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Prediction of the flow and acoustic fields generated by an isolated propeller

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Abstract

Propeller-driven aircraft noise has been a problem to passengers and community for decades. With the recent advances and focus on the development of electric propulsion and unmanned aerial vehicles, the noise emitted by propellers is still a concern. The design of low-noise propellers requires a balance between noise emission and aerodynamic performance, which commonly walk in different ways during design. Numerical approaches such as computational fluid dynamics (CFD) coupled with noise prediction methods can aid in the design of propellers and in their installation effects. Analytical, semi-empirical or reduced order methods are desired for the design process as their use makes possible the analyses of multiple configurations and optimization processes. We propose an analysis and validation of a simulation model for predicting the flow field and aerodynamic characteristics of propellers using the open-source code OpenFOAM. A 4-blade tractor propeller geometry of diameter $D = 0.237$ m was chosen from the literature. Unsteady Reynolds Averaged Navier-Stokes simulations were conducted using the SST turbulence model. A dynamic mesh approach was adopted to simulate the rotation of the propeller, in which the domain is divided into a static region and a rotating region. Results are validated through comparisons with reference data. The acoustic field is obtained by different approaches: acoustic analogies and semi-analytical methods, and the results are compared to other numerical data in the literature. In the first approach, the tonal noise field is obtained from the simulated flow field using the Ffowcs-Williams and Hawkings analogy. In the second approach, the distributed thrust and torque coefficients, obtained via CFD, are used as input to analytical methods for the prediction of the thickness and loading components of propeller noise. Results of both approaches are compared and discussed.

Keywords: Propeller Noise, OpenFOAM, Semi-analytical Methods.

PACS: 43.28.-g; 43.28.Ra; 43.50.Nm.



1. INTRODUCTION

Propeller noise is a main concern in turboprop aircraft, as well as, vertical take-off and landing (VTOL) vehicles. With the recent advances and focus on the development of electric propulsion and unmanned aerial vehicles (UAVs), the noise emitted by propellers is still a concern. The design of low-noise propellers requires a balance between noise emission and aerodynamic performance, which commonly walk in different ways during design [1].

Numerical approaches such computational fluid dynamics (CFD) coupled with noise prediction methods can aid in the design of propellers and in their installation effects. Large-eddy simulation (LES) is a high-fidelity alternative that makes possible the deeper analyses of the phenomena. On the other hand, the use of hybrid approaches [2, 3] as well as unsteady Reynolds Averaged Navier-Stokes (RANS) simulations [4–9] can lead to reasonable results with reduced computational cost. Analytical, semi-empirical and reduced order methods are desired for the design process as their use make possible the analyses of multiple configurations and optimization processes.

When dealing with noise prediction from turbulent flows, different methods can be used. Direct methods perform the noise computation in the same domain as the fluid dynamics, without any modeling for the sound. The full set of equations, Navier-Stokes or Euler, is solved in the domain of interest for both the flow and acoustic fields. This requires a domain sufficiently large in order to calculate noise propagation up to the receptor points. Due to the difficulties of using direct methods, the so called hybrid approaches are commonly used. In such methods, sound-generation due to aerodynamics is treated separately from the acoustic transport process. The sound field is computed, with the flow field as input, by using acoustic analogies such as Ffowcs Williams and Hawkings [10]. An alternative is to use semi-empirical methods to model the sources of noise. For the propeller case, some methods use thrust and torque distributions to estimate the tonal noise [11–14]. When dealing with such methods, CFD can be used both to generate input

data [15] as well as provide source-field information to be used for the adjustment of model parameters.

We propose an analysis of different approaches to predict the acoustic field from an isolated propeller. Our objective is to develop and validate a simulation model, based on the opensource code OpenFOAM, to predict the aerodynamic characteristics and acoustic fields of propellers. Unsteady RANS simulation are conducted using the SST model [16] to solve the flow field over a four-blade propeller. The results of the simulation are used to obtain the acoustic field both by an acoustic analogy approach and by analytical prediction methods.

2. METHODS

This section describes the geometry, computational domain and mesh and the models used for noise computation.

2.1 Geometry and Experimental Data

A 4-blade tractor propeller geometry of diameter $D = 0.237$ m was used (Fig. 1). Details of the geometry and descriptions of the measurements carried out in the Low Speed Low Turbulence Wind Tunnel (LTT) of Delft University of Technology can be found in de Vries et al. [17], Sinnige et al. [18]. For the present analysis, only the isolated propeller will be considered. Experimental data such as thrust coefficients are available in de Vries et al. [17], Sinnige et al. [18] and will be used for validation. On the other hand, the validation of the computed sound field will be provided by comparisons with the very large eddy simulation (VLES) based on the lattice Boltzmann method (LBM) from Avallone et al. [19] for a pylon-mounted installed propeller. Data was extracted from the references using the WebPlotDigitizer tool [20].

2.2 Numerical model

Incompressible, unsteady Reynolds Averaged Navier-Stokes (URANS) simulations were conducted using the SST turbulence model [16] implemented in OpenFOAM v2012 [21]. The simulations used a dynamic mesh approach to simulate the rotation of the propeller, in which the

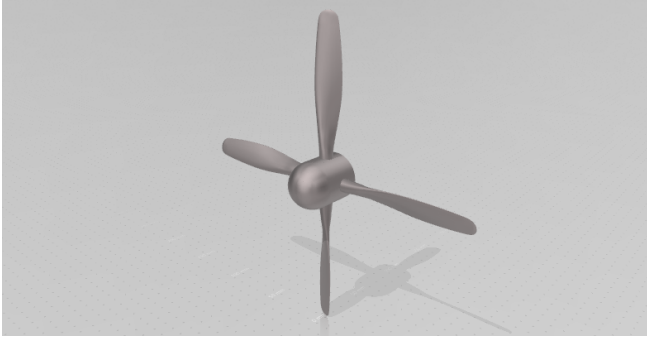


Figure 1: Image of the propeller geometry studied by Sinnige et al. [18]

domain is divided in a static region and a rotating region, coupled by an arbitrary mesh interface (AMI). The computational domain and boundary conditions are depicted in Fig. 2. The baseline cylindrical domain has a diameter of $8D$, where D is the propeller diameter. It extends $3D$ upstream and $7D$ downstream of the propeller.

A freestream inflow/outflow condition was imposed in the upstream and lateral boundary of the domain. A freestream velocity of $V_0 = 40$ m/s was imposed with ambient pressure. For computing turbulence quantities, a low turbulence wind tunnel condition was considered, with turbulence intensity $I = 0.1\%$ and $v_t/v = 5$. For the outflow, a zero-gradient condition was used for velocity and turbulence quantities, with ambient back pressure. On the propeller, a no-slip condition with standard wall functions was used. At the interface between the static and rotating domains, a Cyclic Arbitrary Mesh Interface (AMI) condition was applied. The rotation frequency (n) was calculated to obtain an advance ratio of $J = V_0/nD = 0.8$.

The computational unstructured grid of 5.45×10^6 volumes was generated using the code SALOME [22]. The grid is formed predominantly tetrahedral volumes and is depicted in Figure 3. Near the walls, layers of prismatic elements were created in order to correctly solve the boundary layers, as depicted in Figure 4. Finally, the mesh on the surface of the propeller is shown in Figure 5.

The simulation was initialized with an uniform flow field of V_0 . First order numerical were used in the initial timesteps and were switched to second order numerical schemes during the devel-

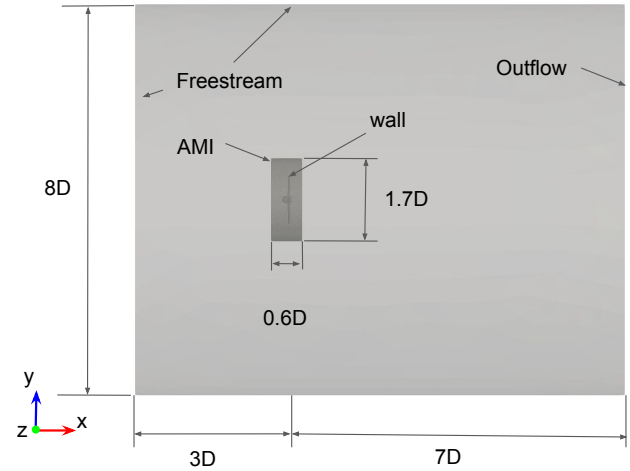


Figure 2: Computational domain and boundary conditions

opment of the flow. For the convective terms, linear upwind and limited linear schemes were used for velocity and turbulence, whereas a Multidirectional limited linear scheme was adopted for the gradient terms. Time discretization was based on an implicit Crank-Nicolson scheme. For the pressure-velocity coupling, the PIMPLE algorithm was used (see Holzmann [23]). The simulation time is equivalent to 30 complete revolutions with approximately 1000 time-steps per revolution.

2.3 Noise computation - CFD

For the far-field noise computations we used a dynamic library called libAcoustics, which is integrated with OpenFOAM and was developed by Epikhin et al. [24] and Ilya Evdokimov et al. [25]. This code contains acoustic analogies to be used in conjunction with CFD computations. Validation of the library has been previously addressed by [24, 26–28].

In this work, the Ffwoes-Williams and Haking analogy (FW-H) [10] based on permeable surfaces was used to compute the far-field noise. In such approach, near field pressure and velocity data are stored on an arbitrary control surface, which should encompass the most significant sound-generating regions. This surface information is used as an input for the far-field noise computations. Particularly the Garrick Triangle (GT) formulation, described by Brès et al. [29], was used as it shows increased computational efficiency for wind tunnel configurations.

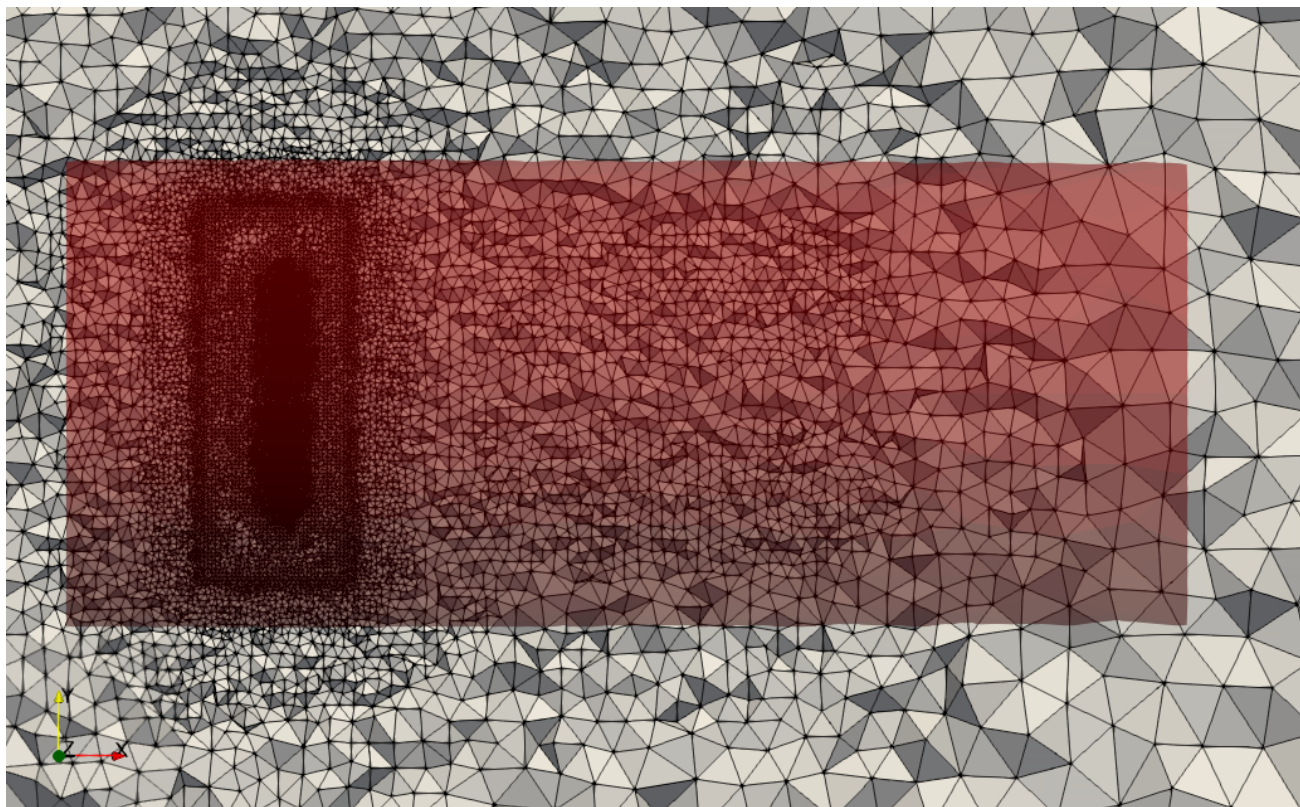


Figure 3: XY-plane slice of the computational mesh. The red-shaded area represents the FWH surface.

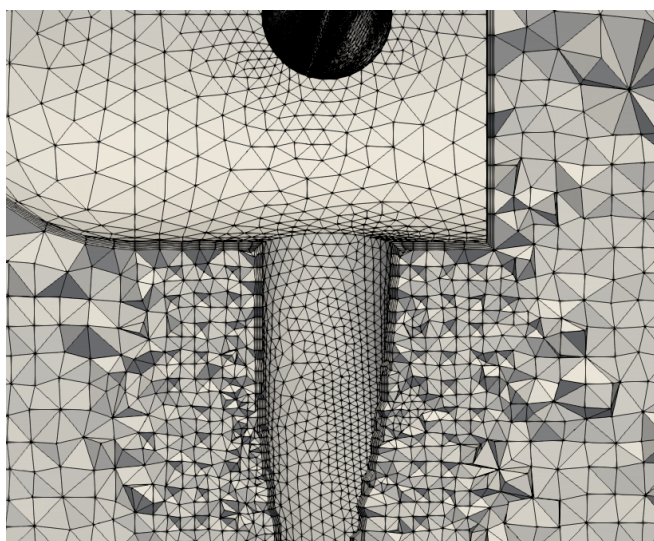


Figure 4: Image of the mesh close to the hub-blade junction.

Two different FWH surfaces were used. The surfaces are three-dimensional and have a cylindrical shape as shown in Figure 3, in red. Surface 1 (S1) has a diameter of $1.05D$ and extends axially from $x = -0.85D$ to $x = 4.2D$. Surface 2 (S2) has the same diameter, extending, however, from $x = -1.1D$ to $x = 5.5D$.

Data were recorded on the FWH surfaces dur-

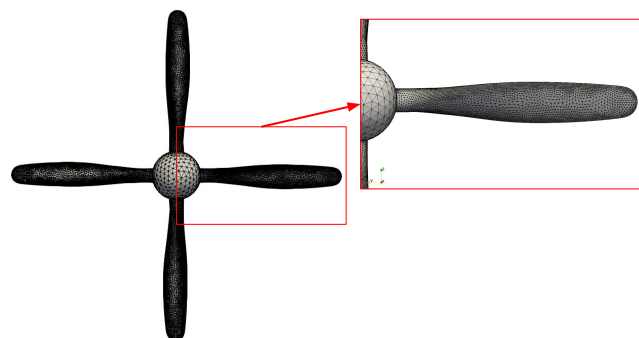


Figure 5: Mesh on the surface of the propeller.

ing 13.5 revolutions, with approximately 1880 timesteps per revolution, resulting in a sampling frequency of 400 kHz. The sound field was calculated on an arc of radius $2D$, consisting of thirty-eight microphone positions, following the polar angle convention shown in Figure 6. The noise Power Spectral Density (PSD) spectrum was obtained by the [30] method with resulting frequency resolution of 42 Hz. A Hanning window was employed with an overlap of 75% between blocks. In order to compare with the reference data, the PSD was converted to Sound Pressure Level (SPL) in 1/12-octave bands.

2.4 Analytical noise prediction

Two analytical methods were selected for the tonal noise prediction. The first one unites the loading noise theory from Gutin [12] with the thickness noise method form Deming [13]. We implemented the far-field equations as presented by Kotwicz Herniczek et al. [11]. The loading noise is computed as,

$$P_{mL} = \frac{mB\Omega}{2\sqrt{2}\pi cS} \int_{hub}^{tip} \left[\frac{dT}{dr} \cos\varphi - \frac{dQ}{dr} \frac{c}{\Omega r^2} \right] J_{mB} dr \quad (1)$$

where P_{mL} [Pa] is the rms of the loading noise pressure, m is the number of the harmonic, B is the number of blades, Ω [rad/s] is the rotational speed, c [m/s] is the speed of sound, r [m] is the local radial position on the blade, S is the observer radial position, φ [rad] is the observer polar angle, dR/dr [N/m] is the elemental trust, dQ/dr [N.m/m] is the elemental torque and J_{mB} is the Bessel function (see eq.3).

The thickness noise equations is defined as follows,

$$P_{mT} = \frac{-\rho(mB\Omega)^2 B}{3\sqrt{2}\pi S} \int_{hub}^{tip} bhJ_{mB} dr \quad (2)$$

where, P_{mT} [Pa] is the rms amplitude of the thickness noise, ρ [kg/m³] is the fluid density, b [m] is the local airfoil chord and t [m] is the local maximum airfoil thickness.

$$J_{mB} = J_{mB} \left(\frac{mB\Omega}{c} r \sin\varphi \right) \quad (3)$$

The second method is the one originally proposed by Barry and Magliozzi [14], which takes into account the effects of forward flight and the blade twist angle. We implemented the modified version of the far-field formulation presented by Kotwicz Herniczek et al. [11]. The loading noise component is computed with the following equation,

$$P_{mL} = \frac{1}{\sqrt{2}\pi S_0} \int_{hub}^{tip} \frac{r}{b \cos\phi_t} \sin\left(\frac{mBb \cos\phi_t}{2r}\right) \left[\frac{(M + X/S_0)\Omega}{c(1-M^2)} \frac{dT}{dr} - \frac{1}{r^2} \frac{dQ}{dr} \right] \left[J_{mB} + \frac{(1-M^2)Yr}{2S_0^2} (J_{mB-1} - J_{mB+1}) \right] dr \quad (4)$$

where X [m] is the axial position of the observer as shown in Figure 6, Y [m] is the distance of the observer to the propeller axis, M is the free-stream Mach number, $S_0 = \sqrt{X^2 + (1-M^2)Y^2}$ is the amplitude radius, ϕ_t is the local blade twist angle and J_{mB} is the Bessel function shown in equation 3. Thickness noise is computed by the following equation,

$$P_{mT} = -\frac{\rho m^2 \Omega^2 B^3}{2\sqrt{2}\pi(1-M^2)^2} \frac{(S_0 + MX)^2}{S_0^3} \int_{hub}^{tip} A_x \left[J_{mB} + \frac{(1-M^2)Yr}{2S_0^2} (J_{mB-1} - J_{mB+1}) \right] dr \quad (5)$$

where A_x is the local airfoil cross-sectional area, which is approximated by $0.6853bh$ [m²].

$$J_{mB} = J_{mB} \left(\frac{mB\Omega Y r}{cS_0} \right) \quad (6)$$

The necessary geometric input parameters were obtained from Sinnige et al. [18] and the thrust and torque distributions were taken from the CFD. To do so, each blade was divided in 26 segments of $\Delta r = 0.033R$, where $R = D/2$ is the blade-tip radius. For each segment, the axial force and moment components were calculate by integration of pressure and wall shear stress. The segment trust (ΔT) and torque (ΔQ) was divided by Δr to approximate dT/dr and dQ/dr of each segment.

3. RESULTS

In this section, the aerodynamic and acoustic results are presented and discussed.

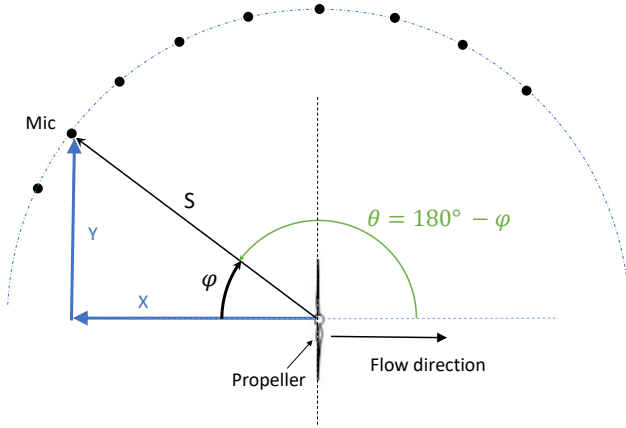


Figure 6: Microphone positions considering different coordinate systems. Black - Gutin [12] and Deming [13] model, Blue - Barry and Magliozzi [14] model and Green - CFD.

3.1 Aerodynamic Results

Table 1 show the obtained thrust and torque coefficients. The thrust coefficient is compared to the experimental result from de Vries et al. [17] and to the numerical data from Avallone et al. [3]. Our numerical simulations underpredicted the experimental thrust coefficient by about 11% and deviates by about 3% from LBM results from Avallone et al. [3]. In spite of the deviations from the measurements, considering that we simulated the isolated propeller instead of the whole apparatus, the results are consistent with the literature.

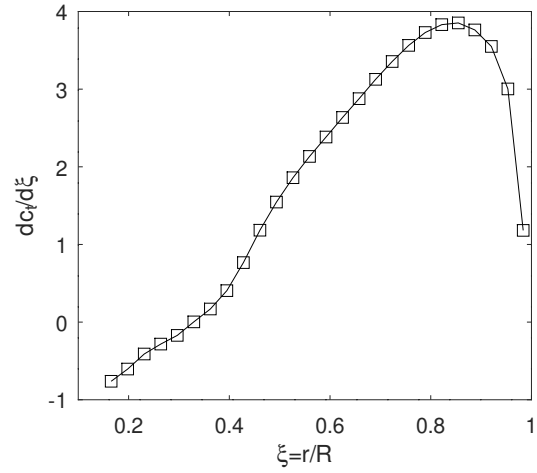
Table 1: Comparison of aerodynamic coefficients of thrust and torque

	Present	Exp. - [17]	Num.- [3]
c_t	0.0842	0.095	0.087
c_q	0.0167	-	-

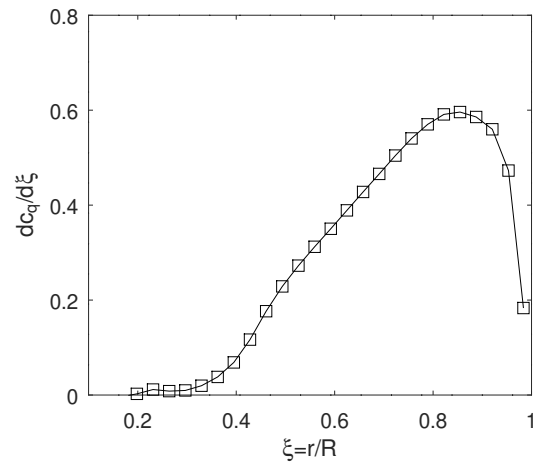
Figure 7 presents the distributions of thrust and torque coefficients as function of the non-dimensional radial position along the blade $\xi = \frac{r}{R}$. The distribution were obtained following the procedure described in Subsection 2.4. As mentioned, these results are used as input data for both noise prediction models.

3.2 Acoustic Results - OpenFOAM

The comparison of SPL spectra obtained from both FWH surfaces, for different directivity angles, is depicted in Figure 8. Each plot presents



(a)



(b)

Figure 7: Radial distribution of elemental thrust (a) and torque (b) coefficients.

the spectra considering positive angles, denoted by $+\theta$, and negative angles, denoted by $-\theta$. Regarding the blade passing frequency (BPF), results computed from different surfaces are very similar. However, for the second harmonic, in some observer positions, not only there are significant differences between S1 and S2, but also the peak levels differ significantly between positive and negative angles. This is an inconsistent result, since the problem should be symmetric.

It is not clear what caused these disparities in the second peak. They could be caused by spurious effect of the wake crossing the surfaces or errors induced by asymmetries of the tetrahedral mesh. Further studies of grid refinement and shape of the FWH surface should be addressed. Nevertheless, these effects do not significantly affect the peak levels of the first BPF for the considered directivity range. As show in Figure 9, minor

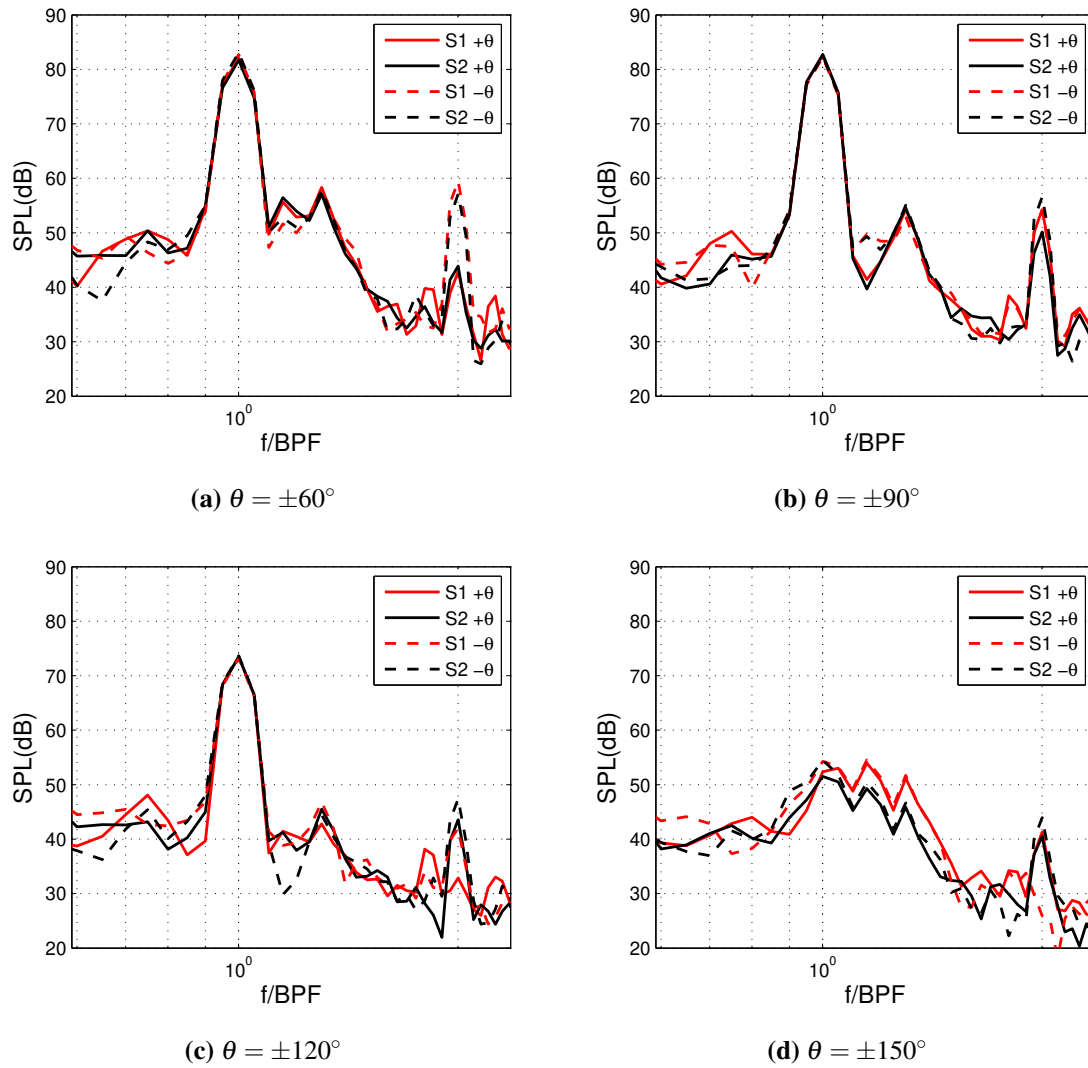


Figure 8: Comparison of SPL spectra computed from different surfaces.

differences can be noted between results from different surfaces in positive and negative angles. For the subsequent analyses only surface S2 was used.

Figure 10 depicts the comparison between SPL spectra in 1/12-octave bands obtained via URANS/FWH with those obtained from LBM by Avallone et al. [3]. It is important to emphasize that Avallone et al. [3] considered an pylon-mounted installed propeller, thus we included spectra for microphones in the retreating and advancing blade sides of the pylon. Due to the nature of URANS modeling, we do not expect to predict the broadband sources and our comparison will be limited to tonal levels. At $\theta = 75^\circ$ (Figure 10) the level of the first harmonic differs by about 5.5 dB between the URANS and LBM. At $\theta = 100^\circ$ the differences are about 6dB with respect to the

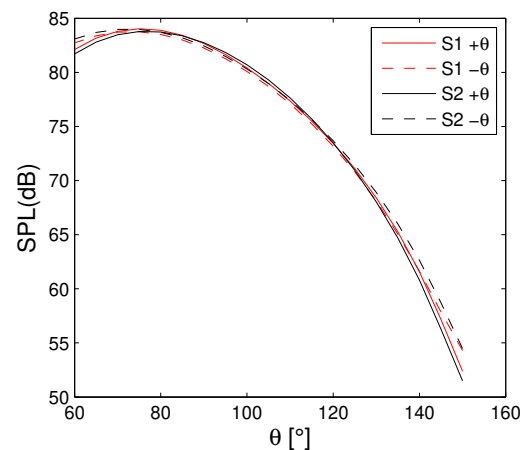


Figure 9: SPL at the BPF computed from different surfaces at different directivity angles.

advancing-blade side and 1.5 dB with respect to the retreating-blade side. Differences are higher than 8 dB for the second harmonic.

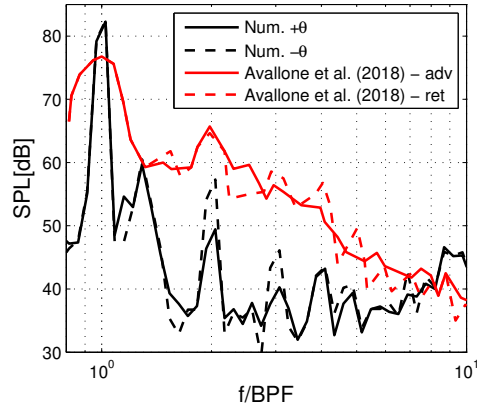
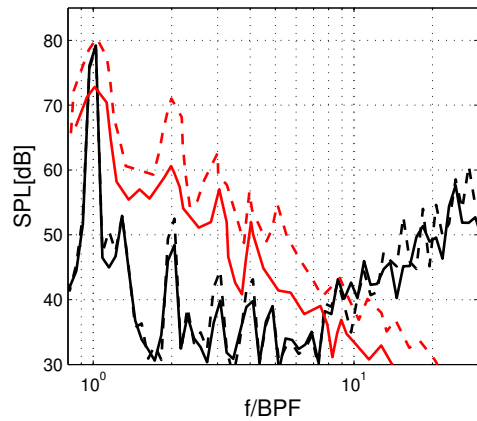
(a) $\theta = \pm 75^\circ$ (b) $\theta = \pm 100^\circ$

Figure 10: Comparison of SPL spectra in 1/12 octave bands with those from Avallone et al. [3]

Even though the comparison between the different simulations is inconclusive, since we compare and isolated propeller with an installed one, results for the BPF tone are consistent with the reference high-fidelity simulations. Comparisons with experimental data are required to assess the real accuracy of the modeling and will be addressed in future studies.

3.3 Acoustic Results - Semi-analytical

The results from the tonal noise prediction models are presented in Figure 11 considering only the blade-passing frequency. In Figure 11(a), results from the Gutin [12] and Deming [13] model (GD) are presented, considering the total noise, as well as the individual contributions of loading and thickness noise. The predictions from the CFD/FWH simulations are used as basis of comparison. The differences between the total SPL and CFD results vary from 1dB at $\theta = 60^\circ$ to 8dB at $\theta = 150^\circ$.

Figure 11(b) depicts results for Barry and Magliozzi [14] model (BM). In this case, the differences between the semi-analytical and CFD results vary from 3dB at $\theta = 60^\circ$ to 14dB at $\theta = 150^\circ$. When comparing both the total SPL from both models, those from the GD model were closer to the CFD/FWH predictions. Nevertheless, both models failed to reproduce the directivity trend.

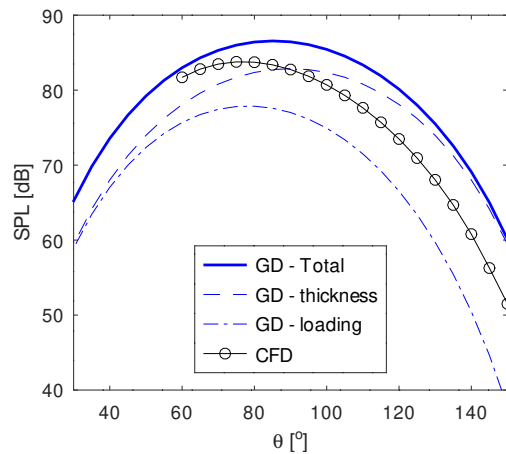
By looking at the individual contributions of loading and thickness noise, we can note that the loading noise models were capable of reproducing the directivity from the CFD predictions. Also it is suggestive that an overprediction of the thickness component by the analytical models may be the responsible for the overestimation of the total SPL. It is important to mention that errors might be present since we are using the far-field version of the equations to predict noise at a radial distance of $2D$.

4. CONCLUSIONS

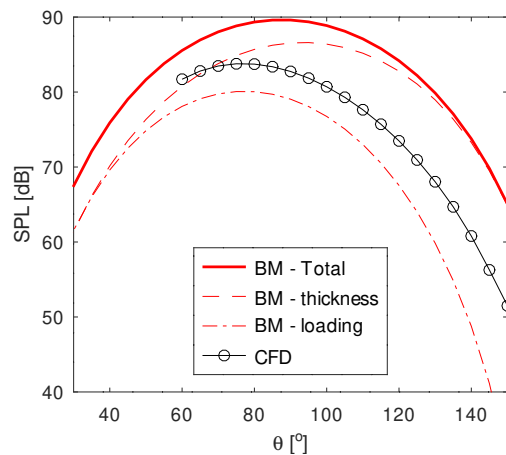
We presented a simulation model to predict tonal noise and aerodynamic characteristics of isolated propellers using opensource tools. In general, aerodynamic and acoustics results in reasonable agreement with experimental and numerical data in the literature. Tonal noise results for the blade-passing frequency did not show significant sensitivity to the size of the FWH surfaces.

Predictions from the analytical models showed higher tonal noise levels at the BPF in comparison to the CFD predictions. The reason for these disparities are possibly due to an overprediction of the thickness noise component by the employed methods. Nevertheless the computational cost of such models is significantly lower, specially if coupled with analytical approaches, e.g. blade element momentum theory, for the estimation of thrust and torque distributions.

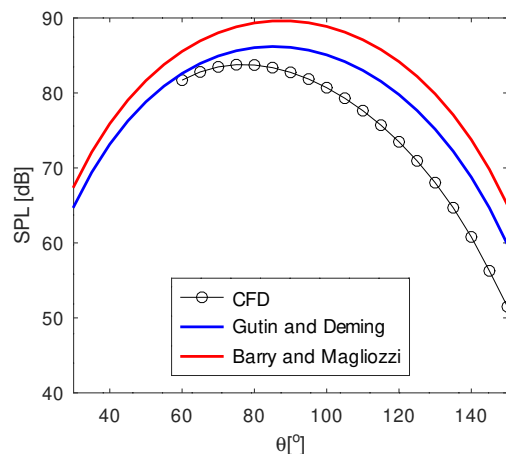
Future works will be directed to improve the simulation model by performing sensitivity tests to grid refinement. Comparisons with experimental results will be performed to assess the accuracy of the CFD simulations. In addition, more effort will be directed to understand the errors of the analytical approach.



(a) Gutin [12] and Deming [13]



(b) Barry and Magliozzi [14]



(c) Both models

Figure 11: Predictions of the tonal SPL at the BPF through different modeling approaches.

5. ACKNOWLEDGMENTS

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