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Autoren

Blech, Christopher; Appel, Christina; Ewert, Roland; Delfs, Jan; Langer, Sabine

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Numerical prediction of passenger cabin noise due to jet noise by an ultra–high–bypass ratio engine

Christopher Blech\textsuperscript{a,∗}, Christina K. Appel\textsuperscript{b}, Roland Ewert\textsuperscript{b}, Jan W. Dels\textsuperscript{b}, Sabine C. Langer\textsuperscript{a}

\textsuperscript{a}Technische Universität Braunschweig, Institute for Engineering Design, Langer Kamp 8, 38106 Braunschweig, Germany
\textsuperscript{b}German Aerospace Center, Institute of Aerodynamics and Flow Technology, Lilienthalplatz 7, 38108 Braunschweig, Germany

Abstract

Within the framework of the Collaborative Research Centre 880, future civil transportation aircraft are investigated. One major aim is a drastic reduction of the noise emission to the ground and an appropriate noise immission into the passenger cabin. The latter forms the focus of this contribution with the aim to ensure an equal or lower noise level in the cabin for new aircraft configurations. Numerical methods are applied to establish a multidisciplinary modelling chain resulting in a prediction of cabin noise due to jet noise by two different engine configurations. An ultra–high–bypass ratio engine is compared to a conventional engine on the basis of a preliminary aircraft design used for both configurations. On the basis of flow calculations in cruise flight as operation point, the hybrid Computational Aeroacoustics solver PIANO combined with the Fast Random Particle Mesh method is applied to compute the pressure fluctuations due to jet noise on the outer skin of the fuselage. These loads are applied to a finite element model considering the structure and the fluid of the aircraft cabin. The acoustic model resolving the structure-borne and airborne sound waves is derived from the design data. The results show a lower sound pressure level induced by the ultra–high–bypass ratio engine in the entire frequency range on the outer skin. Within the cabin, the modern engine is still much quieter, though this fact is not generalisably valid for the entire frequency range as transmission effects of the double wall occur.

Keywords: Aircraft Cabin Noise; Jet Noise; Computational Aeroacoustics; Finite Element Method; Vibroacoustics; Ultra–High–Bypass Ratio engine

1. Introduction

Today’s globalised world is clearly characterised by a great significance of mobility. Beyond certain travel distances, aviation systems have to meet a constantly growing demand of today’s and tomorrow’s society. Within the Collaborative Research Centre 880, future civil transportation aircraft concepts with short take-off and landing characteristics, a low fuel consumption and a drastically reduced noise emission are aimed for. The latter enables efficient point-to-point connections between inner-city airports with a minimal noise pollution of the nearby living environment [1].

Another important aspect besides the noise emission to the ground is the passenger cabin as place of noise immission. An increasing number of passengers per year is consequently leading to more and more people exposed to noise during the actual flight. A high noise level during a long time period or many flights in general may lead to health problems, violate legal requirements or simply yield comfort issues by passengers. In contrast, a reduced cabin noise is expected to provide new possibilities as a pleasant flight or an appropriate work environment. These motivations require a consideration of cabin noise during early design stages of future aircraft. With the introduction of new technologies, an appropriate noise level has to be ensured and higher noise levels has to be clearly excluded by noise level estimations.

An over-the-wing Ultra-High-Bypass Ratio (UHBR) engine with a bypass ratio of 17 is a possible aircraft propulsion system with great take-off characteristics, a low fuel consumption and a highly reduced noise emission in comparison with a conventional engine with a bypass ratio of 5 (BPR5). A modelling chain has been established [2] considering the aeroacoustics of the jet and the sound transmission into the cabin. In order to compare the two aircraft configurations, containing respectively two BPR5 and two UHBR engines with regard to cabin noise during cruise flight, the modelling chain is applied. The research aircraft’s configuration is presented in Sec. 2 while the modelling chain is further divided into three major steps in Sec. 3. As the UHBR engine has a larger dimension and rotates slower compared to the BPR5 engine, the expected noise emission is significantly smaller. However, the Transmission Loss (TL) of a typical fuselage double wall structure is reduced at lower frequencies which are mainly addressed by UHBR engines. Furthermore, due to aircraft/engine integration...
requirements, the engine is mounted closer to the passenger cabin which finally leads to a non-trivial scientific question concerning the quieter engine. The hypothesis to be investigated in this contribution is stated as follows.

A propulsion system comprising Ultra-High-Bypass Ratio engines implies a reduced cabin noise compared to a conventional propulsion system.

Flow simulations solving the Reynolds-averaged Navier–Stokes equations (RANS) of each jet are conducted which serve as basis for a Computational Aeroacoustics (CAA) simulation. The resulting pressure fluctuations on the outer skin are applied as loads in a mechanical model of the aircraft fuselage. The model is solved by use of the Finite Element Method (FEM). It includes the outer skin, circular frames, the cabin floor, insulation material and the cabin fluid. Fluid- and structural domains are strongly coupled in the fuselage model, directly yielding the sound pressure field within the passenger cabin for each configuration. Using this wave-based approach in comparison to an energy-based approach (e.g. Statistical Energy Method) considers the characteristic footprint of the jet on the outer skin and further enables investigations of innovative passive damping measures [3] within the structure of the fuselage under realistic loadings. Energy-based methods are the current state of the art concerning full aircraft NVH-investigations. In literature, wave-based approaches are rarely represented. In [4], cylindrical structures with frames and stringers are investigated by FE models and compared to experiments up to 1 kHz. Similar comparisons are conducted in [5] resulting in a good agreement up to 200 Hz. In both references [4, 5], the insulation and the cabin sidewall panels have been neglected in order to focus the investigation on a validation of the primary structure. A quite similar model compared to this contribution is presented in [6]. Both references share the application which is a propeller aircraft and a weak coupling of the flow region and the aircraft structure. In [6], the recent status of Airbus is shown modelling the entire cabin fluid but only the back of the aircraft. A solution in frequency domain at harmonics of the blade passing frequency is presented in order to investigate different configurations in early design stages. However, a wave-based approach for full aircraft models still results in massive computational cost.

Finally, the model delivers the Sound Pressure Level (SPL) at each seat position enabling a frequency-dependent difference between the two aircraft configurations. On the basis of an auralisation or a simple plot per seat, the systems may be assessed. A more general comparison is conducted using the mean square pressure for the entire passenger cabin. As the models are generic due to preliminary aircraft design data, absolute values must be interpreted in a qualified sense. Rather the SPL differences are meaningful results concerning the question in terms of the quieter engine. It is assumed that the SPL differences are not affected significantly by one of the numerous model assumptions applied in this contribution.

2. Investigated aircraft configurations

A preliminary aircraft design is given within the project. The research aircraft’s mission has a distance of 2000 km, can transport 100 passengers and a total payload of 12000 kg which represents the vision explained above in a technological reference configuration. Both configurations investigated here share the structural dimensions of the fuselage and differ in the applied engine which can be seen in Fig. 1. The conventional BPR5 engine is mounted under the wing while the UHBR engine is mounted over the wing and has a shorter distance to the actual cabin and a different axial position. The potential shielding effect of the wings is not considered within this contribution and will be subject of further studies.

The aircraft’s main properties are given in Tab. 1 – further information on the design process by use of the preliminary aircraft design Code of TU-Braunschweig PrADO can be found in [8]. The UHBR engine has a much greater diameter resulting in a significantly higher bypass ratio of $\mu = 17$ and lower rotation speeds. The
conventional engine operates at a bypass ratio of $\mu = 5$ and rotates at higher speeds. The fuselage has a circular cross-sectional area in the centre with a maximum diameter of 3.510 m. Concerning the fuselage, the design process delivers the thickness distribution of the outer skin, the dimensions of each circular frame, the entire floor design including stiffener and the two bulk heads. Additionally, all material data is given – the entire primary structure is mainly made of Carbon-Fibre-Reinforced Polymer (CRFP).

Table 1: Details on the aircraft fuselage and the two investigated engine configurations.

<table>
<thead>
<tr>
<th></th>
<th>Unit</th>
<th>BPR5</th>
<th>UHBR</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>General</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Aircraft weight</td>
<td>kg</td>
<td>45570</td>
<td>44698</td>
</tr>
<tr>
<td>PAX</td>
<td>-</td>
<td>100</td>
<td></td>
</tr>
<tr>
<td>Mach number at cruise flight</td>
<td>-</td>
<td>0.78</td>
<td></td>
</tr>
<tr>
<td>Range</td>
<td>km</td>
<td>2000</td>
<td></td>
</tr>
<tr>
<td><strong>Engine</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Bypass ratio</td>
<td>-</td>
<td>5</td>
<td>17</td>
</tr>
<tr>
<td>Max. thrust</td>
<td>kN</td>
<td>$2 \times 106.884$</td>
<td>$2 \times 136.145$</td>
</tr>
<tr>
<td>Max. diameter</td>
<td>m</td>
<td>1.464</td>
<td>2.031</td>
</tr>
<tr>
<td><strong>Geometry</strong></td>
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<td></td>
<td></td>
</tr>
<tr>
<td>Max. Length</td>
<td>m</td>
<td>33.176</td>
<td></td>
</tr>
<tr>
<td>Max. Width</td>
<td>m</td>
<td>28.783</td>
<td></td>
</tr>
<tr>
<td>Max. Height</td>
<td>m</td>
<td>9.849</td>
<td></td>
</tr>
<tr>
<td>Max. Diameter</td>
<td>m</td>
<td>3.510</td>
<td></td>
</tr>
</tbody>
</table>

3. Modelling chain for cabin noise prediction

In this contribution, the effect of jet noise on cabin noise at cruise flight conditions is considered only and further noise emitting sources as fan, Turbulent Boundary Layer (TBL) and air-condition systems are neglected. Other sources and especially fan noise will be considered in further studies. However, the process is generally applicable and divided into four main steps in phases A and B as shown in Fig. 2.

Figure 2: Schematic modelling process applying the in-house codes PrADO [8], TAU [9, 10], PIANO/FRPM [11, 12] and elPaSo [13]

The preliminary aircraft design is developed in phase A (Fig. 2 top) and forms the basis for the investigation and is given for this study. The aircraft design fulfils the requirements given within the project (short take-off and landing characteristics, low fuel consumption) – the cabin noise investigation is a subsequent process realised in phase B (Fig. 2 bottom). The applied computational approach for steps 1 and 2 is a hybrid method for
broadband jet noise prediction (Ewert et al.[12, 14, 15]). The three main modelling steps in phase B are as follows:

1. **RANS computation of underlying flow** (Sec. 3.1). Mean flow data and statistical turbulence quantities are calculated for the isolated TBL and the isolated jet, respectively.

2. **CAA modelling of fluctuating sound sources** (Sec. 3.2). Modelling of fluctuating sound sources derived from the Tam & Auriault [16] cross-correlation model and a 3D CAA prediction step using the previously computed fluctuating sound sources are conducted. The turbulence related sources of jet noise are modelled by means of the Fast Random Particle Mesh (FRPM) method. The important refraction effects by the TBL are considered within the CAA perturbation simulation step.

3. **FE modelling of the aircraft’s fuselage** (Sec. 3.3). The time-dependent statistically stationary pressure fluctuations are transposed into the frequency domain by the Fast Fourier Transformation (FFT) and interpolated as pressure loads centrally applied to finite shell elements. By applying the loads obtained on a rigid boundary within the CAA calculation (outer skin of the fuselage), the coupling realised is of weak nature – influences by the structure on the flow are neglected. As result, the sound pressure field is directly given as degree of freedom in the passenger cabin.

**3.1. RANS computation of underlying flow**

In the current case a very well resolved fuselage boundary layer at the fuselage is needed. Therefore, the generic aircraft fuselage is computed as an isolated component. In addition, an isolated RANS meanflow of the jet flow has to be computed. By separating both parts, the geometry is kept very simple, so that the hexahedral mesh part at the fuselage surface is not limited regarding the total height due to wing fuselage transition. The isolated engine allows to employ an axis-symmetric grid for the jet flow.

The RANS computations have been performed with the DLR flow solver TAU (version 2015.2) [9, 10]. It is an unstructured solver, which can also handle structured grids, once converted to an unstructured format. The advantages of structured meshes for aligned flows like boundary layer or jet flows can be kept. The isolated fuselage is meshed with a quad-dominated SOLAR mesh. The boundary layer is resolved with 45 grid points in wall normal direction within the structured part. In total, the fuselage mesh has about 1.85 mio. points. The symmetry plane and the surface mesh is shown in Fig. 3. Additionally the Mach number distribution is depicted. The turbulence modelling is done by means of the RSM-g turbulence model [17] (Reynolds Stress model). The environment conditions are according to cruise conditions, as described in the Collaborative Research Centre reference configuration [1].

![Figure 3: Mesh used for the isolated fuselage and resulting Mach number M.](image)

In addition, an axisymmetric RANS computation of the jet flow is carried out resolving a 15 degree azimuthal slice with a structured mesh segment. This simulation serves as input for the stochastic sound source modelling. Symmetry conditions were applied at the boundaries. The mesh consists of a 2D grid, which was revolved up to the desired angle. The mesh extent is 15D in radial and 30D from the engine outlet in axial direction while D is the engine’s diameter. Turbulence modelling is done by means of the Menter-BSL (Baseline) turbulence model [18]. The geometry of the conventional engine is based on a CFM-56 engine, which is adapted in their dimensions according to the design introduced by the Collaborative Research Centre transport aircraft reference configuration [1]. The thermodynamic cycle data was provided by the software GasTurb of the Institute of Jet propulsion and Turbomachinery of Technische Universität Braunschweig. The design of the UHBR engine was prepared as part in a diploma thesis [19] by means of the codes PrADO, GasTurb and the Noise prediction
tool PANAM [20]. The diameters of the UHBR and the conventional engine bypass duct are 1.78 m and 1.568 m respectively. Both designs are depicted in Fig. 4. The meshes for the engines are prepared with the Gridgen/Pointwise mesh generator and are resolved with about 200,000 grid points per radial-axial plane and 19 points in azimuthal direction.

![Figure 4: a) UHBR and b) conventional nozzle.](image)

3.2. CAA modelling of fluctuating sound sources

The sound pressure fluctuations on the fuselage from both bypass configurations are predicted with a space-time resolving Computational Aeroacoustics (CAA) approach using the structured multi-block (SMB) CAA solver PIANO of the German Aerospace Centre (DLR) [11]. The code utilizes the dispersion relation preserving (DRP) scheme of Tam & Webb [21] for spatial discretisation and a fourth order accurate explicit Runge-Kutta method for time advancement.

The sound propagation is simulated in a three-dimensional volume enclosing main parts of the jet and the considered portion of the fuselage so that the pressure fluctuations on the fuselage are directly resolved in space and time. Further details of the CAA setup will be given in paragraph 3.2.9 below.

The linearised Euler equations (LEE) are used as governing equations to predict the acoustic wave modes over the steady mean-flow at flight conditions from the precursor RANS simulation of the isolated fuselage.

The FRPM module of DLR has been used in parallel with PIANO as a stochastic method to generate the unsteady jet noise source terms on the right-hand side of the linearised Euler equations during run time. The jet mixing noise sources have been modelled in the framework of a primal realisation of the jet mixing noise source model proposed by Tam & Auriault (T&A). The following paragraphs give a brief summary of the applied CAA methodology as far as it concerns its application in this work.

3.2.1. Tam & Auriault jet noise model

The statistical jet noise model of T&A [16] is based on the linearised Euler equations rewritten in cylindrical coordinates as governing acoustic equations. In the genuine method the spreading of the jet flow is omitted assuming a mean-flow parallel to the jet axis. Consequently, the axial, radial, and azimuthal mean-flow velocity components become \( u^0 = u^0(r) \), \( v^0 = v^0 = 0 \), and the mean-flow density is taken as a radial function \( \rho^0(r) \), whereas the mean pressure \( p^0 \) is constant. Furthermore, a source term is introduced that can be expressed in terms of the substantial time derivative of a scalar function \( q_s \) on the left-hand side of the pressure equation,

\[
\rho^0 \left[ \frac{\partial u'}{\partial t} + u_0 \frac{\partial u'}{\partial x} + v' \frac{\partial u'}{\partial r} \right] + \frac{\partial p'}{\partial x} = 0
\]

\[
\rho^0 \left[ \frac{\partial v'}{\partial t} + u_0 \frac{\partial v'}{\partial x} \right] + \frac{\partial p'}{\partial r} = 0
\]

\[
\rho^0 \left[ \frac{\partial w'}{\partial t} + u_0 \frac{\partial w'}{\partial x} + \frac{1}{r} \frac{\partial p'}{\partial \varphi} \right] = 0
\]

\[
\frac{\partial p'}{\partial t} + u_0 \frac{\partial p'}{\partial x} + \frac{\gamma p^0}{r} \left[ \frac{1}{r} \frac{\partial (v' r)}{\partial r} + \frac{1}{r} \frac{\partial w'}{\partial \varphi} + \frac{\partial u'}{\partial x} \right] = \frac{D^0 q_s}{Dt}.
\]

Here \( D^0/Dt := \partial/\partial t + u^0 \cdot \nabla \) denotes the substantial time derivative based on the steady mean-flow velocity from RANS. Next, T&A use the equations adjoint to the frequency domain Fourier transform of Eqs. (1-4) to derive expressions for the free-space vector Green’s functions. These Green’s functions include mean-flow refraction effects. Numerically they are obtained by solving the adjoint equations in the frequency domain with CAA techniques. Finally, the numerically determined values of the Green’s function are used to approximately
solve the integral expression that defines the far-field acoustic spectrum in terms of the noise source space-time correlation

\[ \mathcal{R}_{12}(x_2 - x_1, t_2 - t_1) = \left\langle \frac{D^0 q_s(x_1, t_1)}{Dt_1} \frac{D^0 q_s(x_2, t_2)}{Dt_2} \right\rangle. \]  

(5)

In the above expression, the brackets indicate an ensemble average and \( x_i = (x_i, y_i, z_i)^T \) denotes the spatial coordinates of point \( i \). T&A propose the correlation function Eq. (5) to be described by

\[ \left\langle \frac{D^0 q_s(x_1, t_1)}{Dt_1} \frac{D^0 q_s(x_2, t_2)}{Dt_2} \right\rangle = \frac{q_s^2}{c^2 \tau_s^2} \times \exp \left[ -\frac{|\Delta x|}{u^0 \tau_s} \right] \exp \left[ -\frac{\ln(2)}{\tau_s^2} \left( (\Delta x - u^0 \Delta t)^2 + \Delta y^2 + \Delta z^2 \right) \right]. \]  

(6)

The variables \( \Delta x = x_2 - x_1 \), \( \Delta y = y_2 - y_1 \), and \( \Delta z = z_2 - z_1 \) denote the relative distance between position 1 and position 2, \( u^0 \) indicates the jet convection velocity parallel to the jet axis, and \( \tau_s \) and \( l_s \) indicate a time- and length-scale, respectively. The variance

\[ \hat{R}_{TA} = \frac{q_s^2}{c^2 \tau_s^2} \]  

(7)

is entirely specified by a RANS solution utilizing a two-equation turbulence model. In the T&A [16] model the parameters are specified by

\[ \frac{q_s}{c} = \frac{2}{3} A k^0 \]  

(8)

with \( A = 0.755 \) and \( k \) denotes the turbulent kinetic energy from RANS. The time-scale infers from

\[ \tau_s = c_r \frac{k}{\epsilon}, \]  

(9)

where \( \epsilon \) denotes the dissipation rate from RANS and \( c_r = 0.233 \). The length scale is determined by

\[ l_s = c_l \frac{k^{1/2}}{\epsilon}, \]  

(10)

with \( c_l = 0.256 \). Hence, three free parameters are introduced into the source model. The far-field spectrum derives as

\[ \hat{S}(x, \omega) = \int \int_{V} \int_{-\infty}^{\infty} \hat{p}_s(x_1, x, \omega) \hat{p}_s(x_2, x, \omega) \times \left\langle \frac{D q_s}{Dt} (x_1, t_1) \frac{D q_s}{Dt} (x_1 + r, t_1 + \tau) \right\rangle \exp \left[ i \omega \tau \right] d\tau \, dx_1 \, dr. \]  

(11)

Here, \( \hat{p}_s(x_2, x, \omega) \) indicates the exact spectral Green’s function that includes refraction in the jet shear layer for the angular frequency \( \omega \), as well as source and observer positions \( x_2 \) and \( x \), respectively. The asterisk indicates the conjugate complex. In the T&A approach, the Green’s function is computed numerically for each frequency band and observer position from the adjoint governing equations. Eventually, the far-field spectrum is obtained from the solution of the previous integral using the cross-correlation model Eq. (6).

The source term of T&A is known to represent only the sound generated by the fine-scale turbulence, which especially radiates in directions normal to the jet axis. This was demonstrated in [16], showing very good agreement between measured and predicted spectra for polar angles in the range between 60° and 120°. The approach as discussed above is applicable to cold and mildly hot jet configurations. For hot jets a modification of the time correlation function has been proposed by Tam et al. [22].

### 3.2.2. Stochastic sound source modelling

In some other previous works a primal solution approach for the Tam & Ariault model has been proposed which enables a direct (primal) solution in the time domain by realizing the source term with a stochastic method [23, 14, 15]. It was demonstrated that a primal approach in the time-domain provides results equivalent to the genuine frequency domain method, provided the same two-point space-time source statistics are generated.

A primal approach might have advantages concerning efficiency and regarding its potential to overcome the simplifications introduced in the genuine approach. Especially, an adjoint approach in the frequency domain needs \( n \times m \) computations for \( n \) observer positions and \( m \) frequency bands for the adjoint equations related to the LEE in cylindrical coordinates, Eqs. (1-4), to determine the exact spectral Green’s function used in Eq. (11). A broadband CAA approach to solve the primal LEE in the time-domain, on the contrary, allows to obtain the solution for all \( m \) frequency bands and \( n \) observers with one single computation.

Furthermore, the spreading of the jet flow can be considered. This more complex mean-flow can be considered in the cross-correlation model as well.
3.2.3. Fast Random Particle-Mesh Method based primal approach

In the extended time domain realisation of the T&A jet source model the linearised Euler equations (LEE) are solved together with a source term on the right-hand side of the pressure equation, involving a fully three-dimensional jet-flow with mean variables \((p^0, u^0, \rho^0)^T\) from RANS,

\[
\begin{align*}
\frac{\partial \rho'}{\partial t} + u^0 \cdot \nabla \rho' + \alpha' \cdot \nabla \rho^0 + \rho^0 \nabla \cdot \alpha' + \alpha' \cdot \nabla u^0 &= 0 \\
\frac{\partial u'}{\partial t} + (u^0 \cdot \nabla) u' + (u' \cdot \nabla) u^0 + \frac{\nabla p'}{\rho^0} - \frac{\nabla \rho^0 \alpha'}{\rho^0} &= 0 \\
\frac{\partial \rho'}{\partial t} + u^0 \cdot \nabla p' + u' \cdot \nabla \rho^0 + \gamma \rho^0 \nabla \cdot u^0 &= q_p.
\end{align*}
\]

If restricted to a parallel jet, the above system corresponds to the governing equations as used by T&A , Eqs. (1-4), providing the fluctuating source term is identified with the fine-scale source term,

\[
q_p = \frac{Dq_s}{Dt},
\]

The scalar source term \(q_p\) is realised stochastically with the so called Fast Random Particle-Mesh (FRPM) method, which can be briefly summarised as follows:

- The source term is obtained from the convolution of convective white-noise \(U\) with a Gaussian filter kernel, i.e. for a 3-D problem it reads

\[
q_p(x, t) = \int \hat{A}(x') G(|x - x'|, l_s) U(x', t) \, d^3 x',
\]

with

\[
G(|x - x'|, l_s) := \exp\left(-\frac{2 \ln(2)(x - x')^2}{l^2_s(x')}\right).
\]

- The convective white-noise is generated by means of a Langevin equation in a Lagrangian frame moving with the mean-flow, i.e.,

\[
\frac{D^0}{Dt} U + \frac{1}{\tau_s} U = \sqrt{\frac{T}{\tau_s}} W.
\]

- The term \(W\) in Eq. (16) is a spatio-temporal white-noise source defined by

\[
\langle W(x, t) \rangle = 0, \\
\langle W(x, t) W(x + \Delta x, t + \Delta t) \rangle = \rho^0(x)^{-1} \delta(\Delta x - u^0 \Delta t) \delta(\Delta t).
\]

The scaling function \(\hat{A}\) in Eq. (14) realizes the desired variance of \(q_p\) and is taken as a function of \(x'\). Note, at this stage Eq. (14) with Eq. (16) and white-noise source Eq. (18) does not represent a random process. A solution to the spatio-temporal correlations that result from Eq. (14) can be obtained using the Green function related to the linear equation (16) in combination with the delta correlated white-noise defined by Eq. (18), refer to [12, 24] using standard calculation techniques for distributions [25]. Eventually, together with the definition Eq. (14) an analytical expressions for the two-point cross-correlations of the source term \(q_p\) follows, refer to paragraph 3.2.5.

3.2.4. Numerical discretisation

For the quadrature of the integral in Eq. (14) the computational domain is formally split into equal sized non-overlapping control volumes \(\delta V_k\), continuously covering the resolved source domain without holes. A random particle is assigned to the centre of each control volume \(x_k\). All resulting particles are moving with their local advection velocity at the particle location. The control volumes are bounded by liquid-line surfaces, i.e. the boundary surface is drifting with the flow and the control volume mass \(\delta m_k = \rho^0 \delta V_k\) is invariant over time, refer to the discussion in Ewert et al. [12]. Effectively, the control volume is defined by the inverse of the particle density \(n_p\), i.e.,

\[
\delta V_k = n_p^{-1}.
\]

Random variables are attached to each particle. Their random variates are defined by the integral over the control volume of the stochastic fields at the current time level, i.e.,

\[
r_k^{n+1} := \int_{\delta V_k} \rho^0(x') U(x', t^{n+1}) \, d^3 x'.
\]
Eventually, the convolution Eq. (14) is approximated at discrete time level \( n+1 \) by summation over all control volumes,

\[
q_p^{n+1}(x) \approx \sum_k \frac{\hat{A}(x_k)}{\sqrt{n_p(x_k)}} G(|x-x_k|, l_s) r_k^{n+1}.
\]  

(21)

In the numerical approach, only random variables \( r_k \) are realised and the convective white-noise field \( U \) is only used for the derivation of a discretised random process to generate the actual variates of each random variable. From Eq. (20) together with Eq. (16) one Langevin equation for each random variate derives [12], whose simple first-order discretisation reads [26]

\[
r_k^{n+1} = \left(1 - \frac{\Delta t}{\tau_s}\right) r_k^n + \left(\frac{2 \Delta t}{\tau_s}\right) \sigma_k^n,
\]  

(22)

where \( \sigma_k^n \) are mutually uncorrelated standardised Gaussian random variates, which are independent of themselves at different times \( \langle \sigma_k^n \rangle = 0, \langle \sigma_k^n \sigma_k^m \rangle = \delta_{kl}\delta_{nm} \), and which are independent of \( r_k^n \) at past times \( e.g., \langle r_k^n \sigma_k^m \rangle = 0 \) for \( t^m \leq t^n \). The time increment between time levels \( t^{n+1} \) and \( t^n \) is indicated by the CAA time step \( \Delta t < \tau_s \).

A modification of the time decay model based on a double Langevin procedure has been proposed in [27] to realize the hot-jet extension as introduced by Tam et al. in [22]. Different numerical methods have been applied and discussed in previous work that differ in detail concerning the realisation of particle convection and in the realisation of the discrete filter step Eq. (21) exploiting the properties of a Gaussian filter Eq. (15) to be separable in space.

In the 2D Random Particle-Mesh (RPM) method, [28], streamtraces are pre-computed from an initial seeding position. The size of the seeding area and the downstream extension of the streamtraces specifies the domain where random sources are generated. During run time particles are convected along the streamtraces. The filter step is split into an Gaussian filter applied along each streamtrace, followed by a second filter step that distributes pre-filtered values from the streamtrace to the CAA mesh points. The approach has been extended for the description of 3D jet noise using an azimuthal decomposition to the 3D stochastic source term that yields quasi 2D stochastic realisations for each azimuthal mode order, refer to [23, 14]. This approach will be hereafter referred to as RPM-modal.

In the framework of the Fast Random Particle-Mesh (FRPM) method an auxiliary Cartesian mesh is utilized for the generation of the unsteady sources [29, 12]. The particle convection is computed on this mesh similar to particle-in-cell (PIC) methods. The filter step is accomplished by a sequence of 1D-filter steps applying a Gaussian filter along each coordinate direction. High-efficient recursive filters from signal processing are used. Details about the seeding of particles can be found in [12]. Both discretisation techniques are implemented into the FRPM source module developed at DLR that is executed in parallel to the PIANO code. In the following the notion (F)RPM is used to indicate that the discussion holds for both discretisation methods.

3.2.5. (F)RPM cross-correlation model

Following the procedure as outlined in paragraph 3.2.3, straight forward calculation provides the 3D cross-correlation,

\[
\langle q_p(x, t) q_p(x + \Delta x, t + \Delta t) \rangle = \hat{R}_{FRPM} \exp \left[ -\frac{|\Delta t|}{\tau_s} \right] \exp \left[ -\frac{\ln(2)}{l_s^2} \left( (\Delta x - u_j \Delta t)^2 + \Delta y^2 + \Delta z^2 \right) \right],
\]  

(23)

refer to the more detailed discussion references [12]. This cross-correlation model vastly corresponds with Eq. (6). Using in Eq. (14), a scaling

\[
\hat{A} = \left( \frac{4 \ln(2)}{\pi} \right)^{3/2} \sqrt{\frac{\hat{R}_{TA}}{l_s^2}}
\]  

(24)

with \( \hat{R}_{TA} \) from Eq. (7), the resulting variance in Eq. (23) corresponds with that of the T&A model, i.e., \( \hat{R}_{FRPM} = \hat{R}_{TA} \).

3.2.6. (F)RPM cross-correlation model vs. T&A model

Both cross-correlation function are similar despite the first exponential function governing turbulent decay at time scale \( \tau_s \), where \( |\xi|/u^0 \) is used in Eq. (6) instead of \( |\xi| \) in Eq. (23). The cross-correlation map for both functions is shown in Fig. 5 showing the non-dimensional spatial x-separation \( \frac{\Delta x}{u_j^{1/2}} \) on the horizontal axis (with \( \Delta y = \Delta z = 0 \)) and the non-dimensional time separation \( \frac{\Delta t}{\tau_s} \) on the vertical axis.

1Based on a Fourier-transformation of the spatial and time variables, the wave-number-frequency plot similar to Fig. 5 would result.
The convective ridge defined by
\[ \Delta x - u_j \Delta t = 0, \tag{25} \]
(indicated by the dashed diagonal line) represents the cross-correlation in a Lagrangian frame based on the convection velocity \( u_j \). In this frame and omitting the dependence on spatial separations which are not flow aligned, i.e. \( \Delta y = \Delta z = 0 \), both normalised cross-correlation models reduce to (\( \xi \) replaced by \( \Delta x \))

\[ \mathcal{R}_{12,TA} = \exp \left(-\frac{\Delta x}{u_j \tau_s}\right), \tag{26} \]

and

\[ \mathcal{R}_{12,FRPM} = \exp \left(-\frac{\Delta t}{\tau_s}\right), \tag{27} \]

respectively. Hence, by using Eq. (25) to substitute \( \Delta t \) by \( \Delta x/u_j \) in Eq. (27), both cross-correlation functions become equivalent. This result would also directly follow from Taylor’s hypothesis, which is strictly valid for frozen turbulence. However, the previous result in principle also holds for arbitrary decaying turbulence and supports the conjecture that both cross-correlation functions are equivalent.

Nevertheless, the far-field spectrum that results from the cross-correlation model realised by (F)RPM becomes

\[ \hat{S}(x, \omega) = 2 \left(\frac{\pi}{\ln(2)}\right) \frac{q_{13}^2 \tau_s}{c^2 \tau_s} \int_{V\backslash c} \left| \hat{p}_a(x_1, x, \omega) \right|^2 \exp \left[ \frac{-\omega^2 l_s^2}{4 \ln(2) u_j^2} \right] \frac{1}{1 + \omega^2 \tau_s^2 (1 - M_0 \cos \theta)^2} \, d^2x_1, \tag{28} \]

refer to [23, 30]. On the contrary, the resulting far-field spectrum that follows from the T&A model yields [16]

\[ \hat{S}(x, \omega) = 2 \left(\frac{\pi}{\ln(2)}\right) \frac{q_{13}^2 \tau_s}{c^2 \tau_s} \int_{V\backslash c} \left| \hat{p}_a(x_1, x, \omega) \right|^2 \exp \left[ \frac{-\omega^2 l_s^2}{4 \ln(2) c_0^2} \right] \frac{1}{1 + \omega^2 \tau_s^2 (1 - M_0 \cos \theta)^2} \, d^2x_1. \tag{29} \]

Both resulting spectra emerge from the integration (superposition) of building-block spectra depending on the local jet-noise source variables multiplied with the square of the Green function \( \hat{p}_a \). The main frequency dependence of the building-block spectrum of the T&A cross-correlation model is given by an exponential function with argument \( 2\pi St = \omega l_s/u_j \), i.e. a Strouhal number based on the local length scale parameter, refer to Eq. (28).

Additional contributions come from the denominator of Eq. (28) as well as from the frequency dependence of the Green function. However, the latter contributions to the spectrum turn out to be relatively small. The cross-correlation function of the (F)RPM model depends on \( 2\pi He = \omega l_s/c_0 \). That is, the T&A cross-correlation model resembles Strouhal similarity, whereas the latter model function realizes Helmholtz similarity.

Since the fine scale jet noise contribution is expected to be Strouhal similar, the previous result highlights a general deviation of the cross-correlation function Eq. (23) if being used unmodified.
3.2.7. Velocity correction for zero jet co-flow speed

The previous results indicate a possibility to realize with the cross-correlation model Eq. (23) the proper building-block spectrum as being used in the genuine T&A model, Eq. (28), by scaling the length scale in the (F) RPM cross-correlation function with the local jet Mach number \( M_j = u_j/c^0 \). To be precise, a modified length scale is introduced by stretching the initial length scale from RANS by the local jet Mach number,

\[
l^*_s = l_s M_j^{-1}, \quad \text{or,} \quad c^*_s = c_s M_j^{-1}.
\]

As a consequence, the (F) RPM building block spectrum defined by Eq. (29) changes, viz

\[
\exp \left[ - \frac{\omega^2 l_s^2}{4 \ln(2) u_0^2} \right] \rightarrow \exp \left[ - \frac{\omega^2 l_j^2}{4 \ln(2) u_j^2} \right],
\]

and the proper Strouhal scaling is established. However, as a further side effect, the initial variance \( q_s^2 l_j^2/(c^2 \tau_s) \) in Eq. (29) is also changed due to the replacement \( l_j^* \rightarrow l_s \). To compensate this unwanted side effect on variance, the target variance has to be re-calibrated as well. Specifically, using a variance

\[
(q_s^2)^* = M_j^3 q_s^2, \quad \text{or,} \quad e^* = c M_j^{-3},
\]

the initially intended variance can be conserved.

The approach was successfully validated against experimental data for a single-stream jet in a resting medium and against the predictions of the genuine T&A model, further details can be found in [14, 15, 31, 32, 33, 34]. The difference in the resulting spectra stems from the moduli present in Eqs. (26) and (27) that cause different 'kinks' in the contour lines visible in Fig. 5 a) and b).

3.2.8. Velocity correction for non-zero co-flow velocity

The previous correction method is suitable for a jet in a medium at rest. For the prediction of jet noise during cruise a modification to the discussed correction is proposed in this paper. Approximately, the effect of a co-flow on noise emission can be taken into account by using the difference velocity between jet and cruise flow e.g. on noise emission can be taken into account by using the difference velocity between jet and cruise flow, \( u_j - u^0 \), rather than the jet velocity alone \( u_j \) as the crucial characteristic velocity.

Following this reasoning, as a suitable velocity it has been found in this work that the difference Mach number,

\[
\Delta M_j(x) := \frac{|u_j(x)|}{c^0} - \alpha_c M_c,
\]

represents a suitable value to be used in Eqs. (30) and (32) for a non-zero co-flow velocity. Here, \( u_j(x) \) is the local jet flow velocity from RANS and \( M_c \) and \( c^0 \) indicate the undisturbed co-flow velocity and speed of sound, respectively. The parameter \( \alpha_c \) is a order one parameter that was adjusted in this work based on numerical results for single stream jets. A value of \( \alpha_c \approx 0.9 \) was found suitable to give a good qualitative and quantitative spectral prediction. Note, for vanishing co-flow \( M_c \rightarrow 0 \) the scaling yields identical results to the standard static method.

Fig. 6 compares single stream jet predictions for static and co-flow conditions between the newly proposed extended scaling (red solid line) and experimental results from Seiner [16] up to the CAA mesh cut off at a jet Strouhal number \( Str = f D_j/(M_j c^0) \). The result confirms the proposed methodology in the limit \( M_c \rightarrow 0 \).

Fig. 6(b) provides a comparison of spectra for \( M_j = 0.73 \) and co-flow \( M_c = 0.5 \). Shown are results from the genuine T&A model (black solid lines), experimental data (black symbols) from reference [35], and predictions with the newly proposed extended scaling (red solid line). A fairly good agreement in terms of level is observable. Some deviations are visible for higher frequencies \( Str = f D_j/(M_j c^0) \). However, in total a reasonably good agreement of the spectrum with the reference data is achieved.

For future work it is planned to replace the (F) RPM correlation model with a kink-free model for the Lagrangian correlations Eqs. (26) and (27) to mitigate the need for an extra scaling model to correct for the mean-flow effect.

3.2.9. CAA setup

In Fig. 7, the computational domain for the UHBR configuration is shown. Originally the CAA mesh consists out of 16 blocks with about 47 mio points. For parallelisation purposes the blocks have been split of into 578 small blocks. The maximal resolved frequency of the CAA mesh is 2 kHz. According to this point sampling positions at the fuselage surface are defined, which allow an appropriate spatial resolution. In total ca. 53000
Figure 6: Single-stream jet noise prediction using FRPM with new scaling model; a) Co-flow Mach number $M_c = 0$; b) Co-flow Mach number $M_c = 0.5$; predictions and experimental data taken from [16, 35].

Figure 7: CAA domain for the UHBR engine presented together with a slice showing the turbulence kinetic energy (TKE) distribution from RANS downstream of the jet engine; the grey coloured area indicates the sampling positions at the fuselage surface.

The predicted spectra are compared to a fine scale similarity spectrum (G-Spectrum [16]) adjusted for each
simulation case (black solid line). The slightly steeper roll-off seen in the numerical simulations might be attributable to the more complex jet flow present for the bypass nozzles that causes deviations from the general spectral shape as expected for a single stream jet, which is well described by a G-Spectrum.

3.3. FE modelling of the aircraft’s fuselage

The data available by the preliminary design process includes a static finite element model of the entire aircraft which has been applied in standardised load cases [36] in order to dimension the outer skin, the circular stiffeners, the floor and the bulk head. The obtained thicknesses and cross-sections serve as basis information for the wave resolving mechanical model in frequency domain. In Fig. 10, an overview of all parts is shown. Structural parts are orange, fluid parts are blue. Additionally, an insulation (glass wool), the cabin fluid itself (air) and typical sidewall panels (honeycomb sandwich) are considered in the model. The model underlies the following general assumptions:

1. Linearity
2. Stationarity regarding the response in cruise flight
3. Symmetry with inversely phased excitation by the two identical engines in cruise flight
4. Neglection of the aircraft’s structural front partition (cockpit to wing box)

In real flights, neither the response is stationary nor both engines generate symmetric loads. For this investigation, the SPL differences between the two configurations are of interest. Hence, as the mentioned assumptions are expected to introduce systematic modelling errors, the influences can be neglected. The latter assumption helps realising manageable solution times and memory efforts. The jet noise excitation mainly affects the rear part of the aircraft and the wing box brings a reflecting impedance jump into the system which are justifications for the third assumption. The second assumption leads to a much smaller problem dimension, as symmetric boundary conditions can be applied instead of solving the entire domain.

Figure 8: Sound pressure fluctuations $p$ on the fuselage from PIANO/FRPM simulation.

Figure 9: Predicted spectra for the BPR5 and UHBR configuration at the same microphone position from RPM-modal simulation.
All domains are discretised by finite elements and solved by the working group’s in-house code elPaSo [13]. The code uses PETSc as algebra package and MUMPS (MUltifrontal Massively Parallel sparse direct Solver) as solver.

The outer skin of the fuselage is made of orthotropic CFRP with ten layers by which homogenised values are derived using the classical laminate theory. A quadrilateral 9-node shell element with quadratic polynomial ansatz functions is applied, combining structural disc and plate (Reissner-Mindlin) formulations. Hence, in-plane waves and bending waves are represented which is necessary for the complex cylindrical structure. Nevertheless, bending waves are expected to dominate the behaviour of the thin structures which is determining the necessary mesh size. The outer skin is quite thin compared to all other parts which yields the smallest bending wave length $\lambda_{\text{min}}$ in the entire aircraft structure. The thinnest outer skin field within the area of excitation has a thickness of $t_{\text{min}} = 0.0015$ m. $\lambda_{\text{min}}$ at the highest frequency $f_{\text{max}}$ can be estimated by Eq. (34) for an infinite homogeneous plate [37].

$$\lambda_{\text{min}} = 2\pi \sqrt{\frac{B}{2\pi f_{\text{max}} \rho_{\text{min}}}}$$

$B = \frac{Et_{\text{min}}^3}{12(1-\nu^2)}$ is the constant bending stiffness (with the Young’s modulus $E$ and the Poisson’s ratio $\nu$) and $\rho$ is the density. This estimation is conservative as the circular stiffener at the boundary of each segment (see Fig. 10) increases the stiffness of the system and so $\lambda_{\text{min}}$. Further, the minimum bending wave length is estimated for the thinnest skin field. The mesh size is directly derived by this value and taken for all parts of the fuselage resulting in a well structured mesh with coincident nodes at the interfaces between fluid and structural domains. The estimated wave lengths for the outer skin (thinnest field), the floor, the sidewall panel, the cabin fluid and the insulation are depicted in Fig. 11. The grey lines indicate the minimum wave length which can be resolved by the finally chosen mesh sizes 0.200, 0.150 and 0.100 m. The remaining numerical error can be assessed by comparing the three meshes (see Sec. 4). For the sidewall panel, which is a sandwich structure explained below, the classical laminate theory is applied to roughly estimate the bending wave length. It can be stated with a high degree of certainty that the wave length in that structure is not crucial within the entire frequency range. Between 100 and 200 Hz an intersection between the wave lengths of the insulation and the sidewall panel occurs. At this coincidence frequency, a high sound transmission into the cabin can be expected as the sound radiation is maximal.

The floor, the rear bulk head and all stiffeners are similarly made of orthotropic CFRP and represented by 9-node shell elements in the model. In these parts, a thickness distribution is also applied in dependence on the given preliminary aircraft design data. Damping in all structural parts is generally considered by setting a global loss factor of $\eta = 0.01$ which is an approximation for complex structures with many joints [37]. Again, due to systematic modelling errors coming along with such simplifications, a valid comparison of the two
configurations is still expected. The Young’s modulus \( E \) of the linear elastic materials in all structures becomes complex according to Eq. (35).

\[
E = E(1 + i\eta)
\]  

The generic **sidewall panel** consists of a typical sandwich structure made of an aramid honeycomb core surrounded by glass fibre reinforced plastic (GFRP). Instead of resolving each comb in the core, an approach by Estorff [38] is applied. The honeycomb core is modelled with a homogenised 3D continuum formulation while the GFRP layers are represented by shell sharing the 3D FE nodes. The cross section of the sandwich panel is depicted in Fig. 12 in which the grey elements are 27-node hexahedrons sharing their nodes with the orange 9-node shells. Material data for the two domains in the sandwich structure is taken from literature [39, 40].

For both, the **cabin fluid** and the **insulation**, the Helmholtz equation is applied as model and discretised by 27-node hexahedrons with quadratic ansatzfunctions using an acoustic pressure formulation. The speed of sound \( c \) and the density \( \rho \) of the fluid are complex and frequency-dependent in the equivalent fluid model which is applied for the glass wool in order to model the poro-elastic and damping characteristics. Material data is taken from literature [41]. For the cabin fluid, real values are applied as first approach whereby a damped fluid and a realistic modelling of the passenger cabin is planned in future work.

A strong coupling is applied between adjacent structural (outer skin, sidewall panel and floor) and fluid domains (insulation and cabin). As shear stresses are neglected in both, the equivalent and the ideal fluid domain, the normal displacement \( u_n \) of the shells is taken into account as stated in Eq. (36) [42].

\[
\rho_f \omega^2 u_n = \frac{\partial p_f}{\partial n}
\]  

Here, \( \omega \) is the angular frequency, \( \rho_f \) is the fluid density and \( u_n \) and \( p_f \) are the coupled degrees of freedom (dof) for displacement and pressure. In the FE formulation, the shell domain (Eq. (37)) and the fluid domain (Eq. (38)) are linked by means of the coupling matrix \( C \) (Eq. (39)) which is evaluated at the interface \( \Gamma \).

\[
(K - \omega^2 M) u^{(i)} = C p^{(i)}
\]  

\[
\left(K_1 - \frac{\omega^2}{c_f^2} K_2\right) p^{(i)} = \rho \omega^2 C^T u^{(i)}
\]  

\[
C = \int N_s^T N_f d\Gamma
\]
In the Eq. (37) and (38), $K_1$ is the stiffness (compressibility) matrix, $M_2$ is the mass matrix and $c_f$ is the speed of sound in the fluid domain. Inserting the coupling, the bandwidth of the system matrix is increased significantly. Due to the double wall structure, four interfaces must be considered:

- Outer surface – insulation
- Outer GFRP layer of sidewall panel – insulation
- Inner GFRP layer of sidewall panel – cabin
- Floor – cabin

This leads to a highly complex problem with high computational costs compared to a solution of each single domain. Corresponding solution times and memory requirements are depicted in Tab. 2. If finer meshes shall be investigated in future work, a different solver strategy is necessary as the required memory of the direct solver increases exponentially. The numerical error of the model decreases with finer meshes. A finer mesh size affects not only the resolution of waves, but also the load input by the fluctuating pressure. The input power converges with a finer mesh as a constant value is assumed per element. For each shell element’s central node of the outer skin within the CAA domain, frequency-dependent amplitude an phase values are interpolated.

### 4. Results

Finally, the modelling chain introduced in Sec. 3 delivers the sound pressure distribution over frequency in the passenger cabin fluid. Serving as example, in Fig. 13, the SPL at seat 35 (ear-height) is shown for both engine configurations. Comparing the UHBR engine aircraft configuration with the BPR5 engine aircraft configuration, a much smaller SPL at seat 35 is estimated by the model in the entire frequency range. An obvious increase can be observed at $f \approx 140$ Hz and $f \approx 540$ Hz. The transmission loss of a similar structure (equal data basis) has been investigated in [43] and shows minima in these frequencies as well. The first increased SPL corresponds with the typical minimum of the transmission loss curve and is also predicted as coincidence frequency between the sidewall panel and the insulation (see Fig. 11). In [43], a fuselage section is investigated with a mean value for the outer skin’s thickness instead of the thickness distribution applied here. The second pressure peak in Fig. 13 at $f \approx 540$ Hz particularly corresponds to the second local minima in the TL curve of the investigation in [43]. In this case, the outer skin’s bending wave length matches the insulation’s wave length which lead to a high sound transmission into the cabin at this frequency. However, this is an indicator as the mean value of the outer skins thickness matches.

As stated in Sec. 3.3, the mechanical model of the fuselage underlies a couple of assumptions introducing systematic errors in particular. In Fig. 14, the difference between the two configurations $\Delta$SPL($f$) (see Eq. (40)) is shown for seat 35 in third octave bands for the three applied meshes.

$$\Delta \text{SPL}(f) = \text{SPL}_{\text{UHBR}}(f) - \text{SPL}_{\text{BPR5}}(f)$$

Due to the mesh refinement, a change of these values can be observed without a clear convergence. In further studies, finer meshes will be investigated to show a convergence of the numerical error in the model. Nevertheless, the cabin noise induced by jet noise is much quieter for UHBR engines compared to conventional BPR5 engines of the second generation. The sound reduction potential can be expected at around 20 dB above 50 Hz. In lower frequency ranges, the sound reduction is smaller but still around 10 dB.

Comparing each seat is not representative for the entire cabin, particularly since damping is not yet considered within the cabin fluid leading to local effects. A clear dominance of mode shapes in the cabin yields a high dynamic in the frequency response. Therefore, the Mean square Sound Pressure Level (MSPL) of all seats...
Figure 13: SPL over frequency at seat 35 for both engine configurations; Cabin noise results calculated with the finest mesh ($h_c = 100$ mm).

Figure 14: $\Delta$SPL (according to Eq. (40) corresponding to the benefit by the UHBR engine) in third octave band at seat 35 for three different meshes.

$$N_{\text{seats}}$$ is calculated, according to Eq. (41) and (42). This value gives an overall impression of the sound field in the cabin.

$$\overline{p^2}(f) = \frac{\sum_{i=1}^{N_{\text{seats}}} p_i^2(f)}{N_{\text{seats}}}$$  \hspace{1cm} (41)

$$\text{MSPL}(f) = 10 \cdot \log_{10} \left( \frac{\overline{p^2}(f)}{p_{\text{ref}}^2} \right)$$  \hspace{1cm} (42)

Accordingly, in Fig. 15, the MSPL in the cabin of the two engine configurations is shown. Furthermore, the maximum occurring SPL on the outer skin (CAA excitation) is plotted over frequency giving an impression of the sound transmission loss. As expected, the difference between the outer excitation and the occurring SPL in the cabin increases with frequency. The pressure differences on the outer skin are not similarly transferred into the cabin. Rather, depending on the double wall sound transmission characteristics and the loading, the curves are pretty close in lower frequency regions and constantly different ($\approx 20$ dB) above 200 Hz.

In addition, $\Delta$MSPL is depicted in Fig 16 for different meshes in third octave bands. Due to the mean values over all seats, a mesh refinement has less influence on this more general curve. Comparing with Fig. 14, the fluctuations are smaller. Nevertheless, the sound reduction performance by the UHBR engine is similar.

In Fig. 17, all seats are shown with a corresponding difference in the sum level. In addition, the maximum occurring excitation in dependence on $x$ is plotted on the outer skin. As the engines are mounted differently,
the maximum excitation is shifted to the back of the aircraft in case of the UHBR engine. As the top view is plotted, the distance of the two engines cannot be seen in Fig. 17. In fact, the two engine’s axes have the same distance to the cabin’s centre. Hence, as the UHBR engine has a larger diameter, the turbulent shear layer by the jet is closer to the outer surface. However, by introducing an UHBR engine instead of a BPR5 engine, the overall sum level is decreased between 21 and 24 dB, according to the applied models. This value is highly dominated by the large reduction at high frequencies. The previous graphs and interpretations have emphasised a much lower difference at frequencies below 100 Hz. Finally, it can be stated that the jet noise by an UHBR engine configuration in general induced much less cabin noise compared to a conventional engine configuration which confirms the hypothesis of that contribution. The mainly systematic modelling errors are not expected to change this clear tendency.

5. Conclusion

In this contribution, a numerical modelling chain is presented for the simulation of cabin noise in dependence on jet noise. The numerical methods are applied for the comparison of an Ultra-High-Bypass Ratio engine configuration with a conventional engine configuration. As noise source, the jet of both engines is considered in Computational Aeroacoustics calculations. A novel approach to investigate Ultra-High-Bypass Ratio engines with non-zero co-flow conditions is introduced and applied successfully. Using the difference velocity between jet and co-flow enables a solution of both engine configurations. By use of PIANO [11] in combination with the Fast Random Particle Mesh method FRPM [12], pressure fluctuations on the outer skin of a research aircraft are computed resulting in much higher pressure values by the conventional engine. RANS computations of the
outer flow conducted with TAU [9, 10] serve as basis.

Considering a weak coupling, a finite element model of a research aircraft fuselage is excited by the received pressure on the outer skin and solved by the in-house code elPaSo [13]. Besides the outer skin and circular stiffeners, the model includes the insulation, the sidewall panels and the cabin fluid. The results show a much lower sound pressure level in the cabin at all seat positions for the Ultra-High-Bypass Ratio engine. In sum levels, the reduction is between 21 and 24 dB.

On the basis of this model, future work will be conducted. First, a deeper investigation of some applied model assumptions in the entire modelling chain is planned. Key points are a strong coupling between flow and structural domain, iterative solvers in frequency-domain to speed-up the structural part and more complex models for the cabin fluid (including damping and absorption of the interior equipment) and the insulation (Biot model). Second, further application cases such as different noise sources (e.g. turbulent boundary layer) and a propeller engine will be investigated. Finally, the uncertainties of the noise transmission problem are planned to be quantified in the next period of the Collaborative Research Centre 880 for which the basics are shown in [43].

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