

Icing Performance Degradation of an eVTOL Propeller at the NASA Glenn Icing Research Tunnel

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In March 2023, a 10-day test entry was conducted at the NASA Glenn Icing Research Tunnel (IRT) to study ice accretion on an eVTOL propeller. The primary objective was to collect ice accretion data to support the development of NASA's latest ice accretion solver, GlennICE. Various parametric sweeps of cloud and operating conditions were performed to evaluate the sensitivity of the parameters on the resulting ice accretion, the results of which have been published previously. A subsequent 5-day test entry was carried out in January 2024, focusing on gathering experimental data related to ice shedding and exploring conditional icing scaling methods for propellers. Throughout both test campaigns, propeller performance data was recorded in the form of torque rise over time. The purpose of this paper is to present a subset of the performance data acquired during these tests and provide a discussion of the key findings.

I. Nomenclature

A_c	=	accumulation parameter
C_p	=	power coefficient
C_T	=	thrust coefficient
C_Q	=	torque coefficient
c	=	airfoil chord
D	=	propeller diameter
J	=	advance ratio
N_b	=	number of blades
n	=	freezing fraction
n_c	=	corrected freezing fraction
n_0	=	freezing fraction at stagnation
n_0^*	=	freezing potential at stagnation
P	=	propeller power
Q	=	propeller torque
R	=	propeller tip radius
r	=	radial distance
T	=	propeller thrust
$t_{\infty,0}$	=	freestream total air temperature
V_{ax}	=	axial component of air velocity
V_{eff}	=	effective air velocity
V_{rot}	=	rotational component of air velocity
V_{∞}	=	freestream air velocity
We_L	=	Weber number based on length L

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β	= collection efficiency
β_0	= collection efficiency at stagnation
δ	= drop median volumetric diameter (MVD)
η	= propeller efficiency
ρ_∞	= freestream air density
σ	= propeller solidity
τ	= icing time
ω	= propeller rotational speed, rad/s

II. Introduction

A wide range of electric vertical takeoff and landing (eVTOL) aircraft concepts have been proposed for Advanced Air Mobility (AAM) operations due to their potential to have lower noise, reduced operating costs, and decreased emissions when compared to conventional VTOL aircraft, such as helicopters. Despite the diversity in eVTOL designs, there are certain characteristics which are shared by many. For example, eVTOL concepts typically have distributed electric propulsion systems where thrust generation is distributed across several electrically powered rotors. To reduce their acoustic signature, eVTOL rotors generally operate at much slower tip speeds and have higher solidities than conventional helicopter rotors [1]. In addition, many eVTOL concepts have vectored thrust capabilities where their rotors act as propellers during the cruise phase of flight. An example of an eVTOL aircraft with vectored thrust capabilities is the NASA tiltwing reference vehicle shown in Fig. 1 [2].

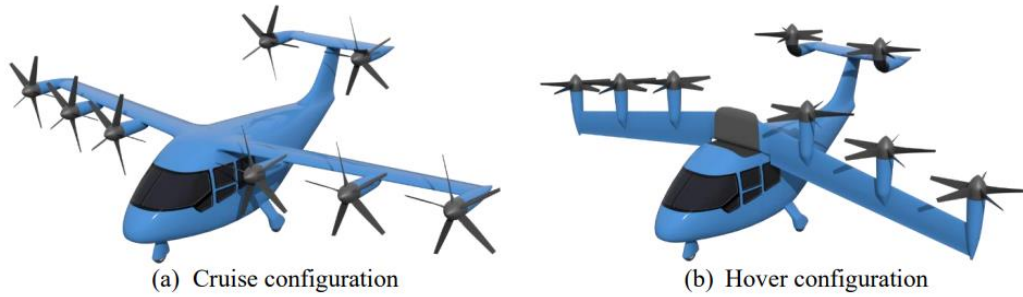


Fig. 1 Renderings of NASA's Tiltwing Reference Vehicle in a Cruise (a) and Hover (b) Configuration

As with all aircraft, icing presents a significant challenge to the safe operation of eVTOL aircraft. Ice can form on lifting surfaces and propulsors which leads to a reduction in lift and thrust, and an increase in drag and stall speed of the vehicle. However, due to the novelty of eVTOL aircraft, the scope and severity of these effects is not as well understood as for conventional aircraft. In addition, eVTOL aircraft presents several unique challenges. For example, the large number of rotors and limited power capacity of eVTOL aircraft prohibits the use of conventional ice protection systems (IPS). As a result, most eVTOL manufacturers are currently not seeking certification for flight into known icing. However, they will still likely be required to demonstrate safe operation in icing for a limited duration in the event of an inadvertent icing encounter. Thus, the ability to accurately predict and understand the impact of icing on their performance is still of great importance.

To investigate the icing phenomenon relevant to eVTOL aircraft, NASA Glenn Research Center developed a general-purpose propeller test stand for conducting fundamental icing research on electrically driven propellers in the Icing Research Tunnel (IRT). A 10-day test entry in the IRT was completed in March 2023, with the goal of generating ice accretions on a non-proprietary propeller geometry under well-characterized conditions [3]. The data from this test is currently being used to support the development of NASA's latest ice accretion solver, GlennICE [4-6]. Three carbon fiber propellers of diameters 0.610, 0.711, and 0.914 m (24, 28, and 36 in) were utilized during the test. A follow-on test lasting 5 days was later completed in January 2024, which focused on gathering experimental ice shedding data as well as evaluating conditional icing scaling techniques for propellers [7,8]. Propeller performance degradation in the form of torque change over time was captured during both test entries. This paper provides an analysis and discussion of a subset of the propeller performance data that was acquired during these tests.

III. Test Article and Instrumentation

A. Propeller Test Rig

A picture of the propeller test rig installed in the IRT test section with the 0.914 m (36 in) diameter propeller is shown in Fig. 2. The propeller is driven by an EMRAX 188 permanent magnet synchronous motor with a continuous power rating of up to 34 kW (45 hp) and a continuous torque rating of up to 52 Nm (38 lbf-ft). The motor has an IP65 ingress protection rating and is liquid cooled to prevent overheating. The propeller hub is mounted directly to the motor via an adapter. To measure propeller forces and moments, the propeller rig is compatible with any of the Interface 6A68 6-axis load cells which can be mounted directly aft of the motor as shown in Fig. 3. Only the Interface 6A68C load cell was available during the 2023 test. However, the 6A68C load cell was only used during the dry (non-icing) portion of the test to acquire baseline propeller performance data due to concerns that the imbalance loads caused by ice shedding could exceed the structural strength of the load cell. The higher capacity, 6A68E load cell was later obtained and used throughout the 2024 test entry to measure forces and moments during the icing encounter. The motor speed is controlled by sending commands to a Cascadia Motion CM200DX Inverter. The inverter also provides estimates of motor power, temperature, and torque.

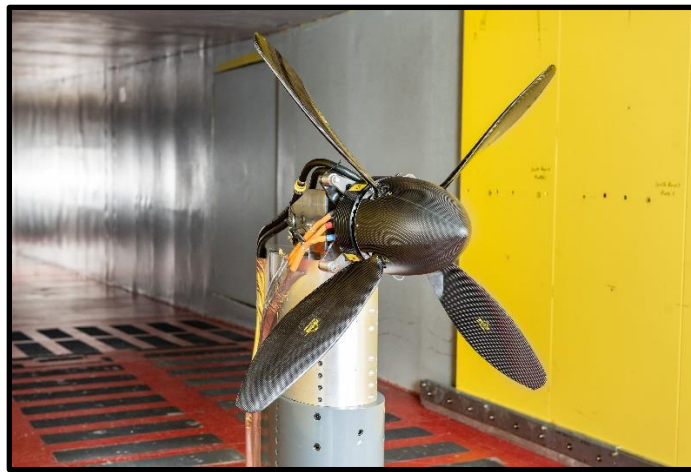


Fig. 2 NASA's eVTOL Propeller Test Stand for Fundamental Icing Studies

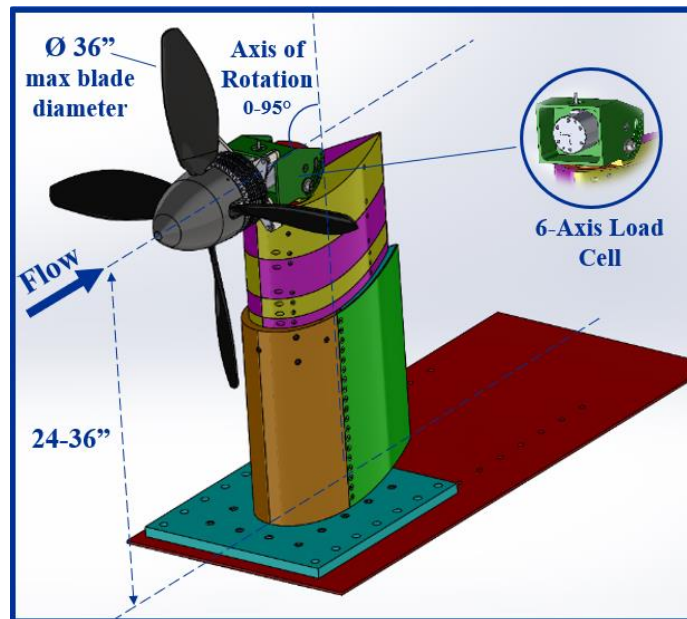


Fig. 3 CAD Model of the Propeller Test Stand Showing Location of Load Cell

B. Propeller Geometry

Three carbon fiber propellers of diameters 0.610, 0.711, and 0.914 m (24, 28, and 36 in) were custom-made using a non-proprietary propeller geometry to maximize data dissemination. All three propellers had a NACA 0012 airfoil profile. The twist and chord distributions for the three propellers are shown in Fig. 4. The propeller geometry was inspired by the Computationally Optimized Proprotor (COPR) design developed by NASA [9]. However, some deviations from the original COPR design were required for manufacturing feasibility purposes. The twist and chord distributions provided in Fig. 4 represent the final design after the necessary modifications were made.

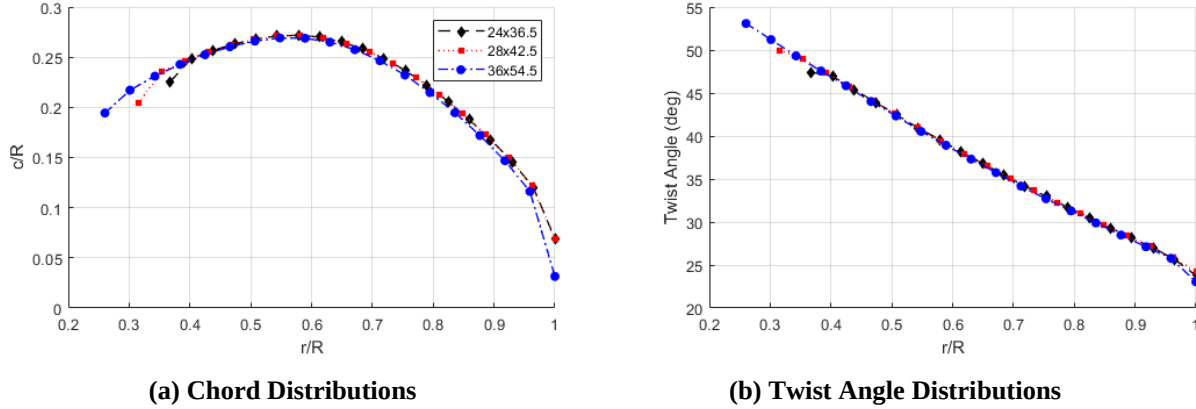


Fig. 4 Comparisons of the propeller chord (a) and twist angle (b) distributions between the three propeller sizes.

It can be seen from Fig. 4 that the three propellers have similar normalized twist and chord distributions from a radial location of approximately $r/R = 0.40$ to $r/R = 1.0$. However, the use of the same hub and spinner for each set of propeller blades prevented geometric similarity towards the root of the blade. In the complimentary paper by Rigby and von Hardenberg, the effect of not geometrically scaling the spinner with propeller size was explored using GlennICE and found to have a negligible effect on the predicted ice accretion [6].

A 4-bladed, variable pitch hub was chosen for this test entry to provide greater flexibility in propeller operating conditions. A pitch-adjustment tool was provided by the propeller manufacturer to change the collective pitch of the blades. For both test entries, the target propeller pitch was 18° higher than the design twist distributions shown in Fig. 4b. After evaluating the laser scans of the propeller blades obtained during the test, it was determined that the blade pitch angle could deviate as much as one degree from the target pitch setting. Future tests will utilize a calibrated inclinometer to provide more consistent pitch settings.

IV. Test Procedure

A. Spinner Drag Characterization

To accurately determine the thrust generated by the propeller from the load cell measurements, it is important to account for additional forces that may influence the net load measured by the load cell. In this case, the drag force acting on the spinner was one such force that contributed to the net axial load measured by the load cell. To account for this, the spinner drag was characterized by conducting a sweep of tunnel speed with the spinner installed on the test stand without the propeller blades. During the tunnel speed sweep, the motor was exercised at a speed of 1000 RPM. Once the drag on the spinner was characterized, the thrust produced by the propeller could be determined by subtracting the spinner drag force from the net axial load measured by the load cell. The results of the spinner drag characterization are shown in Fig. 5.

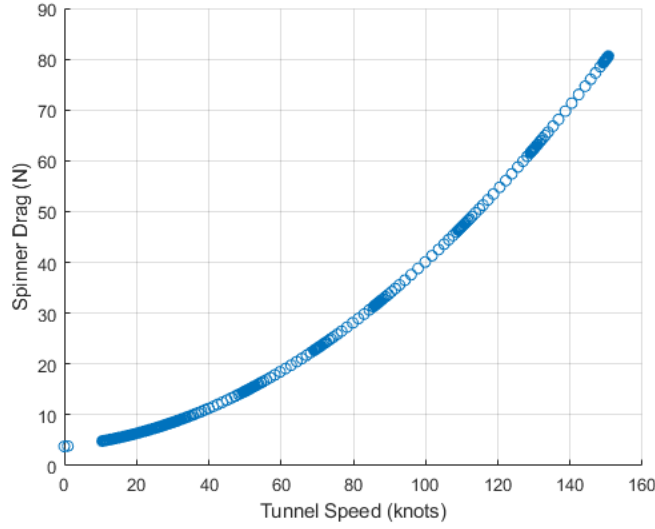


Fig. 5 Measured Spinner Drag vs Tunnel Speed

B. Clean (no ice) Performance Characterization

The first three days of the 2023 test were devoted to characterizing the clean performance of the propellers. The propeller performance is defined using the standard nomenclature for propellers. The non-dimensional coefficients of propeller thrust, power, and efficiency are defined as:

$$C_T = \frac{4\pi^2 T}{\rho_\infty \omega^2 D^4} \quad (1)$$

$$C_P = \frac{8\pi^3 P}{\rho_\infty \omega^3 D^5} \quad (2)$$

$$\eta = \frac{C_T}{C_P} J \quad (3)$$

Where T is the propeller thrust, P is the propeller power, ρ_∞ is the freestream air density, ω is the rotational speed of the propeller in rad/s, D is the diameter of the propeller, and J is the advance ratio which is defined as:

$$J = \frac{2\pi V_\infty}{\omega D} \quad (4)$$

Where V_∞ is the freestream velocity. Note that the relationship between propeller power and propeller torque is defined as:

$$P = \omega Q \quad (5)$$

Where Q is the dimensional propeller torque. Using the relationship defined in Eq. 5, the non-dimensional torque coefficient can be defined as:

$$C_Q = \frac{4\pi^2 Q}{\rho_\infty \omega^2 D^5} = \frac{C_P}{2\pi} \quad (6)$$

To account for varying blade numbers and planforms, the propeller performance coefficients are often divided by the propeller solidity, σ . In its simplest form, the propeller solidity represents the ratio of the total blade area to the swept area of the propeller, as defined in Eq. 7.

$$\sigma = \int_0^1 \frac{N_b c(r/R)}{\pi R} d(r/R) \quad (7)$$

The solidities for the 24, 28, and 36-inch diameter propellers are provided in Table 1. The slight discrepancies in the solidities are a result of using a propeller hub of a fixed diameter for each propeller, which effectively reduces the relative blade area of the smaller diameter propellers.

Table 1 Solidities of the 24, 28, and 36-inch Diameter Propellers

Propeller Diameter	Solidity, σ
24"	0.1875
28"	0.2016
36"	0.2151

When characterizing the aerodynamic performance of a propeller, values of C_T , C_P (or C_Q), and η are determined and plotted over a range of advance ratios. During the 2023 test, the non-dimensional coefficients were obtained by performing a sweep of advance ratios at a fixed propeller RPM. This was done by operating the propeller at some nominal RPM and tunnel speed that resulted in a near-windmilling state (i.e., when the required motor torque was at or near zero), and then gradually decreasing the tunnel speed while maintaining a constant propeller RPM to perform a sweep of advance ratios. It was decided to conduct the advance ratio sweep at a fixed RPM (as opposed to a fixed tunnel speed) to avoid potentially operating the propeller at a resonant frequency of the test stand. In addition, the RPM of each propeller was chosen to be close to the RPMs that were expected to be tested during the icing portion of the test. The propeller performance charts for the 0.9144 m (36 in) diameter propeller are shown in Fig 6. Figure 6 also includes comparisons of the experimental performance data with Blade Element Momentum Theory (BEMT) predictions using XROTOR.

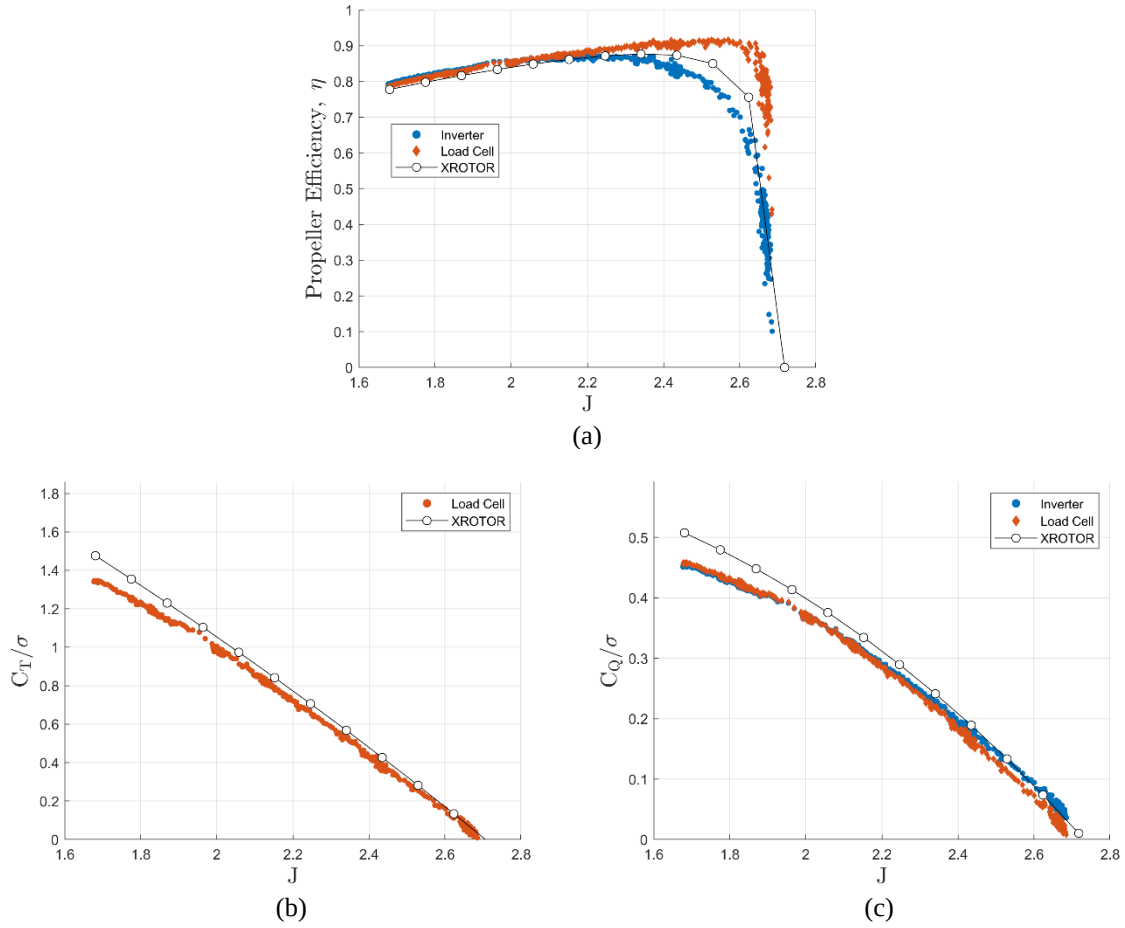


Fig. 6 Experimental Performance Curves for the 36-inch Diameter Propeller with Comparisons to XROTOR Predictions

Recall from Section III that an estimate of the propeller torque could be acquired from two independent sources: 1) from the load cell measured moment about the propeller axis of rotation and 2) from the inverter estimate of motor torque based on the motor rotational speed and power. As such, the propeller performance charts include the calculated torque coefficient and resulting efficiency using both sources of propeller torque. As can be seen from Fig. 6c, the torque measured by the load cell is very similar to the torque estimate provided by the inverter, albeit slightly lower at the higher advance ratios. Given the general good agreement between the load cell and inverter estimate of motor torque, it was decided to utilize the inverter estimate of torque for the remainder of testing. This approach offers a significant advantage over using the load cell, as the inverter estimate of motor torque is not affected by thermal drift, creep, and hysteresis. As a result, the inverter estimate of torque was found to be much more consistent and repeatable than the torque measured by the load cell. In addition, significant time was saved by not having to thermal soak the load cell prior to each run. As such, only the inverter estimate of motor torque is presented herein.

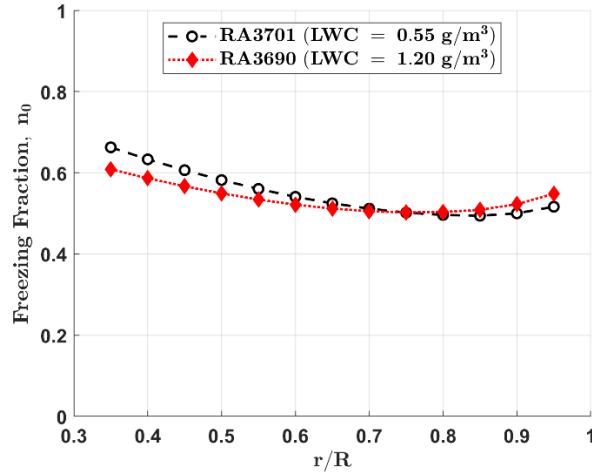
C. Icing Similarly Parameters

To facilitate the interpretation of the results, the key icing similarity parameters (n , β , A_c , and We_L) have been calculated at $r/R = 0.75$ at stagnation and provided for each condition. Details on how these parameters were calculated can be found in Ref. 3 and 8. A list of the key icing similarity parameters and their descriptions can be found in Table 2.

Table 2 Description of Important Icing Similarity Parameters

Parameter	Variable	Description
Freezing Fraction	n	Fraction of water mass flux entering a control volume that freezes within the control volume.
Collection Efficiency	β	Fraction of the freestream droplet concentration that impinges on a surface.
Accumulation Parameter	A_c	Theoretical maximum leading edge ice thickness (assuming n and $\beta = 1$) normalized by the airfoil leading edge nose diameter of curvature.
Weber's Number	We_L	Ratio of the inertial forces acting on the surface of liquid water to its surface tension.

While the icing similarity parameters varied radially along the propeller due to a combination of changing blade chord and effective velocity, only the values calculated at $r/R = 0.75$ will be provided for simplicity. In addition, for the conditions tested in which the values of n_0 , β_0 , A_c , and/or We_L matched at $r/R = 0.75$, it was found that there was generally good agreement of those parameters at other radial locations as well. Figure 7 provides an example distribution of n_0 along the propeller for runs RA3701 and RA3690 for which the freestream total temperature was adjusted to match n_0 at $r/R = 0.75$ to compensate for a difference in Liquid Water Content (LWC). While n_0 only matches at $r/R = 0.75$, the differences in n_0 at other radial locations are minor. However, it is important to note that matching these parameters at $r/R = 0.75$ will not always result in good agreement at other radial locations and that this is simply an observation of the current conditions tested and presented herein.

**Fig. 7 Example Freezing Fraction vs Span Distributions for Runs RA3701 and RA3690**

B. Icing Performance Characterization

During a typical icing run, the propeller was operated at the target RPM and tunnel speed for several minutes until the freestream total air temperature, $t_{\infty,0}$, reached a steady state at the target temperature for that run. After achieving steady state conditions, the propeller was operated for 30 seconds before introducing the icing cloud to acquire baseline performance data. The icing spray would continue for the target duration of that run or until a major shedding event resulted in vibrations that exceeded their predefined limit. If a major shedding event occurred, the tunnel operators would immediately turn off the cloud and begin the shutdown procedure. A linear curve fit was then applied to the performance data from spray-on until spray-off, or until the 1st shedding event (if applicable), whichever occurred first. Figure 8 shows a typical time history plot showing the rise in propeller torque for a run which includes a shedding event around the 7-minute mark.

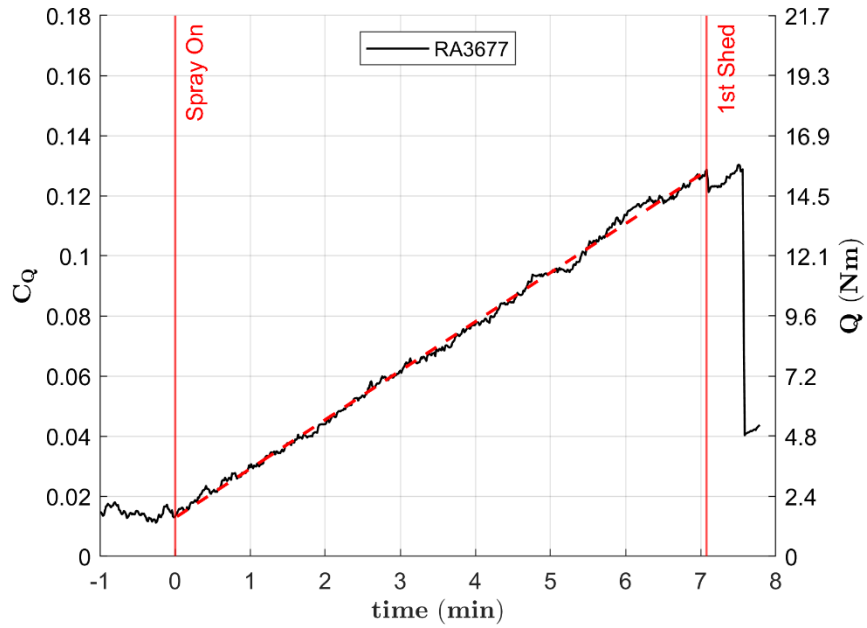


Fig. 8 Example Torque vs Time Plot for Run RA3677

V. Test Data

A. Repeatability

Several repeat cases were performed to understand what level of variation could be expected in the propeller torque measurements due to normal run to run variations in the cloud conditions. Figure 9 shows the propeller torque requirements as a function of time for three runs with the same target cloud and operating conditions. Each case was run indefinitely until a shedding event occurred. As seen from Fig. 9, excellent repeatability in the propeller torque measurements was achieved. Several other repeats were performed at various other tunnel and propeller operating conditions which showed similar levels of repeatability.

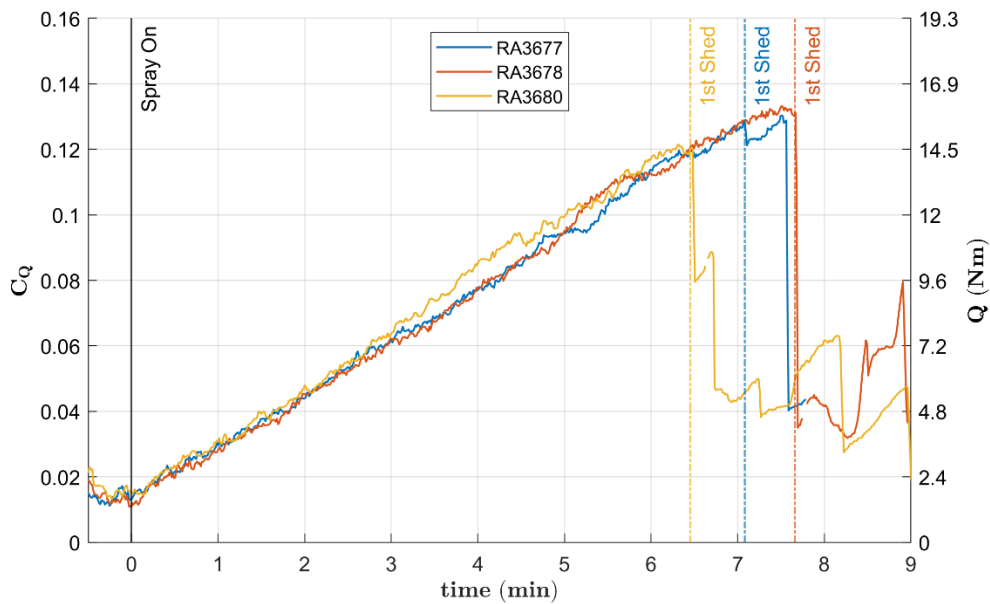


Fig. 9 Repeatability of Propeller Torque Measurements in Icing

Table 3 Target Test Conditions for Fig. 9

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3677	87	-6.0	15	1.20	7.08 [†]	1394	28	0.20	2.71	0.53	12.85	0.88	0.14
RA3678	“	“	“	“	7.66 [†]	“	“	“	“	“	13.90	“	“
RA3680	“	“	“	“	6.45 [†]	“	“	“	“	“	11.71	“	“

[†] Time until first shedding event

B. Effect of LWC

Figure 10a shows the effect of increasing the Liquid Water Content (LWC) from 0.55 to 1.20 g/m³ on the propeller torque for runs RA3683 and RA3684, respectively. The spray time for the 1.20 g/m³ run was reduced to match the accumulation parameter, A_c , of the 0.55 g/m³ condition. All other conditions were identical as shown in Table 4. Based on the analytical calculations of the freezing potential, n_0^* , it was believed that the temperature was sufficiently cold to produce rime ice along the entire span of the propeller for both the 0.55 and 1.20 g/m³ condition. However, analysis of the ice shapes following the test revealed that the freezing fractions for the 1.20 g/m³ case were slightly less than one. Based on this observation, and the results of subsequent temperature sweeps, it is believed that the freezing fractions achieved during testing were approximately 20% lower than the analytically predicted freezing potentials reported in Table 4. As a result, the freezing fraction for run RA3684 may have been closer to $n_c = 1.19 \cdot 0.8 = 0.95$ at $r/R = 0.75$ where n_c is the corrected freezing fraction. The reason for the large discrepancy between the experimentally achieved freezing fractions and the analytical freezing potentials is still being investigated. Nonetheless, a comparison of the propeller torque requirements between these two conditions is presented below.

As expected, the 1.20 g/m³ condition led to a more rapid increase in torque than the 0.55 g/m³ condition due to the faster rate of ice accretion on the propeller. However, by plotting the change in torque as a function of the product of the stagnation collection efficiency and accumulation parameter ($\beta_0 A_c$) as calculated at $r/R = 0.75$, comparisons of the propeller torque can be made at similar stagnation ice thicknesses. This result is shown in Fig. 10b. Note that, in Fig. 10b, the propeller torque is presented in non-dimensional form as the torque coefficient divided by solidity (C_Q/σ). As seen from Fig. 10b, while the change in C_Q/σ for the 0.55 and 1.20 g/m³ LWC conditions are similar, the 1.20 g/m³ condition still shows a slightly higher C_Q/σ for a given $\beta_0 A_c$. This is likely due to the 1.20 g/m³ condition having a freezing fraction slightly less than one as explained in the previous paragraph, resulting in more glaze-like features along the stagnation line of the propeller. It is likely that even better agreement could have been achieved had the temperature for the 1.20 g/m³ condition been reduced further to achieve a freezing fraction of one. The ice shapes obtained at the $r/R = 0.75$ location for these runs are provided in Fig. 10b. Additional ice shapes for runs RA3683 and RA3684 at other radial locations are presented in Ref. 3.

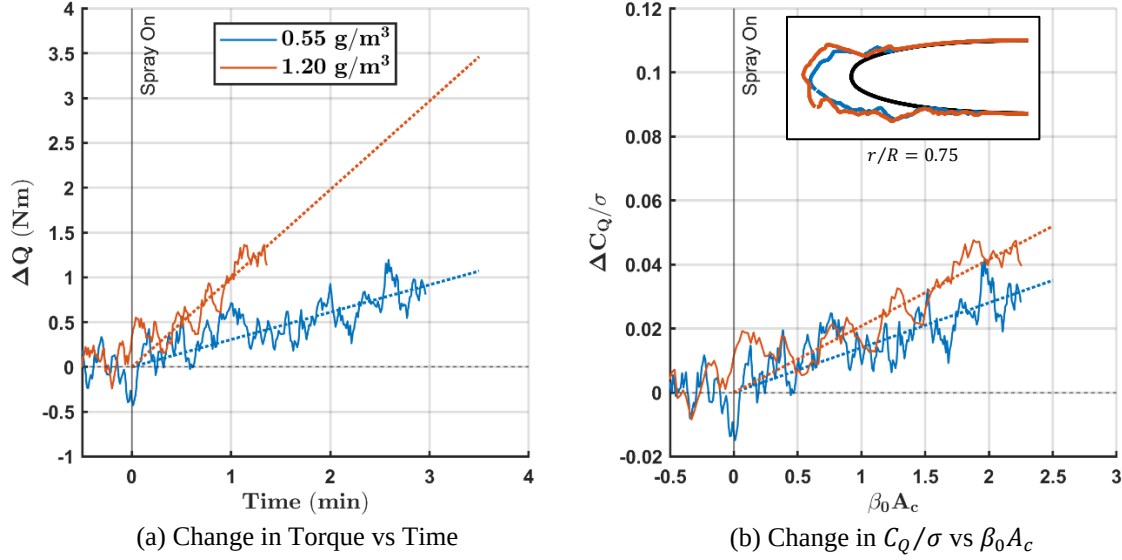


Fig. 10 Effect of LWC on Propeller Torque Requirements ($n_0 = 1$)

Table 4 Target Test Conditions for Fig. 10

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3683	87	-15.0	15	0.55	3.0	1516	28	0.21	2.49	2.37	2.59	0.88	0.15
RA3684	“	“	“	1.20	1.4	“	“	“	“	1.19	2.64	“	“

A similar analysis was conducted to explore the effect of LWC on the propeller torque requirements at glaze conditions ($n_0 < 1$). Figure 11a shows the effect of increasing the LWC from 0.55 to 1.20 g/m³ at a target freezing fraction of $n_0 = 0.51$ for runs RA3701 and RA3690, respectively. Both the spray time and temperature for the 1.20 g/m³ condition were reduced to match A_c and n_0 at $r/R = 0.75$, respectively. All other conditions were identical as shown in Table 5.

As expected, the propeller torque increased more rapidly for the 1.20 g/m³ condition. In addition, when expressed in non-dimensional form as a function of $\beta_0 A_c$, the propeller torque requirements are very similar for both LWC conditions as shown in Fig. 11b. The ice shapes at $r/R = 0.75$ are also provided in Fig. 11b, which shows that very similar ice shapes were obtained at these two conditions. Additional ice shapes for runs RA3701 and RA3690 at other radial locations are presented in Ref. 3.

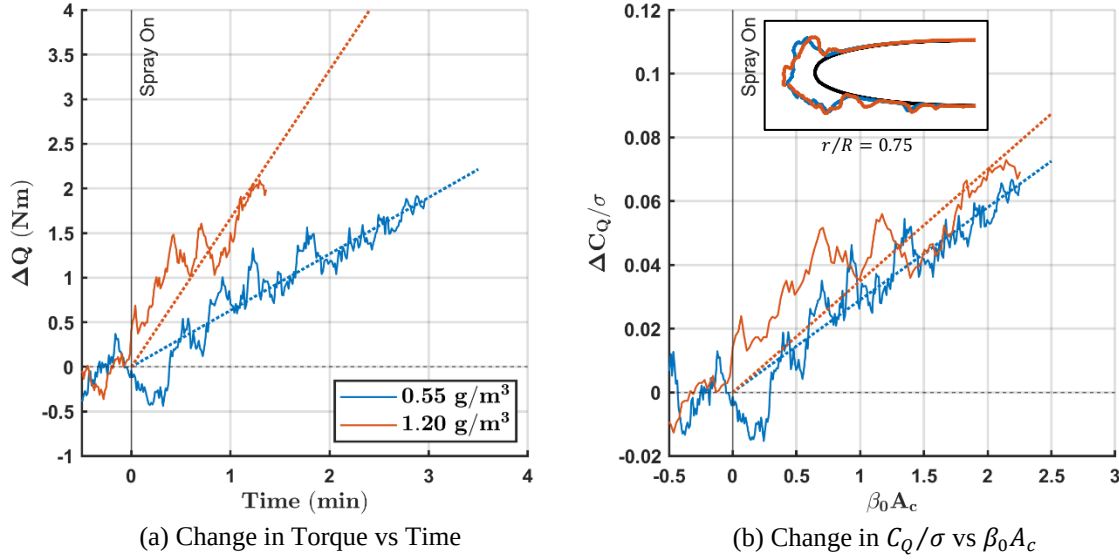


Fig. 11 Effect of LWC on Propeller Torque Requirements ($n_0 < 1$)

Table 5 Target Test Conditions for Fig. 11

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3701	87	-3.0	15	0.55	3.0	1516	28	0.21	2.49	0.51	2.59	0.88	0.15
RA3690	“	-6.0	“	1.20	1.4	“	“	“	“	“	2.64	“	“

The results of the preceding analysis suggest that the propeller torque requirements due to icing may largely be independent of LWC, provided that the distribution of n_0 and $\beta_0 A_c$ along the propeller remains the same or similar. It should be noted, though, that the preceding analysis was performed at the same median volumetric diameter (MVD) and, consequently, β_0 . As will be discussed in the next section, matching β_0 (in addition to the product $\beta_0 A_c$) may be important for iced propeller performance similarity due to the effect of β_0 on the impingement limits, and ultimately the aerodynamic performance, of the propeller.

C. Effect of MVD

Figure 12a shows the effect of increasing the MVD from 15 to 80 μm on the propeller torque for runs RA3711 and RA3712, respectively. All other conditions for runs RA3711 and RA3712 were identical as shown in Table 6. A freestream total temperature of -15°C was chosen to achieve a freezing fraction of $n_0 = 1$ for both conditions, which is consistent with experimental observations and analysis of the ice shapes. As a result of increasing the MVD from 15 to 80 μm, the β_0 at $r/R = 0.75$ increased from 0.88 to 0.99.

As seen from Fig. 12a, the higher MVD condition resulted in a slightly faster rise in propeller torque. This is likely due to the higher collection efficiency of the 80 μm case, which results in a faster rate of ice accumulation (from the product $\beta_0 A_c$) as well as increased impingement limits on the propeller. To make comparisons of the two MVD cases at similar stagnation ice thicknesses, the non-dimensional change in torque is plotted as a function of $\beta_0 A_c$, as shown in Fig. 12b. As can be seen from Fig. 12b, the higher MVD condition requires more torque even at similar values of $\beta_0 A_c$. The difference in C_Q/σ between the 15 and 80 μm MVD condition for a given $\beta_0 A_c$ can likely be attributed to the difference in impingement limits at these two conditions, as evidenced by the ice shapes at $r/R = 0.75$ provided in Fig. 12b. This effect is especially noticeable on the pressure side of the airfoil, where the main ice shape of the 80 μm condition extends significantly further aft on the airfoil. Additional ice shapes for runs RA3711 and RA3712 at other radial locations can be found in Fig. 20 in the Appendix.

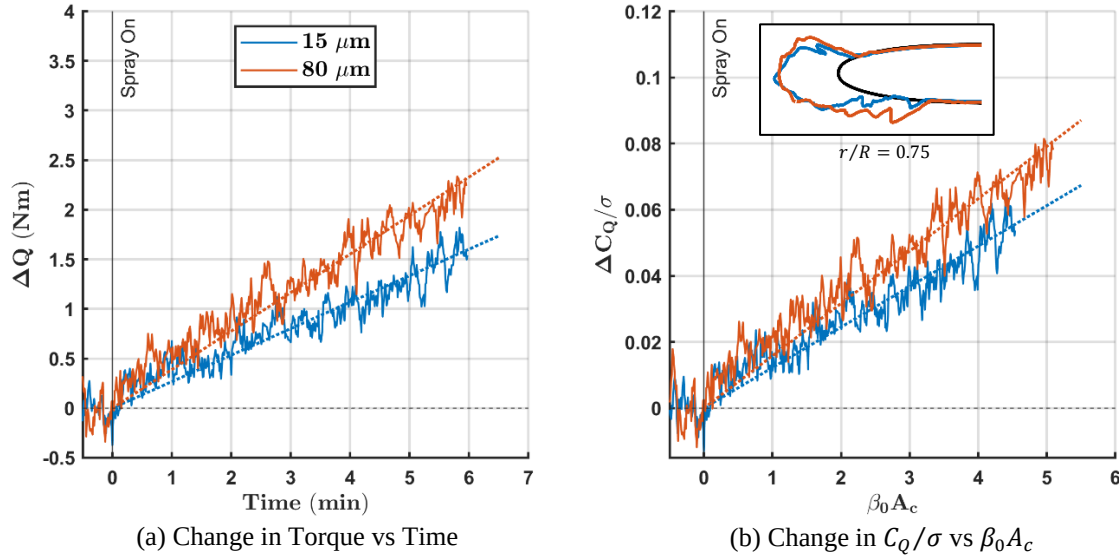


Fig. 12 Effect of MVD on Propeller Torque Requirements ($n_0 = 1$)

Table 6 Target Test Conditions for Fig. 12

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ $^\circ\text{C}$	δ μm	LWC g/m^3	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10^6
RA3711	87	-15.0	15	0.55	6.0	1516	28	0.21	2.49	2.37	5.18	0.88	0.15
RA3712	"	"	80	"	"	"	"	"	"	2.13	"	0.99	"

D. Effect of Temperature

The effect of air temperature on the propeller torque requirements were investigated at freestream total air temperatures ranging from -3 to -18°C . Air temperature is an important parameter in icing as it has a strong influence on the freezing fraction and, consequently, the resulting type and shape of the ice accretion. Figure 13a shows the change in propeller torque vs time at freestream total temperatures of -3 , -6 , -9 , -15 , and -18°C corresponding to runs RA3700, RA3692, RA3696, RA3684, and RA3697, respectively. Note that the spray times varied significantly during these runs in order to achieve other test objectives unrelated to propeller performance. Except for temperature and spray time, all other conditions were identical as shown in Table 7.

As seen from Fig. 13a, the propeller torque increases more rapidly as the total air temperature increases from -18 to -6°C . This is likely caused by the change in the ice morphology during the transition from rime to glaze ice. However, when the total air temperature increases further to -3°C , the trend reverses and the propeller torque requirements are significantly less than the preceding -6°C case. These results are summarized in Fig. 14 which presents the linear curve-fit slopes of the non-dimensional change in torque as a function of the corrected freezing fraction at $r/R = 0.75$. Recall that the corrected freezing fractions are 20% lower than the analytical freezing potentials provided in Table 7. Note that Fig. 14 includes repeat cases, and a 2nd order polynomial curve fit has been applied to the data through the averages of the repeats for a given freezing fraction. Due to the vastly different values of A_c , comparing the ice shapes is impractical and thus are omitted from Fig. 13b for these conditions. In addition, ice shapes are unavailable for some of the conditions due to ice shedding.

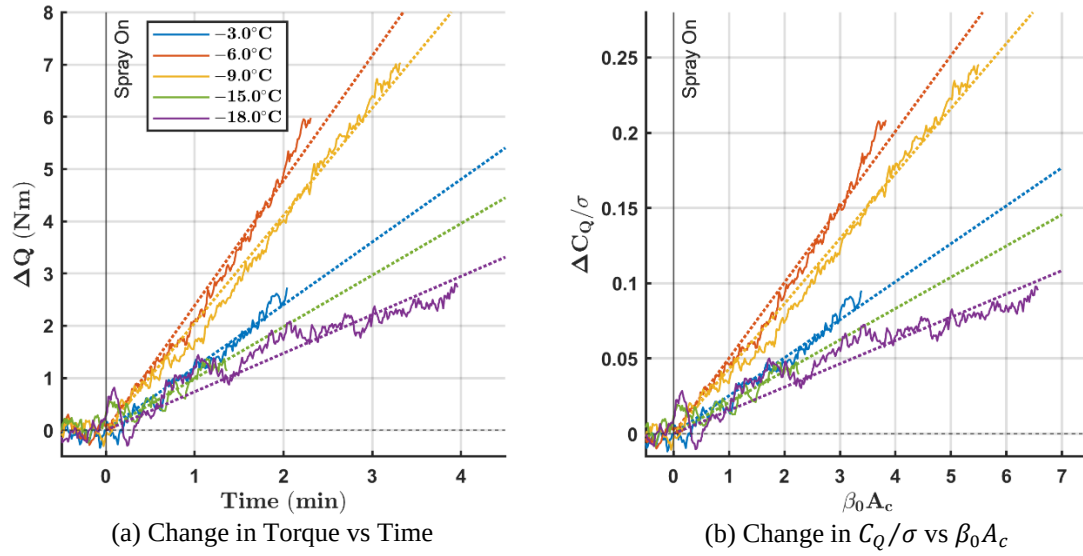


Fig. 13 Effect of Temperature on Propeller Torque Requirements ($n_0 < 1$)

Table 7 Target Test Conditions for Fig. 13

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ $^\circ\text{C}$	δ μm	LWC g/m^3	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10^6
RA3700	87	-3.0	15	1.20	2.1	1516	28	0.21	2.49	0.26	3.96	0.88	0.15
RA3715	"	-6.0	"	"	2.32 [†]	"	"	"	"	0.51	4.37	"	"
RA3696	"	-9.0	"	"	3.33 [†]	"	"	"	"	0.75	6.28	"	"
RA3684	"	-15.0	"	"	1.4	"	"	"	"	1.19	2.64	"	"
RA3697	"	-18.0	"	"	4.0	"	"	"	"	1.40	7.54	"	"

[†] Time until first shedding event

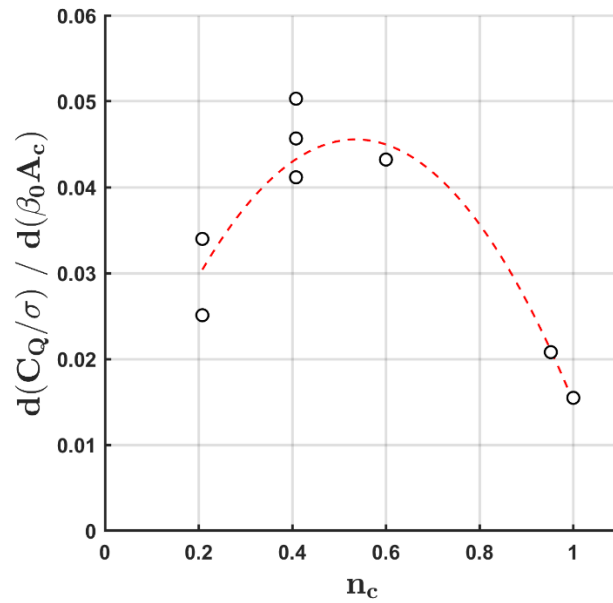


Fig. 14 Rate of Change of C_Q/σ vs $\beta_0 A_c$ as a Function of the Corrected Freezing Fraction at $r/R = 0.75$

A similar analysis was performed to study the effect of temperature at a target freezing fraction of $n_0 = 1$. Figure 15a shows the effect of decreasing the freestream total temperature from -7.5 to -15°C for conditions resulting in rime ice ($n_0 = 1$). Note that the ice is believed to be rime along the entire span of the propeller at these conditions, as verified from experimental observations and from the resulting ice shapes. As seen from Fig. 15a, the propeller torque requirements remain virtually unchanged across the temperature range considered, as a freezing fraction of one is maintained at these conditions. This is consistent with the resulting ice shapes which were also found to be unaffected by temperature. The ice shapes at $r/R = 0.75$ can be found in Fig. 15b. Ice shapes at additional radial locations for these runs can be found in Ref. 3.

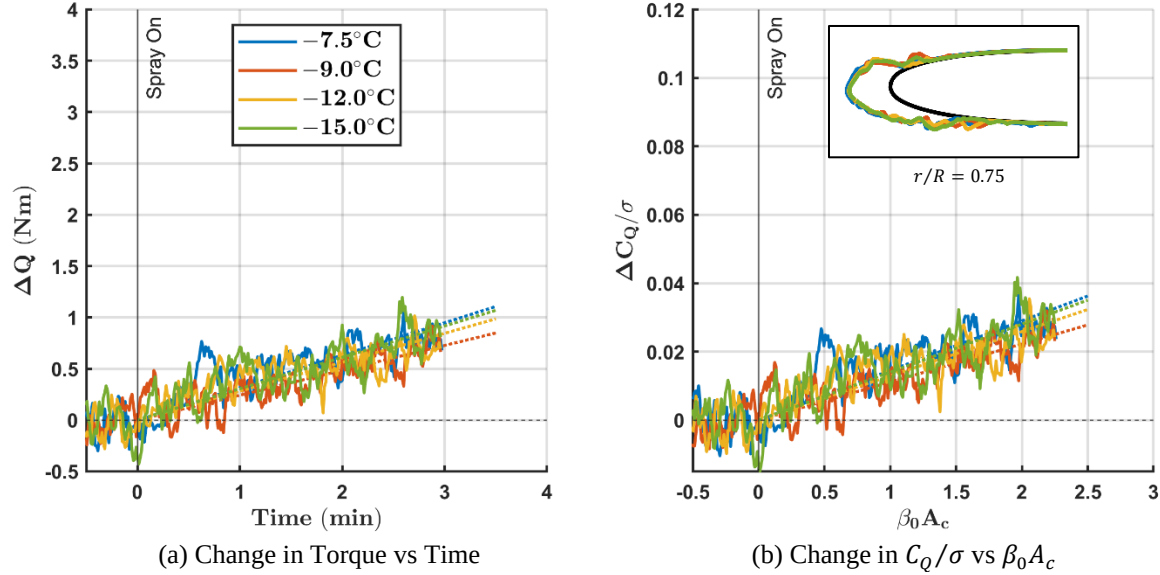


Fig. 15 Effect of Temperature on Propeller Torque Requirements ($n_0 = 1$)

Table 8 Target Test Conditions for Fig. 15

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3705	87	-7.5	15	0.55	3.0	1516	28	0.21	2.49	1.26	2.59	0.88	0.15
RA3703	“	-9.0	“	“	“	“	“	“	“	1.49	“	“	“
RA3704	“	-12.0	“	“	“	“	“	“	“	1.94	“	“	“
RA3683	“	-15.0	“	“	“	“	“	“	“	2.37	“	“	“

The results suggest that the required propeller torque in icing is highly dependent on n_0 and, therefore, is highly dependent on temperature for conditions when $n_0 < 1$. When $n_0 = 1$ is maintained, the ice morphology and resulting aerodynamic impact are largely independent of temperature over the temperatures considered. Additionally, under rime conditions, the impinging droplets freeze instantly upon impact, resulting in an ice shape that more closely follows the original contour of the airfoil. As a result, the performance impact is generally less severe than for glaze conditions. For $n_0 < 1$, the propeller torque requirements initially increase with increasing temperature (decreasing n_0), and then decreases with increasing temperature below a certain value of n_0 . For this study, the peak torque requirements were found to occur somewhere between a n_c of 0.40 and 0.60. More data is needed to fully understand why this trend occurs and whether similar trends are present under different propeller operating regimes.

F. Effect of Tip Speed

Figure 16 shows the effect of increasing the helical Mach tip speed, M_{tip} , of the propeller from 0.21 to 0.31 Mach at a target freezing fraction of $n_0 = 1$ corresponding to runs RA3683 and RA3699, respectively. The higher M_{tip} was achieved by increasing both the propeller RPM and tunnel speed to maintain a constant advance ratio. The spray time

of the higher M_{tip} condition was reduced to match the A_c of the lower M_{tip} condition. All other conditions were identical as seen in Table 9.

As expected, the higher M_{tip} case experienced a faster rise in torque due to the faster rate of ice accumulation along the propeller. However, when the change in torque is non-dimensionalized and plotted against $\beta_0 A_c$, the torque requirements align very well for a given $\beta_0 A_c$, as shown in Fig. 16b. The ice shapes at $r/R = 0.75$ are also shown in Fig. 16b which show good agreement between the ice shapes. Ice shapes at additional radial locations for these runs can be found in Fig. 21 in the Appendix.

Note that, while the higher M_{tip} condition resulted in increased local velocities along the span of the propeller, the differences were not substantial enough to significantly affect β_0 , as shown in Table 9. Had the difference in β_0 been larger, it is likely that the increased impingement limits would have resulted in an effect similar to what was observed when increasing the MVD in Section C.

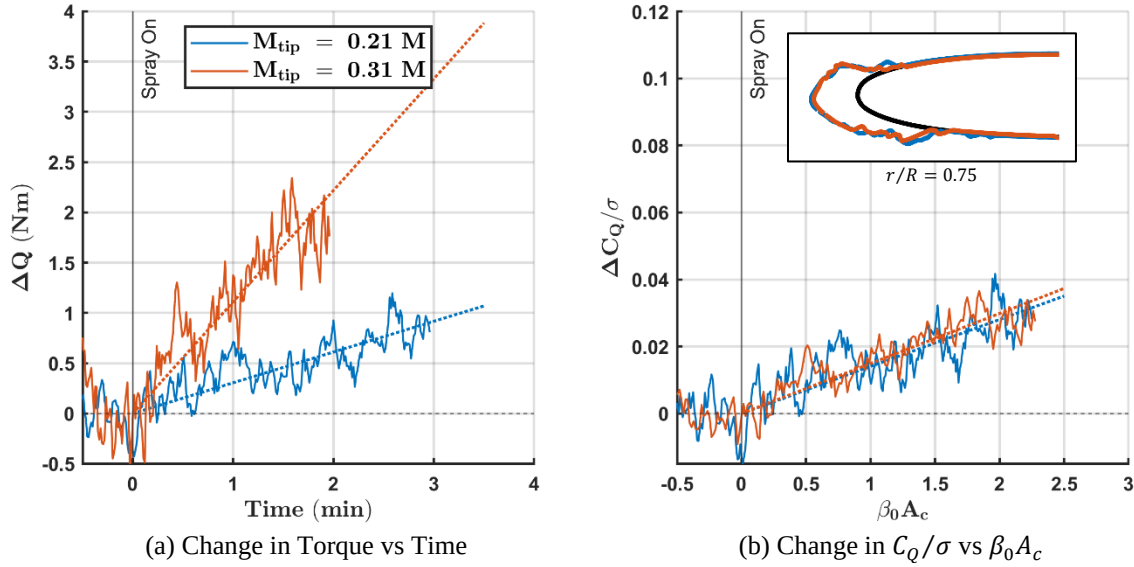


Fig. 16 Effect of Tip Speed on Propeller Torque Requirements ($n_0 = 1$)

Table 9 Target Test Conditions for Fig. 16

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3683	87	-15.0	15	0.55	3.0	1516	28	0.21	2.49	2.37	2.59	0.88	0.15
RA3699	130	“	“	“	2.0	2265	“	0.31	“	1.85	2.58	0.90	0.34

A similar evaluation was performed to explore the effect of M_{tip} on the propeller torque requirements at glaze conditions ($n_0 < 1$). As with the rime case, the higher tip speed was achieved by increasing both the propeller RPM and the tunnel speed to maintain a constant advance ratio. For these conditions, M_{tip} was increased from 0.20 to 0.25 Mach. Like the rime case, the MVD and LWC were the same for both runs. However, matching n_0 required reducing the freestream total temperature for the higher M_{tip} case. The full list of conditions can be found in Table 10.

As seen from Fig. 17a, the propeller torque increases more rapidly for the higher M_{tip} case due to the increased local velocities along the propeller. As was done for the rime case, the non-dimensional change in torque was plotted against $\beta_0 A_c$, as shown in Fig. 17b. As seen from Fig. 17b, the non-dimensional change in propeller torque for a given $\beta_0 A_c$ is slightly higher for the higher M_{tip} case. A possible explanation for this discrepancy is the difference in We_L at these conditions. For fixed-wings, We_L is known to be an important parameter that effects the shape of glaze ice accretions. It is possible that We_L has a similar effect on rotating wings as well which would impact the overall aerodynamic performance of the propeller. Unfortunately, ice shapes were not acquired for these runs due to ice

shedding so more testing is needed to fully understand the impact of We_L on the resulting ice shape and performance degradation of propellers.

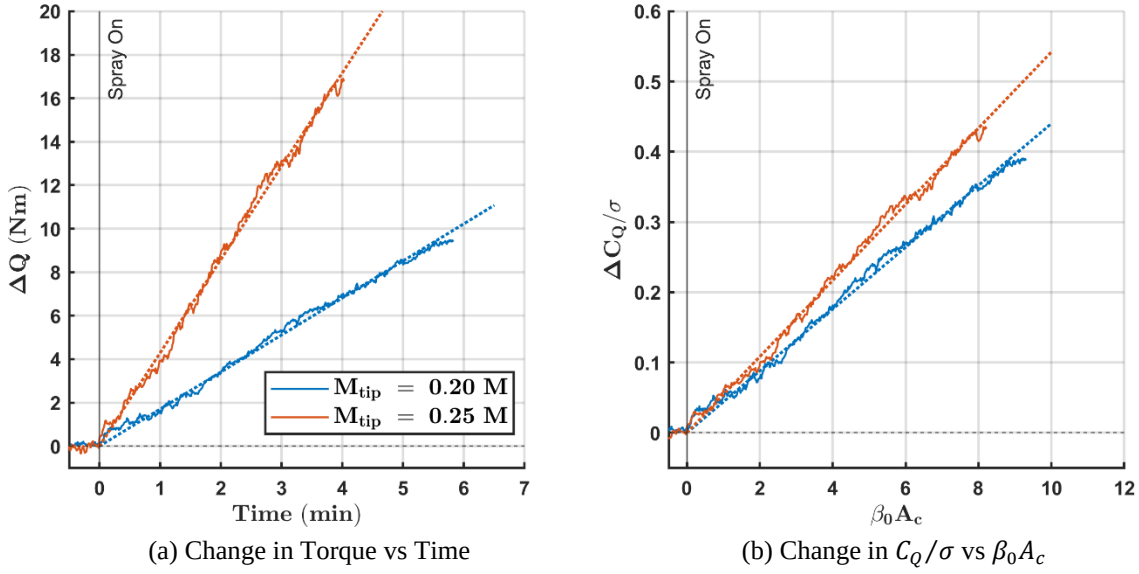


Fig. 17 Effect of Tip Speed on Propeller Torque Requirements ($n_0 = 1$)

Table 10 Target Test Conditions for Fig. 17

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3799	87	-9.0	15	1.20	5.83 [†]	1394	28	0.20	2.71	0.77	10.58	0.88	0.14
RA3803	110	-10.3	“	“	4.03 [†]	1763	“	0.25	“	“	9.25	0.89	0.22

[†] Time until first shedding event

The results of the preceding analysis suggest that the change in the non-dimensional torque requirements when $n_0 = 1$ is largely independent of M_{tip} for a given $\beta_0 A_c$, provided that the distribution of β_0 along the propeller remains the same or similar. It should be noted, though, that the data is limited to a small range of subsonic tip speeds and further investigations are required to understand when these scaling relationships begin to breakdown, especially at higher tip speeds when compressibility effects can no longer be ignored. For conditions when $n_0 < 1$, the results suggest that the change in the non-dimensional torque requirements are not independent of M_{tip} for a given distribution of n_0 and $\beta_0 A_c$, even when a similar β_0 is maintained. Further work is needed to understand whether the difference in We_L may be responsible for this discrepancy, or if there are other contributing factors.

G. Effect of Propeller Size

Figure 18a compares the change in propeller torque requirements for the 24, 28, and 36-inch diameter propellers at a target freezing fraction of $n_0 = 1$ corresponding to runs RA3717, RA3698, and RA3681, respectively. All three propellers were tested at the same tunnel conditions (V_∞ , $t_{\infty,0}$, MVD, and LWC) at a similar advance ratio as shown in Table 11. The spray times were adjusted accordingly to achieve the same A_c for each propeller. Note that, while the MVD was held constant, the β_0 of each propeller is approximately the same at these conditions. As seen from Fig. 18a, the rate of change in torque increases with propeller size. However, when expressed in non-dimensional form, the change in C_Q/σ was found to be independent of propeller size for a given $\beta_0 A_c$, as shown in Fig. 18b. The ice shapes normalized by the local chord length at $r/R = 0.75$ are also shown in Fig. 18b, which show good agreement between the ice shapes. Ice shapes at additional radial locations for these runs can be found in Ref. 3.

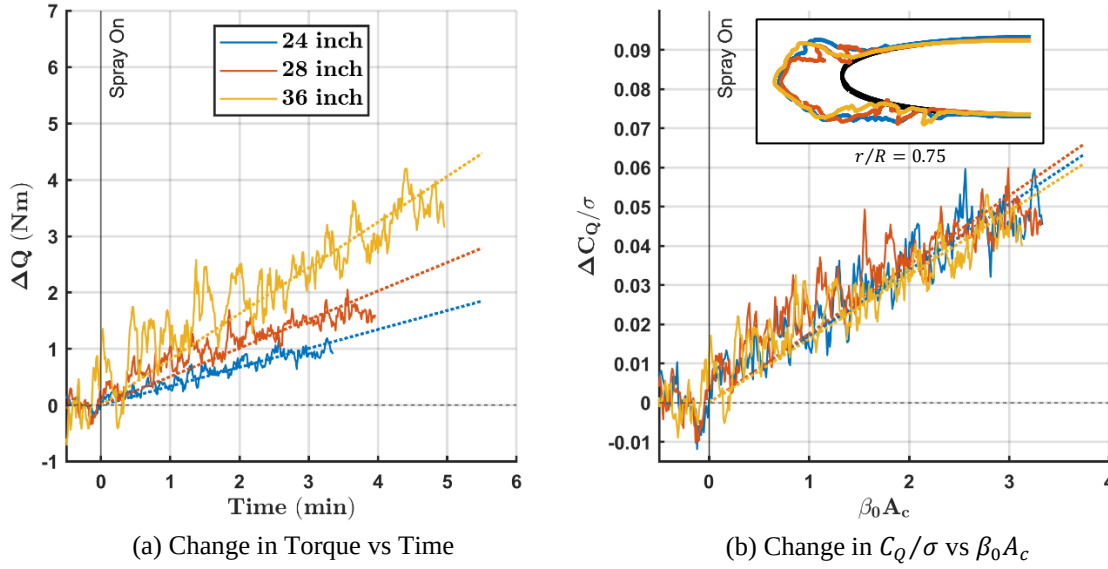


Fig. 18 Effect of Propeller Size on Propeller Torque Requirements ($n_0 = 1$)

Table 11 Target Test Conditions for Fig. 18

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3717	95	-12.0	15	0.55	3.4	1928	24	0.23	2.49	1.95	3.76	0.90	0.15
RA3698	“	“	“	“	4.0	1657	28	“	“	1.84	3.78	0.89	0.18
RA3681	“	“	“	“	5.0	1300	36	“	2.47	1.67	3.67	0.86	0.23

A similar evaluation was performed for the 28 and 36-inch diameter propellers at glaze conditions when $n_0 < 1$. For this comparison, both the 28 and 36-inch diameter propellers were operated at the same advance ratio. However, the freestream total temperature, MVD, and tunnel speed were slightly adjusted for the 36-inch propeller to better match n_0 , β_0 , and We_L , respectively. The full list of operating and cloud conditions can be found in Table 12. Note that the final value of A_c differed between these two runs as the spray times were chosen to achieve test objectives unrelated to this comparison.

Figure 19a shows the change in torque for the 28 and 36-inch diameter propellers at these conditions. As expected, the larger propeller experiences a faster rise in torque. However, when expressed in non-dimensional terms, the change in C_Q/σ is very similar for both propeller sizes at a given $\beta_0 A_c$, as shown in Fig. 19b. The ice shapes normalized by the local chord length at $r/R = 0.75$ are provided in Fig. 19b. Note that the ice shape corresponding to run RA3798 is slightly larger due to the slightly higher value of A_c . Additional ice shapes at other radial locations can be found in Fig. 22 in the Appendix.

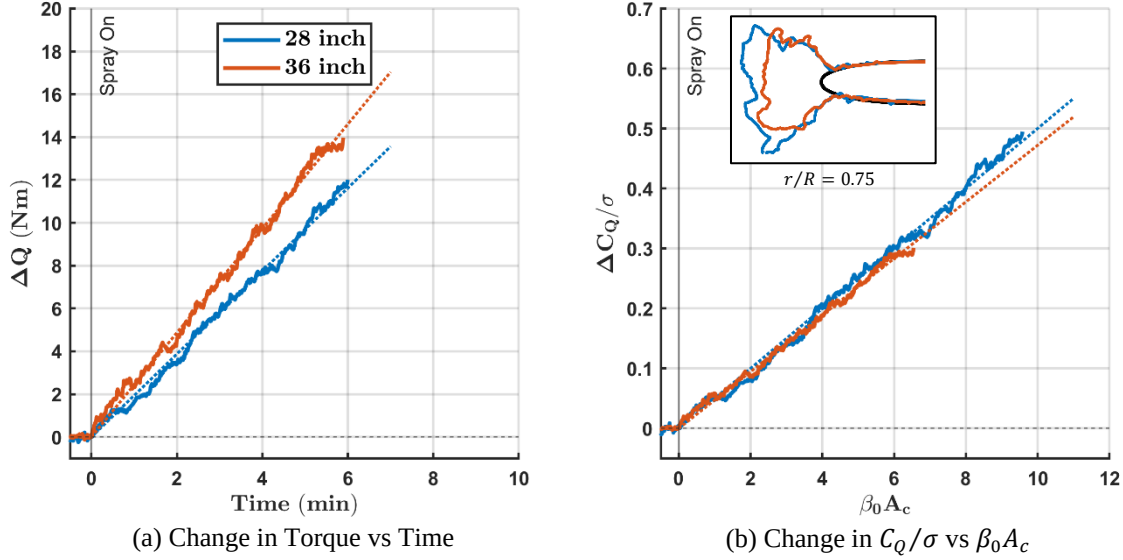


Fig. 19 Effect of Propeller Size on Iced Propeller Torque Requirements ($n_0 < 1$)

Table 12 Target Test Conditions for Fig. 19

Run	Target Operating and Cloud Conditions									Similarity Parameters (calculated at $r/R = 0.75$)			
	V_∞ knots	$t_{\infty,0}$ °C	δ μm	LWC g/m ³	τ min	RPM rev/min	D in	M_{tip} Mach	J	n_0^*	A_c	β_0	We_L 10 ⁶
RA3798	87	-6.0	15	1.20	6.0	1394	28	0.20	2.71	0.53	10.89	0.88	0.14
RA3807	80	-6.3	16	“	“	996	36	0.18	“	“	7.74	0.86	0.15

The results are encouraging for those who wish to gain insights into iced propeller performance degradation using scaled models. However, the data collected during the present study is limited, as it encompasses only a small range of propeller sizes, tip speeds, and advance ratios. Further work is needed over a larger range of propeller sizes, operating conditions, and tunnel conditions to provide greater confidence in the scaling method.

VI. Conclusion

An experimental investigation of an eVTOL propeller under simulated icing conditions was conducted at the NASA Glenn Icing Research Tunnel. Propeller performance degradation, in the form of torque rise over time, was recorded under various cloud and operating conditions. The effect of LWC, MVD, temperature, tip speed, and propeller size on the resulting change in torque were investigated and found to align well with the expected trends. Several repeat conditions were conducted which found the repeatability of the torque data to be excellent.

To aid in the interpretation of the results, the key icing similarity parameters (n_0 , β_0 , A_c , and We_L) have been calculated at the blade $r/R = 0.75$ location. The change in torque was also presented non-dimensionally as the torque coefficient divided by solidity (C_Q/σ) and plotted against the product of the stagnation collection efficiency and accumulation parameter ($\beta_0 A_c$), as calculated at the $r/R = 0.75$ location.

Over the range of conditions tested, it was found that the change in C_Q/σ under rime conditions ($n_0 = 1$) was largely independent of LWC, tip speed, and propeller size for a given $\beta_0 A_c$ and advance ratio, provided that the distribution of n_0 and β_0 are the same or similar. This observation is consistent with the measured ice shapes, which revealed that good ice shape similarity was achieved under these conditions. When independently varying β_0 at rime conditions, it was found that the higher β_0 condition required more torque for a given $\beta_0 A_c$, likely due to the negative aerodynamic impact of the increased impingement limits.

For glaze conditions ($n_0 < 1$), the change in C_Q/σ was also found to be largely independent of LWC and propeller size for a given $\beta_0 A_c$ and advance ratio over the range of conditions tested, provided that the distributions of n_0 , β_0 , and We_L were the same or similar. However, when testing at a different tip speed (and consequently, We_L), it was

found that the higher tip speed condition resulted in a slightly higher C_Q/σ for a given $\beta_0 A_c$. More work is needed to understand whether the difference in We_L is responsible for this observation or if there are other factors involved.

While the results are encouraging for those who wish to apply current icing scaling methods to achieve similarity of propeller performance in icing, the present study is limited to a small range of propeller sizes and operating conditions. Further work is needed over a larger range of tip speeds, advance ratios, and propeller sizes to provide greater confidence in the scaling methodology.

Appendix

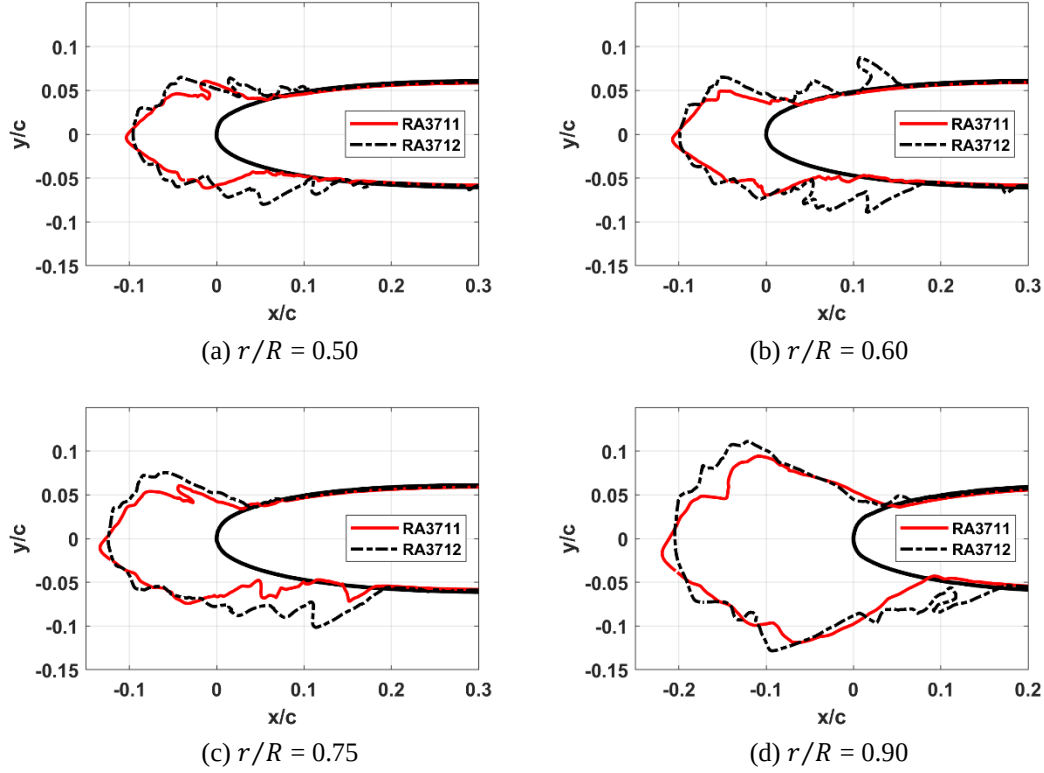


Fig. 20 Effect of MVD on Propeller Ice Shape ($n_0 = 1$). See conditions in Table 6.

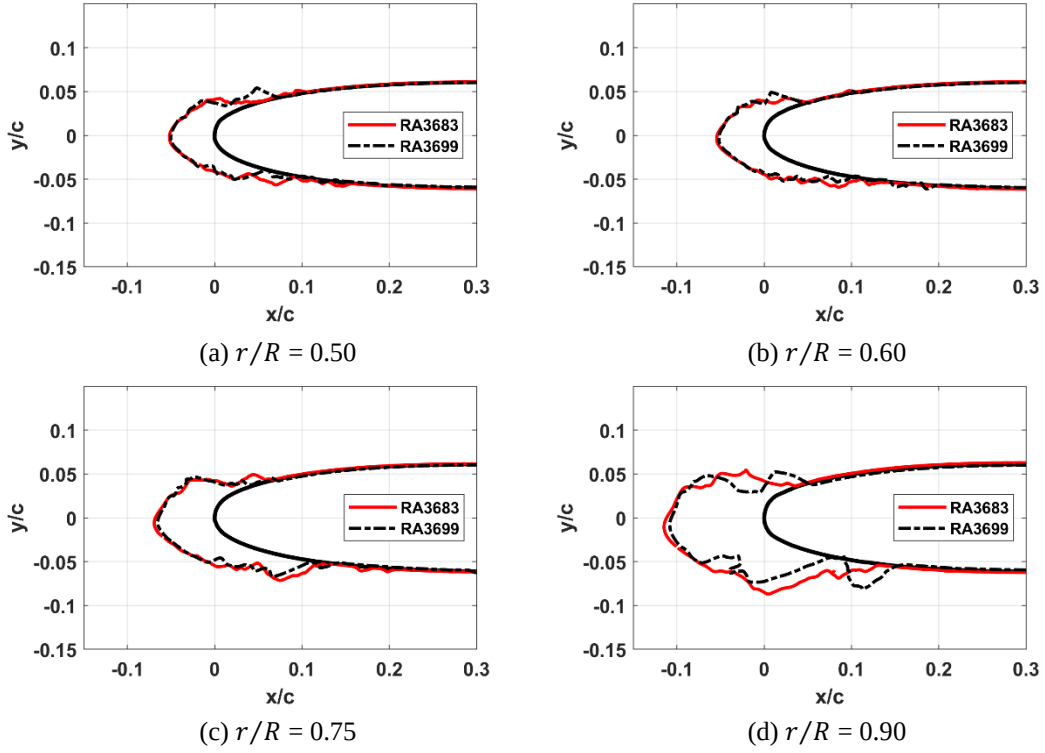


Fig. 21 Effect of Tip Speed on Propeller Ice Shape ($n_0 = 1$). See conditions in Table 9.

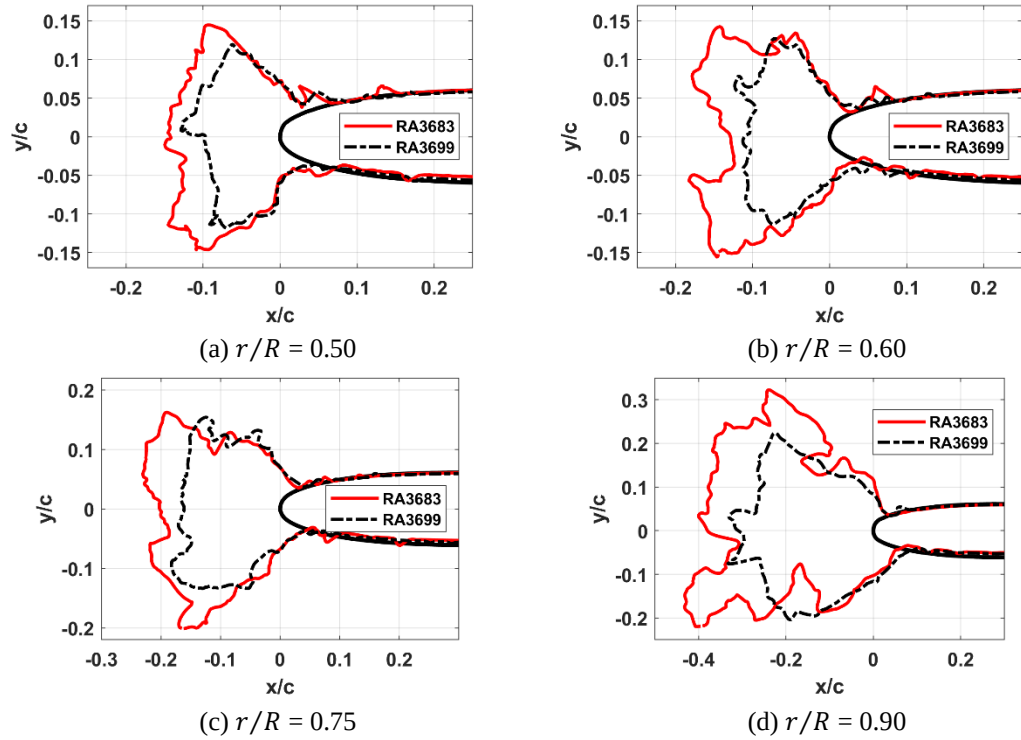


Fig. 22 Effect of Propeller Size on Ice Shape ($n_0 < 1$). See conditions in Table 12.

Acknowledgments

The experimental work presented in this paper was funded by NASA's Revolutionary Vertical Lift and Technology (RVLT) project and the Advanced Air Transport Technology (AATT) project. The authors would also like to acknowledge all of those who helped make this a successful test. In particular, the authors would like to thank Keith Hunker, Xavier Collazo Fernandez, Joe Wisniewski, and Scott Hensley for their invaluable technical support. The authors would also like to thank Jordan Salkin and Quentin Schwinn for their imaging support during testing. In addition, this work could not have been possible without the support of the NASA Glenn Icing Branch and the IRT engineers and technicians for their efforts in making these test campaigns a success.

References

- [1] Gandhi, F., Pepe, J., and Smith, B., "High Solidity, Low Tip-Speed Rotors for Reduced eVTOL Tonal Noise," 78th Annual Forum of the Vertical Flight Society, Ft. Worth, TX, USA, May 10-12, 2022.
- [2] Whiteside, S. K. S., Pollard, B. P., Antcliff, K. R., Zawodny, N. S., Fei, X., Silva, C., and Medina, G. L., "Design of a Tiltwing Concept Vehicle for Urban Air Mobility," NASA/TM-20210017971, 2021.
- [3] von Hardenberg, P. H., Flack, F. C., Rigby, D. L., "Ice Shape Analysis of an eVTOL Propeller in Forward Flight at the NASA Glenn Icing Research Tunnel," *AIAA AVIATION 2024 Forum*, Las Vegas, NV, July-August 2024.
- [4] Porter, C., Potapczuk, M., Ozoroski, T., Sabri, Z., Galloway, E., Rigby, D., Wright, W., and Tsao, P., 2024. "GlennICE Manual 4.1.0." NASA/TM-20240002191, 2024.
- [5] Rigby, D. L. and von Hardenberg, P.H., "Demonstration of Initial GlennICE Relative Frame Capability: Axial-Flow Propeller", SAE International Icing Conference, 23ICE-0029/2023-01-1457, 2023.
- [6] Rigby, D. L. and von Hardenberg, P. H., "GlennICE Simulation of 24, 28, and 36 Inch Diameter eVTOL Propellers in Forward Flight," *AIAA AVIATION 2024 Forum*, Las Vegas, NV, July-August 2024.
- [7] Frantz, J., Giuffre, C., and von Hardenberg, P., "Analysis of Experimental Ice Shedding Events on a Propeller at the NASA Glenn Icing Research Tunnel", *AIAA AVIATION 2025 Forum*, Las Vegas, NV, July 2025.
- [8] Tsao, J., Sabri, Z., von Hardenberg, P., and Flack, C., "A Preliminary Evaluation of Icing Scaling on AAM Propellers," *AIAA AVIATION 2025 Forum*, Las Vegas, NV, July 2025.
- [9] Zawodny, N. S., Pettingill, N. A., Lopes, L. V., and Ingraham, D. J., "Experimental Validation of an Acoustically and Aerodynamically Optimized UAM Proprotor Part 1: Test Setup and Results," NASA/TM-20220015637, 2023.