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### Prediction of Separated Flow Characteristics over a Hump using RANS and DES

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Predictions of the flow over a wall-mounted hump are obtained using solutions of the Reynolds-averaged Navier-Stokes (RANS) equations and Detached-Eddy Simulation (DES). The upstream solution is characterized by a two-dimensional turbulent boundary layer with a thickness approximately half of the maximum hump thickness measured at a location about two chord lengths upstream of the leading edge. The Reynolds number based on the hump chord length is  $9.75 \times 10^5$ . A slot at approximately 65% chord (C) is used for flow control via a spatially uniform (with respect to the spanwise coordinate) steady suction, and with alternating suction/blowing. Solutions of the two- and three-dimensional RANS equations are obtained using the Spalart-Allmaras and SST turbulence models. DES is applied to a three-dimensional geometry corresponding to an extruded section of the hump. DES predictions of the baseline case exhibit a threedimensional chaotic structure in the wake, with a mean reverse-flow region that is 20% shorter than predicted by the two-dimensional RANS computations. DES predictions of the pressure coefficient in the separated-flow region for the baseline case exhibit good agreement with measurements and are more accurate than either the S-A or SST RANS results. The simulations also show that blockage effects in the experiments used to assess the predictions are important - 3D RANS predictions more accurately predict the pressure distribution upstream and over the front portion of the hump. Predictions of the steady suction case show a reduction in the length of the reverse-flow region, though are less accurate compared to the baseline configuration. Unsteady 2D RANS predictions of the sinusoidal suction/blowing case are used to investigate impedance affects associated with increases in the driving velocity. The simulations show that a factor of four increase in the cavity driving velocity increases the average velocity through the slot by only a factor of 2.7.

#### Introduction

THE development of computational tools that can be used to guide and optimize approaches for controlling complex flows at realistic Reynolds numbers comprises a topic of significant current interest. Various active flow control concepts such as pulsed jets [1], piezoelectric actuators [2], and zero-net-mass oscillatory actuators (synthetic jets) [3] have been investigated in flight and through lab experiments. Such experiments have often demonstrated significant performance gains might be achieved through flow control. Computational Fluid Dynamics (CFD) offers a useful tool in understanding flow characteristics and studying the performance gains that can be achieved through flow control, though advances in several areas are needed to improve the robustness of CFD predictions for applications.

The flow fields for which control would be advantageous are complex – the flows are far from equilibrium, e.g., separated or on the verge of separation, and distorted by effects such strong pressure gradients and streamline curvature. This challenges simulation strategies since application Reynolds numbers are sufficiently high that empirical input to any modeling procedure appears unavoidable. In addition, it is desirable in many flow control applications to apply a "microscopic" (e.g., small-scale) input and achieve a desired "macroscopic" (large-scale) output. This implies a wide range in the geometric scales to be simulated, which in turn may challenge aspects such as grid design and construction, possibly more so than in other CFD applications.

Most predictions for engineering applications are obtained from solution of the Reynolds-averaged Navier-Stokes (RANS) equations. RANS models yield predictions of useful accuracy in attached flows as well as some flows with shallow separation. In spite of this usefulness, RANS techniques typically fail to accurately predict complex flow structures in regimes substantially different from the thin shear layers used to calibrate the underlying turbulence models. Simulation strategies such as Large-Eddy Simulation are attractive as an alternative

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for prediction of flowfields where RANS is deficient but carry a prohibitive computational cost for resolving boundary layer turbulence at high Reynolds numbers. This in turn provides a strong incentive for the merging of these techniques in hybrid RANS-LES approaches.

Detached-Eddy Simulation (DES) is among the actively applied hybrid RANS-LES strategies [4]. The method aims at entrusting the boundary layer to RANS while the detached-eddies in separated regions are resolved using LES. DES thus attempts to capitalize on the advantages of RANS and LES and in design applications, the "RANS region" of a DES comprises the entire boundary layer with the remainder of the flow, i.e., the "LES region", treated using a subgrid model. Predictions of the massively separated flows for which the technique was originally designed are typically superior to that which can be achieved using RANS models, especially of three-dimensional and time-dependent features (e.g., see [5] and references therein).

The objectives of the current effort are to apply and assess predictions using RANS and DES of a configuration with and without flow control and posing a significant challenge to predictive strategies. The particular focus of the work are calculations of the flow over a wall-mounted hump. The configuration is a Glauert-Goldschmied type body and corresponds to Case 3 from the recent NASA Workshop on CFD Validation of Synthetic Jets and Turbulent Separation Control. A detailed summary of the workshop is reported at this meeting by Rumsey *et al.*[6]. In the experiment, the hump model was mounted on a splitter plate between two glass endplates (depicted on the cover figure). Both the leading and trailing edges are smoothly faired to the solid walls upstream and downstream of the hump; the chord length is nearly eight times the value of the maximum hump thickness. The boundary layer experiences an adverse pressure gradient upstream of the hump, accelerates over the front convex portion of the body, and separates over a relatively short concave section in the aft region. A small slot extending across the entire length of the span and located at approximately 65% of the hump chord is used to introduce flow control, using either steady suction or sinusoidal suction/blowing. Detailed measurements were acquired for a baseline configuration (no flow control) in addition to the controlled cases.

Predictions are obtained of the steady-state flow via solution of the two- and three-dimensional Reynoldsaveraged Navier-Stokes (RANS) equations and of the three-dimensional and time-dependent flow using Detached-Eddy Simulation (DES). The RANS predictions are obtained using the Spalart-Allmaras [7] and SST [8] turbulence models. The RANS predictions of the two-dimensional flows correspond, nominally, to the central section of the wind tunnel. RANS predictions of the three-dimensional configuration include the influences of the endplates and model the blockage effects of the wind tunnel. As shown below, accounting for the confining effects of the wind tunnel walls is crucial to accurately predicting the pressure distribution over the hump to the point of separation. The DES predictions are of a subset of the test-section – a three-dimensional configuration produced by extruding the cross-section of the model into the spanwise direction.



Fig. 1 Outline of the surface-mounted hump configuration.

A side view of the hump geometry considered in the simulations is shown in Figure 1. The hump is smoothly faired to the solid walls upstream and downstream of the leading and trailing edges. In the experiments [6], the characteristic length is the hump chord, C = 16.536 inches. The maximum thickness of the hump is 2.116 inches, corresponding to a chord-to-thickness ratio of 7.81. The tunnel dimensions at the test section were 28 inches wide by 20 inches high. The hump model was mounted on a splitter plate resulting in a nominal test section height of 15.032 inches (distance from the splitter plate to the top wall).

As shown in Figure 2, a cavity is included in the computational representation of the hump. A slot at approximately x/C = 0.65, where the streamwise origin of the coordinate system coincides with the hump leading edge, is used to apply control via steady suction or sinusoidal suction/blowing, applied uniformly across the spanwise dimension of the configuration. For the case with steady suction, a constant mass flux is prescribed through the lower cavity opening. For the case with sinusoidal variation in the suction/blowing, the frequency of the mass flux exiting/entering the lower cavity surface was the same as in the experiments, i.e., 138.5 Hz, and with the mass flux along the lower cavity surface prescribed such that the maximum velocity through slot during

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#### Configuration

the blowing phase of the cycle was approximately 26.6 m/s (this condition corresponds to an oscillatory blowing momentum coefficient of approximately 0.111%). The reader is referred to the summary of the experiments reported by Rumsey and Gatski [6] for further details.

The computational domain extends 6.39C upstream of the hump leading edge, sufficient to allow the growth of a turbulent boundary layer having a thickness close to the measured value of about 1.1 inches at a location 2.14Cupstream of the leading edge as reported in the experiments. The total streamwise length of the computational domain is 12.74C, with the domain extending 5.35C downstream of the hump trailing edge. The wall-normal extent of the domain extends 0.91C from the lower horizontal surface, identical to the corresponding dimension to the upper wind tunnel wall in the experiments. The coordinate system origin is fixed to the leading edge with x aligned with the freestream (parallel to the lower flat wall), y normal to the horizontal lower wall, and z completing the right-hand rule.

#### **Turbulence Models**

RANS predictions are obtained using two turbulence models: the Spalart-Allmaras one-equation model [7] (referred to as S-A throughout) and the two-equation SST model of Menter [8]. All of the calculations summarized below are of fully turbulent flows, i.e., with turbulent boundary layers initiated along all solid surfaces of the computational domain.

#### Spalart-Allmaras

The Spalart-Allmaras RANS model solves an equation for the variable  $\tilde{\nu}$  which is dependent on the turbulent viscosity [7]. The model is derived based on empiricism and arguments of Galilean invariance, dimensional analysis and dependence on molecular viscosity. The model includes a wall destruction term that reduces the turbulent viscosity in the laminar sub-layer and trip terms to provide smooth transition to turbulence. The trip terms were not used in the present investigations and are therefore not included in the summary below. The transport equation for the working variable  $\tilde{\nu}$  used to form the eddy viscosity is written as,

$$\frac{D\widetilde{\nu}}{Dt} = c_{b1}\widetilde{S}\ \widetilde{\nu} - c_{w1}f_w\left[\frac{\widetilde{\nu}}{d}\right]^2 + \frac{1}{\sigma}\left[\nabla\cdot\left((\nu+\widetilde{\nu})\nabla\widetilde{\nu}\right) + c_{b2}\left(\nabla\widetilde{\nu}\right)^2\right],\tag{1}$$

where  $\tilde{\nu}$  is the working variable. The eddy viscosity  $\mu_t = \rho \nu_t$  is formed via,

$$\nu_t = \widetilde{\nu} f_{v1}, \qquad f_{v1} = \frac{\chi^3}{\chi^3 + c_{v1}^3}, \qquad \chi \equiv \frac{\widetilde{\nu}}{\nu}, \qquad (2)$$

where  $\nu$  is the molecular viscosity. The production term is expressed as,

$$\widetilde{S} \equiv S + \frac{\widetilde{\nu}}{\kappa^2 d^2} f_{v2}, \qquad f_{v2} = 1 - \frac{\chi}{1 + \chi f_{v1}}, \tag{3}$$

where S is the magnitude of the vorticity. The function  $f_w$  is given by,

$$f_w = g \left[ \frac{1 + c_{w3}^6}{g^6 + c_{w3}^6} \right]^{1/6}, \qquad g = r + c_{w2} \left( r^6 - r \right), \qquad r \equiv \frac{\widetilde{\nu}}{\widetilde{S}\kappa^2 d^2}, \tag{4}$$

with large values of r truncated to 10. The wall boundary condition is  $\tilde{\nu} = 0$ . The constants are  $c_{b1} = 0.1355$ ,  $\sigma = 2/3$ ,  $c_{b2} = 0.622$ ,  $\kappa = 0.41$ ,  $c_{w1} = c_{b1}/\kappa^2 + (1 + c_{b2})/\sigma$ ,  $c_{w2} = 0.3$ ,  $c_{w3} = 2$ , and  $c_{v1} = 7.1$ .

#### Shear Stress Transport

The Shear Stress Transport (SST) model was developed by Menter [8] to improve the accuracy of the  $k-\omega$  model for prediction of separated flows. The baseline version combines  $k-\varepsilon$  and  $k-\omega$  formulations, using a parameter  $F_1$  to bridge from  $k-\omega$  near the wall to  $k-\varepsilon$  in the freestream. The transport equations governing k and  $\omega$  take the form,

$$\frac{D\rho k}{Dt} = \tau_{ij} \frac{\partial u_i}{\partial x_j} - \beta^* \rho \omega k + \frac{\partial}{\partial x_j} \left[ \left( \mu + \sigma_k \mu_t \right) \frac{\partial k}{\partial x_j} \right], \qquad (5)$$

$$\frac{D\rho\omega}{Dt} = \frac{\gamma\rho}{\mu_t} \tau_{ij} \frac{\partial u_i}{\partial x_j} - \beta\rho\omega^2 + \frac{\partial}{\partial x_j} \left[ \left(\mu + \sigma_\omega \mu_t\right) \frac{\partial\omega}{\partial x_j} \right] + 2\rho \left(1 - F_1\right) \sigma_{\omega 2} \frac{1}{\omega} \frac{\partial k}{\partial x_j} \frac{\partial\omega}{\partial x_j}, \quad (6)$$

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where  $\tau_{ij}$  above is the (modeled) turbulent shear stress. The switching function  $F_1$  is given by,

$$F_1 = \tanh\left(\arg_1^4\right) \,,\tag{7}$$

$$\arg_{1} = \min\left(\max\left(\frac{\sqrt{k}}{0.09\omega y};\frac{500\mu}{\rho\omega y^{2}}\right);\frac{4\rho\sigma_{\omega 2}k}{CD_{k\omega}y^{2}}\right), \qquad CD_{k\omega} = \max\left[2\rho\sigma_{\omega 2}\frac{1}{\omega}\frac{\partial k}{\partial x_{i}}\frac{\partial\omega}{\partial x_{i}};10^{-20}\right].$$
(8)

The switching function  $F_1$  is also used to determine the values of the model constants. If  $\phi_1$  represents a generic constant in the  $k-\omega$  equations and  $\phi_2$  represents the same constant in the  $k-\varepsilon$  equations, then the model constants employed in (5) and (6) are determined by,

$$\phi = F_1 \phi_1 + (1 - F_1) \phi_2 \,. \tag{9}$$

In the baseline version of the model the turbulent eddy viscosity is determined as  $\mu_t = \rho k/\omega$ . The SST model limits the turbulent shear stress to  $\rho a_1 k$  where  $a_1 = 0.31$ . This in turn leads to an expression for the eddy viscosity,

$$\mu_t = \frac{\rho a_1 k}{\max\left(a_1 \omega; \Omega F_2\right)},\tag{10}$$

where  $\Omega$  is the absolute value of vorticity. The function  $F_2$  is included to prevent singular behavior in the freestream where  $\Omega$  goes to zero and is given by,

$$F_2 = \tanh\left(\arg_2^2\right), \qquad \arg_2 = \max\left(2\frac{\sqrt{k}}{0.09\omega y}; \frac{400\nu}{y^2\omega}\right). \tag{11}$$

The  $k-\omega$  model constants are given by  $\sigma_{k1} = 0.85$ ,  $\sigma_{\omega 1} = 0.5$ ,  $\beta^* = 0.09$ ,  $\beta_1 = 0.0750$ ,  $\gamma = \beta_1/\beta^* - \sigma_{\omega 1}\kappa^2/\sqrt{\beta^*}$ ,  $\kappa = 0.41$ . The values of the  $k-\varepsilon$  model constants are  $\sigma_{k2} = 1.0$ ,  $\sigma_{\omega 2} = 0.856$ ,  $\beta_2 = 0.0828$ ,  $\beta^* = 0.09$ ,  $\gamma_2 = \beta_2/\beta^* - \sigma_{\omega 2}\kappa^2/\sqrt{\beta^*}$ ,  $\kappa = 0.41$ .

#### **Detached-Eddy Simulation**

The DES formulation is obtained by replacing in the S-A model the distance to the nearest wall, d, by d, where  $\tilde{d}$  is defined as,

$$d \equiv \min(d, C_{DES}\Delta), \qquad C_{DES} = 0.65.$$
(12)

In Eq. (12),  $\Delta$  is the largest distance between the cell center under consideration and the cell center of the neighbors (those cells sharing a face with the cell in question). The location where d is determined by the grid spacing, i.e.,  $d = C_{DES}\Delta$ , defines the interface between the RANS region and the LES region. In applications for which the wall-parallel grid spacings (e.g., streamwise and spanwise) are on the order of the boundary layer thickness, the RANS region comprises most or all of the boundary layer and the closure applied is the S-A RANS model. In the LES region the closure is a one-equation model for the subgrid scale eddy viscosity. While most natural applications of DES treat the entire boundary layer in RANS mode, grid refinement in the wallparallel directions (both streamwise and spanwise) will cause the RANS-LES interface to move nearer the wall, activating the DES limiter and reducing the eddy viscosity below its RANS levels. This process can degrade predictions if mesh densities are insufficient to support eddy content within the boundary layer, resulting in lower Reynolds stress levels compared to that provided by the RANS model [4]. For the present configuration, the adverse pressure gradient upstream of the hump thickened the boundary layer. That feature combined with the mesh spacings for the current grids caused the RANS-LES interface to reside sufficiently within the boundary layer such that the upstream flow prediction was altered compared to that obtained using the RANS models. To provide an evaluation of the DES predictions against the RANS for nominally similar upstream conditions, RANS behavior was maintained to a position slightly upstream of the slot, i.e., d = d for x/C < 0.65.

#### Simulation Overview

#### Summary of the cases

The computations performed for each of the three cases – baseline (no flow control), steady suction, sinusoidal suction/blowing – are summarized in Tables 1-3, respectively. Each table reports the grid sizes, grid topology, whether the computation was 2D or 3D, the boundary conditions, and turbulence models employed for a given simulation (the "X" in the table indicating the simulation was performed). The nomenclature in the tables for the grid topology indicates the mesh was structured or unstructured and with additional details for the 3D

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	grid size	grid boundary		RANS		DES
		topology	conditions	S-A	SST	S-A
2D	$421 \times 109$	structured	slip top wall	Х	Х	
	$841 \times 101$	structured	slip top wall	Х	Х	
	$841 \times 217$	structured	slip top wall	Х	Х	
	$841 \times 257$	structured	slip top wall	Х		
	$841 \times 257$	structured	bl top wall	Х		
	$890 \times 257$	structured	bl top wall	Х		
	$1.14 \times 10^{5}$	unstructured	slip top wall	Х		
	$2.47 \times 10^{5}$	unstructured	slip top wall	Х		
3D	$841 \times \times 101 \times 41$	structured, periodic	slip top wall			Х
	$2.59  imes 10^6$	unstructured, half-geometry	bl all walls	Х		
	$4.90 \times 10^{6}$	unstructured, half-geometry	bl all walls	Х	Х	
	$10.72 \times 10^{6}$	unstructured, half-geometry	bl all walls	Х		

Table 1 Summary of the computations of the baseline (no flow control) case.

	grid size	grid	boundary	RANS		DES
		topology	conditions	S-A	SST	S-A
2D	$421 \times 109$	structured	slip top wall	Х	Х	
	$841 \times 101$	structured	slip top wall	Х	Х	
	$841 \times 217$	structured	slip top wall	Х	Х	
3D	$841 \times 101 \times 41$	structured, periodic	slip top wall			Х
	$4.90 \times 10^{6}$	unstructured, half-geometry	bl all walls	Х	Х	

 Table 2
 Summary of the computations of the steady-suction case.

	grid size	grid	boundary	RANS	mass
		topology	conditions	S-A	flux $(kg/s)$
2D	$841 \times 101$	structured	slip top wall	Х	0.0179
	$841 \times 101$	structured	slip top wall	Х	0.0140
	$841 \times 101$	structured	slip top wall	Х	0.0210
	$841 \times 101$	structured	slip top wall	Х	0.0280
	$841 \times 101$	structured	slip top wall	Х	0.0560

 Table 3
 Summary of the computations of the blowing and suction case.

computations as summarized below. The nomenclature for the boundary conditions "slip top wall" indicates a slip condition was applied to the upper surface of the computational domain, "bl top wall" indicates a boundary layer grid on the top wall and that the no-slip condition was applied, and "bl all walls" indicates boundary layer grids on all walls and the imposition of no-slip conditions on all solid surfaces. While results from each of the computations summarized in the tables is not presented in this manuscript, a more detailed summary is available at http://cfdval2004.larc.nasa.gov/results.html.

The predictions of the baseline case were performed using configurations that meshed the cavity and slot, though with the lower (horizontal) cavity surface closed. The steady-suction case imposed a fixed mass flux of 0.01518 kg/s (divided by the 23 inch span of the slot through which suction was applied in the experiments) along the entire lower cavity surface. The zero-net-mass-flux oscillatory suction/blowing was performed by prescribing a sinusoidal variation in the mass flux exiting/entering the lower cavity surface. The suction/blowing frequency was 138.5 Hz. A series of simulations were performed in order to deduce the required peak in the mass flux at the cavity opening to yield a slot velocity during the blowing phase of 26.6 m/s, corresponding to the case measured in experiments. The first entry in Table 3, a mass flux of 0.0179 kg/s, was the value employed in the simulations and corresponding to the measured case. As also shown in Table 3, additional suction/blowing rates were applied in order to investigate the characteristics of the jet velocity through the slot and impedance effects associated with large blowing coefficients.

For each simulation, the reference conditions corresponded to standard atmosphere. At the outlet of the computational domain the pressure was prescribed at 14.696 psia, the reference temperature was 519 Rankine,

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and the corresponding density specified using the ideal gas law. The reference Mach number was 0.1, leading to a Reynolds number per unit of length of  $5.899 \times 10^4$ , which yields a chord-based Reynolds number of  $9.75 \times 10^5$ .

#### Flow solver, grids, and timesteps

The compressible Navier-Stokes equations are solved using *Cobalt*. The numerical method is a cell-centered finite volume approach applicable to arbitrary cell topologies (e.g., hexahedra, prisms, tetrahedra) and described in Strang *et al.* [9]. The spatial operator uses the exact Riemann Solver of Gottlieb and Groth [10], least-squares gradient calculations using QR factorization to provide second order accuracy in space, and TVD flux limiters to limit extremes at cell faces. A point implicit method using analytic first-order inviscid and viscous Jacobians is used for advancement of the discretized system. For time-accurate computations, a Newton sub-iteration scheme is employed, the method is second-order accurate in time. The domain decomposition library ParMETIS [11] is used for parallel implementation and provides optimal load balancing with a minimal surface interface between zones. Communication between processors is achieved using Message Passing Interface.



# Fig. 2 Side view of the structured mesh, $841 \times 217$ cells. Left frame shows grid in the vicinity of the hump, the entire extent of the vertical dimension is shown. The right frame shows a zoomed view of the grid in the vicinity of the slot used for suction/blowing.

The influence of mesh density and grid topology was investigated using a series of calculations on structured and unstructured grids. The influence of the boundary condition applied to the upper surface of the computational domain was also investigated. Each of the unstructured grids and the structured grids employed in the 2D RANS calculations comprised of  $421 \times 109$  cells and  $841 \times 217$  cells were generated by Rumsey *et al.* [6]. A view of the finer mesh in the vicinity of the hump is shown in Figure 2, illustrating the clustering of points in the vicinity of the slot. Three additional structured grids were also employed in the 2D RANS study. The mesh comprised of  $841 \times 101$  cells was created using Gridgen, by coarsening the  $841 \times 217$  mesh in the wall normal direction. This grid was created in order to provide a more efficient mesh for subsequent DES calculations that extruded the geometry into the span. The remaining structured grids ( $841 \times 257$  cells and  $890 \times 257$  cells) applied a no-slip condition to the upper surface of the computational domain, using finer meshes near the upper surface to resolve the boundary layers.

With the exception of the grid comprised of  $890 \times 257$  cells, the upstream section of the computational domain extended 6.39 chords forward of the hump leading edge. The simulation domain for the case with the grid size  $890 \times 257$  extended 10.64 chord lengths upstream of the leading edge. The purpose of the computation was to investigate the influence of thicker upper-wall boundary layer on the pressure distribution over the lower surface (on which the hump is mounted). Along the lower surface of the domain from 10.64 to 6.39 chord lengths a slip condition was applied, with a no-slip condition applied at the streamwise location 6.39 chord lengths upstream of the hump leading edge. This in turn allowed the boundary layer to develop from the same upstream location as the other simulations. Though not shown here, the influence of the longer upstream extent did not result in significant differences in RANS predictions compared to those presented below.

All RANS predictions of the baseline and steady suction cases are of the steady state flow. The governing equations were integrated using large timesteps, corresponding to a  $CFL = 10^6$ . The predictions of the pulsed suction/blowing case were time-accurate. Based on the results of a timestep study (see Figure 6), the timestep for the blowing/suction case, non-dimensionalized by the hump chord length and freestream velocity, was  $8 \times 10^{-4}$  ( $1 \times 10^{-5}$  seconds). The mass flux values in Table 3 correspond to the peak values (divided by the 23 inch span of the slot).

A representative sampling of the influence of the mesh on the 2D RANS predictions is shown in Figure 3.



Fig. 3 Effect of mesh resolution and mesh type (structured or unstructured) on (a) pressure coefficient and (b) streamwise mean velocity profiles at x/C = 1.0.

Plotted in the figure are the pressure coefficient and mean velocity profile at x/C = 1.0 using two structured grids and two unstructured grids. The measurements of the pressure coefficient shows the development of the adverse pressure gradient prior to the leading edge of the hump, the acceleration of the flow to  $x/C \approx 0.5$ , and relatively strong adverse pressure gradient that develops prior to the location of flow detachment at  $x/C \approx 0.65$ . For the S-A RANS predictions shown in the left frame of Figure 3, the length of the recirculation bubble extends approximately 0.63C downstream of separation. The right frame of the figure showing the streamwise mean velocity profile illustrates that the station x/C = 1.0 is within the reverse-flow region. The figure shows that for each grid topology – structured and unstructured – there is insignificant variation in both the pressure coefficient and streamwise mean velocity at x/C = 1.0 with more than a doubling in the number of cells for both the structured and unstructured grids. Assessment of the pressure coefficient and velocity profiles from the 3D RANS predictions also exhibited grid convergence (not shown here though available at the CFDVal2004 web site cited earlier).



Fig. 4 Surface grid for the DES. Spanwise extent of the domain is 0.121C, extruded into the spanwise coordinate with a uniform grid spacing of 0.05 inches.

The parameters of the DES summarized Table 1 and Table 2 corresponded to prediction of the flow over a section of the hump. The grid with  $841 \times 101$  cells (and used in the RANS calculations as summarized above)

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was extruded into the spanwise direction to create the three-dimensional geometry. The spanwise dimension was meshed using 41 points with an equal spacing of 0.05 inches, leading to a spanwise period of 0.121 chord lengths. This grid spacing leads to a resolution of approximately 20 points per boundary layer thickness in the wallparallel directions (based on the reference location upstream of the leading edge) and also yields approximately cubic cells in the separated region downstream of the slot – the focus region [12]. Periodic boundary conditions were applied along the spanwise direction, indicated by the reference "structured, periodic" in the grid topology entry in the tables.



Fig. 5 Locations in the wake x/C = 0.8, x/C = 0.92, and x/C = 1.04 identifying positions where time-series were extracted (left frame) and the frequency spectra of the wall-normal velocity fluctuations at x/C = 0.92 (right frame). The wall-normal location of each coordinate is y/C = 0.093c.

A timestep study was performed using the DES prediction of the baseline configuration (no flow control). Dimensional timesteps of  $2 \times 10^{-5}$ ,  $4 \times 10^{-5}$ , and  $8 \times 10^{-5}$  seconds (corresponding to dimensionless values of 0.0016, 0.0032, and 0.0064 where the flow timescale is defined using the hump chord length and freestream velocity) were employed, the intermediate timestep yielding a CFL around unity in the focus region corresponding to the recirculating region downstream of the hump. Figure 5 shows the three locations, x/C = 0.8, 0.92, and 1.04, at which time series were extracted for calculation of frequency spectra. Shown in the right frame of the figure are the spectra of the wall-normal velocity fluctuations at the second tap location, x/C = 0.92. For the intermediate and smallest timesteps, 0.0016 and 0.0032, Figure 5 exhibits convergence in the higher frequency range for values smaller than about 3000 Hz, with clearly more frequency content compared to the coarser timestep of 0.0064. Consequently, the DES predictions reported below and summarized in Tables 1 and Table 2 were obtained using a dimensionless timestep 0.0032 ( $4 \times 10^{-5}$  seconds). Figure 5 also shows in the spectra a peak occurring at approximately 270 Hz, corresponding to a Strouhal number based on the hump height and freestream speed of around 0.43.

RANS predictions of the 3D geometry were obtained using a series of unstructured grids with resolutions ranging from  $2.59 \times 10^6$  to  $10.72 \times 10^6$  cells and generated using VGRIDns [13]. These meshes were of half the tunnel geometry, i.e., from the splitter plate to the upper wall, over half the spanwise extent of the lower test surface, including the endplates that are attached to the hump and with symmetry conditions imposed across the centerplane of the tunnel (indicated by the "half-geometry" entry in the tables). The 3D RANS predictions are of the steady state flow obtained by integrating the equations using large timesteps for a CFL =  $10^6$ . Tests of the time-dependent nature of the RANS flow fields were performed for a few select cases in which the RANS equations were integrated in a time-accurate fashion. These tests showed that the time-accurate RANS predictions evolved to steady solutions.

The 2D RANS predictions of the suction/blowing case were time accurate. The influence of the timestep on the slot velocity is shown in Figure 6. Results using four timesteps are shown, the figure illustrating convergence in the temporal evolution of the slot velocity for a dimensionless timestep of  $8 \times 10^{-4}$ .

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Fig. 6 Influence of the timestep on the average slot velocity for the case with sinusoidal suction/blowing. Timesteps are made dimensionless using hump chord length and freestream velocity.

#### Results

**Baseline and Steady-Suction Cases** 



Fig. 7 Streamlines and contours of the streamwise velocity in the S-A RANS prediction of the baseline case (left frame, upper row) and steady suction case (right frame, upper row) and vorticity isosurfaces (colored by pressure) in the DES prediction of the baseline case.

Contours of the streamwise velocity normalized by the freestream speed from the S-A RANS prediction and vorticity isosurfaces from the DES prediction of the baseline case are shown in Figure 7. Included in the RANS results are streamlines showing the recirculation zone that develops in the aft region of the hump. For the baseline case, the (steady) S-A RANS result shows that the boundary layer separates at about the location of the slot and with reattachment downstream at x/c = 1.28. For the case with steady suction shown in the right frame of the figure the streamlines indicate that the suction is not sufficient to eliminate the recirculation zone downstream of the slot, though comparison of the frames shows that the size (height and streamwise extent) of the separated region has been decreased compared to the baseline case. Reattachment is predicted at x/C = 1.124 in the 2D

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S-A RANS. Figure 7 shows that for the DES prediction, beginning in the region downstream of the slot there are a range of eddies resolved, consistent with LES treatment of the separated region.



Fig. 8 Pressure coefficient predictions. Baseline case (left frame), steady suction case (right frame).

The pressure coefficient from predictions of the baseline and steady suction cases are shown in Figure 8. The RANS predictions of the 2D solution are from the finest grids (841 × 217 cells) and from the intermediate grid for the 3D solutions ( $4.90 \times 10^6$  cells). In forming the pressure coefficient, for the 2D simulations the reference pressure was adjusted in order to match experimental measurements upstream of the hump leading edge. For the 3D calculations the pressure coefficient was computed using the reference conditions in the freestream at x/C = -2.14 (the coordinate origin x/C = 0 is at the hump leading edge), corresponding to the same reference location as used in the experiments.

Case	reattachment
	x/C
measurements	1.11
2D S-A	1.28
2D SST	1.28
DES	1.13
3D S-A	1.25
3D SST	1.24

Table 4 Reattachment locations for the baseline configuration (no flow control).

For the baseline case in Figure 8a, the experimental measurements show the development of an adverse pressure gradient upstream of the hump leading edge and with a region of flow acceleration beginning near x/C = 0 to about x/c = 0.5. Downstream of x/c = 0.5, the pressure gradient becomes strongly adverse with boundary layer detachment occurring near the slot (x/C = 0.65). The experimental measurements show the flow reattaches at x/C = 1.11, reattachment locations predicted by the models for the baseline case are summarized in Table 4.

Figure 8a shows that upstream of  $x/C \approx 0.25$  the 2D S-A RANS and 2D SST RANS predictions are essentially identical and in good agreement with the experimental measurements, reflecting comparable accuracy in predicting boundary layer growth upstream of the hump and the region of strong acceleration over the front of the hump. The peak suction predicted by the S-A RANS in the vicinity of x/C = 0.5 is in closer agreement to the measurements than obtained using the SST model, though both 2D RANS results are tangibly lower than the measured peak. The pressure coefficient sharply increases from  $x/C \approx 0.5$  to  $x/C \approx 0.65$ , a feature captured by the 2D RANS. Figure 8a shows the maxima near x/C = 0.65 predicted in the 2D S-A RANS is larger than obtained using SST, with the SST prediction exhibiting better agreement with measurements near the location of boundary layer separation. The 2D RANS reattach further downstream, at x/C = 1.28, than the location of mean reattachment reported in the experiments of x/C = 1.11 (c.f., Table 4). Figure 8a shows neither 2D RANS prediction of the pressure coefficient in the separated region (between x/C = 0.65 and x/C = 1.28) is in particularly good agreement with the measurements, rather each are shifted below the measured mean pressures

towards higher values. Figure 8a also shows that the recovery in the pressure coefficient following reattachment is different than measured.

The DES prediction yields a similar pressure distribution as the 2D RANS predictions to approximately the point of flow detachment, i.e., up to around x/C = 0.65. Following boundary layer separation, Figure 8a shows that the DES prediction of the pressure coefficient is substantially more accurate than using 2D RANS. In addition, mean flow reattachment in the DES occurs further upstream than in the 2D RANS with the DES predicting reattachment at x/C = 1.13. Downstream of reattachment, Figure 8a shows an improved prediction using DES of boundary layer recovery, though the mean pressure is slightly higher than measured for x/C > 1.2.

The under-prediction of the peak suction near x/C = 0.5 by the 2D RANS and DES motivated an investigation of mesh resolution, the influence of the upper-surface boundary condition, and the development of the upstream boundary layer over the lower (test) surface (c.f., Table 1). For the 2D RANS and DES predictions in Figure 8a, a slip surface was imposed along the upper boundary, i.e., zero tangential stress and zero surface-normal velocity. Though not shown here, the difference in the peak suction between the 2D RANS and measurements could not be accounted for via changes in the upper surface boundary condition, e.g., imposing a no-slip surface and resolving the boundary layer along the upper wall, and/or increases in mesh resolution.

The influence of blockage effects – arising from the wind tunnel walls and including the endplates mounted at the ends of the hump – on the pressure distribution were investigated using solutions of the 3D RANS equations. Figure 8a shows the results from the 3D RANS cases, using both the S-A and SST models. As apparent in the figure, the peak suction pressure is stronger in the 3D RANS compared to either the 2D RANS or DES. Both 3D RANS results shows good agreement with the measured distribution, a consequence of similar blockage now accounted for in the 3D computation. In addition, Figure 8a shows the pressure levels in the separated region are more accurately predicted in the 3D RANS, with the S-A result in slightly better agreement with the measurements. While the figure shows that the pressure levels predicted in the 3D RANS in the vicinity 0.65 < x/C < 1 are more accurate than in the 2D RANS, reattachment occurs in nearly the same location (c.f., Table 4) and Figure 8a shows a different recovery in  $C_p$  than measured.

The pressure coefficient predicted using the same techniques – 2D RANS, 3D RANS, and DES – for the steady suction case are shown in Figure 8b. As also observed in the baseline case, a similar adverse pressure gradient upstream of the hump is observed. For the steady suction case, Figure 8b shows that the peak negative pressure prior to flow detachment is higher. Similar to the behavior observed in the baseline case, the 2D RANS results under-predict the peak pressure before flow detachment due to blockage effects in the measurements that are not taken into account in the 2D RANS. It is useful to stress again that grid sizes for the 3D RANS computations are sufficient such that the pressure coefficient is grid converged. Thus, the results in Figure 8 reflect physical effects (blockage) rather than a numerical artifact due to inadequate resolution.

As previously illustrated in Figure 7, suction through the slot is not sufficient to fully attach the flow, though does reduce the size of the recirculation region. The measurements in Figure 8b shows there is a larger increase in the pressure both just downstream of flow detachment (x/C = 0.65) and in the vicinity of reattachment as compared to the baseline case. The 2D RANS predictions also exhibit larger pressure increases in the suction over the baseline cases, though the figure shows the overall agreement with measurements in the separated region is unsatisfactory.

The DES prediction for the steady suction case is very similar to the 2D RANS results prior to flow separation. The pressure rise following flow detachment is also in better agreement with measurements compared to the RANS, though Figure 8b shows the DES prediction of the steady suction case is less accurate compared to the baseline flow in Figure 8a. The figure shows that the variation in the pressure coefficient in the DES appears similar to the measured distribution, though with a downstream lag, i.e., toward larger x/C. In the present DES the development and growth of turbulent eddies in the separated region relies on amplification of the instabilities in the separated shear layer detaching from the hump. This process is not 'instantaneous' and the lag apparent in the pressure coefficient in Figure 8b is consistent with a delay in the development of eddies within the recirculating region, also consistent with the shallower separation compared to the baseline case.

The 3D RANS predictions of the steady suction case exhibit similar features observed for the baseline. The pressure distribution over the hump, prior to separation, is more accurately recovered than in the 2D RANS or DES. Also similar to the RANS predictions of the baseline configuration, the pressure levels in the separated region are more accurate than using 2D RANS, though the overall agreement with measurements for 0.65 < x/C < 1.3 is inadequate. Note also that evidence of the blockage effects are apparent in the recovery of  $C_p$  downstream of x/C = 1.3 where the 3D RANS predict the same pressure levels as measured and the 2D RANS and DES predicting higher pressures.

Predictions and measurements of the streamwise mean velocity at four steamwise stations, x/C = 1.0, 1.1, 1.2,and 1.3, corresponding to locations within and slightly downstream of the separated region are shown in Figure 9.

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Fig. 9 2D RANS and DES predictions of the streamwise mean velocity for the baseline case at x/C = 1.0, 1.1, 1.2, and 1.3.

At x/C = 1.0 the reverse-flow velocity very close to the wall is over-predicted using the S-A model, though there is very good agreement for y/C > 0.02. The peak reverse flow velocity at x/C = 1.0 predicted using 2D SST is closer to the measurements very near the wall though with the mean streamwise velocity under-predicted over most of the remainder of the profile. The figure shows that the DES prediction agrees very well with the measurements. With downstream evolution the 2D RANS results lag the measured profile, the S-A predictions in slightly better agreement with measurements. DES predictions are in better agreement with the experiments though exhibit a slight lag compared to the measurements near the outer layer of the profile.

Figure 10 shows the comparison of the 2D RANS and DES predictions to the measurements of the mean streamwise velocity profiles for the steady suction case. The predictions show larger disagreement with measurements than observed in the comparisons for the baseline case, especially in the region nearest the wall where both RANS and DES under-predict the velocity. The disagreement is anticipated based on the pressure distributions summarized above.

Figures 11 show the Reynolds stress profiles for the baseline and steady suction cases obtained using 2D RANS and DES. For both the baseline and steady-suction cases the DES results in the figure show that the modeled stress is essentially zero and that all of the Reynolds stress supported by the computation is being resolved. In the baseline case at x/C = 0.9 the overall agreement between the DES and measurements is adequate – both the maximum and location of the peak shear stress appear reasonably predicted. The 2D RANS predictions yield very similar profiles, a consistent feature for both cases and the two streamwise stations shown is the relatively low shear stress compared to the measured profile, which results in the larger separation bubble (e.g., reattachment length further aft than measured) than indicated in the experiments. As shown in Figure 11b, the resolved shear stress in the DES at x/C = 1.0 is substantially greater than the RANS results, though also above

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Fig. 10 Predictions of the streamwise mean velocity in the steady suction case at x/C = 1.0, 1.1, 1.2,and 1.3.

the measured profile. For the steady suction case, the DES prediction at x/C = 0.9 (Figure 11c) does not agree as well with measurements as the corresponding profile in the baseline case: the levels are lower than measured and the peak is further from the wall.

#### Sinusoidal Suction/Blowing

Streamlines and contours of the streamwise velocity normalized by the freestream speed from the S-A RANS prediction of the sinusoidal suction/blowing case are shown in Figure 12. The results shown in the figure correspond to the control case measured in experiments in which the maximum velocity through the slot during the blowing phase of the cycle was 26.6 m/s. As summarized in Table 3, this velocity corresponds to a mass flux over the entire slot of 0.0179 kg/s. In contrast to the baseline or steady-suction cases depicted in Figure 7, Figure 12 shows the influence of the suction/blowing leads to the formation of 'eddies' which rapidly develop from the slot and evolve downstream.

The time-averaged pressure coefficient for the case corresponding to Figure 12 is shown in Figure 13. The pressure distribution upstream and over the front portion of the hump, to about x/C = 0.5, is very similar to that measured and predicted in the baseline case (c.f., Figure 8). As previously discussed, the mismatch in the peak suction between the simulations and experiments near x/C = 0.5 is a result of blockage effects not modeled by the 2D computations. Just prior to the location of the slot (x/C = 0.65) there is an abrupt pressure rise that is slightly over-predicted in the calculations. The rollup of structures immediately downstream of the slot result in a secondary minima that occurs around x/C = 0.75 in the measurements and, less pronounced, in the RANS at around x/C = 0.70. Analogous to the behavior observed in the 2D RANS of the baseline and steady-suction cases, the predicted pressure distribution does not agree well with measurements further downstream of the slot.



Fig. 11 Reynolds shear stress in the baseline (a and b) and steady-suction (c and d) cases at x/C = 0.9 and 1.0.



### Fig. 12 Streamlines and contours of the streamwise velocity in the instantaneous solution from the S-A RANS prediction of the pulsed suction/blowing case.

The RANS predictions of the sinusoidal suction/blowing case were also used to investigate impedance effects associated with increases in the cavity driving velocity, specified via changes in the mass flux. In the incompressible limit, the volume flux exiting the slot would be identical to the volume flux through the cavity opening. The average fluid velocity through the slot would then be dictated by the area ratio of the slot to that of the cavity. For the current ratio of the cavity area to the slot area, the average slot velocity would be approximately 70 times the cavity velocity for an incompressible fluid. Compressibility effects will an introduce an impedance that

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Fig. 13 Pressure coefficient predictions for sinusoidal suction/blowing case.



 $V_{diaphragm}/V_{diaphragm,initial}$ 

Fig. 14 Ratio of the average slot velocity to the driving ("diaphragm") velocity.

will lower the volume flux relative to that which can be achieved in the incompressible limit, in turn lowering the average slot velocity which implies a less effective control.

A series of 2D S-A RANS calculations were performed to investigate impedance effects, the parameters are summarized in Table 3. The mass flux was increased by a factor of 1.5, 2, and 4 times an initial value of 0.0140 kg/s. Shown in Figure 14 is the ratio of the average velocity through the slot,  $V_{slot,avg}$ , during the blowing phase to the maximum driving velocity through the cavity,  $V_{diaphragm}$ . The slot velocity has been made dimensionless using the driving velocity, the horizontal axis of Figure 14 is non-dimensionalized by the diaphragm velocity for the initial calculation corresponding to a mass flux of 0.0140 kg/s. For the initial calculation,  $V_{diaphragm}/V_{diaphragm,initial} = 1$ , Figure 14 shows the average slot velocity is approximately 56 times the driving velocity of the diaphragm, lower than the maximum value of 70 that would be achieved in the incompressible limit. Figure 14 shows that for increases in the driving velocity. For an increase in the driving velocity by a factor of four (corresponding to a cavity mass flux of 0.0560 kg/s), the figure shows that the average slot velocity is about 38 times the driving velocity, corresponding to an increase by a factor of only 2.7 compared to the factor-of-four increase in the driving speed.

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#### Summary

RANS and DES were applied to prediction of the flow over a wall-mounted hump. The influence on the predictions of the grid topology was investigated in addition to grid refinement – in both 2D and 3D configurations – and the effect of the upper surface boundary condition. RANS predictions of the 2D flow using either grid topology and over a reasonable range of grid refinement exhibited insignificant changes. The 2D RANS predictions were not appreciably altered with changes in the upper surface boundary condition. The main motivation for investigations of the upper surface boundary condition arose from the mismatch in the peak suction pressure near the top of the hump, which was under-estimated in the 2D RANS as well as the DES. The larger suction in the experimental measurements of the pressure was more accurately recovered using 3D RANS that accounted for the confining influence of the wind tunnel walls and including the endplates attached to the hump. Grid refinement in the 3D RANS was essential for verifying this feature was not a result of numerical error arising from the use of coarse meshes.

For the baseline configuration (no flow control), the 2D RANS over-predicts the mean pressure in the separation zone. Comparison to the 3D RANS results show the 2D predictions are nearly a shift of the 3D pressure distributions, accounting for the confining effects of the wind tunnel walls that are more accurately captured in the 3D RANS. The DES predictions of the baseline configuration were encouraging – the three-dimensional structure resolved in the wake improved the physical description of the flow in the separated region and the quantitative prediction of the mean pressure distribution and mean streamwise velocity. For the flow with steady suction, the 2D RANS and 3D RANS yielded different predictions again due to different blockage effects.

DES predictions in the present investigations were obtained on a single grid, additional computations on coarser/finer meshes were beyond the scope of the current investigations, though would be warranted for a deeper examination on the technique in the current flow. One drawback of the DES as employed here is that the "RANS region" was maintained to the location of the slot, an undesirable feature in practice though compatible with the primary objectives of the work to gauge the feasibility and an initial assessment of the accuracy of the approach for a complex configuration directly relevant to flow control.

In spite of using only a single mesh, grid design was guided based on experience acquired in computation of other flows, e.g., attempting to ensure nominally cubic cells in the 'focus region' (separated region downstream of the slot), and choosing the timestep to yield a CFL of about unity based on the grid dimensions in the focus region [12]. A timestep study performed using the DES predictions of the baseline flow showed that for timestep employed in the current calculations, the frequency range resolved, based on spectra of the wall-normal velocity fluctuations, extended to approximately 3000 Hz.

While DES predictions of the baseline configuration were encouraging, the disagreement with experimental measurements for the steady suction case did not recover the pressure distribution nor the mean streamwise velocity in the separated region to similar accuracy. The relatively more shallow separation as compared to the baseline configuration poses a strong challenge to hybrid RANS-LES methods and highlights the need for incorporation into the simulations of strategies to seed upstream eddy content into the boundary layer – in regions where the boundary layer grid is sufficient to support eddies. The degree to which DES predictions would be improved by such a procedure poses an interesting question, one that deserves further investigations.

2D unsteady RANS was also used to predict the case with sinusoidal suction/blowing, the solutions exhibiting a time-dependent 'shedding' of vortical structures. That the mean pressure distribution did not exhibit good agreement with measurements again provides impetus for continued development and application of techniques such as DES. The 2D unsteady RANS were used to investigate impedance effects associated with increases in the driving force, i.e., mass flux (velocity) through the cavity opening. A factor-of-four increase in the cavity mass flux resulted in an increase of the average velocity through the slot by only a factor of 2.7 – an important result given the less efficient control that would likely result with increases in the driving force.

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