SUMMARY

1. PURPOSE. To provide security and policy review on the document at Tab 1 prior to release to the public.

2. BACKGROUND.

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Title: Closed-Loop Flow Control on a Tangent Ogive at a High Angle of Attack using Model Predictive Control

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Thesis/Dissertation Book Other: __________________

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Description: This conference paper shows the model predictive control to track arbitrary side force trajectories using port and starboard actuation. Off design cases are also explored and performance of the closed loop system is assessed.

Release Information:

Previous Clearance information: (If applicable)

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3. DISCUSSION.

4. RECOMMENDATION.

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Closed-Loop Flow Control on a Tangent Ogive at a High Angle of Attack using Model Predictive Control

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Active closed-loop flow control, a multidisciplinary field, has shown the potential to improve the performance of many systems involving fluid flows. A considerable amount of effort has been placed in developing reduced order models that represent the fluid dynamics of the system of interest while enabling modern control theory. Given all the effort into reduced order modeling, the resulting control algorithms tend to remain relatively simplistic. This paper looks to reverse that role, in which a simplistic model is used for model predictive control with the addition of a non-linear compensator. As a test bed, the side force generated on an asymmetric forebody at a 30° angle of attack is investigated. A Smith predictor is used to account for the convective time delay, increasing the reference tracking capabilities of the system as well as the frequency at which the system can reject disturbances. The Smith predictor uses a linear time-invariant model developed from open-loop dynamics as a reduced-order model. A combination of sigmoids are used to transform the output of the linear-time invariant model to appropriate mass-flow rates applied to either the port or starboard actuator at the nose of the forebody. The control system is implemented into full-order Navier-Stokes simulations. The results show that nonlinear control of this type is possible, but very sensitive to uncertainties in the model relative to the real system as illustrated by off-design test cases.

Nomenclature

\begin{align*}
A & \rightarrow I & \text{Transformation variables} \\
C & \text{Linear observation matrix} \\
C_{\mu} & \text{Momentum coefficient} \\
C_g & \text{Side force coefficient} \\
C_y & \text{Estimated side force coefficient} \\
D & \text{Diameter} \\
G_r(s) & \text{PI controller} \\
G_p(s) & \text{Delay-free LTI model} \\
G(s) & \text{LTI model} \\
L & \text{Length of the ogive model} \\
m & \text{Mass-flow rate out of actuator} \\
P & \text{Pressure} \\
s & \text{Laplace variable} \\
l & \text{Time} \\
V_{\infty} & \text{Freestream velocity} \\
V_{act} & \text{Velocity out of actuator} \\
x_s & \text{Surface pressure sensor location} \\
\delta & \text{Actuation angle} \\
K_p, T_W, \zeta, T_d, T_s & \text{LTI variables} \\
\tau & \text{Dimensionless time, } tU/L
\end{align*}

I. Introduction

Active closed-loop flow control has shown the potential to enhance the performance of many man-made systems involving fluid flow. However, the main stumbling block has been the design of controllers based on the Navier-Stokes equations. As such, research into closed-loop flow control has centered around sophisticated reduced-order modeling (ROM) approaches [1–5]. While the increase in ROM fidelity has provided more physical insight into the fluid dynamics of various flows fields, the developed control algorithms from these models ultimately remain relatively elementary. For example, proportional-integral-derivative (PID) [6–12] or fixed gain [13–15] control algorithms are typically adopted in the flow control research community. The improvement of model fidelity still result in simplistic control algorithms. This paper looks to modify this approach by developing a relatively simplistic reduced-order model and then implementing nonlinear model predictive control based on the model to account for time delays and nonlinear outputs in the plant.

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During the past two years, the Flow Control Research Group at the US Air Force Academy Department of Aeronautics has sought to extend previous closed-loop flow control work by implementing a feedback flow-control system on an axisymmetric forebody at a 50° angle of attack. When an axisymmetric forebody is at a moderate angle of attack, two vortices form on the leeward side of the forebody in an asymmetric pattern. As such, an asymmetric pressure distribution on the forebody creates a large side force and yawing moment. The cause of this is linked to a convective instability where small disturbances near the nose of the forebody rapidly grow as they propagate down the model causing one of the two primary vortices to separate earlier than the other. While this creates a large problem due to the out-of-plane loading that results, it also creates an ideal flow control opportunity where minimal actuation energy near the tip of the body can be used to help steer the vehicle. Due to the sensitivity of the flow to small geometric disturbances [16–18], nose bluntness [19–22], Reynolds number [22–24], and angle of attack [23–25], open-loop active flow control has shown the ability to alter the side force and yawing moments on various forebody configurations. However, the unpredictability of this flow field, given such a large parameter space, necessitates the use of feedback control to modulate the input disturbance and thus steer the vehicle in a controlled and robust manner. The reader is referred to two recent review paper [23,25] that provide a very good background of the communities current understanding of the flow and various solutions to mitigate the problem.

The idea that the asymmetric vortex problem requires a closed-loop flow control approach to control the side force and yawing moments on slender bodies of revolution at high angles of attack is not new. In fact, Bernhardt and Williams [9,26,27] as well as Patel et al. [28] have previously implemented different closed-loop flow control systems to either mitigate or reference track a prescribed side force. Furthermore, the authors have also implemented PI control of the asymmetric vortex problem, showing the ability to set-point track a prescribed side force and thus reduce the adverse effects of the phantom yaw problem [29,30]. As stated above, control of this flow field was limited to simplistic PI control. The problem with a classical control approach is this flow field exhibits two features that require de-tuning of the PI controller gains to maintain stability, thus reducing the performance of the closed-loop system. The first problem results from the convective instability, as discussed above, which exhibits both a dead-time and transition-time before the side force adjusts to changes in actuation at the tip of the model. The second is that the change in side force to various actuation inputs is highly nonlinear and dependent upon the initial (unforced) flow state. In an attempt to overcome the control problems associated with the first flow feature, Fagley et al. [31] used a form of model predictive control (Smith predictor) based on a linear time-invariant (LTI) model of the response in side force to different momentum coefficients applied to the port or starboard actuator. Results showed that the transition time to changes in a set-point during reference tracking could ideally be decreased down to the physical limit of the convective time delay. However, implementation of this control scheme still required that the Smith predictor gains be de-tuned due to the nonlinear response in the side force to different magnitudes of momentum addition from the actuators. In this paper, this nonlinear response is addressed through a numerical transformation, such that the implementation of the Smith predictor occurs in a nearly linear space where an LTI model better represents the dynamics of the actual plant.

II. Previous Work and Setup

Based on the previous work of others, the development of an active closed-loop flow-control system to take advantage of the convective flow instability was undertaken at the USAF Academy. As a starting point, the unforced dynamics of the flow around an ogive forebody with a sharp nose, fineness ratio of 3.5, Reynolds number of 156,000 based on the base diameter, and angle of attack of 50° was selected as the on-design test point. Furthermore, a small pin was placed on the starboard side of the model to help create a deterministic unforced flow state for the on-design test conditions. Unsteady DDES computational results were obtained using Cobalt V6.0 from Cobalt Solutions, LLC. The computations were performed on an unstructured grid containing approximately 16 million elements. All computations were second-order accurate in space and time, with a time step corresponding to a CFL number of approximately four. No turbulence model was used due to the low Reynolds number simulated and the results were compared to experimental results to validate the numerical setup [32]. For reference, the positive x-direction is along the center of the forebody, the positive y-direction is in the starboard direction, and the positive z-direction following a right-hand coordinate system.

In addition to the unforced computations, open-loop forcing computations were performed. To create a controllable disturbance to the flow, two actuators were placed near the nose of the forebody at 0° and 180° from the y-axis (starboard and port sides). This provided the ability to adjust the mass-flow rate of either the port or starboard actuator, as well as the angle (δ) in which fluid is injected into the flow. A schematic of the forebody is shown in Fig. 1. In
initial simulations presented in Porter et al. [32], the angle of the jet, $\delta$, was set to $30^\circ$ to mimic the plasma actuators used in a companion experiment [12]. Below, a new open-loop database is presented in which this angle is adjusted to $90^\circ$, or normal to the surface of the forebody.

As shown in Fig. 1, four pressure transducers were also placed on the model. The location of the pressure transducers was determined to provide an optimal instantaneous estimation of the side force, $C_y$, based on linear estimation scheme using the CFD results from the unforced and open-loop data sets,

$$C_y = CP(x_s, t) = C_1 P_1 + C_2 P_2 + C_3 P_3 + C_4 P_4.$$  \hspace{1cm} (1)

Following the development of both the unforced and open-loop flow-state databases, a reduced order model was developed. The reduced order model served as a plant for the controller development. This allowed for rapid testing and iterations of various control schemes while capturing the relevant dynamics of the actual system. After several iterations, ranging from refining the gains of the LTI model to development of a nonlinear Hammerstein-Wiener model, convergence to acceptable PI gains were determined and implemented into Navier-Stokes feedback simulations, Fig. 2 [32]. As shown, the feedback system was able to set-point track three different reference signals, each with different actuation requirements. While the results show the controller is fully capable of set-point tracking, the results are still sub-optimum, although they are the best results to date. For instance, when the controller is turned on ($\tau = tU/L = 0$), the side force transitions from its unforced value to its set-point in approximately $\Delta \tau = 1.4$, whereas open-loop results indicate that this transition can occur as fast as $\Delta \tau = 1$. As stated above, this was due to the de-tuning of the controller to maintain stability and robustness. Furthermore, large unsteady fluctuations in the controlled side force occur around $\tau = 8$ resulting from an amplification of disturbances and the nonlinear nature of the flow. In fact, this phenomena was predicted by an analysis of the the feedback control system with the final LTI reduced-order model [32].

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**Figure 1.** Model geometry of a von Kármán ogive with fineness ratio of 3.5: a) schematic of the model [38], and b) definition of mass-blowing angle.

![Model geometry of a von Kármán ogive](image1.png)

**Figure 2.** Navier-Stokes feedback flow control results to reference track a given side force. All simulations begin with a step change in the reference to $C_y = 0.58$. A second step change in reference is then made to $C_y = 0.58$ (blue), $C_y = 0$ (green), and $C_y = -1.0$ (red). The reference signals are shown as black dotted lines [33].

![Navier-Stokes feedback flow control results](image2.png)
III. Results

A. Open-Loop Database

With the relatively successfully PI control of this flow field, a more advanced control algorithm is sought to overcome the problems arising from the convective time delay and the nonlinear response of the side force to changes in mass-flow rate of either actuator. The previous closed-loop results shown above, Fig. 2, were all obtained with the actuators set to blow at $\delta = 30^\circ$. It was postulated that blowing at a $30^\circ$ angle created the nonlinear steady-state response in side force as the mass-flow rate changed. In an attempt to get around this phenomenon, new open-loop investigations were undertaken in which the angle of the actuation jets were changed to $\delta = 90^\circ$, or normal to the surface. Multiple open-loop simulations were performed where the magnitude of the mass-flow rate of either the port or starboard actuator was adjusted through step inputs. The steady-state results of the normal blowing response ($90^\circ$) are shown in Fig. 3 as a function of mass-flow rate, the blowing coefficient ($V_{act}/V_{in}$), and the momentum coefficient ($C_p$). Positive values correspond to starboard actuation and a side force in the starboard direction, while negative values indicate port actuation or a side force in the port direction.

In addition to the new open-loop database generated, Fig. 3 also shows the previous results obtained in which the jet from each actuator exited at a $30^\circ$ angle. Furthermore, Fig. 3 shows an off-design open-loop data set ($90^\circ$, off-design), in which the pin on the starboard side of the forebody was moved to the port side of the forebody to create a different deterministic unforced side force. This off-design data set was generated using normal blowing and will be discussed below when looking at the performance of feedback at off-design. This is deemed an off-design test case since a different unforced flow state is present (positive side force as opposed to negative) and therefore the response to forcing follows a slightly different path. This is representative of applying the feedback controller to multiple bodies at the same flow conditions, where geometric machining tolerances create different geometric disturbances and therefore natural flow states. As shown, since the two follow different forcing paths, prescribing a momentum coefficient out of the port actuator does not yield a deterministic side force which is exactly the reason that open-loop table-lookup type approach cannot work in real life.

The data in Fig. 3 is plotted as a function of three different input quantities to determine the best input for developing a reduced-order model. Note that “best” simple refers to the one that yields the least amount of error using the form of reduced-order model discussed below, and in no way implies an optimum variable for model-development. In Fig. 3, by definition the mass-flow rate is only dependent upon the normal velocity component out of the actuator, whereas the blowing coefficient and momentum coefficient are dependent upon the magnitude of the velocity out of the actuator. For normal blowing, these are basically the same, but normalized differently. However, at $\delta = 30^\circ$, the mass-flow rate does not account for the velocity component tangential to the flow. In previous reduced-order models and feedback control simulations [32], such as those shown in Fig. 2, the control output was a momentum coefficient for each actuator. In Cobalt, this required a transformation from momentum coefficient to mass-flow rate which was accomplished using a fourth-order polynomial fit. For the model developed below, the mass-flow rate was used to eliminate this transformation and a potential source of error. Therefore, the mass-flow rate was determined as the “best” input variable for the reduced-order modeling since all exhibit the same nonlinear trends, but the mass-flow rate eliminates errors associated with additional transformations.

It is interesting to note that when the data between the three cases shown is plotted as a function of the mass-flow rate, they nearly collapse over a range of mass-flow rates ($0.03 \leq \dot{m} \leq 0.07$). The off-design starboard actuator was not tested during open-loop simulations to reduce computational costs. However, when the normal blowing and $30^\circ$ blowing test cases are compared using either the blowing coefficient or momentum coefficient, large differences are observed, with the normal blowing cases appearing to have a more “bi-stable” type appearance in which the side force saturates much quicker (i.e. the slope is much steeper in this range). At these flow conditions however, the flow is not “bi-stable” by the standard definitions in that it always returns to its forced flow state when forcing is turned off, rather the region of proportionality is very narrow when blowing normal to the surface.

While normal blowing did not eliminate the nonlinear steady-state response for which it was originally designed, it was chosen because it still provided a larger range of side forces. Furthermore, normal blowing appears to be more efficient than blowing at a $30^\circ$ angle since much smaller momentum coefficients are required to achieve a given change in side force. Finally, normal blowing appears to eliminate one source of nonlinearity associated with a Coanda jet forming at high forcing amplitudes when forced at a $30^\circ$ angle. As shown in Fig. 3, at a blowing coefficient of 1, the $30^\circ$ data begins to change direction. At a blowing coefficient of one, a wall jet is formed causing the vortex on the side being force to stay attached to the forebody. However, this clearly has been eliminated from the normal blowing data set as a wall jet is not able to form when blowing normal to the surface.
Figure 3. Steady-state side force to step inputs applied to the port (negative values) or starboard (positive values) actuator as a function of:

a) mass-flow rate, b) blowing coefficient, or c) momentum coefficient.
B. Model Development

To develop a linear time-invariant model, a dead zone was implemented before model development. The dead zone required a minimum mass-flow rate \(|\hat{m}_n| > 0.03\) out of either actuator. This eliminated some of the nonlinearity seen in Fig. 3 at the very low forcing amplitudes. To account for the remaining nonlinear response in forcing, the data was fit with a system of exponential sigmoid functions of the form,

\[
C_{y,\text{tran}} = \frac{A}{B + e^{-C(1000\hat{m}_n - D)}} + \frac{E}{1 + e^{-G(1000\hat{m}_n - H)}} + I. \tag{2}
\]

This transformed the forcing input into a space where the forcing amplitude and side force were approximately linear, in a least-squared sense. The advantage of using Eq. (2) is that it naturally saturates at a given value preventing the controller from applying non-realistic forcing magnitudes. Furthermore, the shape of the exponential sigmoid is similar to the open-loop data (once the dead-zone is removed), such that once transformed, a linear time-invariant model could be developed to replicate the dynamics of the actual system with minimal steady-state error. Once this transformation was applied, a linear time-invariant second-order model with one zero and a pure time-delay of the form

\[
G(s) = K_p \left[ \frac{1 + T_z s}{1 + 2\zeta T_z s + (T_z)^2} \right] e^{-T_d s}, \tag{3}
\]

was developed, where \(K_p = 0.975\), \(T_w = 2.88 \times 10^{-3}\), \(\zeta = 0.77\), \(T_z = -7.1 \times 10^{-5}\), and \(T_d = 0.006\). Figure 4 shows the transformed data, along with the steady-state response predicted by the LTI model. As such, the LTI model serves as the plant for controller synthesis, as well as the model for predictive control. During feedback, the output from the controller (input to the actuators) is converted back into actual mass-flow rates by inverting Eq. (2).

![Figure 4](image)

**Figure 4.** Open-loop data a) fit with Eq (3) after removal of a dead-zone, and b) in transformed space using Eq. (2) and the corresponding steady-state value from the LTI model (Eq. (3)).
C. Smith Predictor Architecture

As shown above, the system dynamics relating the input forcing to either the port or starboard actuator are well modeled by a second-order system with one zero and a pure time delay in a transformed space. Note that the transformation above is not unique, and many different transformations could be used to obtain similar results. In transformed space, using the LTI model, Eq. (3), a Smith predictor control law was developed. Figure 5 shows a schematic of the Smith-predictor architecture. The Smith-predictor consist of two feedback loops. The inner loop compares the model delay-free side force to the reference, while the outer loop compares the actual side force to the predicted output, where the time delay is accounted for. The difference between the actual side force and the predicted side is the effective difference in the output after accounting for the time-delay.

\[ G_f(s) = \frac{1}{0.0002s + 1}. \]  

Finally, the gains of the Smith predictor based on the transformed LTI were adjusted to have a crossover frequency of 350 radians/sec, which corresponded to approximately one flow through time with a phase margin of 60°.

D. Smith-Predictor Response

To test the Smith predictor, closed-loop Navier-Stokes simulations were performed using the controller architecture described above. The feedback simulations were conducted using the unforced data set from the open-loop database. The goal of the simulations was to test the controller's capability to set-point track. Therefore, the initial reference in side force was set to 0.58 and the controller was turned on at \( \tau = 6.5 \). The reference was held constant until \( \tau = 15.19 \) at which point the reference was set to zero. Finally, at \( \tau = 23.8 \) a sinusoidal reference signal was input with an amplitude of two and frequency of \( f_T = 0.25 \) (one cycle every four flow through times). Figure 6 shows the resulting side force during nonlinear model predictive control. Generally the controller was able to set-point track and follow the input reference signal. When the controller is turned on, after the initial dead time associated with the convective instability, the side force begins to transition towards the reference of 0.58. In fact, the side force first reaches this reference in \( \Delta \tau = 1.13 \), which is very close to the prescribed crossover frequency of the controller. When compared to the PI results [32], the response time of the controller was significantly increased as the PI control case crosses the set-point in \( \Delta \tau = 1.43 \). However, the nonlinear Smith predictor controlled side force overshoots and oscillates around the set-point. Since each case was run from different unforced starting point, the initial error when control is turned on is different. Therefore a true comparison of the performance of each actuator can not be directly made. While it looks like the nonlinear Smith predictor test case has a larger overshoot, and potentially more unsteady oscillations about the set-point, this could be the result of a larger associated error term driving up the integrator in the Smith predictor. In contrast, at the end of the PI control case at a reference of 0.58, a large oscillation appears, while it appears the Smith predictor is beginning to stabilize. In the end, both appear to be comparable given the limited data of one simulation for each feedback simulation.
Figure 6. Set-point tracking a prescribed side force (dotted line) using a nonlinear Smith predictor control architecture compared to the original PI feedback results [32].

Figure 7. Actuation output during feedback control using a nonlinear Smith predictor control law.

When the reference is changed to zero, the side force again begins to transition to this new reference after the initial dead time associated with the convective instability. As observed with the original PI results [32], this is a much slower transition due to the accumulation of error in the integral term. In this case, both the nonlinear Smith predictor and the original PI controlled side force first cross zero at almost the exact same time. However, the Smith predictor appears to have a harder time holding this set point as the fluctuations are noticeably larger than the fluctuations observed with the original PI controller. Note however, at this reference value, the set-point is at a point where the slope of the line relating the mass-flow rate and the resulting side force is very steep and the flow is the most unstable. The steepness of the response function means that small uncertainties in the model have a much larger effect than those associated during the original PI development, potentially pointing towards a potential pit-fall of nonlinear Smith control type architectures for flow control using simplistic models.

Finally, the controller was tested using the sinusoidally varying reference signal. Due to the dead time associated with the convective instability, the actual side force always lags behind the reference signal by the associated dead time in the system. However, it is generally able to follow the reference signal, although it cannot achieve the maximum and minimum values prescribed. Figure 7 shows the output of both the port and starboard actuators during the feedback run. As shown, during the periods where the reference signal is above the values obtained during the open-loop testing and model development, the actuator saturates at the maximum allowable output. It is also interesting to note that when the controller tries to follow the sinusoidally varying reference signal, the unsteady high-frequency fluctuations
appear to be greatly reduced. Small high frequency fluctuations can be seen in the side force once the actuator reaches its maximum output, but these are much smaller than those seen when trying to hold a set reference point. In addition, it appears that as the side force transitions from positive to negative, or vice versa, a small slope change is seen. This is consistent with the fact that the controller has a hard time holding a zero side force reference and the larger fluctuations seen in open-loop data as the side force was maintained around zero.

E. Off Design - Angle Change

With successful nonlinear model predictive control established at the on-design point, the natural question that followed was the controller performance at off-design test conditions (i.e. how robust is the controller). After all, the goal of feedback control is to adapt to changes in the flow conditions autonomously, something not achievable through passive or open-loop flow control techniques. As a first test, the angle of the jet was switched back to 30° since the relative changes in the mass-flow rate were very similar (see Fig. 3). Therefore, the model should have a very good chance to adapt to these small changes and still perform very favorably. The simulation was restarted from the same unforced test case using the exact same nonlinear smith predictor control law. For comparison, only the first two static references of 0.58 and 0.0 were used during the simulation of the off-design test cases. Figure 8 shows the resulting side force with the actuators set to blow at an 30° angle. Surprisingly, the response at this off-design test condition is a little faster with a smaller overshoot. Even more interesting is that the unsteady fluctuations are greatly reduced. The exact reason for this is not clear at this point. Due to the success of this initial off-design simulation, the simulation was continued through the oscillation set-point tracking, although the results are not shown. At the start of the oscillation reference tracking segment, the actual side force went in the opposite direction of the reference and the simulation was stop before completion. Originally, the simulation was stopped due to the belief that there was an error in the code. Upon further investigation, it was determined that as the mass-flow rate out of the actuator increased, a Coanda wall-jet formed (as should have been expected), attaching the starboard vortex to the forebody. Tests were not rerun under these conditions limiting the output of the actuators, but the initial results could point to the fact that in addition to manipulating the mass-flow rate out of each actuator, some amount of jet-vectoring might aid in the overall performance of the system. At low mass-flow rates, the jet would be vectored in the freestream direction and at large reference side forces, the jet would be vectored normal to the surface.

![Image](image1.png)

Figure 8. Off-design feedback flow control in which the actuator have been set to blow at a 30° angle instead of normal to the surface.

F. Off Design - Initial condition

While the above off-design test case is not all that realistic to one experienced in real life, it provided some insight into potentially new control ideas through jet vectoring. To test a more realistic off-design test case, a new grid was built in which the geometric disturbance (pin) placed originally on the starboard side of the forebody was moved to the port side of the forebody. This resulted in an average unforced side force coefficient of approximately 0.12 as opposed to the previous unforced side force coefficient of -0.6. Once again, the same off-design feedback test case was run to gauge the performance of the control with a new geometric disturbance in the flow. Figure 9 shows the
resulting side force on the model during control compared to the on-design test case. The controller has a much harder time achieving this set-point, and is very slow to respond, taking almost twice as much time to first cross the set-point compared to the on-design test case. It also appears that the amplitude of the unsteady oscillations is larger, but with such a short time period, it is hard to make any statistically relevant discernment between the two. However, the nonlinear Smith predictor control law still was able to track the actual reference. This could indicate that during model development, a full range of open-loop data sets must be used to avoid overtraining the model to a specific test case.

Figure 9. Off-design feedback flow control in which the geometric disturbance on the model was moved from the port side to the starboard side created a new unforced flow state and open-loop system response.

IV. Conclusion

Previous feedback control on an ogive forebody using a PI control law showed the ability to set-point track, but was limited in its responsiveness due to the flow's convective flow instability creating a dead-time before the flow responds to changes in actuation. Furthermore, the nonlinear response of the side force on the model, to changes in the mass-flow rate applied to either actuator, required further de-tuning of the control law gains to maintain a robust and stable controller. To overcome these problems, a nonlinear Smith predictor control architecture was implemented in Navier-Stokes feedback flow-control simulation. The Smith predictor uses a linear time-invariant model to predict the flows response in the future which helps reduce the adverse effects of the convective instability during feedback control. To overcome the nonlinear steady-state response of the side force to changes in forcing amplitude, a transformation using exponential sigmoids was used such that in the transformed space, the input and output of the model were proportional in a least squares sense.

Before building the nonlinear Smith predictor control architecture, a new open-loop data base was generated where the actuators were set to blow normal to the surface of the model. This new arrangement provided a larger range in side forces while reducing the momentum addition requirement to achieve a specific response. Furthermore, by blowing normal, the Coanda effect was eliminated (which is probably the reason that larger side forces could be generated) eliminating one of the flow phenomena responsible for the nonlinear response of the system. Following the development of this new database and the reduced-order model, the Smith predictor was implemented into Navier-Stokes feedback flow control simulations. The results showed that this controller had the capability to set-point track at a higher bandwidth than previously obtained using standard PI control laws. However, the unsteady fluctuations in side force were on average larger than those seen in the PI feedback simulations due to new uncertainties resulting from model control. Off-design simulations indicate that these fluctuations could be the results of the change in the angle of the jet out of the actuators, and that potentially a jet-vectoring scheme might help improve the performance of the system. Finally, an additional off-design simulation was performed in which the unforced flow state was altered by changing the location of the geometric disturbance added to the model. This is a realistic off-design test scenario representing the differences from forebody to forebody due to manufacturing tolerances. The results indicate that indeed the controller was capable of reference tracking, although the performance at off-design was reduced compared
to the performance obtained at on-design. This could indicate that during the development of the LTI model, a larger database is needed to prevent over training the model for one specific flow condition.

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References


