Prediction of Helicopter Rotor Discrete Frequency Noise for Three Scale Models

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A new computer program that uses Farassat's most advanced subsonic time domain formulation has been written to predict helicopter rotor discrete frequency noise. A brief description of the program (WOPWOP), is followed by a comparison of predicted and experimentally measured acoustic pressure and spectra for a ¼ scale UH-1 model rotor blade and a 1/7 scale OLS (AH-1G) model rotor blade. The rotorcraft flight simulation computer program C81 was used to predict the spanwise loading on the rotor for aerodynamic input into the acoustic prediction. Comparisons are made for different flight conditions and microphone locations with good results. In general, the acoustic pressure is underpredicted. The acoustic predictions for a tapered rotor blade and predictions for microphones well below the tip path plane show less underprediction. Finally, in-plane motion of the rotor blade is shown to significantly affect the peak-to-peak amplitude of the acoustic pressure for high advancing tip Mach numbers.

Nomenclature

A = lead-lag amplitude, deg
c_0 = speed of sound in the undisturbed medium
C_T = thrust coefficient, = thrust/$\rho\pi R^2(\Omega R)^2$
f = equation of body surface, = 0
$H(f)$ = Heaviside function
ℓ_i = force per unit area on the body surface
M = local Mach number of the body surface
M_{AT} = advancing tip Mach number
M_r = local Mach number in the radiation direction
$p'(x,t)$ = acoustic pressure
R = rotor radius
R/D = rate of descent
r = distance from source to observer
$\hat{r}$ = unit vector in radiation direction
t = observer time
T_{ij} = Lighthill stress tensor
V = forward speed
v_n = local normal velocity of the body surface
x = observer position
Z_0 = phase angle of lead-lag motion
$\delta(f)$ = Dirac delta function
ρ_0 = density of the undisturbed medium
μ = rotor advance ratio, = $V/\Omega R$
Ω = rotation rate of rotor
ψ = azimuth angle, measured counterclockwise from the downstream direction
ζ = lead-lag angle
$\Box^2$ = wave operator, = $(1/c_0^2)\,(\partial^2/\partial t^2) - \nabla^2$

Introduction

HELICOPTER rotor noise has been an important area of acoustic research for several years. With the advent of advanced materials and new manufacturing techniques, the helicopter rotor designer has been given more freedom to design lower-noise rotors while still meeting the performance objectives of the rotorcraft. Indeed, the latest helicopters have made considerable advances in aerodynamic, dynamic, and acoustic performance, which has lead to generally quieter and smoother-riding helicopters. To continue the present trend of designing quieter helicopters, more accurate rotor noise prediction capability is needed. Various theories based on time and frequency domain methods have been used to address this need with wide-ranging levels of success. What is needed by a rotor designer is an accurate and easy-to-use prediction method that is able to account for subtle design details such as blade planform, airfoil shape, blade twist distribution, and realistic helicopter rotor blade motions. The prediction method should also be robust, i.e., applicable to a wide range of flight conditions and blade designs. If the prediction is based solely upon a first-principles method with no "tuning" parameters, so much the better.

A new computer program has been written at Langley Research Center as a step to improve rotor discrete frequency noise prediction.[1] This program, WOPWOP, is based upon the time domain formulation of Farassat referred to as formulation 1A. This formulation is a solution of the Ffowcs Williams-Hawkings equation with the quadrupole term neglected. The WOPWOP program includes realistic helicopter blade motions in its calculation of the near- and far-field acoustic radiation. It is the intent of this paper to show some typical comparisons of noise calculations with experimental data and to suggest some areas in which care should be taken to achieve reasonable noise predictions. Three different rotor systems will be evaluated in this comparison.

The first section of this paper consists of a brief discussion of the new WOPWOP program and the underlying formulation used. The second section of the paper will then consist of experimental/computational comparisons for a ¼ scale UH-1 model scale test in the Langley 14 × 22 ft Subsonic Wind Tunnel and a 1/7 scale operational loads survey (OLS) model scale test in the Duits-Nederlandse Windtunnel (DNW). For the UH-1 test, a baseline rectangular planform rotor was tested together with an advanced tapered blade set. The final section of the paper will address the importance of the in-plane motion of the rotor blade to the calculated acoustic signature.

Computational Approach

The governing equation is the Ffowcs Williams-Hawkings (FW-H) equation,[2] which can be written as

$$\Box^2 p'(x,t) = \frac{\partial}{\partial t}[\rho_o v_n |\nabla f| \delta(f)] - \frac{\partial}{\partial x_i}[\ell_i |\nabla f| \delta(f)] + \frac{\partial^2}{\partial x_i \partial x_j}[T_{ij} H(f)] \tag{1}$$

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The three source terms are known as thickness, loading, and quadrupole. Farassat has developed several solutions of the FW-H equation that are valid for subsonic and supersonic blade motions by neglecting the quadrupole source term.[3-5] In the WOPWOP program, only the subsonic formulation 1A of Farassat is used. This formulation is written as

$$p'(x,t) = p_T'(x,t) + p_L'(x,t) \tag{2}$$

where

$$4\pi p_T'(x,t) = \int_{f=0} \left[\frac{\rho_o \dot{v}_n}{r(1-M_r)^2}\right]_{\text{ret}} \mathrm{d}S + \int_{f=0} \left[\frac{\rho_o v_n (r\dot{M}_i\hat{r}_i + c_0 M_r - c_0 M^2)}{r^2(1-M_r)^3}\right]_{\text{ret}} \mathrm{d}S$$

and

$$4\pi p_L'(x,t) = \int_{f=0} \left[\frac{\dot{\ell}_i \hat{r}_i}{r(1-M_r)^2}\right]_{\text{ret}} \mathrm{d}S + \int_{f=0} \left[\frac{\ell_r - \ell_i M_i}{r^2(1-M_r)^2}\right]_{\text{ret}} \mathrm{d}S + \frac{1}{c_o}\int_{f=0} \left[\frac{\ell_r(r\dot{M}_i\hat{r}_i + c_0 M_r - c_0 M^2)}{r^2(1-M_r)^3}\right]_{\text{ret}} \mathrm{d}S$$

Here p_T' and p_L' denote the acoustic pressure due to thickness and loading, respectively. The dots $\dot{\ell}_i$, $\dot{M}_i$, and $\dot{v}_n$ denote the rate of change with respect to source time. Equation (2) is derived in Refs. 1 and 4. Formulation 1A is computationally more efficient than previous formulations since a time differentiation, which is normally calculated numerically, has been evaluated analytically. By doing this, impulsive blade loadings, such as those occurring in blade-vortex interactions, may be used directly to obtain reliable results.

Although the acoustic formulation allows arbitrary blade motion, geometry, and observer locations, the numerical solution is of interest only for realistic flight conditions and rotor geometries. Thus, this program has been written to include all rigid blade motions. The inclusion of all helicopter rigid blade motions, and in particular the in-plane motion of the rotor blade, along with the formulation 1A of Farassat is believed to be unique to this code. The numerical integration of the integrands takes place on the actual blade surface. The time history is Fourier decomposed to find the acoustic spectra in terms of sound pressure level (SPL) and phase for each harmonic. All computations are made in a frame fixed to the fluid and are based entirely upon first principles. Once the mesh size has been refined sufficiently to assure numerical convergence of the associated integrations, no parameters remain to "tune" the solution to an arbitrarily desired result.

For the acoustic predictions presented in this paper, the rotorcraft flight simulation computer program C81 (AGAP8410)[6] was used to estimate the flapping and feathering coefficients as well as the loading distribution on the rotor disk. An engineering approximation to the local pressure distribution based on thin-airfoil theory[7] and corrected for compressibility by the Prandtl-Glauert rule is used to model the chordwise loading. This pressure distribution model is simplistic, but should be quite realistic away from the rotor tip and is computationally very efficient. A uniform inflow model was used for these initial aerodynamic loading predictions. The aerodynamic model does not account for three-dimensional effects, blade-vortex interaction, or random loadings. The blade section drag was assumed to be the result of a constant shear stress for the blade section. Some of the specific details of the aerodynamic calculations used as input to the acoustic predictions are included in the following sections.

Comparison of Prediction and Experiment

To evaluate the effectiveness of WOPWOP, two model scale rotor tests have been selected for comparison with predictions.

UH-1 Comparison

The first comparison is for a ¼ scale UH-1 rotor model with baseline and advanced main rotor blade sets tested in the Langley 14 × 22 ft Subsonic Wind Tunnel by Conner and Hoad.[8,9] The baseline main rotor is the standard, two-bladed, rectangular rotor blade with NACA 0012 airfoil sections and 10.9 deg linear twist. The advanced rotor blades were designed such that the section critical Mach number and drag divergence would be avoided, thus improving the performance of the rotor system.[10] The planform of these two blades is seen in Fig. 1.

The loading calculations from C81 are based upon a rigid rotor with a uniform inflow velocity distribution. The actual flapping and feathering angles measured in the test were used for these acoustic predictions. The measured rotor speed and speed of sound for each test condition were used in the acoustic predictions to carefully match the advancing tip Mach number and advance ratio. It will be shown later in the paper that even relatively small changes in the speed of sound can make noticeable differences in the predicted peak acoustic pressure. Data are given in Ref. 8 for two microphones located in the tip path plane for forward speeds of 80–110 knots. Microphones 4 and 5 were located approximately ± 40 deg from the upstream direction. Both were approximately 2.15 rotor radii from the rotor shaft. The test conditions used for comparison with the predictions are shown in Table 1 for each UH-1 rotor tested.

Figure 2 shows a comparison of the predicted and experimental acoustic pressure and spectra for the baseline UH-1 rotor operated at a forward speed of 80 knots. At both microphone locations, the acoustic pressure waveform (Figs. 2a and 2c) agrees fairly well with the shape of the experimental waveform, even though the program does underpredict the peak amplitude and does not predict the fine structure of the waveform. The underprediction has been seen before for rotors with high advancing tip Mach numbers when using linear acoustic theory and is thought to be primarily due to the

Table 1 Operating conditions from UH-1 rotor test

Run	Rotor	V, knots	M_{AT}	c_0, m/s	μ
167	Baseline	80	0.828	350.1	0.166
173	Baseline	110	0.866	352.7	0.227
202	Advanced	80	0.818	355.1	0.165
210	Advanced	110	0.858	355.5	0.231

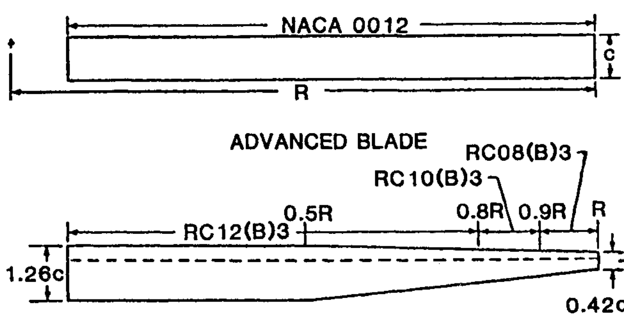
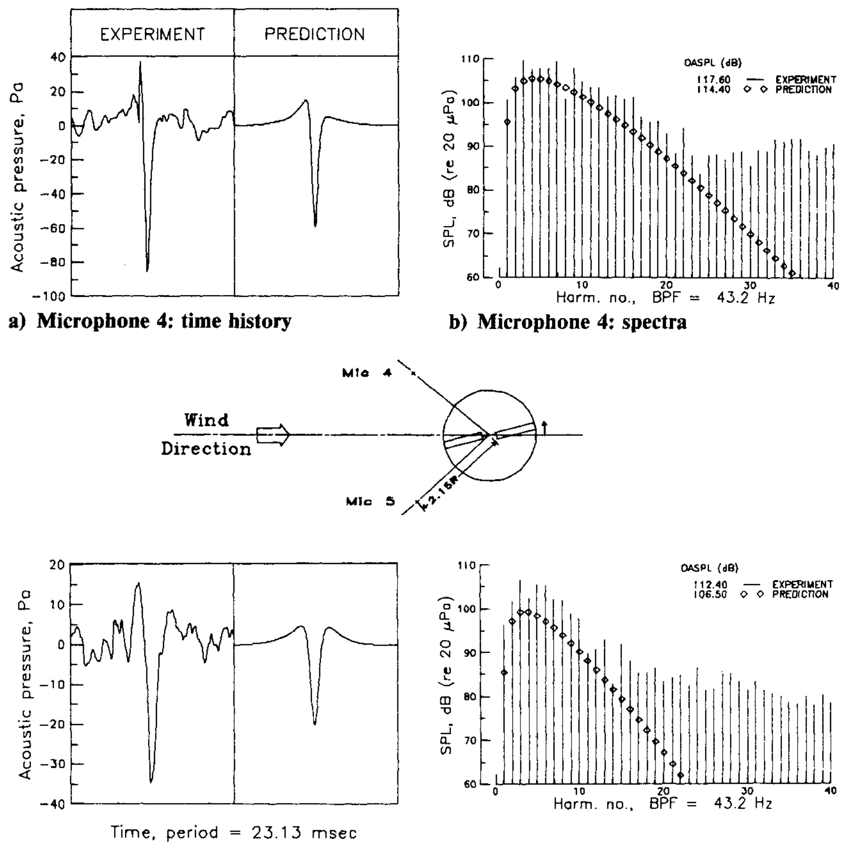


Fig. 1 Planform of UH-1 baseline and advanced rotor blades (from Ref. 8).



a) Microphone 4: time history
b) Microphone 4: spectra
c) Microphone 5: time history
d) Microphone 5: spectra

Fig. 2 Comparison of prediction and experiment for baseline UH-1 rotor, $V = 80$ knots.

neglecting of nonlinear terms in the formulation. Hanson and Fink,[11] as well as Schmitz and Yu,[12] have shown that the quadrupole term can be important for high-speed rotating machinery, particularly those with thick blades. The high-frequency wiggles in the experimental data may be due to blade-vortex interaction, unsteady flow, and tunnel reflections. It should be noted that the 14 × 22 ft Subsonic Wind Tunnel is not an anechoic facility. In the spectrum comparisons, Figs. 2b and 2d, the experimental data are underpredicted by 2–5 dB for about the first 20 harmonics for microphone 4 and slightly more for microphone 5. The high-frequency part of the spectrum cannot be predicted using the current blade loading input to WOPWOP.

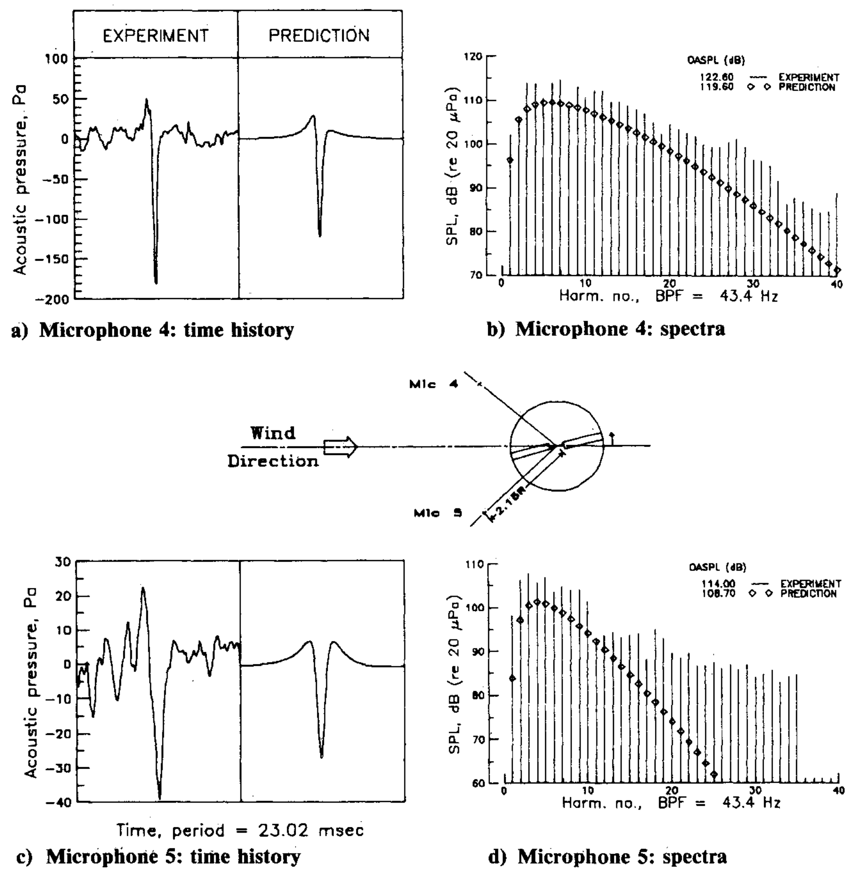
Figure 3 shows a similar comparison of predictions and experiment for the baseline rotor at a forward velocity of 110 knots. In this case, the advancing tip Mach number has increased to 0.866 from 0.828. Again, the predicted acoustic pressure and SPL are underpredicted by about the same magnitude as in the previous case. Notice that, while the predicted SPL in Fig. 3b is low, the general shape of the spectrum agrees better with the experimental spectrum than in Fig. 2. This case probably shows better agreement of spectrum shape primarily because the noise radiated is higher and further from the tunnel background noise. Since the baseline UH-1 blade is a rather thick blade and the tip Mach numbers are relatively high, these predictions are probably beyond the limit for linear acoustic theory.

The advanced UH-1 rotor blade has a much thinner airfoil section near the tip and was designed to avoid the section critical Mach number on the advancing blade. For this reason, the advanced rotor blade is a much better candidate for using linear acoustic theory, since transonic effects should not be nearly so strong. Unfortunately, the experimental data for the advanced UH-1 rotor are not as good since the acoustic pressure amplitude is not as great as for the baseline rotor. This can be seen in the comparison for the advanced rotor at a forward velocity of 80 knots shown in Fig. 4. First, notice in Figs. 4a and 4c that the predicted peak acoustic pressure seems to be quite close to the experiment for both microphone locations. Little can be said about the match between the waveform shapes. Also note in Figs. 4b and 4d that the predicted SPL level and general shape are close for the first 10 harmonics of the blade passage frequency. Again, the aerodynamic calculations have no high-frequency loading that would generate higher frequency acoustic energy. A similar comparison is made for the advanced rotor at a forward velocity of 110 knots in Fig. 5. For this case, the prediction underpredicts in both acoustic pressure and SPL as it did for the baseline rotor, only to a lesser degree. This may indicate the transonic effects are again becoming important for the higher advancing tip Mach numbers.

Another final comparison can be made between the baseline and advanced rotor blades as was done by Hoad and Conner.[9] It is important to note that the prediction method correctly accounts for design changes in the advanced rotor blade and has better agreement for cases in which linear theory should be more applicable. The predictions presented in Figs. 2–5 are improved from those used in Ref. 9 in that a better loading model and the newer formulation were used. In Ref. 9, the SPL for the baseline rotor is overpredicted, while in the present paper it is underpredicted, even though the acoustic pressure waveforms are similar. In Ref. 9, the measured experimental data were actually plotted 6 dB too low and were corrected in Ref. 8.

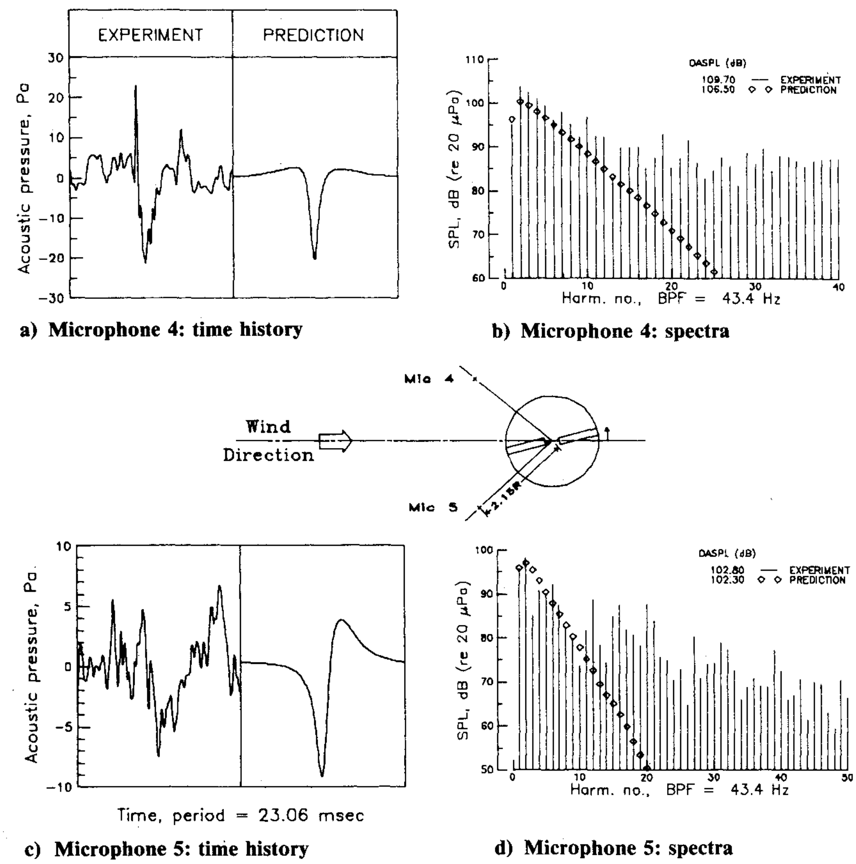
OLS Experiment-Prediction Comparison

To evaluate the directional prediction capability of WOPWOP, the 1/7 scale test of the operating loads survey (OLS) rotor tested by Schmitz et al.[13,14] in the Duits-Neder-



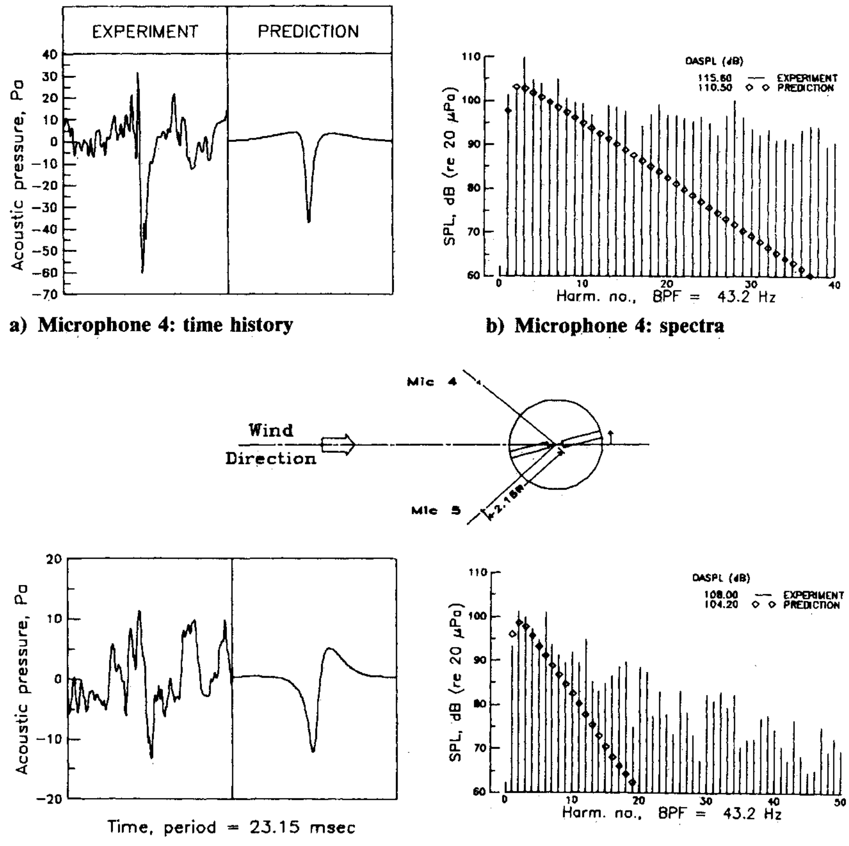
a) Microphone 4: time history
b) Microphone 4: spectra
c) Microphone 5: time history
d) Microphone 5: spectra

Fig. 3 Comparison of prediction and experiment for baseline UH-1 rotor, $V = 110$ knots.



a) Microphone 4: time history
b) Microphone 4: spectra
c) Microphone 5: time history
d) Microphone 5: spectra

Fig. 4 Comparison of prediction and experiment for advanced UH-1 rotor, $V = 80$ knots.



a) Microphone 4: time history

b) Microphone 4: spectra

c) Microphone 5: time history

d) Microphone 5: spectra

Fig. 5 Comparison of prediction and experiment for advanced UH-1 rotor, $V = 110$ knots.

Table 2 Operating conditions for OLS rotor test

V, knots	M_{AT}	c_0, m/s	μ	C_T	R/D, ft/min
115	0.837	340.3	0.26	0.0054	0

landse Windtunnel (DNW) was used. This test was chosen since several microphones were positioned around the rotor model for a good assessment of the noise directivity and the data are of high quality. The OLS rotor is a two-bladed, rectangular, teetering rotor with a modified BHT 540 airfoil section as shown in Fig. 6.

The loading calculations in C81 assume the rotor is a flexible rotor, although it was later discovered that the scale model tested was not dynamically scaled. A quasisteady aerodynamic trim was used which held the lateral blade flapping to nearly zero as was done in the model test. The operating conditions for the comparison are shown in Table 2. It is important to note that the OLS model rotor tested in the DNW was designed for clockwise rotation (when looking from above) with positive thrust upward. Symmetry was used to compare the acoustic pressure time histories from the experiment to the predictions.

A comparison of predicted and experimental longitudinal acoustic pressure directivity is shown in Fig. 7. Notice that the microphone 2 comparison shows the same kind of underprediction as did the UH-1 rotor in the tip path plane. For the microphones 30 and 45 deg below the tip path plane, the predicted acoustic pressure is somewhat similar in shape and amplitude to the experiment, while the pulse shape seems reversed. These waveforms are primarily dependent upon the loading noise. Figure 8 shows a comparison of the lateral directivity of the acoustic pressure. Again, all of the in-plane signatures are underpredicted, while the 30 deg down waveforms are in fair agreement. It is believed an improved

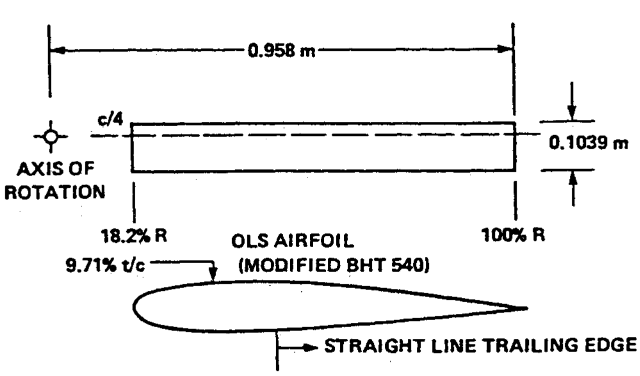


Fig. 6 OLS blade planform (from Ref. 13).

aerodynamics model including nonlinear inflow and a free wake coupled with transonic aerodynamics will improve the acoustic pressure waveforms out of the tip path plane. In the tip path plane, the nonlinear effects must be included in the acoustic formulation for high speeds and thick blades. Reference 15 shows a prediction with nonlinear effects included, using the experimentally measured blade motion and blade surface pressures. The results in that comparison are very good. A complete theoretical formulation for inclusion of the nonlinear effects is found in Ref. 16.

Rotor In-Plane Motion

In view of the previous predictions, it is interesting to look then at how changes in the program input can affect these predictions. In Ref. 13, it was suggested that the inclusion of lead-lag motion, due to a worn pitch bearing, could lead to a

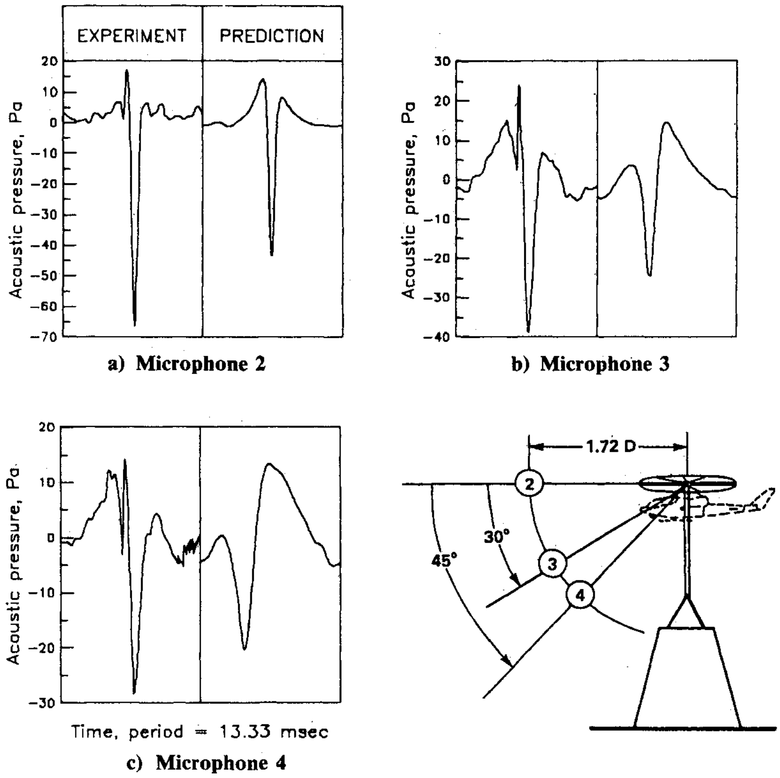


Fig. 7 Longitudinal directivity comparison of predicted and experimental acoustic pressure for 1/7 scale OLS model, $V = 115$ knots.

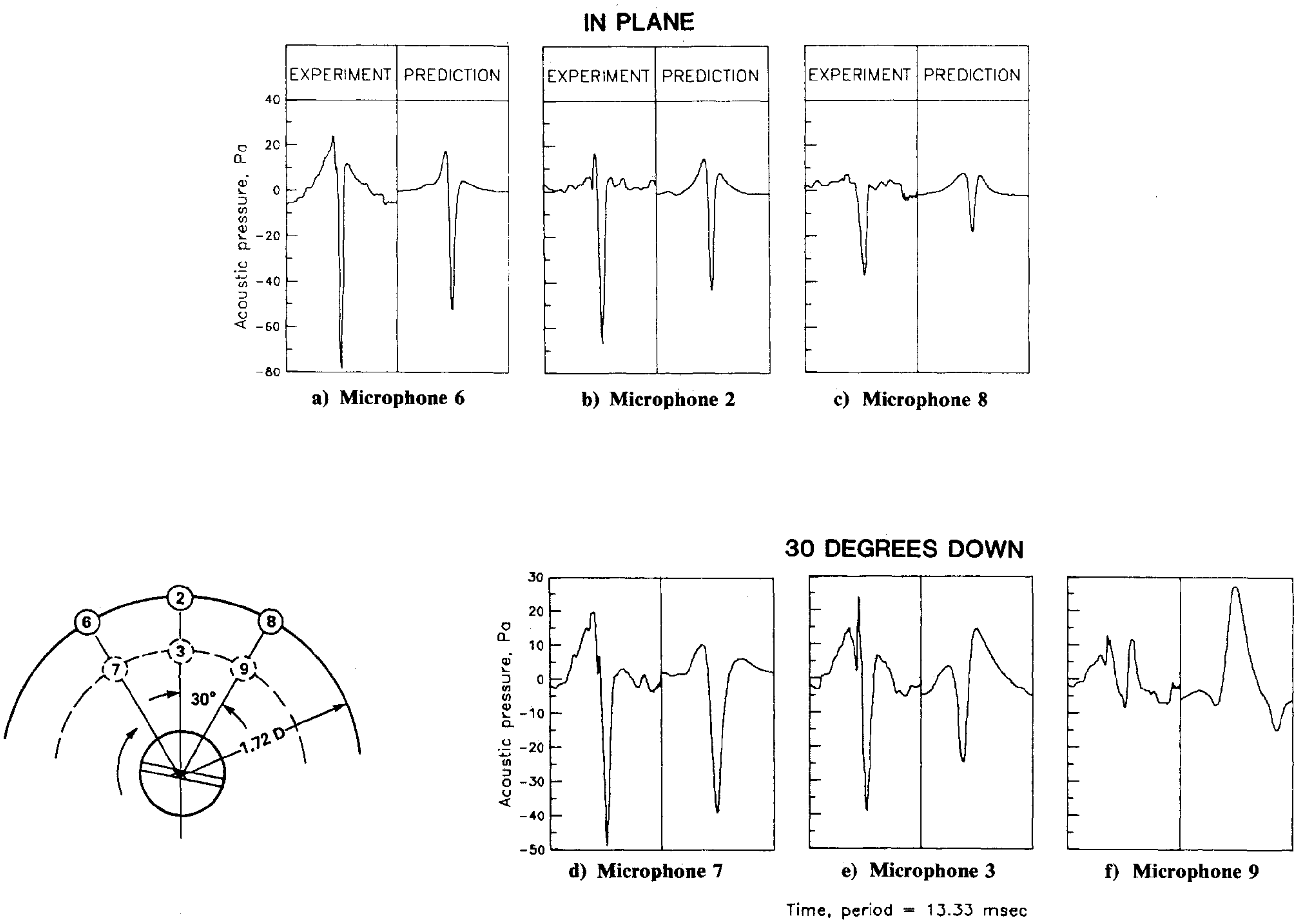
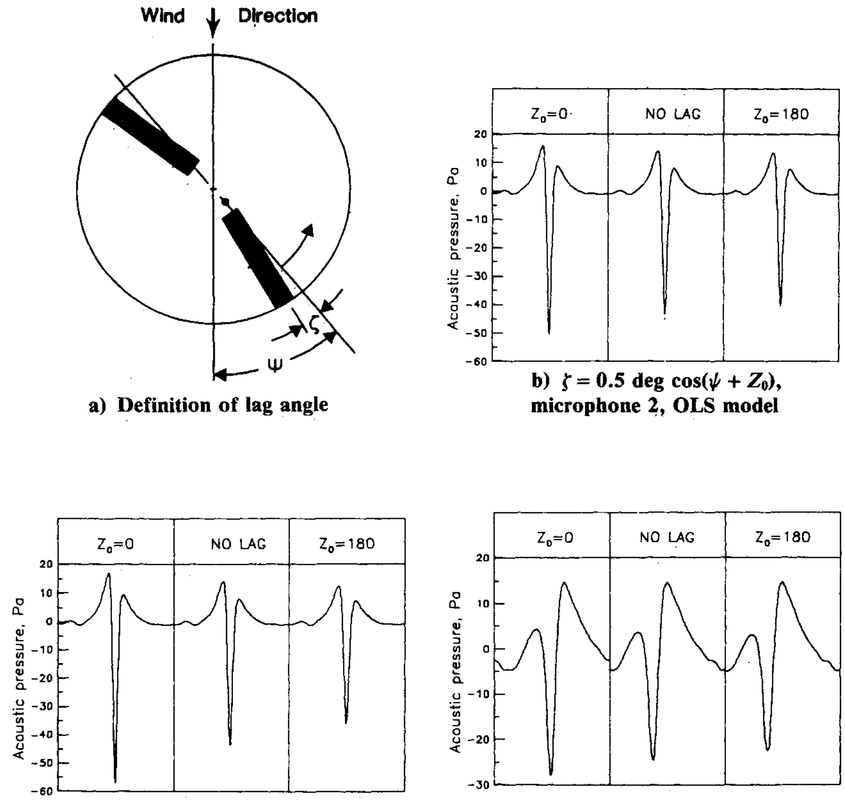


Fig. 8 Lateral directivity comparison of predicted and experimental acoustic pressure for 1/7 scale OLS model, $V = 115$ knots.



a) Definition of lag angle

b) $\zeta = 0.5$ deg $\cos(\psi + Z_0)$, microphone 2, OLS model

c) $\zeta = 1.0$ deg $\cos(\psi + Z_0)$, microphone 2, OLS model

d) $\zeta = 1.0$ deg $\cos(\psi + Z_0)$, microphone 3, OLS model

Fig. 9 Sensitivity of acoustic pressure to in-plane motion.

noticeable change in the radiated acoustic pressure. Since the WOPWOP program could simulate in-plane motion, a numerical experiment was conducted to determine to what extent in-plane motion affects the acoustic pressure. For this numerical experiment, the OLS model operating conditions for 115 knots forward speed were used to examine lead-lag motion of different phasing and amplitudes. The lag angle, shown in Fig. 9a, is defined as

$$\zeta = A \cos(\psi + Z_0) \tag{3}$$

The maximum peak acoustic pressure occurs at a phase angle $Z_0 = 0$ deg and a minimum peak acoustic pressure occurred at a phase angle $Z_0 = 180$ deg. These phase angles are compared in Figs. 9b and 9c to the predicted case for the OLS rotor with no lead-lag motion for the microphone 2 position (in the tip path plane). Figure 9b is for a lag amplitude $A = 0.5$ deg and Fig. 9c is for a lag amplitude $A = 1.0$ deg. In Fig. 9c, a substantial change in peak acoustic pressure is predicted relative to the case with no lead-lag motion. Figure 9d shows a similar but much reduced effect for microphone 3 (30 deg down) with the same 1.0 deg lead-lag motion. Thus, the effect of in-plane motion on the acoustic radiation appears to be primarily in the tip path plane. This sensitivity to in-plane motion of the rotor blades suggests that dynamic scaling of models for acoustic tests is important.

This rather surprising change in acoustic pressure in the tip path plane due to in-plane motion has been traced almost entirely to an effective change in advancing tip Mach number. In a similar prediction in which the shaft rotation rate was adjusted to account for the in-plane motion on the advancing side while keeping the advancing tip Mach number constant, no variation in peak acoustic pressure was observed. The strong dependence upon advancing tip Mach number in the acoustic prediction shows the importance for very accurate measurements in a rotor acoustic test and the need for careful and complete reporting of measured operating conditions with experimental acoustic data.

Conclusions

Using a new helicopter rotor discrete frequency noise prediction program and the rotorcraft flight simulation computer program C81, linear acoustic calculations are presented and compared with three rotor systems. In the first comparison, it was found that the acoustic pressure and sound pressure level predicted for the baseline rectangular UH-1 rotor blade for two moderately high forward speeds were underpredicted. The general shape of the SPL was correct for about the first 20 harmonics even though the level was about 2-5 dB too low. For this 12% thick blade, nonlinear transonic effects that were neglected are important. In predictions for an advanced UH-1 rotor, the comparisons showed better agreement in peak acoustic pressure level and SPL. This was expected since the advanced blades more closely meet the assumptions of a linear theory. The acoustic program did correctly account for rotor design difference between the two UH-1 rotors. In the third comparison with a 1/7 model of the OLS rotor, in-plane acoustic pressure peak amplitudes were underpredicted, while acoustic pressure predictions for loading dominated noise agree well in peak amplitude and are similar in shape. Apparently, nonlinear transonic effects are not nearly as important out of the tip path plane. Improved aerodynamic input should improve the acoustic predictions out of the tip path plane correspondingly.

It was also shown in this paper that a small amount of in-plane motion can lead to significant changes in the predicted acoustic pressure peak amplitude. These changes are due primarily to the effective change in the advancing tip Mach number, and they suggest that dynamic scaling in wind-tunnel testing is important. Also because of the sensitivity in the noise prediction to advancing tip Mach number, the speed of sound must be accurately accounted for and used in acoustic predictions.

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