied when compared to harder PZT materials. Its lower coercive forces make this material easy to polarize, but it the potential to de-pole under high AC fields [50, 70].

Even though hard PZT material is a better candidate for anti-icing prototype designs than softer PZT material, hard PZT has larger coercive forces that make it difficult to polarize (larger electric fields are required). For those prototypes polarized in-house (shear segments), the use of soft PZT material is required to avoid exceeding the electric field limits of available amplifiers. PZT-5 is used for the d_{15} shear segments, while PZT-4 is used for the disk actuators presented in this thesis.
C.2 Shear Actuator Fabrication

The poling, cleaning, surface preparation and bonding processes involved in the fabrication of shear segments is described in this section.

C.2.1 Continuous Poling of Piezoelectric Materials

Palacios et al. designed, built, and fabricated a multiple segment continuous longitudinal poling machine [46]. The poling machine showed to fully pole four 101.6 mm (4 inch) long PZT-5A bars with an electric field of 15 KV/cm and at temperatures not higher than 120° C. A power supply of 30 KV of voltage was driven at 50% of its capacity, showing to provide enough voltage to create the necessary electric field to pole the segments.

Poling the piezoelectric segments forming the shear actuator is a key step in the fabrication process of the proposed ice protection system. In prior work involving PZT shear actuators, the poling of the segments was done at the U.S. Naval Research Lab in Washington, D.C. [51], where a single segment poling machine was available.

Poling piezoelectric ceramics is the main process that gives these materials their unique qualities. This process is required to align the polarization of the polycrystalline materials that form the ceramics. Poling is usually done for thin sections of small pieces, and normally in the transversal direction, limiting the voltage required to obtain full polarization in the ceramic material. The poling of these thin geometrical sections can be easily done with a small power supply, since high electric fields are obtained as a result of the small thickness of the sheets that are being poled.
In general, the poling process is executed by applying a temporary high electric field that creates a polarization in the direction of the electric field. For small sections of PZT ceramics, this can be accomplished by applying 2.5 KV/mm electric fields at temperatures around 120º C [51]. As mentioned, this can be done by using small power supplies. On the other hand, when trying to polarize long segments of PZT along their longitudinal direction, a very high voltage, and therefore very expensive and potentially hazardous power supply, would have to be used. The U.S. Naval Research Lab created the first continuous poling device that is capable of poling a long bulk section at a time without needing large power supplies [51]. To accomplish the objectives of this work, the author created his own poling facility able of poling four ceramic segments at a time, speeding up the manufacturing process.

C.2.2 Poling Machine Design

The poling machine (Figures C.4, and C.5) continuously transfers high voltage over 1 centimeter sections along the piezoelectric segments until the entire segment is poled. Flexible conductive rubber was used as electrodes. The separation between the electrodes conducting the voltage along the segments is 1 cm, and the applied voltage is 15 KV. The electrode assembly, as well as the PZT segments, is immersed in a dielectric oil bath (peanut syrup) at a temperature of 120º C. The poling speed, given by the “rolling car” speed, is 1 mm/min. The design of the apparatus allows for the poling of up to four segments at a time. The separation of the electrodes (1 centimeter) creates average electric field strength of approximately 2.1 KV/mm at the applied voltage [51].
Figure C.4: Schematic of Continuous Poling Machine, Proposed in Reference [71] by Centolanza et al.

Figure C.5: Photo of the Continuous Poling Machine, Reference Palacios et al. [46]
To avoid electric breakdowns from voltage arching from one electrode to the other, the rubber electrodes are separated from each other by a sheet of non-conductive ceramic used to hold the electrodes in the oil bath. It is important to avoid transmitting voltage to any conducting material close to the electrodes or the PZT segments themselves, or to any other parts of the device, such as the frame structure, the hot plate, or the motor that displaces the electrodes along the segments.

**C.2.3 Rationale for Selected Electric Field and Temperature**

To reduce hazardous risks and to speed up the process of the heating of the oil, the lowest possible electric field and temperature are desired during the poling process. The electric field and the temperature must be sufficient to provide the highest piezoelectric coupling coefficient for a given material. The poling field conditions and the poling temperature were determined from the empirical tests done at the Naval Research Lab [51] (Figure C.6, Figure C.7).

![Figure C.6: Poling Field Strength Dependence of Strain d33 of PZT 5-A [51]](image)
To pole PZT-5A, the poling field strength must range between 1.4 and 3 KV/mm. PZT-5A specimens can be poled to $d_{33}$ values of over 430 m/V with small average fields of 1.4 KV/mm applied at temperatures of 70º C. By applying a 15 KV to the electrodes continuously along the length of the segment at a speed of 1 mm/min, a 2.1 KV/mm average electric field is obtained across the 1 centimeter distance separating the electrodes [51]. If the process is run at 120º C, ideal poling of the specimen is ensured, producing PZT segments with theoretical $d_{33}$ coefficient values of 400 V/m (Table C-1).

To validate the poling machine’s effectiveness, the experimental measurement of the $d_{33}$ coefficient of five different poled segments was conducted. A known vibration amplitude was applied to the specimen by a $d_{33}$ coefficient measuring device. The device measures the generated voltage for the given strain (direct piezoelectric effect) to determine the materials piezoelectric coefficient. The average experimental $d_{33}$ coefficient measured was 430 m/V, exceeding the expected theoretical poling coefficient of the material (Table C-1). To further validate the poling process, $d_{15}$ piezoelectric

![Figure C.7: Poling Temperature and $d_{33}$ Field Strength Dependence of PZT-5A [51]](image)
coefficient calculations were conducted by measuring the strains generated by a PZT-5 shear segment when a given electric field was applied. $d_{15}$ piezoelectric coefficient values of up to $781 \times 10^{-12}$ m/V were calculated for input electric fields close to $2 \times 10^4$ V/m [46].

C.2.4 Cleaning, Sputtering, Wiring and Bonding

After segment poling, the piezoelectric material is immersed in an acetone ultrasonic cleaning bath for 5 minutes. Excess oil is eliminated.

To create conductive gold electrodes on the desired side faces of the segments, all sides of the piezoelectric segments (except those that will have the electrodes) are covered with tape. The segments are sprayed with gold paint. Then the tape is removed, leaving the two electrodes on the piezoelectric segment. Two Ampere, 1,000 Volt, rated cables are glued to the electrode sides with silver epoxy glue (E-Solder 3021).

An impedance analyzer (HP 4195) is used to measure the impedance of each segment and to verify that the piezoelectric material is properly poled. The effective coupling factor, $K_{eff}$, is calculated from the resonance, $f_r$, and anti-resonance frequency, $f_a$, (Equation C.4). An effective coupling factor ranging between 0.25 and 0.27 is expected to ensure proper segment poling. The piezoelectric segments are glued with non-conductive Eccobond 286 epoxy to the host structure. This epoxy is designed to operate under piezoelectric ultrasonic vibration and have a temperature operational bandwidth ranging from $-55^0$ C to $105^0$ C.
C.2.5 Surface Conditioning

Conditioning the surface prior to applying an actuator to a host structure is extremely important in order to avoid actuator delamination. For example, ZX transverse shear stresses on the order of 4 MPa are predicted at the interface of a disk actuator and a steel plate when 50 Volts (19.6 KV/m) are applied. The high stresses provided by the ultrasonic actuator create large enough stresses to make the Eccobond non-conductive glue fail if the host structure or actuator surfaces are dirty with oils or other particles. An example of actuator bonding failure is presented in Figure C.8.

To prevent actuator de-bonding, CSM-2 degreaser is applied right after sanding the host structure surface with a 10 micron sandpaper. The degreaser is cleaned with a dry cloth and then M-Prep Conditioner (6556) from M-Line Measurement Group, Inc. was applied and cleaned with a new cloth. Once the surface is completely clean, an M-Prep Neutralizer from the same company is used to avoid chemical reactions between the Eccobond non-conductive glue and the M-Prep Conditioner.
The actuator surface should also be free of oils or other particles that could weaken the glue adhesion strength. Ultrasonic cleaning eliminates all of these particles and oils from the PZT material surface.

C.3 Shear Segment Finite Element Modeling

In the free-free condition finite element model of the PZT-5 segment (Figure C.9), the shear resonant frequency is calculated to be 113 KHz. The results are presented in Figure C.10. The admittance peak for the free-free isolated shear patch provides an admittance of 0.011 1/Ohms, or an impedance of 90 Ohms.
The maximum total displacement of the actuator is calculated to be 0.5 µm. The theoretical internal XY shear stresses generated by the segment reach maximums of 2.2 MPa (Figure C.11).
C.3.1 Shear Segment Isotropic Host Structure Model

The finite element analysis is used throughout this research to model not only piezoelectric material, but also piezoelectric shear segments actuating isotropic plates and isotropic plates covered with ice. Ice layers are also modeled as isotropic structures. When the segments are glued to a host structure, constraining forces increase the impedance of these segments to values ranging between 600 and 1,200 Ohms (Figure C.12).
Theoretical ice interface transverse shear stresses responsible for ice de-bonding can be calculated using the finite element tools presented in this chapter. As an example, a shear segment actuator formed by two adjacent shear segments of 50.8 x 7.62 x 3.175 mm mounted to an aluminum plate is modeled. The aluminum host structure is 167.6 x 50.8 x 1 mm (Figure C.13).

Contour plots are used to illustrate the maximum predicted shear stresses at a given time during the sinusoidal motion of the structure. Theoretical XY shear stresses of up to 10 MPa are predicted at the aluminum plate surface when the actuator is driven at its first shear resonance (60 KHz, 500 $V_{p-p}$, 32.5 KV/m, Figure C.14). At the same driving conditions, the calculated XZ transverse shear stresses at an accreted 1.27 mm (0.05 inch) thick ice patch reach 5 MPa, which exceeds the adhesion strength of ice to aluminum (Figure C.15). The maximum ice adhesion shear strength of freezer ice to aluminum encountered in the literature is 1.66 MPa [30].

Figure C.12: FEM Simulation Impedance of the Double Shear Actuator Bonded to a 1 mm Thick Aluminum Plate
Figure C.13: Schematic Poling and Electrodes and Mesh FEM Model of Shear Actuator and Aluminum Plate
Figure C.14: XY Shear Stress Results on a 167.6 x 50.8 x 1 mm Aluminum Plate and Accreted Ice Interface under the Effects of a Shear Actuator (500 V_{p-p}, 60 KHz)
Figure C.15: Interface Transverse Shear Stresses between a 1.27 mm Thick Ice Layer and a 167.6 x 15.2 x 1 mm Aluminum Plate Actuated by a Shear Actuator (500 V_{p-p}, 60 KHz)
C.3.2 Shear Segment Experimental vs. Predicted Impedance

The shear segment FEM analysis was validated by comparing the experimental impedance of a 101.6 mm long piezoelectric segment to those results obtained from the finite element model. The results are illustrated in Figure C.16. The calculated ultrasonic resonance frequency of the shear segment agrees within 1.2% of the impedance low peak measure experimentally. The experimental impedance value at this resonance frequency disagrees with predictions by 15%. The observed discrepancies between the FEM impedance predictions and the experimental results for the shear patches illustrated in Figure C.16 are believed to be due to defects introduced during the fabrication process. Uneven gold electrode surfaces, variable electric fields during the poling process and irregular silver epoxy glue at the connection between the wiring and the electrodes introduces changes in the systems impedance.

Figure C.16: Experimental and Predicted Impedance for a 101.6 x 7.62 x 3.175 mm PZT-5A Shear Segment
The ability of the finite element model to capture the trend of the impedance of the shear patch validates the analysis, as well as the proper poling of the segment.

C.3.3 Double Shear Actuator Experimental vs. Predicted Impedance

Two shear patches were bonded together with poling directions opposite to each other to obtain a net shear force and displacement twice that of one actuator (Figure C.17). The actuator was attached to a 167.6 x 50.8 x 1 mm aluminum plate.

The theoretical impedance of the system also matches the trend of the experimental impedance results, depicted in Figure C.18. The predicted impedance low frequencies agree with experimental impedance low frequencies with an error of less than 3%. The predicted impedance value at these impedance low frequencies present errors of up to 40% when compared to experimental values.

Figure C.17: Photo and Schematic of Double Shear Patch Actuator
The large discrepancies observed in Figure C.18 between the experimental and the predicted impedance of the shear actuator-aluminum plate system are believed to be attributed to losses introduced by the adhesive layer between the actuators (not modeled), and uneven poling of the segments.

C.4 Experimental Ice Tests $d_{15}$ Shear Segment Actuators

Experimental de-icing results obtained for $d_{15}$ shear actuators are presented. Due to the high impedance low value, $Z_m$, of the shear resonance mode this mode could not be driven efficiently. Impedance matching between the source and the actuator was not possible. The predicted transverse interface shear stresses able to instantaneously delaminate accreted ice were not reached. Melting of the ice interface was observed, but

![Figure C.18: Experimental and Theoretical Impedance of Double Shear Patch Actuator](image)
time delays between actuator turn on and ice de-bonding suggests fatigue and thermal effects as a major source for interface de-icing.

The effects of the shear ultrasonic waves on ice adhesion were studied for different amplitudes and frequencies of vibration. A 1.5 mm thick aluminum plate was driven by two 101.6 x 7.62 x 3.175 mm piezoelectric shear segments at ultrasonic frequencies (Figure C.20).

A 2.54 mm thick (12.9 cm², 2 inch² surface) ice layer was allowed to accrete to the aluminum plate at a temperature of negative 20°C. The shear actuators generated standing shear waves on the plate. Shear forces were applied to the thin layer of ice 95 seconds after the actuator was turned on. The shear force required to de-bond the ice layer was recorded for each different frequency and amplitude. A schematic of the experiment is shown in Figure C.19. As ultrasonic shear vibration frequency approaches the resonance frequency of the shear patches–aluminum system, the adhesion strength of the accreted ice decreased (Figure C.21).

![Figure C.19: Schematic Ice Bonding Strength under Ultrasonic Shear Vibration](image)

At this frequency, maximum stresses at generated at the ice interface for a given input voltage. The results showed that shear adhesion strength reduces to zero as the ultrasonic vibration frequency approaches 130 KHz, 450 V<sub>p-p</sub>. 
The admittance of the actuator–plate system was calculated using the finite element analysis. A structure resonance occurs at 134 KHz, differing by 3% from the optimum de-icing frequency measured during experimental results (Figure C.22).

Figure C.20: Aluminum Plate (1.5 mm Thick) Actuated by Two Shear Patches. Surface Particles Show Shear Pattern at Different Frequencies

Figure C.21: Experimental Adhesion Failure vs. Actuator Operational Frequency (450 V_{p-p})

The admittance of the actuator–plate system was calculated using the finite element analysis. A structure resonance occurs at 134 KHz, differing by 3% from the optimum de-icing frequency measured during experimental results (Figure C.22).
When the piezoelectric actuators were driven at the resonance frequency of the system (130 KHz, 450V amplitude, 100 Watts) for a period of 3 minutes, a 1.5 mm ice layer was completely melted up to 3.8 cm away from the shear actuator location at an ambient temperature of negative 20°C (Figure C.23). At the shear resonance frequency of the actuator is excited, transverse shear stresses of up to 38 MPa were predicted in the actuator vicinity, reaching values of up to 4 MPa 3.8 cm away from the actuator location. Positions exceeding 3.8 cm away from the actuator, where surface shear stresses were predicted to be insufficient to affect ice bonding, did not de-ice in less than 3 minutes.

An optical microscope of x10 amplification was used to capture the effect of ultrasonic waves (Figure C.24). As ultrasonic waves were applied to the aluminum–ice structure at 130 KHz, 450 V$_{p-p}$, instantaneous cracks and melting around the cracks was observed at temperatures under negative 15°C. No significant thermal activity was measured by a thermocouple on the actuators region when instantaneous microscopic cracking occurred. On the other hand, temperatures over 30°C were recorded on the
actuator region 100 seconds after the actuator was turned on. Similar temperatures were recorded 350 seconds later on the overall plate’s surface.

Figure C.23: Shear Stresses on a 1.5 mm Thick Aluminum Plate (134 KHz, 450 V_p-p)
A second actuator formed by five shear patches made out of PZT-5 of width 7.62 mm and separated 7.62 mm from each other were placed on a 1 mm thick aluminum plate. The segments were 50.8 mm long each.

Ice adhesion tests were performed to measure the effects of the ultrasonic shear vibration on ice bonding. Different vibration amplitudes were studied. The shear adhesion strength of ice patches of controllable size were quantified with and without ultrasonic vibration. Adhesion failure of accreted ice without vibration occurred at a shear stress of 0.35 MPa. The ultrasonic vibration weakened the ice bonding at frequencies surrounding the resonance of the actuator. Only a vibration of 130 KHz effectively affected the ice–aluminum interface, making the ice adhesion fail 45 seconds after the vibration started (450 V<sub>p-p</sub>, 30 KV/m). When 450 V<sub>p-p</sub> were applied to the actuator microscopic cracking was also observed along the entire plate length. Well formed ice patches melted approximately 100 seconds after the actuator was turned on, as it is illustrated in Figure C.27. Ice patches located up to 17.7 cm away (7 inches) from the
vibration source, which corresponds to 80% of the overall plate surface, were de-bonded (Figure C.25).

A finite element analysis of the structure was performed to analytically verify the experimentally observed resonance frequency of the shear actuator, and to quantify the generated shear stresses on the system. The poling and electrode configuration of the system is illustrated in Figure C.26.

When driven at the shear resonance of the actuator (134 KHz), transverse shear stresses on a 2.5 mm thick ice layer were predicted to be up to 5 MPa, sufficient to affect the ice bonding. The theoretical resonance of the system matches the one measured experimentally with an error of 3%. As compared to the local effects presented in the
prior case (Figure C.23), where the interface cracks were localized around the actuator, this configuration de-bonds ice patches up to 18 cm away from the actuation source. In the local de-icing case, the excited plate was not a wavelength multiple of the ultrasonic vibration, allowing for destructive interference to occur at the boundaries of the plate (Figure C.23).

Even though the presented de-icing results validated ultrasonic effects on ice bonding, predicted instantaneous ice de-bonding due to large transverse shear stresses was not observed. The lack of ice de-bonding is attributed to the mismatch on impedance between the source and the actuator. The actuators were not be driven efficiently, and much of the input power was reflected back to the amplifier. In addition, thermal activity due to mechanical and dielectric losses on the actuator were measured seconds after the system was turned on. Temperatures on the plate surface reached positive values after 30 seconds. Although the experiments were promising due to the observed instantaneous ice interface microscopic cracking, these proof-of-concept experiments were marginal. The results did not present a clear validation of the conceptual high interface stress de-icing ultrasonic concept since clear thermal effects were recorded.
C.5 Shear Segment Actuator Thermal Analysis

For the PZT-5 shear segments modeled, the electric field is applied along the width of the actuators. The 15.2 mm wide actuator formed by two 50.8 mm long, 7.62 mm wide segments is used to validate the presented thermal analysis. From the impedance of the actuator (Figure C.18) and the input voltage, the power dissipation can

Figure C.27: FEM XY Shear Stress Results for the De-icing Actuator – Aluminum Plate System (450 V<sub>p-p</sub>, 134 KHz)
be calculated. The 7.62 mm wide shear segments create a theoretical steady state temperature of up to $84^0$ C when driven at their resonance frequency (57.5 KHz) and 450V. Experimental tests showed a steady state temperature of $81^0$ C on the shear segment surface after 35 minutes of operation at its resonance frequency. At these same driving conditions, temperatures above freezing were reached on the surface of the actuator 28 seconds after being turned on. The observed results and theoretical prediction show that thermal effects due to dielectric and mechanical losses play a key role in ice interface melting as heat propagation travels on the host structure.

The experimental and theoretical actuator temperatures for different frequencies (and correspondent impedances) are listed in Table C-2. During these theory validation experiments, the input voltage to the actuator was 200 V to avoid thermal degradation of the segments. The actuators remained turned on for 4 hours. The temperature of the segments was recorded and compared to theoretical predictions.

The observed melting effects occurring after the actuator was turned on (100 – 200 sec. later) are consistent with results presented in literature, and they are attributed to mechanical and dielectric losses on the piezoelectric material. During the above presented shear actuator experiments, heat propagation has a major effect on the melting of the ice interface when the piezoelectric actuator is driven continuously.

Table C-2: Experimental and Theoretical Temperatures for a 5 x 1.5 x 0.3 cm Shear Actuator Attached to a 0.7 mm Thick Aluminum Plate (200 V)

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<th>Frequency</th>
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<th>Theo. Temp.</th>
<th>Exp. Temp.</th>
<th>% Error</th>
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</thead>
<tbody>
<tr>
<td>20 KHz</td>
<td>8,000 Ohms</td>
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<td>-16.0$^0$ C</td>
<td>6.8%</td>
</tr>
<tr>
<td>45.2 KHz</td>
<td>2,210 Ohms</td>
<td>-9.52$^0$ C</td>
<td>-8.8$^0$ C</td>
<td>8.18%</td>
</tr>
<tr>
<td>57.5 KHz</td>
<td>1,000 Ohms</td>
<td>3.15$^0$ C</td>
<td>3.6$^0$ C</td>
<td>14.28%</td>
</tr>
</tbody>
</table>
C.6 Shear Segment Impedance Matching

Impedance matching was not possible for the low impedance resonance points of the shear actuator used during experiments, since these frequencies presented higher impedance values than the maximum allowed impedance of the amplifier, as it is shown in Figure C.28. Much of the power sent to the actuator was reflected back to the amplifier (Figure C.29). The power that was transformed into mechanical vibration did not generate large enough stresses to delaminate the accreted ice, but was sufficient to create microscopic cracking on the ice, as it is presented in Figure C.24.

![Graph showing experimental and theoretical impedance comparison](Image)

**Figure C.28: Experimental and Theoretical Impedance of Double Shear Patch Actuator**
Figure C.29: Forward and Reversed Power without Impedance Matching (AR800A Amplifier Display Window)
VITA

JOSE LUIS PALACIOS

EDUCATION

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  • 62nd AHS Annual Forum Nominee for "Best Paper Award"

SELECTED PUBLICATIONS AND PRESENTATIONS
  • Palacios, J., Smith, E., “Ultrasonic Shear Wave Actuator for Helicopter Rotor Blades” SAE Aircraft & Engine Icing International Conference Seville, Spain, September 2007