Coupled high-fidelity URANS simulation for helicopter applications

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1. Motivation and objectives

Transonic flows on the advancing side of a helicopter rotor and blade–wake/blade–vortex interactions (BWI, BVI) are the major sources of helicopter noise. Both phenomena should be accurately predicted for a successful improvement of state-of-the-art helicopter design. To account for the former, a fully compressible CFD solver is essential in computing the flow around rotor blades. In most compressible flow solvers, however, the artificial dissipation formulated for shock capturing may lead to an erroneous dissipation of the wake or tip vortices and their subsequent spreading. Furthermore, the typical operating Reynolds number is very high, which makes the application of LES to helicopter rotor flow infeasible.

This article summarizes our technical efforts toward the development of high-fidelity CFD tools for rotorcraft applications as part of the DARPA Helicopter Quieting project. The key ideas in our strategy to overcome the difficulties previously stated are:

1. Coupling fully compressible and incompressible CFD solvers for near-blade and wake regions, respectively;

2. Incorporation of the low-dissipation algorithm (Mahesh et al. 2004; Ham & Iaccarino 2004) into the incompressible URANS solver for the wake region, in order to minimize the dissipation of vortical structures;

3. Using the most advanced four-equation $v^2-f$ turbulence model (Durbin 1995) for improved predictive performance.

This combination will resolve both compressibility and wake effects with solvers best suited for each purpose. We use the multi-block, structured SUmb code (Van der Weide et al. 2006) and the unstructured CDP solver (Ham & Iaccarino 2004) for compressible and incompressible flow solvers, respectively. These two codes have been successfully applied to the full gas turbine simulation (Medic et al. 2007). Note that the initial setup of the necessary computational frameworks such as a moving-mesh capability, coupling strategy, and validation cases is already reported in Hahn et al. (2005). As a continuing work, this article will mainly focus on more challenging validation cases.

This paper is organized as follows: Section 2 briefly describes new aspects of the revised coupling software. Section 3 introduces the technique of anisotropic adaptive mesh refinement. Validations for the UH-60A and HART-II cases are provided in Sections 4 and 5, respectively, followed by the rotational correction on the $v^2-f$ model in Section 6.

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2. Revision of coupling software

In order to facilitate the coupling procedure, it is more advantageous to have a separate coupling module that exclusively performs complicated searches, interpolations, and data transfer in an automated manner. For this purpose, we developed an initial version of coupling software, named CHIMPS (Coupler for High-performance Integrated Multi-Physics Simulations), through a collaboration with the ASC project in 2005 (Schlüter et al. 2005). We completely revised CHIMPS in 2006. The main objective of this revision is to provide more direct control of coupling procedures by specifying the frequency and nature of the data exchange between the applications. Furthermore, more functionalities are introduced to handle a wider class of applications within a coupled simulation (for example, particle-based simulations in addition to grid-based approaches) and to support various kinds of code-to-code data exchange (for example, spatially integrated data in addition to simply interpolated ones). This redesign not only provides more comprehensible, user-friendly, and organized API routines but also demonstrates efficient performance. On the other hand, along with the existing Python API, the CHIMPS library now supports a Fortran API to expedite the debugging of large-scale integrated simulations. C API may be also added in the near future. We examined the performance of the revised software for various applications, including simple interpolations, internal- and external-flow computations, and particle-based simulations. All test cases yielded successful results both in accuracy and scalability. This revised CHIMPS was also successfully applied to all the validation cases presented below. For detailed information on the revised CHIMPS (including users’ manual), see Alonso et al. (2006).

3. Anisotropic adaptive mesh refinement (AAMR)

One of the objectives of the present research in rotorcraft aerodynamics is to develop a computational approach that enables the accurate representation of the development of wake vortices and their interactions. Adaptive Mesh Refinement (AMR) allows efficient clustering of cells in the specific regions of interest. It is therefore a possible candidate to achieve the required high resolution in the vortical regions without affecting the grid elsewhere. The present implementation is an extension of the classical AMR technique to include anisotropic refinement, since mesh resolution in the wake is only required in the vortex core, where large gradients are expected. Along the vortex axis, the grid resolution can be relaxed quite dramatically, unless strong interactions between the vortices cause breakdown. The meshes are built (and refined) using a tool developed for the immersed boundary technique (Iaccarino & Ham 2005); any orthogonal coordinate basis can be used to take advantage of the quasi-circularity of the vortices in a rotorcraft wake. As illustrated in figure 1 for a hovering case, the grid alignment between the vortex and the azimuthal direction allows full advantage to be taken of the anisotropy. In comparison, the Cartesian grid with the same resolution requirement contains typically 50% more grid cells. Once the grid is generated, it is converted into an unstructured polyhedral mesh.

One of the difficulties in the application of AMR schemes is the definition of the region of interest (i.e. where to apply refinement). Typically a scalar quantity (marker) is defined and all the cells above a threshold value are split. In the present work, the use of anisotropic AMR introduces an additional difficulty: the refinement marker must be a vector. Our choice for a marker quantity is illustrated in figure 2. Due to the specific nature of the refinement requirement in this application, we have chosen a vortex-related
parametrization. The first observation is that, although we require a vector quantity, we first rely on the classical scalar detection and calculate a direction vector afterward. Helicity (the dot product of velocity and vorticity vectors) is used as a scalar marker in the present study, which is presented in the leftmost plot of figure 2. It allows vortex cores to be sharply detected and filters out the vorticity generated in the boundary layer and shed in the near wake. The vorticity vector is then used to identify the vortex axis. An example of the grid generated for a rotor in hover is also presented in figure 2.

The final and most complicated step in applying this technology to rotorcraft wakes is to embed the refinement procedure within the solution loop, as the vortices move within the domain of interest. The current implementation is based on a loose coupling between grid generator and flow solver. The mesh is regenerated at specific checkpoints during the calculations, usually every 100 time steps. Simple linear interpolation is used to transfer the solution between grids. To illustrate the application of dynamic AMR, three different grid systems and corresponding solutions are illustrated for the problem of a translating vortex dipole in figure 3. The grid is generated using the detection scheme previously described. The marker is then spread over a larger area by applying a
smoothing procedure: this allows a good solution accuracy to be retained as the vortex moves with the lapse of time. Two adaptive grids with different amounts of smoothing are shown in figure 3, along with a uniform grid as a reference. From figure 3, it appears that the amount of smoothing has a considerable effect on the preservation of vortices. In particular, the grid obtained by spreading the marker on a large area yields a solution almost comparable to that obtained from a fully uniform mesh. Note that the amount of smoothing applied to the marker is a function of the speed of vortices.

4. Validation on the forward flight: UH-60A

In the present study, the ability to handle the fluid–structure interaction is devised by coupling a structural analysis code, the University of Maryland Rotorcraft Comprehensive Analysis Code (UMARC), with the SUmb/CDP/CHIMPS flow analysis platform. The calculations are performed in a loosely coupled fashion (i.e. data exchange between CFD and CSD happens every revolution) to ensure stable convergence of aeroelastic motion, aerodynamic loading, and vehicle trim. Figure 4 shows grid topology used for the near- (SUmb) and off-body (CDP) meshes in this simulation. The total number of grid points for this simulation is approximately 6 million.

The fluid–structure analysis has been performed on a few forward-flight conditions for the UH-60A rotor where measured data are available from flight tests by Bousman et al. (1994). Results obtained for two critical steady-flight conditions are presented in this report: (1) high-speed forward flight (critical for high vibration); (2) high-altitude stall (critical for torsional loading and push-rod load).

The high-speed forward-flight condition is dominated by two critical flow phenomena that determine the phase and magnitude of the aerodynamic loading (Sitaraman et al. 2005). The first effect is the generation of large nose-down pitching moments in the advancing-blade phase because of the incidence of high transonic Mach numbers. These high nose-down pitching moments cause a large elastic torsional response that determines the phase and magnitude of the sectional aerodynamic lift as well (i.e. causes blades to be negatively loaded in the advancing-blade phase). Another dominant physical phenomenon is the blade–wake interaction, again on the advancing-blade phase. Blades with negative tip loading generate strong inboard wake sheets in contrast to strong rolled-up
tip vortices. These inboard wake sheets interact with the following blade and, due to their proximity and strength, cause appreciable impulsive perturbations in the sectional aerodynamic lift. This impulsive lift is the main contributor to fixed-frame vibrations at this flight condition.

Figure 5 shows time histories of aerodynamic loading obtained from the coupled aeroelastic simulation and the comparison with flight-test data. From the phase and magnitude of the normal force and pitching moment, it is evident that the coupled aeroelastic simulation accurately resolves the transonic effects in the advancing-blade phase. The evidence of impulsive loading is also found in the advancing-blade phase, suggesting the appropriate resolution for the phenomenon of blade–wake interaction as well. Overall, we obtained a good correlation between the flight-test data and computed aerodynamic loading for the entire rotor disk; this is also clear in contours of the normal force and its azimuthal derivative presented in figure 6.

For the high-altitude stall condition, the significant physical phenomena are the dynamic stall events in the retreating-blade phase, which are highly aeroelastic in nature. A high pitch-angle variation for generating large thrust at a high-altitude operating condition causes the first stall event (named trim stall). The trim stall event creates negative pitching moments, which cause an elastic torsional response that subsequently causes the flow to reattach and stall again (named torsion stall; Sitaraman & Baeder 2006). Therefore, a fully consistent fluid–structure coupling is absolutely necessary to obtain the correct phase and magnitude of the stall events. Present calculations show a good correlation with test data at this flight condition (figure 7), both in the phase and magnitude of the stall events: this is most evident in the time histories of pitching moment.
Figure 5. Time histories of the normal force (top), pitching moment (middle), and chord force (bottom) for the UH-60A high-speed forward-flight condition (8534). Solid lines and error bars represent simulation results and experimental data, respectively.

Figure 6. Contours of the normal force (top) and its azimuthal derivative (bottom) for the UH-60A high-speed forward-flight condition (8534). Experimental data and simulation results are shown on the left and right, respectively.
Figure 7. Time histories of the normal force (top), pitching moment (middle), and chord force (bottom) for the UH-60A high-altitude stall condition (9017). Solid lines and error bars represent simulation results and experimental data, respectively.

Figure 8. Contours of the normal force (top) and its azimuthal derivative (bottom) for the UH-60A high-altitude forward-flight condition (9017). Experimental data and simulation results are shown on the left and right, respectively.
and chord force. The contour plot of aerodynamic loading on the entire rotor disk, shown in figure 8, also exhibits a good agreement with the experiment, both in the location and magnitude of the stall events.

5. Validation on the descent flight: HART-II

The descent-flight condition for helicopters is dominated by blade–vortex interaction events that cause mid- to high-frequency impulses in aerodynamic loading and contribute significantly to the mid-frequency noise spectrum.

The SUmb/CDP/CHIMPS flow analysis has been conducted for a descent-flight operating condition, chosen from the HART-II wind-tunnel tests by Yu et al. (2002). Prescribed blade motions are used for this case in order to calibrate the resolution requirements for the flow analysis. Similarly to the UH-60A case, we initially coupled a single SUmb and a single CDP (without any mesh adaptation) for the near- and cylindrically shaped off-body regions, respectively, with approximately 6 million mesh elements in total. In this case, the wake effects could not be sufficiently accounted for. Consequently, we have devised a coupling of a single SUmb and two different instances of CDP (i.e. 3-mesh coupling), which capture the rotor-disk and wake regions, respectively. Since the near wake at this flight condition is populated by strong tip vortices, the conventional mesh generation would require an extremely large grid number. Therefore, we have applied the AAMR technique (described in Section 3) to the CDP domain for the wake region, where the CDP grids are pre-adapted based on the free-wake solution. Figure 9 shows the comparison of airload histories for the two different mesh systems. A significant improvement is noted in the aerodynamic loading with the 3-mesh coupling: the BVI events are evident on both the advancing and retreating sides. In the case of the 2-mesh coupling, the vortex/wake system is diffused quite significantly and the associated BVI events are completely smoothed out. Therefore, this simulation does provide proof-of-concept demonstration for the benefit of AAMR in predicting blade–vortex interactions. Nevertheless, it is to be noted that the mesh resolution for the 3-mesh coupling is still unsatisfactory for precisely accounting for the wake structure. The BVI events captured by the 3-mesh coupling are still much weaker than those demonstrated by the experi-
ment, which leaves room for improvement. A finer mesh system with similar topology will be applied in the near future.

6. Turbulence-model improvement for dynamic stall using LES database

Linear eddy-viscosity models have an inherent limitation in the correct representation of influence from system rotation or streamline curvature. Pettersson Reif et al. (1999) suggested a rotational correction for the $v^2/f$ model, where the eddy-viscosity coefficient is modified to mimic the second-moment closure in the case of isotropic turbulence that is subject to system rotation. This correction introduces a new eddy-viscosity coefficient, $C^*_{\mu}$, which is represented as a function of non-dimensional strain- and rotation-rate tensors, $\eta_1$ and $\eta_2$, viz.

$$ C^*_{\mu} = C_{\mu} \left[ 1 + \frac{\alpha_2 |\eta_3| + \alpha_3 |\eta_3|}{1 + \alpha_4 |\eta_3|} \left( \frac{1 + \alpha_5 \eta_1}{1 + \alpha_5 \eta_2} + \alpha_1 \sqrt{\eta_2} + \alpha_1 \sqrt{\eta_2} \sqrt{|\eta_1| - \eta_3} \right) \right]^{-1}, $$


where $C_{\mu}$ is the uncorrected eddy-viscosity coefficient, $\eta_1 = S_{ik}^* S_{ik}^*$, $\eta_2 = \Omega_{ik}^* \Omega_{ik}^*$, $S_{ik}^* = \frac{1}{2} T (\partial U_i/\partial x_k + \partial U_k/\partial x_i)$, $\Omega_{ik}^* = \frac{1}{2} T (\partial U_i/\partial x_k - \partial U_k/\partial x_i + 4.5 \epsilon_{kim} \Omega_{jm}^*)$, $\epsilon_{kim}$ and $\Omega_{jm}^*$ are the permutation symbol and angular velocity for system rotation, $T = \min [\max (k/\epsilon, 6 \sqrt{v/\epsilon}), a k/\sqrt{3} C_{\mu} S_{ik}^*]$, $S = \sqrt{2 S_{ik}^* S_{ik}^*}$, $S_{ik} = S_{ik}^*/T$, $\alpha_1 = 0.055 \sqrt{T_1}$, $\alpha_2 = \frac{1}{4} f_1$, $\alpha_3 = \frac{1}{4} f_1$, $\alpha_4 = \frac{1}{4} \sqrt{T_1}$, $\alpha_5 = \frac{1}{30}$, and $f_1 = \sqrt{v^2/0.367 k}$. Finally, the eddy viscosity is computed as follows:

$$ \nu_t = C^*_{\mu} v^2 T. $$

On the other hand, further analysis on homogeneous rotating turbulence shows that a constraint should be placed on $C^*_{\mu}$ in order to limit the turbulent production to levels attained by the original second-moment closure. In the present study, effects of this constraint are tested simply by introducing a limiter to $C^*_{\mu}$ as follows:

$$ C^*_{\mu} = C_{\mu} \min \left[ 1 + \frac{\alpha_2 |\eta_3| + \alpha_3 |\eta_3|}{1 + \alpha_4 |\eta_3|} \left( \frac{1 + \alpha_5 \eta_1}{1 + \alpha_5 \eta_2} + \alpha_1 \sqrt{\eta_2} + \alpha_1 \sqrt{\eta_2} \sqrt{|\eta_1| - \eta_3} \right) \right], $$


This variant was shown to provide an improved predictive performance for the laminarization near the core of wing-tip vortex in a static 3D airfoil (Duralaismy & Iaccarino 2005). In order to examine its performance in highly non-equilibrium wall-bounded flows, the correction is further applied to a 2D dynamic stall in the present study. For a precise assessment of accuracy, a flow over a pitching NACA0012 airfoil is computed using both LES and URANS.

The Reynolds and Mach numbers chosen are $Re = u_\infty c/\nu_\infty = 1.3 \times 10^5$ and $M = u_\infty/a_\infty = 0.3$, where $u_\infty$, $\nu_\infty$, and $a_\infty$ are velocity, kinematic viscosity, and speed of sound at the free stream, and $c$ is the chord length. A NACA0012 airfoil is pitching about the leading edge and the pitching condition is $\alpha(t) = \alpha_0 + \Delta \alpha \sin(\omega t)$, $\alpha_0 = 10^\circ$, $\Delta \alpha = 5^\circ$, $\beta = \omega c/2 u_\infty = 0.5$, where $\alpha(t)$ is the angle of attack, $\omega$ is the pitching frequency, and $\beta$ is the reduced frequency. Note that the static airfoil at this mean angle of attack has a small separation bubble near the leading edge, with transition in the separated shear layer. A fully compressible LES code, based on the sixth-order compact scheme and staggered arrangement of conserved variables (Nagarajan et al. 2003), and SUmb are
used for the present LES and URANS, respectively. For both LES and URANS, an O-mesh system with $480 \times 300$ points in the chordwise and normal directions, respectively, is used. The domain extends approximately $20c$ from the airfoil in each direction. For the 3D LES simulations, a rather small spanwise domain of $0.1c$ is used along with 128 grid points near the wall. This spanwise resolution is decreased away from the wall using a zonal approach. For URANS, three different variants of the $v^2-f$ model, the original uncorrected version together with the corrections (6.1) and (6.3), are considered. For all the computed cases, the simulation is initialized with the flowfield from the static-airfoil calculation at the mean angle of attack. For URANS, 1200 time steps per pitching period guarantee the time accuracy for the uncorrected version and (6.1), whereas 4800 time steps per pitching period are necessary for (6.3). On the other hand, 30,000 time steps per pitching period are used for LES.

Figure 10 shows contours of the spanwise vorticity from LES at four different time instants. This figure clearly shows the evolution of a dynamic-stall vortex (DSV) and its detachment. Formation of a large DSV is found around the mid-chord location during the downstroke (figure 10c) and it moves closer to the trailing edge at the instant of lowest incidence (figure 10d). When the DSV is in the vicinity of the upper airfoil surface, it induces a suction peak on the airfoil, leading to high lift. Figure 11 shows a comparison of the surface pressure distribution among the LES and three URANS simulations at four different time instants. The uncorrected $v^2-f$ model substantially underpredicts the suction peak throughout the DSV formation and detachment process. The rotational correction without the limiter, (6.1), introduces a certain amount of improvement over the uncorrected one, but it is still grossly inaccurate both in the strength and phase of the DSV. On the other hand, the rotational correction with the limiter, (6.3), shows a significant enhancement in the overall accuracy. It reproduces the amplitude of the suction peak quite closely, although it is still wider than that predicted by LES. Figure 12 shows time histories of lift and drag coefficients from the LES and URANS simulations. The superiority of the rotational correction with the limiter is especially prominent during the later stage of downstroke and earlier stage of upstroke (i.e. near the valleys of lift and drag coefficients), where the recovery from stall (decreasing drag coefficient) is predicted.
more accurately than the other $v^2-f$ variants. However, the maximum values of lift and drag still deviate considerably from the LES values. A proper modification of the limiter near the solid surface will be explored in a future study.

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