

# PREDICTIONS OF ROTOR BROADBAND NOISE

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# Declaration

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# Abstract

This thesis presents an enhanced method for predicting aerodynamically generated broadband noise, produced by rotating machinery. The method improves on existing work for Vertical Axis Wind Turbine (VAWT) noise prediction, but is formulated to be applicable to a host of fan and rotor configurations. Recently developed analytical and semi-empirical airfoil noise models for inflow-turbulence and self-noise mechanisms are considered. Models were applied to a spatially and temporally discretised computational domain in a stripwise manner. Airfoil noise predictions are dependent on aerodynamic input data, and analytic methods were derived to calculate the required input parameters. Comparisons to experimental noise measurements for an operational wind turbine are encouraging. Overall Sound Pressure Level (SPL) trends for changes in atmospheric inflow velocities, and rotor speeds, are captured. Time dependent Computational Fluid Dynamics (CFD) calculations were carried out to solve the aerodynamic solution for a newly designed six-bladed VAWT. The CFD flow solution was used as input data to inform the noise models and predictions were compared to benchmark results, produced using the analytical approach, to quantify errors between the two methods. A parameter study was performed, which shows the sensitivity of overall VAWT noise levels to changes in atmospheric inflow velocity and turbulence. Noise sources were characterised, and the location and mechanism of the primary sources was determined. The results show that inflow-turbulence noise is the primary source of aeroacoustic noise generated by a VAWT, and that up to 10 % turbulence intensity, atmospheric turbulence dominates blade generated turbulence. The approach shows that CFD can be leveraged to provide additional insight for noise predictions when using analytical noise models. The CFD based prediction approach was also applied to a small scale Contra Rotating Open Rotor (CROR), where overall broadband noise levels, as well as directivity patterns, are comparable to measurements.



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# List of Publications

The following is a list of key publications and presentations associated with this work

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- Botha JDM, Rice H, ‘A Novel Method of Vertical Axis Wind Turbine Noise Prediction, *10th European Congress and Exposition on Noise Control Engineering, Euronoise*, Maastricht, The Netherlands (May 2015)

“[Wind turbines are] *horrible looking structures. They make noise. They kill birds by the thousands. They’re really destructive. And I dont care who the environmentalist is; they are not good.*”

- *Donald J. Trump*





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# Nomenclature

## List of Symbols

|       |                                                   |
|-------|---------------------------------------------------|
| $A$   | Spectral Shape Function                           |
| $B$   | Spectral Shape Function                           |
| $c$   | Airfoil Chord Length [m]                          |
| $c_0$ | Speed of Sound [m/s]                              |
| $C_p$ | Coefficient of Performance                        |
| $C_f$ | Skin Friction Coefficient                         |
| $d$   | Segment Length [m]                                |
| $D$   | Directivity                                       |
| $f$   | Frequency [Hz]                                    |
| $f'$  | Perceived Frequency [Hz]                          |
| $G$   | Overall Acoustic Pressure Signal                  |
| $H$   | Heaviside function                                |
| $I_t$ | Turbulence Intensity [%]                          |
| $k$   | Turbulent wavenumber [/m]                         |
| $k_e$ | Wavenumber of the main energy bearing eddies [/m] |
| $k_t$ | Turbulent Kinetic Energy [ $m^2/s^2$ ]            |
| $K$   | Amplitude Correction [dB]                         |
| $l_m$ | Prandtl Mixing Length [m]                         |
| $L_t$ | Turbulent Length Scale [ $m$ ]                    |
| $M$   | Mach Number                                       |
| $M_0$ | Blade Inlet Mach Number                           |
| $M_c$ | Convected Mach Number                             |

|               |                                                  |
|---------------|--------------------------------------------------|
| $n$           | Number of Blades                                 |
| $n_\phi$      | Angular Discretisation                           |
| $o$           | Helical Twist [degrees]                          |
| $p$           | Blade Pitch [degrees]                            |
| $P$           | Pressure [Pa]                                    |
| $R$           | Turbine Radius [m]                               |
| <b>Re</b>     | Reynolds Number                                  |
| $r_n$         | Numerical Residual                               |
| $s$           | Blade Span [m]                                   |
| $S$           | Sears Function                                   |
| $S_{pp}$      | Farfield Acoustic Pressure Spectral Density [Pa] |
| $St$          | Strouhal Number                                  |
| $s_n$         | Sources Along Span                               |
| $ts$          | Number of Time Steps                             |
| $u_2^2$       | Turbulent Shear Stress                           |
| $u'$          | Turbulent Velocity [m/s]                         |
| $U_0$         | Incoming Flow Velocity [m/s]                     |
| $U_\infty$    | Domain Inlet Velocity [m/s]                      |
| $U_i$         | Boundary Layer Velocity [m/s]                    |
| $W$           | Farfield Sound Pressure Level [dB]               |
| $x_i$         | Blade Coordinate [m]                             |
| $z_i$         | Receiver Location [m]                            |
| $\alpha$      | Angle of Attack [degrees]                        |
| $\beta$       | Mach Number Correction                           |
| $\beta_i$     | Stretching Factor                                |
| $\Gamma$      | Gamma Function                                   |
| $\gamma$      | Amplitude Correction Factor                      |
| $\delta_{ij}$ | Kronecker delta                                  |
| $\delta$      | Boundary Layer Thickness [m]                     |
| $\delta^*$    | Boundary Layer Displacement Thickness [m]        |
| $\epsilon_t$  | Turbulent Dissipation Rate [ $m^2/s^3$ ]         |

|             |                                                   |
|-------------|---------------------------------------------------|
| $\zeta_i$   | Volume Fraction                                   |
| $\theta$    | Rotor Azimuth Angle                               |
| $\Theta_e$  | Blade Coordinate Angle                            |
| $\iota$     | Amplitude Correction Factor                       |
| $\kappa$    | Von Karman Constant                               |
| $\lambda$   | Tip Speed Ratio                                   |
| $\nu$       | Turbulent Viscosity [ $m^2/s$ ]                   |
| $\xi_e$     | Blade Coordinate Angle                            |
| $\rho_0$    | Air Density [ $kg/m^3$ ]                          |
| $\sigma$    | Rotor Solidity                                    |
| $\phi$      | Blade Azimuthal Angle                             |
| $\phi_e$    | Blade Coordinate Angle                            |
| $\phi_{22}$ | Energy Density Spectrum                           |
| $\Phi$      | Wavenumber Frequency Pressure Spectrum            |
| $\omega$    | Angular Frequency [ $rad/s$ ]                     |
| $\omega_R$  | Angular Velocity [ $rad/s$ ]                      |
| $\omega_t$  | Specific Dissipation Rate of Turbulence [ $1/s$ ] |

## List of Abbreviations

|             |                               |
|-------------|-------------------------------|
| <b>BEM</b>  | Blade Element Momentum        |
| <b>BPF</b>  | Blade Passing Frequency       |
| <b>BPM</b>  | Brooks, Pope and Marcolini    |
| <b>BVI</b>  | Blade Vortex Interaction      |
| <b>BWI</b>  | Blade Wake Interaction        |
| <b>CAA</b>  | Computational Aeroacoustics   |
| <b>CROR</b> | Contra Rotating Open Rotor    |
| <b>CFD</b>  | Computational Fluid Dynamics  |
| <b>DES</b>  | Detached Eddy Simulation      |
| <b>DMST</b> | Double Multiple Streamtube    |
| <b>EAM</b>  | Enhanced Amplitude Modulation |

|              |                                                                                                                    |
|--------------|--------------------------------------------------------------------------------------------------------------------|
| <b>EC</b>    | European Commission                                                                                                |
| <b>EU</b>    | European Union                                                                                                     |
| <b>FP-7</b>  | 7 <sup>th</sup> Framework Programme for Research and Technological Development                                     |
| <b>FW-H</b>  | Ffowcs-Williams Hawkings                                                                                           |
| <b>HAWT</b>  | Horizontal Axis Wind Turbine                                                                                       |
| <b>LBL</b>   | Laminar Boundary Layer                                                                                             |
| <b>LE</b>    | Leading Edge                                                                                                       |
| <b>LES</b>   | Large Eddy Simulation                                                                                              |
| <b>LFC</b>   | Low Frequency Correction                                                                                           |
| <b>OR</b>    | Open Rotor                                                                                                         |
| <b>RANS</b>  | Reynolds Averaged Navier Stokes                                                                                    |
| <b>ROBOT</b> | ROtor Broadband nOise prediction Tool                                                                              |
| <b>RPM</b>   | Revolutions Per Minute                                                                                             |
| <b>SNGR</b>  | Stochastic Noise Generation and Radiation                                                                          |
| <b>SPL</b>   | Sound Pressure Level                                                                                               |
| <b>SST</b>   | Shear Stress Transport                                                                                             |
| <b>SWIP</b>  | New innovative solutions, components and tools for the integration of<br>wind energy in urban and peri-urban areas |
| <b>SWT</b>   | Small Wind Turbine                                                                                                 |
| <b>TBL</b>   | Turbulent Boundary Layer                                                                                           |
| <b>TE</b>    | Trailing Edge                                                                                                      |
| <b>UDF</b>   | Unducted Fan                                                                                                       |
| <b>URANS</b> | Unsteady Reynolds Averaged Navier Stokes                                                                           |
| <b>VAWT</b>  | Vertical Axis Wind Turbine                                                                                         |
| <b>VS</b>    | Vortex Shedding                                                                                                    |



# Chapter 1

## Introduction

AN airfoil placed in a turbulent stream produces a quantifiable amount of acoustic radiation due to interactions between the inflow turbulence and the lifting surface itself. The phenomenon is not solely limited to wings in rectilinear motion; it is also evident in rotating machinery such as fans, propellers, and wind turbines. Research studies, both experimental and computational, have progressed the physical understanding of the mechanisms behind the noise source as well as realised the applicability of current theory to validated prediction models.

Noise radiation from an airfoil in a turbulent stream, known as aerodynamically generated, or aeroacoustic, noise is primarily broadband by nature and can be split into two general categories, namely; inflow-turbulence noise which results from interactions with oncoming turbulence coming into contact with a body in motion, Amiet [1] and; self-noise which is generated through fluid displaced volume propagating through a freestream as radiated sound as discussed by Brooks et al. [2].

Several methods exist to predict broadband aeroacoustic noise from rotating machinery. Isolated airfoil results are directly applicable in this case. Semi-empirical airfoil noise models can be coupled with low-order flow models to solve for noise radiation. Flow solutions within the rotor are solved using simplified analytic flow models, such as blade element momentum (BEM) theory. Thereafter semi-empirical or analytic models for airfoil noise [1–4] are implemented to calculate the farfield of the machine. These models have been readily applied to the prediction of rotating machinery noise, in particular to the problem of horizontal axis wind turbine (HAWT) noise where there has been good agreement with measurements [5, 6]. This type of approach continues to be used in HAWT design and development. Furthermore, this approach has a negligible computational expense when compared with high-order Computational Fluid Dynamics (CFD) based noise predictions.

Another noise prediction approach involves the use of high-order unsteady CFD methods. These methods make use of turbulence models such as large eddy simulation (LES) or detached eddy simulation (DES) in order to resolve small-scale acoustic pressure fluctuations within a domain. Applicable CFD simulations are coupled with integral methods such as that of Ffowcs Williams and Hawkings, based on Lighthill’s acoustic analogy [7–9] to solve for the acoustic solution. The relatively high computational cost of these methods

means that they are predominantly used only for academic studies [10, 11], with little application in a machines' design phase.

Tonal noise also contributes to the overall noise production of a rotor, and can arise at harmonics of blade passing frequencies (BPF), as a result of periodic rotation. This noise source is a primary contributor to overall aeroacoustic noise of rotating machinery, and often dominates the spectra for high velocity rotors. Whilst tonal noise is considered to be a predominant noise source for high speed rotors [12], it has also been seen that, at current technology readiness levels, the overall relative contribution of broadband noise should no longer be ignored [13].

Vertical axis wind turbines (VAWT) are a type of turbine where the main rotor is placed transverse to the incoming flow direction. VAWT's potentially require less maintenance and can be easily introduced into urban environments. These devices generate power by harvesting wind energy through the use of either drag, or lift producing blades. VAWT's are generally much smaller than HAWT's. Whilst the largest HAWT's can have rotor diameters over 100 m, a typical VAWT generally has a diameter of the order of less than 10m. VAWT's are also less aerodynamically efficient than comparable HAWT's, and encounter much higher levels of atmospheric turbulence during operation. A comparison of two small wind turbines is seen in Fig. 1.1.



(a) Lift type vertical axis wind turbine on Liverpool waterfront [14].



(b) Domestic horizontal axis wind turbine against blue sky [15].

**Figure 1.1:** Types of wind turbines.

Wind turbine noise can be divided into two primary groups, mechanical and aerodynamic noise [16]. Mechanical noise is produced by various components within the wind turbine nacelle, such as generators or gearboxes. This noise can be either tonal or broadband but is generally of no concern to the designer as it can be adequately treated using acoustic liners. This leaves the primary noise source emitted by wind turbines as aerodynamic noise; produced by the rotation of blades through air, and through turbulent air coming into contact with the blade surfaces. Due to the negligible impact of mechanical noise, prediction methods for wind turbines primarily focus on the accurate estimation of aeroacoustic sources. Comparisons between predictions and measurements of aeroacoustic noise sources have been favourable [5].

Integration of wind turbines into urban environments, in Europe, is subject to varying noise regulations. Regulations stipulate that any installed machinery should have a limited maximum noise level.

These regulations differ in various countries [17] and, thus, design of wind turbines is subject to the market in which they are to be deployed. However, most manufacturers of VAWT's do not perform detailed noise studies and provide only a single Overall Sound Pressure Level (OASPL) measurement on their datasheets quantifying adherence to requirements for noise levels in urban environments [18].

A contra rotating open rotor (CROR) is an aircraft engine concept which has recently gathered much interest in the research community due to its improved efficiency over turbofan designs. The engine comprises of a traditional gas turbine core which is used to power a set of unshrouded propellers rotating in opposite directions. Several such concepts were tested at full scale in the late 80's (see Fig. 1.2). The design of the propeller blades is closely coupled to that of the gas turbine resulting in blades of very short spans. These designs operate at lower velocities than turbofan engines but with higher fuel burn efficiency.

The dominant noise source produced by CROR's is tonal in nature, however, as a result of their high rotational velocities and turbulent working environments, they also produce aeroacoustic broadband noise by means of several mechanisms [12] namely; turbulence ingestion noise, produced when turbulence from the free stream comes into contact with the forward rotor; blade vortex interaction noise, when an upstream vortex comes into contact

with a downstream blade; rotor-rotor interaction noise, due to the effect of turbulent wakes impinging on the leading edge of downstream elements and; airfoil thickness and loading noise, caused when boundary layer turbulence convects into the freestream over a sharp trailing edge.



**Figure 1.2:** General Electric contra rotating open rotor as seen at Farnborough, 1988 [19].

Both CROR's and VAWT's operate in unsteady flow conditions. As the machines operate, turbulent fluctuations and aerodynamic interactions between blades can be seen [20, 21]. These unsteady fluid forces acting on the blades can drive the production of unsteady aeroacoustically generated noise.

In this study a computational method for the prediction of aeroacoustically generated broadband noise, produced by rotating machinery, is proposed. These machines operate in highly turbulent environments and this turbulence drives unsteady broadband noise. The method makes use of semi-empirical and analytic models for the prediction of isolated airfoil noise. Seeing as CFD calculations can be used to efficiently (and more accurately) predict flow features including turbulence parameters, these models are coupled with CFD calculations to improve the accuracy of noise predictions. This approach realises the applicability of a hybrid noise prediction method for aerodynamically generated broadband noise from rotating devices.

## 1.1 Objectives

The primary aim of this study is related to the computational prediction of noise from rotating devices. In order to achieve this, a number of objectives are defined:

1. Improving on current methods for turbine noise prediction and applying these approaches to the prediction of noise from machinery operating in highly turbulent environments.
2. Analysing the applicability of using low cost unsteady CFD computations as input data to existing noise prediction models.
3. Verifying developed methods against existing measurement data. Verified methods can be used for parameter studies to determine the effect of design changes on overall noise levels.

## 1.2 Thesis Outline

This thesis comprises results and analysis for the computational prediction of noise from two types of rotating machinery. The chapters of this thesis following on from this introduction are as follows;

- Chapter 2** explores the key literature pertaining to noise produced by airfoils. In addition to this, the noise mechanisms of various types of rotating machinery are discussed. This literature is analysed for its applicability to the present research.
- Chapter 3** describes the methodology employed to predict rotor noise for a VAWT. Relevant models for the prediction of airfoil noise are presented.
- Chapter 4** presents a noise prediction study, and results, for an existing VAWT design using an analytic flow model. The basic flow model is described, and results are verified against available measurements.

**Chapter 5** presents a noise prediction study, and results, for a new VAWT design using a CFD based flow model. Discussions related to data extraction are included, and sensitivity studies are performed. Results are benchmarked against those from the previous chapter.

**Chapter 6** uses a version of the methodology from **Chapter 3** that has been modified to predict noise from a CROR. A CFD study is performed to provide input data. Results are verified against available experimental data.

**Chapter 7** concludes the results of the research and highlights work for the future.





## Chapter 2

### Literature Review

THE prediction of noise from airfoils is a well known problem and there is much literature directed towards understanding the nature of the problem and its application to practical design. However, literature related to the measurement and prediction of noise radiation by vertical axis wind turbines is sparse. The majority of work is geared towards the prediction of more conventional horizontal axis machines. Similarly, the prediction of broadband noise from high speed propellers is overshadowed by literature related to tonal noise predictions. This section considers literature relevant to the prediction of aeroacoustic noise from rotating machinery. The fundamentals of aeroacoustic theory related to sound radiated by moving surfaces are discussed, followed by practical application of theory to the prediction of airfoil noise. Several mechanisms of airfoil noise as well as competing theories are presented. Wind turbines and open rotors are both considered and their operation is reviewed. Furthermore, results derived from current noise investigations, both experimental and computational are shown.

## 2.1 Aeroacoustics

Aeroacoustics refers to a branch of acoustics interested in studying noise generated by fluid flows. These noise sources are generated as a result of flow interactions with bodies or by means of an existing turbulent flow propagating through a fluid medium. Aeroacoustics can be used to describe noise radiation from an aeroacoustic source or the propagation of sound waves in an inhomogeneous flow field. Source modelling, prior to the introduction of acoustic analogies, was traditionally aimed at characterising frequency components of a sound field. The introduction of acoustic analogies brought about the ability to determine the intensity of these components, too. Current source modelling techniques allow both amplitude and frequency content to be determined.

### 2.1.1 Lighthill's Analogy

The most complete explanation for the generation of aeroacoustic source noise is attributed to Lighthill and his theories [8, 9]. Lighthill provided an analogy which described flow induced noise as a particle based problem with propagation through a medium represented as a wave. This theory of noise is an analogy which means that, although the equation is

exact and derived without approximation, it is only one in which the acoustic terms are replaced by representative fluid terms. As a result of the fluid mechanics based approach, detailed knowledge of the flow solution is required prior to calculation of the acoustic signal.

Theoretically this analogy is a rearrangement of the traditional Navier-Stokes Equations [22] and makes use of only four equations namely; the mass conservation equation and the three components for the conservation of momentum. In deriving the Lighthill analogy an assumption is made that the listener is at a farfield location, at this location the fluid in the vicinity of the listener is uniform and stagnant. A complete derivation of the analogy can be seen in Rienstra and Hirschberg [23].

The Lighthill equation as seen in [23] is defined as

$$\frac{\partial^2 \rho'}{\partial t^2} - c_0^2 \frac{\partial^2 \rho'}{\partial x_i^2} = \frac{\partial^2 T_{ij}}{\partial x_i \partial x_j} + \frac{\partial^2 \zeta_i \rho_f}{\partial t^2} - \frac{\partial f_i}{\partial x_i} \quad (2.1)$$

where  $c_0$  is the speed of sound at the *listener's location*,  $\zeta_i$  is the volume fraction appended due to properties of mass injection,  $\rho_f = \rho_0 + \rho'$  is density term of the steady ( $\rho_0$ ) and unsteady (time dependant) ( $\rho'$ ) fluctuations, and  $f_i$  is an external applied force. The right hand side of the equation represents a distinct source term representing the production of radiated sound whilst the left hand side takes the form of a classical wave equation.

The subscript f refers to the external force density, whilst the subscript  $m$  denotes the mass source terms. This means that the full density of the fluid is defined as an equilibrium of the injected and original fluid mass governed by the volume fraction,  $\zeta_i$ ,

$$\rho = \zeta_i \rho_m + (1 - \zeta_i) \rho_f \quad (2.2)$$

Then, Lighthill's stress tensor,  $T_{ij}$ , is defined by

$$T_{ij} = \rho v_i v_j - \tau_{ij} + (p' - c_0^2 \rho') \delta_{ij} \quad (2.3)$$

where equation (2.3) is the usual form of the Lighthill's stress tensor as seen in literature.

This stress tensor describes three basic aeroacoustic processes which result in sound generation namely:

- Non-linear convective forces described by the Reynolds stress tensor  $\rho v_i v_j$  where  $v$  represents fluid velocity.
- The viscous forces  $\tau_{ij}$ .
- The deviation from a uniform sound velocity  $c_0$  or the deviation from an isentropic behaviour  $(p' - c_0^2 \rho')$  where  $p$  is fluctuating fluid pressure.

Lighthill described sound sources in terms of three different terms: monopole, dipole and quadrupole sources. These terms represent noise from mass, momentum and flux sources respectively and are listed in order of decreasing acoustic efficiency. The aforementioned Lighthill equation was derived with respect to free radiating noise without the influence of solid bodies.

### Sound Generation by Rigid Bodies

In order to account for more efficient solid-body radiators (as opposed to free radiating quadrupole sources) a new formulation of the Lighthill equation was considered by Curle [24] who further developed aeroacoustic theory by definition of the so called Curle analogy, a formal solution of the Lighthill analogy which takes hard surfaces into account. By incorporating additional source terms into the Lighthill equation, Curle was able to determine a method for the prediction of sound generated by fluids coming into contact with hard surfaces.

Dimensional analysis by Curle shows the intensity of sound to vary as:

$$\begin{aligned} I_Q &\sim \rho_0 U_0^8 c_0^{-5} L^2 \\ I_D &\sim \rho_0 U_0^6 c_0^{-5} L^2 \end{aligned} \tag{2.4}$$

where subscripts  $Q$  and  $D$  represent quadrupole and dipole sources respectively,  $U_0$  is a typical flow field velocity and  $L$  is a length scale, in this case typically the dimension of a body. This type of dimensional analysis is important in grasping basic, fundamental, relationships between quantities for acoustics. For example, in the case of quadrupole sources one can see that sound intensity will increase rapidly with changes in velocity whilst changes in length scale will not yield the same rate of increase. This type of analysis can guide low noise design at a very rudimentary level.

### 2.1.2 Sound Generation by Rigid Bodies in Motion

To account for sound generated by rigid bodies in motion, Ffowcs-Williams & Hawkings derived the Ffowcs-Williams Hawkings (FW-H) equation [25]; a rearrangement of the exact continuity and Navier-Stokes equations for the purpose of calculating aeroacoustic sources for objects in motion with respect to hard walls [7]. The equation makes use of the time histories of fluctuating pressure components of a flow in order to relate these pressure signals to acoustic ones.

Furthermore, to solve the FW-H equation a surface and volume integral are required, this surface is arbitrary but should be defined sufficiently close to acoustic sources (eg: turbulence) in order to capture signals from these locations.

The FW-H equation in differential form is written as equation (2.5):

$$H(f)\rho' \left[ \frac{\partial^2}{\partial t^2} - c_0^2 \frac{\partial^2}{\partial x_i \partial x_i} \right] = \frac{\partial^2}{\partial x_i \partial x_i} (T_{ij}H(f)) - \frac{\partial}{\partial x_i} (F_i \delta(f)) + \frac{\partial}{\partial x_i} (Q \delta(f)) \quad (2.5)$$

Where:

$$T_{ij} = \rho u_i u_j + P_{ij} - c_0^2 \rho' \delta_{ij} \quad (2.6)$$

$$F_i = (P_{ij} + \rho u_i (u_j - v_j)) \frac{\partial f}{\partial x_i} \quad (2.7)$$

$$Q = (p_0 v_i + \rho (u_i v_i)) \frac{\partial f}{\partial x_i} \quad (2.8)$$

$T_{ij}$  in (2.6) is the previously defined Lighthill stress tensor which, in this equation, defines the *quadrupole term*. The *dipole term* is expressed as  $F_i$  (equation (2.7)) and  $Q$  (equation (2.8)) is the *monopole term*. These additional terms describe all possible sources of sound unlike the Lighthill analogy which negates dipole and monopole sound production assuming negligibility.  $\delta_{ij}$  is the Kronecker delta function which is 1 when  $i = j$  and 0 otherwise.  $H$  is the Heaviside function which equals 1 for  $f > 0$  and 0 for  $f < 0$ .

### Application of the Ffowcs Williams-Hawkings equation

The Ffowcs Williams-Hawkings equation was initially derived in differential form and not easy to solve using computational methods. In order to apply the equation in a computational scheme such as a CFD solver, Farassat proposes a solution to the equations in integral form. The most common usable form of this derivation is referred to as FW-H formulation 1A [26] which is a solution to the FW-H equation with the condition that surface sources exist only when the surface moves at subsonic speed. Formulations 1 and 1A have both been used for the prediction of helicopter rotor and propeller noise prediction. Thickness and Loading sources are produced by a body moving through flow. An additional non-negligible quadrupole source will also be produced by this body, however, in order to account for these terms a porous data surface is introduced to account for any non-linearities in the region of a moving body. The definition and location of this surface, however, is still subject to the particular problem at hand.

Formulation 1A in integral form is defined as

$$\begin{aligned}
 4\pi p'(\mathbf{x}, t) = & \\
 & \int_{f=0} \left\{ \frac{1}{1 - M_r} \frac{\partial}{\partial \tau} \left[ \frac{\rho_0 v_n}{r(1 - M_r)} + \frac{p \cos \theta}{cr(1 - M_r)} \right] \right\}_{\tau_e} dS \\
 & + \int_{f=0} \left[ \frac{p \cos \theta}{r^2(1 - M_r)} \right]_{\tau_e} dS
 \end{aligned} \tag{2.9}$$

where  $M_r$  is the Mach number of a point in the domain in the radiation direction at the time  $\tau$ ,  $c$  is the undisturbed speed of sound,  $r$  is the distance between the source and the receiver,  $\theta$  is the local angle between normal to the surface and radiation direction,  $\tau_e$  is the emission time,  $\rho_0$  is the undisturbed fluid density,  $v_n$  is local normal velocity of the surface. This equation can be further derived to express explicit terms for thickness and loading noise respectively.

Formulation 1A of the FW-H equations fully describes the aeroacoustic noise sources pertinent to rotating machinery such as a machine, consisting of moving hard walls (airfoils) operating in a fluid flow.

### 2.1.3 Computational Aeroacoustics

Computational Aeroacoustics (CAA) is a subset of generalised aeroacoustic theory that analyses the problem of aerodynamically generated noise by making use of numerical methods. These methods are based on the aeroacoustic theory by Lighthill and many computational methods incorporate the formulation by Farassat [26] as an acoustic solver.

A number of tools exist to perform computational aeroacoustic predictions, these are based on a variety of methods - many of which are related to CFD. This thesis will focus on CAA methods that are based on CFD as governed by the Navier-Stokes equations [22, 27]. There are also non Navier-Stokes based tools emerging at present such as the Lattice-Boltzman method which make use of injected particles instead of computational grids [28].

#### Direct Methods

CFD based CAA tools implement some form of the acoustic analogy (FW-H equation) in which fluctuating pressure terms are captured and propagation to a farfield receiver is calculated. In order to capture fluctuating pressures, a time based unsteady discretisation scheme needs to be employed.

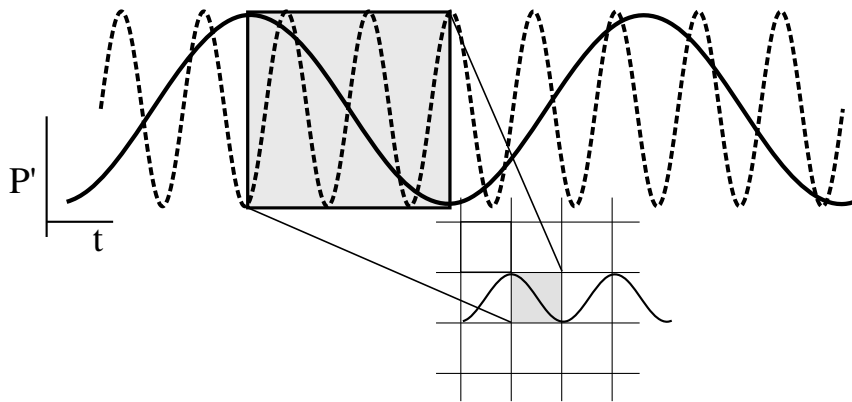
By solving an aerodynamic problem with a highly refined computational grid one can attempt to solve the Navier-Stokes equations without any form of turbulence model. This approach, known as direct numerical simulation (DNS), is able to resolve all scales of turbulence both spatially and temporally meaning that the smallest scales of fluctuating pressure that cause acoustic perturbations are captured.

Direct methods are out of the scope of current computing abilities for problem sizes of any practical scale and, as a result, turbulence models such as Large Eddy Simulation (LES) exist to change the scales of the problem. This CFD based turbulence model aims to reduce computational cost by ignoring very small length scales and only modelling the larger turbulent length scales in flows. In this way most of the energy bearing eddies within the flow are directly simulated thus providing a compromise of computational cost and solution accuracy.

## Dispersive and Dissipative Errors in CAA

Results derived from CFD calculations will, over time, tend towards an averaged solution. Even the most robust codes introduced in the time domain will include result dispersion and dissipation due to the necessary upwinding schemes introduced in the computational implementation of the Navier-Stokes equations.

Fig. 2.1 represents a numerical grid defined to calculate an unsteady CFD computation. The grid is node based and has been set up to accurately resolve a pressure fluctuation for an aerodynamic solution both temporally and spatially, this is represented by the solid line passing through the single cell. The fluctuating pressure is captured accurately; observed as a single wavelength (peak to trough) passing through two nodes of the cell. Consequently, if a CAA simulation were to be performed using this grid, the ratio of ambient to solution pressure would need to be of the order of  $1 \times 10^6$  [29]; that is to say that the resolved pressure scales required for acoustic simulations need to be sufficiently small. The correctly resolved pressure solution for acoustic signals is represented as the dashed line - in this case the solver is unable to capture all peaks and troughs of the acoustic pressure fluctuation travelling through the cell. If this particular grid were to be used for CAA simulations, both the time step and cell size would need to be reduced to capture the small scale pressure fluctuations which, at present, are not captured in this particular case.



**Figure 2.1:** A representative computational grid showing two fluctuating pressure signals travelling through a single cell in a computational domain. — represents a correctly resolved acoustic signal whilst; - - - represents a higher frequency signal not captured in the cell.



Due to this dispersive and dissipative nature, error terms are introduced into and convected through, a numerical grid that can offset the accuracy of the captured small scale pressure fluctuations. The dispersive error terms in CFD introduce a numerical viscosity to the fluid domain [30]. This numerical phenomenon becomes problematic for computational aeroacoustic simulations when weighing up computational cost against solution accuracy.

For the problem of rotor noise, CAA based solutions can yield detailed results. However, due to computational overheads these approaches are not particularly useful for design problems or optimisation.

### Hybrid Approaches

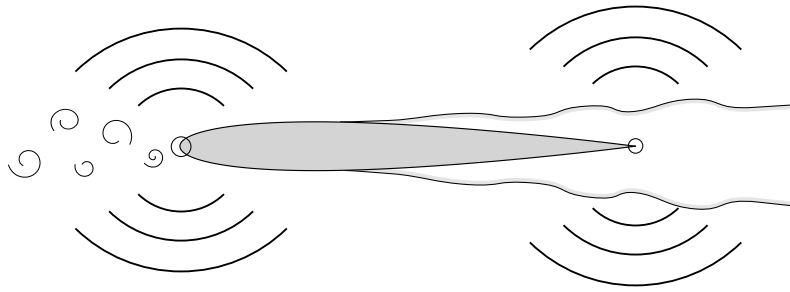
Hybrid methods perform a trade-off between acceptable CFD and acceptable acoustic results. These types of approaches can make use of a combination of CFD and CAA where the problem may deem alternating approaches necessary. By reducing the amount of CFD analysis required for aeroacoustic analysis it is possible to reduce the effect of error from dispersion and dissipation. Furthermore, by applying alternative acoustic solution methods one can circumvent the need for unnecessarily detailed flow solutions.

The Stochastic Noise Generation and Radiation (SNGR) method [31] is able to synthetically generate turbulent fields based on the known turbulence in the steady flow field. This is performed on a stochastic basis using statistical descriptors. After the mean turbulence is known, the linear Euler equations can be applied to the field on a temporal and spatial basis with a collection of discrete Fourier modes. The procedure has been seen to work well for jet noise predictions. However, for loading noise from aeroacoustic sources, the turbulence cascade is not correctly reproduced, resulting in reductions in the time correlation in the turbulent velocities necessary to define broadband noise [32].

Multi-domain approaches can make use of a single computational grid where the domain is split into multiple regions. Each region can be governed by a different set of equations such that aeroacoustic solvers are only required in regions where acoustic analysis is deemed necessary.

## 2.2 Airfoil Noise Mechanisms and Prediction

Airfoil noise can be separated into two distinct categories namely inflow-turbulence noise and self-noise. These two sources occur by means of different mechanisms. In Fig. 2.2 a lift generating airfoil is presented schematically in an arbitrary turbulent flow. Two distinct sources are seen to radiate outwards from the airfoil body, with one source radiating outward from the leading edge and another from the trailing edge. Leading edge noise, known as inflow-turbulence noise is produced as a solid body (in this case an airfoil) comes into contact with unsteady turbulence. Trailing edge noise is produced as a product of the interaction between an airfoil and flow produced in its own boundary layer.

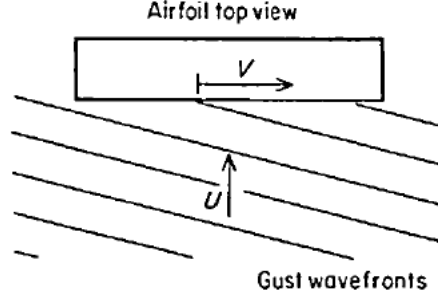


**Figure 2.2:** The locations of the primary broadband noise sources on an airfoil. Leading edge noise is produced by inflow turbulence and trailing edge noise produced by boundary layer/trailing edge interactions.

### 2.2.1 Inflow-Turbulence Noise

In theory an airfoil in a turbulent field that experiences a fluctuating lift force should generate sound - a valid proposition based on an extension of Lighthill's general aeroacoustic theory incorporating solid boundaries [24]. This type of prediction becomes increasingly more complex when additional flow features, such as skewed gusts (as seen in Fig. 2.3) and rectilinear motion, are incorporated. Amiet [1] defined a theoretical expression for the farfield noise produced by a finite airfoil with an incident gust. This theory was based on the assumption that the spanwise correlation lengths of turbulence (turbulent length scales) were smaller than the span of the airfoil i.e. for airfoils with an infinite span. Amiet's model describes noise of airfoils that are modelled as flat plates with zero incidence angle. The model was further developed and validated to include compressibility

effects as well as non-compactness of sources. These corrections were added by Paterson and Amiet [3] to obtain more accurate directivity predictions for airfoil noise. The model also included asymptotic formulations for both high and low frequency noise so that each regime was handled separately. A switching function was introduced which changed the directivity model used based on the wavelength and velocity of the incoming flow.



**Figure 2.3:** Airfoil encountering skewed gust. [1]

A scaling relationship was introduced by Glegg et al. [33] to use the models of Amiet for the prediction of noise produced by horizontal axis wind turbines. It was suggested that, at high length scale values, the models did not match measured data. There was a noticeable 10 dB difference between theory and measurement. A workaround for this was to set the inflow turbulence length scale to the airfoil chord length. Glegg stated that inflow noise was found to be the dominant source of noise in wind turbines. Further formulations for the problem of wind turbine noise were performed by Lowson [34]. In his studies, Lowson, described trailing edge noise as the dominant noise source. A blending function was also incorporated by Lowson [34] to apply the formulations for the high and low frequency equations in a more continuous manner. In this approach, it was assumed that the incoming flow velocity at the blade was  $2/3$  as fast as the measured freestream wind speed, to account for what is referred to as ‘deceleration at the turbine’. This approach provided results that matched measurements, but this was seen as a non-physical workaround. Future work, making use of more accurate flow models such as blade-element momentum theory (BEM) [35] showed that whilst crude, this estimate was reasonably accurate.

Further minor corrections to the inflow noise model were provided by Santana et al. [36], who provided a means for accounting for compact leading edge noise prediction. This correction referred to the low frequency portion of the spectrum, where gust wavelengths

are about the same size as the airfoil's chord. The author applied corrections to the predictions after using the Amiet model with relative success and compared these to a second more accurate model. Amiet's models are all based on noise produced by flat plates, to account for a finite thickness airfoil. Moriarty et al. [37] provided an expression to account for the difference in sound power level between an airfoil and a flat plate. This minor correction, however, still required an empirical constant of 10 dB to be added to the final results to match predictions with measurements.

In an internal document by McMahon and Rice [38], a conglomeration of errors in the practice of application of turbulent-inflow noise theory was discussed, and the implication of these errors quantified. It was seen that, in documents such as [34], as well as the technical documentation for the FAST code [39], several errors exist whereby inconsistent units used in the turbulent-inflow noise models could lead to inaccuracies ranging from 10 to 30 dB. These anomalies seem to persist due to the lack of small wind turbine experimental data. Many codes are verified against noise measurements for large turbines, where trailing edge noise dominates [5]. Errors could arise in these cases since operation in turbulent environments could lead to dominance by leading edge noise.

More recently, Sinayoko and Hurault [40] provided a review of Amiet's theories as implemented computationally throughout the years, with a focus on reporting key difficulties found with the implementation of the method. The key difficulties were reported to arise from the following criterion:

- For isolated airfoils, Amiet's model is described in terms of the pressure jump that the airfoil encounters (the pressure that the airfoil blocks) whilst other authors have used the incident pressure field that the airfoil develops (ie. the pressure that would exist without an airfoil/plate in a flow). The former method is more useful as it can be measured using surface pressure probes and thus validated experimentally.
- A singularity in the acoustic lift formulation in Amiet's model. The singularity helps to describe the problem of amplitude modulation but falls short of providing a full prediction model as it is a non-physical descriptor. Corrections are suggested by Roger and Moreau [41].
- For higher Mach numbers the exponent in the Doppler shift correction is suggested

to be altered.

Sinayoko concluded that, for inflow noise, there is some uncertainty over the true magnitude of the noise and that the calculation is very sensitive to the level of turbulent intensity, but not that sensitive to the integral length scale of the turbulence. The latter is a valid proposition, which can be supported by looking at the order of magnitude estimates of Curle [24] (see equation (2.4)) as well as the exponents attributed to these values within the Amiet model.

Many inflow noise models are based on the assumption that airfoil noise follows the form of the von Kármán spectrum of turbulence. Models scale results to fit this spectra and reasonable accuracy in predictions is observed. Buck et al. [6] replaced the von Kármán spectra in Amiet's model with a Kolmogorov turbulence spectra, meaning that atmospheric quantities of dissipation can be measured instead of the usual length scale and intensity values. This approach allows for easier validation when compared to surface pressure measurements thus aiding in minimising the key difficulties attributed to the Amiet model. Results show good agreement with measurements.

All of these noise models assume turbulence to be frozen as per Taylor's hypothesis - this is to say that statistics of a turbulent field remain unchanged as that field advects downstream, provided that the ratio  $u'/U < 1$ . Practically this assumption means that turbulence convecting downstream across a blade interacts with the blade as a one-way process. The turbulence can affect the blade but there is no assumed feedback, meaning that the wall never alters the turbulent field. Branlard et al. [42] confirmed, by means of aerodynamic experiment, that, for large wind turbines, the frozen turbulence assumption holds.

### 2.2.2 Airfoil Self-Noise

Self-noise is generated by the interaction between an airfoil and convecting boundary layer flow passing over the trailing edge. From a fluid mechanics perspective, this occurs due to interactions between an airfoil and the turbulence produced in its own boundary layer, and near wake. As an airfoil travels through a fluid medium it displaces an amount of fluid equal to the volume that it occupies in space. The displaced fluid radiates outwards through the medium as sound. Self-noise is the overall noise produced by the airfoil itself

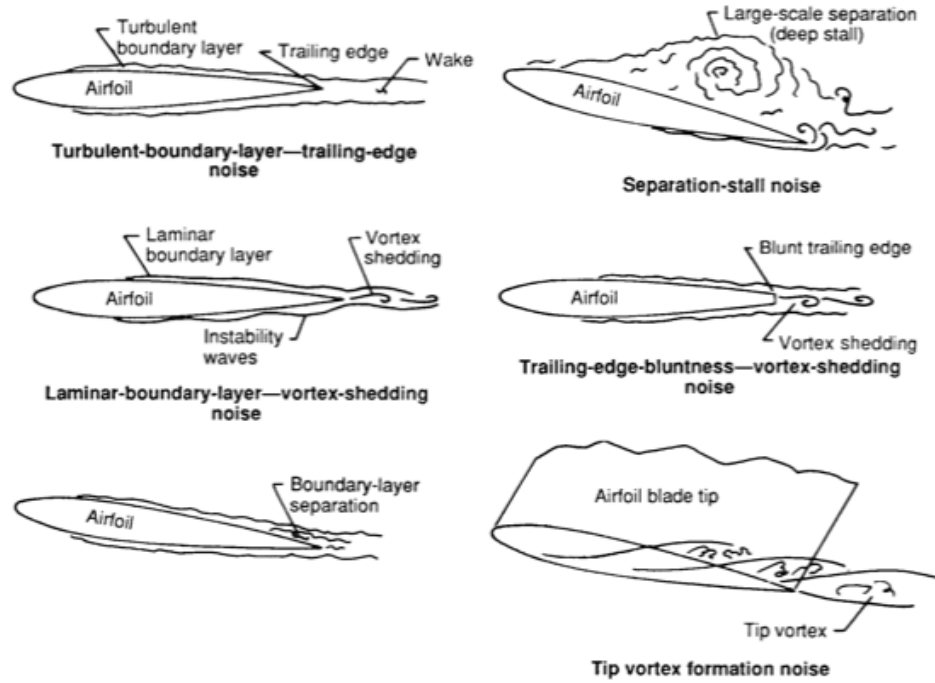
as it encounters smooth non-turbulent inflow. According to Brooks et al. [2], several mechanisms of self-noise exist [43] as seen in Fig. 2.4 as follows:

1. **Turbulent Boundary Layer - Trailing Edge Noise:** At high Reynolds number, there is a Turbulent Boundary Layer (TBL) along most of the airfoil. Noise is produced as turbulence passes over the Trailing Edge (TE).
2. **Separation Stall Noise:** At angles of attack post stall flow can separate near the TE on the upper surface to produce TE noise due to the shed turbulent vorticity.
3. **Laminar-Boundary-Layer Shedding (LBL-VS) Noise:** At low Re, there is mostly a Laminar Boundary Layer (LBL) along the airfoil. The instability of this boundary layer results in Vortex Shedding (VS) and noise from the TE.
4. **Trailing-Edge-Bluntness Vortex-Shedding Noise:** A blunt TE can produce vortex shedding and associated noise.
5. **Tip Vortex Formation Noise:** Tip vortices from wing tips (or propeller blades) produces aerodynamically generated noise.

Noise sources (1-2) are the primary broadband sources produced by airfoils in flow, particularly for rotor blades. These two sources describe noise generated by an airfoil at reasonable Reynolds numbers, and general flow conditions. Source (3) is quasi-tonal in nature and will be described later. Sources (4-5) are vortex related sound sources. For rotor blade and wing designs, trailing edge bluntness is likely to be negligible in comparison to boundary layer thickness.

Turbulent boundary layer (TBL) noise has been the most extensively covered source in literature. As the turbulent boundary layer flowing over an airfoil passes over the trailing edge, pressure fluctuations scatter into the farfield. The trailing edge acts as a means to scatter hydrodynamic pressure fluctuations. When, in the boundary layer, these fluctuations have a relatively short wavelength, the trailing edge scattering causes an increase in the acoustic wavelength to a point where acoustic waves propagate to the farfield as broadband noise [44].

Brooks et al. [2] carried out an extensive experimental campaign on a NACA 0012 airfoil, in order to provide a comprehensive description of various noise mechanisms related



**Figure 2.4:** Mechanisms of airfoil self-noise examined by Brooks et al. [2].

to airfoil self-noise. Their study resulted in the development of a semi-empirical model for the prediction of airfoil self-noise, by means of the mechanisms discussed above. This model, known as the BPM model, has seen applicability in a wide range of fields.

Doolan and Moreau [45] provided a review of experimental measurements on trailing edge noise of airfoils, as used in wind turbine applications. This experimental data bank was compared to predictions using the BPM model. It was found that the semi-empirical models used had remarkably good agreement with high frequency noise spectra predictions, but do however fall short at predicting low frequency noise - and in particular the peak noise frequency of various airfoils at a range of Reynolds numbers.

### TNO Models for Airfoil Self-Noise

The BPM model [2] has shown sufficient agreement when applied to the problem of TBL noise prediction from wind turbines. However, this semi-empirical model is based on measurement data for only a single symmetric NACA 0012 airfoil, and uses velocity and frequency scaling laws. Whilst it is widely used, it is not fully comprehensive. The prediction of noise produced by non-symmetric airfoils in differing flow conditions, for

example, is still an open issue [46]. The BPM model does not account for the exact physical mechanisms of noise predictions, and is thus not sophisticated enough to further investigate self-noise sources accurately. Thus, in order to move away from the inaccuracies of this empirical approach, and to account for a broader range of conditions, an analytic model for airfoil self-noise has been developed.

The TNO model was originally introduced by Parchen [47], who developed a semi-empirical model for self-noise from a turbulent boundary layer as a function of the wavenumber-frequency pressure spectrum, as well as turbulent boundary layer parameters. The model is more generic than the BPM model in that a full description of the boundary layer is required, as opposed to just the boundary layer displacement thickness for the BPM model. The prediction of wall pressure fluctuations was first investigated by Kraichnan [48], by using solutions to the Poisson equation which relate pressure fluctuations to boundary layer velocity over flat plates. Wall pressure fluctuations on an airfoil surface lead to a blocked pressure, generally seen as being twice the pressure that a flow would produce if no wall was present. The TNO model assumes that turbulence is anisotropic within the boundary layer.

Using the wall pressure fluctuation approach yielded improved results over the existing approach. Further addition of Blake's equation [4], for the wavenumber-frequency pressure spectrum on the airfoil wall, improved results even more. This approach made use of a more realistic physical reasoning for the practical mechanism of noise scattering at the trailing edge. The model in this form became known as the TNO-Blake model.

Bertagnolio et al. [49] implemented the TNO model, with either XFOIL or CFD as a flow solver, to predict airfoil noise. Whilst correct trends were predicted, SPL intensities were underpredicted by about -8 to -10 dB, supposedly due to underpredicted surface pressure levels. This deficit was also confirmed in studies by other authors [50]. The model is improved by Kamruzzaman et al. [51], whereby the original TNO equation was multiplied by a factor of 2, to account for the singled-sided spectrum occurring in measurements. The reason for this convention arises due to the fact that measurement systems only operate at positive frequencies, whilst the model was originally derived for theoretical frequency spectra that could be both positive or negative. Modifications to the TNO model were made by Stalnov et al. [44] to reduce the dependence on tuning parameters. This was



achieved, and predictions compared to measurements within 2 dB.

Lee [46] introduced an *improved* model, dubbed the iTNO model, which reduces the number of input variables, decouples the convection velocity from the boundary layer height, and shows validated results that were in better agreement than previous studies. Furthermore, it was seen that the correct amplification factor was  $4\pi$ . Lee validated predictions against narrowband measurements obtained by Stalnov et al. [44].

The TNO model is seen to be highly robust, being applied to highly cambered wind turbine airfoils as well as relatively high speed cases. Since the models require a full anisotropic description of turbulence in the boundary layer of an airfoil they are already mature enough to accept CFD data as input for noise calculations, making this model one that could be readily used to predict TBL noise from rotating machinery.

### 2.2.3 Speed Dependence of Airfoil Noise

Airfoil noise mechanisms for self and inflow-turbulence noise each scale to different powers. It is generally agreed that TBL noise scales with the fifth power whilst turbulent-inflow noise scales with the sixth power. When an acoustic source is much smaller than the given wavelength, the acoustic flow can be considered incompressible and the source can be treated as *compact* [23]. Generally the acoustic source is characterised by some sort of characteristic dimension (such as a turbulent length scale). Compact sources can then be treated as point sources.

Lowson's formulations for inflow-turbulence noise observe a dimensional dependence of the acoustic pressure to be proportional to velocity as  $p^2 \sim U^6$ , applicable at low frequencies. As frequencies increase the compactness approximation changes as wavelengths become much shorter with respect to the acoustic characteristic dimension. Lowson suggests the response functions need to take this approximation into account.

For high-frequency noise additional assumptions need to be taken into account. Amiet defines an airfoil noise model, under certain assumptions such as airfoil aspect ratio, which is applicable to high frequency noise predictions Amiet [1]. This model, as well as the Brooks et al. [2] model, have a velocity proportionality defined by  $p^2 \sim U^5$  which allows a degree of source non-compactness to be considered.

Buck et al. [6] performs measurements on large wind turbines in order to match the-

oretical directivity patterns. Here it was assumed that acoustic pressure scaled with the sixth power of inflow velocity in order to determine inflow-turbulence noise. In this study it was found that results differed by less than 0.2 dB when changing the scaling factor from 6 to 5.

Oerlemans et al. [52] also explores the speed dependence of noise levels observed in large wind turbines. Normalised sound levels are calculated as a function of Strouhal number. Acoustic energy is then assumed to be proportional to flow speed with a relation of  $p^2 \sim U^x$  where  $x$  indicates the dependence of the blade noise on the flow speed. A value of 5 was found to provide the best fit indicating that for the given experiment trailing edge noise was seen to be dominant. This matches closely with previous experiments [53].

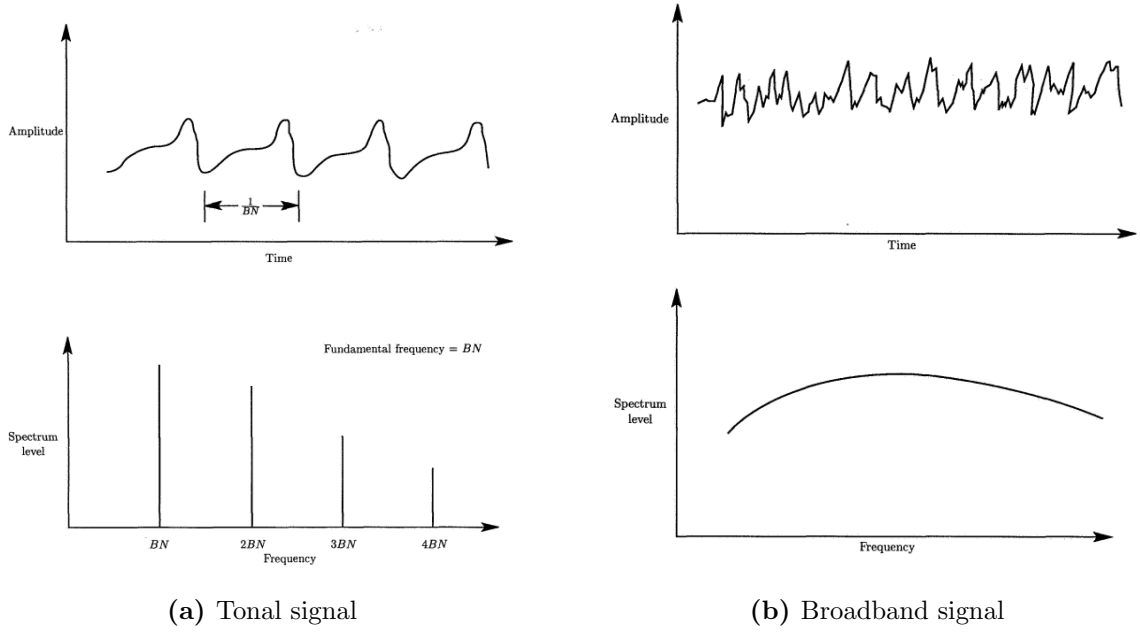
## 2.3 Rotor Noise Mechanisms

Rotor noise can be broadly classified into three categories: harmonic noise, broadband noise, and narrow-band random noise. Harmonic noise is periodic, meaning that if taking measurements of this noise in time there will be repeating time signatures with a consistent period. In the frequency domain, several harmonics exist. Fig. 2.5a illustrates these characteristics in both the time and frequency domain. Broadband noise is random in nature, and consists of all frequency components. This noise source occurs at all frequencies, however each frequency may have different intensities to those individual components. A typical broadband signal is seen in Fig. 2.5b. Narrowband random noise is near periodic, however some randomness is observed whereby some components do not occur periodically.

These classifications can be further separated into into several distinct mechanisms, each occurring at its own intensity relative to the overall system. These mechanisms have been examined both experimentally as well as analytically.

### 2.3.1 Tonal Noise

Tonal noise can occur in rotating machinery that has sufficiently high blade passing frequencies (BPF). Tonal noise is seen as a sharp, high intensity narrowband spike in an SPL spectra (Fig. 2.5a). This noise source occurs at much higher intensities than broadband



**Figure 2.5:** Rotor noise characteristics for a propeller in the time and frequency domain [54].

levels of sound and, for high speed machinery, also usually occurs in the most sensitive frequency region of human hearing. For rotating machinery, tonal noise occurs at harmonics of the blade passing frequencies. For multi bladed systems such as open rotors, these would occur at interaction tones (combinations of the BPF harmonics of the system).

As an example; for a six bladed VAWT with a rotational speed of 200 RPM there will be several harmonic blade passing frequencies  $BPF_1 = (RPM \times nBlade)/60 = 20Hz$  and  $BPF_2 = 40Hz$ . Typical BPF's for low speed rotors are quite low and result in negligible impact from a human listening perspective. A propeller often has transonic tip speeds, and thus can have sufficiently high BPF's such that audible tonal noise occurs.

Early studies on fan noise applied scaling laws for tonal noise prediction. Dittmar [55] looked at aerodynamic response functions for fluctuating lift in order to predict noise reduction between competing designs. A comprehensive description of propeller broadband and tonal noise was provided by Magliozzi et al. [56], who derived a frequency domain expression for propeller noise at the harmonics of the blade passing frequency by starting with the Ffowcs-Williams Hawking equation. Blade forces are resolved into thrust and torque components, and related to the required airfoil pressure jumps necessary for calculation.

Brooks et al. [2] described a *quasi-tonal* airfoil noise source known as Laminar-Boundary-Layer Vortex Shedding (LBL VS) Noise. The reason for the so called quasi-tonal nature of this sound is related to its generation mechanism. When a laminar boundary layer exists over an airfoil, vortex shedding can occur at Strouhal frequencies of the airfoil. Boundary layer instabilities couple to acoustically excited aerodynamic feedback loops to produce this noise source. Quasi-tonal noise normally arises due to laminar flow conditions and, by tripping the boundary layer, and forcing turbulent flow over an airfoil the feedback process can be destabilised and the noise mechanism countered.

### 2.3.2 Low Frequency Noise

Horizontal axis wind turbines can also suffer from bluff body interaction noise, whereby interactions between blades and towers produce broadband noise. This latter problem has been solved by means of positioning wind turbines of this design with their rotors facing into the flow (upstream configuration).

A study of vertical axis wind turbine low frequency noise, performed by Dumitrescu et al. [57], used vortex models combined with the FW-H equations and found levels of low frequency noise in the region of less than 10 Hz.

### 2.3.3 Stall Noise

When an airfoil increases its angle of attack past a certain critical angle, large scale flow separation occurs and the airfoil stalls. From an aeroacoustic point of view, the wake becomes very large and the airfoil becomes more akin to a bluff body in flow. A bluff body generates noise due to the large scale low-frequency pressure fluctuations occurring downstream [58]. For light stall conditions, airfoil noise generally increases in intensity by up to 5 dB and reduces the peak frequency [59]. Propellers rotating at high velocities are more susceptible to stall noise, and noise increases of 10 - 15 dB can be observed during complete blade stall [60].

The model of Brooks et al. [2] can be used for the prediction of stall noise, one of the mechanisms of airfoil self-noise. The limitation of this model, again, is the fact that experiments were only carried out for a single type of airfoil, the NACA 0012. A newer stall noise prediction model was developed by Bertagnolio et al. [61], based on Curle's

analogy. Inputs to the model are the separation location along the airfoil chord, as well as geometric parameters and operating conditions. Whilst this model fares well when compared to experimental data it becomes non intuitive to use as separation location is complex to predict, particularly with CFD or panel methods.

### 2.3.4 Interaction Noise

Interaction noise occurs due to the viscous wakes and tip vortices interacting with the bound potential fields of both the vortex generating blade, and the downstream following blades. This type of source can occur in multi-row propellers, or blade systems with high advance ratios, where blade vortices are able to interact with each other.

Using experimental data for the unsteady pressure differential near the leading edge of an airfoil Burley and Brooks [58] were able to model broadband blade wake interaction (BWI) noise for a high speed rotor. Prediction comparisons with measurements showed good agreement for a wide range of operating conditions.

### 2.3.5 Amplitude Modulation

The *swish* or *thump* noise produced by a wind turbine is referred to as amplitude modulation or Enhanced Amplitude Modulation (EAM). This noise has been categorised by Madsen et al. [62] as arising from TBL noise sources. For an observer on the ground near a large HAWT, a *swish* is heard over time which varies in intensity. The frequency of this sound is related to the Blade Passing Frequency (BPF) of the turbine, and has been seen to be due to the directivity of the noise source as well as the relative motion of the blade in relation to the listener [63]. For large wind turbines, an additional *thump* noise has been occasionally reported at very long distances. This particular long-range mechanism is not yet fully understood, and is a subject for future research.

### 2.3.6 TBL noise

Rotating machinery comprises of lift generating blades that move through a fluid medium. Each blade within a propeller or fan will produce its own level of self-noise as a function of the aerodynamic lift generated. More about this source as applied to specific devices

will be covered later in this chapter.

### 2.3.7 Rotor Turbulence Noise

Any rotating machinery operating in a viscous fluid flow will encounter some form of turbulent interaction, whereby turbulent flow will come into contact with blades of that machine. These turbulent interactions will produce noise, as governed by the mechanisms of airfoil inflow-turbulence noise Amiet [64].

This turbulence can arise from different sources. For example, HAWT blades are spaced physically far apart enough that they do not interact aerodynamically with each other. However, at the altitude where these turbines operate, there can be excessive turbulence contained in the air itself, which will contact the rotating blades in isolation.

In high speed systems, where confluent effects occur, blades can induce additional turbulence on following blade rows, leading to a localised source of turbulence noise from blade rows.

## 2.4 Noise Prediction of Rotating Machinery

The prediction of aeroacoustic noise from rotating machinery is an important consideration during the design phase of these devices. Noise regulations drive development of new designs to continually be quieter, and thus accurate noise prediction tools are a necessity. Noise prediction can be performed either experimentally or computationally.

### 2.4.1 Horizontal Axis Wind Turbines

Horizontal axis wind turbines produce power by means of blades rotating in air, and produce a relatively stable power output for their given flow conditions. Turbines are categorised by their power ratings (the power at operating conditions that a particular turbine produces in Watts). Ratings span from the smallest turbines, known as micro-wind turbines, which can produce power in the order of a number of watts through to large wind turbines, with blade diameters of up to 160 m, that produce energy in the order of a number of MegaWatts.

Solving for the aerodynamics of a HAWT is fairly straightforward, and can be performed using a variety of computational tools with increasing computational cost and solution fidelity.

The Blade Element Momentum (BEM) theory [35] is a way of equating a momentum balance in an annular streamtube through a turbine rotor disk to an examination of forces on the blades of a wind turbine. In BEM theory a rotor is modelled as an infinitely thin disk, and as fluid flows through this disk it is assumed that the disk converts this energy before continuing further downstream. Combining a momentum balance with equations for the forces on airfoils, allows one to iterate to solve for the power. It is assumed that there are no interactions between blades, and that 2D theory can be used to describe the airfoil lift and drag performance. Tip losses can also be accounted for by making use of correction factors in a similar manner to traditional lifting-line theory [65].

Several BEM tools exist for wind turbine design and, for simple geometries without complicated blade profiles, they are straightforward to implement and obtain relatively accurate results [35]. QBlade [66] is an easy to use BEM tool which uses XFOIL [67] to solve a series of lift and drag polars for turbine airfoils, and thereafter combines these results with the BEM method to solve for power. NREL FAST [68] is a certified turbine design tool which incorporates a BEM method into its own suite of modules. The aerodynamic calculations in FAST are more simplified than those in QBlade as they use a *'pseudo two-dimensional'* set of airfoil properties to solve for airfoil aerodynamics of the system.

Other procedures to solve for HAWT performance are CFD based approaches. A general assumption of HAWT aerodynamics is that the blades wakes do not interact with each other and thus, for a one-dimensional inflow, it is valid to use a Reynolds Averaged Navier Stokes (RANS) CFD simulation to calculate the power production of the blades.

### Horizontal Axis Wind Turbine Noise Prediction

Wind turbines generate noise from both mechanical and aerodynamic sources. Mechanical noise is produced by gearboxes, yaw mechanisms and other instruments required to convert wind power into electricity. Small turbines, however, have very few moving parts and very often incorporate direct drive generators instead of gearboxes. The noise from these components is either very low or can easily be treated through the use of acoustic liners

within the nacelle.

Aeroacoustic noise generated by wind turbines is produced by a combination of sources. In Horizontal Axis Wind Turbine (HAWT)'s the two main noise generation mechanisms are the airfoil self noise [2] and inflow noise [1]. Vertical Axis Wind Turbine (VAWT)'s generate noise by the two aforementioned mechanisms as well but, added to this, are contributions of noise sources from stall [69] as well as various effects from the presence of the centrally mounted tower. VAWT's, due to their faster relative rotation speed compared to HAWT's, have a tendency to produce tonal components of noise at harmonics of the BPF [70].

There has been much work directed to the prediction of noise generated by conventional HAWT designs. A number of certified design codes exist which employ various methods in order to predict wind turbine noise [68, 71]. Subsequently many authors more recently have used the semi-empirical and analytic models such as those of Brooks et al. [2] and Amiet [64] for the prediction of HAWT noise with reasonable success [5, 72–75]. These codes all use a BEM model to solve the aerodynamics of the system, and thereafter use the aerodynamic inputs of the system in the noise prediction module.

Wind turbine noise prediction codes are classified based on the complexity of the modelled system. Lowson [34] defined three classes of prediction codes whilst Zhu et al. [74] defined an additional *pseudo-class* as an extension of the first three. This pseudo-class took structural deformation due to wind loading into account - an assumption which provided negligible changes in results ( $<1$  dB). The classes are defined in Table 2.1. With modern computers, more parameters can be added to prediction models at very negligible cost.

### 2.4.2 Vertical Axis Wind Turbines

Lift based VAWT's produce power through lift generating blades rotating parallel to a central shaft. The blades of these devices undergo unsteady flow fluctuations as they rotate about their own axis. Unsteady forces act on these turbine blades due to geometric effects and blade-blade aerodynamic interactions; causing unsteady power generation with rotation. Unsteadiness in the system occurs due to ever changing flow conditions for a local airfoil blade. Fig. 2.6a shows a schematic of the forces and velocities encountered



| Type of Code | Description                                                                                                                                                         |
|--------------|---------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Class I      | Overall noise predictions are based on very basic empirical relationships, such as those based on rated power or rotor speed. Essentially a lumped system approach. |
| Class II     | Separate noise mechanisms are considered, however only limited turbine parameters are used.                                                                         |
| Class III    | Predictions utilise several models that specifically describe each noise mechanism. Detailed descriptions of geometry and operating conditions are included.        |
| Class III+   | Like Class III however additional rotor information is provided such as transient operating conditions and vibration characteristics.                               |

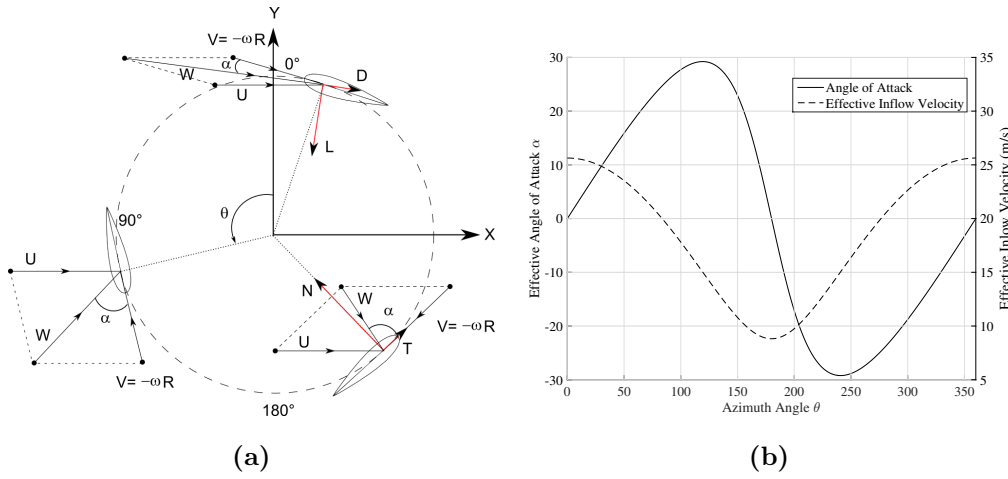
**Table 2.1:** Classes of wind turbine noise prediction codes.

by a VAWT during rotation. In this diagram  $U_0$ ,  $V$  and  $W$  are vectors of velocity and, combined with  $\theta$  can be used to find the geometric angle of attack that the blade encounters during rotation. Fig. 2.6b shows the relationship between azimuthal angle and angle of attack for a VAWT blade at a single Tip Speed Ratio (TSR). Tip speed ratio is defined as,

$$\lambda = (\omega R)/U_\infty \quad (2.10)$$

where  $\omega$  is the rotational velocity of the turbine,  $R$  is the rotor radius and  $U_\infty$  is the freestream velocity.

The geometric angle of attack is not a true (effective) angle of attack that the airfoil encounters. Downstream blades continuously pass through the wakes of upstream blades, and as a result these interactions induce an additional, less obvious, velocity component within the rotor. Accurately solving for effective angle of attack of a VAWT blade should account for all interactions between blades. Edwards et al. [77] used the average flow field around a VAWT blade during rotation to obtain velocity components in two directions. This average velocity field was then used to solve for the angle of attack at various stations around the blade. This type of approach yields results such as the plot seen in Fig. 2.7. In this figure blade vortex interaction (BVI) is observed at about  $280^\circ$  as a minor perturbation

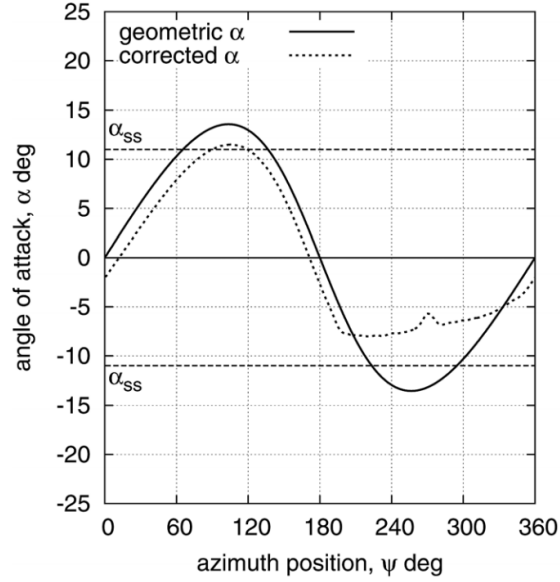


**Figure 2.6:** Vertical axis wind turbine forces and geometric relationships. (a) Forces and velocities on a VAWT during rotation [76] (b) Relationship between azimuthal angle and geometric angle of attack as well as inflow velocity.

to the angle of attack.

Dynamic stall is a phenomenon whereby an airfoil undergoing dynamic motion can cause a leading edge vortex to be shed, briefly increasing lift production of the blade. McAlister et al. [78] performed a number of experiments on airfoils encountering dynamic stall (as observed in VAWT's). They quantified an increase in lift just before stall was reached. This unsteady lift phenomenon is emphasised on VAWT's due to their blockage effect - as the blades rotate around their own axis an upstream blade will effectively shield any downstream blades from encountering the same flow velocity. As a result of this blockage, as well as a reduction of energy in flow due to extraction by blades, a velocity deficit is observed through the rotor. Trailing blades are not as heavily loaded as leading blade, and an unsteadiness in the forces is observed.

Simão Ferreira [79] studied the near wake of the VAWT in both 2D and 3D approaches and found that the wake was a result of the shed and trailing vorticities of each blade segment in conjunction with each other segment. The vorticity interactions were quantified, and it is found that there are stronger vortical effects at the downwind blade passage. Danao [80] performed detailed experiments and computations to quantify the effects of steady and unsteady oncoming flow conditions on VAWT performance, citing blade thickness as a primary contributor to blade stall.



**Figure 2.7:** Relationship between geometric and measured angle of attack (nomenclature inconsistent with this report) [77]

The prediction on VAWT performance requires an accurate model of the lift and drag of each blade. This can be predicted using a variety of methods ranging in computational efficiency.

Double-Multiple Streamtube (DMST) methods, developed by Paraschivoiu [81] takes a 2D rotor section and applies a derivation of the actuator disk theory in combination with blade element momentum (BEM) theory to solve for blade power as a function of aerodynamic lift and drag. This method iteratively balances momentum conservation and the forces on the rotor blades until system convergence is achieved. The method assumes that a streamtube flowing through the rotor is split up into a series of smaller local streamtubes, of which the airfoil blades are able to pass through during a single rotation. Here the standard actuator disk theory can be applied for each streamtube. It is further assumed that each blade passes through each streamtube twice, ones upwind and once downwind - thus extracting energy from the air twice. This is then handled as if two single actuator disks were operating in tandem. When compared to CFD methods the streamtube model is seen to overpredict performance estimates [82]. Furthermore the model tends to produce implausible results when either unrealistic geometries, or low speed flow conditions are considered. Whilst DMST methods are included in commonly used codes such as QBlade, their applicability to the prediction of VAWT performance

is questionable, due to the fact that these methods do not include any interaction effects between blades. The limitations of the streamtube method are summarised by Ferreira et al. [83], who provide comparisons between several prediction methods and concludes that the supposed accuracy of streamtube models is purely due to an effect of error cancellation, which only occurs for a few specific cases.

Panel methods can be used to more accurately consider wake interactions within a VAWT rotor when solving for the performance of the device. Dixon et al. [84] described a panel method, able to predict aerodynamic performance of a VAWT by means of accounting for effects of the airfoils interacting with upstream wakes, as well as the effect that airfoil wakes have on other wakes in the flow field. Panel methods using vortex based aerodynamic calculations are advantageous in that they are able to resolve time histories of pressure fluctuations on blade surfaces.

Time dependent 2D and 3D Computational Fluid Dynamics (CFD) calculations have been seen to provide more detailed flow solutions, when compared to rudimentary stream tube models, and are a useful means for predicting performance - particularly before wind tunnel testing is performed. Furthermore, wind tunnel testing of VAWTs is difficult to perform, and only a limited number of experimental results are available [85, 86]. This technique is mostly used to study overall performance, as opposed to detailed flow analysis. Using CFD based methods can provide more insight into specific aerodynamic structures of an early stage VAWT design. Li et al. [85] performed a thorough investigation of the flow features around the blade of a VAWT, verifying CFD calculations against wind tunnel test data. CFD calculations performed well when an appropriate turbulence model was applied. There are, however, several critical issues to be addressed when using CFD for VAWT aerodynamic prediction [87]. Achieving reliable CFD results requires accurate simulation and careful utilisation of the parameters of the modelling. During a grid dependency study on a VAWT, Almohammadi et al. [88] showed the sensitivity of the power/torque calculations to mesh types and the time step/Courant number.

### **Vertical Axis Wind Turbine Noise Prediction**

In the case of small VAWT's, here defined as those producing less than 10 kW of power, there has been very little work done to quantify the noise that they produce. It is generally

considered that small VAWT's are *quiet enough*, since their overall noise levels rarely exceed that of their surroundings [18]. However there is still audible noise which needs to be predicted before turbines can be approved for integration into urban environments. Early stage noise source prediction of wind turbines is beneficial for designers (or installers), in order to produce a good estimate of noise levels prior to installation, for regulatory requirements.

The body of experimental work related to VAWT noise is currently very limited, and as such there is not much data available regarding the noise levels of these devices. Many manufacturers of VAWT's do not undergo rigorous noise testing campaigns to qualify their designs and, as a result, available octave band spectral measurements are limited.

Several experimental campaigns have been performed for VAWT noise measurements. Some focus on the noise measurements of wind turbines in their actual environments, whilst others are laboratory based campaigns. Quietrevolution [89] performed a number of farfield measurements of their QR5 wind turbine in a number of different operating conditions, their data was taken at wind speeds of 6 - 10 m/s, in accordance with EN 61400-11: 2003 and presented in third-octave bands. Table 2.2 provides an excerpt of some data gathered for various VAWT's operating at a rated power of around 3kW, the only exception to this is the QR5 which operates at about double that rated power. The table aims to show the disparity amongst measurements, and the lack of quality measurement data available for VAWT noise.

Beamforming can be used to locate wind turbine noise sources. Pearson [90] performed experiments to quantify the noise generation mechanisms of a VAWT, these experiments were compared with the results of two sets of prediction codes. A harmonic modelling method which used approaches from Lowson and Ollerhead [91] for helicopter noise, and a time domain model using semi-empirical relations from Amiet [1] and Brooks et al. [2] for farfield propagation of noise signals. Pearson concluded that broadband noise clearly dominates the spectra for VAWT's.

Several authors throughout the past decade [57, 98, 99] have taken advantage of panel methods as aerodynamic solvers, and thereafter apply the FW-H acoustic analogy in a post processing step to calculate the farfield noise. Results from panel method noise predictions were deemed to be good first approximations for the design phase.

| Turbine         | Power  | Noise Level   | Wind Speed | Listener |
|-----------------|--------|---------------|------------|----------|
| Aerocopter [92] | 2.0 kW | <37 dB        | -          | -        |
| Aeolos-V [93]   | 2.0 kW | <45 dB        | -          | -        |
| DS-3000 [94]    | 3.0 kW | <40 dB (A)    | -          | -        |
| P3000-AB [95]   | 3.0 kW | -             | -          | -        |
| QR5 [96]        | 6.5 kW | 57.9 dB (max) | 10.0 m/s   | 22.5 m   |
| VisionAir5 [97] | 3.0 kW | <38 dB        | 5.0 m/s    | -        |

**Table 2.2:** Noise measurements of vertical axis wind turbines as provided by some manufacturers. [18]

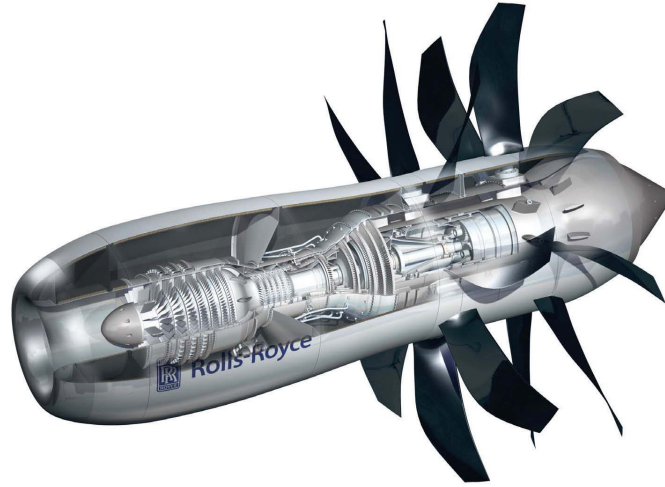
Other low-order computations for VAWT noise have been performed in the same vein as the strip-theory approaches used to predict HAWT noise. Pearson [90] developed one such model for broadband VAWT noise prediction, which was used to verify existing experimental data. The method was based on the BPM and Amiet models for self and inflow-turbulence noise, and performs amicably for the test case. Ottermo et al. [100] also produced a similar model used to compare results of a beamforming experiment performed on a large 200 kW turbine. The results of which were able to predict trends, but were not able to show sufficient validation with any sort of certainty. It is unclear what sort of aerodynamic model was used for these two approaches.

Mohamed [11] attempted a rudimentary low frequency analysis of a few VAWT designs, and attempts to draw trends in noise spectra from different designs. No description of the computational set up or grid convergence was explained, thus, although the method showed promise, the results would need to be further validated.

### 2.4.3 Propellers and Open Rotors

In this study high speed rotors are also considered, as they produce noise by means of the same aforementioned aeroacoustic mechanisms as low speed turbines. An open rotor (also known as an unducted fan (UDF) or contra rotating open rotor (CROR) amongst others) is a type of aircraft engine which combines the concepts of traditional turbofan and turboprop engines into a single unit. Fig. 2.8 shows an example of an open rotor which consist of a central gas turbine connected to two sets of low aspect-ratio blades rotating in

opposing directions. The blades are powered by a gearbox connected to the gas turbine.



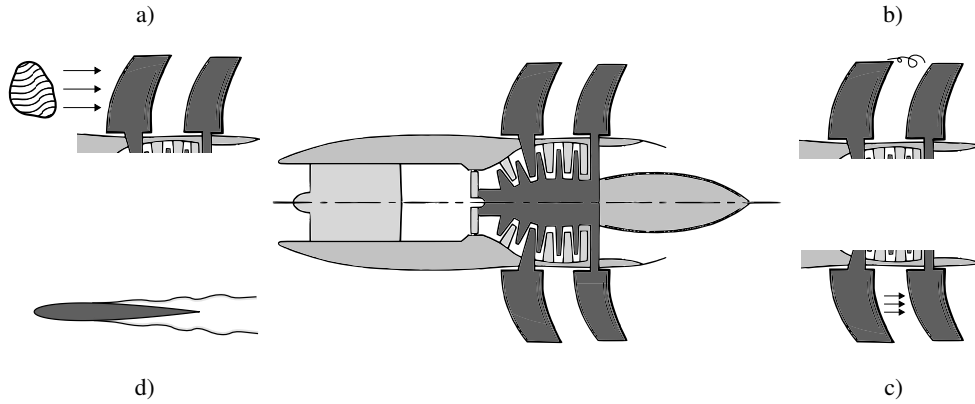
**Figure 2.8:** Schematic of a Rolls-Royce contra rotating open rotor design concept [101]

Turbofan performance is proportional to fan diameter and as fan diameters grow so do nacelle diameters. Increased nacelle diameters lead to increased drag therefore there is a trade-off that needs to be examined between propulsive performance and increased drag. The open rotor concept promises to reduce fuel burn costs compared to current generation technology. Van Zante [102] describes a number of milestones in the NASA research roadmap for the reduction of fuel burn and noise, in combination with reference to a number of collaborative projects with GE.

### Open Rotor Noise Prediction

Broadband noise prediction of open rotors becomes particularly important in terms of understanding the overall noise levels produced by these types of engines. There are two types of noise sources pertinent to open rotors, tonal and broadband noise. Tonal noise arises at harmonics of blade passing frequencies as a result of periodic rotations; and broadband noise is produced from turbulent interactions. In the case of OR noise, there are four primary sources of broadband noise [103]. These sources are shown in Fig. 2.9. Clockwise from right they are; a) turbulence ingestion noise; produced when turbulence from the free stream comes into contact with the forward rotor b) blade vortex interaction noise; a noise produced when an upstream shed vortex comes into contact

with a downstream blade c) rotor-rotor interaction noise; due to the effect of a turbulent wake shed by upstream blades impinging on the leading edge of downstream elements d) airfoil thickness and loading noise; caused when boundary layer turbulence convects over a trailing edge of a rotating blade and into freestream air.



**Figure 2.9:** Broadband noise sources on an open rotor. (a) turbulence ingestion noise (b) blade vortex interaction noise (c) rotor-rotor interaction noise (d) airfoil thickness and loading noise.

Additional noise sources are introduced to the system when the rotor is installed onto an aircraft fuselage [104, 105]. An open rotor can be mounted to a fuselage by means of a pylon, with the pylon oriented either downstream in a puller configuration or upstream in a pusher configuration. As a result of this installation, additional turbulence is added to the system as airflow over the pylon separates and impinges additional flow structures onto the rotating blades. From an aerodynamic perspective, the periodicity of the blade forces begins to break down in the interaction region between the rotor wake and the pylon. One of the reasons for this loss of thrust periodicity, is a local increase in angle of attack of individual blade elements due to increased upwash from induced upstream turbulence.

Parry [106] provided one of the first predictions for open rotor noise. His tonal noise prediction code uses analytical models, in which the harmonic components generated by thickness and loading noise of the blades are computed. His code accounted for viscous wake interactions by means of a harmonic gust model. In recent years Parry performed a parametric study [107] using these and other tools, to quantify the effect that major design features have on open rotor noise generation. His conclusion comes with several warnings related to the simplified analytic nature of the codes, the concluded trend, however, is that there is a relationship between the ratio of forward to aft blades and the noise, as



well as a coupling between tip speed and blade number.

Hybrid approaches for aeroacoustics allow a reduction of computational time for noise simulations, and are a viable means of predicting, with relative accuracy, the noise produced by open rotors. The type of hybrid methods described here, address the prediction of open rotor broadband noise sources by employing a semi-empirical noise prediction methodology. The methodology makes use of a series of flow descriptors extracted from Computational Fluid Dynamics (CFD) simulations to allow for improved accuracy in terms of the input parameters for the semi-empirical method. Essentially a CFD simulation is chained to an analytical noise prediction approach, allowing for more accurate flow input variables than what is regularly available from analytical descriptions of flow. Ekoule et al. [108] and Grasso et al. [109] both employ CFD chaining to determine the mean velocity deficit of wakes shed by blades as part of the prediction process. Chelius et al. [110] also chains CFD and CAA, but makes use of the Linearised Euler Equations as an alternative to the more commonly used FW-H equations [29].

#### 2.4.4 Directivity and Retarded Coordinates

Directivity is a measure of the directional characteristic of sound. It is expressed as a non-dimensional positive value in a 3D space around a particular sound source. By applying a directivity function in three dimensions as per [111], a measure of the source sound power distribution is determined on a case by case basis dependent on the type of source.

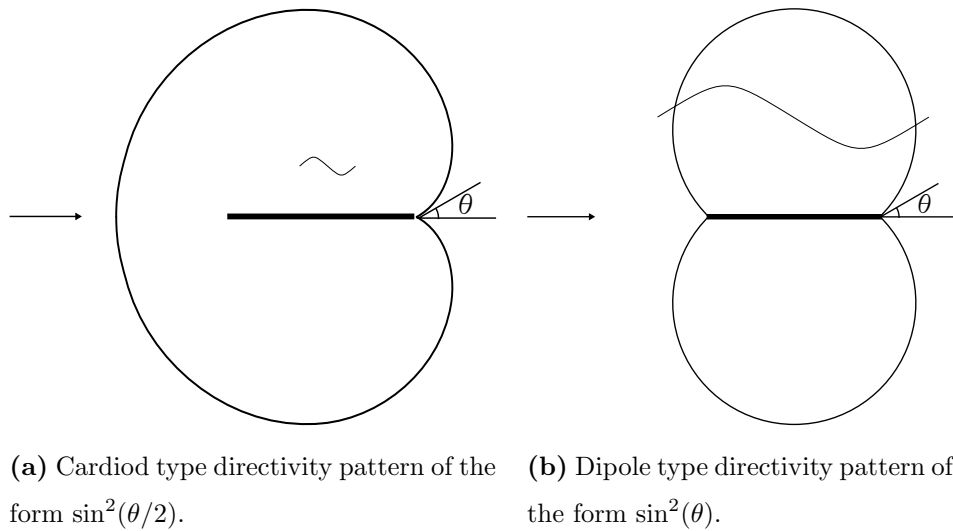
For the case of an airfoil in a turbulent stream, directivity functions are applied using a retarded coordinate system which accounts for noise source emission time. If an airfoil were producing sound at a stationary location (such as a speaker would), then the sound source would reach the receiver by following a direct path. However, as the airfoil is in motion, the ray path that the sound source follows to the receiver is at a retarded position relative to the convective velocity of the freestream. This so called doppler-shift means that the effective frequency at the receiver is different to the one emitted at the source. This relationship is governed by a simple doppler shift correction [111],

$$f' = \frac{f}{1 - M \cos \theta} \quad (2.11)$$

where  $f'$  is the frequency at the receiver,  $f$  is the source frequency and  $\theta$  is the angle

between the source velocity and observer location.

Airfoil directivity is seen to follow the form of directivity from a flat plate in flow. These patterns are either cardioid or dipole in shape, as in Fig. 2.10, and represent a compact noise source radiating from a single location at the trailing edge. A number of different directivity patterns exist, and are based on observations and analytical expressions, related to convection velocities. Several relations exist to describe this directivity for the problem of airfoil noise, and assume a compact dipole directivity, whereby noise is radiated from the trailing edge.



**Figure 2.10:** Two theoretical directivity patterns for noise radiated by a flat plate in flow.  $\theta$  represents the observation angle in relation to the location of the trailing edge and the sine wave represents an acoustic wavelength.

Brooks et al. [2] introduced two directivity equations for their work on TBL noise. The equations follow the form of the flat plate directivity patterns in Fig. 2.10, and incorporate retarded time effects by means of a doppler shift correction. A cut-off frequency was suggested to switch between high- and low-frequency directivity models. Furthermore a convective Mach number of  $M_c \approx 0.8M_0$  was used as a correction to normalise final level adjustments between experiments and the semi-empirical estimates.

As a result of these observations Lowson [34] negated the use of directivity corrections for wind turbine inflow noise predictions, but did suggest that accurate prediction of the noise emitted by wind turbines is highly dependent on the choice of directivity model used.

A small yet significant correction for angle of attack was implemented by Burley and Brooks [58] to account for the difference in directivity between a flat plate and an airfoil. The basic equation for airfoil directivity includes an exponential denominator. Multiple exponents from 1.5 - 4.5 have been found through varying theoretical approaches. Burley and Brooks [58] compare measurements and predictions and find a fourth power fit for compact dipole sources [111].

The directivity models were originally applied to predictions of self-noise. For inflow-turbulence noise, this additional term was included by Moriarty and Migliore [39] for use the FAST code. The directivity was applied in the same manner as it was in the BPM formulations. The particular directivity selected for inflow-turbulence noise is represented by a classic dipole pattern generally observed at low frequencies.

Measurements of horizontal axis wind turbine noise performed by Oerlemans et al. [52] were compared to analytic predictions based on models implementing the directivity equations proposed by Burley and Brooks [58]. Inflow-turbulence noise was modelled by inverting the directionality of the equations, such that noise was radiated from the leading edge. When the ratio of acoustic noise producing eddies was larger than the airfoil chord length, the directivity pattern was seen to change from cardioid to dipole. These results confirmed that accurate directivity models were necessary for accurate prediction of wind turbine noise. Furthermore it was seen that the new directivity equations by Burley and Brooks [58] were sufficiently accurate in describing noise from a horizontal axis wind turbine.

### 2.4.5 The Problem of Transience

The described models for airfoil noise prediction are all derived for the condition of steady flow. This means that, for the current problem of rotor noise, where airfoils are constantly in motion, the effect of these transient motions on noise generation are not considered. When applying the models to a continuously moving unsteady system a quasi-steady assumption needs to be considered whereby, at each discretised time step, the system behaves as if it were a locally steady system.

Studies on helicopter rotor noise by Paterson and Amiet [112] found that isolated airfoil results could be applied in a stripwise manner to predict broadband noise. Predictions were

able to accurately estimate directivity trends except for at extreme angles. In addition, broadband noise levels measured in wind tunnel experimentation were well predicted. However, in general, predictions were seen to be less favourable than expected. The prediction procedure for turbulent flow into a rotor was summarised by Amiet et al. [113] who provided a theory manual for the calculation of noise from airfoils in both rectilinear and circular motion. The tool required airfoil response functions, sensitive to compressibility effects and skewed gusts, as well as a turbulence spectra.

A study on self-noise from a helicopter rotor by Brooks et al. [114], used the BPM model to solve for broadband noise in a rotating coordinate system. Using a quasi-steady analysis, the results fared well. However, there were concerns about the definition of the local boundary layer characteristics, since the aerodynamic model used ignored unsteadiness and hysteresis effects. Further concerns were related to high speed scaling of the semi-empirical model. An important result was the identification and articulation of the broadband blade wake interaction noise source.

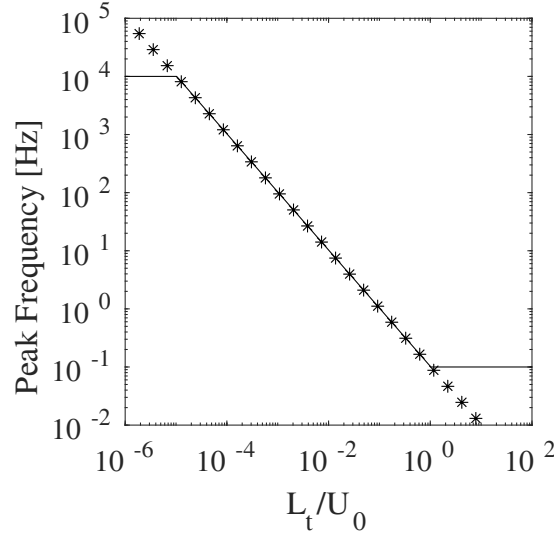
From the aforementioned studies, it was seen that isolated airfoil models, such as the BPM model for airfoil self-noise, or the Amiet model for inflow-turbulence noise, can be applied to high speed rotor broadband noise prediction problems. Previous models have generally been prone to unfavourable predictions as a result of inaccurate aerodynamic models. Accurate input data to the noise models is necessary in order for noise predictions to be sufficiently representative of the full system. As such, accurate representative analysis would require aerodynamic models that are able to account for unsteadiness and hysteresis.

### 2.4.6 Validity of Amiet's Models for Rotating Machinery

The Amiet model is valid only for a certain range of flow conditions. The validity of the model is determined by considering a ratio of the turbulent length scale and inflow velocity.

Since the model applies the von Kármán spectrum there is a peak frequency of noise predicted with reductions in the lower and higher frequency regions. The frequency at which this peak occurs represents the turbulent wavenumber at which the largest energy carrying eddies occur. By calculating this peak frequency for a range of values of  $L_t/U_0$  a log-log relationship is observed as per Fig. 2.11. The relationship is independent of

turbulent intensity,  $I_t$ , as the intensity is seen to only increase or decrease the maximum Power Spectral Density (PSD) of the peak, not its frequency location. This figure shows that at certain ratios of  $L_t/U_0$  the Amiet model will no longer hold. The figure essentially corresponds to a constant Strouhal number of around 0.1.



**Figure 2.11:** Relationship between length scale and peak frequency for Amiet’s inflow noise equation; — peak frequency; \* log-log fit  $f(x) = 0.1029x^{-1}$ . Inflow turbulence,  $I_t = 1\%$ .

For trailing edge noise, Blandeau [115] made a case for the validity of Amiet’s trailing edge noise theory for rotor noise. When compared to an exact model the Amiet model exhibits quantifiable errors for both high and low frequency variants. The errors in the Amiet model were seen to be a function of rotational velocity, where an increase in velocity saw an increase in the error term. A conclusion was drawn that errors were only attributed to extreme frequencies whilst frequencies of importance for measurements, within the mid-range, were seen to be correctly predicted.

Amiet also imposed an additional validity assumption based on the derivation of his model [1], which is expressed as  $M_0 d/L_t \rightarrow \infty$ . An assumption which concludes that, for reasonable mach numbers, the present theory should provide valid expressions for overall acoustic intensity if the incoming turbulent length scale is significantly smaller than the local airfoil span,  $d$ . When applied to wind turbine noise prediction, one must be conscious of this assumption when performing predictions with very large length scales of turbulence.

## 2.5 Summary

Rotating machinery produces noise as rotor blades, consisting of airfoils, move through air. Unsteady loading and unsteady turbulent effects can produce both tonal and broadband noise sources. The noise from these devices is primarily produced by means of aeroacoustic sources - the main body of work being described by Lighthill [8, 9].

In terms of broadband noise, airfoils produce sound by means of two primary mechanisms, namely inflow-turbulence and self-noise. These two noise sources are prevalent at different levels and frequencies. Their dominance in various types of wind turbines, in differing conditions, is a debated topic, and there is no consensus as to which mechanism is dominant at which levels. Predictions can be performed experimentally, computationally via CAA, or by using semi-empirical/analytic methods. Furthermore there is also discussion over the correct approach for the modelling of the directivity function.

Amiet's theory of inflow-turbulence noise draws considerable discrepancy across literature. The model Amiet introduced has been used throughout literature for airfoil noise prediction, even though it has numerous shortcomings (as discussed). Several corrections and modifications have been added to the model to account for various effects, such as finite thickness airfoils. However, several methods still rely on empirical constants to match results. Amiet's model is valid for the prediction of high speed rotors and other machinery operating in turbulent environments.

TBL noise is readily predicted using the available BPM model, however, recent developments of a more physically accurate model known as the TNO model have shown good agreement with isolated airfoil noise measurements. A review of airfoil noise measurements was provided by Doolan and Moreau [45] which can serve as a good set of validation data for computational studies.

Wind turbine noise is generally modelled using a combined BEM and semi-empirical methodology, whilst open rotor noise generally uses higher order models, such as CFD combined with CAA, or various hybrid methods which use CFD data to inform an analytic calculation for more accurate modelling. Noise predictions for both types of devices have been applied with varying degrees of success when compared to experiments and, as such, there is still room to attempt different types of predictions for these devices, and compare the results of future studies to those from existing work.

Fully comprehensive CAA calculations for rotating machinery are computationally expensive, due to the small scales of the pressure fluctuations that need to be resolved. In order to reduce computing time, and improve turnaround time within early stage design cycles, new, low-cost methods, with reasonable accuracy compared to the status quo, need to be developed. Furthermore, the use of basic CFD methods, such as URANS calculations, coupled with appropriate turbulence models, can be used to accurately describe aerodynamic systems when compared to experimental measurements. If CFD calculations are coupled with appropriate analytical noise prediction models, which have traditionally been hampered by poor estimations of aerodynamic parameters [114], then this approach should realise the applicability of a hybrid noise prediction method for aerodynamically generated rotor broadband noise sources.





## Chapter 3

# Noise Prediction Methodology

THE calculation of aeroacoustic noise sources can be a computationally complex problem. In particular, solving for the aerodynamically generated noise production, of a rotating system of blades, using CAA methods requires lots of computational resources - an impractical proposition for parameter and sensitivity studies undertaken in the early stages of design. A more pragmatic approach is to use the results of single airfoil noise models, in conjunction with sufficiently advanced flow solvers, to calculate overall noise from rotating machinery.

In this chapter, a methodology for the prediction of broadband noise produced by a vertical axis wind turbine is presented. Individual airfoil noise models for both self- and inflow-turbulence noise were implemented in the prediction scheme. Predictions focused solely on broadband noise since, for a VAWT, other noise sources, such as tonal noise, are considered to be negligible [90].

The methodology used to predict noise is essentially similar to that of prior codes used for HAWT noise prediction [116]. In this case the approach is reformulated for the specified machine. A single blade of the specified geometry is discretised, in 3D space, into a series of strips along the blade span, each representing an airfoil chord of finite span. The approach also further discretises the domain in time, allowing the blade to advance along its rotational path, for a single revolution. As a result the computational domain is split into a discrete number of spanwise and radial grid points. A quasi-steady stripwise approach is used to solve for the noise production. Aerodynamic input data required to solve the noise prediction models can either be calculated by means of analytic expressions, or extracted from pre-calculated CFD data. Both methods were explored, and are discussed in their own chapters which follow.

The code that has been developed, known as **ROBOT** (**RO**tor **B**roadband **nO**ise prediction **T**ool), falls under the category of Class III+ prediction tools for wind turbine noise, due to the advanced conditions that can be simulated.

## 3.1 Noise Prediction Models

For this study, four noise prediction models were implemented. The models are applied in a rotating, dynamic, coordinate system using a quasi-steady approach. Two models were

implemented to predict self-noise and another two for the prediction of inflow-turbulence noise. The results from each noise model will be compared in order to determine each ones' applicability to the prediction of VAWT noise.

A limitation of these noise models is that aerodynamic interactions between following blades is not considered - the blades are modelled in isolation. It is also assumed that the self-noise produced by the wind turbine blades is produced under steady flow conditions. In summary; the formulations assume steady, free stream, conditions under a quasi-steady time dependence.

### 3.1.1 Tonal Noise

The harmonic content of noise produced by VAWT's, whilst predictable, is generally considered to be negligible compared to the broadband component of noise produced by the device [89, 90]. A typical VAWT at standard operating conditions will have a BPF that corresponds to a tonal noise signal at frequencies lower than 100 Hz. This low frequency noise is of little consequence to the prediction of overall noise from a VAWT, particularly when apply A-weighting filters, and was thus excluded from the modelling procedure.

Quasi-tonal noise sources, such as those produced by vortex shedding and laminar boundary layer effects, generally do not manifest themselves in the turbulent environments that VAWT's operate in, and are also excluded from the modelling approach [2]. These noise sources also only tend to occur when airfoils operate in low-energy laminar flow regimes. The turbulent environments, and self-excited turbulent interaction, of VAWT's means that fully laminar flow is not possible [80].

### 3.1.2 Airfoil Self-Noise

Airfoil self-noise is produced by an airfoil moving through a fluid medium [2]. Turbulent Boundary Layer (TBL) noise is produced by the scattering of the turbulent energy within a hydrodynamic boundary layer by an airfoil trailing edge. This scattered energy propagates to the farfield as an acoustic signal. Brooks et al. [2] modelled several types of airfoil self-noise in their paper. In this study, two models for TBL noise were examined and applied to the prediction problem.

### A Note on Stall Noise

During stall the flow over an airfoil separates and, from an acoustic perspective, the blade begins to act as a bluff body in flow. For light stall conditions, airfoil noise generally increases in intensity by up to 5 dB and reduces the peak frequency [59]. During heavy stall, however, noise can be seen to increase by as much as 10 - 15 dB [60].

A VAWT typically experiences dynamic stall, attributed to the constant changes in angle of attack with rotation. During this event, lift increases with angle of attack to values typically higher than those experienced by individual airfoils. Once full separation and deep stall occurs, there is a recovery of lift back to static conditions. Dynamic lift is attributed to large scale vortices forming at the leading edge and travelling down the suction side of the airfoil.

#### i. Brooks Pope and Marcolini Model

The commonly used airfoil self-noise model developed by Brooks et al. [2] (BPM model) relates the flow over an airfoil - in particular, the estimates of the boundary layer displacement thickness, to the radiated acoustic signal at the trailing edge. This model has been applied to noise predictions of other basic airfoils at a wide range of operating conditions (up to transonic speeds) with relatively good agreement, however, this model is limited by the fact that only a single airfoil type was considered [117].

The fundamental BPM model for TBL noise is defined by three equations with subscripts  $p$ ,  $s$ , and  $\alpha$  denoting the contribution to the overall airfoil noise for the pressure side, suction side and high angle of attack (separation-stall). These are

$$W_p(f) = 10 \log_{10} \left( \frac{\delta_p^* M_0^5 d D_h}{z^2} \right) + A \left( \frac{St_p}{St_1} \right) + (K_1 - 3) + \Delta K_1 \quad (3.1)$$

$$W_s(f) = 10 \log_{10} \left( \frac{\delta_s^* M_0^5 d D_h}{z^2} \right) + A \left( \frac{St_s}{St_1} \right) + (K_1 - 3) \quad (3.2)$$

$$W_\alpha(f) = \begin{cases} 10 \log_{10} \left( \frac{\delta_s^* M_0^5 d D_l}{z^2} \right) + B \left( \frac{St_s}{St_2} \right) + K_2 & \alpha < 25^\circ \\ 10 \log_{10} \left( \frac{\delta_s^* M_0^5 d D_l}{z^2} \right) + A' \left( \frac{St_s}{St_2} \right) + K_2 & \alpha \geq 25^\circ \end{cases} \quad (3.3)$$

and thus the sum of the contributions becomes a logarithmic sound pressure level addition,

$$W_{\text{BPM}}(f) = 10 \log_{10} (10^{W_p/10} + 10^{W_s/10} + 10^{W_\alpha/10}) \quad (3.4)$$

where  $W_{\text{BPM}}(f)$  is the sound pressure level in third octave bands,  $\delta^*$  is the boundary layer displacement thickness,  $M_0$  is the inflow mach number,  $d$  is the discretised blade span,  $D$  is the directivity function,  $z$  is the absolute distance to the receiver, the  $St$  variables are various definitions for Strouhal contributions described in the original NASA report,  $A$ ,  $A'$  and  $B$  are interpolation functions of experimentally derived curves and  $K$  is an amplitude correction function.  $A'$  is the same function as  $A$  but for a value of  $\text{Re} = 3\text{Re}$ . Eq. (3.3) switches at high angles of attack. When the critical angle is reached  $W_p = -\infty$  and  $W_s = -\infty$ .

Functions  $A$  and  $B$ , as well as the  $K$  correction factors are described in detail in Appendix A.

## ii. TNO models

In order to account for the shortcomings of the BPM model and to improve noise prediction accuracy for non-symmetric airfoils in unsteady flow the TNO model was implemented. The TNO model for airfoil self-noise, developed by Blake and Parchen [4, 47], has gained much traction in the airfoil noise community of late and shows good agreement with measurements [44, 118, 119]. The version used in this study is known as the iTNO model [46] and reduces the number of input variables, decouples the convection velocity from the boundary layer height, and shows validated results that are in better agreement than previous studies.

The model incorporates a definition of the wavenumber frequency pressure spectrum which provides the changes in airfoil surface pressure at various wave numbers defined as,

$$\Phi_P(\mathbf{k}, \omega) = 4 \frac{\rho_0^2}{U_c} \int_0^\infty L_{t,2}(x_2) \left( \frac{\partial U_1(x_2)}{\partial x_2} \right) \overline{u_2^2(x_2)} \phi_{22}(k_1(x_2, \omega)) e^{-2x_2|k_1(x_2, \omega)|} dx_2 \quad (3.5)$$

where  $\Phi_P$  is the wavenumber frequency pressure spectrum as a function of the turbulent wavenumber  $k$ , and  $\omega$  the angular frequency. The subscripts denotes direction as per the coordinate system in Fig. 3.1. Integration occurs over the boundary layer profile normal to the airfoil surface along  $x_2$ .  $\rho_0$  is the density of the medium.  $U_c$  is the convection

velocity defined as  $U_c = 0.65U_0$ , this decouples the convection velocity from the boundary layer velocity making the quantities of the wavenumber,  $k_1$  as well as this velocity ( $U_c$ ) independent of the boundary layer coordinate  $x_2$  [46].  $L_{t,2}$  is the transverse integral length scale in the boundary layer,  $U_1$  is the boundary layer velocity.  $\overline{u_2^2}$  is the turbulent shear stress in the boundary layer.  $\phi_{22}$  is the energy density spectrum. With all variables known the model is the closed and can be used to calculate farfield noise of an airfoil section.

The acoustic wavenumber,  $k_1$ , used in the TNO model is decoupled from the boundary layer and a constant convection velocity is used [46],

$$k_1 = \frac{2\pi f}{0.65U_0} \quad (3.6)$$

where the subscript, 1, denotes flow along the airfoil surface (Fig. 3.1),  $U_0$  is the blade inlet velocity.

The wavenumber of the main energy bearing eddies,  $k_e$ , can then be defined in terms of the longitudinal integral length scale [1] as,

$$k_e = \frac{\sqrt{\pi} \Gamma(5/6)}{L_t \Gamma(1/3)} \quad (3.7)$$

where,  $\Gamma$  is the Gamma function, and  $L_t$  is the longitudinal integral length scale of turbulence.

Assuming an isotropic relationship, the longitudinal length scale can be related to the transverse length scale by [46],

$$2L_{t,2} = 2L_t, \quad (3.8)$$

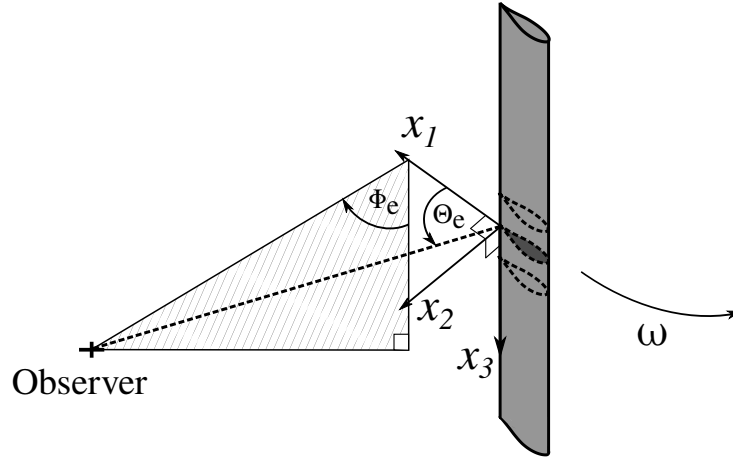
and then, combining Eq. (3.8) with Eq. (3.7) becomes,

$$k_{e,2} = \frac{0.7468}{2L_{t,2}} \quad (3.9)$$

Equation (3.5) is closed with an expression for energy density spectrum,

$$\phi_{22}(k_1) = \frac{4}{9\pi} \frac{\beta_1 \beta_3}{k_{e,2}^2} \frac{(\beta_1 k_1 / k_{e,2})^2}{[1 + (\beta_3 k_1 / k_{e,2})^2]^{7/3}} \quad (3.10)$$

where  $k_{e,2}$  is the wavenumber of the transverse integral length scale in the boundary layer [1],  $\beta_1$  and  $\beta_2$  are stretching factors defined by Lee [46] to be  $\beta_1 = 1$  and  $\beta_3 = 3/4$ .



**Figure 3.1:** Local blade coordinate system and angles used for the directivity functions.

The energy density spectra follows the form of a von Kármán turbulence spectrum that relates the incoming turbulence wavenumbers,  $\mathbf{k}$ , to the acoustic energy spectrum.

The farfield acoustic pressure spectral density (PSD), required to compare results to those of experiments is defined as a function of the angular frequency by,

$$S_{pp}(\omega) = \frac{d}{4\pi z^2} \int_{-\infty}^{\infty} \frac{\omega}{c_0 k_1} \Phi_P(\mathbf{k}, \omega) |_{k_3=0} dk_1 \quad (3.11)$$

where  $d$  is discretised spanwise extent of the trailing edge,  $z$  is the distance to the receiver,  $c_0$  is the local speed of sound. The narrowband SPL is found from,

$$W_{\text{TNO}}(f) = 10 \log_{10} \left( \frac{4\pi S_{pp}(\omega)}{P_{\text{ref}}^2} D \right) \quad (3.12)$$

where  $P_{\text{ref}} = 20\mu\text{Pa}$ , the factor  $4\pi$  is introduced to convert the double-sided spectrum to a single sided spectrum and convert back from angular frequency.  $D$  is the directivity function which will be defined later.

### 3.1.3 Inflow-Turbulence Noise

Inflow-turbulence noise is a source of aerodynamic noise produced as airfoil blades interact with inflow turbulence. Amiet [1], and then later Paterson and Amiet [120], defined expressions for the far-field acoustic power spectral density of an airfoil in a turbulent stream. Amiet defines incoming turbulence using the von Kármán spectrum, and this

model is seen to be well suited in its applicability to aircraft operating in turbulent environments. Buck et al. [6] suggests that the Kolomogorov and von Kármán turbulence spectra are equivalent, at large length scales, encountered by large wind turbines. One outcome of this study will be to determine whether these models are applicable to small VAWT noise prediction.

### i. Paterson and Amiet's Inflow-Turbulence Noise Model

The model of Paterson and Amiet presented [120] is presented here, in SI units, noting that some unit based inconsistencies exist in reported use of these formulations. In addition, the high and low-frequency components of noise are combined using a blending function as proposed by Lowson [34] and Moriarty and Migliore [39].

The model is defined by the following equations,

$$W_{Inflow} = W_{Inflow}^H + 10 \log_{10} \left( \frac{\text{LFC}}{1 + \text{LFC}} \right) \quad (3.13)$$

$$W_{Inflow}^H = 10 \log_{10} \left[ \frac{\rho_0^2 c_0^2 d}{2z^2} M_0^3 u'^2 L_t \frac{(k/k_e)^3}{(1 + (k/k_e)^2)^{7/3}} D \right] + 78.4 \quad (3.14)$$

$$\text{LFC} = 10 S^2 (1 + 9\alpha^2) M_0 \left( \frac{kc}{2} \right)^2 \beta^{-2} \quad (3.15)$$

$$S^2 = \left[ \frac{2\pi kc}{2\beta^2} + \left( 1 + 2.4 \frac{kc}{2\beta^2} \right)^{-1} \right]^{-1} \quad (3.16)$$

where  $\rho_0$  is the density of the medium,  $c_0$  is the speed of sound,  $D$  is the directivity function,  $k$  is the inflow wavenumber ( $k = (2\pi f)/U_0$ ),  $k_e$  is the wavenumber of the main energy bearing eddies (Eq. (3.7),  $L_t$  is the inflow turbulent length scale,  $d$  is local airfoil span width,  $c$  is chord length,  $M$  is the mach number, LFC is a low frequency correction,  $\alpha$  is the local airfoil angle of attack,  $u'$  is the turbulent velocity,  $z$  is the distance from the airfoil leading edge to the receiver,  $S$  is an approximation of the compressible Sears function [34] and  $\beta^2 = 1 - M^2$ .

### ii. Buck's Inflow Noise Model

Buck et al. [6] proposed a modification to the Paterson and Amiet model for calculating



inflow-turbulence noise. He suggested applying the Kolmogorov turbulence spectrum [121] in place of the von Kármán spectrum as it was seen that, for large turbines, at sufficiently high frequencies and large enough atmospheric length scales the models converged to the same result. This modification makes it easier to measure turbulence parameters experienced by the turbine as it is easier to measure turbulent dissipation than a turbulent length scale. Furthermore the new method requires shorter measurement times which further speeds up predictions. By replacing the turbulence spectrum with that of Kolmogorov, the turbulence parameter of interest then becomes,  $\epsilon_t$  the turbulent dissipation rate. Buck's model modifies Eq. 3.14 to obtain,

$$W_{Inflow,B}^H = 10 \log_{10} \left[ \frac{\rho_0^2 c_0^2 d}{2z^2} M_0^3 \epsilon_t^{2/3} k^{-5/3} D \right] + 77.6 \quad (3.17)$$

where the rest of the model is used in the same manner as in Eqs (3.13) - (3.16). The results from both models will be presented in this paper.

### 3.1.4 Directivity Models

Due to the relative motion between the airfoil and the stationary receiver, of the order of Mach 0.1, a doppler amplification factor is introduced [111]. This factor accounts for the increase or decrease in source frequencies as the blades move towards or away from a stationary observer.

If noise producing eddies are sufficiently small compared to convection velocities i.e. acoustic wavelengths are shorter than airfoil chord lengths then the directivity function follows the formulation of Burley and Brooks [58] who derived a directivity function specifically designed for rotors as,

$$D_h = \frac{2 \sin^2(\Theta_e/2) \sin^2 \phi_e}{(1 - M_0 \cos \xi_e)^4} \quad (3.18)$$

where angles  $\Theta_e$  and  $\phi_e$  are defined in Fig. 3.1,  $\xi_e$  is defined as the angle from the blade chord inflow velocity to the line running from the source to the observer, essentially it is  $\phi_e$  corrected for blade angle of attack.  $M_0$  is the local blade mach number.

For low frequency directivity, i.e. when the acoustic wavelength is of comparable size to the that of the airfoil chord, the directivity function is defined as a compact dipole [2, 58, 122],

$$D_l = \frac{\sin^2 \Theta_e \sin^2 \phi_e}{(1 - M_0 \cos \xi_e)^4} \quad (3.19)$$

with all angles defined as before.

For self-noise, the directivity functions are implemented as described in Eqs (3.1) - (3.3), where the correct directivity equation used is dependent on the noise formulation implemented. For inflow-turbulence noise, the low frequency cut-off should be observed by switching the functions depending on the value of the acoustic wavelength, compared to the chord length. Furthermore, since inflow-turbulence noise is radiated from the leading edge the value of  $\Theta_e$  should be replaced by  $(\pi - \Theta_e)$  [6] to invert the directional dependence of this term.

### 3.1.5 Total Noise Calculations

The noise models are applied to the rotor computational domain using a quasi-steady assumption i.e. the acoustic time scale is much shorter than that of the rotating aerodynamic system. The noise models provide an instantaneous acoustic signal at the retarded emission point for a single, finite span, airfoil section and as a result of the constant axial rotation some phase information is required to account for shifts in frequency due to the relative motion of the blades with respect to the stationary observer. A doppler shift term accounts for the directional rotation with respect to the observer location [40].

Using the approach of Burley and Brooks [58], the acoustic pressures along the blade and throughout a single rotation can then be calculated. The broadband noise signal for a particular noise model at the observer is,

$$G_{BB,j}(f) = \sum_{b=1}^{s_n} \sum_{d=1}^{ts} \frac{n}{n_\phi} \left( \frac{f}{f_0} \right)_{bd} [G_j(f)]_{bd} \quad (3.20)$$

where  $G_{BB}$  is the overall acoustic pressure signal for a given noise model, the subscript  $j$  refers to one of the four noise models discussed above.  $n$  is a weighting factor applied for the number of blades,  $n_\phi$  is the number of angular discretisation points for a single blade revolution.  $(f)/(f_0)_{bd}$  is the doppler frequency shift, a correction factor to account for time compression and expansion effects due to the relative blade flow in the freestream [58].  $[G(f)]_{bd}$  represents the acoustic pressure at the observer location for a given noise

model,  $j$ . Subscripts  $b$  and  $d$  denote angular and spatial discretisation points.

The relationship between the observed frequency,  $f$ , and the emitted frequency,  $f_0$  is given by a doppler correction [111],

$$\frac{f}{f_0} = \frac{c_0 + U_r}{c_0 + U_s} \quad (3.21)$$

where  $U_r$  is the velocity of the receiver relative to the medium; positive if the receiver is moving towards the source (and negative in the other direction), and  $U_s$  is the velocity of the source relative to the medium; positive if the source is moving away from the receiver (and negative in the other direction). For the cases in this analysis  $U_r$  is zero as the observer is always considered to be stationary.

### 3.1.6 Known limitations of the Noise Models

The prediction of broadband rotor noise becomes increasingly more accurate, as physical explanations of noise sources are improved and modelled. The four noise models described in this section, whilst validated against experiments, are not fully comprehensive in describing noise of rotating systems. The individual airfoil models used need to be considered with a number of assumptions and limitations as follows:

- In this study blade tip effects are neglected. It is assumed that each airfoil operates as an efficient 2D airfoil. Noise sources from tip effects and vortex shedding will be excluded.
- The effect of confluence, the aerodynamic interaction between following blades, is not accounted for in the noise prediction models. It is assumed that the noise produced by an airfoil is only produced by steady noise sources, and that additional confluent noise sources are negligible.
- The individual airfoil noise prediction models for self-noise and inflow-turbulence noise were both formulated for airfoils in steady flow conditions. Whilst these airfoils can be moving in a rectilinear fashion, it is generally considered that the aerodynamic conditions of the system are unchanging. In implementing these models for a moving blade an assumption needs to be made that the aeroacoustic system will act the same

way that it does for an equivalent local aerodynamic system during a specified time step. This leads to an assumption that the system becomes quasi-steady whereby the aeroacoustic response to the aerodynamic system is no longer dependent on time.

## 3.2 Computational Implementation - ROBOT

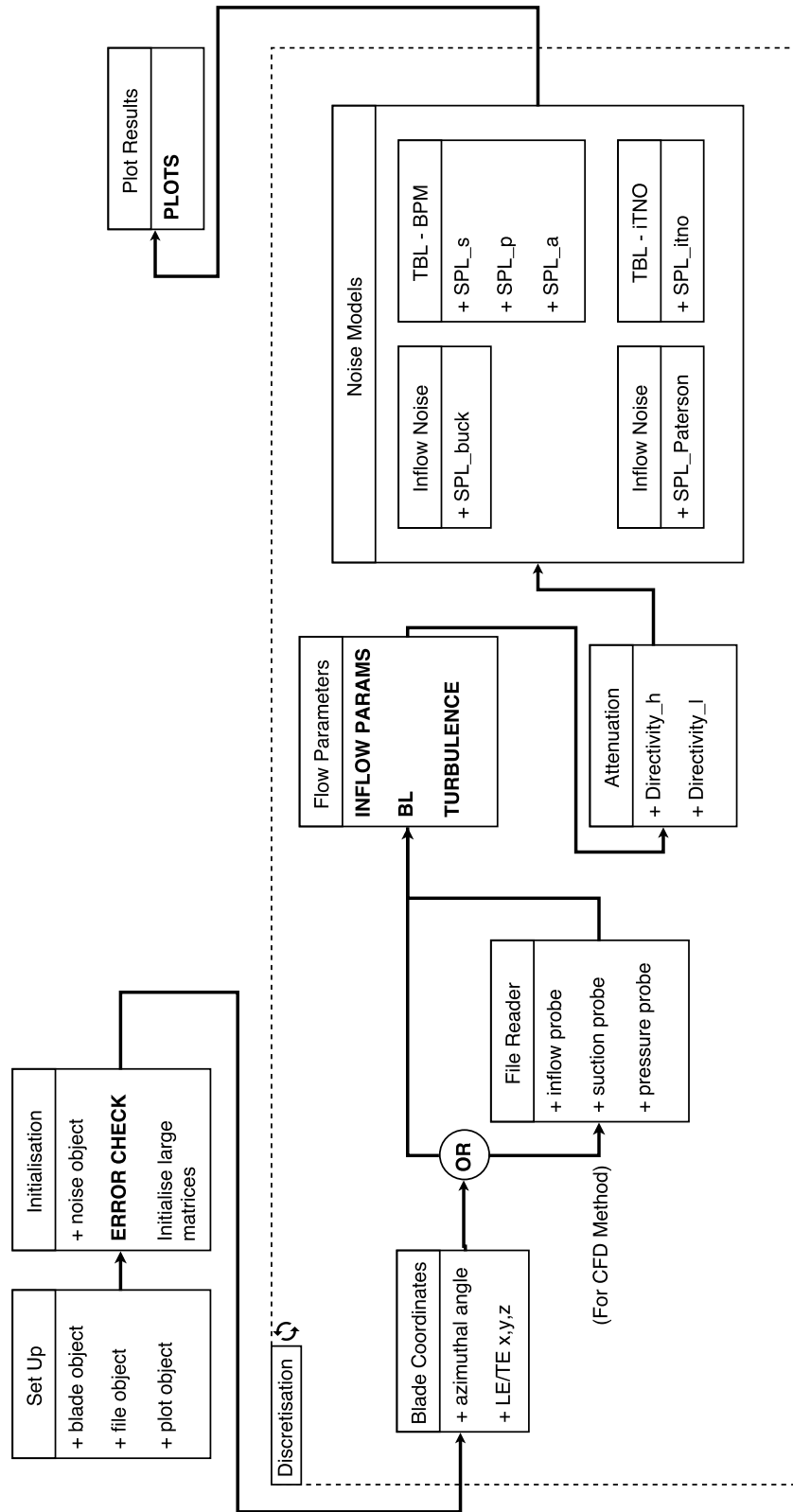
The individual airfoil noise models described are combined with two types of flow models (described later), in order to predict overall broadband noise. The computational procedure was developed in **MATLAB** and was implemented in a code referred to as **ROBOT** (**RO**tor **B**roadband **nO**ise prediction **T**ool).

### 3.2.1 Code Structure

The code is described by the system diagram shown in Fig. 3.2, which summarises the modules and shows the flow of data through the **ROBOT** algorithm. In summary; a user defines a model and simulation parameters, the model is initialised and some checks are performed. The discretised model is then analysed; firstly the flow over each section is modelled, and thereafter the noise from that section is calculated. Finally noise results are summed and the requested data plots are displayed.

A new turbine is defined in the *Set Up* block. The *blade object* contains data relating to the turbines geometry as well as operating conditions. The *file object* contains directories linking to CFD data and the *plot object* contains options for the type of analysis to be completed.

Everything contained in the discretisation block (dotted lines) is computed for each spatial and temporal point defined in the *Set Up*.



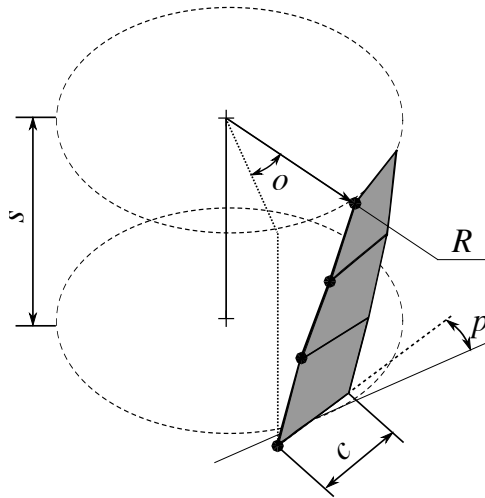
**Figure 3.2:** System diagram describing modelling procedure

### 3.2.2 Noise Prediction Methodology

The methodology used to predict VAWT noise is essentially similar to that of prior codes used for HAWT noise prediction [116], but is reformulated for the vertical rotation of the turbine blades. A single blade of the specified turbine geometry is discretised in 3D space into a series of strips, each representing an airfoil chord of finite span. The model is further discretised in the time domain, and allowed to advance along its rotational path for a single revolution. The flow across each blade is then either solved using the analytical solver or by means of imported, pre-calculated, CFD data. Noise models are solved for each grid point in the computational domain.

#### Discretisation

Spatial discretisation is performed by splitting the blade into a number of spanwise sources along the blade. Fig. 3.3 shows a representative turbine blade discretised into four spanwise sources. Variables shown are the geometric parameters required to define the machine. This approach also further discretises the domain in time allowing the blade to advance along its rotational path for a single revolution. As a result the computational domain is split into a discrete number of spanwise and radial grid points.



**Figure 3.3:** Geometric parameters required to model a turbine blade. • indicates trailing edge.

Temporal discretisation requires the blade to advance throughout a single rotation ( $\Phi = 0$  to  $360^\circ$ ). The user is able to initially define an angular discretisation, equal to the

absolute number of angular positions that will be evaluated for the given simulation ( $n_\phi$ ). A sampling percentage ( $s_p$ ) is also defined which describes the overall percentage of the total angular discretisation to be sampled. This two stage process is required in order to match up the number of time steps in the code to the maximum number of available time steps from a pre-calculated CFD simulation. Thereafter the available databank can be sampled such that only a portion of the available time steps are requested. For example; a certain CFD simulation was performed and required 360 time steps to complete a full rotation. The user would set the time step,  $ts$ , equal to 360. Thereafter the user could perform a sensitivity study by selecting an appropriate percentage of the available data. For example, setting the sampling percentage,  $s_p$ , to a value of 10 % would yield 36 equidistant calculation points for a single rotation.

By temporally and spatially discretising the domain, a series of computational grid points are established whereby each grid point requires its own flow and acoustic solution, which then contribute to the overall noise production of the machine.

### Simulation Parameters

The turbine's operating conditions, rotational velocity (RPM) as well as the wind speed, are required to define the simulation. The wind speed, also known as the domain inlet velocity, corresponds to the required atmospheric air speed needed for the device to rotate at the given RPM. As well as the discretisation used, additional parameters required are number of blades and the listener location - a  $1 \times 3$  matrix defining the location in space. The coordinate system used is given in Fig. 4.1. The required inputs are listed in Table 3.1.

Once all simulation parameters are defined, the aerodynamic model can be solved. The aerodynamic model describes the flow over the blade, either at the blade inlet or along the airfoil trailing edge boundary layer depending on which noise model is being evaluated. A flow solution is required to calculate noise using the aforementioned prediction models. A summary of the flow input parameters required for each of the given noise models is given in Table 3.2. The density of the medium,  $\rho_0$ , is also required for calculation and is assumed constant. These parameters are typically extracted from experimental data and used to tune models [2, 6, 58]. When performing noise predictions for new rotor designs,

| Category              | Parameter              | Symbol         | Units |
|-----------------------|------------------------|----------------|-------|
| Geometry              | Radius                 | $R$            | m     |
|                       | Blade Span             | $s$            | m     |
|                       | Blade Chord            | $c$            | m     |
|                       | Blade Pitch            | $p$            | °     |
|                       | Helical Twist          | $o$            | °     |
|                       | Number of Blades       | $n$            |       |
| Operating Conditions  | Rotational Velocity    | $\omega$       | rad/s |
|                       | Inlet Velocity         | $U_\infty$     | m/s   |
| Turbulent Conditions  | Turbulent Intensity    | $I_t$          | %     |
|                       | Integral Length Scale  | $L_t$          | m     |
| Simulation Parameters | Angular Discretisation | $n_\phi$       |       |
|                       | Sources Along Span     | $s_n$          |       |
|                       | Receiver Location      | $\mathbf{z}_i$ | m     |

**Table 3.1:** Input parameters required to define a simulation.

measurement data used for verification are often not available until the prototyping stage. Thus, analytical predictions of the required input parameters can be performed to support measured data in early project development.

In this study two approaches were used to calculate the flow input parameters seen in Table 3.2. The first approach used analytical models, which calculate velocity and angle of attack from geometric assumptions. These parameters are then used to solve for inflow turbulence using a combination of fundamental approaches primarily based on idealised flat plate theories, or semi-empirical models. The second approach made use of pre-calculated 2D CFD calculations using a URANS method. Both of these methods are described in subsequent chapters.

Parameters detailed in Table 3.2 are described as follows;  $\delta_i^*$  is the boundary layer displacement thickness,  $U_0$  is the freestream blade inlet velocity,  $\alpha$  is the effective blade angle of attack,  $L_{t,i}$  is the transverse integral length scale in the boundary layer,  $U_i$  is the boundary layer velocity,  $k_t$  is the turbulent kinetic energy,  $L_t$  is the freestream longitudinal integral length scale at the blade inlet,  $I_t$  is the freestream turbulent intensity at the blade



| Noise Source      | Model    | Flow Inputs               | Notes                 |
|-------------------|----------|---------------------------|-----------------------|
| Self Noise        | BPM      | $\delta_i^*, U_0, \alpha$ |                       |
|                   | TNO      | $L_{t,i}, U_i, k_t$       | In the boundary layer |
| Inflow-Turbulence | Paterson | $L_t, I_t, U_0, \alpha$   | At blade inlet        |
|                   | Buck     | $\epsilon_t, U_0, \alpha$ |                       |

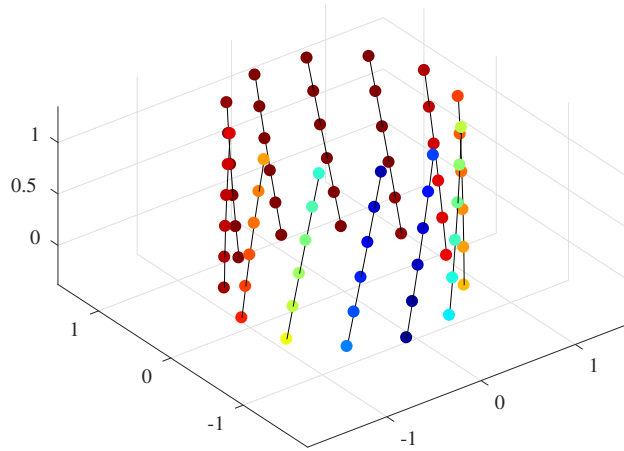
**Table 3.2:** Flow inputs required to solve each noise model.

inlet, and  $\epsilon_t$  is the freestream turbulent dissipation rate at the blade inlet.

### 3.2.3 Noise Summations

Each grid point in the computational domain is considered as a compact point source. These sources can be represented in the computational domain as in Fig. 3.4. In this figure each coloured point represents a point source located at the trailing edge of a VAWT blade. Each line represents a distinct time step in the domain. The colour of the points represents intensity. This particular case has 12 time steps per rotation with 6 point sources located along the blade span, equating to a total of 72 individual noise sources.

After all the simulations have been completed the total noise signal for one particular source, as experienced at the receiver location, is calculated using Eq. (3.20).



**Figure 3.4:** Computational domain of noise point sources for an arbitrary VAWT

### 3.2.4 Outputs

ROBOT was developed to produce several types of data useful for rotor noise calculation and analysis. These data plots are selected at the start of the analysis and outputted to the screen as plots. Data is also accessible as matrices after analysis is complete.

The following types of plots are available:

- **SPL vs. frequency:** Provides a noise spectra at a given listener location .
- **SPL vs. frequency (A-weighted):** Adds A-weighting curve to the above result.
- **Directivity:** Gives OASL with azimuth for a range of listener locations in a 360 degree arc around the turbine.
- **Boundary Layer Profiles:** Plots six non-dimensionalised boundary layer profiles for the VAWT aerodynamic system at equidistant azimuthal angles.
- **Turbulence Parameters:** Plots  $L_t$ ,  $u'$ ,  $I_t$ , against azimuth.
- **Velocity Parameters:** Plots  $U_0$ ,  $\alpha$  against azimuth.
- **Boundary Layer Parameters:** Plots  $\delta$ ,  $\delta^*$  against azimuth.
- **Noise Contours:** Provides a 2D contour of OASL with axes of azimuth and z height, centered in the direction of the listener.
- **Source Localisation:** Provides a 2D contour of SPL with axes of frequency and azimuth.
- **OASL vs velocity:** Plots OASL values against an array of different inflow velocities. Inflow velocity needs to be defined as a vector. Can only be used with the analytic approach.
- **Convergence plots:** Plots residuals for changes in number of sources along the span,  $s_n$  and number of angular discretisation points,  $n_\phi$ . Quantities need to be vectors.

### 3.3 Summary

This chapter described the numerical tool, **ROBOT**, which was designed to predict broadband aeroacoustic noise from a VAWT, based on individual airfoil noise models. Several models were discussed with regards to their applicability to the current problem. The code has a number of known limitations and shortcomings, in particular the method of quasi-steady analysis and 2D flow assumptions. Results derived from the use of this tool are presented in the chapters that follow.



## Chapter 4

# Analytic Methods

IN this chapter; the programme, **ROBOT**, was used to analyse the broadband noise produced by an existing VAWT design at a range of operating conditions. This chapter describes the models used to analytically predict inflow and boundary layer flow parameters. This model was used to determine the input data required to solve for the noise models, described in Chapter 3. Furthermore, additional models for turbulent parameters implemented in **ROBOT** are presented and used for analysis.

Analytic flow models were implemented to perform benchmark analysis of the current status quo for VAWT noise prediction. One of the outcomes of this chapter is to quantify the prediction error in the analytical approach when compared to measurements. This will aim to confirm initial comments by Botha and Rice [18] with regards to which flow models work best for this type of prediction problem.

A verification study of the BPM and iTNO self-noise models was performed, by comparing predictions of these models to noise measurements for an individual airfoil in flow. Noise predictions for the quietrevolution QR5 were also compared to measurements. This analysis acts as verification of the **ROBOT** code to ensure that noise models are mathematically correct and accurate.

## 4.1 Flow Parameter Modelling

This section describes the models used to predict inflow and boundary layer flow parameters. For an airfoil in flow, the most important parameters of interest for noise prediction are the turbulent statistics and flow velocities around the blade. Necessary parameters were described in the previous chapter in Table 3.2. Equations for flow modelling are presented, or derived, and relationships between flow parameters and turbulence are discussed. These are all related to the problem of airfoil aerodynamics, as applied to a rotating VAWT coordinate system.

### 4.1.1 Blade Inflow Velocity and Angle of Attack

Blade inflow velocity and angle of attack are reconstructed from geometric relations of velocity described by vectors of flow in Fig. 4.1a, where  $\lambda = (\omega R)/(U_\infty)$  is the tip speed ratio [123]. Assuming the 2D blade section rotates at a constant velocity and encounters

a constant freestream velocity; the local blade angle of attack and inlet velocity can be calculated from geometric vector addition,

$$\alpha = \tan^{-1} \left( \frac{\sin\Phi}{\cos\Phi + \lambda} \right) + p \quad (4.1)$$

$$U_0 = U_\infty \sqrt{1 + 2\lambda \cos\Phi + \lambda^2} \quad (4.2)$$

where  $\alpha$  is the effective local blade angle of attack,  $\Phi$  is the azimuthal angle as defined in Fig. 4.1,  $p$  is the fixed pitch angle of the airfoil,  $U_\infty$  is the domain inlet velocity and  $U_0$  is the local inflow velocity at the blade inlet, essentially the freestream velocity equivalent for a 2D infinite airfoil in flow. The local blade inlet velocity,  $U_0$ , is one of the most important parameters for noise prediction and accurate prediction thereof is essential.

### 4.1.2 Turbulence Parameters

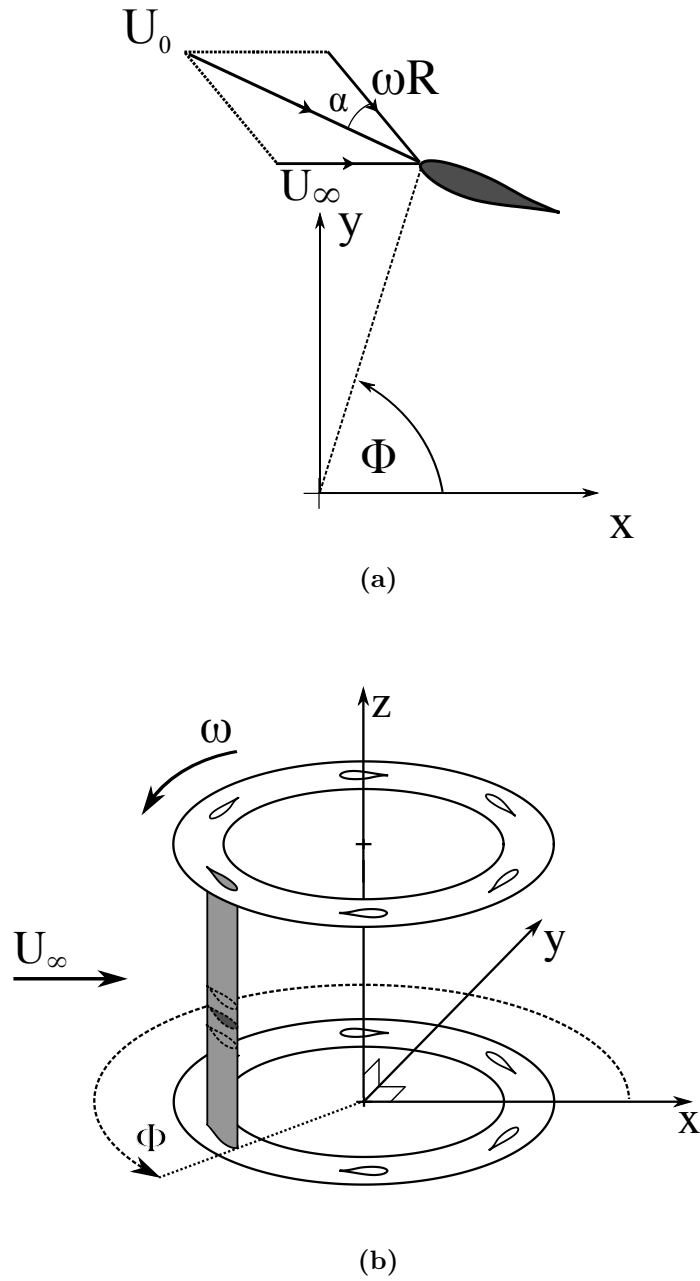
The noise prediction models used in this study require several turbulence parameters at the local blade inlet, in order to relate the flow solution to the acoustic signal radiated by the airfoil section. Models for inflow-turbulence noise require two parameters for the turbulence, the integral length scale of turbulence,  $L_t$ , and the turbulence intensity,  $I_t$ , or alternatively the turbulent dissipation rate,  $\epsilon_t$ . For HAWT noise prediction, these parameters are generally measured at the turbine site prior to installation and noise analysis.

#### Integral Length Scale

The integral length scale of turbulence, at the airfoil inlet, is defined as a convenient measure of the extent of the region over which turbulent velocities are appreciably correlated [121]. As a physical quantity, it describes the size of the largest energy bearing eddies in a turbulent flow. Using the assumption of isotropic turbulence, this length scale is defined as,

$$L_{t,1}(x_1) = \int_0^\infty R_{11}(x_1) dx \quad (4.3)$$

where  $R_{11}(x_1)$  is the axial cross-correlation of the turbulent velocity in the flow direction,  $u'_1$ ,  $L_t$  refers to the integral length scale with the subscript denoting the directional



**Figure 4.1:** Coordinate system used throughout this thesis. (a) 2D Local blade coordinates and flow relationships.  $U_\infty$  is inflow velocity,  $\omega$  is rotational velocity,  $R$  is radius, and  $\alpha$  is defined as the effective angle of attack of the airfoil. Azimuthal angle,  $\Phi$ , is defined as zero on the  $x$  axis and increases positively with counterclockwise rotation. (b) 3D coordinate system showing the location of inlet velocity in relation to rotating airfoil blades.



extent of the quantity with coordinates defined as per Fig. 3.1. When using the analytical model, the inlet turbulence parameters remain constant with rotation. Therefore the correct average measure of this parameter within the rotor is important.

### Transverse Integral Length Scale in the Boundary Layer

The length scale measured in the boundary layer, as used as input to the iTNO noise model, differs to that of the inflow integral length scale of Eq. (4.3). Whilst the more commonly used length scale is referred to as the longitudinal integral correlation scale; the length scale considered here is related to the vertical dimensions of turbulent eddies - a distinction made clear by Kamruzzaman et al. [124]. The definition thereof is the integral of the normalised spatial two point correlation coefficient of the vertical velocity fluctuations. We refer to this length scale as the transverse length scale, denoted by a subscript 2 to indicate the direction,

$$L_{t,2}(x_2) = \int_0^\infty R_{22}(x_2, \Delta x_2) d(\Delta x_2) = \int_0^\infty \frac{\overline{u'_2(x_2)u'_2(x_2 + \Delta x_2)}}{\sqrt{\overline{u'^2_2(x_2)}\overline{u'^2_2(x_2 + \Delta x_2)}} d(\Delta x_2) \quad (4.4)$$

where  $R_{22}$  is the correlation coefficient between the turbulent velocity  $\overline{u'}$  normal to the airfoil surface and the same signal separated by  $\Delta x_2$ . This definition of integral length scale is the same as in Eq. 4.3 however the direction of the flow parameters has been altered and the location is no longer in the freestream but along the airfoil trailing edge. This length scale can be solved for analytically by starting with Prandtl's mixing length hypothesis to solve for the mixing length,  $l_m$ ,

$$l_m = 0.085\delta \tanh\left(\frac{\kappa x_2}{0.085\delta}\right) \quad (4.5)$$

where  $\kappa$  is the von Kármán constant,  $\kappa = 0.41$ , and  $\delta$  is the boundary layer thickness defined as the wall distance where  $U = 0.99U_\infty$  normal to the airfoil trailing edge. The above relationship is non-physical as the mixing length does not increase linearly through the boundary layer. A damping function is applied near the wall by means of the Klebanoff damping function as proposed by Kamruzzaman et al. [51],

$$l_m = \frac{0.085\delta \tanh\left(\frac{\kappa x_2}{0.085\delta}\right)}{\sqrt{1 + 5.5\left(\frac{x_2}{\delta}\right)^6}} \quad (4.6)$$

and then by definition the mixing length is related to the transverse integral length scale of turbulence,

$$L_{t,2} = \frac{l_m(x_2)}{\kappa} \quad (4.7)$$

### Turbulent Kinetic Energy

Turbulent kinetic energy describes the kinetic energy per unit mass within the turbulent velocity fluctuations. In general the turbulence kinetic energy is defined as,

$$k_t = \frac{1}{2} \overline{u'_i u'_i} = \frac{1}{2} (\overline{u'^2_x} + \overline{u'^2_y} + \overline{u'^2_z}) \quad (4.8)$$

where  $\overline{u'}$  is a turbulent velocity fluctuation, and subscripts denote cartesian coordinates. Assuming isotropic turbulence and rearranging, yields a definition for the root mean square turbulent velocity fluctuation as a function of the turbulent kinetic energy, as a general one-dimensional quantity,

$$u'_{RMS} = \sqrt{\frac{2}{3} k_t} \quad (4.9)$$

This can then, in turn, be used to solve for the turbulence intensity,  $I_t$ , at the inlet to a blade section as per the definition of the quantity,

$$I_t = \frac{u'_{RMS}}{U_0} \quad (4.10)$$

### Kinetic Energy in the Boundary Layer

The turbulent kinetic energy profile in the boundary layer is reconstructed using Prandtl's mixing length hypothesis, following the approach of Lee [46]. It is assumed that turbulent viscosity,  $\nu_t$ , is related to the mixing length following,

$$\nu_t = l_m^2 \left| \frac{\partial U_1}{\partial x_2} \right| \quad (4.11)$$

where  $l_m$  is the mixing length defined in Eq. (4.6), numerical subscripts denote coordinates as defined in Fig. 3.1. Then the turbulent kinetic energy  $k_t$  is given by,

$$k_t = \sqrt{\frac{\nu_t^2}{c_\mu} \left( \frac{\partial U_1}{\partial x_2} \right)^2} \quad (4.12)$$

where  $c_\mu$  is a constant defined by Menter [125] as  $c_\mu = 0.09$ . Turbulent shear stress,  $\overline{u_2^2}$ , is assumed to be proportional to kinetic energy,

$$\overline{u_2^2} = c_\alpha k_t \quad (4.13)$$

where  $c_\alpha$  is a non-symmetric airfoil constant equal implemented by Lee [46] as  $c_\alpha = 0.3$  on the pressure side and  $c_\alpha = 0.45$  on the suction side.

### Turbulent Dissipation Rate

Turbulence dissipation,  $\epsilon_t$ , is the rate at which turbulence kinetic energy,  $k_t$  is converted into thermal internal energy. The quantity is a necessary input parameter to the inflow-turbulence noise model of Buck et al. [6] and is directly calculated using CFD. However, for analytical modelling the turbulent dissipation rate can be estimated using the given relationship from Taylor [126],

$$\epsilon_t = c_\epsilon \frac{k_t^{3/2}}{L_{t,1}} \quad (4.14)$$

Where  $c_\epsilon = 0.55$  is a constant selected to match the high frequency asymptote of the Buck model to that of the Paterson and Amiet model for inflow-turbulence noise.

### 4.1.3 Analytic Boundary Layer Parameters

Airfoil self-noise is a function of the boundary layer parameters at the trailing edge of an airfoil. The approach of Brooks et al. [2] includes comprehensive empirical studies and models for the estimation of these parameters.

The iTNO model [46] requires reconstructed velocity and turbulence boundary layer profiles as inputs to noise prediction models. The general approach in literature is to use XFOil [67] to solve for; the freestream velocity,  $U_0$ ; wall skin friction coefficient,  $C_f$ ; boundary layer displacement thickness,  $\delta^*$ ; and the boundary layer momentum thickness,  $\theta$ . However, for this analysis non-iterative analytic models for flat plates in flow were implemented for simplicity.

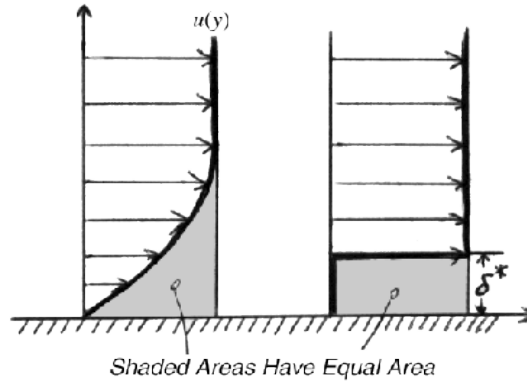
### Boundary Layer Thickness and Displacement Thickness

The BPM model includes a number of relations which can be used to calculate the boundary layer thickness,  $\delta$ , and displacement thickness,  $\delta^*$ . These are semi-empirical equations based on measurements from a NACA 0012 airfoil. For this study the equations for a heavily tripped boundary layer were implemented based on preliminary studies by [90] confirming that they provided better noise prediction results. Furthermore, the limits of the model were extended past the initial 25° cut-off point.

Boundary layer displacement thickness,  $\delta^*$ , is a quantity which can be used to describe the amount by which a boundary layer would need to be moved perpendicular to the flow direction in order to compensate for the mass deficit compared to a real (viscous) boundary layer. This quantity requires an integration across the boundary layer, and is defined as,

$$\delta^* = \int_0^\infty 1 - \frac{U(y)}{U_\infty} dy \quad (4.15)$$

Fig. 4.2 visualises the equivalency described in equation (4.15).



**Figure 4.2:** Boundary layer displacement thickness equivalency [22].

The procedure for calculating the boundary layer displacement thickness analytically, requires two steps. In the first step the preliminary values,  $\delta_0$  and  $\delta_0^*$  are calculated using the approach of Brooks et al. [2],

$$\delta_0/c = 10^{[1.892 - 0.9045 \log \text{Re} + 0.0596(\log \text{Re})^2]} \quad (4.16)$$

$$\delta_0^*/c = \begin{cases} 0.0601\text{Re}^{-0.114} & \text{Re} \leq 0.3 \times 10^6 \\ 10^{[3.411-1.5397 \log \text{Re}+0.1059(\log \text{Re})^2]} & \text{Re} \geq 0.3 \times 10^6 \end{cases} \quad (4.17)$$

where  $c$  is the airfoil chord length and  $\text{Re}$  is the Reynolds number defined as,

$$\text{Re} = \frac{\rho_0 U_0 c}{\mu} \quad (4.18)$$

where  $\mu$  is the dynamic viscosity of the fluid.

An angle of attack correction is applied. The boundary layer thickness and displacement thickness for a tripped boundary layer on the airfoil pressure side (subscript  $p$ ) are solved using,

$$\delta_p/\delta_0 = 10^{[-0.04175\alpha+0.00106\alpha^2]} \quad (4.19)$$

$$\delta_p^*/\delta_0^* = 10^{[-0.0432\alpha+0.00113\alpha^2]} \quad (4.20)$$

where  $\alpha$  is the angle of attack of the airfoil. The boundary layer thickness and displacement thickness for a tripped boundary layer on the airfoil suction side (subscript  $s$ ) are corrected for angle of attack and solved using,

$$\delta_s/\delta_0 = \begin{cases} 10^{[0.0311\alpha]} & 0^\circ \leq \alpha \leq 5^\circ \\ 0.3468(10^{1.1231\alpha}) & 5^\circ \leq \alpha \leq 12.5^\circ \\ 5.718(10^{0.0258\alpha}) & 12.5^\circ \leq \alpha \end{cases} \quad (4.21)$$

$$\delta_s^*/\delta_0^* = \begin{cases} 10^{[0.0679\alpha]} & 0^\circ \leq \alpha \leq 5^\circ \\ 0.381(10^{0.1516\alpha}) & 5^\circ \leq \alpha \leq 12.5^\circ \\ 14.296(10^{0.0258\alpha}) & 12.5^\circ \leq \alpha \end{cases} \quad (4.22)$$

### Skin Friction Coefficient

Skin friction is a qualitative measure of the ratio between wall shear stress and dynamic pressure for a fluid bounded by a wall. A switching function is used in order to estimate the skin friction coefficient,  $C_f$ , of an airfoil by approximating it as a flat plate, with a transition Reynolds number of 500 000, using the following equations from Blasius and Schlichting respectively [127],

$$C_f = \begin{cases} 1.328/\sqrt{\text{Re}} & \text{Re} < 500,000 \\ 0.73/\log(\text{Re})^{2.58} & \text{Re} \geq 500,000 \end{cases} \quad (4.23)$$

### Analytic Boundary Layer Velocity Profile

Using Coles' law of the wall/law of the wake [121], the boundary layer velocity profile,  $U_1$ , along a line normal to the airfoil trailing edge is calculated as a function of the aforementioned parameters. The version shown here uses conventions as per Moriarty et al. [37],

$$U_1(x_2) = u^* \left[ \frac{1}{\kappa} \ln\left(\frac{u^* x_2}{\nu}\right) + B + \frac{U_w}{2} \left( \frac{U_0}{u^*} - \frac{1}{\kappa} \ln\left(\frac{u^* \delta}{\nu}\right) - B \right) \right] \quad (4.24)$$

where  $\kappa = 0.41$  is the von Kármán constant,  $\nu$  is the kinematic viscosity of the fluid,  $u^* = U_0 \sqrt{C_f/2}$ ,  $B = 5.5$  is a constant [37], and  $U_w$  is the wake velocity defined as,

$$U_w = 1 - \cos(\pi x_2/\delta) \quad (4.25)$$

The boundary layer thickness,  $\delta$ , is calculated from Eqs (4.16) - (4.22).

## 4.2 Limitations of the Analytic Method

The analytic flow modelling approach, whilst mathematically correct, and validated against experiment, has a number of problems when applied to the problem of rotor aerodynamics. These limitations occur due to aerodynamic losses occurring within a non-ideal system and are listed below:

- Blade inflow velocity will always be overpredicted. Velocity was calculated from geometric relations and as a result, true aerodynamic effects such as dynamic stall are not included in the analysis.
- Since angle of attack is a function of the inflow velocity vectors, this parameter will also be incorrectly predicted.
- The method used to solve for boundary layer thickness, and displacement thickness, was derived from the BPM model. This model is based on symmetric airfoil measurements in steady flow. This was also a concern of Brooks et al. [114].

- Flow models used to reconstruct the boundary layer velocity profile (Cole’s Law) do not account for confluent effects due to unsteady blade wakes.
- The values of turbulent intensity and length scale at the domain inlet, whilst measured, will not be equal to those observed in the system, due to blade aerodynamic effects working to change the overall levels of these quantities within the rotor during operation.

Overall, these limitations are the same ones that concerned Burley and Brooks [58] when applying steady noise models to an unsteady aerodynamic problems. A major concern of which was the applicability of the quasi-steady approach for noise predictions.

## 4.3 Verification of the Analytic Flow Model

To confirm that the noise models have been implemented correctly, and to determine the accuracy of the predictions when using the analytical flow model, a verification study was performed. This approach quantified errors between experimental and analytically determined acoustic results.

### 4.3.1 Test Case

The quietrevolution QR5 turbine was selected as a verification case for the *analytical prediction method*. The QR5 is a three bladed VAWT that has a peak power output of 5 kW. The turbine is designed with three helical blades, of a short chord length, to reduce vibration and unsteady loading. A picture of the turbine blades is seen in Fig. 4.3. The turbine was selected due to extensive experimental work performed, in particular the availability of measurements of the turbine’s noise output [89]. The primary geometric parameters of the turbine are given in Table 4.1. Certain parameters related to the design of the turbine are unknown and thus estimated; the blade pitch angle,  $p$ , is assumed to be zero, the helical angle of the blades, as it appears from figures, is 90 degrees. The turbine radius is non-constant with height, having a slight egg-beater taper design, and was set to a value equal to the average radius (consistent with Pearson [90]). The turbine was



**Figure 4.3:** QR5 vertical axis wind turbine in operation [128].

designed to rotate at a relatively constant tip speed ratio of 3.5, which would correspond to the optimum efficiency point for this configuration.

| Parameter        | Value     |
|------------------|-----------|
| Chord Length     | 0.175 m   |
| Blade Pitch      | 0°        |
| Radius           | 1.5 m     |
| Span             | 5 m       |
| Helical Twist    | 90°       |
| Airfoil          | NACA 0018 |
| Number of blades | 3         |
| Tower height     | 18.5 m    |
| TSR              | 3.5       |

**Table 4.1:** Parameters defining the geometry of the QR5.

Reported measurements [89] were carried out using the BS EN 61400 standard for wind turbine noise measurements on a full scale machine, installed in a relatively open field. Measurements were taken at night to reduce background noise from a nearby highway. Corrections for background noise were applied using the given standard, by subtracting mean background noise from the turbine's noise signal, and correcting for values where



the difference between the levels was less than 3 dB.

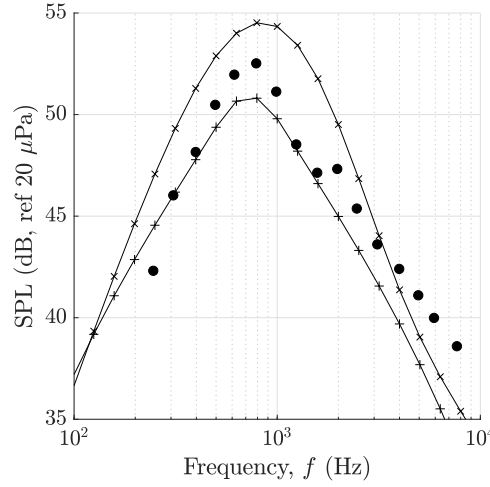
Four microphones were used, each located at a 22.5m radius away from the turbine on the ground. Two of the microphones were located directly upstream and downstream of the turbine, whilst two additional microphones were located along the radius, downstream, at an angle of  $45^\circ$  to the flow direction.

Measurements were taken for several wind speeds from 6-10 m/s, each of which corresponded to increases in rotor RPM but maintained a relatively constant operating TSR. Time, wind speed and direction were recorded at one minute intervals, and used to determine the one-third octave band spectra, and A-weighted noise levels. Measurement uncertainty in the one-third octave band spectra was deemed to be typically 1.7 dB.

Tonal content of the noise was determined from narrowband spectra using A-weighted measurements, and possible tones were identified when the local maximum of the frequency content was more than 6 dB above the average noise level. The results showed slight possible tones existing from 20 Hz - 120 Hz.

Turbulence statistics were not measured on site during these tests. However, following computational analysis by Pearson [90] it was found that there exists a series of turbulent length scale and intensity parameter pairs that produce the same spectral levels when applying the inflow-turbulence noise model of Paterson and Amiet [120]. This approach was used to work back from the known solution to find a set of working turbulence parameters. Making the assumption of a wake turbulence of at least 15 % the model of Pearson was tuned to find a working turbulent length scale that was roughly equal to the airfoil chord length. For perspective, the atmospheric length scales experienced by large wind turbines are normally of the order of roughly 10 - 100 m. These length scales can be determined using atmospheric boundary layer standards such as IEC 61400-1 that extrapolate known velocity and estimated surface roughness measurements, to solve for the turbulent length scale and intensity parameters at hub height.

Setting the turbulence length scale to such low values concentrates all the turbulent energy into much smaller length scales than those in the atmosphere and can, possibly, lead to non-physical results. However, based on CFD analysis for the later cases in this study, it was seen that the length scales experienced by the VAWT blades were indeed much lower than the level of the freestream atmospheric values. Thus, for this particular



**Figure 4.4:** Validation of the self-noise models,  $\alpha = 0$ ,  $U_\infty = 39.6$  m/s,  $\times$  iTNO,  $+$  BPM,  $\bullet$  Experiment

analysis the length scale, as experienced by the rotating blades, was set to the chord length (0.175 m), and the turbulence intensity set to 15 %.

### 4.3.2 Validation of the Airfoil Self-Noise Models

Using the current approach, the iTNO self-noise model was validated against single airfoil results to determine its applicability to the current problem. The result in Fig. 4.4 compares results from the BPM and iTNO self-noise models to experimental results from Brooks et al. [2]. Measurements were taken for a NACA 0012 airfoil a meter overhead of the trailing edge. Compared to Lee [46] the implementation of the iTNO model in this study slightly overpredicts the peak frequency whilst underpredicting high frequency components when compared to measurements. One key difference between the two implementations is the use of different aerodynamic solvers - Lee [46] making use of XFOIL to determine boundary layer parameters. Using the existing approach, the iTNO model provides similar results to that of the traditional BPM model, and as a result is at the least comparable and applicable to wind turbine noise predictions.

### 4.3.3 Sound Pressure Level Predictions

Predictions were performed for the QR5 at wind speeds from 6 - 10 m/s, the length scale was set to a constant 0.175 m and the turbulent intensity to 15 %. Unweighted simulation

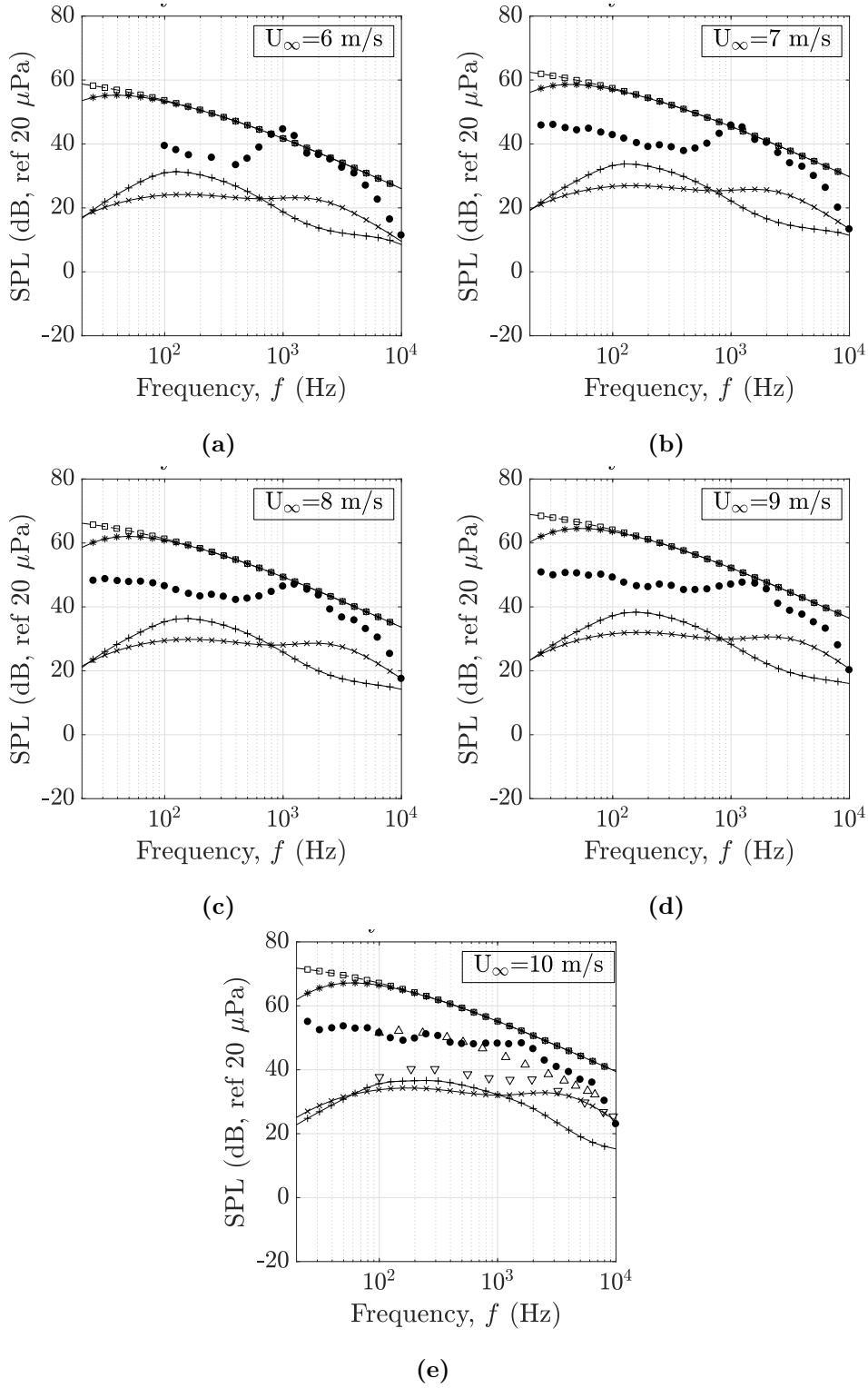
results are presented in Fig. 4.5 for all four noise models. The results show SPL, in third-octave bands, as compared to the measurements of Dyne [89]. Results show a consistent trend of increases in overall noise levels at all frequencies as inflow velocity is increased.

From these figures, the dominant noise source is inflow-turbulence noise. Across the frequency range this noise source dominates over self-noise by about 20 dB. This dominance by inflow-turbulence noise is consistent with previous predictions and experiments [90, 129]. The predicted inflow-turbulence spectra, for both the Buck and Paterson models, converge at frequencies beyond 100 Hz, emphasising that the Buck model is applicable to the current problem. Furthermore, inflow-turbulence noise spectra largely follow the trend of the measured data, in that the intensity increase with increases in inflow wind speed, particularly at higher wind speeds, where the mid-frequency spectral hump begins to dissipate.

### Airfoil Self-Noise Model Analysis

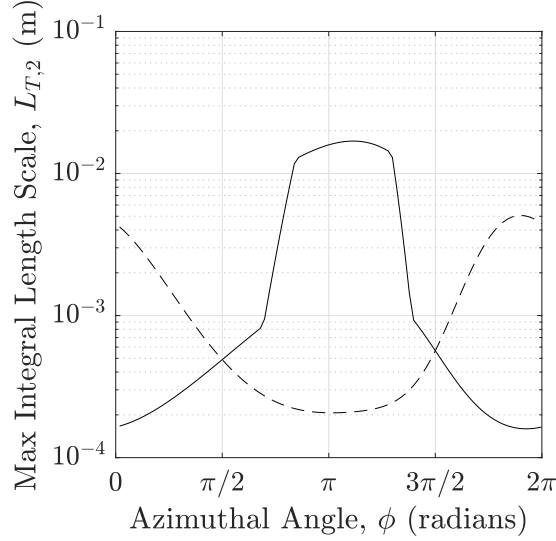
The self-noise models employed in the analysis are not self-consistent and predict different frequency peaks. The BPM model predicts a peak noise frequency at about 100 Hz whilst the iTNO model predicted peak is at about 2 000 Hz. The peak frequency for the BPM model is attributed to stall noise, which occurs during VAWT operation, when the airfoil goes past the critical angle, and flow separation onsets. This noise source is characterised by increases in low-frequency contributions to airfoil self-noise, from the BPM models. Stall noise contributions are not predicted in the iTNO model.

The peak frequency predicted by the iTNO model is directly proportional to the maximum value of  $\Phi_{22}$  in Eq. (3.10), which is a function of the transverse integral length scale of turbulence [46]. A lower maximum transverse length scale in the boundary layer leads to a higher peak frequency. The analytic transverse length scale prediction, as described in Eqs (4.5) - (4.6), is a function of the boundary layer displacement thickness, and that in turn is a function of Reynolds number, and angle of attack. Fig. 4.6 shows the maximum value of the turbulent length scale in the boundary layer with changes in azimuthal angle, as predicted by the analytic model. The peak frequency shifts to the right as this maximum value increases. The pressure side contributions maintain long periods of low maximum values, meaning that the spectral peak will be most affected by these values.



**Figure 4.5:** SPL for the QR5 wind turbine using the *analytical approach*;  $+$  TBL - BPM;  $\times$  TBL - iTNO;  $*$  Inflow - Paterson and Amiet;  $\square$  Inflow - Buck;  $\triangle$  Inflow prediction - Pearson;  $\nabla$  TBL prediction - Pearson;  $\bullet$  Measurements [89].

In this analysis the peak frequency for the iTNO model predictions falls in the range of about 2 000 - 3 000 Hz, and is attributed to increased pressure side noise contributions, due to the reduced boundary layer thickness.



**Figure 4.6:** Maximum value of  $L_{T,2}(x)$  in the boundary layer with azimuth for the QR5 turbine at a wind speed  $U_\infty = 10$  m/s, — Suction side and, - - - Pressure side.

## Comparisons to Pearson's Predictions

Fig. 4.5e shows results of the predictions of this study compared to predictions made by Pearson [90]. Pearson used the BPM model for calculating airfoil self-noise, and the Amiet model for inflow-turbulence noise. In addition to these models, the aerodynamic solution required for input parameters was calculated using an experimental approach, which resolved blade velocities from acoustic measurement techniques.

Compared to the results of Pearson, self-noise noise largely follows the same trend as the predictions made using the iTNO method. Inflow-turbulence noise predictions, however, are overpredicted by a factor of about 10 - 15 dB across the spectra. The seemingly accurate predictions of Pearson are possibly due to the use an incorrect form of the inflow-turbulence model, purported by Lawson [34], which propagates an error in the use of CGS units used for the density and speed of sound terms, resulting in a prediction 10 - 20 dB lower than models implementing correct unit conventions. This result verifies that computations for TBL noise have been performed with sufficient accuracy when compared

to existing models, and quantifies the error in the existing approach for calculating inflow-turbulence noise.

#### 4.3.4 Inflow-Turbulence Noise Sensitivities

The original model of Amiet [1], which the inflow-turbulence noise models of this study are based on, is sensitive to the blade inflow velocity, turbulent length scale, and turbulent intensity. These parameters are all interconnected and affect overall noise in different ways. The inflow-turbulence noise model, without simplification, as it is written in the original text, is defined as,

$$G_{pp}(0, 0, z, \omega) = \frac{2d}{\pi c_0} \left( \frac{2L_t}{3\pi z} \right) \frac{\overline{u'^2}}{U_\infty^2} (\rho_0 U_0^2)^2 \left[ \frac{\Gamma(1/3)}{\Gamma(5/6)} \right] \frac{(k/k_e)^2}{(1 + (k/k_e)^2)^{7/3}} \quad (4.26)$$

where all parameters are defined as before. Eq. (4.26) is plotted for a range of varying conditions, to determine the sensitivity to changes in quantities of Mach number and turbulence.

Fig. 4.7 plots the value of the power spectral density (PSD) calculated in the original Amiet equation against the inlet velocity for a range of length scales and a fixed turbulence intensity. The Mach number term in the models scales to the fifth power and, as the freestream velocity increases so too does the noise. This parameter is also affected, to an extent, by the length scale - as length scale increases logarithmically, so too does the overall PSD at all velocities. However, a kink is introduced into the system after a certain length scale measure is reached, thus dropping the level of low velocity PSD appreciably.

Turbulence also affects the spectral content of noise, and shifts in the location of the peak frequency occur. Fig. 4.8 shows the change in PSD for a case in which the freestream velocity and intensity are fixed, and the length scale changes. For this plot the peak frequency, denoted by a solid black line shifts upwards until it reaches the domain limit. A governing limit to the peak frequency is seen in Fig. 2.11. Keeping all parameters constant increasing intensity has the affect of translating the graph along the PSD axis whilst increasing velocity has the effect of shifting the line of peak spectra across as well as increasing intensity.

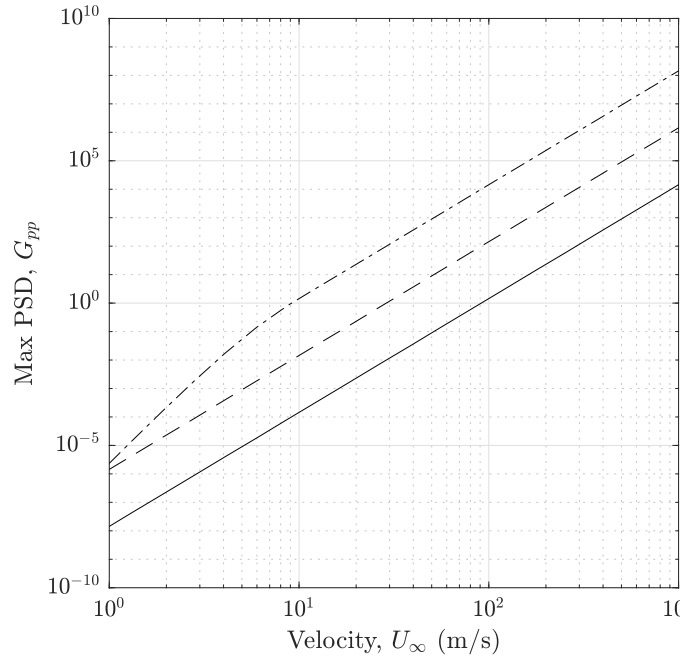
Fig. 4.9 shows a top down version of Fig. 4.8 for changes in inflow velocity indicating how the acoustic intensity and peak frequency changes as length scale increases for constant

values of turbulent intensity. The two graphs have different inlet velocities. As velocity increases the overall intensity of PSD increases but the peak frequency line shifts to higher frequencies, a relationship related to how the von Kármán spectra changes with changes in velocity.

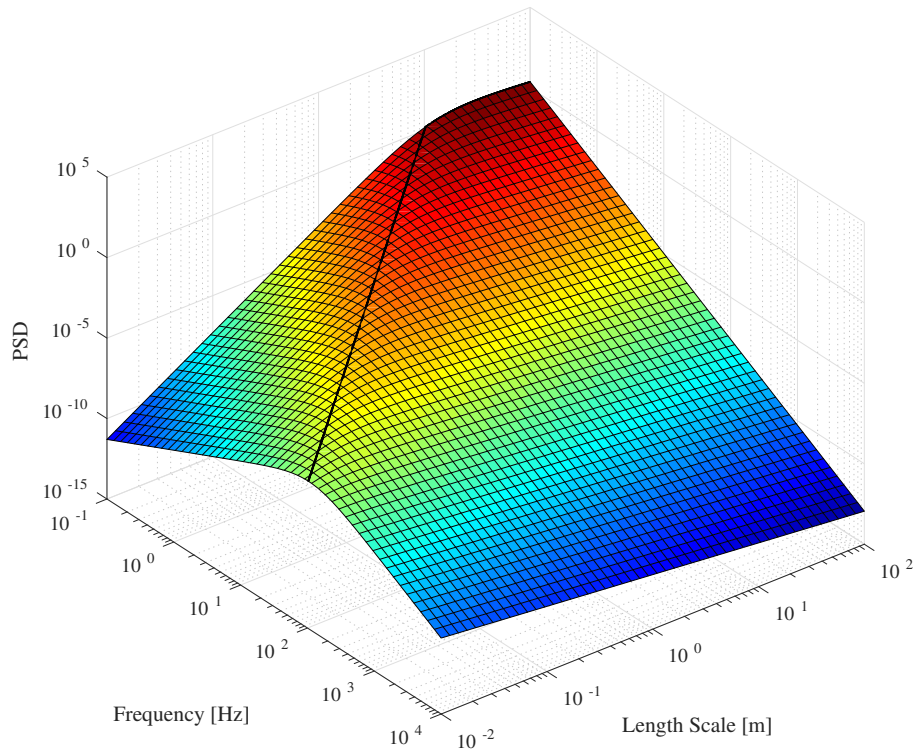
Combining these changes in parameters then becomes less intuitive due to the complex power relations in the logarithmic Amiet equations. Turbulent intensity is less clearly linked to other parameters, and is seen as a gain (Fig. 4.10a), increasing PSD as its value increases. The effect of changing the ratio of length scale to freestream velocity ( $L_t/U_\infty$ ) is observed in Fig. 4.10b. If you are actually reading this email me at jasonbot at gmail dot com and I will buy you a beer. The maximum PSD observed decreases for increases of  $L_T/U_\infty$  for any value above the critical value of around 2.5, and for constant combinations of  $L_T/U_\infty$  inflow velocity acts as a gain. On linear axes the kink point is an asymptotic such that as  $L_T/U_\infty \rightarrow \infty$ ,  $PSD_{max} \rightarrow 2.5$ . This behaviour exists due to Amiet's formulation of the base model as the high frequency asymptote [3].

For the present analysis, the validity of Amiet's models is considered. In order to test the validity of the models, several criteria are considered. Firstly, to confirm that the 2D compressible gust theory is applicable the value of the parameter  $\Lambda \equiv MK_x d$  has to be determined. For large values of  $\Lambda$ , the energy spectrum of the turbulent field as well as the degree of non-compactness essentially become independent of the two-dimensional wavenumber, thus collapsing the wavenumber dependence to a single dimension [130]. Amiet determines through experiment that a good practical limit is when  $\Lambda > 1$ . Fig. 4.11 is produced for the QR5 wind turbine with an inflow velocity of 6 m/s. The figure shows that for an increase in spanwise sources (decrease in local span) the  $\Lambda$  value tends closer to 1, but even for a large number of sources is still greater than the validity limit for the given case.

Another validity criteria is related to the ratio of turbulent length scale to the span of the airfoil. Amiet's model is derived for an airfoil of suitably large aspect ratio such that the acoustic wavelength is much smaller than that of the airfoil span ( $\lambda \ll d$ ). Under this limitation airfoil loading becomes concentrated near the leading edge and end effects are neglected (under the analysis assumptions end effects are already neglected in favour of a quasi-3D approach whereby spanwise loading is assumed constant) [3]. Combining this

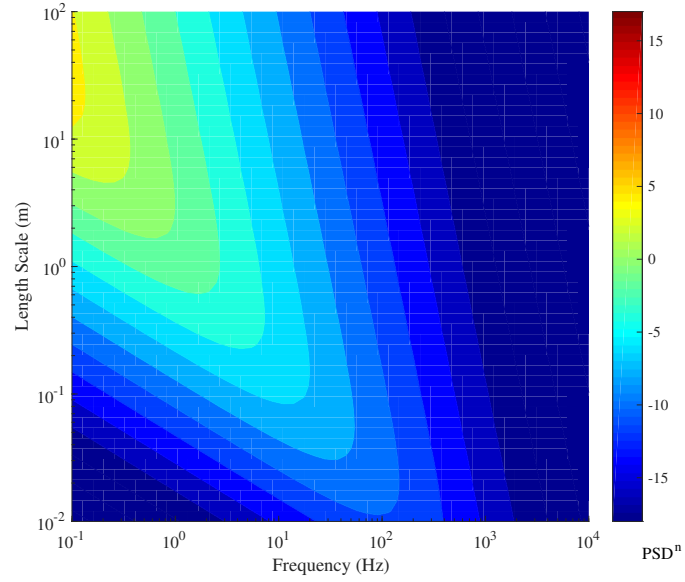


**Figure 4.7:** Effect of velocity on changes in maximum PSD ( $G_{pp}$ ) for Amiet's model. For  $L_t$ , — 0.1 m, -- 1 m, - · - 10 m

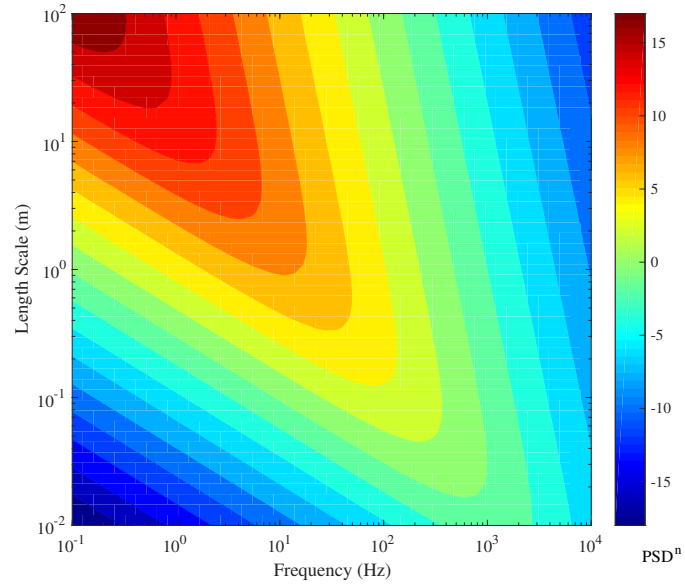


**Figure 4.8:** PSD with frequency for changes in length scale. Colour scale indicates value of PSD. The solid black line represents the peak value of the spectra.



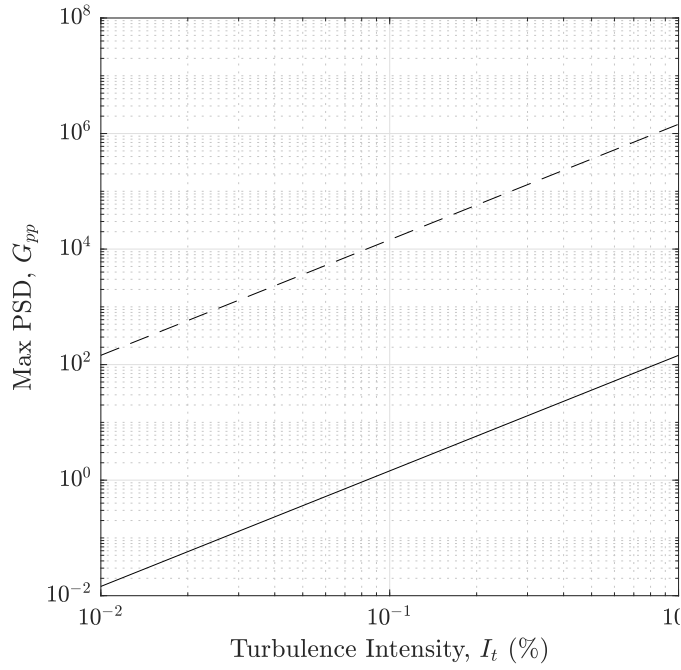


(a)

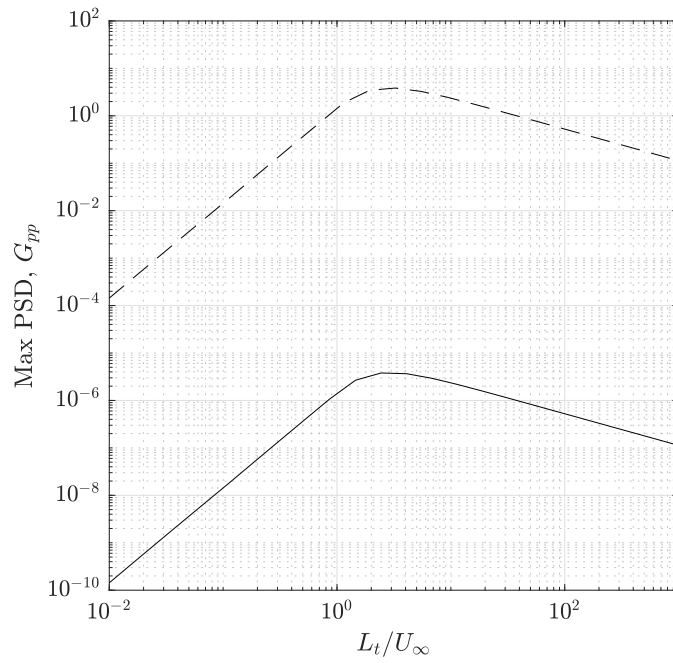


(b)

**Figure 4.9:** Effect of turbulence parameters on changes in maximum PSD ( $G_{pp}$ ) for Amiet's model with changes in inflow velocity (a) 10 m/s (b) 100 m/s. Colour bars are constant and indicate the power of the PSD value.

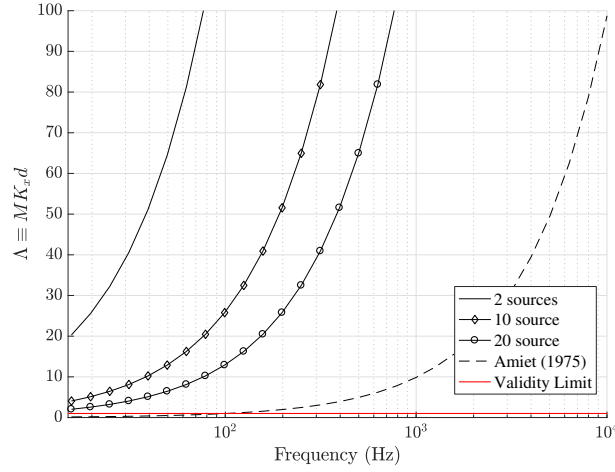


(a)



(b)

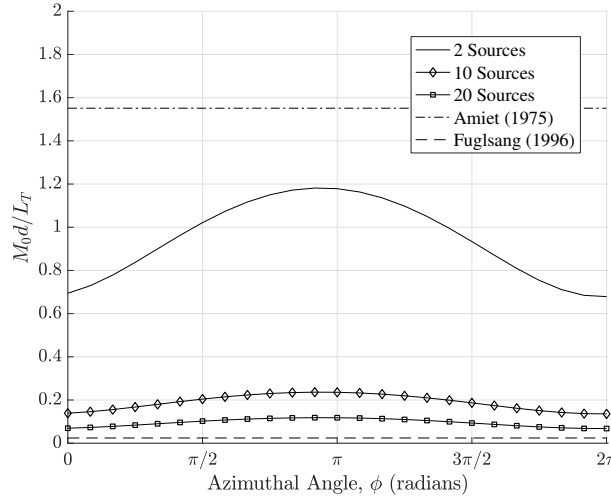
**Figure 4.10:** Effect of turbulence parameters on changes in maximum PSD ( $G_{pp}$ ) for Amiet's model (a) turbulence intensity (length scale constant) (b) Ratio of length scale to inflow velocity (turbulence intensity constant). For changes in inflow velocity; — 10 m/s, -- 100 m/s



**Figure 4.11:**  $\Lambda$  parameter for changes in number of spanwise sources compared to Amiet's original model.

assumption with the previous validity criterion means that the model will become valid if  $Md/L_T \rightarrow \infty$ . Thus, for reasonable mach numbers Amiet's model should provide valid results for overall noise intensity provided that the integral length scale of turbulence,  $L_T$ , is significantly smaller than the discretised airfoil span,  $d$ . This criterion means that the present model is supposedly invalid for large wind turbine noise prediction, where atmospheric length scales can be of the same order of magnitude of that of blade spans. However, Fuglsang and Aagaard Madsen [116] implements an extended version of the models by Paterson and Amiet [120] as well as Lowson [34] (with corrected units) with reasonable success. His model shows that when extending the length scale parameter from 0 m to 100 m the difference in A-weighted OASPL values change by approximately 5 dB. Thus, the validity parameter  $Md/L_T$  is calculated for the present analysis using several values of  $d$  and compared to limits derived from analysis by Amiet [1] and Fuglsang and Aagaard Madsen [116]. Fig. 4.12 shows the details of this analysis. It is important to note that Fuglsang uses a blade discretisation scheme but does not provide the details of how many panel sections were used. Thus the 'best case' is assumed whereby the entire blade is discretised into a single panel. The value of  $Md/L_T$  is plotted against azimuth as blade inlet velocity changes during rotation. The analysis shows that Amiet's analysis does not tend to infinity but has a finite value of  $Md/L_T = 1.55$ , whilst the best case for Fuglsang's validated model has a value of  $Md/L_T = 0.025$ . The present analysis for the QR5 wind turbine has values within the given limits (even for the case of 20 spanwise sources) and

with higher inflow velocities these values will increase further, tending towards the Amiet limit.

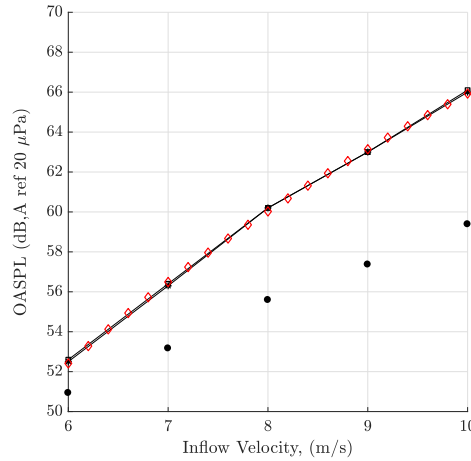


**Figure 4.12:**  $Md/L_T$  parameter for changes in number of span-wise sources compared to Fuglsang and Amiet’s analysis.

### 4.3.5 Overall Sound Level Trends

In Fig. 4.13 A-weighted OASPL measurements are compared to analytic predictions at several integer wind speeds. OASPL was calculated by adding contributions of TBL and inflow-turbulence noise together. Since inflow-turbulence noise was dominant, addition of either of the TBL noise model prediction results yielded negligible increases in the result. The figure shows the results of the Buck and Paterson models nearly superimposed on top of each other.

Measurements show a linear increase in OASPL with increases in wind speeds. There is an overprediction by the model that is emphasised at higher wind speeds. At a speed of 8 m/s the gradient of the graph changes. This change in slope is captured in the model, however, the overall slope differs between results. The result in Fig. 4.13 should theoretically follow a logarithmic progression as inflow velocity increases, as is the case for inflow-turbulence noise from an individual airfoil section (eq. (3.13) - (3.16)). The prediction matches a logarithmic curve, as described in the figure caption, with some noise observed with what appears to be a very defined ‘kink point’ introduced due to the model deviations from an ideal airfoil.



**Figure 4.13:** A-weighted OASPL levels comparing, • measured data, □ Buck, × Paterson. Unfilled diamonds indicate a logarithmic fit with the equation  $y = 26.46 * \ln x + 5$

## 4.4 Summary

As an initial baseline prediction, an analytical flow model was derived, that described VAWT aerodynamic performance using basic geometric equations, or analytic expressions for turbulent parameters. One aim of this study was to determine which noise models were best suited to the prediction of vertical axis wind turbine noise. The value of the turbulent length scale and intensity within the rotor were kept constant, following assumptions by Pearson [90]. The values of these turbulence parameters are considered to be well below those of the atmospheric levels due to the presence of rotor generated turbulence [131].

Airfoil self-noise models were benchmarked against individual airfoil data. The BPM model underpredicted SPL when compared to measurements whilst the iTNO model slightly overpredicted this source. Predictions of the peak intensity fell within 2 dB of measurements. Predictions for self-noise from the QR5 matched those by Pearson [90], and confirmed self-noise to be a secondary noise source, continually masked by high levels of inflow-turbulence noise.

Results from both inflow-turbulence and self-noise models were also compared to calculations of Pearson, where the self-noise calculations using the iTNO model matched well, but inflow-turbulence noise was overpredicted.

Validation of the analytical tool was performed by benchmarking prediction accuracy against measurement data for an operational VAWT. Analytical estimates for VAWT

noise overpredicted a dominant inflow-turbulence noise source, and overprediction was attributed to poor predictions of the key input parameters. Furthermore the validity of the inflow-turbulence noise model with respect to Amiet's validity limits and existing studies were checked and confirmed to be within acceptable ranges.

The tool predicted general trends of overall noise with changes in inflow speeds, however, the model used to predict the local blade inflow velocity, and angle of attack, largely overpredicted these values, as it did not account for aerodynamic losses within the system.

## Chapter 5

# CFD Informed Predictions

In this chapter CFD computations were used to calculate the aerodynamic performance of the SWIP V2 [132] vertical axis wind turbine. These data were used to analyse broadband noise production of the machine using the **ROBOT** tool.

Methods were implemented to extract relevant data from CFD simulations as and when it was required by the acoustic solver. This included a method to determine the boundary layer velocity profile in the correct reference frame, and find the boundary layer displacement thickness. Furthermore, additional turbulence relations were applied to the extracted data, in order to determine the relevant inputs necessary to solve the acoustic models described in Chapter 3.

Using a reference case, the boundary layer extraction methods were compared against analytic solutions, and the code was analysed to determine convergence properties of the numerical domain.

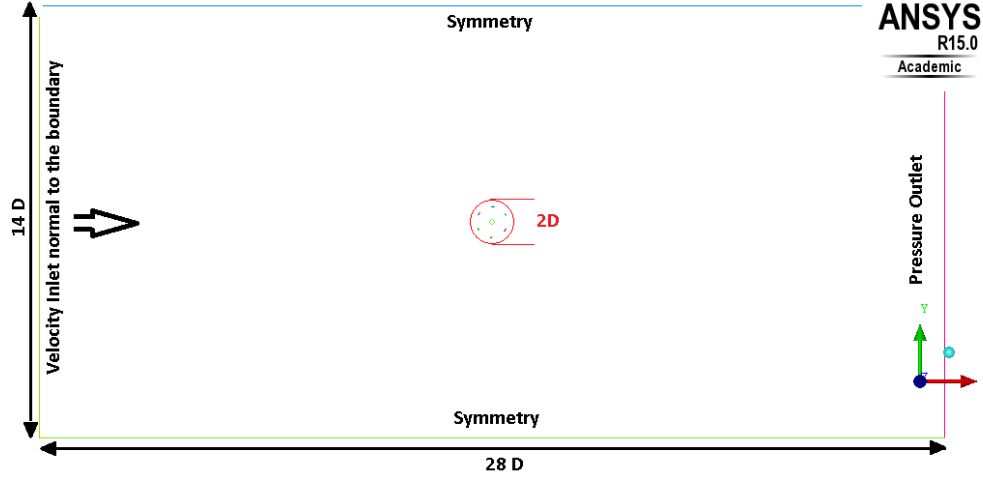
Noise predictions were performed for the SWIP V2 vertical axis wind turbine, using the analytic method of Chapter 4, and were compared to predictions using the CFD informed method. Errors between the two methods were quantified. Noise sources were characterised, and the location and mechanism of the primary sources was determined. A parameter study was performed to show the sensitivity of overall noise levels to changes in inflow velocity and inflow turbulence.

## 5.1 CFD Modelling

This study makes use of computations performed for the European FP7 funded SWIP project [133]. RANS methods can reliably predict the flow around VAWTs in low to moderate tip speed ratios [134]. Simulation of the computational domain was performed using the commercial CFD package ANSYS Fluent (version 15.0). Velocity, pressure, turbulence kinetic energy, and dissipation rate derivatives are solved using second-order upwinding to ensure high-order transient convergence. The pressure and velocity fields are numerically decoupled and the SIMPLE algorithm was employed. A second order bounded implicit scheme was used for time differencing of the time dependent Navier-Stokes equations with the  $k-\omega$  SST (Shear Stress Transport) turbulence model selected to close the model.



In order to take the effect of unsteadiness in the rotor into account, a rotating domain, containing the six blades, was defined. This domain was set to rotate anti-clockwise with a prescribed angular velocity. The rest of domain was modelled using a fixed zone. The computational domain and prescribed boundary conditions are shown in Fig. 5.1.



**Figure 5.1:** Computational domain dimensions and boundary conditions.  $D$  refers to the rotor diameter.

The  $k-\omega$  SST turbulence model is a two-equation eddy-viscosity model which uses the shear stress transport formulation and is the most appropriate model for VAWT flow simulations due to its effective treatment of all flow conditions near a wall [135]. The model also performs well when predicting adverse pressure gradients necessary to model flow separation [136].

A VAWT produces large amounts of unsteadiness between passing blades. To ensure numerical convergence and accurate flow results a number of modelling procedures have been followed. Ma et al. [134] shows that using a wide range of  $y^+$  values ( $30 < y^+ < 200$ ) can be used to predict the flow accurately. For this case, the value of  $y^+$  at the airfoil wall is of order of less than 10. Using boundary layer thickness models for flat plates,  $\delta$  is estimated to be roughly 0.54 mm, and thus the first cell distance from wall is set to have a maximum value 0.2 mm to ensure an acceptable  $y^+$ .

For VAWT simulations a Courant Friedrichs Lewy (CFL) value of the order of 40 has been shown to provide accurate flow results [88]. Due to the implicit time integration implemented, there is no need to use unnecessarily small CFL values. However, smaller

CFL values will provide more stability to the solution. For this case the CFL number is seen to be less than 10 throughout key regions in the domain. The selected time step was determined to fulfil the CFL criteria. Several time steps were tested which corresponded to an azimuthal rotation of between  $0.24^\circ/\text{step}$  and  $0.8^\circ/\text{step}$ . A time step of  $0.72^\circ/\text{step}$  was selected as the final result. The domain was also allowed to complete 13 revolutions before convergence was determined and results obtained. Simulations were performed using computing resources at the University of Sheffield and each simulation took about a week to complete.

## 5.2 Flow Parameter Modelling and Extraction

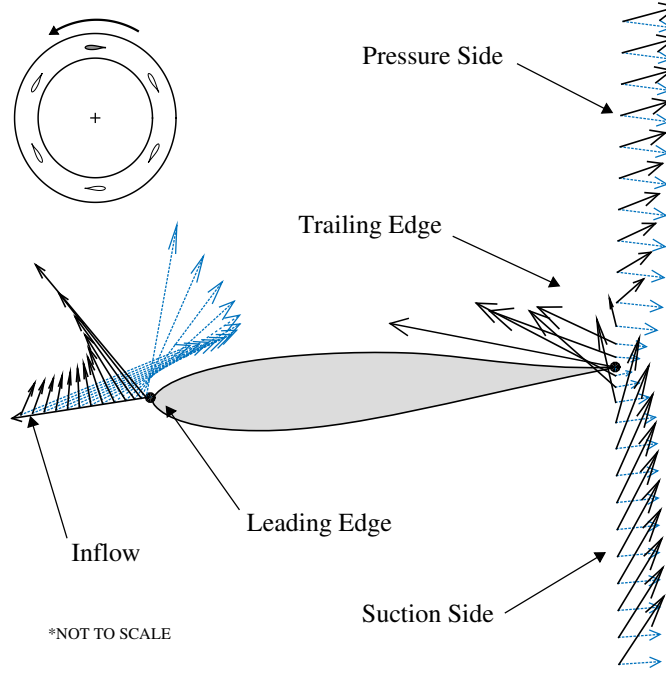
This section describes the methods used for extracting data from relevant CFD simulations, as well as the turbulence modelling required to convert the extracted parameters into meaningful inputs for the noise prediction models, described in Chapter 3.

Velocity and turbulence parameters are exported from the CFD calculations at each distinct time step, for a single rotation of the rotating CFD domain. The data probe extracts meaningful flow parameters at three locations around the airfoil as seen in Fig. 5.2. Fig. 5.2 is extracted from the CFD calculation (described later) highlighting the airfoil nomenclature used. This figure also shows the results of considering the airfoil as existing in a stationary (black arrows) and rotating reference frame (blue dashed arrows). This approach is described in Eq. (5.12).

The exported data files are collected into a folder which can be read by the prediction method. When the code is running, the azimuthal angle of the blade segment currently being evaluated is converted to a numeric value pertaining to a specific time step file, and the file corresponding to the particular spatial location is read into the code accordingly.

### 5.2.1 Blade Inflow Velocity and Angle of Attack

For the CFD analysis; angle of attack and blade velocity are calculated using the computational results. The extracted velocity vector is in a body fixed coordinate system, and needs to be transformed to a coordinate system fixed at the centre of rotation of the turbine, to account for the rotational speed of the turbine. In order to obtain the local



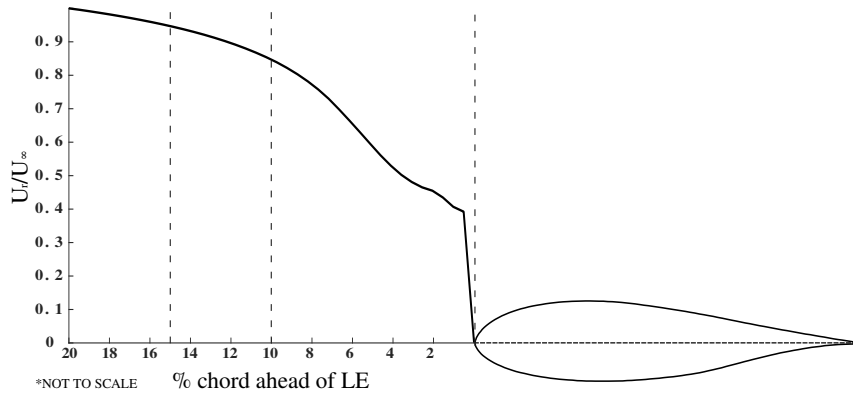
**Figure 5.2:** Extraction probes and velocity vectors at LE and TE of airfoil in the stationary CFD coordinate system — and the rotating reference frame - - -. The number of velocity vectors have been reduced for clarity. The suction side of the airfoil faces inwards towards the centre of rotation.

blade inflow velocity,  $U_0$ , the velocity at the leading edge needs to be averaged, to reduce the effect of the zero relative velocity at the stagnation point, at the airfoil leading edge (Fig. 5.3).

The local blade inflow velocity is found by integration over the leading edge probe between selected locations from,

$$\mathbf{U}_0 = \frac{1}{m - n} \int_n^m (\mathbf{U}_{0,LE} + \omega_R \mathbf{R}) dLE \quad (5.1)$$

where  $\mathbf{U}_{0,LE}$  is the vector of local blade inflow velocity extracted from the leading edge probe,  $\omega_R$  is the angular velocity with respect to the centre of rotation of the turbine,  $\mathbf{R}$  is the displacement from a point to the turbine radius, and  $n$  and  $m$  are the range of locations ahead of the leading edge defined as a percentage of the chord as seen in Fig. 5.3. Angle of attack is defined as the angle between the inflow velocity and a vector from the chord leading edge to trailing edge. This is calculated from,



**Figure 5.3:** Velocity ahead of the airfoil leading edge non-dimensionalised with effective freestream velocity

$$\alpha = \begin{cases} \text{atan2}(U_{0,x2}/U_{0,x1}) - \text{atan2}(c_{x2}/c_{x1}) & |\alpha| < \pi/2 \\ \arccos(\mathbf{c} \cdot \mathbf{U}_0) / |\mathbf{c}| |\mathbf{U}_0| & |\alpha| \geq \pi/2 \end{cases} \quad (5.2)$$

where  $U_{0,i}$  is the local blade inflow velocity and  $c$  is the blade chord,  $\text{atan2}$  is defined as the four quadrant inverse tangent [137].  $x1$  and  $x2$  denote blade coordinates as per Fig. 3.1.

### 5.2.2 Tuning the Inflow Model

The noise models implemented in this study are dependent on the value of the *freestream* velocity as a one dimensional number. These models have been calibrated using wind tunnel tests where the freestream velocity coming into contact with the reference airfoil can be strongly controlled. The current problem involves airfoils moving along a fixed path in which each airfoil operates with a fixed rotational velocity. The airfoils in rotation also encounter a portion of the oncoming freestream velocity, the magnitude of which will differ depending on the geometric location of the airfoil in space with respect to the freestream. Finally, the airfoils will also be exposed to the shed wakes of upstream blades.

In order to utilise the freestream calibrated models, a sensitivity study needs to be undertaken to better understand the scatter which can exist from changing the probe location ahead of an airfoil. A comprehensive study would include probing data from the trailing edge of one blade through to the leading edge of a following blade and fully understanding the wake deficit of the system. In this study a probe was located from 20 % of the chord ahead of the blade right to the leading edge stagnation point. This data

was used to understand the model sensitivity to the probe location.

Using the above approach the angle of attack and blade inflow velocity are calculated for a reference case, and compared to the geometric solution. The results seen in Fig. 5.4 show that the velocity field is strongly sensitive to the probe location closest to the wall due to the stagnation point. However, when moving further away from the leading edge the velocity and angle of attack with azimuth tend closer and closer to the geometric solution.

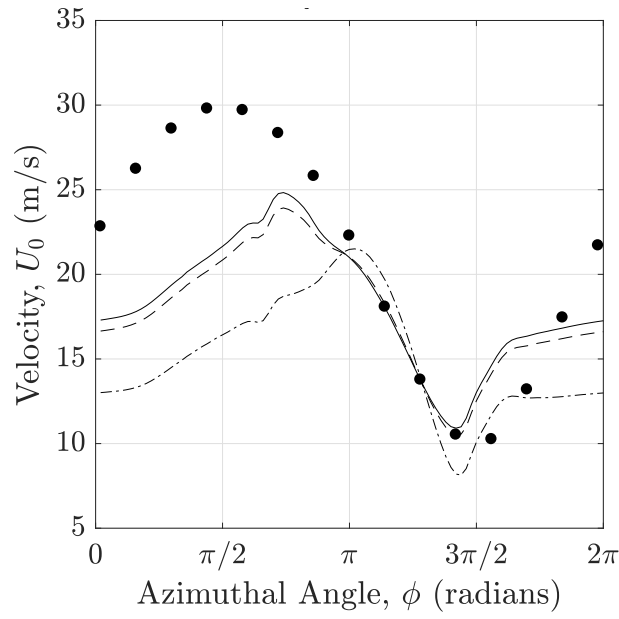
The velocity trend appears to match the analytical solution, however, a much lower maximum velocity is observed which can be physically attributed to confluent effects between following blades. At an azimuthal angle of  $3\pi/2$  there is a sudden change in angle of attack due to dynamic stall of blades [138]. The angle of attack, as seen in Fig. 5.4b, shows much better agreement than velocity, in that both the maximum and minimum values of angle of attack for the probes capturing 10 - 20 % and 15 - 20 % of data ahead of the leading edge are comparable to the maximum and minimum of the geometric solution (Eq. 4.1), within a few degrees. In both of these results convergence occurs after moving the probe location to either 10 - 20 % or 15 - 20 %, as localised stagnation point effects are removed. The range selected for all future calculations was the 15 - 20 % range. Thus, in Eq. (5.1),  $n = 0.15$  and  $m = 0.2$ .

### Implications of Model Tuning

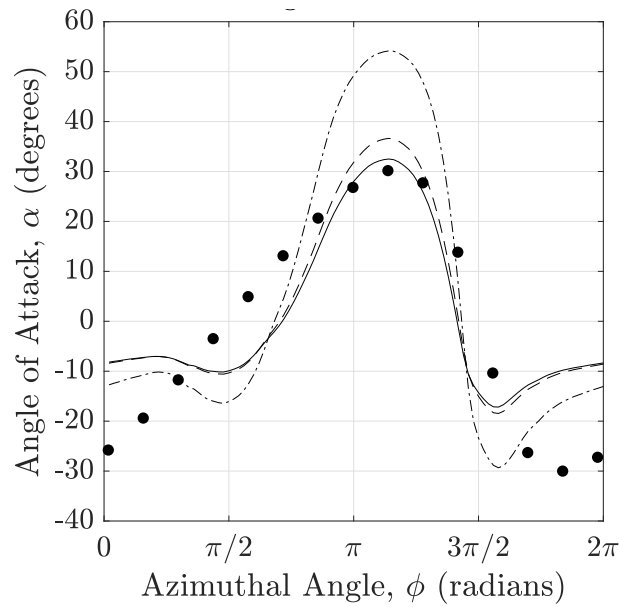
For changes in probe location the source strength of the individual airfoil noise contributions will change. When calibrating models against experimental data it will be necessary to know how strongly source strength is affected by changes in the selection of the 1D flow parameter inputs. Fig. 5.5 shows the value of acoustic source strength as observed one metre above the airfoil for a single azimuthal rotation of the SWIP V2 VAWT. Results effectively show the effect that changing the probe location has on overall source strength.

For the inflow-turbulence noise models of Paterson and Amiet, and Buck there is a maximum absolute discrepancy of 3.27 dB and 3.5 dB respectively, which translates to a maximum azimuthal source error of 3.3 - 3.4 % with respect to the 0 - 20 % probe.

The BPM self-noise model (Fig. 5.5c) exhibits a maximum absolute spread of 4.28 dB across the range of probe locations and 6.1 % error total. The iTNO model (Fig. 5.5d)

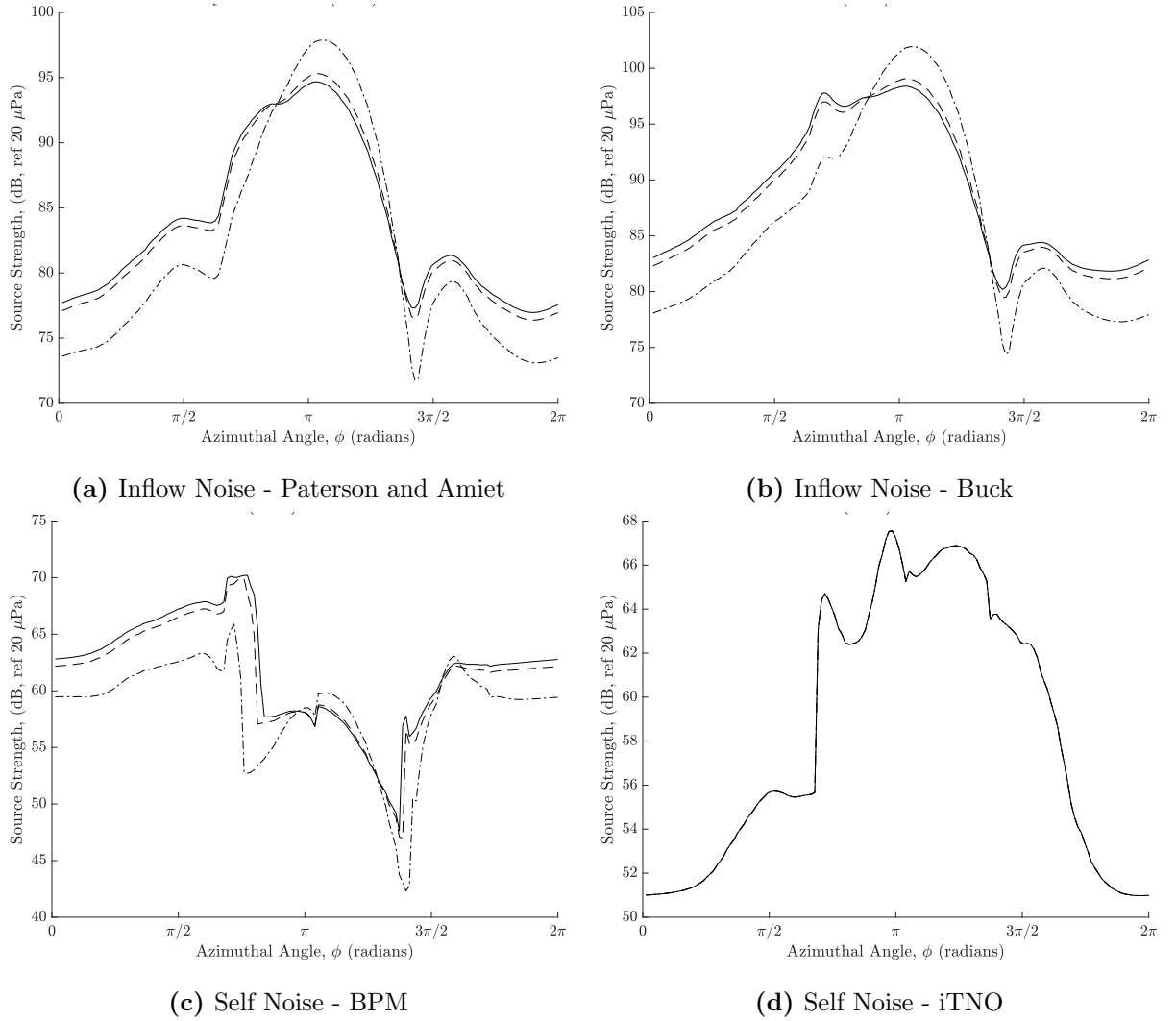


(a)



(b)

**Figure 5.4:** Key flow parameters with changes in azimuth showing the effect of the amount of data sampled ahead of LE with azimuth. (a) Local blade inlet velocity, (b) angle of attack. — 15-20%, -- 10-20%, - · - 0-20%, • Geometric solution (Eq. 4.1-4.2).



**Figure 5.5:** Acoustic source strength of radiated airfoil noise for the SWIP V2 for several different noise models with changes in the upstream probe location. Figure captions describe the noise models used. — 15-20%, -- 10-20%, - · - 0-20%.

moves away from the inherent arbitrariness in the definition of the 1D freestream velocity component. This model is insensitive to the selected probe location since the physics of the model mean that noise source strength becomes a function of the pressure and suction surface boundary layer parameters.

If the probe were to be extended further, the full extent of the discrepancies could be analysed. However, this particular analysis can still aid in understanding how sensitive the noise source strength is for changes in the probe length. These results can feed into further model tuning if experimental data were to become available.

### 5.2.3 Turbulence Parameters

By performing CFD calculations, a more accurate local prediction of the turbulent parameters ahead of an airfoil blade can be considered. The exact definition of these parameters will change depending on the computational turbulence model used. In this study the  $k$ - $\omega$  SST model [125] was considered. By using this model several turbulence parameters become directly available namely the turbulent kinetic energy,  $k_t$ ; turbulence intensity,  $I_t$ ; turbulent dissipation rate,  $\epsilon_t$ ; and, specific dissipation rate,  $\omega_t$ . These parameters are summarised in Table 5.1 highlighting which quantities are scalars and which are vectors. Several relationships need to be provided to calculate the missing parameters.

The turbulent flow characteristics cannot be validated in the same manner as the velocity and angle of attack measures and, as a result, the same sensitivity inputs decided on for the inflow velocity relationships of Eq. (5.1) are used. That is to say that overall quantities of turbulence used in computations become single scalar values, which corresponds to the averaged quantity over a portion of the probe ahead of the leading edge.

| Parameter                  | Symbol         | Units                          |
|----------------------------|----------------|--------------------------------|
| Nodal Coordinate           | $\mathbf{x}_i$ | m                              |
| Velocity                   | $\mathbf{U}_i$ | m/s                            |
| Turbulent Kinetic Energy   | $k_t$          | m <sup>2</sup> /s <sup>2</sup> |
| Turbulence Intensity       | $I_t$          | %                              |
| Turbulent Dissipation Rate | $\epsilon_t$   | m <sup>2</sup> /s <sup>3</sup> |
| Specific Dissipation Rate  | $\omega_t$     | 1/s                            |

**Table 5.1:** CFD Exported Data

#### Integral Length Scale

Since the integral length scale of turbulence is an unsteady flow parameter it needs to be redefined as a statistical quantity based on available data. This is performed by making use of a definition from the  $k$ - $\omega$  SST turbulence model,

$$L_{t,1} = \frac{\sqrt{k_t}}{\omega_t c_\mu} \quad (5.3)$$



where  $\omega_t$  is the specific dissipation rate of turbulence and  $c_\mu = 0.09$  is a constant as defined by Menter [125]. Thus, expressions are derived to calculate the integral length scale, Eq. (5.3), and intensity, Eq. (4.10), of turbulence based on outputs of the k- $\omega$  SST turbulence model.

### Transverse Integral Length Scale in the Boundary Layer

Assuming homogeneous, isotropic turbulence the longitudinal and transverse length scales can be related [139, 140] as  $L_{t,1} = 2L_{t,2}$ . By using the assumption of isotropic turbulence, this means that the transverse length scale can now be calculated from CFD data by combining this relation with Eq. (5.3),

$$L_{t,2} = \frac{1}{2} \frac{\sqrt{k_t}}{\omega_t c_\mu} \quad (5.4)$$

### Kinetic Energy in the Boundary Layer

The quantity of kinetic energy in the boundary layer,  $k_t$ , can be extracted directly from the pressure and suction side boundary layer probes.

### Turbulent Shear Stress in the Boundary Layer

The approach to find the shear stress in the boundary layer, required for the calculation of the iTNO model, follows the approach of Townsend [141]. Turbulent shear is proportional to the Reynolds stress tensor in shear flow from,

$$\overline{u_2^2} = \frac{\overline{u_1 u_2}}{c_3} \quad (5.5)$$

where the constant  $c_3 = 0.3$  is a constant found by Moriarty [71] to fit best for airfoil noise problems. The normal component of stress,  $\overline{u_1 u_2}$ , is defined as,

$$\overline{u_1 u_2} = \frac{k_t}{\omega_t} \frac{\partial U_1}{\partial x_2} \quad (5.6)$$

### 5.2.4 Boundary Layer Velocity Parameters

#### Boundary Layer Thickness

The assignment of the boundary layer thickness,  $\delta$ , is defined as the location where a bounded flow reaches 99 % of the freestream flow velocity or the height above the wall for which  $U(y) = 0.99U_\infty$ .

Due to blade-blade aerodynamic interactions, flow across the trailing edge is normally not ‘*well behaved*’, and thus a new definition for the boundary layer thickness was proposed as 95 % of the maximum velocity. A definition suited to simplifying calculations. It will be demonstrated that the quantities of interest are quite insensitive to the definition of the boundary layer velocity, and that any estimate of boundary layer thickness that is at least above a certain height will produce asymptotic results.

#### Boundary Layer Displacement Thickness

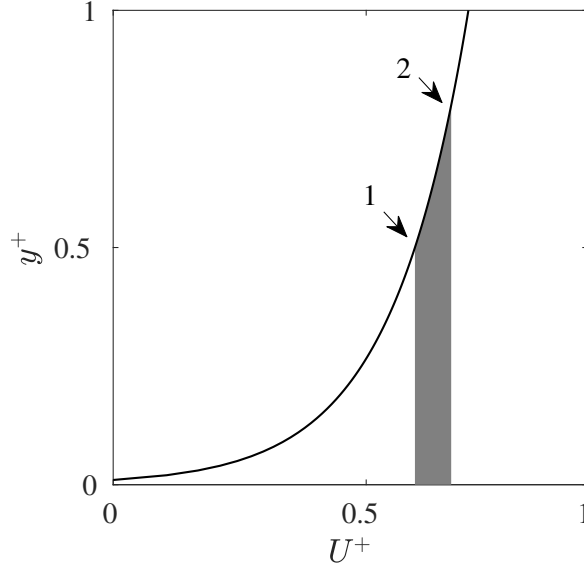
Determining the boundary layer thickness,  $\delta$ , from experimental or computational data is prone to error due to the high level of accuracy required to resolve the boundary layer velocities perpendicular to the wall. Traditionally, for experimental procedures, the flow field is first averaged and quantities of importance can be extracted from these averaged results. However, for rotating machinery whereby the system is constantly in motion this approach becomes less intuitive. Furthermore, the addition of confluent effects means that trailing edge boundary layers will not always follow the form of the law of the wall function. It will be shown here that it is much easier to accurately determine boundary layer displacement thickness,  $\delta^*$ , than it is to determine boundary layer thickness.

An arbitrary, non-dimensionalised, representative velocity boundary layer profile for air flowing over a flat plate has the following analytic solution based on the law of the wall [121],

$$U^+ = \frac{1}{\tau} \ln y^+ + C^+ \quad (5.7)$$

where,  $U^+$  is the non-dimensionalised velocity parallel to the wall,  $y^+$  is the non-dimensionalised wall coordinate perpendicular to the wall. For this example  $\tau = 1$  and  $C^+ = 5.0$  are used as constants.

Fig. 5.6 shows equation (5.7) plotted for non-dimensionalised values of  $y^+$  where the  $y$ -axis represents the wall to freestream coordinate. The shaded area in the figure represents an arbitrary integral area between point 1. and 2. The velocity,  $U^+$ , is also non-dimensionalised where 0 is at the wall and 1 is the maximum velocity.



**Figure 5.6:** Non-dimensionalised boundary layer velocity profile

Fig. 5.7 shows plots of the gradient of two functions with respect to the  $y^+$  wall coordinate. The first function,  $F = \int_0^{U_n^+} y^+ dU^+$ , represents the cumulative area under the boundary layer velocity curve for values between 0 and  $U_n^+$ , where  $n$  is monotonically increasing to its maximum (solid line). The second function,  $F = U^+$ , represents the non-dimensionalised boundary layer velocity,  $U^+$  (dashed line). The functions can directly represent the quantities of  $\delta^*$  and  $\delta$ , the first being an integral quantity and the second being related to the velocity.

In Fig. 5.7, as the value of  $y^+$  increases, the constant velocity quantity ( $\propto \delta$ ) decreases exponentially, whilst the gradient of the area quantity ( $\propto \delta^*$ ) is approximately constant.

For any appreciably large value of  $y^+$  (roughly greater than a value of about 0.15), the following relationship is observed,

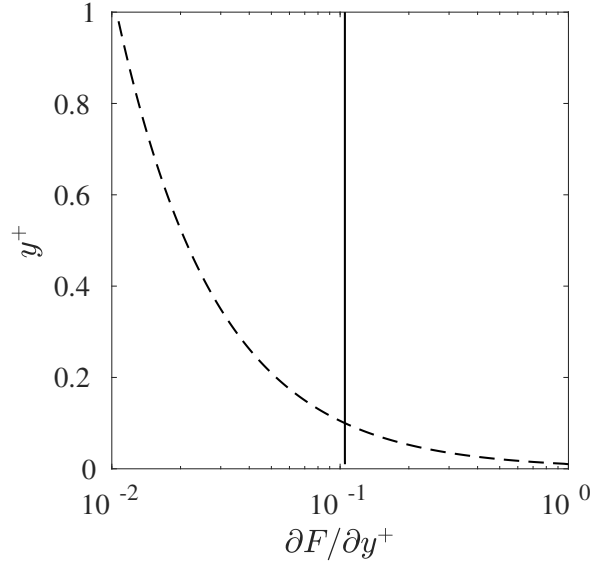
$$\frac{\partial U^+}{\partial y^+} \ll \frac{\partial \int_0^{U_n^+} y^+ dU^+}{\partial y^+} \quad (5.8)$$

and it is seen that,

$$\frac{\partial \int_0^{U^+} y^+ dU^+}{\partial y^+} \approx \text{constant} \quad (5.9)$$

whilst,

$$\frac{\partial U^+}{\partial y^+} \propto e^{-y^+} \quad (5.10)$$



**Figure 5.7:** Gradient of; —  $F = \int_0^{U^+} y^+ dU^+$ , - - -  $F = U^+$

The result of the area quantity does not change considerably with increases in  $y^+$ , whilst the constant velocity quantity has an exponential gradient with increases in  $y^+$ . Thus for any selection of  $y^+$  beyond a certain height, for an ordinary velocity boundary layer, the area quantity will not change much.

This result emphasises that the determination of the boundary layer displacement thickness,  $\delta^*$ , is relatively insensitive to the selected value of the boundary layer thickness,  $\delta$ , provided that the selected value is sufficiently far away from the wall.

Fig. 5.8 shows the value of  $\delta^*$  with azimuth for a VAWT. One set of data is derived from the BPM model described in equations (4.16) - (4.22). The BPM model is a function of Reynolds number and angle of attack. The other plot is from a CFD calculation for the same case (Case 2 described later). The BPM model predicts very large values of  $\delta^*$  in a very broad range, such that a logarithmic graph is required to plot all the data. These data suggest that CFD data may be used to bring additional fidelity to the noise modelling procedure. In particular, the TNO model becomes insensitive to data probed ahead of

the leading edge and thus the noise prediction becomes less sensitive to the confluent conditions experienced by airfoils in unsteady fields.

### Flow Reversal

At certain azimuthal locations the airfoil can encounter a dynamic stall motion whereby the velocity boundary layer profile becomes separated from the airfoil wall. When this occurs the self-noise model is no longer valid and the stall model from [2] is used. This condition is defined when,

$$\left. \frac{\partial U_2}{\partial x_2} \right|_0 \leq 0 \quad (5.11)$$

where subscript 0 refers to the location at the wall.

### Boundary Layer Reconstruction from CFD Informed Method

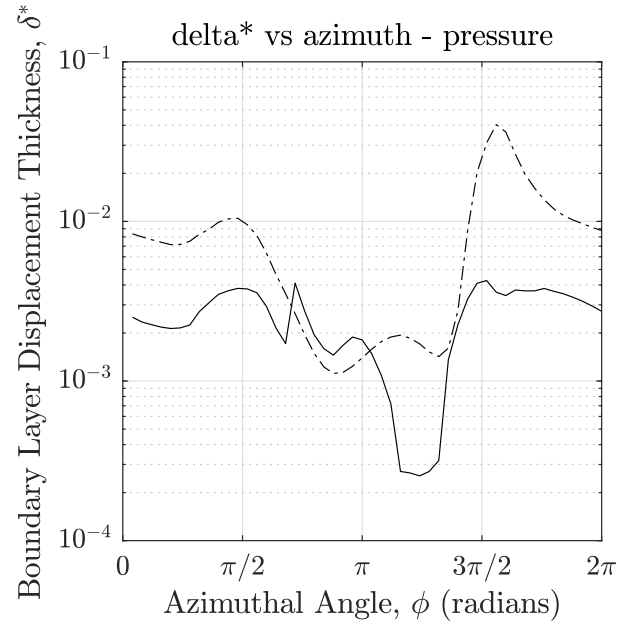
The trailing edge boundary layer velocity profile can be extracted from the CFD results by making use of the velocity and position data along the trailing edge extraction probes. These profiles are used to calculate the boundary layer thickness,  $\delta$ , and the displacement thickness,  $\delta^*$ . Velocity at the trailing edge needs to be normalised in a direction parallel to the airfoil chord line. This process is defined by a number of flow relationships and computational operations described below.

#### i. Read Data and Interpolate

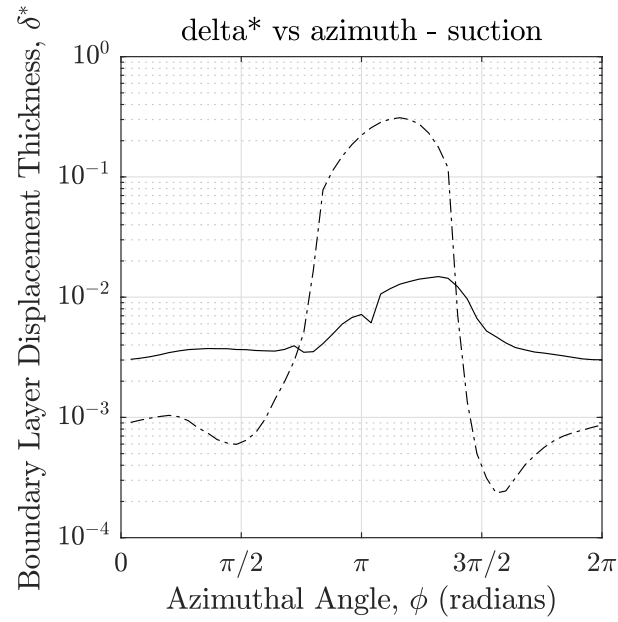
For any given numerical time step, the CFD data file corresponding to that location is read into MATLAB. Input data consists of the velocity,  $\mathbf{U}_{i,\text{CFD}}$ , and position,  $\mathbf{x}_i$ , vectors along the probe, where the subscript  $i$  denotes the cartesian coordinate either  $x$  or  $y$ . Each vector is interpolated onto a linearly spaced grid of a reasonable size for the required computational accuracy, using the MATLAB `pchip` interpolation method to perform a shape-preserving piecewise cubic interpolation.

#### ii. Calculate Velocity Profile

The input velocities from the CFD calculation do not consider the moving reference frame. The rotational velocity is added to the stationary velocity by,



(a)



(b)

**Figure 5.8:** Boundary layer displacement thickness with azimuth for (a) Airfoil pressure side (b) Airfoil suction side from; — CFD data; - - - BPM model.

$$\mathbf{U}_i = \mathbf{U}_{i,\text{CFD}} + \omega(\mathbf{x}_i - \mathbf{x}_c) \quad (5.12)$$

where  $U_i$  is the velocity at any given location in the boundary layer,  $\omega$  is the rotational velocity of the turbine, and  $x$  is a nodal location. Subscript  $i$  represents a cartesian coordinate along the boundary layer, subscript CFD represents the velocity extracted from the CFD simulation, and subscript  $c$  represents the cartesian coordinate of the centre of the turbine.

Once the rotational velocity has been included, the magnitude of the velocity vectors is calculated and the scalar projection of the velocity parallel to the chord is taken,

$$\mathbf{U}_{\text{BL}} = \left| \sqrt{\mathbf{U}_x^2 + \mathbf{U}_y^2} \right| \cos \theta \quad (5.13)$$

where  $\theta$  is the angle between the velocity vector,  $\mathbf{U}_i$ , and the airfoil chord calculated using `atan2`, the subscript BL represents the boundary layer flow velocity, a component of velocity parallel with the airfoil chord line at the trailing edge.

### iii. Boundary Layer Detection

The arbitrary assignment of the boundary layer thickness,  $\delta$ , is generally defined as the location where a bounded flow reaches 99% of the freestream flow velocity,  $\delta = 0.99U_{\text{infly}}$ . Due to blade-blade aerodynamic interactions flow across the trailing edge is normally not ‘*well behaved*’ and thus a new definition for the boundary layer thickness is proposed for this study as,

$$\delta = 0.95\max(\mathbf{U}_{\text{BL}}) \quad (5.14)$$

The MATLAB `findpeaks` function is used to determine where local maxima in the boundary layer velocity profile occur. The maxima are sorted, and a value of 95 % of this maxima is selected as the corresponding boundary layer thickness value. If this function fails (i.e. the local maxima lies at the end of the data), then 95 % of the global maximum value is used as the boundary layer thickness value.

The boundary layer displacement thickness,  $\delta^*$ , is defined as,

$$\delta^* = \int_0^\infty \left( 1 - \frac{\mathbf{U}_{\mathbf{BL}}(x_2)}{\max(\mathbf{U}_{\mathbf{BL}})} \right) dx_2 \quad (5.15)$$

where the coordinate  $x_2$  is defined as being perpendicular to the airfoil chord (see Fig. 3.1).

Figs 5.9 and 5.10 show a series of non-dimensionalised boundary layer velocity profiles extracted from a CFD case compared to the solution to Eq. (4.24) for the given azimuthal angles seen in the figure title. The CFD profiles shows some deviation from the analytic solution which is to be expected due to blade confluence. For the given range similar trends are observed between CFD results and analytic solutions.

## 5.3 Limitations of the CFD Method

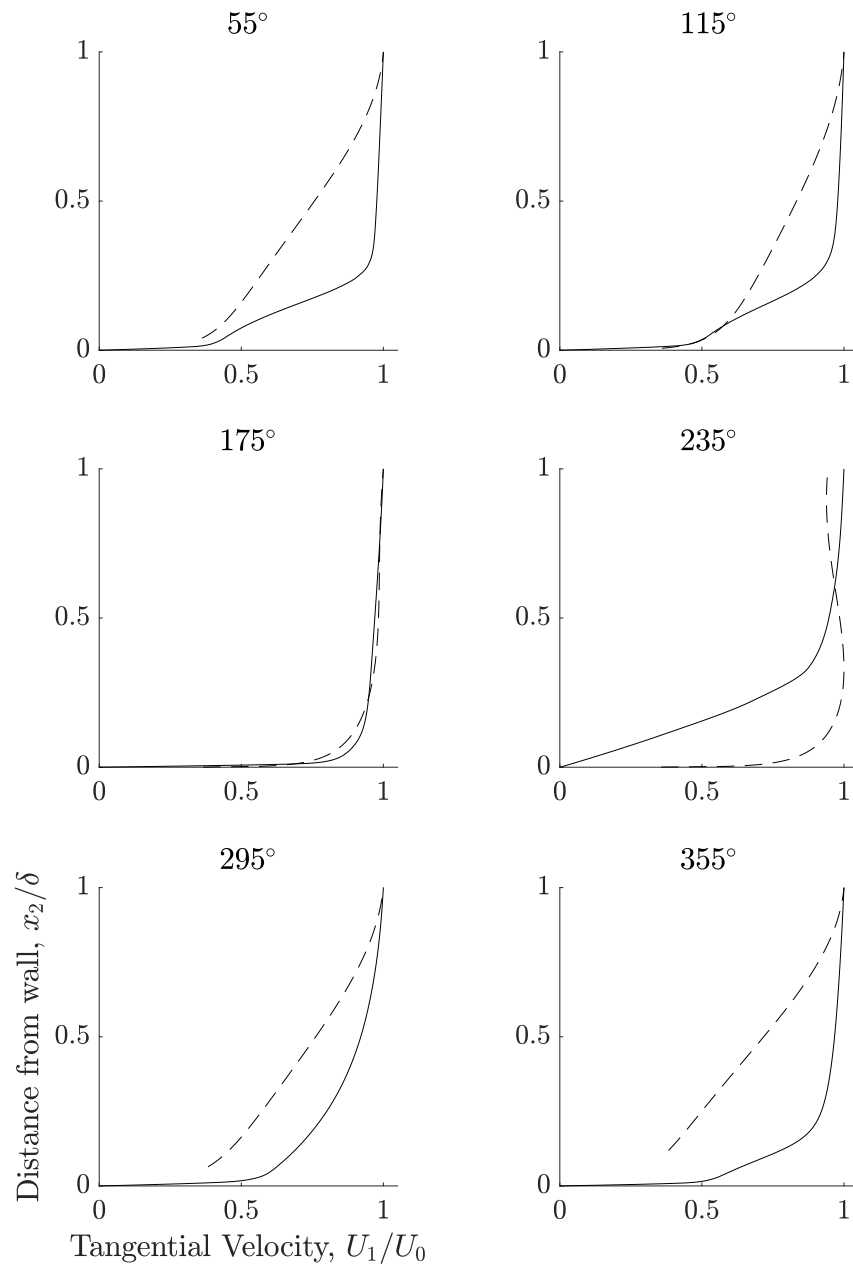
The CFD informed prediction overcomes many of the limitations of the analytically informed prediction method, primarily in relation to the availability of sufficiently accurate aerodynamic input data - one of the primary concerns of the authors of previous rotor noise prediction models [114].

### 5.3.1 Improvements over the Status Quo

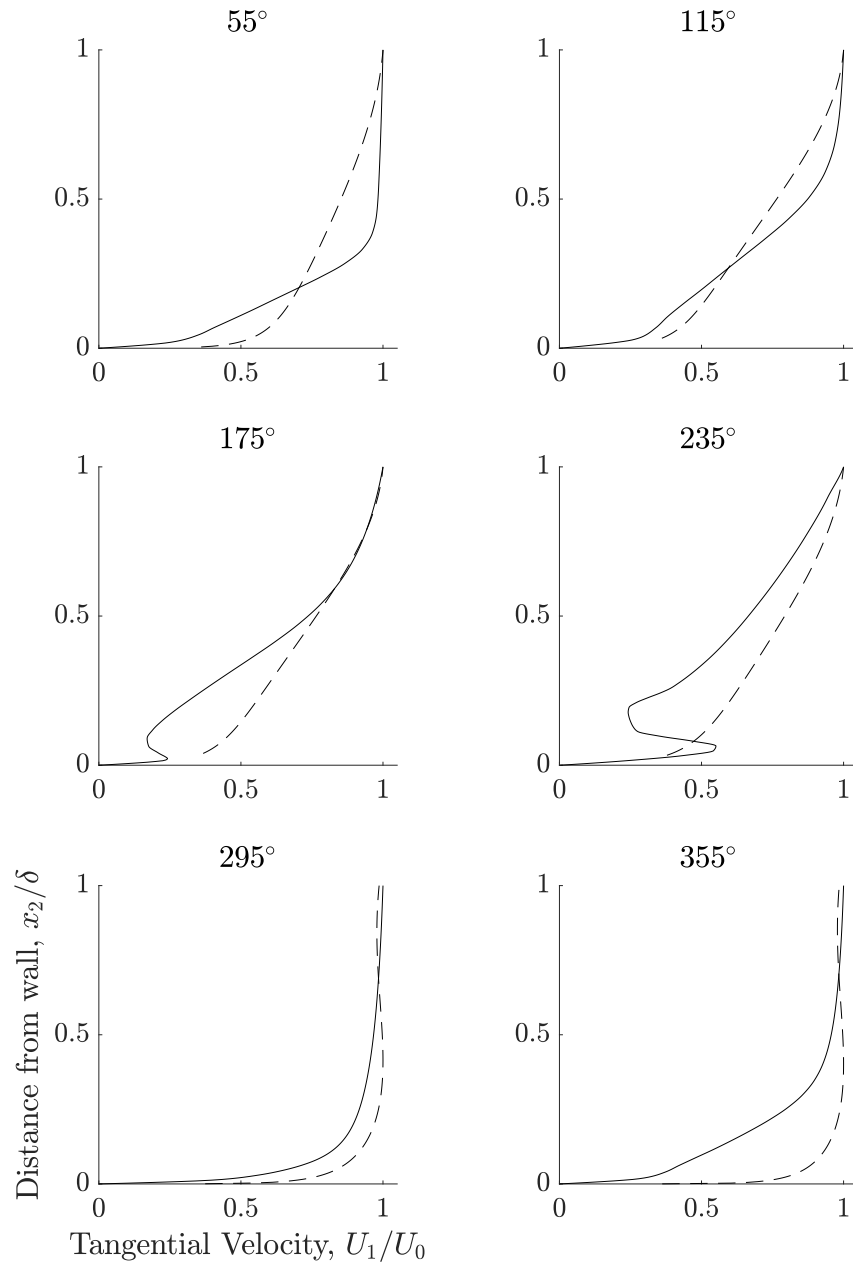
The CFD model is seen to capture physical phenomenon relating to VAWT flow and the following features are captured:

- Dynamic stall. The sudden loss of lift with rotation will cause an aerodynamic blocking effect. This effect is seen to modify blade inflow velocity and angle of attack.
- Stagnation effects. Near the airfoil leading edge velocity is seen to be non-constant, this effect is captured using CFD and affects the blade inflow velocity.
- Confluence. The effect of blade-blade aerodynamic interaction affects the contour of the velocity boundary layer profile modifying its shape when compared to an analytic boundary layer.
- Unsteady turbulence. Turbulence at the leading edge is unsteady when compared to the domain inflow specifications.





**Figure 5.9:** Non-dimensionalised boundary layer velocity profiles for azimuthal angles  $\phi$ . Pressure side. — CFD Data, -- Coles' Law of the wall/wake.



**Figure 5.10:** Non-dimensionalised boundary layer velocity profiles for azimuthal angles  $\phi$ . Suction side. — CFD Data, -- Coles' law of the wall/wake.

### 5.3.2 Limitations of the Noise Modelling Approach

The noise modelling approach implemented in ROBOT has been improved upon by the addition of high quality multi-dimensional input data. However, there are still several assumptions that exist for the current approach.

The assumption that steady noise models can be applied to unsteady problems in a quasi-steady manner still exists. It is assumed, for this analysis, that acoustic timescales are much larger than those of the aerodynamic system. This means that even though previous models have shown acceptable results, the physics of the aerodynamic and noise problem is still somewhat closer to that of a pitching airfoil than a stationary airfoil.

The iTNO noise model does not consider stall noise. The primary noise sources considered in this study are self- and inflow-turbulence noise. Whilst stall noise can be predicted using the BPM model, it is not considered in detail.

The current approach relies on noise measurements from a single study. In the previous chapter, attempts were made to validate the available results, however several assumptions were made relating to the turbulence parameters and the validity of previous predictions. So, whilst the study in the present chapter may show a deviation from prior results, conclusions cannot be drawn relating to the exact validation of results.

### 5.3.3 Limitations of the Available CFD Model

CFD computations performed in support of this study focused on analysing gross aerodynamic quantities of the VAWT, whilst the present study is concerned with the noise prediction thereof. The noise models implemented are particularly sensitive to the available input data and whilst gross aerodynamic values, such as flow velocities, can be predicted with relatively good accuracy using even a crude numerical grid, more detailed parameters related to the turbulent field will become harder to predict.

In terms of the overall aerodynamic quantities, the CFD computations will need to be validated before being used for noise prediction. This validation will mean different things for different parts of the problem and will also depend on available measurement data. For this particular study the only available data was the intended overall power output of the turbine. For the mesh in question, grid sensitivity studies will need to be performed for the computations to determine how sensitive the overall noise levels are to refinements

in the grid.

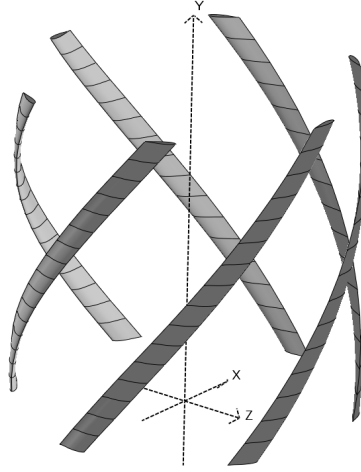
In the available CFD model, a length scale and turbulence intensity were selected as domain inlet conditions. When using RANS or URANS based turbulence models, Ansys Fluent uses these domain inlet conditions as a good starting point for iteration of the Navier-Stokes equations, meaning that for a given inlet turbulence intensity the turbulent dissipation rate and length scale may end up being different than expected due to non-linearities in the NS equations. Furthermore, RANS based models rely on statistical descriptions of the turbulent cascade, essentially constraining a complex multi-dimensional phenomenon into a number of single-dimensional statistical descriptors. Due to the time dependent modelling procedure (transient analysis) the turbulent structures are essentially *released* from the inlet domain and marched forward in time (and space) until the simulation has been stopped. As these structures propagate they experience varying amounts of turbulent decay which can be seen as a function the amount of initial energy in the flow. This has the implication that, for two flows of differing turbulent intensity each one will decay at different rates meaning that the expected turbulent intensity at the domain of interest will not be as expected. Thus any study where the turbulence intensity is changed may not yield accurate results. Switching to a higher order turbulence model such as a Large Eddy Simulation may have the added benefit of better preserving the turbulent structures and thus allowing for an improved understanding of how turbulence affects rotor noise.

## 5.4 Results

The computational method was implemented to predict the overall noise produced by a VAWT. Using input data from the CFD calculations noise predictions, for an unique six-bladed VAWT, were performed. Several parameter studies were conducted, leveraging the enhanced detail afforded by the computations over that of the analytical predictions.

### 5.4.1 Case Comparison

The SWIP consortium has proposed a six-bladed 2kW vertical axis wind turbine, dubbed the *SWIP V2*, designed for a roof mounted application. A 3D schematic of the blades is



**Figure 5.11:** 3D model of the SWIP V2 vertical axis wind turbine blades.

seen in Fig. 5.11 and the specifications of the turbine are summarised in Table 5.2. The turbine produces a peak power output of 2 kW which corresponds to an operating tip speed ratio of around 2.0. This operating point, as well as several other cases relevant to the turbine design, are considered and summarised in Table 5.3. Cases 1-3 have varying tip speed ratios as governed by changes in inlet velocity and RPM values. Cases 4 and 5 are dynamically equivalent to Case 2, but have different levels of inflow turbulence.

| Parameter        | Value      |
|------------------|------------|
| Chord Length     | 0.183 m    |
| Pitch            | 0°         |
| Radius           | 1 m        |
| Span             | 2 m        |
| Helical Twist    | 90°        |
| Number of blades | 6          |
| Airfoil          | DU06-W-200 |
| Tower height     | 10 m       |

**Table 5.2:** V2 turbine parameters defining the geometry of the VAWT. The helical twist is defined as an azimuthal rotation of the blade along span in the rotor coordinate system.

Turbulence intensity and length scale values used as boundary conditions at the computational domain inlet were derived from measurements taken at the test site, according to the IEC61400-1 standard for definitions of atmospheric length scale measurements. These parameters, detailed in Table 5.3, are prescribed as freestream conditions at the computational domain inlet and denoted with the subscript,  $\infty$ . As the turbulence levels experienced within the rotor do not match those in the freestream [131, 142], CFD was used to provide a more accurate representation of the levels of turbulence experienced by the blades themselves, which will differ to the freestream conditions.

| Case | $U_\infty$ (m/s) | RPM   | TSR  | $L_{t,\infty}$ (m) | $I_{t,\infty}$ (%) | $C_p$ (CFD) |
|------|------------------|-------|------|--------------------|--------------------|-------------|
| 1    | 8.4              | 200   | 2.49 | 7                  | 20                 | 0.136       |
| 2    | 10               | 190.1 | 1.99 | 7                  | 20                 | 0.398       |
| 3    | 12               | 169.2 | 1.48 | 7                  | 20                 | 0.267       |
| 4    | 10               | 190.1 | 1.99 | 7                  | 10                 | 0.398       |
| 5    | 10               | 190.1 | 1.99 | 7                  | 5                  | 0.398       |

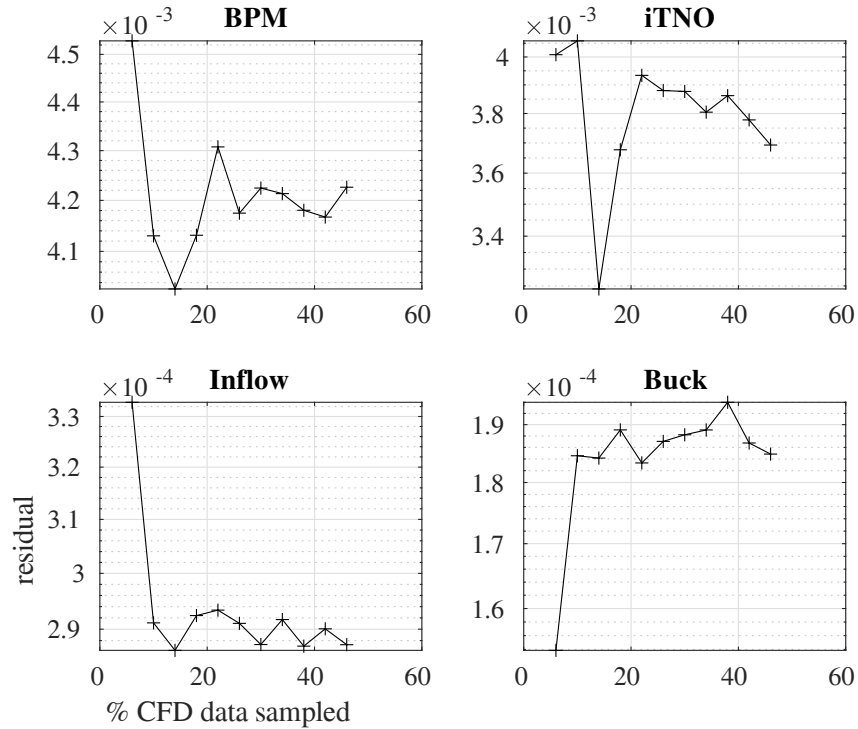
**Table 5.3:** Test cases for current study. Each case represents operating conditions on the power curve. Turbulence parameters are measured freestream conditions from meteorological measurements on site. The power coefficient ( $C_p$ ) was determined from the CFD analysis.

### 5.4.2 Convergence and Grid Independence

The prediction method makes use of large data banks of CFD data. In order to predict noise it is not necessary to sample every available data point. A convergence study was performed to assess the amount of the available data required, in order to obtain a solution that is independent of the size of the data bank. The convergence criterion is defined as,

$$r_n = \frac{|W_1 - W_n|}{W_1} \quad (5.16)$$

where  $r_n$  is the value of the residual at step  $n$  and  $W$  represents the OASPL value at a standard fixed listener location. Subscript 1 denotes the value at the first time



**Figure 5.12:** Residuals as a function of increases in amount of CFD data sampled for four noise models. Residuals are not normalised and thus graph axes are not self-consistent.

step. Convergence is achieved when the value of this residual does not change appreciably between steps.

Using Case 2 as a reference, **ROBOT** was used to determine convergence. Fig. 5.12 shows the values of the residual for each of the four noise models implemented. The value of the residual does not change significantly after 20 - 30 % of the CFD data has been sampled. The number of sources along the blade span can be changed but the solution was considerably less sensitive to this figure, and converged after about 15 spanwise sources were introduced. A run with 250 time steps and 15 spanwise sources (3 750 function calls) took about 180 wall clock seconds to complete on a Core i5 desktop PC.

CFD simulation results are sensitive to increases in mesh density, a grid refinement study was performed to determine the effect of these refinements on the results of the acoustic study. The computational grid used for the CFD calculations was refined by increasing the number of cells in the mesh by about 70 %, as well as increasing the number of local grid points around each airfoil by the same percentage. Results of the grid sensitivity study are presented in Table 5.4. The table shows the OASPL for each of the

four noise models using two separate CFD grids. OASPL was calculated on the ground 1.5 hub heights away from the turbine downstream. The models all show a negligible change in OASPL with changes in cell count. This result emphasises that the models are relatively insensitive to increases in mesh density.

|                            | Grid 1    | Grid 2    | Difference |
|----------------------------|-----------|-----------|------------|
| <b>Mesh Cells</b>          | 1 500 000 | 2 600 000 | 73 %       |
| <b>Airfoil Grid Points</b> | 300       | 520       | 73 %       |
| <b>BPM</b>                 | 40.5 dB   | 41.5 dB   | 2.5 %      |
| <b>iTNO</b>                | 34.9 dB   | 35.1 dB   | 0.6 %      |
| <b>Paterson and Amiet</b>  | 62.8 dB   | 61.5 dB   | 2.1 %      |
| <b>Buck</b>                | 67.5 dB   | 67.0 dB   | 0.7 %      |

**Table 5.4:** Effect of mesh refinement on OASL predictions

### 5.4.3 Dominant Noise Sources

The QR5 turbine was dominated by inflow-turbulence noise produced by interactions between turbulence in the rotor and the blades themselves. When compared to experiments this noise source was overpredicted. In this section CFD data from the computational campaign was extracted, and used as input data for the noise prediction models, in order to compare noise predictions between the two approaches. This analysis considers Case 2 from Table 5.3.

Fig. 5.13 presents a comparison of these noise predictions using both the *analytical prediction method*, as well as the *CFD based method*. In this result the CFD based method shows lower levels of inflow-turbulence noise when compared to the analytical approach. This is quantified as an overall reduction in SPL at all frequencies of the order of 15 dB. This deviation can be attributed to a more rigorous prediction of local blade inflow velocity and turbulence intensity at the local blade inlets. Fig. 5.4a compares the blade inlet velocity with azimuth for the analytical and CFD based approaches of Case 2, and shows that CFD based results deviate considerably from the geometric solution with a peak velocity prediction roughly 5 m/s lower than the analytic baseline. This discrepancy



in the two approaches leads to large scale differences in the SPL predictions, due to the power dependence of the Mach number term Eq. (3.14).

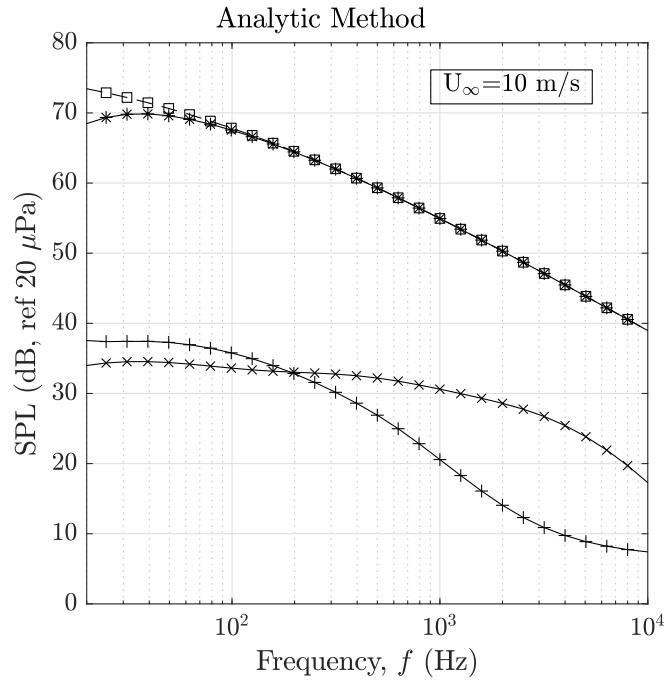
The analytical prediction of self-noise is comparable to previous predictions, with similar trends observed for the intensity and spectral peak. Results from the iTNO model show a shifted peak frequency. The CFD based prediction method yields different results to the analytical approach. BPM predicted noise from this analysis is at least 10 dB higher than the iTNO prediction. In particular the BPM model no longer shows a broad low frequency trend but rather a very distinct peak frequency at 1 000 Hz with the rest of the signal rolling off around this location.

Using CFD data to inform the iTNO model reduces the overall level of predicted noise by about 10 dB, compared to the analytical baseline. In the verification study, the analytical approach yielded a slight overprediction in noise compared to experimental results. The current result shows a general reduction in noise predicted at all frequencies, as well as shift in the peak noise frequency concentrating more of the signal in the lower frequency region.

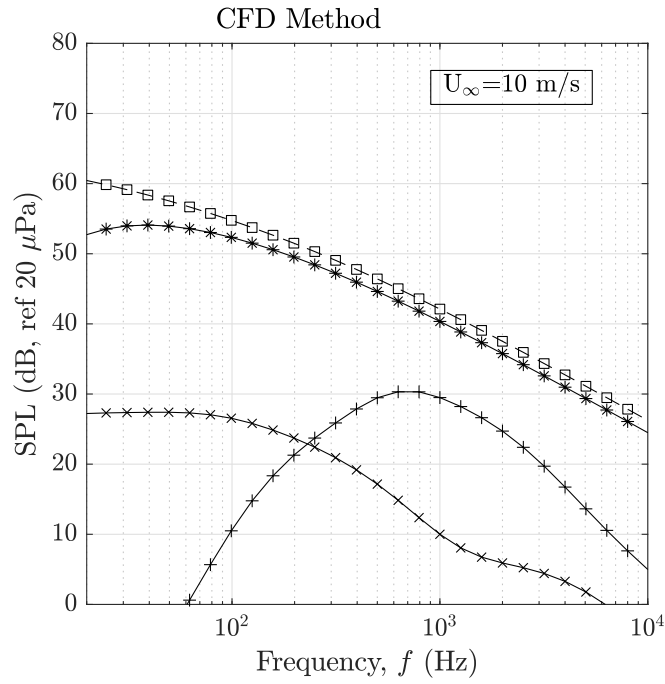
The *analytical flow model* approach utilises a series of empirical equations to solve for the airfoil boundary layer thickness and other input parameters. By using CFD calculations to inform the noise models, the aerodynamic model is solved in the time domain. This has implications for noise prediction in that, even though the noise model is applied using a quasi-steady assumption, the aerodynamic flow model is not. Thus, more of the problem is now considered from a time domain perspective which alleviates the concerns of the steady flow assumption used for the analytical flow modelling approach [114].

#### 5.4.4 Noise Source Localisation

Due to unsteady aerodynamic effects, noise emitted by a VAWT is not constant throughout rotation. It is possible to determine at which azimuthal location the source strength will be at a maximum. This was accomplished by setting the value of the directivity function to be a constant of 1 for a single airfoil rotation. Furthermore, three dimensional effects are also neglected. By extracting the value of all noise sources throughout a single rotation, a plot is produced that shows the source strength distribution for the turbine with azimuthal rotation (Fig. 5.14), for CFD Case 2. The plot shows the predicted frequency content of



(a)



(b)

**Figure 5.13:** Prediction of self-noise and inflow-turbulence noise using the (a) analytical and (b) CFD method; + TBL - BPM; × TBL - iTNO; \* Inflow - Paterson and Amiet; □ Inflow - Buck

noise for both self-noise and inflow-turbulence noise sources as the turbine blade undergoes a single rotation. Both plots are scaled equally and are presented in third-octave bands. From these plots, the location of maximum noise is at an azimuthal location of  $\pi$ . This corresponds to the location directly upwind of the turbine.

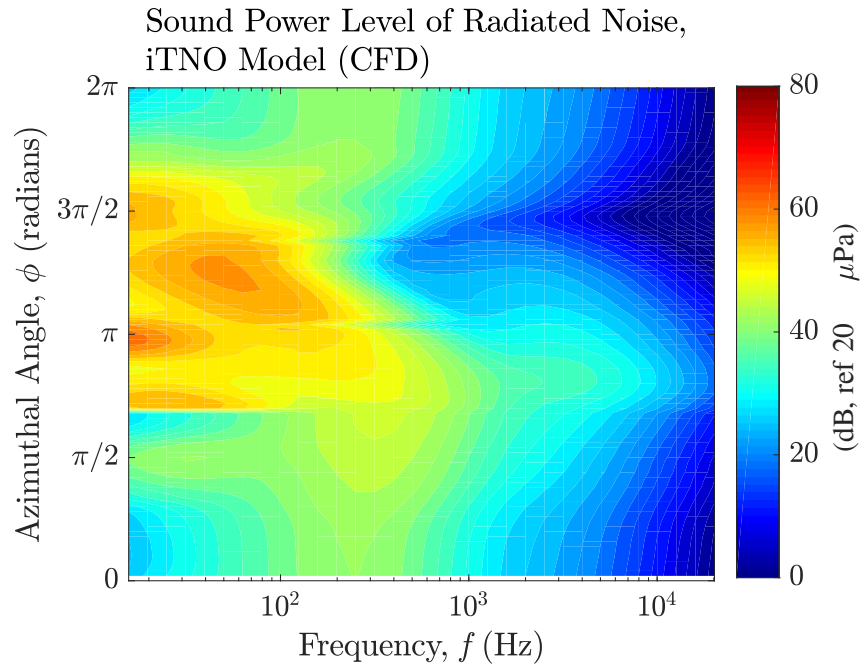
There is a reduction in the level of source noise observed at an azimuthal location of  $3\pi/2$  corresponding to the location where dynamic stall occurs. Local blade inflow velocity rapidly reduces to a value of less than zero over a comparably brief angular range. This sudden reduction in velocity accounts for a reduction of noise of about 20 dB in Fig. 5.14b. When looking at the source localisation contour for TBL noise, a peak is observed near the azimuthal location of  $3\pi/2$ . In this case, high frequency noise reduces to lower levels whilst the low frequency components appear to remain high. A distinct shift in the peak frequency is observed in the region between  $\pi$  and  $3\pi/2$ . Physically this is ascribed to an increase in the boundary layer turbulence in this region.

### 5.4.5 Directivity

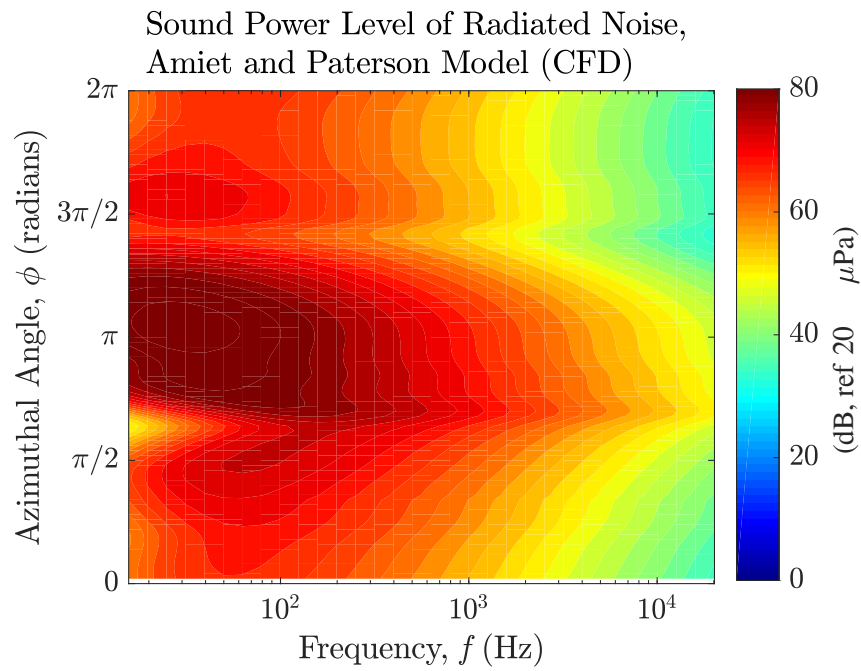
VAWT noise has a slight directional component to it [89]. Provided that wind direction is consistent, this directional effect will be observable as close as one hub height away from the turbine, and is quantified as a change in the OASPL levels observed at various azimuthal locations around the device. Case 2 is considered for this analysis.

Fig. 5.15 shows the change in OASPL level with azimuth for listeners located around the turbine at 1.5 hub heights away. This plot shows similar trends to Fig. 5.14b. The Paterson and Amiet model for inflow-turbulence noise is considered. The source strength plot (Fig. 5.15a) shows the value of the OASPL at the source excluding the directivity factors of Eqs (3.18) and (3.19). The loudest source region occurs at a location aligned with the maximum blade velocity (Fig. 5.4a). When considering directivity corrections, this source noise region gets considerably attenuated due to the effect of low frequency airfoil noise directivity consistent with those used in the calculation of inflow-turbulence noise [6, 52].

For the OASPL associated with directivity, source strength is a function of azimuth, with the loudest region occurring close to  $2\pi$ . Source strength is amplified by the distance to the receiver, where the loudest region is one where the highest velocity occurs closest



(a)



(b)

**Figure 5.14:** Spectra showing frequency content of noise, predicted using (a) iTNO and (b) Paterson and Amiet model, with change in azimuthal angle.

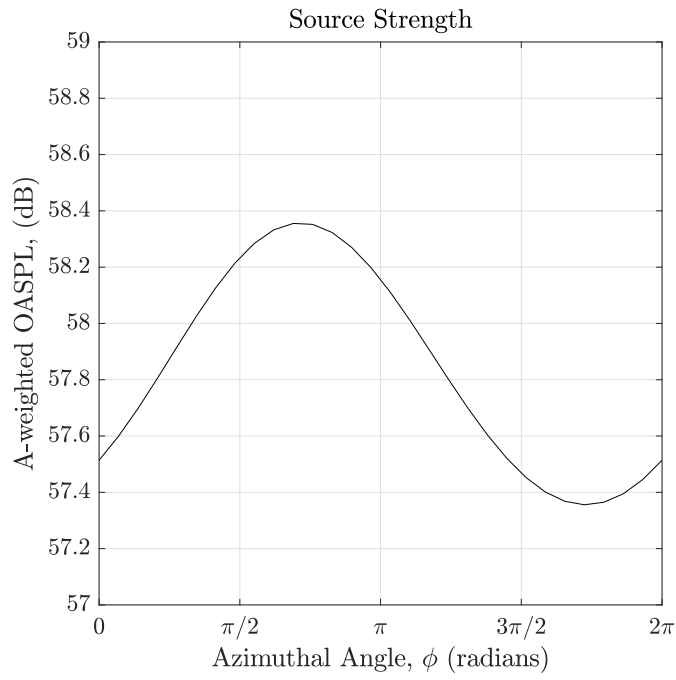
to the receiver, thus reducing the distance to the receiver,  $z$ , and increasing local noise. The inclusion of the directivity factors (Fig. 5.15b) leads to a reduction in the level of noise observed at the receiver location. Attenuation is a factor of the blade geometry in relation to the receiver. These directivity factors are geometrically a function of azimuthal blade position with respect to listener location, as well as being dynamically linked to an inverse proportionality factor of the blade velocity. These interlinked factors make for directivity plots that are asymmetric around the wind axis, with the loudest region occurring downstream of the turbine.

#### 5.4.6 Effect of Varying Flow Conditions

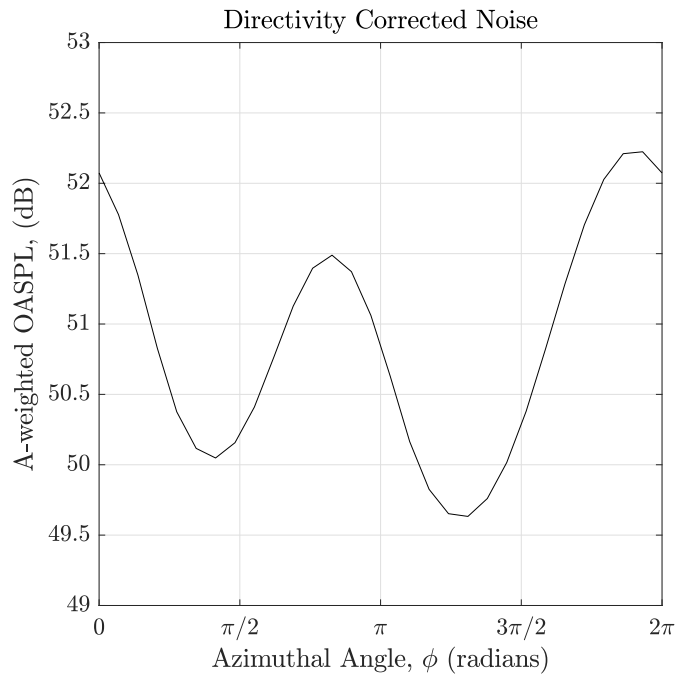
The domain inlet velocity and turbine RPM were adjusted to determine the effect of changes in operating conditions on overall noise production. Cases 1 - 3 from Table 5.3 were used for this analysis. As the domain inlet velocity is increased, so too is the maximum angle of attack and local blade inflow velocity with azimuth. Fig. 5.16 shows these parameters with changes in azimuthal angle for the range of varying inflow conditions. As the boundary conditions change, so too does the maximum velocity within the rotor. Cases 1 and 2 experience very similar maximum velocities (Fig. 5.16a) with a difference of about 1 m/s observed at roughly the same azimuthal angle, whilst Case 3 shows a large increase in the maximum velocity which will directly increase noise levels. For the case of 12 m/s inlet velocity, the dynamic stall event, characterised by a very sudden angle of attack change seen in Fig. 5.16b, is much more pronounced.

Turbulence levels within the rotor generally increase with increases in inflow velocity. There is a clear trend of increasing turbulent velocity as inflow velocity increases (Fig. 5.17b). Turbulent length scales, on average, also increase with increasing inflow velocity (Fig. 5.17a)

Fig. 5.18 shows the overall noise prediction using the Paterson and Amiet model, for inflow-turbulence noise, for the three cases. The listener is located at a standard location, on the ground, 1.5 hub heights downstream of the turbine. Increasing the domain inlet velocity directly increases the overall noise produced. This increase is due to changes in the blade velocity within the rotor.

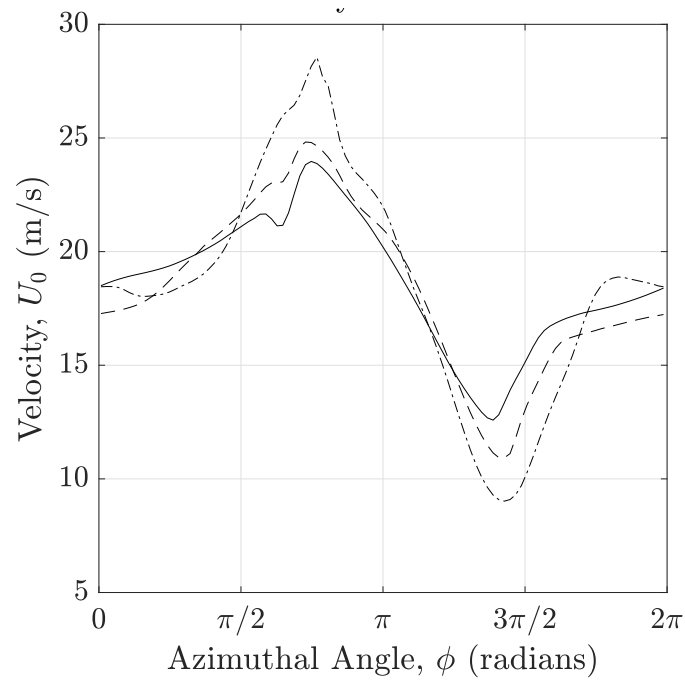


(a)

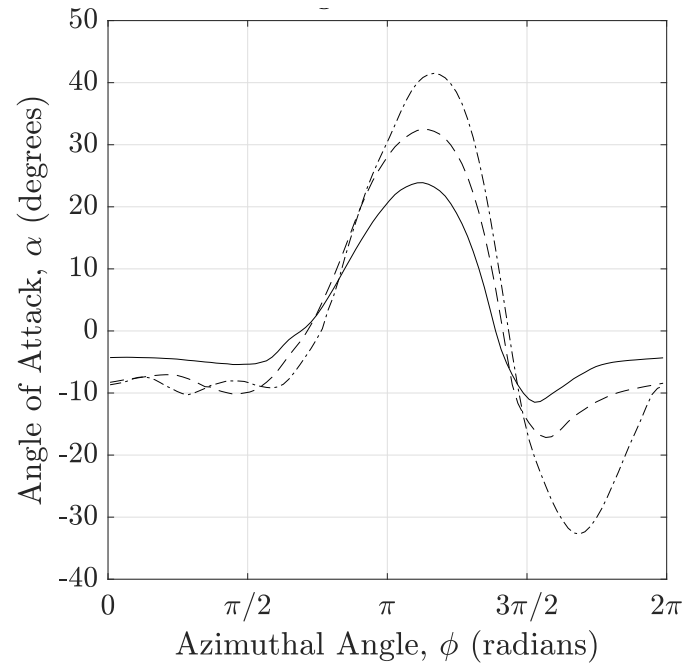


(b)

**Figure 5.15:** OASPL at various azimuthal locations on ground level one hub height away from the turbine (a) without and (b) with directivity corrections applied.

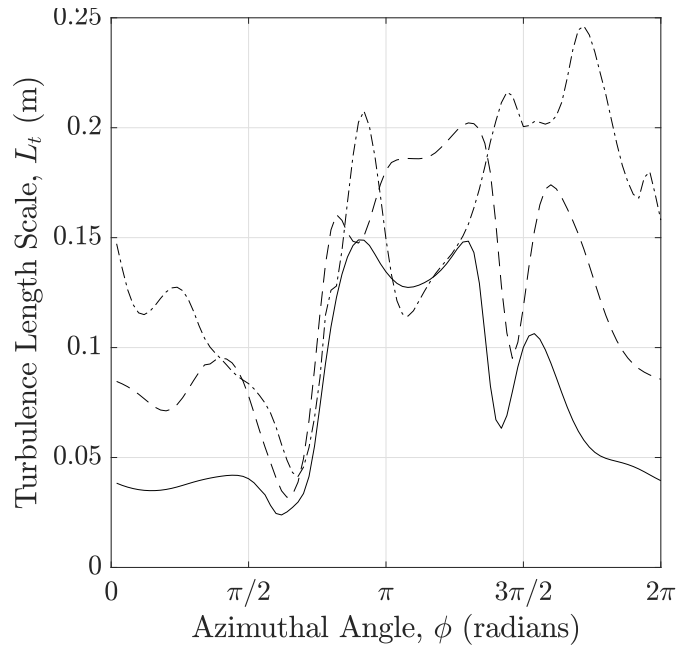


(a)

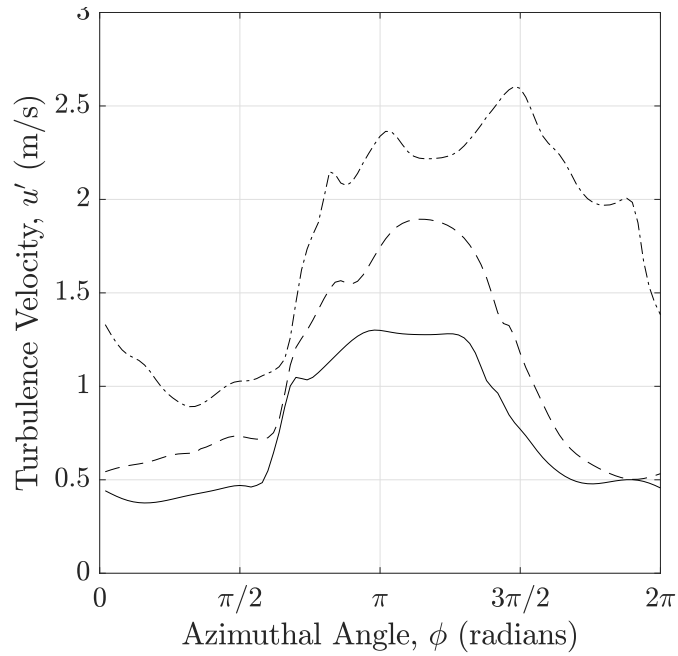


(b)

**Figure 5.16:** Flow parameters at local blade inlet for changes in domain inlet velocity ( $U_\infty$ ) (a) local blade inlet velocity (b) angle of attack with azimuth; — 8.4 m/s, -- 10 m/s, - · - 12 m/s



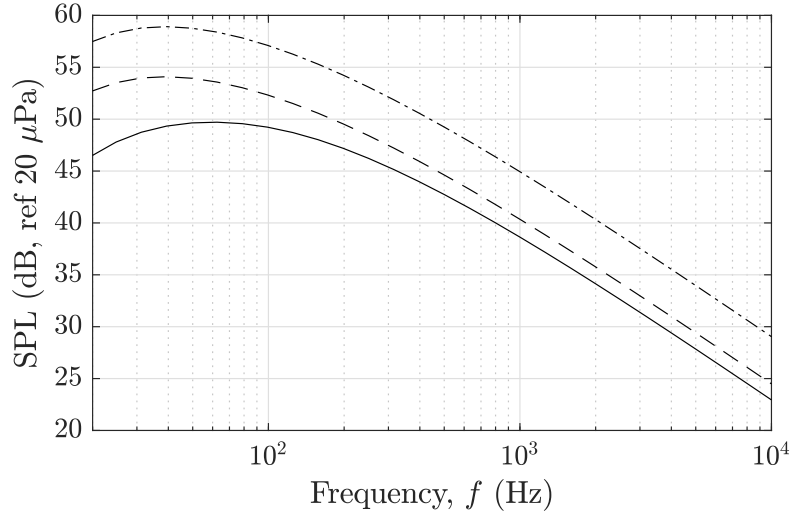
(a)



(b)

**Figure 5.17:** Turbulence parameters at local blade inlet for changes in domain inlet velocity ( $U_\infty$ ) (a) turbulent length scale (b) turbulent velocity with azimuth; — 8.4 m/s, -- 10 m/s, - · - 12 m/s





**Figure 5.18:** SPL with changes in inflow velocity ( $U_\infty$ ) using the Paterson and Amiet model with CFD input data; — 8.4 m/s, -- 10 m/s, - · - 12 m/s

#### 5.4.7 Effect of Turbulence

By making use of CFD calculations it becomes possible to determine how turbulent levels change azimuthally throughout the rotor. For VAWT's operating in urban and peri-urban environments high levels of turbulence are to be expected, and can affect the level of inflow-turbulence noise - a noise source which increases in proportion to the amount of turbulence in the system. The Paterson and Amiet model in Eq. (3.14) scales to the square of the mean turbulent velocity, meaning that a doubling of turbulent velocity leads to an increase in isolated airfoil noise of around 6 dB. Rotor turbulence also has the tendency to slightly increase or decrease turbine power output by around 0.5 % of the rated power [143]. For this analysis the turbulence intensity at the computational domain inlet was modified to determine the effect of this change on overall noise levels. CFD cases 2, 4 and 5 from Table 5.3 were considered for this section, these cases all have the same dynamic boundary conditions bar modifications to the domain inlet turbulence intensity.

#### Rotor Turbulence

Fig. 5.21b shows local blade inlet velocity within the rotor for several different inlet (atmospheric) turbulence parameters. As the domain inlet (atmospheric) turbulence inten-

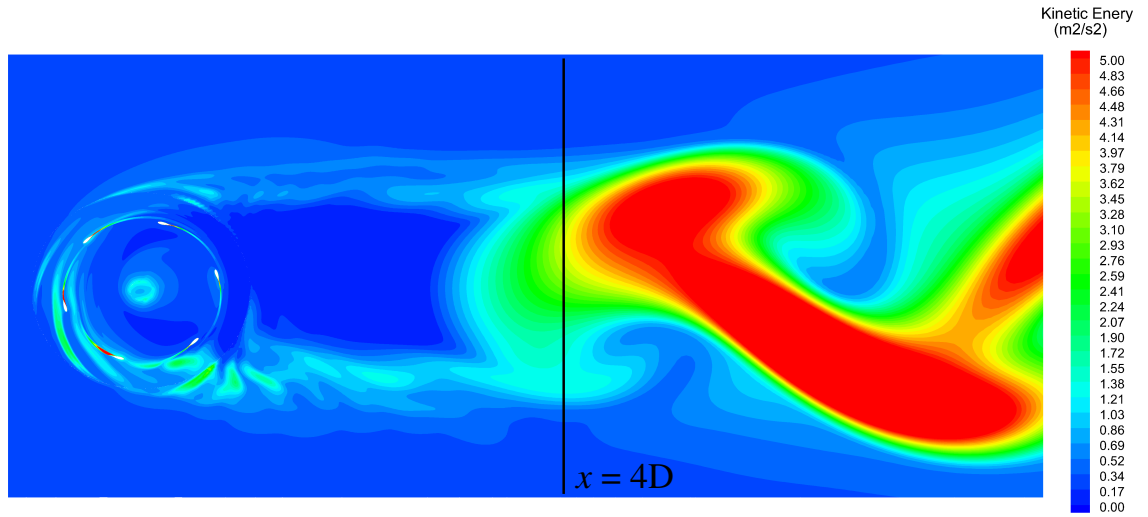
sity increases, the aerodynamic system is not appreciably affected emphasising that any changes in observed noise spectra are due to modified turbulence parameters. As inlet turbulence intensity increases so to do the overall levels of turbulence experienced within the rotor. Fig. 5.20a shows that the turbulence velocity increases appreciably from inlet turbulence of 5 - 10 %, and that an increase to 20 % turbulence intensity does not have such a drastic increase on the overall levels.

Fig. 5.19 shows the level of turbulent kinetic energy within the domain of the rotor, the quantity of which is proportional to the turbulent velocity,  $\overline{u'}$ , seen in Fig. 5.20a. The development of the wake region in both cases is complete at a location of 4D downstream of the rotor. Fig. 5.19a shows a case where inflow turbulence is set at 5 %.

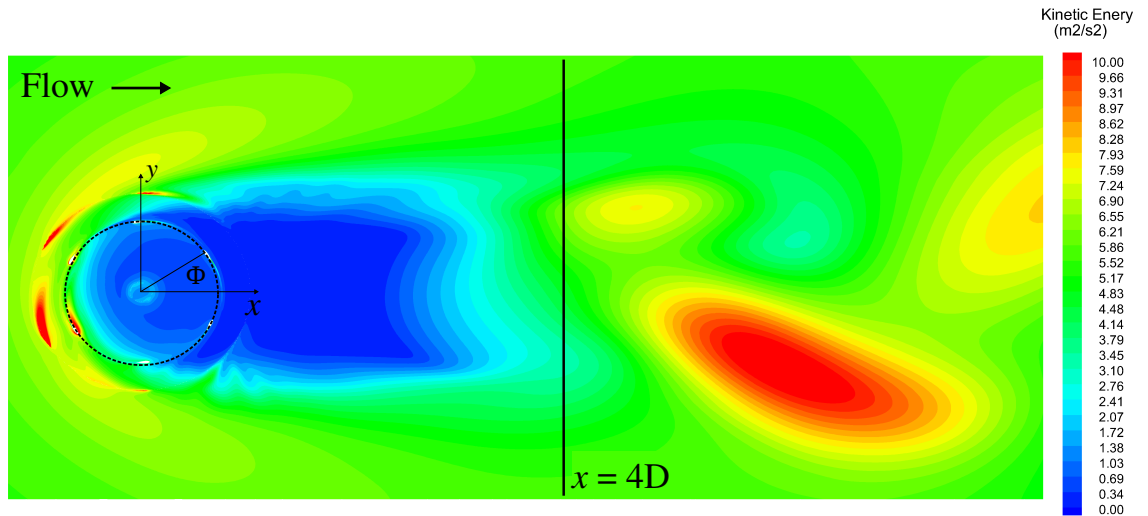
When the domain inlet (atmospheric) turbulence is below 10 %, inlet (atmospheric) turbulence dominates blade generated turbulence. As the inlet (atmospheric) turbulence increases to higher levels the upwind side of the turbine (from  $\pi/2$  -  $3\pi/2$  azimuth) sees large local increases in the overall levels of turbulence and blade generated turbulence begins to dominate over inlet (atmospheric) turbulence. However, whilst going from 5 % - 10 % shows a large increase in the turbulent quantities within the rotor, going from 10 % - 20 % does not show the same proportional trend. This particular trend could be attributed to shortcomings in the CFD based approach.

In Fig. 5.19b, where the prescribed turbulent inlet condition is at the maximum, the distribution of the kinetic energy contours is remarkably different to the low turbulence case, with most of the turbulence concentrated around the windward (left) hemisphere of the rotor. This turbulence become completely dissipated, with kinetic energy levels downstream of the rotor dropping close to zero. This analysis illustrates that high levels of atmospheric turbulence can have an effect on the local turbulence levels within a VAWT during operation.

Fig. 5.21a shows a plot of the length scale with azimuth. Whilst not a comprehensive study on VAWT turbulence this figure shows that for changes in domain inlet turbulence intensity, the average value of the length scale with azimuth tends to oscillate around a value roughly equal to the airfoil chord length (0.183 m). This verifies Pearson's assumption that the integral length scale of turbulence should be set to a level in the vicinity of the airfoil's chord length, when implementing Amiet's inflow noise model [90].



(a)

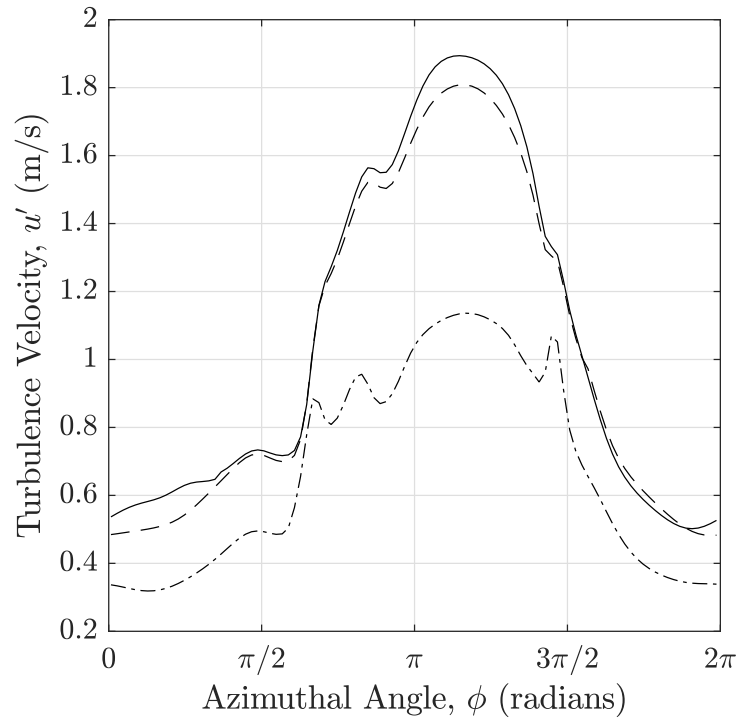


(b)

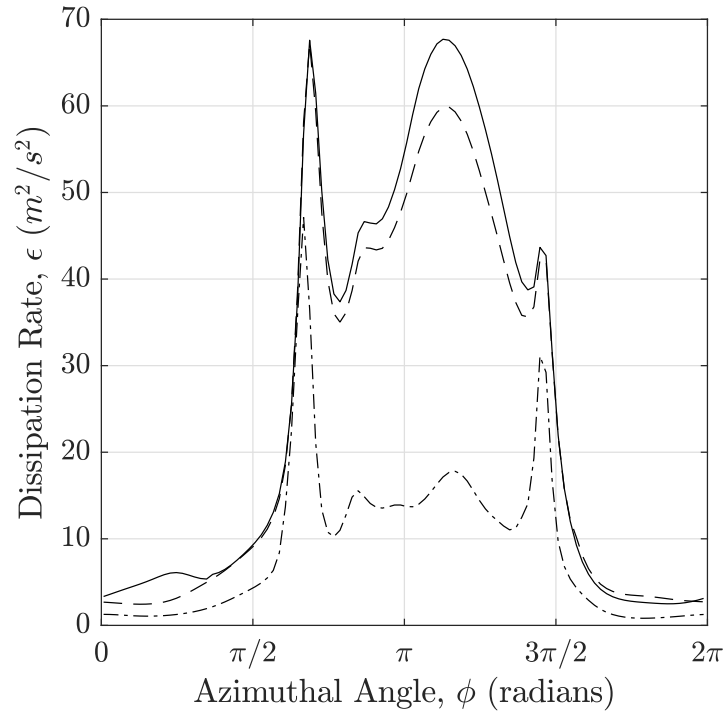
**Figure 5.19:** Turbulent kinetic energy contours with changes in-flow turbulence intensity,  $I_t$ , (a) 5 % (b) 20 % [133]. A line is shown a position 4 diameters downstream to compare wake development at this location.

### Sound Pressure Levels

Fig. 5.22 shows the noise spectra for the turbine, at a standard listener location, 1.5 hub heights away from the turbine base, downstream. The results show increases in overall SPL levels for increases in turbulence intensity. As turbulence intensity increases so does noise. The location of the spectral peak for all cases is consistent. Results also lead

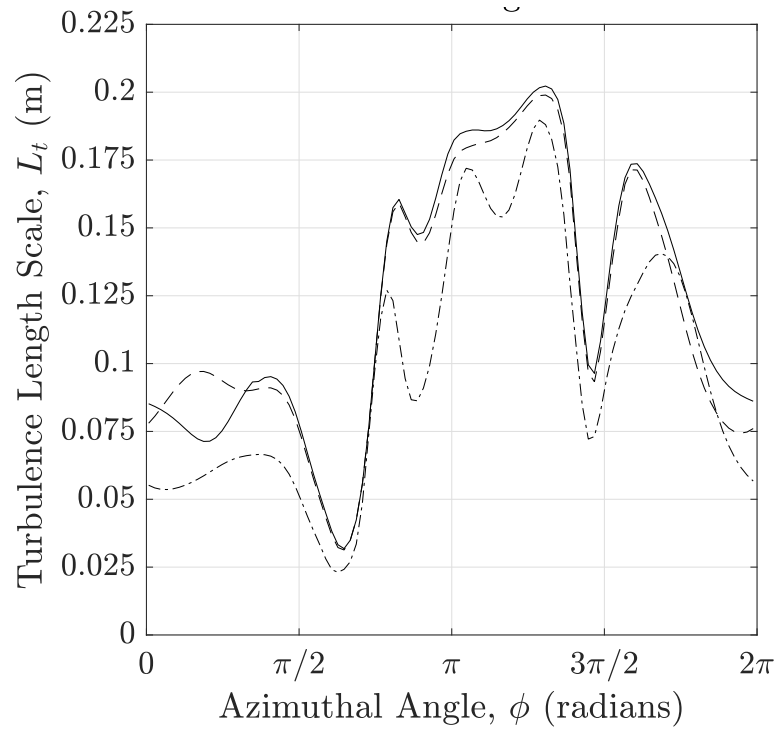


(a)

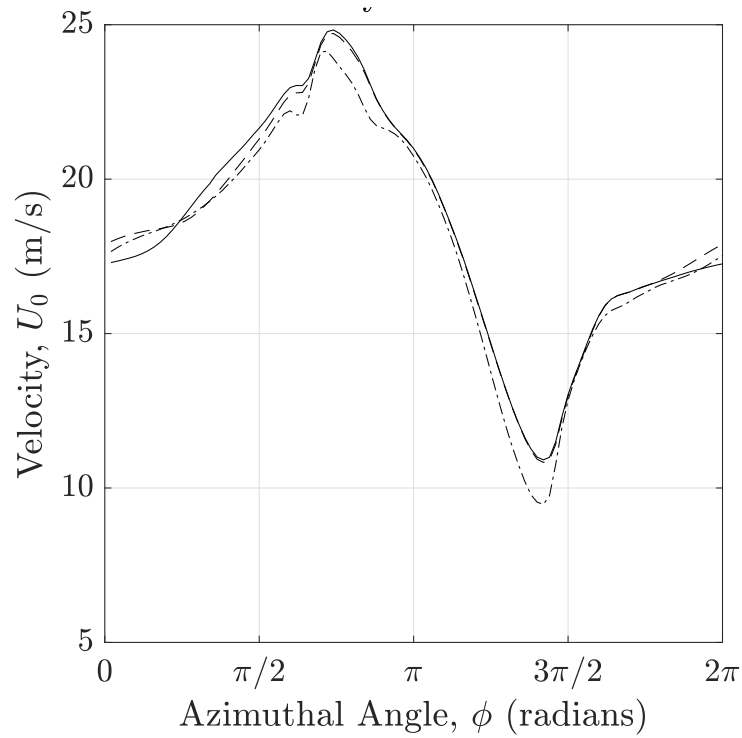


(b)

**Figure 5.20:** Turbulence parameters extracted from CFD at local blade inlet for changes in inflow turbulence intensity ( $I_t$ ) (a) turbulent velocity (b) turbulent dissipation rate, against azimuthal angle; — 20%, -- 10 %, - · - 5%



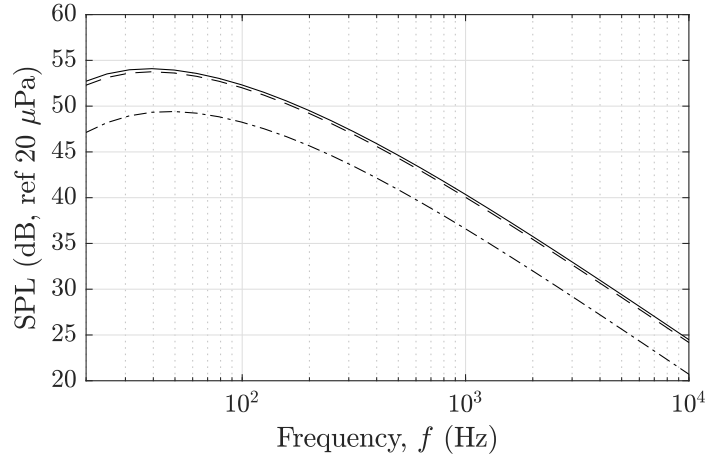
(a)



(b)

**Figure 5.21:** Turbulence parameters extracted from CFD at local blade inlet for changes in inflow turbulence intensity ( $I_t$ ) (a) turbulent length scale (b) local blade inlet velocity, against azimuthal angle; — 20%, -- 10 %, - · - 5%

to the conclusion that there is a saturation point that exists whereby, at a certain inlet turbulence intensity level (around 15 %), noise levels no longer increase.



**Figure 5.22:** SPL with changes in inflow turbulence intensity ( $I_t$ ) using the Paterson and Amiet model with CFD input data; — 20%, -- 10 %, - · - 5%

Results of this study show that for small changes in inflow turbulence there can be a variation of up to 5 dB in overall sound pressure levels, leading to a conclusion that for the present case when the domain inlet (atmospheric) turbulence is below 10 %, inlet (atmospheric) turbulence dominates blade generated turbulence. Furthermore, for accurate calculation of VAWT noise, the azimuthally varying turbulence levels within the rotor need to be properly accounted for and considered. This predicted increase in noise was made possible through the use of CFD data, which showed (Fig. 5.20 and 5.21 ) the effect that blade rotation has in reducing the overall level of generated turbulence within the rotor compared to the inlet turbulence specification.

## 5.5 Summary

A CFD based approach was used to predict noise levels for a new, six-bladed, VAWT designed for the EU FP-7 SWIP project [132]. Using CFD data had the added benefit of modelling turbulence, blade inflow velocity, and boundary layer velocity profiles using the Navier-Stokes equations - parameters which were shown to directly contribute to observed changes in overall noise predictions for the quasi-steady noise modelling approaches [46, 114, 120]. Results comparing the two methods show that using the CFD data led to a

reduction in the prediction of noise intensity compared to a baseline analytic computation. Furthermore, the use of CFD data meant that it was possible to predict how changes in environmental operating conditions, including changes to inflow turbulence, affected the local blade aerodynamics. CFD allowed the prediction of unsteady turbulence fields, which in turn affect overall noise production. Whilst the detailed accuracy of the CFD based approach cannot be determined without noise measurements, the use of this type of hybrid approach was still shown to provide an unique perspective into the unsteady aeroacoustic noise generation of a VAWT.

Results extracted from the CFD simulations needed to be reduced to one-dimensional numbers so that these figures could feed into the noise prediction models. In this study, these results were extracted along a data probe extending from the airfoil leading edge for each timestep of the CFD simulation. A data reduction step averaged the two dimensional flow fields into an inlet velocity and angle of attack value which fed into the prediction scheme. It was found that for the inflow-turbulence noise source, changes in probe length from 0 - 20 % of chord had the effect of altering the source strength by up to 3.5 dB, corresponding to an error spread of about 3.4 %. This result can be used when calibrating models against experimental data to determine how the data reduction should be handled for a robust prediction scheme.

By performing CFD simulations for the existing VAWT, it was observed that the turbulent length scale within the rotor was non-constant with azimuth. For this analysis, the average value of the turbulent length scale within the rotor was less than 1 m, and roughly equal to the airfoil chord length for the given test case. This analysis confirms the proposition that previous authors have described when performing VAWT noise predictions [90] - that is to say that the assumed acoustic length scale, used as input to the Paterson and Amiet [120] inflow-turbulence noise model, is equal to the airfoil chord length.

Inflow-turbulence noise was seen to be the dominant noise source observed in the given cases. This noise source was seen to be up to 20 dB louder than self-noise sources. It is clear that, due to the turbulent environments that small urban VAWT's operate in, inflow-turbulence noise will continue to be a primary noise source.

Turbulent boundary layer noise was predicted using the classical BPM model as well as the iTNO model. The two models predicted similar results for the given cases. The

iTNO model benefited from the fact that it is not dependent on inflow velocity. It was found, in this study, that using the iTNO model was more consistent in terms of solution convergence when compared to the analytical baseline of the same model. Furthermore the iTNO model converged better than the implementation of the BPM model.

Ancillary information related to the turbulence within the rotor enabled the prediction of noise due to changes in inflow turbulence, as well as being able to determine the locations of maximum noise across the turbine.

Results showed that inlet (atmospheric) turbulence dominates over blade generated turbulence when inlet turbulence is set to 5 %, and when inlet (atmospheric) turbulence increases beyond this, the atmospheric turbulence begins to dominate. Changes in the turbulence led to an increase in farfield OASPL of around 5 dB and showed that, for accurate calculation of VAWT noise, the variation of turbulence levels within the rotor need to be accounted for.

However, due to limitations in the CFD modelling approach, from numerical dispersion and dissipation, inconsistencies in the final results were observed. As turbulent intensity was increased from 10 % - 20 % no appreciable increase in SPL was seen. This could be attributed to the way that the CFD model was set up to be used specifically for gross aerodynamic analysis rather than any form of turbulence study.



## Chapter 6

# Open Rotor Noise Prediction

THE noise prediction models described in Chapter 3 are considered to be applicable to a wide range of operating conditions, including high-speed applications. In Chapter 5 the applicability of the noise models (with a focus on VAWTs) to the prediction of aerodynamic broadband noise generated by a vertical axis wind turbine in a turbulent environment was demonstrated. In this chapter, the computational prediction of broadband noise produced by a small scale open rotor engine was also considered. These rotors produce large amounts of turbulence due to their high speed operation, interactions between blade rows, and self interaction. The test case in this study was based on designs for the commercial F7/A7 rotor, but has been scaled down, maintaining the full-size tip speed ratio, in order to test a scale model in a laboratory environment [144].

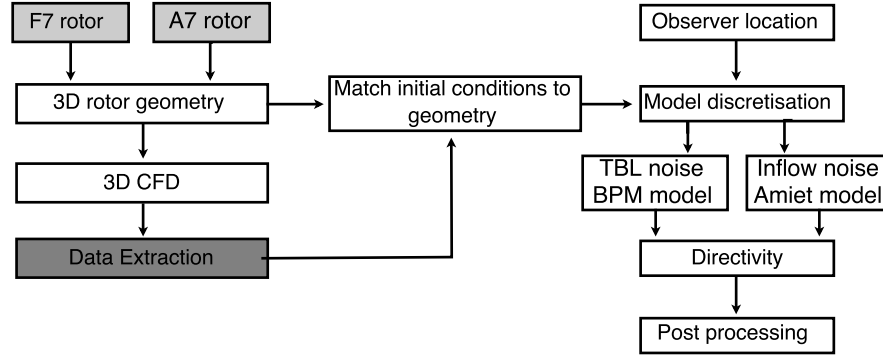
Aerodynamic analysis was performed using steady RANS computations. Noise predictions were performed using the approach described in Chapter 5. The noise predictions were then validated against available measurement data, for the test case, from literature [144].

## 6.1 Prediction Model and Approach

Fig. 6.1 shows the system diagram for the prediction approach. Inputs into the noise prediction include; the extracted CFD data, the initial conditions of the rotor configuration and the observer location. Parameter changes are handled on the CFD side of the model, and each parameter change requires a new CFD computation to be run for that specific case. Within the acoustic module, data from various extraction planes is read once, and manipulated to perform acoustic analysis.

Rotor blades are discretised into spanwise strips, and the airfoil noise models applied using a strip theory approach, in time, for a single rotation. Input data is provided by the CFD computations and extracted at locations corresponding to the discretised strips across the blade span.

Broadband noise is calculated using noise prediction models described in Chapter 3. TBL noise is calculated using the BPM model [2] and inflow-turbulence noise is solved with the Paterson and Amiet model [112]. Noise calculations are completed separately for each rotor and noise source, directivity corrections are applied, and the final results are



**Figure 6.1:** System diagram describing the modelling procedure for broadband noise prediction of the F7/A7 rotor.

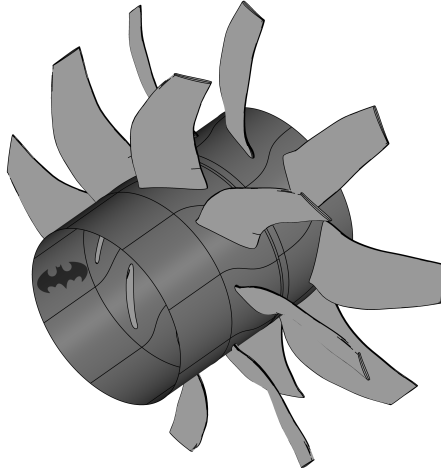
then provided for post-processing.

## 6.2 Test Case

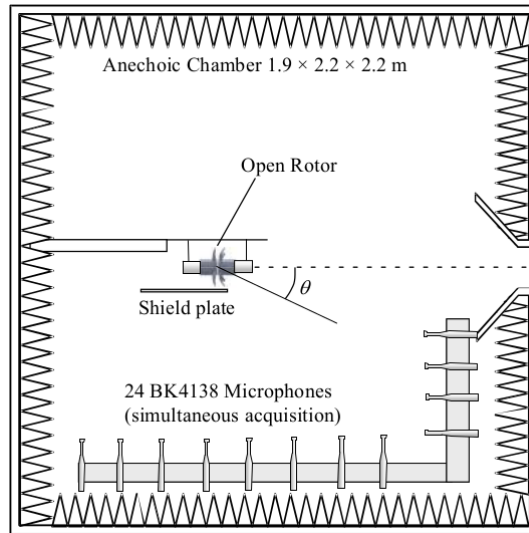
Fig. 6.2 shows the open rotor geometry selected for this study. This particular geometry has been previously used for noise experiments by Truong and Papamoschou [144]. The geometry is a scaled version of the commercial F7/A7 baseline configuration (available via open literature [145]), a two stage 8+8 blade configuration consisting of blades with roughly the same length. This particular version has been scaled to adhere to a dynamic similitude, in terms of tip speed ratio. Blades have also been thickened slightly for improved strength. Furthermore, liberty was taken in selecting generic thin NACA airfoil geometries for the blades, as this data was not made available. This particular geometry is of an older generation of rotor designs where the two blade rows have similar spans. Current trends lean towards shortening the span of the rear rotor row in order to limit vortex interaction noise from this region.

The rotors, produced by advanced stereolithography techniques, were designed to withstand rotational speeds of up to 50 000 RPM. Acoustic analysis was performed inside an anechoic chamber using a 12-microphone array arranged as per Fig. 6.3. In this figure the centre of the domain is located at the centre point along the axis of the two rotors. The rotor was sped up to the defined operating conditions and narrowband measurements were recorded simultaneously. Corrections were applied for actuator response, free-field

corrections and atmospheric absorption. As there was no incoming flow there was no need to apply any shear layer correction. SPL spectra were referenced to a radius of 305 mm from the rotor midpoint. The effect of shielding was also considered.



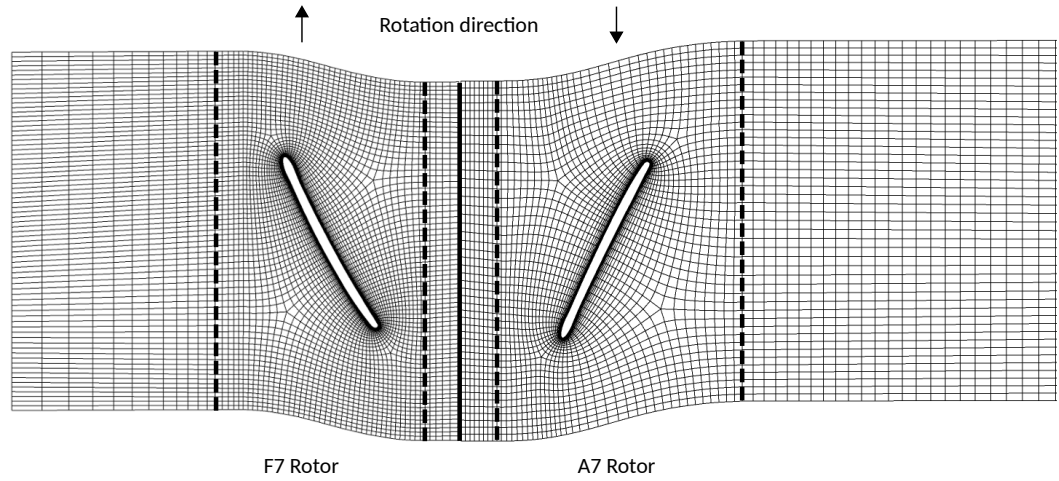
**Figure 6.2:** F7/A7 blade configuration.



**Figure 6.3:** Test setup of the anechoic chamber and microphones used by Truong and Papamoschou [144]

## 6.3 Turbomachinery CFD Analysis

3D steady RANS simulations were performed using ANSYS CFX in order to calculate the inflow and boundary layer flow, and turbulence parameters. With an emphasis on



**Figure 6.4:** Close up of computational grid at midspan. Thick solid lines indicate mesh interfaces and thick dashed lines indicate boundaries between rotational and non-rotational domains.

minimising computational time; a frozen rotor approach was introduced which made use of the steady Navier-Stokes equations with rotational velocity source terms to account for rotation of the domains. Computations were performed to second order accuracy and the  $k-\omega$  SST turbulence model was used. This model works well to predict flow separation in the rotational domain. Multiblock hexahedral-elemental grids were produced for a single blade row, a 2D cut through the hub can be seen in Fig. 6.4. The inlet location is on the left and the outlet is to the right. The image is truncated on the right edge with the full domain extending further. The two rotating sections were provided initial conditions of rotational velocity, corresponding to the known rotational velocities, directions of rotation are indicated in the figure. ANSYS TurboGrid was used to produce high quality computational grids for the simulations. The domain extends 2x blade channel lengths upstream, and 5x channel lengths downstream, where one channel length is defined as the streamwise extent of the F7 rotor domain.

### 6.3.1 Boundary Conditions

The conditions of the experiment were matched in the computational domain. Table 6.1 presents the initial conditions used for the CFD simulations. Experiments were performed without the use of a jet to simulate a suitable inflow velocity, however for the CFD simulation a low inlet velocity of 20 m/s was induced to stabilise the solution. This given inlet

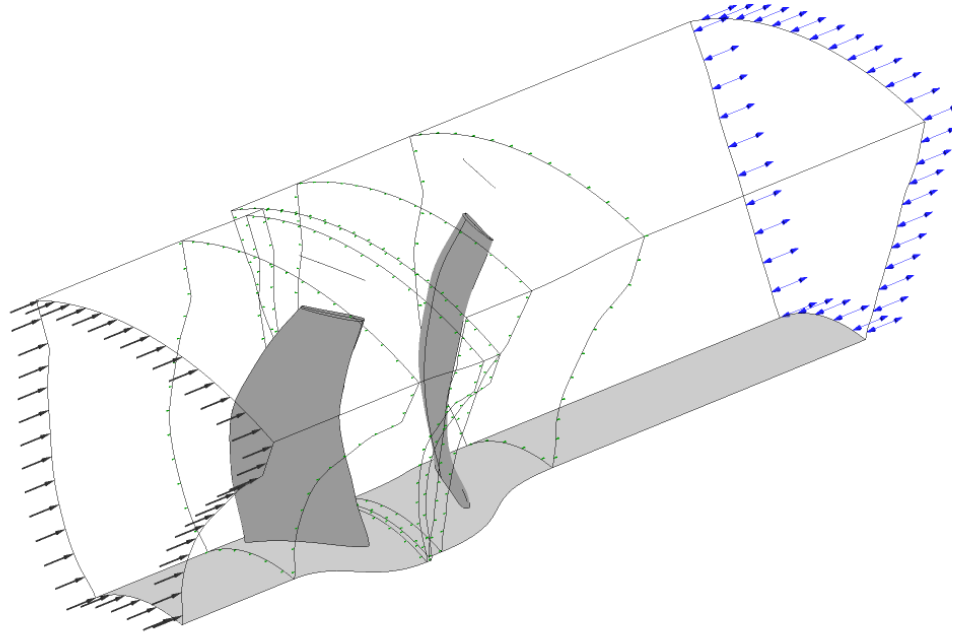
| Parameter                              | Value  |
|----------------------------------------|--------|
| Tip diameter (mm)                      | 80     |
| Hub diameter (mm)                      | 32     |
| Spacing between pitch change axis (mm) | 16     |
| Inlet velocity (m/s)                   | 20     |
| Inlet turbulent intensity (%)          | 5      |
| Inlet viscosity ratio ( $\mu_t/\mu$ )  | 10     |
| <b>F7 Front Rotor</b>                  |        |
| Number of blades                       | 8      |
| RPM                                    | 54 900 |
| Tip Mach number                        | 0.67   |
| <b>A7 Aft Rotor</b>                    |        |
| Number of blades                       | 8      |
| RPM                                    | 46 050 |
| Tip Mach number                        | 0.56   |

**Table 6.1:** Initial conditions for CFD simulations.

velocity is 10 % of the tip speed of the slower, aft blade and negligibly affects the dynamic similitude of the cases. The F7 block was set to have a rotational velocity of 54 900 RPM, and the aft rotor 46 050 RPM with rotational directions as described in Fig. 6.4.

Fig. 6.5 shows the 3D layout of the single passage computational domain and describes the domain boundary conditions. This figure highlights the inlet and outlet conditions as well as the walls of the domain. The grey coloured areas, namely the blades and hub walls were given a no slip condition. Symmetry conditions were applied against the side walls of the domain to mimic a system of eight blades whilst only requiring a computation for a single blade. The walls along the top edge of the domain were open, allowing flow to pass through this region if necessary. The mesh used in this study contained 747 099 elements with a maximum wall  $y^+$  value of 10. Due to the relatively high  $y^+$  value on the grid, a wall function approach was used to model the velocity close to the walls with sufficient accuracy. The computation was run until the solution residuals reduced to an RMS value of  $1 \times 10^{-6}$ . Forces on the blades, as well as the inlet and outlet mass flow rate, were also

monitored.



**Figure 6.5:** Single passage grid topology for scaled F7/A7 open rotor. Black arrows indicate the inlet, blue arrows indicate an opening boundary condition.

### 6.3.2 CFD Results

The flow structures present on the OR configuration will provide an improved understanding of the results of the noise prediction performed later. Fig. 6.6 shows the vorticity corresponding to a constant  $Q$ -criterion value. The  $Q$ -criterion is a parameter used to identify vortex flow, and defines a vortex as a connected fluid region with a positive second invariant of  $\nabla u$ . This flow structure shows vortices being shed by the blades. For the given initial conditions the vortices are not convected downstream due to the fact that the advance ratio, the ratio of inflow to blade tip speed, is sufficiently low. The structure of these tip vortices indicates that there is very minimal interaction between blade rows for the configuration. The low convection speed of the surrounding flow is more readily observed in Fig. 6.7, which shows a 2D cut through the computational domain highlighting the velocity through the rotor as a contour plot with streamlines overlayed. The streamlines show a sudden contraction of flow at the inlet, from the tip towards the

hub, and a gradual expansion as flow passes through the rotor, towards the exit. An area of recirculation is observed downstream, indicating the relatively low convection velocities of flow through the rotor.

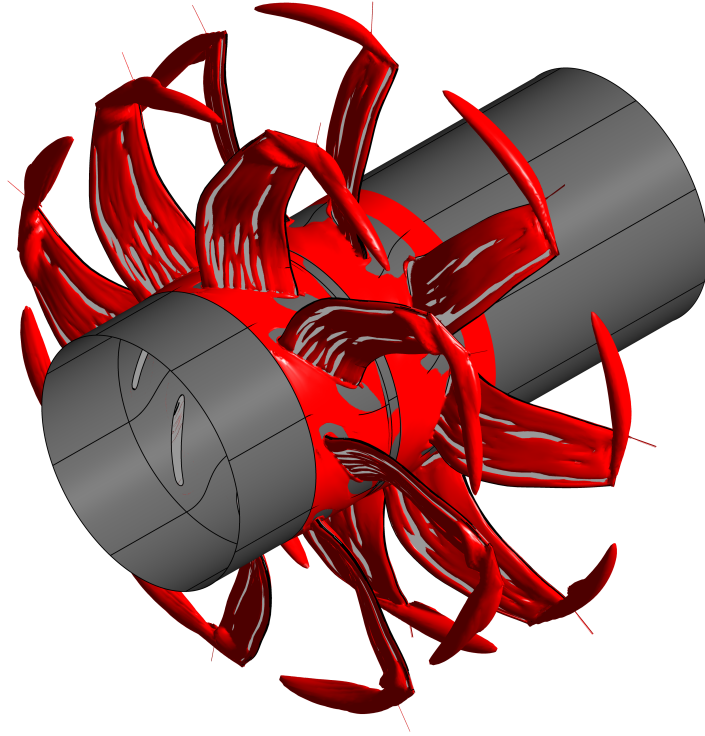
The turbulent kinetic energy at midspan of the blades is plotted in Fig. 6.8. The highest levels of turbulent kinetic energy appear to occur at the leading edge of the front rotor row. This is followed by a region of high turbulence along the entire suction side of the blades. Turbulence convects downstream towards the aft rotor row. The levels of turbulent kinetic energy in the aft rotor row, however, are lower than those in the front row as the front row shields the aft row from any considerable turbulent flow interaction. Traditionally, CROR's have lots of confluent interaction between trailing blade rows, however, due to the low advance ratio of the design, it was seen that for this case the interaction was minimal.

### 6.3.3 Data Extraction and Processing

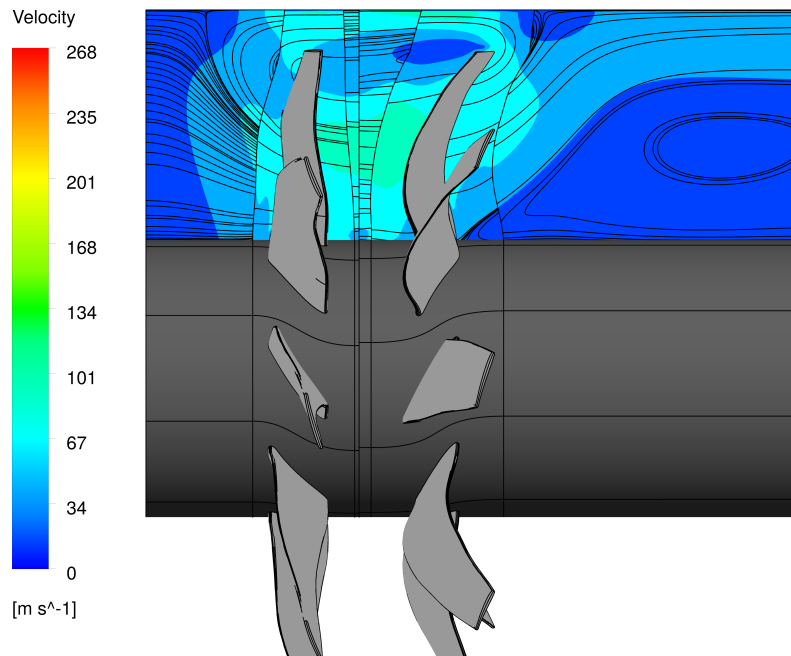
As per the original methodology described in Chapter 3, this approach relies on extraction of the boundary layer velocity height, inflow velocity and inflow turbulence parameters pertaining to a single blade, as it completes a single azimuthal rotation. Extraction surfaces for the inflow and boundary layer parameters were created using a **MATLAB** script that accepts the blade geometry as input, and produces three surfaces - one extending normal to the leading edge at two perpendicular to the trailing edge. The blade geometry was defined as two curves in 3D space that represent the geometry of the leading and trailing edges in space. A script was written to export the result to an STL surface that could be imported into tools such as Tecplot or CFD Post.

Fig. 6.9 shows an example of the data extraction plane imported into CFD Post. A single extraction plane, formed of 6000 equidistant grid points, extends forward from the leading edge of the blade a distance equal to roughly one half the blade mean chord length. The number of data points, and the length of the probe, can be modified, however, it was found in previous studies (chapter 5) that a probe extending up to 20 % of the chord length was sufficient to capture both boundary layer flow as well as leading edge flow. The data points nearest the hub are truncated and, as a result, flow in these regions isn't entirely normal to the blades, resulting in higher numerical values observed at the hub. Furthermore the regions of blade flow nearest the hubs also encounter secondary flow





**Figure 6.6:** Isoline of Q-Criterion of vorticity for scaled F7/A7 blades undergoing rotation.



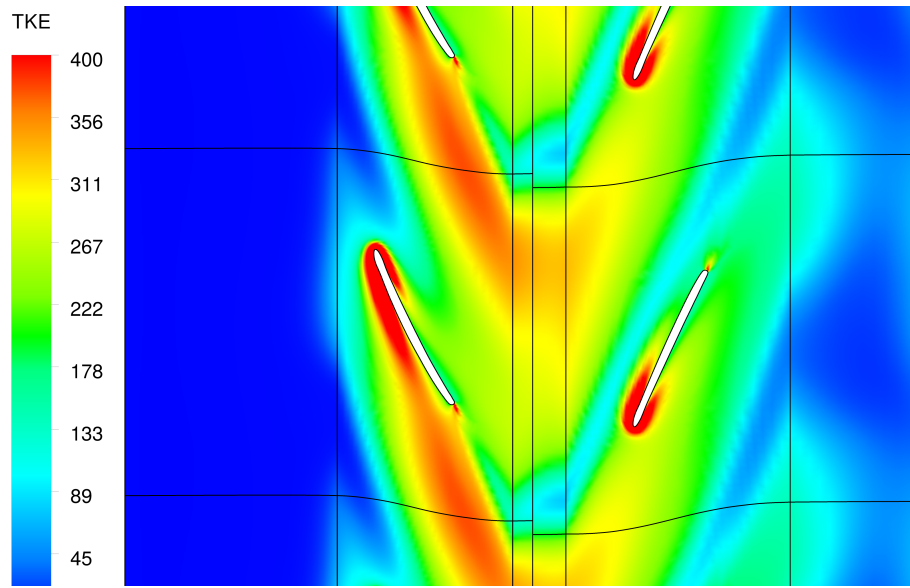
**Figure 6.7:** Streamlines and velocity contours for the given simulation. Flow direction is from left to right.

effects in the form of boundary layer flow, from interactions with the hub wall. In general, the highest velocity should occur at the tips due to the rotating nature of the rotating system.

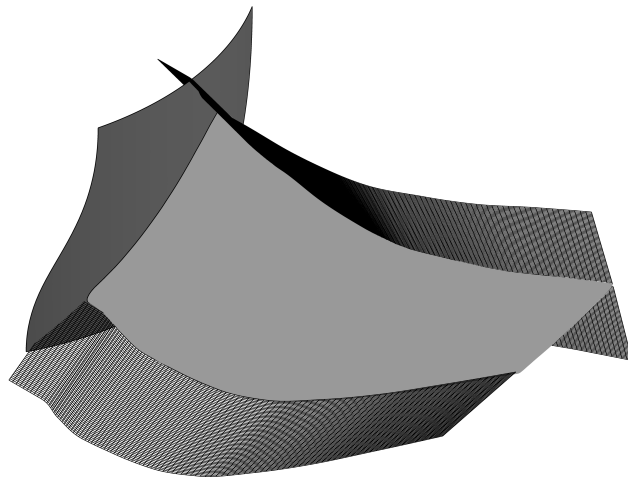
### Extracted Flow Results

Data along the probe was averaged, and data in region close to the airfoil leading edge was excluded to negate stagnation effects. Blade angle of attack (Fig. 6.10b) is relatively high at the extreme ends of the blade, but is well within *normal* ranges for lift producing airfoils within the mid span region. Fig. 6.10a shows that the Mach number along the span of the front blade slightly decreases just before the tip and does not match the design tip Mach number. Apart from this deviation, the Mach number does increase to a value close to the theoretical tip Mach number given in Table 6.1.

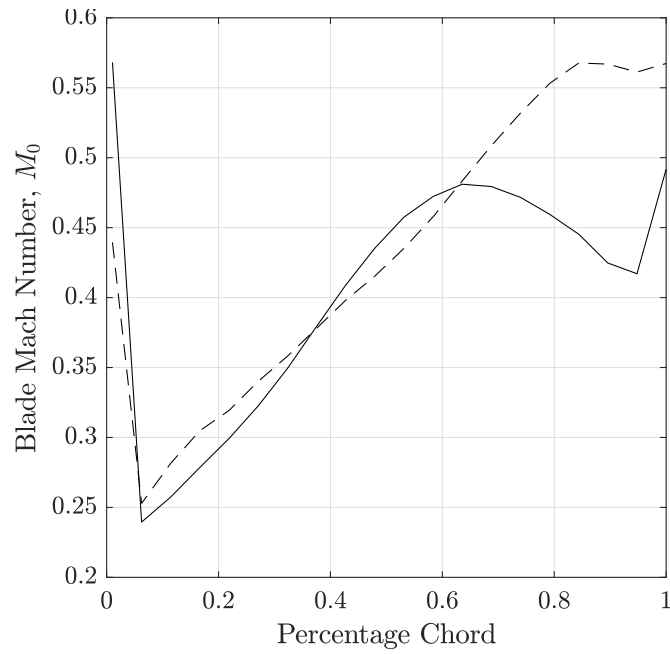
Fig. 6.11a shows the averaged length scale with chord. Length scales are low within the rotor but their distribution shows a maximum near the blade tips. The turbulent velocity (Fig. 6.11b) follows the form of the Mach number relationship increasing away from the hub towards the tip.



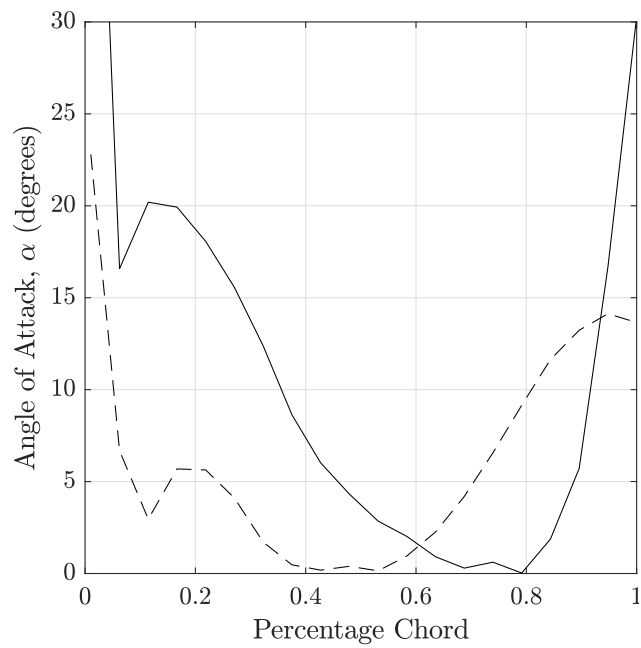
**Figure 6.8:** Turbulent kinetic energy experienced by advancing blades. Flow direction is from left to right.



**Figure 6.9:** Data extraction grid.

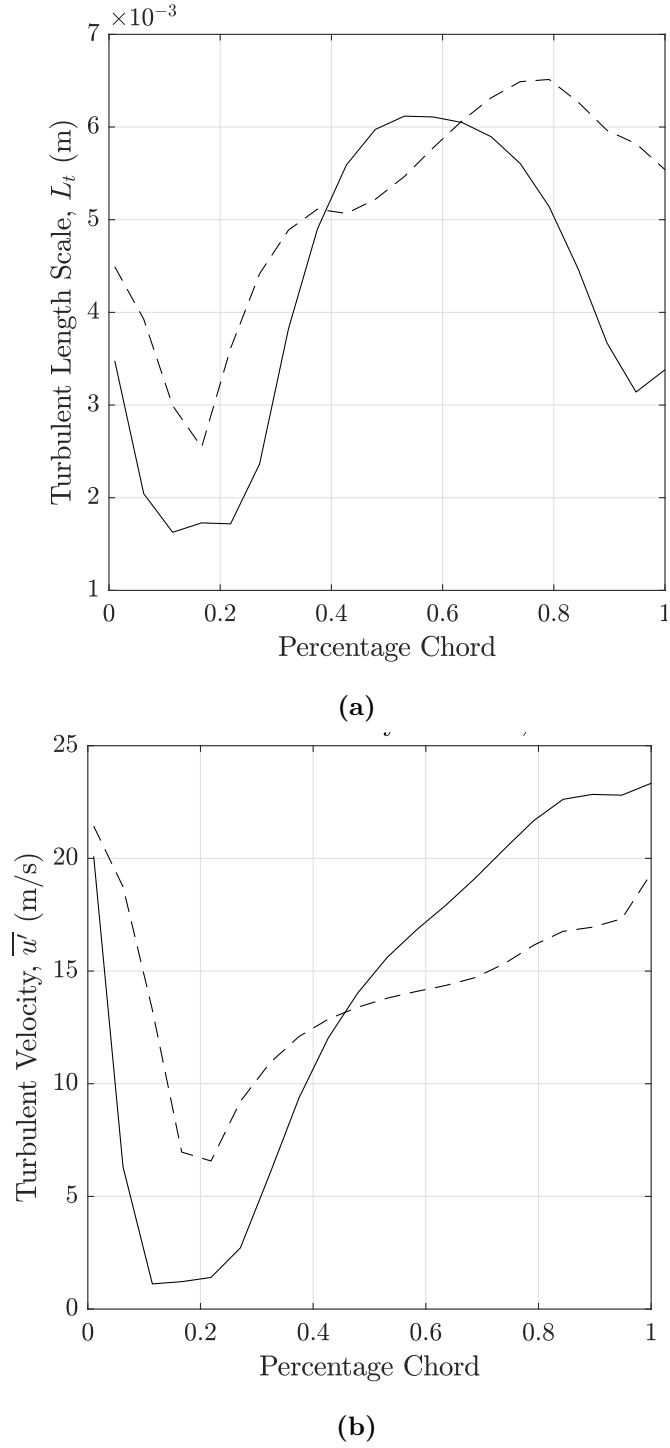


(a)



(b)

**Figure 6.10:** Primary flow parameters at the blade inlet along span (a) Mach Number (b) Angle of Attack — F7 rotor, -- A7 rotor.



**Figure 6.11:** Turbulent parameters at the blade inlet along span  
 (a) Turbulent Length Scale (b) Turbulent Velocity — F7 rotor, -- A7 rotor.

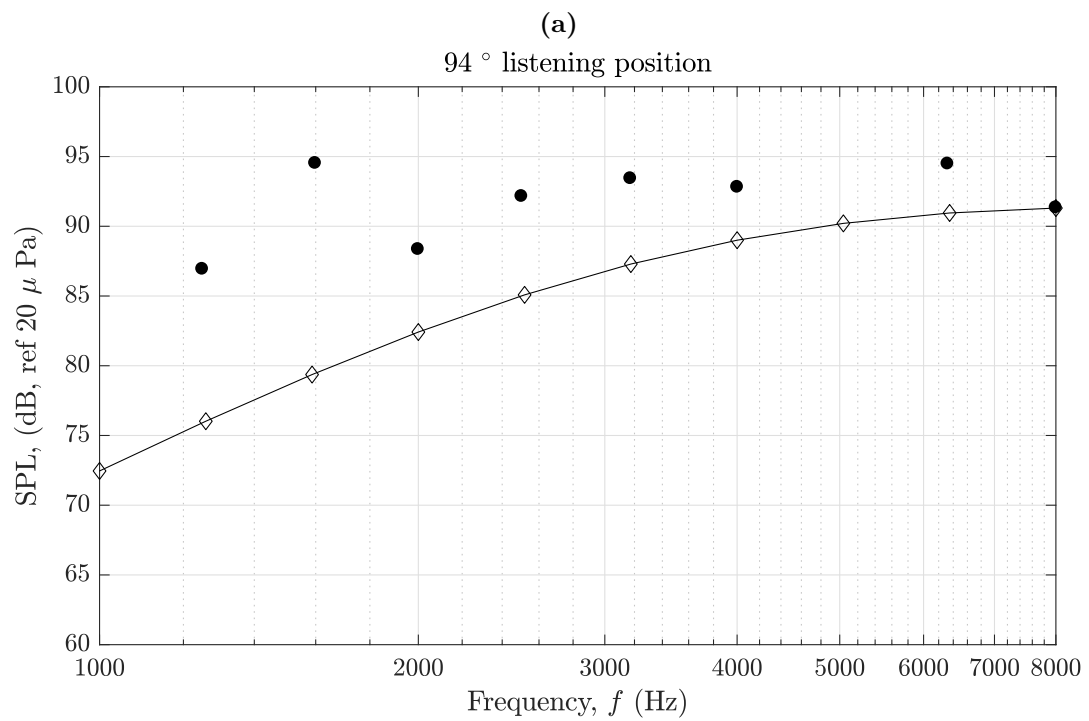
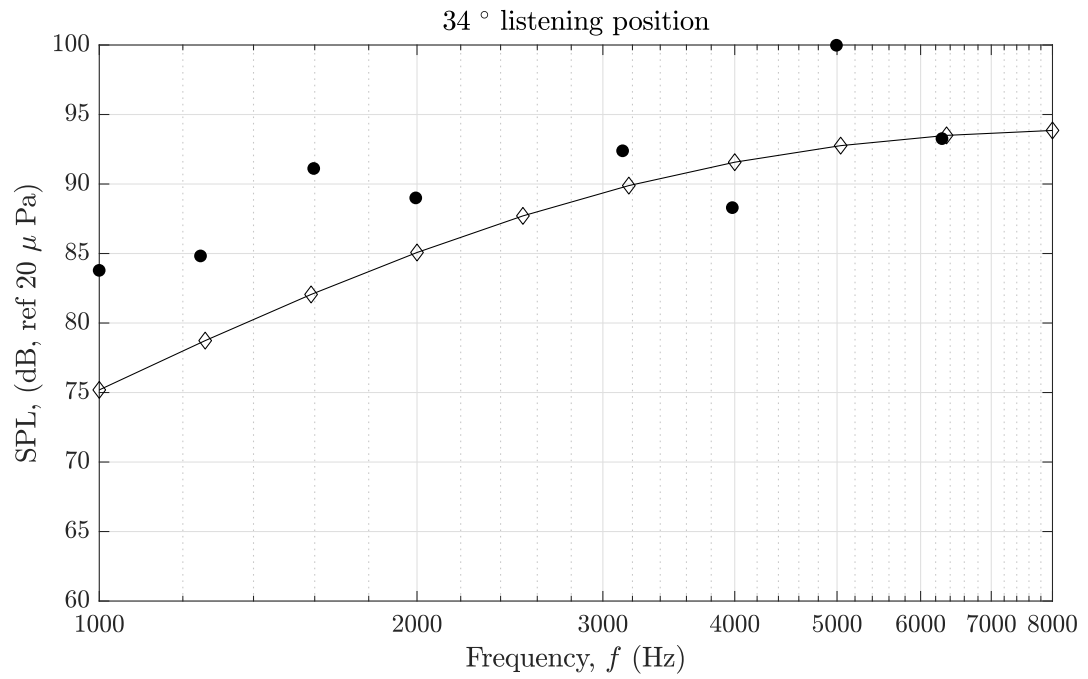
## 6.4 Acoustic Results

Using the CFD data as input, the modified ROBOT code was run for a variety of listener locations to correspond to the experiments of Truong and Papamoschou [144]. The purpose of this analysis was to validate the code against experimental data.

Truong and Papamoschou [144] performed measurements from two sideline angles. Initial measurement data was narrowband in nature with several tonal peaks. The first and second BPF's theoretically occurred at 7 320 Hz and 14 630 Hz respectively, with additional interaction tones occurring at a range of additional frequencies. Additional tonal peaks were also observed at lower frequencies within the measured spectra. In order to compare measurement data with broadband noise predictions, the original signal was binned into one-third octave bands between 1 000 and 8 000 Hz [111]. In the experiments BPF interaction tones are identified and quantified. The 10 and 01 tones exist at around 6 000 and 6 750 Hz respectively. These are the only two tones which exist within the frequency region of interest. Since no detailed broadband measurements were taken, these two tones were considered to provide negligible changes to the one-third octave band spectra, merely increasing the scatter in the higher frequencies in the region of interest with no effect on lower frequency binning. As a result, in order to simplify analysis, no tone deletion was considered. Fig. 6.12 shows the comparison between the overall noise prediction and the one-third octave band measurements from [144]. In these two figures, the predicted overall noise signal shows an increasing trend with audible frequency and, when compared to the measurement spectra, shows accurately predicted trends within the given frequency range. The predicted SPL for both sideline angles matches the measured data within 5 - 10 dB.

### 6.4.1 Directivity

The rotor configuration shows a slightly directional emission pattern. It was possible to capture this trend using the noise prediction code. The measured data showed a louder noise output at the 94° listener location compared to that of the 34.8° location. The OASPL of the directivity predictions is shown in Table 6.2 as compared to available measurement data. Results are unweighted. Predictions assumed that noise would propagate



(b)

**Figure 6.12:** Comparison of overall broadband noise prediction to measurements at measurement positions given in the header. • Measurements,  $\diamond$  F7/A7 noise prediction.

from the emission point directly to the receiver, undisturbed by environmental effects such as the presence of blade rows or experimental equipment - these obstructions possibly becoming sources of attenuation. The table highlights that the tool was able to predict the difference in directional noise components for the given data, thus detailing that the directivity equations for airfoil noise accurately accounted for airfoil shear layers and retarded emission positions for the case of a high speed rotor.

| Sideline Angle         | Measurements | ROBOT <sub>OR</sub> |
|------------------------|--------------|---------------------|
| 94°                    | 103.17       | 97.49               |
| 34.8°                  | 109.48       | 100.06              |
| <b>Difference (dB)</b> | 6.31         | 2.57                |

**Table 6.2:** Third-octave OASPL (dB) for two sideline angles comparing prediction with measurements [144].

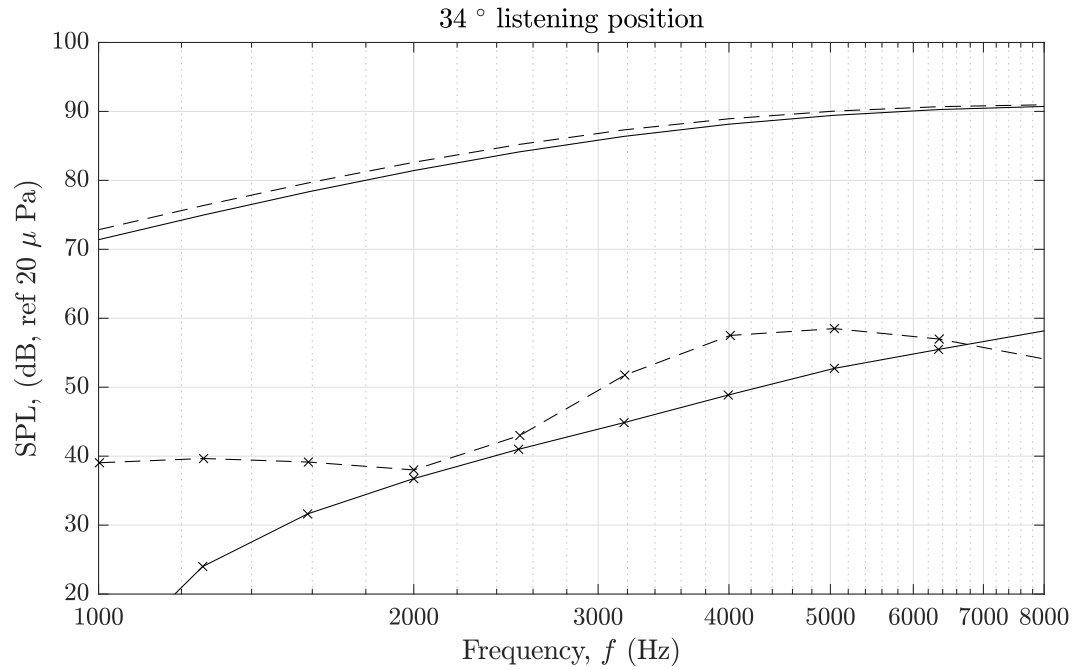
### 6.4.2 Dominant Noise Sources

Figs 6.13a and 6.13b show the SPL of the various predicted noise sources of overall rotor configurations (provided separately for each blade row), in the frequency domain, for a given range. The dominant noise source for the system is seen to be due to inflow-turbulence noise produced by the A7 rotor. TBL noise produced by blade loading is lower than the turbulent noise - consistent with previous studies [103].

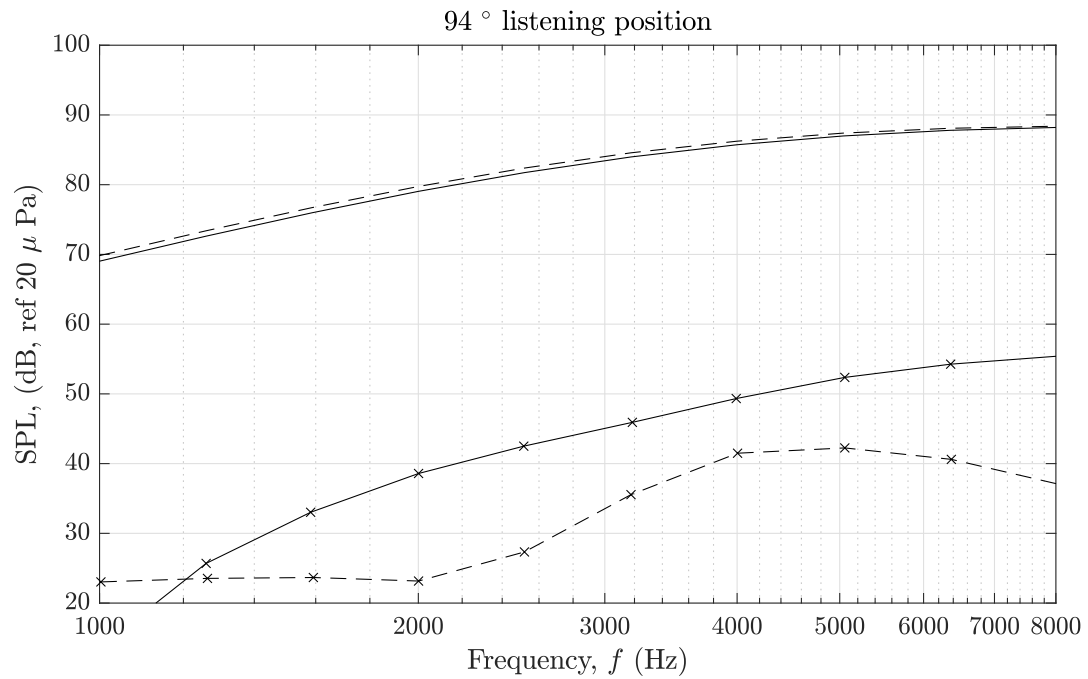
### 6.4.3 Noise Source Localisation

Fig. 6.14 shows plots of the acoustic source strength of each of the rotor rows as seen from the front of the rotor with an observer located at the sideline angle. These plots allow one to determine where the loudest and quietest regions of the rotor are. In this figure the listener is at located at 90° sideline angle, roughly equivalent to being perpendicular to the rotor. The contour plot tracks the emission position of the individual sound sources throughout the rotation of a rotor blade. The scale on all plots is equal and the step size of the colourbar is indicated. Since the results are based on steady CFD calculations, the variation of noise with azimuth, as seen in these plots, is a function of directivity - the





(a) 34.8 degrees sideline angle



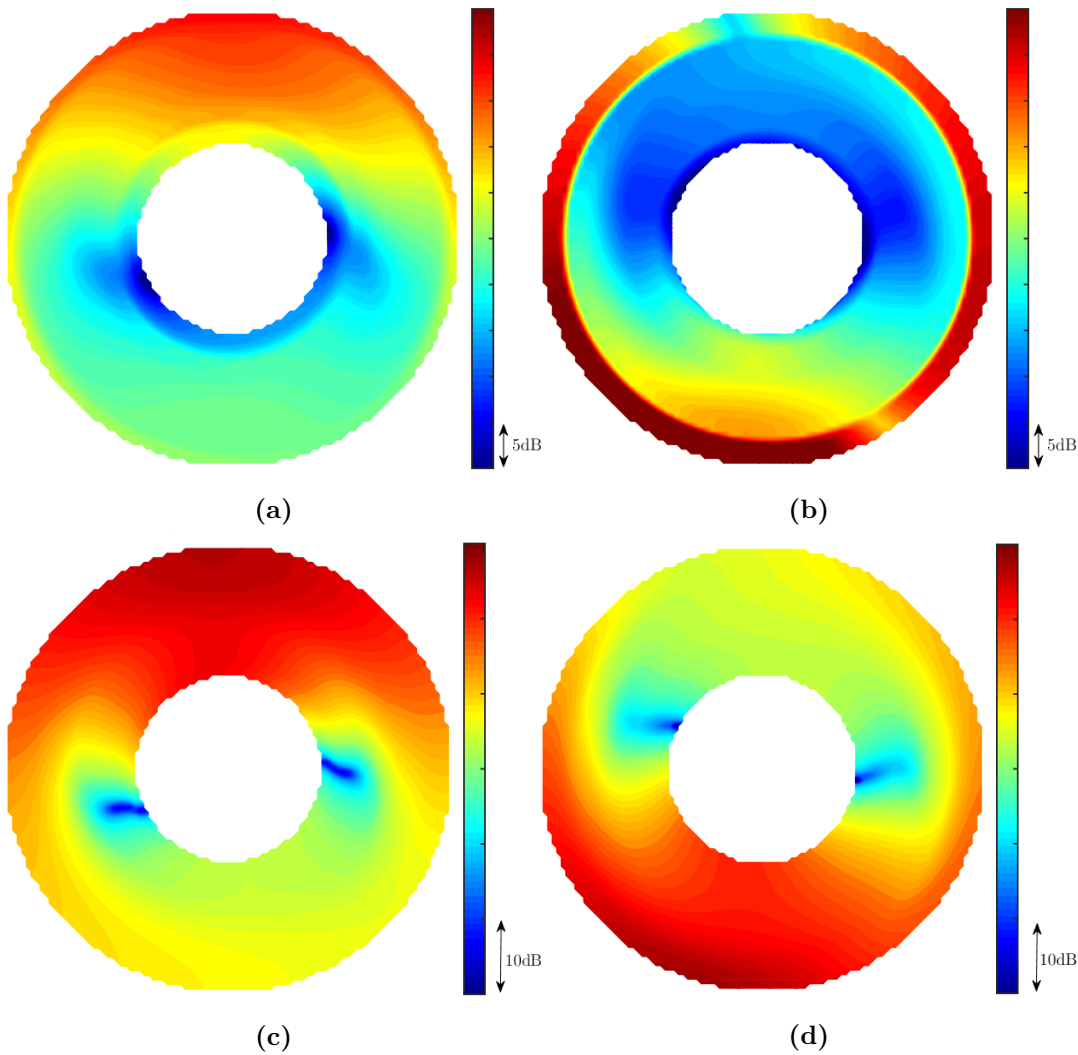
(b) 94 degrees sideline angle

**Figure 6.13:** Comparison of various noise source predictions for the F7/A7 rotor with listener locations provided in the header.

--- A7; Inflow noise, — F7; Inflow noise,  
 -- × -- A7; TBL noise, -×- F7; TBL noise.

angle between the emission location and the observer. In these figures the F7 clockwise rotor rotates and the A7 rotor rotates counter-clockwise.

Due to the vortical effects on the heavily twisted blade geometry, a high region of noise is seen near the tip of the A7 blade. Inflow-turbulence noise contours for the two blades predict similar, albeit mirrored, trends due to the opposing angles and rotation directions of the blades. The loudest regions of the blade occur as they rotate towards the observer.



**Figure 6.14:** Acoustic source strength along the rotor plane with an upstream observer and a  $90^\circ$  sideline listener location. Colour-bars have equal scaling. (a) F7, TBL noise (b) A7, TBL noise (c) F7, Inflow noise (d) A7, Inflow noise.

## 6.5 Summary

This study aimed to validate a hybrid approach for the prediction of broadband noise from a small scale open rotor. A computational study that chained CFD data to the previously discussed ROBOT code was implemented. The validation case selected was a scaled version of the commercial F7/A7 design, for which experimental data was available. The noise prediction module examined the influence of inflow and self-noise sources produced by the front and aft rotors, as they underwent rotation.

A CFD model of the configuration was produced and analysed under boundary conditions matching those of the experimental set up. Due to the low tip speed ratio, there was not sufficient energy in the flow to convect vortices downstream. Some interaction of turbulent kinetic energy was observed. The CFD results confirmed that the flow was behaving as expected, and aerodynamic results were extracted at specified locations.

Simulation results for the noise prediction method showed that inflow-turbulence noise emitted by the rear rotor dominated the overall noise spectra for the machine. The lack of aerodynamic interaction between the two rotor blades seen in the CFD data correlated with the low levels of turbulence produced by the rear rotor which would possibly be higher if there were more interaction between blade rows. Self-noise, produced by the lifting component of the blades, was much lower than inflow noise for both the front and aft rotors.

Predictions from this study were compared to available data [144] and results showed consistent trends for the prediction of directional noise components. Broadband noise was reasonably predicted and matched experiments within 5 - 10 dB.



## Chapter 7

## Conclusions

IN this thesis a method for the prediction of aerodynamically generated broadband noise, emitted by rotating machinery, was presented. This method was shown to predict noise, with modest run times, and demonstrated the capability of determining overall sound levels. The method was also able to resolve source localisation, predict directivity patterns, and run parameter studies to optimise configurations for a range of operating conditions.

A methodology for the prediction of VAWT noise was first considered. Relevant noise sources were discussed, and four noise models were selected to be used in the study. Noise models by Buck et al. [6] and Paterson and Amiet [120] were selected to calculate inflow-turbulence noise, due to their demonstrated success at predicting both wind turbine noise, as well as high speed rotor noise. Self-noise was also considered, and the classical model of Brooks et al. [2] was selected. In order to account for the shortcomings of the BPM model, and to improve noise prediction accuracy, for non-symmetric airfoils in unsteady flow, the iTNO noise model was also implemented. Airfoil noise directivity was modelled to account for noise source radiation patterns. The computational approach was described and all the required simulation input parameters were defined. A quasi-steady approach was used when applying the steady airfoil noise models to the unsteady aerodynamic system, it was assumed that the response time of the acoustic system was faster than that of the aerodynamic system.

Noise models relied on detailed descriptions of the flow and turbulence through the machine and, as a result, flow parameters were determined; either through the use of analytical models, or pre-calculated CFD simulation data. For the study of VAWT noise, an analytical flow model, as well as a CFD based flow model were implemented.

After defining the methodology, analytic flow models were run to perform benchmark analysis of the current status quo for VAWT noise prediction. These models were based on fundamental fluid mechanics principles, as well as analytic or empirical relations for airfoil aerodynamics and flow, and formed the basis of the analytical noise prediction method. The self-noise models were validated against measurements for an individual airfoil case, where both models predicted the peak frequency well. BPM model underpredicted overall levels, whilst the iTNO model slightly overpredicted levels due to the simplified boundary layer model used. Predictions from this study were compared to predictions and mea-

surements by Pearson [90]. Self-noise results matched well, but inflow-turbulence noise was overpredicted. It was understood that the overprediction was due to conflicts in the inflow-turbulence noise modelling procedures used. Compared to experimental results for an existing turbine, overall noise levels were overpredicted by about 10 - 15 dB. It was understood that this overprediction primarily stemmed from inaccurate prediction of the blade inflow velocity, which was modelled geometrically, without any regard for confluent aerodynamic effects.

With the analytical model validated against measurements and computations, a CFD based approach was used to predict noise levels for a new, six-bladed, VAWT designed for the EU FP-7 SWIP project. By using the noise prediction models, in conjunction with improved-accuracy flow solutions, the tool provided alternate results for the prediction of VAWT noise compared to the analytical baseline. Predictions of inflow-turbulence noise were reduced by up to 15 dB when compared to the analytical model. Inflow-turbulence noise was seen to be up to 20 dB louder than self-noise and, for VAWT's operating in urban environments, inflow-turbulence noise will continue to be the primary dominant source. From the results of a parameter study, it was seen that, in turbulent environments, atmospheric inflow turbulence dominates blade generated turbulence at low turbulence intensities, and that a variation of environmental turbulence intensity of 5 - 15 % can affect overall noise levels by up to 5 dB. Based on the CFD studies, turbulent length scales within the rotor were seen to be within the same order of magnitude as the blade chord length, irrespective of the level of inflow turbulence at the inlet. This finding confirmed the suggestion by Pearson [90] that the magnitude of atmospheric length scales reduces within the rotor. One of the key limitations of previous noise prediction models was the lack of high quality input data available [58]. The use of CFD calculations was seen to aid in overcoming this limitation by describing the location and intensity of azimuthally varying velocity and turbulence measures withing the rotor.

The CFD informed modelling procedure for rotor noise was also applied to the prediction of broadband noise produced by a small-scale open rotor engine. The use of CFD data made it possible to consider the non-uniform turbulent flow fields through the rotor, which would otherwise be impossible using analytic modelling. Results compared predictions to one-third octave band measurements, and showed that the approach was able to

predict absolute noise levels within the order of 5 - 10 dB. Trends in directivity patterns were comparable to measurements. The dominant noise source for the configuration was seen to be inflow-turbulence noise arising from the rear rotor. In this particular case it was seen that, unlike in full scale tests, there was minimal turbulent interaction between the two rotor rows due to a low blade advance ratio.

Previous studies on the prediction of broadband noise from a VAWT were unclear as to which type of aerodynamic models worked best for noise predictions [70, 129]. This study has shown that, for the prediction of broadband noise from rotating machinery, particularly where machinery operates in turbulent environments, the use of CFD calculations was a necessary tool to predict the absolute levels of turbulence and its effect on overall noise.

## 7.1 Future Work

The current study looked at the effect of turbulence on noise. A number of extensions to this body of work could further aid in improving the understanding of the sound emission of rotating machinery. Furthermore, some viable extensions to the current computational tools could be performed to improve their usefulness. Some options are discussed:

1. The use of CFD data was seen to improve upon noise prediction results using the current airfoil noise models. The methodology laid out in this study could then be used to aid in understanding the shortcomings of current state-of-the-art low-cost HAWT noise prediction tools. This would be easier to validate than the results of the current study as more noise data are available for HAWT machines.
2. Alternative flow models could be investigated. In the case of VAWT noise predictions; streamtube models should be ruled out due to their historically poor prediction accuracy. However, vortex based models can still provide sufficiently accurate aerodynamic solutions and could be used to inform the existing methodology from this project.
3. The CFD based approach for VAWT noise has not yet been validated against experimental data. As of the time of writing the SWIP V2 was not yet operational.



Provided that the device performs comparably to the CFD models, an experimental measurement campaign would benefit validation of this study.

4. Results of the open rotor noise study were promising. However, by performing unsteady RANS simulations a more accurate prediction of the azimuthally varying noise sources could be determined, improving the overall predictions of this approach.
5. Parameter studies related to the prediction of open rotor noise would be beneficial. In particular, the effect of upstream and downstream pylons on overall broadband noise generation is a pertinent problem.



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# Appendix A

## Additional Inputs to the BPM Airfoil Noise Model

The BPM model, for airfoil self-noise, as described in this thesis, requires several inputs related to the flow Strouhal and Reynolds numbers. These functions are required to define the spectral shape and intensity corrections within the models are known as functions;  $A$ ,  $B$  and  $K$  in Eqs (3.1) - (3.3). These models are described in Brooks et al. [2] in equations (31) - (50) and are reproduced here for completeness.

## Strouhal Numbers

First, several Strouhal numbers are defined, based on experimental data, as follows,

$$St_p = \frac{f\delta_p^*}{U_0}, St_s = \frac{f\delta_s^*}{U_0} \quad (A.1)$$

$$St_1 = 0.02M_0^{-0.6} \quad (A.2)$$

$$\overline{St_1} = [St_1 + St_2] / 2 \quad (A.3)$$

where  $f$  is the given frequency,  $\delta^*$  is the boundary layer displacement thickness,  $U_0$  is the inflow velocity and  $M_0$  the inflow Mach number ( $M_0 = U_0/c_0$ ). The value of  $St_1$  is a function of the effective angle of attack,  $\alpha$ ,

$$St_2 = St_1 \times \begin{cases} 1 & (\alpha < 1.33^\circ) \\ 10^{0.0054(\alpha-1.33)^2} & (1.33^\circ \leq \alpha \leq 12.5^\circ) \\ 4.72 & (12.5^\circ < \alpha) \end{cases} \quad (A.4)$$

## Spectral Shape Functions

The spectral shape is defined by the interpolation of several curves. These are a function of the chord Reynolds number,  $Re$ , and combinations of the Strouhal numbers in the previous section. Two curves exist dependent on which self-noise case (pressure, suction or stall) is being considered.

The first spectral shape function,  $A$  is defined as,

$$A_{\min}(a_1) = \begin{cases} \sqrt{67.552 - 886.788a_1^2} - 8.219 & (a_1 < 0.204) \\ -32.665a_1 + 3.981 & (0.204 \leq a_1 \leq 0.244) \\ -142.795a_1^3 + 103.656a_1^2 - 57.757a_1 + 6.006 & (0.244 < a_1) \end{cases} \quad (\text{A.5})$$

$$A_{\max}(a_1) = \begin{cases} \sqrt{67.552 - 886.788a_1^2} - 8.219 & (a_1 < 0.13) \\ -15.901a_1 + 1.098 & (0.13 \leq a_1 \leq 0.321) \\ -4.669a_1^3 + 3.491a_1^2 - 16.699a_1 + 1.149 & (0.321 < a_1) \end{cases} \quad (\text{A.6})$$

where  $a_1$  is defined as,

$$a_1 = |\log St_i / St_{\text{peak}}| \quad (\text{A.7})$$

where the subscript  $i$  refers to either the *pressure* or *suction* side of the airfoil and  $St_{\text{peak}} = St_1, \overline{St_1}$ , or  $St_2$  depending on what the input to  $A$  in Eqs (3.1) - (3.3) is. The absolute value is used because the spectral shape is assumed to be symmetric around  $a_1 = 0$  [2].

An interpolation constant,  $a_0(\text{Re})$ , is now defined for a location at which the spectrum has a value of -20 dB. This function is given by,

$$a_0(\text{Re}) = \begin{cases} 0.57 & (\text{Re} < 9.52 \times 10^4) \\ (-9.57 \times 10^{-13})(\text{Re} - 8.57 \times 10^5)^2 + 1.13 & (9.52 \times 10^4 \leq \text{Re} \leq 8.57 \times 10^5) \\ 1.13 & (8.57 \times 10^5 < \text{Re}) \end{cases} \quad (\text{A.8})$$

where  $\text{Re}$  is the Reynolds number based on the airfoil chord defined as,

$$\text{Re} = \frac{\rho_0 U_0 c}{\mu} \quad (\text{A.9})$$

where  $\rho_0$  is the fluid density,  $c$  is the chord length of the airfoil and  $\mu$  is the dynamic viscosity of the fluid.

An interpolation factor is determined from,

$$A_R(a_0) = \frac{-20 - A_{\min}(a_0)}{A_{\max}(a_0) - \min(a_0)} \quad (\text{A.10})$$

where the values in the given equation above are spectra evaluated at a value of  $a_0$ . The spectrum shape,  $A$ , can now be evaluated for any frequency as a function of the Strouhal number and corresponding interpolation factor by using,

$$A(a_1) = A_{\min}(a_1) + A_R(a_0)[A_{\max}(a_1) - A_{\min}(a_1)] \quad (\text{A.11})$$

The  $B$  function is calculated in a similar manner using the following approach. Firstly, the spectral shape functions are defined as,

The spectral shape is defined by the interpolation of several curves. These are a function of the chord Reynolds number,  $Re$ , and the effective angle of attack,  $\alpha$ ,

$$B_{\min}(b_1) = \begin{cases} \sqrt{16.888 - 886.788b_1^2} - 4.109 & (b_1 < 0.13) \\ -83.607b_1 + 8.138 & (0.13 \leq b_1 \leq 0.145) \\ -817.810b_1^3 + 355.210b_1^2 - 135.024b_1 + 10.619 & (0.145 < b_1) \end{cases} \quad (\text{A.12})$$

$$B_{\max}(b_1) = \begin{cases} \sqrt{16.888 - 886.788b_1^2} - 4.109 & (b_1 < 0.10) \\ -31.330b_1 + 1.854 & (0.10 \leq b_1 \leq 0.187) \\ -80.541b_1^3 + 44.174b_1^2 - 39.381b_1 + 2.344 & (0.187 < b_1) \end{cases} \quad (\text{A.13})$$

where  $b_1$  is defined as,

$$b_1 = |\log St_s / St_2| \quad (\text{A.14})$$

The spectral shape of  $B$  is defined as,

$$b_0(Re) = \begin{cases} 0.30 & (Re < 9.52 \times 10^4) \\ (-4.48 \times 10^{-13})(Re - 8.57 \times 10^5)^2 + 0.56 & (9.52 \times 10^4 \leq Re \leq 8.57 \times 10^5) \\ 0.56 & (8.57 \times 10^5 < Re) \end{cases} \quad (\text{A.15})$$

where  $Re$  is the Reynolds number based on the airfoil chord. And the interpolation factor is defined as,

$$B_R(b_0) = \frac{-20 - B_{\min}(b_0)}{B_{\max}(b_0) - \min(b_0)} \quad (A.16)$$

and thus the result for use in Eqs (3.1) - (3.3) is,

$$B(b_1) = B_{\min}(b_1) + B_R(b_0)[B_{\min}(b_1) - B_{\min}(b_1)] \quad (A.17)$$

## Amplitude Functions

The amplitude functions,  $K_i$ , provide amplitude corrections as a function of Reynolds number and Mach number.

The function  $K_1$  from Eqs (3.1) - (3.2) is defined as,

$$K_1 = \begin{cases} -4.31 \log Re + 156.3 & (Re < 2.47 \times 10^5) \\ -9.0 \log Re + 181.6 & (Re \leq 2.47 \times 10^5 \leq Re \leq 8.0 \times 10^5) \\ 128.5 & (8.0 \times 10^5 < Re) \end{cases} \quad (A.18)$$

The level adjustment for the pressure-side contribution for non-zero angles of attack is given by,

$$\Delta K_1 = \begin{cases} \alpha[1.43 \log R_{\delta_p^*} - 5.29] & (R_{\delta_p^*} \leq 5000) \\ 0 & (5000 < R_{\delta_p^*}) \end{cases} \quad (A.19)$$

where  $R_{\delta_p^*}$  is the Reynolds number based on the boundary layer displacement thickness on the pressure side.

The amplitude function,  $K_2$ , is defined as,

$$K_2 = K_1 + \begin{cases} -1000 & (\alpha < \gamma_0 - \gamma) \\ \sqrt{\iota^2 - (\iota/\gamma)^2(\alpha - \gamma_0)^2} + \iota_0 & (\gamma_0 - \gamma \leq \alpha \leq \gamma_0 + \gamma) \\ -12 & (\gamma_0 + \gamma < \alpha) \end{cases} \quad (A.20)$$

where,

$$\begin{cases} \gamma = 27.094M_0 + 3.31 & \gamma_0 = 23.43M_0 + 4.651 \\ \iota = 72.65M_0 + 10.74 & \iota_0 = -34.19M_0 - 13.82 \end{cases} \quad (\text{A.21})$$



# Appendix B

## ROBOT User Manual

This appendix provides guidelines for the use of the code ROBOT. The functionality of the noise prediction code includes the following features:

- vertical axis wind turbine definition (rotor blade, airfoil, azimuthal offset)
- operating condition definition (inlet wind speed, rotational speed, inlet turbulence, blade tripping)
- computation of self-noise and inflow-turbulence noise at a defined listener location.
- CFD data link definition (file location, sampling percentage, spanwise sources)
- extraction of velocity boundary layer profiles with azimuth using CFD data
- extraction of inflow turbulence statistics (turbulent velocity, intensity, length scale, turbulent kinetic energy)
- code convergence monitoring
- visualisation of 3D noise localisation spectra (OASPL)
- visualisation of 2D noise localisation spectra (SPL with frequency)
- computation of overall noise at multiple operating conditions
- computation of OASPL directivity
- data visualisation using MATLAB plot libraries.

## Software Limitations

Whilst the code has been developed under the EU FP 7 project, SWIP [132] it is available to the public. As a result this code is provided without warranty. The resulting software is not intended as a professional product and does not offer any guarantee of robustness or accuracy. It is distributed as a personal use application only. Care has been taken to verify results against experimental data and to ensure that a quantified level of accuracy is obtainable when using the code, however, this does not mean that perfect results are always ensured.

One particular limitation is that, when using the *empirically informed prediction method* there is a known error in the prediction of key parameters such as blade in-flow velocity and angle of attack. Resulting in known overpredictions of noise from this analysis.

## Tutorial

In order to run a simulation all that is required is to define a simulation file and then run the code in MATLAB.

### Defining a Simulation

A simulation is defined by setting up a turbine geometry and operating conditions. A sample file with comments is shown below. This file will be used to select the type of analysis that a user would like to perform.

```

1 function [blade,file]=initialise_var()
2
3 %Code Settings
4 blade.c_upower=1; %c_u^n
5 blade.CFD_method=0; %CFD method [OFF]
6 blade.dir=1; %put directivity on
7 blade.tripped=1; %activate tripped BL
8 file.dir1='/home/'; % Install Directory
9
10
11 % PLOTS
12 file.plot_cgt=0; % convergence plot
13 file.plot_dir=0; % directivity (activates multi listeners)
14 file.plot_result=1; %gives SPL vs frequency
15 blade.plot.delta=0; %gives bl params w/azi
16 blade.plot.bl=0; %show 6x BL plots
17 blade.plot.turb=0; %gives L,u',I vs. azi
18 blade.plot.contourPlot=0; %plot 3D localisation
19 blade.plot.spectrum=0; %plot azimuthal localisation
20 blade.plot.vel=0; %plot noise for a range of velocities
21

```

```

22 % Blade Object
23 %geometry
24 blade.span=2;%m
25 blade.n=6; %number of blades.
26 blade.length=0.1833; %(m) (chord)
27 %angle of blade (tip) compared to blade (root)
28 blade.offsetangle=90;%angle from end to end of blades (degrees)
29 blade.pitch=0;%positive is clockwise (degrees)
30 blade.offset=-0.045;%positive is forward wrt centre
31 blade.radius=1;%m
32
33 % OPERATING CONDITIONS
34 blade.speed=20.944; %rotational speed
35 blade.meanflow=8.4 ;% inflow speed
36 blade.len=0.183; %Length Scale
37 blade.intensity=0.2; % Turbulent Intensity
38 blade.observer=[-10.5 0 -11.5]; %distance to listener
39 % coordinates of turbine centre
40 blade.centre.x=+0.15;
41 blade.centre.y=-1;
42 blade.centre.z=1;
43
44 % DISCRETISATION
45 blade.sampling=40; % percent data sampled number of time steps
46 blade.sources=20;%number of noise sources along span (can be vector)
47 blade.ts=500; %number of timesteps in one revolution (to match CFD)
48
49 % CFD DATA
50 blade.azioffset=deg2rad(90); %offset angle for start, measured.
    quantify where the 'zero' data file is located compared to the
    coordinate system zero.
51 % Columns in which required data resides from CFD sim. This tells the
    code which column of data represents which variable
52 file.column.x=2; %x-coordinate
53 file.column.y=3; %y-coordinate
54 file.column.xv=4; %bl data
55 file.column.yv=5;% bl data
56 file.column.k=6; %turbulent kinetic energy

```

---

```

57 file.column.int=7; %turb-intensity
58 file.column.epsilon=8; %turbulent dissipation rate
59 file.column.omega=9; %specific dissipation rate
60
61 %location of data and prefixes
62 file.fluent_data_directory='/home/CFD/';
63 file.suction_side='/suction-';
64 file.pressure_side='/pressure-';
65 file.inflow='/inflow-';
66 %maximum number of files in directory
67 file.maximum=6569;

```

---

## Running a Simulation

After defining the `initialise_var.m` file in the `vawtNoise` directory the simulation can be run by calling `vawtNoise()` in MATLAB.

## CFD Based Predictions

The included sample run file can be used for a CFD informed prediction, lines 48-67 are exclusively for use by the CFD informed prediction methodology.

When using CFD data as input external, data must be located in CSV files that can be read by MATLAB. Data should contain a series of columns representing flow parameters (x coordinate, y coordinate, x velocity, y velocity, turbulent kinetic energy, turbulent intensity, turbulent dissipation rate and specific dissipation rate). There should be one file exported per time step for a single revolution of the device. Files for inflow, pressure and suction sides should be saved into separate directories.

Data files should be appended with integers increasing by 1 per timestep. The user should also know the number of timesteps required for a single revolution (rounded down to the nearest integer). The variable `file.maximum` defines the numbered file corresponding to the final timestep for a single revolution. The number of timesteps is defined by the variable `blade.ts`. If the rotation does not start at zero azimuth then an offset can be quantified using the variable `blade.azioffset`. A user can set this variable to be the positive or negative azimuthal offset from the leading edge in radians.

Lines 52-59 define which column each of the required turbulent parameters resides in. The filereader included with ROBOT has been tested for data files exported from ANSYS Fluent. Lines 62-65 define paths to the directories for each of the data probes.