Reducing transonic wind tunnel sting interference effects for concealed store release testing

Graham Doig^{1,2}, Goran Bogdan², Kaveh R Kabir³ and Murray R Snyder^{4,5}

Abstract

Internal weapons bays are becoming increasingly common on aircraft for reasons of stealth and aerodynamic performance, and will be even more prevalent on coming generations of unmanned combat aerial vehicles (UCAVs). Wind tunnel testing of store releases to assess forces and moments for safety and clearance must be conducted with a store mounted to an angled strut rather than a conventional rear sting, to allow the full range of motion as the store “drops” from inside the aircraft. Interference from this strut can disrupt the flowfields and thus the reliability of moments obtained, and therefore an investigation was conducted to quantify the potential extent of discrepancies; original small-scale transonic wind tunnel testing was undertaken in a limited program which was supported by extensive numerical work. It was concluded that the precise geometry of the strut/store interface was of critical importance, with a typical design producing non-linear interference at high angles of attack. A simple improved design is proposed – making use of a blended interface and a more appropriate supercritical aerofoil strut cross section – yielding marked improvements in force and moment predictions.

Keywords

Store release, CFD, wind tunnel, transonic, sting interference

Introduction

In order to optimize both stealth capabilities and aerodynamic performance, it is increasingly common for modern military fighter/bomber aircraft to carry stores inside an internal cavity, typically and hereafter referred to as the weapons bay. New-generation advanced unmanned combat aerial vehicles (UCAVs) will be configured in this way as common practice.

Potentially complex behaviours of stores when released from aircraft must always be extensively tested to ensure no damage to the aircraft and no loss of expensive munitions; this has historically meant wind tunnel testing, which can be expensive (particularly if using a relatively large-scale transonic facility). More expensive still is actual flight testing, which is naturally the most realistic flowfield, but prohibitively costly and risky at the design phase. Data acquisition and repeatability are also issues in the latter instance. Effective and reliable wind tunnel testing is therefore essential prior to any flight testing, yet each new store design or modification must be individually flight tested, resulting in a large number of flights required; clearly, obtaining accurate

predictions from the wind tunnel can minimize any danger and down-time. It is now relatively common for computational fluid dynamics (CFD) work, particularly using Reynolds-Averaged Navier–Stokes (RANS) modelling, to be involved in the process to both predict design problems in advance of physical testing and, increasingly, to plan and help interpret the tunnel data itself before scaling up to real-world Reynolds numbers to anticipate further issues.^{1–5}

Mounting an aircraft tunnel model to one sting, and the store from the rear to another, is not applicable for weapons bay work where the store is originally in the cavity when ejected, before crossing the highly turbulent shear layer and proceeding to clear

¹Aerospace Engineering Department, California Polytechnic State University, San Luis Obispo, USA

²UNSW Australia, Sydney, Australia

³Macquarie University, Sydney, Australia

⁴George Washington University, Washington, DC, USA

⁵United States Naval Academy, Annapolis, USA

Corresponding author:

Graham Doig, California Polytechnic State University, San Luis Obispo, CA 93407, USA.

Email: grahamdoig@hotmail.com

the vicinity of the aircraft. An additional strut arm connecting to the middle of the store is one standard design approach (a very short rear sting and strut could interfere much more with the cavity flow), whereby the sting itself can be far from the store, reducing interference. However, the strut tends to be simplistic and produces its own effect on the store pressure distribution – the typical procedure to evaluate this involves a full sweep of angles and positions with a clean store, then a repeat with the strut arrangement; a form of superposition can then be undertaken in subtracting one result from the other, assuming that the difference between the two states should be relatively linear, predictable and consistent.⁶ Unfortunately, this is rarely the case, and particularly at high angles there appears to be increasing divergence in the two behaviours that can be attributed to unwanted strut interference. A representative diagram of the pitching moment issue, with inset images of a typical store/strut arrangement, is shown in Figure 1, based on prior studies.^{6,7} The behaviour of the store by itself is the ideal result which cannot be reproduced when it must emerge from a cavity, and a predictable, systematic offset occurs when the store is mounted to the strut and pitched at negative angles. At positive angles, a divergence in predicted pitching behaviour occurs such that the store by itself would continue to experience a linearly increasing moment. However, the strut interference effect induces a less-predictable trend.

While it may be possible to evaluate the differences and compensate at all conditions, this would have to be repeated for every single spatial condition for every strut and store combination, decimating the proposed advantages of an efficient CFD/tunnel program ahead of reduced flight testing for store certification.¹ It would be markedly more preferable to be able to trust results obtained with the strut knowing that behaviour would not change non-linearly or suddenly across the angle of attack, roll or yaw ranges.

A recent study by Finney and Hallberg⁶ compared computational fluid dynamics simulations with wind tunnel test data from the Navy Internal Carriage and Separation (NICS) cavity^{8,9} and provided a useful illustration of the strut interference problem. Forces were measured on a Mk-82 bomb model as it traversed the longitudinal axis of a cavity at several different bay depths, and a scenario similar to that shown in Figure 1 emerged. Unfortunately, no detailed description of the store support mechanism in the NICS test data was available, though a very simple strut was used with a long arm attached to the sting. Finney and Hallberg's CFD simulations showed little differentiation between simulations run on a clean store (with no attached strut) and the baseline Mk-82 with strut. Based upon the disparity between wind tunnel and their CFD results, Finney and Hallberg concluded that their strut was probably

Figure 1. Representative graph of the pitching moment interference issue.

not a representative model of that used in the NICS cavity, highlighting the need for CFD to properly reproduce the experiments in order to repeat the strut effects.

Although the comparisons at negative angle of attack were in good agreement with the test data, and matched wind tunnel data for an aft mounted sting for this store, there was substantial disagreement at positive angles of attack. A follow-up numerical study with a more representative strut indicated that there was significant deviation between store/strut CFD and clean-store wind tunnel results when angle of attack α exceeded approximately 10° ,⁷ and the discrepancy was not significantly aided by greatly increasing the length of the strut arm in an attempt to reduce any possible interference from the connecting sting pod at the bottom. The pod was found to be sufficiently far away already, and rather the junction between strut and store was identified as being a major source of flow disruption.¹⁰

The present work builds on these previous investigations which originally identified the interference issue^{6,7,11}; the study focuses on the influence of the interface between the strut and the store and is augmented by original wind tunnel testing to allow a more comprehensive validation of the numerical method.

Stemming partly from a very limited wind tunnel testing period, a highly synergistic integration of CFD and experiments was pursued following the success of previous investigations using the US Naval Academy (USNA) small-scale Transonic Wind Tunnel and RANS modelling.^{11–14} Simplistic CFD (not described here) was originally used to identify the flow features likely to be encountered, and then the same numerical approach was used to help design the wind tunnel experiments (anticipating blockage issues, wall effects, and the location of high gradients for pressure tapping, etc.). Three-dimensional printing of tunnel

models allowed quick redesign and manufacture for increased strength and rigidity, and the experiments were performed specifically with CFD validation in mind; for this reason, the floor and ceiling were kept fully closed (at least for the results reported here) and every condition of the experiment was noted such that CFD could be conducted with thorough testing of turbulence modeling and mesh parameters. Multi-variable testing allows for a more comprehensive assessment of numerical accuracy than, for instance, forces alone – therefore, surface pressure measurements were taken as well as forces, moments, and surface visualisation.

As a result of the approach, the study aims were threefold – to properly understand the nature of the store/strut interference issue, to propose a pathway for effective design to avoid such issues, and to evaluate the usefulness of a short-duration, tightly-coupled CFD and experimental program in which neither dataset is necessarily complete in a traditional sense. The following sections detail the experimental methodology first, then the numerical approach including validation and verification, before discussing the aerodynamics in more detail in order to provide context for the proposed strut redesigns that are expected to greatly improve results.

Experimental method

Testing in the US Naval Academy 8-inch by 8-inch transonic blowdown tunnel involved all models being mounted to the force balance sting by the rear of the store itself, as shown in Figure 2 (which also highlights other features of the experimental design and the model size and placement in the tunnel). This mounting allowed a more direct measurement of the influence of the strut arm and the original sting mount compared to a clean store-only case, and more importantly left the store free of wall interference close to the roof of the tunnel. A Mk82 geometry was simplified to include a more basic boat-tail than an actual store, and by removing the nose pod and other small geometric features.⁶

The strut thickness was 5.4 mm compared to the store diameter of 12.8 mm at its maximum, and featured sharp junctions to the store and the lower sting pod. The strut rear was a straightforward perpendicular angle to the freestream-aligned side, such that flow would separate entirely there, and the main arm was swept at 40° from the vertical to match the arrangement previously tested and reported in the literature.⁷ The strut leading edge expansion angle was 30°. Both this angle and the strut thickness were an exaggeration of the models in the literature on which it was based,⁷ to ensure sufficient strength in the Nylon to avoid breakage in the tunnel and to exaggerate the effects previously noted; later results showed this choice to have little bearing on the trends obtained compared to previous results.

Tests were conducted for CFD validation with the tunnel porous floor and ceiling fully closed (as the porous plate and plenum chamber would have been effectively impossible to model properly), though a separate set of test data was conducted with the porous floor and ceiling open to alleviate any wall effects. The second set of data is not described here. Therefore, while wall effects were certainly influential, they would also exist in the numerical model, ensuring an effective match of data sets – an estimate of wall effects is described in Figure 10. A description of the conditions tested, for both the store by itself and the store and strut, is shown in Table 1 with a summary of calculated errors.

The small scale of the model dictated a limited number of pressure tappings, particularly on the thin strut. For this reason, the three models were constructed from laser-sintered Nylon12 (sanded and painted to ensure a smooth finish, and measured for repeatability to within 0.1 mm of the store diameter before and after), featuring different arrays of pressure ports. With repeatability of the Mach number in the tunnel within a satisfactory range for the present purposes, this allowed the combination of data sets to provide overall adequate spatial resolution from 18 taps for CFD validation.

All geometries were sized to avoid excessive blockage rather than to achieve a specific Reynolds number; at zero angle of attack, the blockage of the store/strut/sting model (based on projected frontal area) was 1.4% or 2.3% if the portion of the rear force balance and sting support that sits in the rear of the test section is included. At 12° this was increased to 2.2%, which is considerable at $M_\infty = 0.85$ with closed walls, though blockage and wall effects were later quantified using CFD and found to be slight at all but the highest angles. The store-only configuration had a maximum blockage of 0.8% at 12°, and the discrepancies between model types and their blockages at different angles provoked minor adjustments to the stagnation pressures required to ensure that the same freestream Mach number was attained for each test.

Boundary layer transition was triggered with a small roughness line 2 mm from the leading edge of the store, strut and sting pod, and the incoming turbulence intensity of the tunnel for the measurement period was determined to be 0.12% through extensive calibration testing. Deflection of the model either through forces acting on the model and sting, or mild bending of the Nylon models, was measured using stills extracted from videos taken during the wind-on startup phase – values are reported in Table 1 (to the accuracy afforded by the resolution of the stills) and are higher at the highest angles tested: up to 0.25° above 8°. While CFD could have been useful to estimate this influence on the forces and moments measured, the values are presented as-is in

Figure 2. Relevant parameters of wind tunnel setup and simplified Mk. 82 store model.

Table 1. Tunnel parameters and measured error.

	Value	Estimated exp. error
Mach number	0.85	± 0.01
Angle of attack	-8° to 12° (+20 for CFD)	0.05 (at 0° wind off), 0.1 (at 0° wind on) to 0.25 ($\pm 8^\circ$ wind on)
Turbulent intensity	0.12%	± 0.015

subsequent charts without this effect included in the error approximations.

Due to the small-scale of the model preventing extensive quantitative pressure data (and limited time in the facility itself), multiple investigative techniques were employed to maximize available information about the flowfield and the store. Temperature-sensitive liquid crystal coating was applied for surface visualization, designed to facilitate a pinpointing of separation/reattachment or shock locations on the strut. This visualization was time-dependent, as the model cooled during the operation of the tunnel, thus allowing only a short window in which to capture the thermochromic liquid crystal (TLC) colour play. The TLC coatings were loosely calibrated but quantitative data was not the aim in this instance. A standard halogen light was used for illumination, and high-definition video was obtained at 60 fps – from this, stills were extracted for comparison to CFD.

A three-axis force balance was used to generate data for the forces and moments obtained during

multiple runs at Mach 0.85. Angles of attack from -6° to 12° were examined at 2° intervals, with a minimum of two runs per model for each of the models (to average results), giving a total number in excess of 130 tunnel runs. Data was acquired at 10 Hz for approximately 10 s of steady, established flow, and the values of all properties were averaged over the middle 5 s of this time in post-processing. Mach number typically varied by approximately 0.01 during any given data-acquisition period. All error plots in subsequent graphs are based on combinations of standard deviations in force or velocity measurements stemming from both repeatability tests and variations within each actual run. It is acknowledged that the 0.5 mm diameter of the pressure taps at the surface is large relative to the overall dimensions of the model. However, this could not be avoided to allow for practical manufacture and instrumentation; in critical areas, the pressure gradient is strong and thus a consideration of the average pressure in the tapping areas in the comparison CFD was required.

Numerical method

General approach

ANSYS Fluent 14 was utilized to generate all results discussed here. The code was run in pressure-based coupled mode on 64-bit machines. Convergence, typically obtained in less than 1000 iterations, was deemed to satisfy a minimum acceptable level when no significant ($<0.1\%$) variation in aerodynamic force coefficients was observed with extensive continued iteration (>1000 additional steps); most cases converged to a much lower level than this, and at normalized residual values of 10^{-5} or lower. Second-order upwind discretization was assumed to provide adequate accuracy in a computationally efficient manner, and the solver was configured to evaluate gradients at the cell centres. Results obtained with the Fluent density-based solver were highly comparable, but with run times of up to 150% compared to the pressure-based solver.

The model pivoted around the centre rear of the store for different angles of attack, same as in the tunnel tests. The test section was physically extended significantly downstream from the diffuser which exists in the real facility, to allow diffusion of flow features prior to the outlet boundary as an aid to the solver. Original inlet and outlet distances were $5L$ and $6L$ respectively – increases to $7.5L$ and $9L$, and $10L$ and $12L$ were examined and it was found that there were negligible changes ($<0.3\%$) to the aerodynamic forces and moment between the latter two cases (from $\sim 45\%$ change between the former two). Therefore, $7.5L$ and $9L$ were used for all wind tunnel simulations. Later cases featuring a full domain without tunnel walls had boundaries as indicated in Figure 3.

Tunnel wall boundary layer measurements were not available, but to avoid additional mesh complexity and solver time in each CFD run, simulations of the anticipated tunnel boundary layer thickness were used to deduct that thickness from the domain such that zero-shear walls could be implemented. Comparisons of both sets of results at 0° showed $<0.2\%$ difference in forces obtained, thus the simpler method was chosen for all subsequent cases.

Verification and validation

To test for mesh convergence, three cell densities were generated for the wind tunnel emulation models, featuring 2.7×10^6 , 5.0×10^6 , and 30.6×10^6 cells in total for the store/strut/sting arrangement (store-only meshes had cell counts approximately 20% less). In order, these meshes featured 120 lengthwise nodes on the store and 20 nodes around the circumference at the widest point, then 180 and 30, and 360 and 60 respectively. Lift and drag forces for the store only and store/strut/sting models were assessed at zero degrees α , the results of which are plotted in

Figure 3. Example of basic mesh layout (top) and domains for wind tunnel and 'free flight' runs.

Figure 4. It is clear that while all meshes of the store only configuration featured very little error due to mesh resolution, the strut/sting model was more sensitive; if the 30.6 million cell mesh is considered the most accurate, the 5 million cell mesh featured 5.1% error in lift and 1.8% error in drag. However, the additional computational expense of the larger mesh was not warranted for the lift number, as the store-only forces (extracted separately from the strut/pod model) from the full model were an order of magnitude lower and therefore in line with the true clean store numbers, indicating that the main discrepancies existed on the strut and sting pod rather than the store of interest. The errors of the 5 million cell mesh were placed in the context of those achieved in the experiment and deemed to be of an acceptable level for rapid turnaround of CFD cases; particularly since trends were of more importance than absolute accuracy for this particular investigation.

Once converged solutions were obtained, limited local mesh refinement was undertaken to improve shock resolution, typically adding 0.2×10^6 cells to the mesh. Very mild unsteadiness in some solutions – likely due to excessive shock diffusion or displacement across cells – was addressed in this way, though the effect on forces and moments was never significant. For later runs that were unconstrained by tunnel walls, the mesh cell counts were approximately 300,000 greater, but the mesh around the store was identical.

After arriving at the meshing strategy, the wind tunnel results were examined in conjunction with the numerical results, and are presented in this fashion

Figure 4. Mesh convergence for lift and drag coefficients, store with and without the strut/sting.

here. The pressure distributions at the tapping stations are presented in Figure 5 for the zero degrees α case. The CFD prediction has been afforded an approximation (thick grey band) of errors, including the way in which the pressure distribution varies across the 0.5 mm width of the pressure tap holes in the tunnel model.

The numerical results show good agreement with the measured data within the bounds of error and uncertainty, particularly with regards to the trends and well-defined points such as the stagnation location at the foremost point of the lower sting pod (station F, $x/c=0$), and the strut peak pressures. Correlation is acceptable on the wedge portion of the strut, but in all cases minor discrepancies emerge downstream of this region. A strong shock wave and potential for extensive separation on the strut were anticipated, presenting a complex flowfield exacerbated by vortices forming at the junctions between the strut and the store, as well as the strut and the lower sting pod (where stations A and E were located). These features, combined, present considerable challenges for RANS turbulence modelling – two common models for transonic aerospace applications were used for CFD comparisons: the 1-equation Spalart-Allmaras (SA) model¹⁵ and Menter's $k-\omega$ Shear Stress Transport (SST) model.¹⁶

At virtually all stations, the SA model predicted a greater suction peak in the presumably separated zone downstream of the wedge angle, and a milder gradient as the flow reattaches when compared to the SST results. As a result of this consistent behaviour, the results are ambiguous for determining superior model performance as no model is clearly better than the other for matching all three points at each station. Despite this, the matching of the trends across stations and in particular across the sting pod is encouraging, indicating that both models have a basic competence for the flowfield.

At 8° , shown in Figure 6, the models appear more closely matched for suction peak and reattachment behavior, and at station E the SST model now predicts a greater maximum low pressure on the wedge angle. This α featured considerably more interference from the strut, as can be observed later (Figure 12). While correlation remains good, again, particularly with regards to trends, a more notable set of discrepancies can be observed from the tappings downstream of the leading-edge wedge, indicating a stronger pressure gradient in the experiments than the CFD was able to predict at stations A to E. This suggests that the CFD was somewhat underpredicting the expansion of flow from the wedge onto the main (flow-aligned) side of the strut, potentially due to separation. A similar scenario in the expansion region at the rearmost tap of station E was seen in both cases, on the sting pod boat-tail.

For both angles presented here, the SA model appears better at predicting the flow over the sting pod, and marginally better at predicting C_P at the majority of strut stations. Without higher-resolution pressure data to draw on, it was decided that the SA model was the preferable option as it combined satisfactory performance with a slightly reduced computational expense. Ideally, well-calibrated pressure-sensitive paint would help greatly in helping differentiate the models. However, only thermochromic liquid crystal (TLC) coatings were available within the narrow budget and timeframe and these did not provide enough instantaneous clarity and range to help refine the analysis of the model discrepancies.

Nevertheless a comparison was made to images taken of the TLC tests; these are presented in Figure 7. In the images, and consistent with time even as the model cooled and the colour-change observed varied, a clear line could be seen on the flow-aligned side of the strut approximately 1.5 mm behind the wedge region. The CFD results indicated that the flow was separating at this point, remaining so over a large portion of the strut (though no significant vibration was observed or measured, this suggests the potential for strong buffet in different flow conditions). The three dimensionality of the flow due to the sweep angle delayed the separation from the wedge angle as seen in the inset of Figure 7, which shows pathlines in the cell next to the surface of the strut for two different angles. It is likely that the experimental separation line was slightly (<0.5 mm) rearwards of the predicted points due to the model paint coating blending the otherwise sharp angle to a small extent, helping the flow around the corner more effectively than in the sharp, idealized CFD model.

Overall, the match of the CFD and the experiment in terms of this separation line was deemed to be good other than at 8° , where the CFD separation line was considerably closer to the wedge angle, potentially due to the paint issue mentioned as well as slight

Figure 5. 0° Chordwise pressure coefficients at each strut station (c^* is local chord) vs. CFD predictions with SA and SST models.

differences in the angle of attack due to model deflection. With separation lines fairly constant over the whole strut, this further suggests that the influence of the lower sting pod is very slight and that a simple lengthening of the strut would be unlikely to influence the pressure distributions over the upper part of the strut and the store itself.

Forces and moments

The lift and drag force coefficients and pitching moment coefficients from the tunnel tests, presented for store-only and store/strut/sting configurations (using the superposition differential principle described in the introduction), are shown in Figures 8–10, respectively. All moments were summed based on a centre of pressure for the clean

store predicted at $x/l=0.485$. The trends of all three graphs are consistent with other reported studies on stores using typical strut stings.^{6,7}

The Figure 8 drag coefficient (C_D) plot indicates a distinct “bucket” shape for lower drag at low angles of attack, and a considerable increase at the higher angles where separation, particularly around the sharp fins, is experienced as opposed to the fully attached flow at low angles creating minimum drag conditions. The results obtained from the difference between clean store-only models and the strut-mounted versions (corrected) produced a consistently lower C_D across the whole range, and although if one looks at only the drag force of the store part of the store/strut arrangement, the CFD indicates that the drag coefficient (based on only the store frontal area) is higher – likely due to the strut interference including

Figure 6. $+8^\circ$ Chordwise pressure coefficients at each strut station (c^* is local chord) vs. CFD predictions with SA and SST models.

additional wave drag and boundary layer disruption as is discussed in the subsequent section.

The CFD results tend towards under-prediction of the drag coefficients, excepting the store only at high angles of attack ($>8^\circ$). The discrepancy may be due to the increasing error in true tunnel attack at higher angles, coupled with a tendency for the turbulence model to over-estimate the extent of separation from the fins and base of the model. Nevertheless, the “bucket” is less pronounced in the experimental results than with the CFD for the strut-corrected model, with higher measured drag – it is possible that the CFD may fail to accurately capture the extent of separation on the strut and original sting pod as α increases, though this is just one hypothesis and unfortunately cannot be established conclusively from the available pressure and visualization.

The lift coefficients in Figure 9 indicate that C_L is under-predicted with the strut-corrected version compared to the clean store, in both experiments and CFD. The CFD matches the experimental results more closely than for drag, and both results are within respective margins of error of each other apart from above $10^\circ\alpha$. The negative offset of lift at $0^\circ\alpha$ is a clear indication of the unwanted influence of the strut even when the store results are corrected for its influence (as C_L should be zero here), and the marked divergence of the coefficients from both types of model at α above 4° help to point to the pitching problem that the study set out to investigate.

Accordingly, the pitching moment coefficients (C_M) presented in Figure 10 highlight the expected result, being that the corrected store/strut moments begin to

Figure 7. Thermochromic liquid crystal visualization of strut separation lines for various angles (experiment denoted by white overlay, CFD prediction in black).

Figure 8. CFD comparisons to experimental results for drag coefficient vs. angle of attack.

diverge, increasingly, from the relatively linear store-only moments at angles of attack above 4° . At lower angles, the discrepancy is fairly constant and would lend itself to a simple corrective factor due to a very

Figure 9. CFD comparisons to experimental results for lift coefficient vs. angle of attack.

systematic offset. At higher angles, the discrepancy begins to become more exaggerated. The maximum angles tested did not fully explore the extent of the divergence, though this is shown more clearly in the

non-wind tunnel model results in the next section. The store itself in the store/strut model (from forces only acting on the store surfaces, extracted from the CFD) exhibits a moment which would not serve to return the store to a more neutral angle of attack, with the strut, the trend is correct still but further strut effects would be present in any experiment measuring the total moments of the full geometry so these moments are presented merely for comparison and as an aid to pinpoint the strut influence. Taken all together, the moments shown are indicative that the simplified

strut/store model presented here suffers the effects reported by others.

CFD results from the unbounded (no tunnel walls) model is included in Figure 10 as an indication that wall effects were slight, especially for the low-blockage store-only model. The higher-blockage strut model in the tunnel exhibited a general over-prediction of the moment with decreasing consistency in trends from point to point; however, the primary goal was to validate CFD ability to reproduce the important trends and flow features and therefore the CFD approach was well placed to evaluate proposed solutions to the moment divergence problem.

Further numerical analysis

The CFD was interrogated further to provide a clearer representation of the problems of such a strut. Figure 11 shows a visual representation of the regions of supersonic flow around the store. The CFD results highlighted the fact that a flow region greater than Mach 1 formed over the angle between the strut leading edge wedge and the parallel main section for all angles of attack with the strut model, aided by the separation bubble on the strut producing an aerofoil-esque flow curvature to follow.

The flow downstream of the strut wedge remains separated for a portion of the strut surface as was indicated by the TLC results, but the supersonic region around the surface maintains until further downstream, or into the wake at 8° and above.

Figure 10. CFD predictions of pitching moment coefficient vs. angle of attack.

Figure 11. Mach 1 iso-surface visualization for clean store and sting/strut configurations at various angles of attack.

Figure 12. Pressure coefficients on the surface of the store and strut for clean and sting/strut configurations at various angles of attack.

Notable from these graphics is a “creep” of the strut shock wave onto the store in the region of the junction interface, which is also in the vicinity of the store boat-tail (and therefore a region of strong pressure gradient). No such shockwave was observed for the store-only comparison model, which means that at zero α , a minor change in forces and moments could be expected. However, at the higher angles of attack, and particularly up to 14° — where the issue was expected to be more exaggerated— significant supersonic flow exists around a large portion of the lower store at the strut junction and downstream, and the vortex produced here could also be expected to exert a strong influence on the fins downstream. The additional low pressure associated with this effect is the overwhelming reason for the increasing divergence of the pitching moment predictions from store-only to store-strut models, mentioned at the start of this paper as being the primary motivation for this study.

This hypothesis is supported by Figure 12, which shows the surface pressure coefficient distributions on the store-only and store/strut/sting models at 0° , -4° , 8° and 14° . The clean store exhibits relatively benign changes as would be expected from the pitching moment results, with the rear fins providing a strong restoring moment coupled with the low pressure dip on the upper portions where the central body meets the boat-tail region. With the strut in place, what begins as a mild exaggeration and asymmetry of the C_p at low angles of attack becomes a very strong

interference at the higher end— the strut and the junction between it and the store become increasingly exposed to the oncoming flow. A combination of the strong vortex forming there and the effective reduction in strut sweep angle encouraging a stronger shock wave serves to significantly influence the pressure distribution, to the extent where the pitching characteristics of the store would become far removed from that which would be expected of a “clean” store. The lower portion of the boat-tail experiences strong low pressure (suction) behind the strut and the flow to the fins is greatly affected— these effects result in the divergent values of the store-only moments seen previously in Figure 10. The junction begins close to the store mid-point but ends far downstream, and it is further yet to the tail before these influences manifest themselves fully; thus the location of the strut junction appears to be poorly positioned, resulting in highly undesirable flow over the full boat-tail and fins where the moment-producing forces are most prominent.

Results of redesign

Two very simple redesigns were considered as initial steps that can later be optimized for use with specific geometries— results presented here indicate preferential characteristics for strut shape and location. While it is very likely that the strut could be less extensive (thinner, shorter chord) depending on the material

Figure 13. (a) Drag coefficient, (b) lift coefficient, and (c) pitching moment coefficient for clean store, original configuration (positions 1 and 2), and revised designs A and B at different positions, vs. angle of attack. (d) Store length-wise centre pressure coefficients for original and redesigned struts.

and strength needed, the overall length and maximum thickness were the only variables preserved. A double-wedge profile (REV A) was tested along with one featuring an aerofoil cross-section (REV B), chosen as a modified NACA0012 (SC-2-0012,¹⁷ exhibiting some of the rudimentary features of a supercritical section to delay shock onset). These can be seen at the top of Figure 13; not shown is the full strut, which was designed with variable sweep such that the angle formed a continuous curve away from the junction (at 23°) to the horizontal section required to mate to the original sting mount. This was designed to mitigate the onset of supersonic flow and associated wave drag and buffeting, providing a level of immunisation against unwanted reductions in the sweep angle with increasing store α . A more complex,

real-world strut could be designed to maintain an angle appropriate to the Mach number even as the store angle of attack changed, but the redesigns here were intended on being fixed, unhinged arrangements. Two positions were established, one at the site of the original strut junction (position 1), and another on the forward portion of the store (position 2). Both the original strut and the revised designs were evaluated at position 2, to provide a clearer comparison of the effect of moving the junction location.

The REV A junction centre was approximately coincident with the centre of pressure of the clean store, and REV B further upstream than this with a longer trailing taper on the junction. Simulations with the expanded computational domain (no tunnel walls) were run for the clean store and the original store and

Figure 14. Pressure coefficients on the surface of the store and strut for (top) clean and fore and aft REV B sting/strut configurations at 0° and 12° angles of attack.

strut (-8° , -4° , 0° , 4° , 8° , 12° , 16° and 20°), as well as the two new revisions (-8° , 0° , 12° , 16° and 20°).

Figure 13(a) shows that the drag under-prediction from the original strut results has largely been solved with both redesigns – differences of approximately 0.001–0.002 (typically 2–3%) in C_D exist across the α range with no additional corrections required, as opposed to 0.005–0.008 with the original strut. The aerofoil-sectioned REV B is marginally closer to the clean store plot, and performs better closer to the rear (position 1) than the forebody. Moving the original strut had the effect of going from under-prediction to over-prediction when only the store forces were considered, indicating a worse interference effect than previously. Figure 13(b) indicates that the predicted lift coefficients are similarly much improved with REV B (and slightly more accurate at position 2), to the point where the values are near-identical to clean store results at the higher angles. The REV A strut, however, over-predicts lift by a similar amount to that which the original strut under-predicts, caused by the large pressure gradient over the wedge at the junction, which gives the store a slightly higher pressure on the lower side towards the boat-tail than on top. A refinement of the junction could minimize this – the current design preserves the wedge shape into the store even with a fillet, whereas a more subtle blend would reduce this effect. The aerofoil section blend is naturally more gentle and tangential at all times to the store circumference, leading to a negation of this problem and an extremely mild effect on the store pressure gradients.

Accordingly, the moment coefficient graph in Figure 13(c) shows that in contrast to the increasing divergence of store moments from the original strut compared to the clean store, both revisions offer marked improvements to the moment fidelity, tending towards a slight over-prediction of the restoring moment at the highest angles tested. REV B offers

the superior match to the clean store C_M , staying more true to the plot at the highest angles and matching the trend of the curve better than the double wedge model – this is the result of largely shock-free flow over the whole strut, the careful junction blending, and the junction location just upstream of the centre of pressure; all three traits appear to be highly preferable and should be applied to future design optimizations. At low and negative angles, position 2 is preferable, but at higher angles where the original problem was most exacerbated, the rearwards position 1 is more effective. Moving the original strut to position 2 has a detrimental effect, in fact producing a more erroneous moment coefficient than the original.

Figure 13(d) shows that the REV B pressure profile around the mid-store (symmetry) plane matches the clean store profile in near-exact fashion, with the only exception being at the junction leading edge where C_P jumps by about 0.2 locally – the boat-tail C_P is negligibly affected. It offers a demonstrable improvement over the original strut interference and can be further refined to minimize the junction pressure spike in a similar means by which the extended trailing edge does. The pressure coefficient plots in Figure 14 clarify the overall strut interference for Rev B in both positions, and indicate that the forward positions at 0 and 12 degrees both have a slightly higher influence on the store than the rearward position.

Conclusions

The adverse interference effects caused by a strut sting for store release wind tunnel testing were investigated using a combination of CFD and wind tunnel experiments at Mach 0.85. The sting was based on ones typically used for this kind of work, and the store was a simplified Mk82. Blowdown wind tunnel tests were designed to be effective as a means of validating

the numerical model, which was used in turn to enhance the value of multi-variable wind tunnel results and elucidate, with higher resolution, the nature of the strut/store interference.

It was found that the junction between the strut and the store is by far the main cause in discrepancies between the sting model and the “store only” model free of any sting effects. The unrefined aerodynamic characteristics of the strut caused the flow to separate in places, whereas in the store-only case the flow is largely separation-free. The disruption of the strut contaminates the pressure distribution over the store, causing a shift in the predicted moment coefficient that increasingly deviates from the store-only case with increasing angle of attack. The strut blunt trailing edge was too close to the start of the store “boat-tail” section, causing a constructive interference effect that exaggerates the discrepancies, and also influences the flow reaching the fins and wake region. At high angles of attack, the effective sweep angle of the strut is reduced, leading to a stronger shock that eventually surrounds the store aft of the junction. A redesigned strut using a supercritical aerofoil section, variable sweep, and a careful blend of the store/strut junction was shown to provide marked improvements to the accuracy and consistency of predicted moments, such that little correction would be necessary to raw tunnel results. Although not pursued here, in an industrial study the validated CFD methodology could subsequently be applied to the full-scale problem in much more detail to obtain results for conditions which could not be examined in the tunnel.

Acknowledgments

Many thanks are extended to Alex Cenko for providing significant continuing advice and background information during this work. Dr Doig’s travel to the US Naval Academy was financed by the American Australian Association through a QANTAS Airlines-sponsored fellowship. The assistance of technical staff at the US Naval Academy is gladly acknowledged, particularly Rusty Foard, Fritz Woolford, George Burton and Matt Stanley.

Declaration of Conflicting Interests

The author(s) declared no potential conflicts of interest with respect to the research, authorship, and/or publication of this article.

Funding

The author(s) received no financial support for the research, authorship, and/or publication of this article.

References

1. Cenko A. Experience in the use of computational aerodynamics to predict store release characteristics. *Progr Aerosp Sci* 2001; 37: 477–495.
2. Cenko A. One CFD calculation to end point flight testing. *Aeronautical J* 2006; 110: 39.

3. Cenko A. Store separation lessons learned during the last 30 Years. In: *Proceedings of ICAS2010*, Nice, France, 19–24 September 2010.
4. O’Brien CW, Snyder MR, Hallberg EN, et al. Effects of targeting pod modification on F/A-18C Hornet weapons release. *Aeronautical J* 2012; 116: 743–755.
5. Finley DB, Messina T, Chankaya K, et al. Comparison of CFD-based simulation of external fuel tank separation with flight test. In: *52nd AIAA aerospace sciences meeting*, Maryland, National Harbor, 13–17 January 2014, AIAA Paper 2012-3337.
6. Finney L and Hallberg E. An investigation of cavity flow effects on a store in the vicinity of the shear layer. In: *International Test and Evaluation Association, 15th aircraft/stores compatibility symposium*, Fort Walden Beach, FL, 19–22 April 2010.
7. Snyder MR, Shah R, O’Brien CW, et al. Strut effects on store freestream aerodynamics. In: *29th AIAA applied aerodynamics conference*, Honolulu, Hawaii, 27–30 June 2011, pp.1–11.
8. Dix RE, Perkins TM and Grubbs MA. *Store loads, static and fluctuating pressures, and separation trajectories near a generic cavity*. TN: Arnold Engineering Development Centre, 1987.
9. Cenko A, Chen D and Turzanski R. Influence function method application to cavity flowfield predictions. *J Aircraft* 1989; 26: 760–764.
10. Doig G, Snyder M, Bogdan G, et al. Aspects of sting interference for transonic store release wind tunnel testing. In: *51st AIAA aerospace sciences meeting and exhibition*, Grapevine TX, 2013.
11. Doig G, Barber TJ, Neely AJ, et al. Experimental validation as an integral component of computational fluid dynamic research. *ANZIAM J* 2010; 51: 265–279.
12. Barber TJ, Doig G, Beves C, et al. The synergistic integration of CFD and EFD for ground effect aerodynamics studies. *Proc IMechE, Part G: J Aerospace Engineering* 2012; 226: 602–619.
13. Doig G, Barber TJ, Neely AJ, et al. Aerodynamics of an aerofoil in transonic ground effect: Numerical study at full-scale Reynolds numbers. *Aeronautical J* 2012; 116: 407–430.
14. Doig G, Barber TJ, Neely AJ, et al. Aerodynamics of an aerofoil in transonic ground effect: Methods for blow-down wind tunnel scale testing. *Aeronautical J* 2012; 116: 599–620.
15. Spalart P and Allmaras S. A one-equation turbulence model for aerodynamic flows. AIAA Paper 1992-0439, La Recherche Aerospatiale 1, 1994, p.5.
16. Menter FR. Two-equation eddy-viscosity turbulence models for engineering applications. *AIAA J* 1994; 32: 269–289.
17. Abbott IH and Von Doenhoff AE. *Theory of wing sections, including a summary of airfoil data*. Mineola, New York: Courier Dover Publications, 1959.

Appendix

Notation

- C_D drag force coefficient in the x -direction, based on projected frontal area

C_L	lift force coefficient based on projected platform area	k	turbulent kinetic energy
CM_α	pitching moment coefficient	l	store length (mm)
C_P	pressure coefficient	$M_{\infty \leftarrow}$	freestream Mach number
h	height of wind tunnel test section (mm)	α	angle of attack
		$\omega\psi$	specific turbulence dissipation