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**Citation:** Peters, Andreas, and Zoltan S. Spakovszky. "Rotor Interaction Noise in Counter-Rotating Propfan Propulsion Systems." Volume 7: Turbomachinery, Parts A, B, and C (2010).

**As Published:** <http://dx.doi.org/10.1115/GT2010-22554>

**Publisher:** ASME International

**Persistent URL:** <http://hdl.handle.net/1721.1/116187>

**Version:** Final published version: final published article, as it appeared in a journal, conference proceedings, or other formally published context

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GT2010-88) (

## ROTOR INTERACTION NOISE IN COUNTER-ROTATING PROPFAN PROPULSION SYSTEMS

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### ABSTRACT

Due to their inherent noise challenge and potential for significant reductions in fuel burn, counter-rotating propfans (CRPs) are currently being investigated as potential alternatives to high-bypass turbofan engines. This paper introduces an integrated noise and performance assessment methodology for advanced propfan powered aircraft configurations. The approach is based on first principles and combines a coupled aircraft and propulsion system mission and performance analysis tool with 3-D unsteady, full wheel CRP CFD computations and aero-acoustic simulations. Special emphasis is put on computing CRP noise due to interaction tones. The method is capable of dealing with parametric studies and exploring noise reduction technologies. An aircraft performance, weight and balance and mission analysis was first conducted on a candidate CRP powered aircraft configuration. Guided by data available in the literature, a detailed aerodynamic design of a pusher CRP was carried out. Full wheel unsteady 3-D RANS simulations were then used to determine the time varying blade surface pressures and unsteady flow features necessary to define the acoustic source terms. A frequency domain approach based on Goldstein's formulation of the acoustic analogy for moving media and Hanson's single rotor noise method were extended to counter-rotating configurations. The far field noise predictions were compared to measured data of a similar CRP configuration and demonstrated good agreement between the computed and measured interaction tones. The underlying noise mechanisms have previously been described in the literature but, to the authors' knowledge, this is the first time that the individual contributions of front-rotor wake interaction, aft-rotor upstream influence, hub-endwall secondary flows and front-rotor tip-vortices to interaction tone noise are dissected and quantified. Based on this investigation, the CRP was re-designed for reduced noise incorporating a clipped rear-rotor and increased rotor-rotor spacing to reduce upstream influence, tip-vortex,

and wake interaction effects. Maintaining the thrust and propulsive efficiency at takeoff conditions, the noise was calculated for both designs. At the interaction tone frequencies, the re-designed CRP demonstrated an average reduction of 7.25 dB in mean SPL computed over the forward and aft polar angle arcs. On the engine/aircraft system level, the re-designed CRP demonstrated a reduction of 9.2 EPNdB and 8.6 EPNdB at the FAR 36 flyover and sideline observer locations, respectively. The results suggest that advanced open rotor designs can possibly meet Stage 4 noise requirements.

### NOMENCLATURE

$A$	Blade area
$B_1, B_2$	Front-rotor/rear-rotor blade count
$BPF_1, BPF_2$	Front-rotor/rear-rotor blade passing frequency
$c_0$	Speed of sound in ambient fluid
$C_P$	Power coefficient
$C_T$	Thrust coefficient
$D, D_1, D_2$	Average/front-rotor/rear-rotor diameter
$EPNL$	Effective perceived noise level
$f$	Frequency
$F_i$	Force per unit area on blade surface in direction $i$
$G$	Green's function
$h$	Flight altitude
$J$	Advance ratio
$k$	Loading harmonic order
$k_m$	Wavenumber
$m, n$	Harmonic of blade passing frequency
$M$	Flight Mach number
$M_N$	Mach number normal to blade surface
$M_t$	Tip Mach number
$N, N_1, N_2$	Average/front-rotor/rear-rotor shaft speed
$N_{LP}$	Low-pressure spool rotational speed
$OASPL$	Overall sound pressure level
$p'$	Acoustic pressure disturbance
$p_t$	Stagnation pressure

$P_{Tm}, P_{Lm}$	Thickness/loading noise at harmonic $m$
$r_t, r_h$	Tip radius/hub radius
$S$	Amplitude radius
$SPL$	Sound pressure level
$t$	Time
$T$	Time limit for acoustic analogy integrals
$T_1, T_2$	Period of front-rotor/rear-rotor revolution
$V_N$	Velocity normal to blade surface
$\mathbf{x}$	Observer coordinates, $x, y, z$
$x/D_1$	Rotor-rotor axial spacing (defined as distance between front- and rear-rotor stacking lines)
$\mathbf{y}$	Source coordinates, $x_0, y_0, z_0$
$\beta_1, \beta_2$	Front-rotor/rear-rotor blade angle setting at 75 % span, measured from the tangential direction
$\gamma$	Specific heat ratio
$\eta_P$	Propulsive efficiency
$\theta$	Polar directivity angle
$\rho_0$	Free stream density
$\sigma$	Phase radius
$\tau$	Source time
$\varphi$	Blade sweep
$\varphi_S$	Tangential source coordinate
$\varphi_{ref}$	Blade position reference angle
$\varphi_0$	Tangential blade coordinate in source integration
$\Omega$	Rotor rotational speed

## INTRODUCTION AND BACKGROUND

Advanced open rotor designs have the potential to extend the inherent fuel efficiency benefits of conventional turboprop engines to flight Mach numbers of up to 0.8. By recovering the residual swirl downstream of the front rotor, counter-rotating propfan (CRP) concepts can provide an increase of 6 – 8 % in propulsive efficiency compared to single rotor configurations [1-2]. CRPs have been investigated intensively in the 1970s and 1980s and demonstrated significant reductions in fuel burn of up to 30 % compared to high bypass engines of 1980 vintage which are currently deployed on most civil aircraft [3]. Currently, propfans are being extensively studied again due to their potential for reduced environmental impact and their inherent noise challenge.

In order to explore the fuel burn benefits and acoustic performance of CRP aircraft configurations, a multidisciplinary integrated noise and performance assessment capability is required and presented in this paper. Existing methods are used for aircraft mission and engine cycle analysis, noise prediction of engine core and airframe sources, and for the aerodynamic propfan design and performance assessment.

A key aspect of the methodology is the capability to estimate CRP noise. In previous work, various approaches have been undertaken to predict CRP noise and a summary can be found in [4]. Based on his helicoidal surface theory for propellers [5], Hanson developed one of the first analytical models for CRP noise prediction [6]. With the recent advances in numerical methods for aerodynamic and aeroacoustic

analyses, hybrid methods based on coupling CFD (Computational Fluid Dynamics) and CAA (Computational Aeroacoustics) have also been pursued [7-9]. However, CAA methods are expensive in CPU time and memory requirements due to the high mesh densities necessary to accurately resolve acoustic pressure disturbances. Thus, carrying out parametric studies in the CRP design phase using coupled CFD/CAA approaches is generally not yet feasible.

In the present paper, a frequency domain method for CRP noise is developed by extending Hanson's single rotor noise approach [10] to counter-rotating configurations. One of the main advantages is the low computation time requirement allowing the method to be used for detailed parametric studies and the investigation of advanced source noise mitigation concepts.

The theory requires the a priori determination of unsteady blade surface pressures to define the acoustic source terms. In the past, difficulties in obtaining the aerodynamic data have led to inaccurate noise results [10] but the emergence of CFD now provides the capability to estimate the unsteady blade loading. In this paper, high-fidelity full-wheel 3-D RANS computations using the commercially available CFD tool Numeca FINE/Turbo are demonstrated to successfully generate the required surface pressure information.

One of the primary concerns in developing a viable CRP engine design is the noise impact of open rotors, in terms of both in-flight cabin noise and takeoff/approach community noise. At cruise, thickness and loading noise are the key noise sources and the rotor-alone tones dominate the CRP spectrum. At the low-speed conditions, rotor-rotor interaction noise due to aerodynamic interference effects dominates the noise signature as described in Magliozzi et al. [11]. The main focus of the present work is on the computation of CRP interaction tones as they tend to control the radiated noise at the FAR 36 noise certification conditions.

It is assumed in this paper that the mechanisms responsible for the CRP interaction noise can be attributed to the following flow features: (1) rear-rotor upstream influence interacting with the front rotor, (2) tip-vortices shed from the front rotor interfering with the rear rotor, (3) front-rotor viscous wakes affecting the rear-rotor loading, and (4) front-rotor hub wake and hub boundary layer influencing the rear-rotor hub loading [12]. Several CRP noise reduction technologies such as variations in rotor-rotor spacing, rotor tip speed, or rotor blade count [13], as well as reductions in rear-rotor diameter [14] and blade wake management [15] have been explored in the past. In order to evaluate the effectiveness of these noise reduction technologies, the above noise source mechanisms are dissected and quantified for the takeoff condition using the newly developed CRP noise method.

The noise sources are dissected and the analysis serves as the basis for a re-designed CRP with the objective to reduce rotor-rotor interaction noise. For CRPs to be a viable alternative to advanced, high-bypass ratio, low-speed turboprop engine designs their acoustic performance must be improved.

The key question that arises is what noise reduction levels can possibly be achieved for an advanced open rotor design.

**Scope of the Paper.** The overall goal is to define an advanced CRP configuration with improved noise characteristics while maintaining the required aerodynamic performance for a given aircraft mission<sup>1</sup>. Working towards this aim, the objectives are to: (1) dissect and quantify the impact of the mechanisms responsible for interaction tone noise, (2) explore and define necessary CRP noise reduction technologies, and (3) quantify the potential noise reductions on a consistent aircraft mission basis.

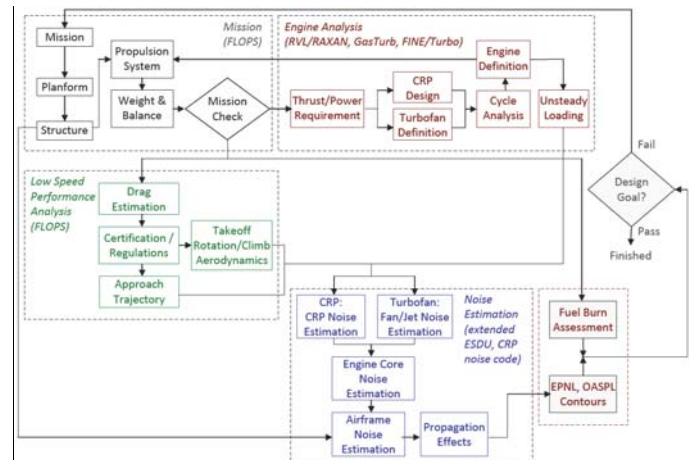
The conceptual framework is outlined first followed by a description of the aircraft configuration used to validate the methods and the definition of the baseline CRP aerodynamic design. The development of the CRP noise estimation method and the required CFD approach are then discussed and implemented for the baseline CRP. Next, the derived CRP noise method is validated by comparing the baseline CRP noise results to measured data available for the same CRP configuration albeit with differences in the exact details of the blade profiles. Then, the noise sources are dissected and quantified. Based on this analysis, noise reduction technologies are devised and implemented in a re-designed CRP configuration. This design is again assessed for noise and the achieved interaction tone reductions are quantified. Finally, the acoustic benefits of advanced source noise mitigation concepts are investigated on the aircraft system level.

## INTEGRATED AIRCRAFT PERFORMANCE AND NOISE ASSESSMENT FRAMEWORK

An overview of the newly established integrated aircraft performance and noise assessment framework is depicted in Figure 1. The overall methodology consists of four major modules and is capable of handling both turbofan and propfan powered aircraft configurations. In the following, a short description of the key modules is presented and more details are given in [16].

In the mission analysis module the airframe, engine type, and engine/airframe integration parameters are specified. The component weights are calculated and a detailed mission performance analysis is performed using NASA's Flight Optimization Software FLOPS [17]. The thrust requirement for the defined mission is input into the engine analysis module, which includes the cycle analysis for the specified turbofan or CRP gas generator using GasTurb [18]. In the case of a CRP powered aircraft, the aerodynamic design of the propfan is carried out using the single and dual rotor vortex lattice methods, Rotor Vortex Lattice (RVL) and Rotor AXisymmetric ANalysis (RAXAN) [19]. In order to determine the time varying blade surface pressures required for the CRP acoustic analysis, full wheel unsteady 3-D RANS simulations of the

counter-rotating stage are performed using Numeca FINE/Turbo [20]. In the low speed performance analysis module, the takeoff and approach trajectories are computed using a combination of a low speed drag polar method [21] and the low speed aerodynamics assessment method included in FLOPS. Iteratively, the engine/aircraft configuration characteristics required to meet the mission constraints are determined.



**Fig. 1: Aerodynamic and acoustic performance assessment framework for counter-rotating propfans.**

The airframe characteristics, engine cycle data, unsteady CRP blade loading, and takeoff and approach trajectories are then used in the noise estimation module. With the newly developed CRP noise estimation method and a combination of analytical and empirical methods (ESDU) [22], noise levels are computed for engine and airframe at the FAR 36 observer locations. The present paper focuses on the computation and assessment of interaction tone noise at takeoff conditions. The overall methodology is employed here to establish a credible baseline CRP design and to ensure that the assessment of an improved design is made on a consistent aircraft mission basis.

## AIRCRAFT CONFIGURATION AND PROPULSION SYSTEM DESIGN

A credible and representative baseline configuration is required to validate the performance and noise assessment methodology. A 737 size, short to medium range twin-engine aircraft with advanced high-bypass turbofan engines was selected. The aircraft seats 150 passengers and has a range capability of 6,480 km (3,500 nm) at cruise Mach number 0.78 and initial cruise altitude capability of 10,670 m (35,000 ft). The takeoff field length requirement is constrained to 1980 m (6,500 ft). The baseline aircraft and datum turbofan engine characteristics with a bypass ratio of 8.9 were defined in collaboration with industry.

Powered by two aft fuselage pylon mounted pusher CRPs, a propfan powered aircraft for the same mission was defined, denoted here as the baseline CRP configuration. The

<sup>1</sup> Certification challenges such as blade containment are acknowledged but not taken into account in the present analysis.

integration of the CRPs led to modifications of the turbofan powered baseline airframe. This included a rearward shift of the main wing and landing gear to meet static stability requirements and fuselage weight penalties due to structural reinforcements and cabin noise insulation. In addition, propfan blades, gearbox, and larger pylons resulted in a 31 % propulsion system weight increase compared to the datum turbofan engine. More detail on the aircraft configurations can be found in [16].

The development of the baseline CRP geometry was guided by data available in the literature for a model scale CRP [23]. Selected configuration characteristics and cruise and takeoff operating condition details<sup>2</sup> are summarized in Table 1.

**Table 1: Model scale baseline CRP characteristics and operating condition parameters (extracted from [23]).**

Configuration Characteristics		Operating Condition		
		Cruise	Takeoff	
$D_1$ [m]	0.56	$M$ [-]	0.78	0.25
$D_2$ [m]	0.56	$h$ [m]	10,670	Sea Level
$B_1$ [-]	10	$N_1$ [rpm]	6,665	6,665
$B_2$ [-]	8	$N_2$ [rpm]	6,665	6,665
$x/D_1$ [-]	0.224	$\beta_1$ [°]	63.5	46.5
$r_h/r_t$ [-]	0.4	$\beta_2$ [°]	63.5	46.5
$\phi_1$ [°]	40	$J$ [-]	3.90	1.43
$\phi_2$ [°]	40	$C_T$ [-]	1.10	1.18

Assuming initial values for adiabatic efficiency, the overall stagnation temperature ratio distribution was first calculated based on the radial distribution of stagnation pressure ratio given in [23]. Assuming that additional mass flow is entrained through the rear rotor, the shaft work split between front and rear rotors was determined based on the given torque split.

Using the Euler turbine equation, the tangential velocity radial distributions for front and rear rotors were calculated. Imposing radial equilibrium and using a simplified actuator disk and control volume analysis, the static pressure radial distributions were computed and integrated to obtain front- and rear-rotor loading and thrust coefficients. The assumed values for adiabatic efficiency and entrained mass flow were iteratively varied until the exit swirl was minimized and the front- and rear-rotor performance agreed with the data in [23].

The analytically computed performance is summarized and compared to measured data in Table 2. The front- and rear-rotor power coefficients  $C_{P,1}$  and  $C_{P,2}$  are determined from the measured shaft power and the known torque split of 45/55 given in [23]. For the operating condition analyzed, the calculated thrust results compare well with the measured performance.

Extracting the axial chord distribution and the stacking line location from [23] and assuming circular arc camber lines and a NACA 65A008 thickness distribution, the blade coordinates

<sup>2</sup> Advance ratio, power coefficient, and thrust coefficient are defined using the average shaft speed  $N = (N_1 + N_2)/2$  and average rotor diameter  $D = (D_1 + D_2)/2$ .

were defined guided by a velocity triangle analysis. Finally, the detailed aerodynamic design and performance investigation was carried out using the single and dual rotor vortex-lattice methods in RVL/RAXAN. However, the vortex-lattice approach does not capture compressibility effects. The analysis was performed at the takeoff condition defined in Table 1 and the detailed aerodynamic design was finalized by varying the blade angle settings and comparing the global performance with measurements summarized in Table 3.

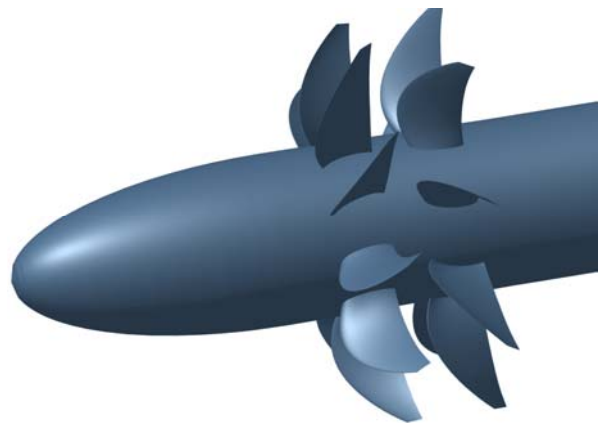
**Table 2: Computed model scale baseline CRP cruise performance compared to measured data.**

	Calculated	Measured (from [23])	Rel. Error
Front-rotor thrust coefficient at $C_{P,1} = 2.32$	0.47	0.48	-2.1 %
Rear-rotor thrust coefficient at $C_{P,2} = 2.61$	0.63	0.62	1.6 %

**Table 3: Model scale CRP performance at takeoff computed in RVL/RAXAN compared to data from [23].**

	Calculated (RVL/RAXAN)	Measured (from [23])
$C_P$	2.79	2.78
$C_T$	1.31	1.18
$\eta_P$	67.1	60.6

For a takeoff blade setting of  $\beta_1 = \beta_2 = 46.5^\circ$ , the total power coefficient calculated with RVL/RAXAN is  $C_P = 2.79$ , in good agreement with the measurements. The detailed blade geometry of the baseline CRP differs from that in [23] and consequently the takeoff aerodynamic performance is improved by  $\Delta\eta_P = 6.5\%$ . The hub geometry is extracted from [23] and the baseline CRP is illustrated in Figure 2.



**Fig. 2: Baseline CRP design.**

The model scale CRP was sized to meet the thrust requirement at takeoff/top-of-climb by maintaining tip Mach number and thrust coefficient and constraining the full scale CRP to equal tip speeds and diameters as in the model scale

design. The resulting full scale baseline CRP characteristics and operating conditions at takeoff are summarized in Table 4.

**Table 4: Full scale baseline CRP configuration and takeoff operating condition parameters.**

Configuration Characteristics		Takeoff Operating Condition	
$D_1$ [m]	3.81	$M$ [-]	0.25
$D_2$ [m]	3.81	$h$ [m]	Sea Level
$B_1$ [-]	10	$N_1$ [rpm]	934
$B_2$ [-]	8	$N_2$ [rpm]	934
$x/D_1$ [-]	0.224	$\beta_1$ [°]	46.5
$r_h/r_i$ [-]	0.4	$\beta_2$ [°]	46.5
$\phi_1$ [°]	40	$J$ [-]	1.43
$\phi_2$ [°]	40	$C_T$ [-]	1.31
		$\eta_P$ [%]	67.1

The baseline CRP gas turbine cycle was based on the datum turbofan. The low-pressure turbine drives the propfan rotors through a gearbox with gear ratio of 8:1 in constant torque split design, similar to the gearbox featured in the 1989 PW/HS/Allison 578-DX propfan propulsion system with a gear ratio of 8.3:1 [24]. The selected gear ratio resulted in a high speed low-pressure spool with  $N_{LP,CRP} = 7,872$  rpm. The datum turbofan low-pressure spool operates at  $N_{LP,fan} = 3,800$  rpm, thus a reduction in engine core size was required for the baseline CRP configuration. Further details of this design can be found in [16].

### CRP NOISE ESTIMATION METHOD

The CRP noise estimation method is based on Goldstein's formulation of the acoustic analogy for moving media [25] and Hanson's frequency domain single rotor noise method [10]. Thickness and loading noise sources are the main sources implemented in the CRP noise estimation method. For thin blades, significant quadrupole noise radiation is a strictly transonic phenomenon as for example reported by Hanson et al. [26]. The CRP blade designs investigated in this work are highly swept and relative tip Mach numbers are below the critical value of 0.85 at the low speed conditions considered here for noise assessment. Therefore, quadrupole sources are currently not accounted for but can be included for cruise noise calculations in the future. In the following, the extension of Hanson's single rotor noise method to counter-rotating configurations is briefly outlined.

The Ffowcs-Williams and Hawkings acoustic analogy formulation generalized for a moving medium can be written as

$$p'(\mathbf{x}, t) = - \int_{-T}^T \int_{A(\tau)} \rho_0 V_N \frac{DG}{D\tau} dA(\mathbf{y}) d\tau + \int_{-T}^T \int_{A(\tau)} F_i \frac{\partial G}{\partial \tau} dA(\mathbf{y}) d\tau \quad (1)$$

where  $p'(\mathbf{x}, t)$  is the acoustic pressure disturbance at observer location  $\mathbf{x} = (x, y, z)^T$  and time  $t$ .

The thickness noise source is described by the first term in Eq. (1).  $V_N$  denotes the normal surface velocity and  $V_N dA d\tau$  is the volume displaced by the surface element  $dA$  in the time increment  $d\tau$ .  $G$  is a Green's function and  $D/D\tau$  is the convective derivative. The loading noise source is given by the second term in Eq. (1) where  $F = (F_r, F_\phi, F_x)^T$  and  $F_i dA$  denotes the force on blade surface element  $dA$  in direction  $i$ .

As described in [10], thickness and loading noise can be calculated independently. Representing the time signal  $p'(\mathbf{x}, t)$  as a Fourier series, the single rotor thickness noise harmonic  $P_{Tm}$  for blade passing frequency harmonic  $m$  at observer location  $\mathbf{x}$  can be expressed as

$$P_{Tm}(\mathbf{x}) = \gamma B \int_A e^{-imB\phi_S} \frac{1}{2\pi} \int_0^{2\pi} M_N \left( M \frac{\partial G_m}{\partial x_0} - ik_m G_m \right) e^{-imB\phi_0} d\phi_0 dA \quad (2)$$

where  $M_N = V_N/c_0$  is the Mach number normal to the blade surface,  $k_m = mBM_t$  is the wavenumber, and  $M_t = \Omega r/c_0$  is the tip Mach number.  $B$  denotes the number of blades. The axial and tangential source coordinates are given by  $x_0$  and  $\phi_S$ , respectively and the Green's function is

$$G_m = \frac{e^{ik_m \sigma}}{4\pi S} \quad (3)$$

with the phase radius  $\sigma$  given by

$$\sigma = \frac{M(x - x_0) + S}{1 - M^2} \quad (4)$$

and the amplitude radius  $S$  written as

$$S = \sqrt{(x - x_0)^2 + (1 - M^2)((y - y_0)^2 + (z - z_0)^2)} \quad (5)$$

Analogous to the thickness noise calculation, the single rotor loading noise harmonic  $P_{Lm}$  can be written as

$$P_{Lm}(\mathbf{x}) = B \int_A e^{-imB\phi_S} \frac{1}{2\pi} \int_0^{2\pi} F_i (\phi_0 - \phi_S) \frac{\partial G_m}{\partial y_i} e^{-imB\phi_0} d\phi_0 dA \quad (6)$$

where the elements of the blade loading  $F_i dA$  are computed using 3-D unsteady RANS simulations and the Green's function derivatives are calculated analytically.



**Extensions to Counter-Rotating Propfans.** The unsteady interaction of the two rotors due to wake, tip-vortex and potential field effects is captured in the aerodynamic calculations. Since the acoustic analogy is based on the coupled aerodynamics, the acoustic interaction is inherently accounted for by carefully superposing the noise fields from the two rotors as suggested by Hanson [6].

Thickness noise is produced at multiples of the blade passing frequency only such that the harmonic order  $m$  takes on all integer values from 1 to  $+\infty$ . In order to account for the CRP inherent interaction tones caused by unsteady blade loading, the formulation for single rotor loading noise in Eq. (6) has to be modified. For the general case of unequal tip speeds and blade counts, the observer will perceive frequencies at

$$f = nBPF_1 + k(BPF_2 - BPF_1), \quad (7)$$

where  $n = 1, 2, \dots, \infty$ ,  $k = 0, 1, \dots, n$ , and  $BPF_{1,2} = B_{1,2}N_{1,2}$  denotes the blade passing frequency of the respective rotor. The value of the sound harmonic  $m$  in Eq. (6) is changed to  $m' = f/BPF$ , where  $BPF = BPF_1$  or  $BPF = BPF_2$ , depending on which rotor loading noise is computed. In contrary to the single rotor case in which each blade experiences identical loading changes and thus generates identical noise signals, it is important to note that in the general case of unequal tip speeds and blade counts, or in the presence of an upstream pylon or angle of attack effects, the individual rotor blades do not necessarily emit identical noise signals. Thus, instead of simply multiplying the source term by the blade number  $B$  in Eq. (6), the noise signals from each blade  $b$  have to be added up while taking into account the phase lags due to the blade position. Implementing these modifications, Eq. (6) becomes

$$P_{Lm,CRP}(\mathbf{x}) = \sum_{b=1}^B e^{-im'B\varphi_{ref}(b)} \int_A e^{-im'B\varphi_s} \frac{1}{2\pi} \int_0^{2\pi} F_{b,i} \frac{\partial G_{m'}}{\partial y_i} e^{-im'B\varphi_0} d\varphi_0 dA, \quad (8)$$

where the reference angle accounting for the blade position is given by

$$\varphi_{ref}(b) = (b-1) \frac{2\pi}{B}. \quad (9)$$

In Eq. (8),  $F_{b,i}$  denotes the force per unit area on the surface of blade  $b$  in direction  $i$  (radial, tangential, axial).

A conceptual outline of the established CRP noise estimation method is depicted in Figure 3. Required inputs are the blade geometries of the two rotors, the CRP configuration details (such as for example rotor-rotor axial spacing), operating condition parameters, and the observer coordinates relative to the CRP. Using the unsteady blade loading data calculated externally (for example 3-D CFD), the thickness and

loading source components are computed. In order to obtain the full CRP noise spectrum, the formulations for thickness and loading noise in Eqs. (2) and (8) are evaluated separately for each rotor. The noise fields are then superposed to determine the CRP narrowband spectrum or acoustic pressure signals.

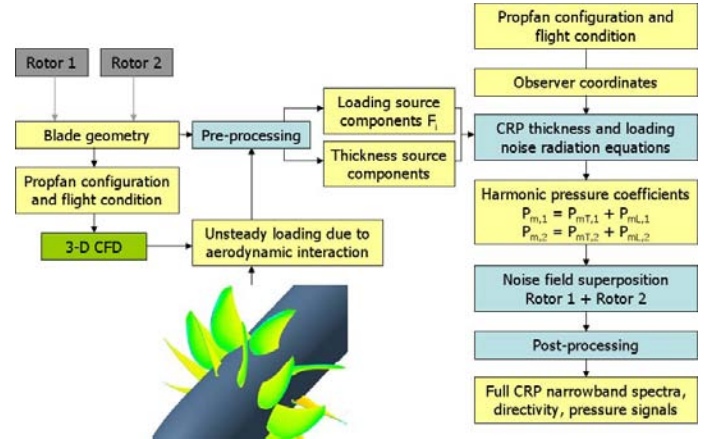
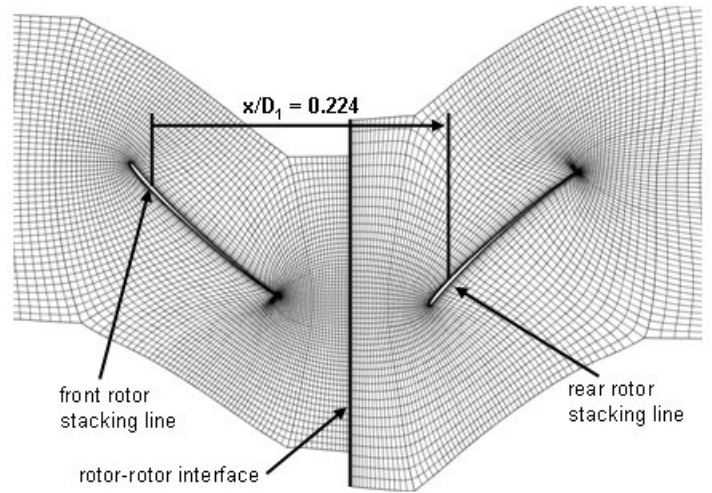
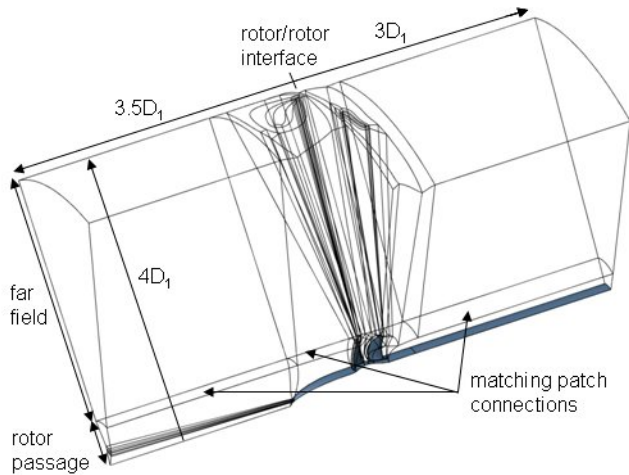


Fig. 3: CRP noise estimation methodology.

### CFD SIMULATION SETUP

3-D unsteady RANS simulations were carried out using the commercially available software package Numeca FINE/Turbo to investigate the aerodynamic interaction between the two rotors and to obtain the time varying blade pressures. Similar to previous studies [7, 9], the eddy viscosity is resolved using the one-equation Spalart-Allmaras turbulence model [27]. Multi-block structured hexahedral grids were used for the baseline and advanced design CRPs. To accurately resolve the front-rotor viscous wakes and tip-vortices, the meshes between the two rotors and around the blade tips were generated with particular care. The blade grid topology (O4H) was extended all the way to the far field boundary to assure a continuous grid and to eliminate all non-matching block patches at the interface between the rotor passage and far field sub-domains. At the interface between the two rotor relative frames, the radial node distribution is continuous. The governing equations are solved in the relative frame leading to high relative Mach numbers near the far field radial boundary. This in turn can induce excessive artificial dissipation leading to non-physical rotational flow in the far field regions. To avoid this, the far field radial boundary was located at  $4D_1$ , far enough from the CRP domain to avoid interference with the capture streamtubes.

The grid-block topology of the baseline CRP single passage grid generated using Numeca's Autogrid 5 is depicted in Figure 4. There are 101 radial grid points in the rotor passage, 85 grid points in the pitchwise direction across the passage, and 121 grid points on the blade suction and pressure surfaces in the chordwise direction. All of the unsteady simulations used to obtain the time-dependent blade loading for the CRP noise calculations were carried out as full-wheel computations featuring 16.5 million cells for the baseline CRP.



**Fig. 4: Baseline CRP grid-block topology (left) and close-up of rotor meshes at mid-span (right).**

In addition to the investigation of different grid topologies, detailed grid convergence studies were conducted by applying FINE/Turbo's multigrid technique and by gradually increasing the grid density between the two rotors and on the blade surfaces. Steady and unsteady blade pressure results as well as wake and tip-vortex resolution were used to determine when grid convergence was reached. The effect of time step size on the unsteady flow solution was studied as well. In general, about 50,000 iterations were required to reach a settled unsteady flow solution. More details on the grid and time step studies can be found in [16].

The time varying pressure data obtained from the CFD calculations is Fourier transformed to determine the loading harmonics required as inputs to the CRP noise estimation method. In the absence of angular inflow or upstream pylon effects, the rear-rotor upstream influence causes the loading on the front rotor to vary at frequencies

$$f_{load,1} = kBPF_2 \left( 1 + \frac{N_1}{N_2} \right), \quad (10)$$

where the loading harmonic  $k = 0, 1, 2, \dots, \infty$ . Similarly, the front-rotor viscous wakes and tip-vortices lead to rear-rotor unsteady loading effects at frequencies

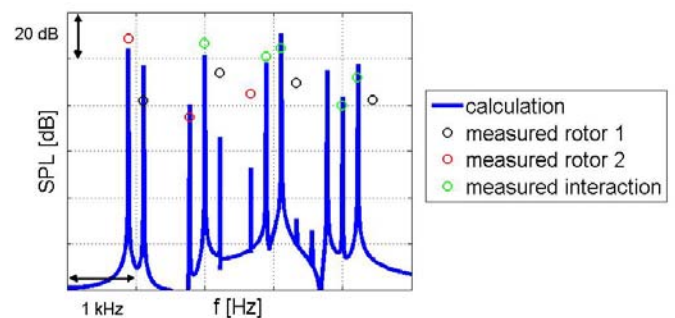
$$f_{load,2} = kBPF_1 \left( 1 + \frac{N_2}{N_1} \right). \quad (11)$$

With the above, the loading waveform is reconstructed as part of the pre-processing in the CRP noise estimation method to determine the loading source components  $F_{b,i}$  in the CRP loading noise calculation described in Eq. (8).

### CRP NOISE METHOD VALIDATION

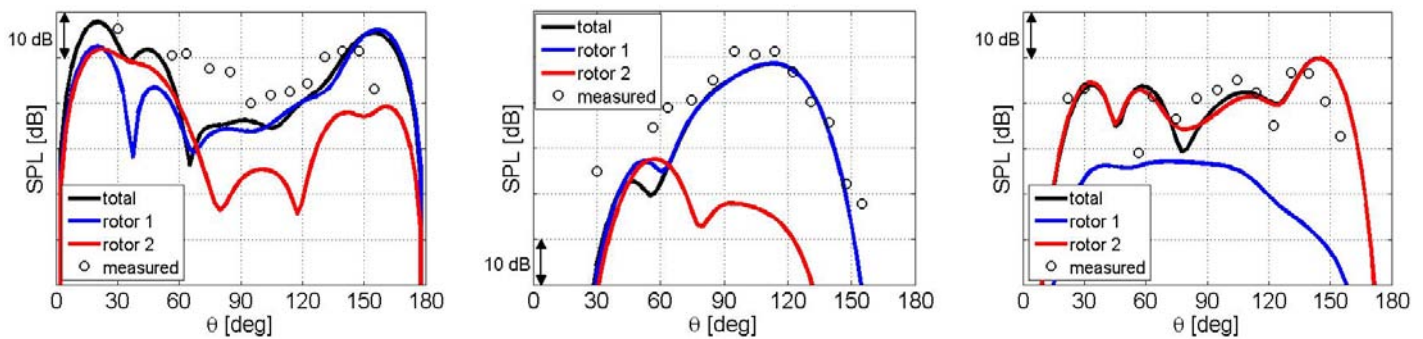
In order to validate the CRP noise estimation method, computed baseline CRP noise results are compared to acoustic measurement data available for the same CRP configuration (operating conditions and overall geometry such as hub-to-tip ratio, rotor-rotor axial spacing, and sweep were identical). However, it is important to note that the details of the blade geometry (camber, thickness, or stacking line distribution) were not available from the literature and are not necessarily the same. In addition, the measurements included a pylon upstream of the CRP whereas uniform inflow was assumed in the computed baseline CRP noise results.

For an axial microphone position with polar angle  $\theta = 85^\circ$ , the narrowband spectrum is given in Figure 5. In general, the first six interaction tones at frequencies  $BPF_1+BPF_2$ ,  $BPF_1+2 \cdot BPF_2$ ,  $2 \cdot BPF_1+BPF_2$ ,  $BPF_1+3 \cdot BPF_2$ ,  $3 \cdot BPF_1+BPF_2$ , and  $2 \cdot BPF_1+2 \cdot BPF_2$  are in good agreement with the measured data marked by the green circles. The measured rotor-alone tones (black and red circles) are strongly influenced by the upstream pylon present in the experiments and therefore show some discrepancies compared to the calculated results, in particular for higher harmonics.



**Fig. 5: Baseline CRP spectrum at  $85^\circ$  polar angle from the inlet centerline.**





**Fig. 6: Baseline CRP interaction tone noise level at frequency  $BPF_1+BPF_2$  (left), at  $2\cdot BPF_1+BPF_2$  (center) and at  $BPF_1+2\cdot BPF_2$  (right).**

Polar directivity results are depicted on the left in Figure 6 for the first interaction tone at frequency  $BPF_1+BPF_2$ . The front-rotor contribution to the interaction tone level is marked in blue and the contribution from the rear rotor is marked in red. The acoustic measurement data is indicated by the circles and the total computed noise is marked by the black line.

The interaction tone noise levels at frequencies  $2\cdot BPF_1+BPF_2$  and  $BPF_1+2\cdot BPF_2$  are shown in the center and right-hand plot of Figure 8, respectively. Overall, there is good agreement between the calculated and measured data. The larger discrepancies at around  $\theta = 75^\circ$  of the  $BPF_1+BPF_2$  interaction tone and at the low polar angle range of the  $2\cdot BPF_1+BPF_2$  interaction tone are most likely due to: (1) the significant influence of non-uniform inflow generated by the upstream pylon as investigated in detail for example by Janardan et al. [13] and Woodward [28] and, (2) the differences in the blade geometric design between the baseline CRP and the experimental model CRP.

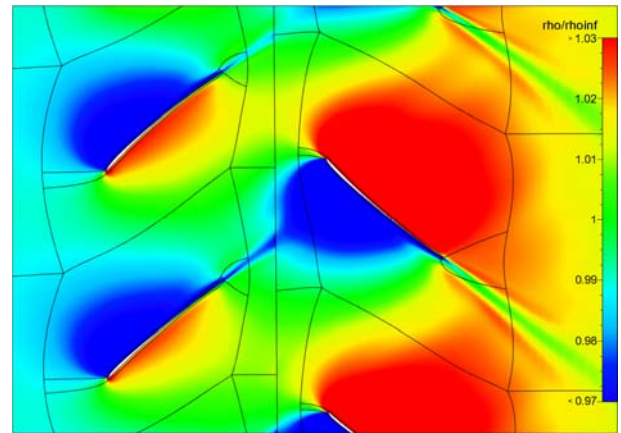
### BASELINE CRP ACOUSTIC ASSESSMENT

Before investigating the interaction tone noise levels in detail, the underlying mechanisms are briefly outlined. The results of the baseline CRP acoustic assessment are discussed next, followed by the description of the CRP re-design for reduced interaction noise and the comparison of the baseline and advanced CRP acoustic and aerodynamic performance results.

**Interaction Tone Noise – Source Mechanisms.** The aerodynamic mechanisms producing CRP interaction noise can be categorized into the four effects described earlier: rear-rotor upstream influence interacting with the front rotor, front-rotor tip-vortices interfering with the rear rotor, front-rotor viscous wakes interacting with the rear rotor, and front-rotor hub wake and hub boundary layer affecting the rear-rotor hub loading. The results given in the following are for takeoff conditions at  $M = 0.25$ .

The rear-rotor potential field directly influences the front-rotor loading and the flow field around the CRP blades for a

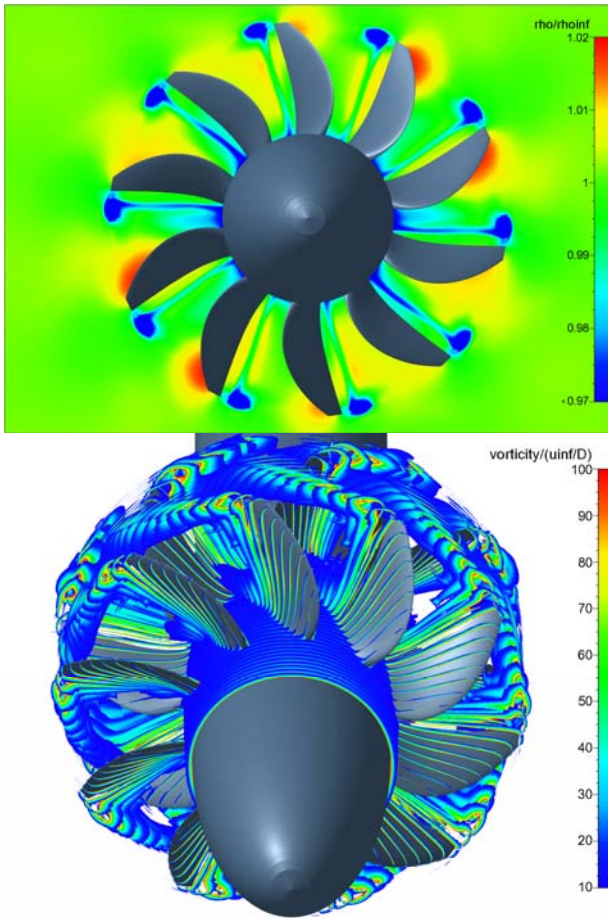
radial cut at mid-span is shown in Figure 7. The baseline CRP is operated at equal tip speeds such that a front-rotor blade interacts  $2B_2 = 16$  times with the potential field of a rear-rotor blade during one revolution.



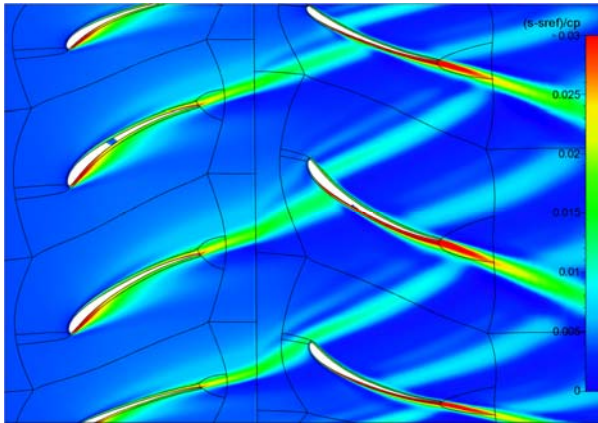
**Fig. 7: Baseline CRP density distribution at mid-span.**

The second noise source mechanism investigated is the interaction of the front-rotor tip-vortices, represented as low density regions on the top in Figure 8. The vortex system is also shown on the bottom highlighting the helical motion and convection of the tip-vortices through the interface between the front- and rear-rotor reference frames and interacting with the rear rotor.

The interactions of the front-rotor wakes and the hub wake and endwall boundary layer with the rear rotor represent the third and fourth noise source mechanisms, respectively. The viscous wakes are depicted in Figure 9 near the hub at 10 % span. Similar to the tip-vortex noise source mechanism, a rear-rotor blade interferes  $2B_1 = 20$  times with the viscous wake during one rotor revolution. The thin secondary wake preceding the blade wake observed in Figure 8 is due the flow separation and reattachment on the blade suction surface near the leading edge of the highly-cambered hub profile.



**Fig. 8: Baseline CRP density distribution at  $x/D_1 = 0.12$  (top) and blade-tip vortex system (bottom): front rotor tip vortices interact with rear rotor.**



**Fig. 9: Baseline CRP entropy distribution near hub (at 10 % span).**

**Dissection of Interaction Tone Noise.** Next, the underlying noise source mechanisms are dissected and their contributions to the interaction tone noise levels are quantified. For example, in order to quantify the relative effect of tip-

vortex interaction at the interaction tone frequency under consideration, the loading source is computed between 75 % and 100 % on the rear rotor only. In this spanwise range, which was determined by investigating the tip-vortex trajectory, it is hypothesized that the tip-vortex interaction mechanism is the dominant contributor to the interaction tone noise. Similarly, the hub wake/endwall boundary layer noise source mechanism is assumed to control the interaction noise generation in the range of 0 % and 12.5 % span, and the viscous wake related mechanism is conjectured to be the dominant contributor in the remaining spanwise range. The influence of the rear-rotor potential field is calculated by accounting for the loading sources on the front rotor only. The noise source dissection approach is summarized in Table 5 below.

**Table 5: Approach to dissecting baseline CRP interaction noise source mechanisms.**

Noise Source Mechanism	Contributing Rotor	Spanwise Range
Upstream influence	Front	0 – 100 %
Tip vortex	Rear	75 – 100 %
Viscous wake	Rear	12.5 – 75 %
Hub wake/endwall BL	Rear	0 – 12.5 %

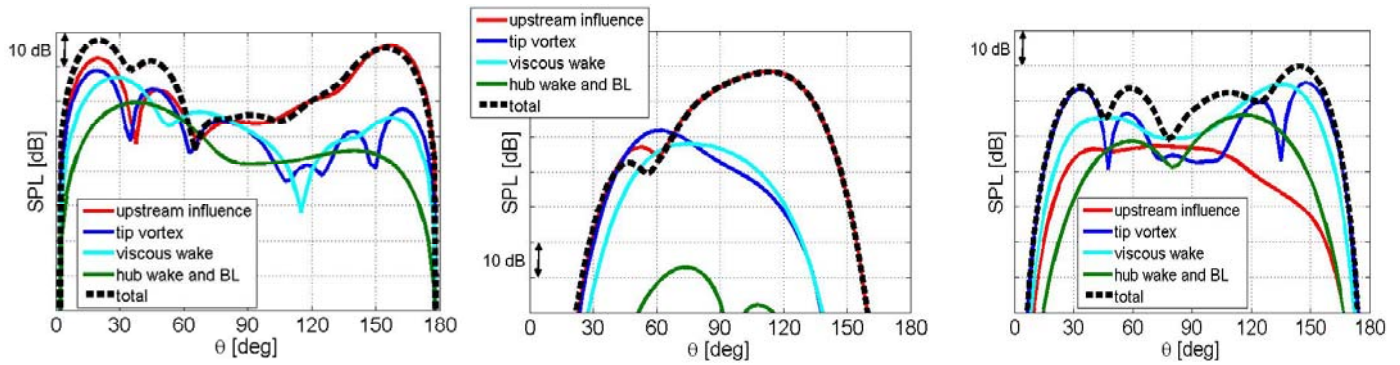
The relative contributions of each of these mechanisms are depicted in Figures 10 by means of interaction tone directivities at frequencies  $BPF_1+BPF_2$ ,  $2 \cdot BPF_1+BPF_2$ , and  $BPF_1+2 \cdot BPF_2$ , respectively.

For the first interaction tone, the noise level is dominated by a combination of upstream influence, tip-vortex and viscous wakes in the forward arc, whereas the upstream influence dominates the noise level in the aft arc. It should be noted that destructive and constructive interference effects can lead to the total CRP noise level falling below the contributions from either the front or the rear rotor as observed for example for the polar angle range between  $60^\circ$  and  $80^\circ$  for the first interaction tone on the left in Figure 10.

Tip-vortex interaction is suggested to control the interaction tone  $2 \cdot BPF_1+BPF_2$  up to a polar angle of  $70^\circ$ . In the aft arc, the potential field interaction dominates as shown in the center plot of Figure 10. Over a wide range of polar angles the interaction tone  $BPF_1+2 \cdot BPF_2$  is again governed by all noise source mechanisms as shown on the right in Figure 10.

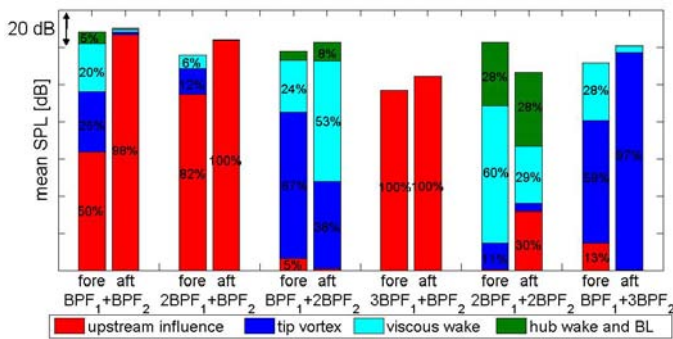
The noise source dissection analysis for the first six interaction tones is summarized in Figure 11. The overall sound pressure levels were computed for the forward and aft arcs respectively and the noise source mechanisms were quantified based on their acoustic pressure contributions to the overall sound pressure levels. In conjunction with the detailed directivity results, this approach allows to directly assess and to prioritize the impact of the different noise source mechanisms. The following observations can be made. (1) As expected, the interaction of rear-rotor upstream influence with the front rotor dominates the interaction tones at multiple frequencies of the front rotor,  $2 \cdot BPF_1+BPF_2$ , and  $3 \cdot BPF_1+BPF_2$ . (2) Similarly, noise from tip-vortex interaction with the rear rotor is more





**Fig. 10: Dissection of CRP noise mechanisms for interaction tones  $BPF_1+BPF_2$  (left),  $2\cdot BPF_1+BPF_2$  (center) and  $BPF_1+2\cdot BPF_2$  (right), baseline CRP,  $M = 0.25$ .**

pronounced in interaction tones at multiple frequencies of the rear rotor,  $BPF_1+2\cdot BPF_2$ , and  $BPF_1+3\cdot BPF_2$ . (3) Interaction tones at equal multiples of rotor frequency,  $BPF_1+BPF_2$  and  $2\cdot BPF_1+2\cdot BPF_2$ , are suggested to be governed by all noise source mechanisms.



**Fig. 11: Baseline CRP noise mechanism contributors to first six interaction tones (percentages based on  $p^2$  averaged over forward and aft polar arcs),  $M = 0.25$ .**

It is important to note the following implication relative to potential noise reduction strategies: although certain interaction tones are dominated by one or another mechanism with possible preferences either in the front or rear arcs, to achieve significant interaction tone noise reductions, *all noise mechanisms* need to be addressed. This is the key objective of the advanced design CRP discussed next.

### ADVANCED CRP DESIGN FOR LOW NOISE

Based on the above baseline CRP acoustic investigation, the CRP was re-designed with the focus on reducing interaction tone noise while maintaining or possibly improving the aerodynamic performance. The following four noise reduction technologies were implemented: (1) increased rotor-rotor axial spacing, (2) reduction of the rear-rotor diameter, (3) differential tip speeds, and (4) blade count variations.

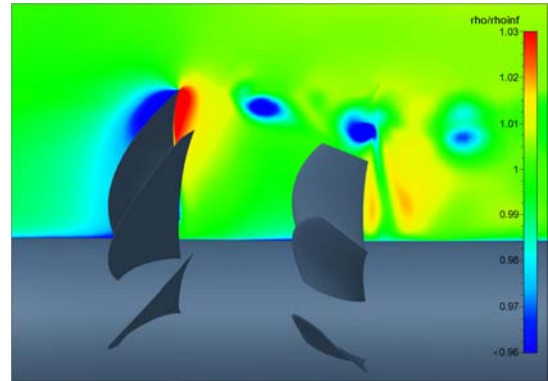
Increasing the axial spacing between the rotor results in an increased decay of the front-rotor viscous wakes and tip-vortices before they interact with the rear rotor. In addition, the strength of the rear-rotor potential field near the front rotor is significantly reduced. Therefore, it is hypothesized that increased rotor-rotor spacing will mitigate several interaction tone mechanisms, in agreement with noise reductions previously reported by for example Janardan et al. [13] and by Woodward et al. [14].

The rear-rotor diameter was also reduced to potentially eliminate the interaction of the front-rotor tip-vortex [12]. The rear rotor was clipped at 75 % span based on a tip-vortex trajectory analysis. For a reduced rear-rotor diameter, the blade loading needs to be increased to maintain the thrust level. This can be achieved either by increasing the blade angle setting or the tip speed, or a combination thereof. In return, however, a higher rear-rotor blade loading leads to an increased upstream influence, which can impair the acoustic benefits of increased rotor-rotor axial spacing. Compared to the baseline CRP design, the thrust level was maintained at the takeoff condition, which is relevant for the noise assessment. In order to limit the loading increase on the rear rotor while maintaining thrust, the number of blades was increased from 8 to 11. A model scale version of the advanced design CRP configuration is used for the acoustic assessment and dissection of noise mechanisms such that the results can be compared to the model scale baseline CRP data on a consistent basis.

Similar to the baseline CRP, the advanced design CRP was also sized to full scale at the takeoff condition. Both designs feature the same front-rotor diameter and tip speed but the rear-rotor characteristics differ as summarized in Tables 4 and 6. Simulating the full scale advanced design CRP in RVL/RAXAN, it was ensured that the thrust level was maintained. The advanced CRP is the result of a first design iteration with the objective to reduce noise. A second design iteration is needed to assess cruise performance and fuel burn levels which could not be carried out due to time constraints.

**Table 6: Full scale advanced design CRP configuration and takeoff operating condition parameters.**

Configuration Characteristics		Takeoff Operating Condition	
$D_1$ [m]	3.81	$M$ [-]	0.78
$D_2$ [m]	3.24	$h$ [m]	10,670
$B_1$ [-]	10	$J$ [-]	4.68
$B_2$ [-]	11	$N_1$ [rpm]	934
$x/D_1$ [-]	0.35	$N_2$ [rpm]	747.2
$r_h/r_t$ [-]	0.4	$\beta_1$ [°]	50.3
		$\beta_2$ [°]	50.5
		$\eta_p$ [%]	67.1



**Fig. 12: Advanced design CRP geometry and near-field density distribution.**

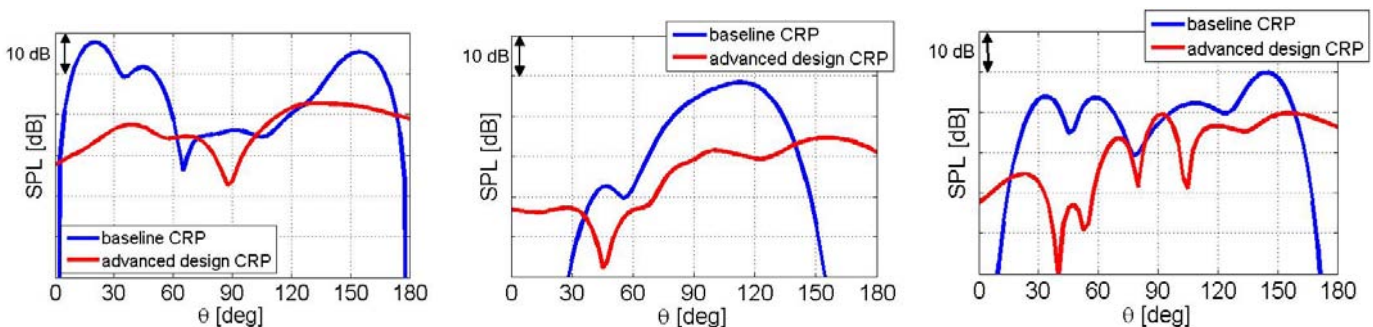
Since the rear-rotor tip speed was reduced for the advanced design CRP, the required planetary gearbox ratio between the low-pressure spool and the rear-rotor shaft increased from 8:1 to 10.1:1. Compared to the baseline CRP, the loading on the front-rotor blades is larger than the rear-rotor load, which is reflected in a reversal of the torque split. The larger torque is transferred through the planetary gearbox carrier. As the carrier driven rotor must be located farthest away from the engine core, the advanced CRP design is suggested to be more suitable for a tractor configuration. A 5 % increase in the propulsion system weight was assumed in the mission and overall performance analysis to account for the increased rotor-rotor spacing and the additional blades. More details can be found in [16].

The CFD simulations necessary for further aerodynamic and acoustic analysis of the advanced CRP design required the generation of a modified full-wheel mesh. Compared to the baseline CRP grid, additional cells were needed in the blocks between the two rotors as the rotor-rotor axial spacing increased. Moreover, clipping the rear rotor required an increase in grid density between the rear-rotor blade tip and the far field sub-domain to accurately resolve the front-rotor tip-vortex in this region. Consequently, the advanced design CRP full-wheel mesh was comprised of 26 million cells. The geometry of the advanced design CRP is presented in Figure 12 along with the near-field density distribution showing the front- and rear-rotor tip vortices as well as the rear-rotor viscous wake.

**Acoustic Performance for Advanced Design CRP.** For the advanced design CRP, the periodicity is  $4T_2 = 5T_1$ , as the tip speed ratio is  $N_1/N_2 = 1.25$ . Therefore, in order to capture all of the loading frequencies, it is necessary to record the surface pressure for every blade over four rear-rotor revolutions (equivalent to five front-rotor revolutions). Due to data processing and CPU time limitations the remaining analysis is based on surface pressures recorded for 1.5 rear-rotor revolutions after reaching quasi-periodic flow conditions.

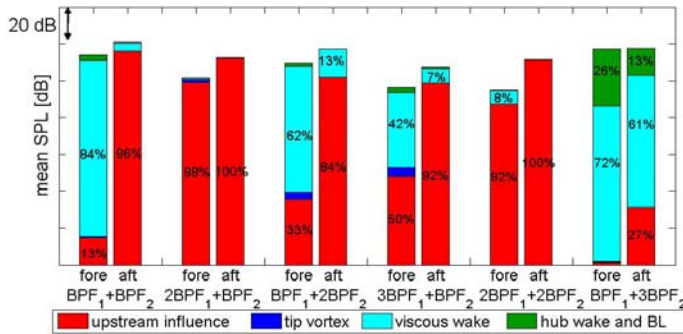
The first three interaction tone directivities for the baseline and advanced design CRPs are compared in Figure 13. For all three interaction tone frequencies, the noise levels are significantly reduced over a wide range of polar directivity angles. The advanced design CRP interaction tone levels do not approach zero at low and high polar angles which is conjectured to be due to the influence of unequal tip speeds. For equal tip speeds, there are substantial destructive superposition effects strongly reducing the noise levels close to the axis of rotation.

The dissected CRP noise mechanisms are presented in Figure 14. Similar to the baseline CRP case, the effects of the noise mechanisms are quantified by computing the loading source terms for a spanwise section only. Based on analyzing the tip-vortex trajectory it is assumed that the acoustic



**Fig. 13: Comparison of baseline and advanced design CRP directivity at interaction tone frequencies  $BPF_1+BPF_2$  (left),  $2 \cdot BPF_1+BPF_2$  (center) and  $BPF_1+2 \cdot BPF_2$  (right),  $M = 0.25$ .**

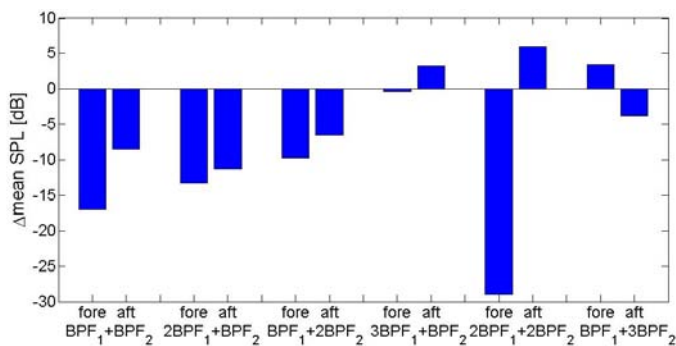
interaction of the front-rotor tip-vortex with the rear-rotor blade tip is limited to 90 % - 100 % of rear-rotor span. The front-rotor viscous wakes affect the rear rotor over the 17 % - 90 % rear-rotor span range, while the front-rotor hub wake influences the rear-rotor loading between 0 % and 17 % span.



**Fig. 14: Advanced design CRP noise mechanism contributors to first six interaction tones (percentages based on  $p^2$  averaged over forward and aft polar arcs),  $M = 0.25$ .**

As expected, clipping the rear-rotor blade results in a significant reduction in tip-vortex interaction as depicted in Figure 14. Viscous wake and upstream influence effects are the dominating noise source mechanisms as both the wake strength and the strength of the potential field interactions are substantially increased due to a higher front-rotor loading.

Overall, the noise levels are greatly reduced, in particular for the first three interaction tones, as summarized in Figure 15. Averaged over all interaction tones investigated, the mean SPL is reduced by 7.25 dB, for the first three interaction tones, the average reduction is 11 dB. Since the interaction tone levels are spread over a larger range of polar angles, the mean SPL actually increases for some of the higher interaction tone frequencies, such as for example in the forward arc of interaction tone at  $BPF_1+3 \cdot BPF_2$ .



**Fig. 15: Relative change in mean SPL for advanced design CRP compared to baseline CRP.**

In summary, the acoustic performance investigations of the baseline and advanced design indicate that, in order to achieve significant noise reductions, it is important to implement noise

reduction technologies that address *all* noise source mechanisms at play as the overall noise is governed by a multiple sources of similar strength. Clipping the rear rotor, increasing the axial spacing, and operating at differential tip speeds are effective approaches to reduce CRP interaction noise. Further increasing the axial spacing is assumed to result in additional acoustic benefits. However, the trade-offs between acoustic and aerodynamic performance need to be carefully investigated as a larger rotor-rotor spacing can increase the propulsion system weight and reduce the amount of swirl recovered by the rear rotor.

## SYSTEM LEVEL NOISE ASSESSMENT

Using the overall integrated performance and noise assessment methodology depicted in Figure 1, the acoustic performance of the baseline and advanced design CRP aircraft arrangements were investigated. For the analysis of the CRP aircraft configurations, CRP, low-pressure compressor, combustor, low-pressure turbine, and airframe noise sources are accounted for. In the case of the turbofan powered aircraft, fan and jet noise are additionally included in the assessment.

Pylon and angle-of-attack effects were not included in the CFD analysis and any aerodynamic interaction of non-uniform inflow with the CRP rotors is not captured. The presence of an upstream pylon or angle-of-attack effects leads to unsteady blade loading at the BPF harmonics which in turn influences the rotor-alone tone noise. As a result, the rotor-alone tones are underestimated in the present analysis. However, the interaction tones generally dominate the CRP noise spectra at low-speed conditions [11]. Thus, underestimating rotor-alone noise is not believed to significantly affect overall CRP noise levels. In addition, the analysis presented here is at FAR 36 flyover and sideline observer locations only. The computed EPNL values at the FAR 36 flyover observer location are tabulated for the three investigated aircraft configurations in Table 7 along with the Stage 4 noise limits<sup>3</sup>. CRP noise was found to be the dominant noise source in both the baseline CRP and advanced design CRP configurations. By implementing advanced source mitigation concepts, the CRP noise was significantly reduced. Since the noise from the remaining engine sources is substantially decreased due to the reduction in core size, the total noise generated by the advanced design CRP powered aircraft was reduced relative to the datum turbofan configuration. Relative to the baseline CRP aircraft arrangement, a total noise reduction of 9.2 EPNdB is suggested by implementing noise reduction technologies.

Overall, the results suggest that the baseline CRP powered aircraft does not reach the Stage 4 noise limits by a considerable margin (2.7 EPNdB). On the other hand, the advanced design CRP was found to meet the Stage 4 noise

<sup>3</sup> Because of the weight penalties due structural reinforcements, cabin insulation and increased propulsion system weight, the maximum takeoff weight of the CRP aircraft arrangements increased relative to the datum turbofan powered aircraft. This in turn led to slightly higher Stage 4 noise limits for the CRP powered aircraft configurations.



restriction with a margin of 6.5 EPNdB. A noise breakdown for the different engine and airframe sources together with details on the system level noise assessment can be found in [16].

**Table 7: Total EPNL in EPNdB at FAR 36 flyover location.**

	<b>Datum Turbofan Aircraft</b>	<b>Baseline CRP Aircraft</b>	<b>Advanced Design CRP Aircraft</b>
Estimated	87.6	94.2	85.0
Stage 4	91.2	91.5	91.5

At the FAR 36 sideline location, the implementation of advanced source mitigation concepts led to a total noise level reduction of 4.2 EPNdB as tabulated in Table 8. Keeping in mind the aforementioned assumptions, the results suggest that Stage 4 noise limits can be met by all three configurations investigated. The margin is smallest for the baseline CRP aircraft (2.7 EPNdB) and largest for the advanced design CRP (11.3 EPNdB).

**Table 8: Total EPNL in EPNdB at FAR 36 sideline location.**

	<b>Datum Turbofan Aircraft</b>	<b>Baseline CRP Aircraft</b>	<b>Advanced Design CRP Aircraft</b>
Estimated	89.7	94.1	85.5
Stage 4	96.6	96.8	96.8

Minimizing the tip-vortex interaction and decreasing the strength of potential field and viscous wake interactions by reducing the rear-rotor diameter, increasing the rotor-rotor spacing demonstrated acoustic benefits of around 9 EPNdB at both flyover and sideline observer locations. These benefits indicate that the advanced design CRP can meet Stage 4 noise restrictions with a margin of 8.9 EPNdB averaged over the flyover and sideline noise certification conditions. It should be noted that a second design iteration should be carried out to further assess the cruise aerodynamic performance of the advanced design CRP.

## SUMMARY AND CONCLUSIONS

An integrated methodology was developed in order to assess the aerodynamic performance and to investigate the noise challenges associated with advanced propfan powered aircraft configurations. The methodology was validated using an advanced turboprop aircraft configuration for a short to medium range mission. A baseline counter-rotating propfan engine was designed based on information available in the literature. The focus of the acoustic performance assessment was to predict the interaction tones which tend to dominate the noise levels at low-speed operating conditions. The individual contributions of front-rotor wake interaction, aft-rotor upstream influence, hub-endwall secondary flows and front-rotor tip-vortices to interaction tone noise were dissected and quantified

for the first time. Furthermore, the analysis demonstrated that all noise source mechanisms need to be addressed in order to achieve significant noise reductions. A re-design of the baseline CRP was carried out with the goal to reduce interaction tone noise. Minimizing the tip-vortex interaction and reducing wake and upstream influence effects by increasing the rotor-rotor spacing and decreasing the rear-rotor diameter yielded significant interaction noise reductions relative to the baseline design (the average interaction tone reduction was 7.25 dB in mean SPL computed over the forward and aft polar angle arcs).

On the aircraft system level, the re-designed CRP demonstrated noise reductions of 9.2 EPNdB and 8.6 EPNdB relative to the baseline CRP aircraft configuration at the FAR 36 flyover and sideline observer locations, respectively. The acoustic assessment suggests that Stage 4 noise limits can possibly be met with advanced open rotor designs.

Future work includes the detailed assessment of the CRP aerodynamic performance at cruise and the investigation of non-uniform inflow, such as for example due to a pylon, which can influence the CRP noise and performance characteristics. Finally, in light of higher relative tip Mach numbers at cruise conditions relevant for cabin noise, the CRP noise estimation method can be extended to account for quadrupole noise sources.

## ACKNOWLEDGMENTS

The authors would like to kindly thank Prof. Mark Drela at MIT for his suggestions and input on the CRP design. At Pratt & Whitney, the support and encouragement of Dr. Bruce Morin, Dr. Wes Lord, and Dr. Jayant Sabnis, and the help of Robert Bengston and Naushir Bala in aircraft and engine cycle assessment are gratefully acknowledged. The authors are also indebted to Dr. Daniel Shannon at United Technologies Research Center for his input on acoustic assessment, and to Roque Lopez and Dr. Alain Demeulenaere at Numeca USA for their assistance with FINE/Turbo. This research was funded by Pratt & Whitney which is gratefully acknowledged.

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