Towards Bio-inspired Robotic Aircraft: CPG-based Control of Flapping and Gliding Flight

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This paper presents experimental micro aerial vehicle (MAV) research with low-frequency flapping and articulated wing gliding. The importance of phase difference control via an abstract mathematical model of central pattern generators (CPGs) is confirmed with a robotic bat on a 3-DOF pendulum platform. An aerodynamic model for the robotic bat based on the complex wing kinematics is presented. Closed loop experiments show that control dimension reduction is achievable by controlling the phase differences of CPG oscillators. Unstable longitudinal modes are stabilized and controlled using only two control parameters. A transition of flight modes, from flapping to gliding and vice-versa, is demonstrated within the CPG control scheme. The essential elements of perching, which cover the major control aspects of gliding flight, are demonstrated on a robotic MAV equipped with articulated wings. A novel feature of the robotic MAV considered in this paper is that wing dihedral, controlled independently on both wings, is used for yaw control as well as for maintaining the flight path angle.

I. Introduction

There is a growing interest in the aerospace community in the development of robotic micro aerial vehicles (MAVs) to learn and mimic avian flight. MAVs fly in low Reynolds number regimes of $10^3$ to $10^5$, which corresponds to that of small birds or bats. MAVs with wings equipped with multiple degrees-of-freedom such as flapping, wing twist and sweep (shown in Fig. 2) provide greater payload capability than insect MAVs and greater maneuverability than conventional fixed-wing aircraft. These MAVs can be used for intelligence gathering, surveillance, and reconnaissance missions in tightly constrained spaces such as forests and urban areas. Advances in actuators and control systems have led to development and analysis of articulated and flapping MAVs inspired by animals. Birds and bats achieve remarkable stability and perform agile maneuvers using their wings very effectively. One of the goals of reverse-engineering animal flight is to learn more about the various aspects of avian flight such as stability, maneuverability and control from the dynamics of MAV.

From a controls standpoint, there are three major flight regimes or modes in animals: high-frequency flapping, low-frequency flapping, and gliding. Insects reside almost entirely in the high-frequency domain, utilizing small musculature to produce passive pitching dynamics over the course of several wingbeats. Much bat and bird flight is low frequency enough that averaging theorems do not apply in practice and intra-wingbeat effects are significant. Additionally, steady state equilibria (or approximations thereof) do not exist, which distinguishes this regime from the other two. Dietl and Garcia give an example of an analysis of this regime by Floquet Theory. The authors are unaware of any such Floquet analysis on low-frequency flapping fliers that has shown stability. Biologically, bat flight looks more like walking locomotion in other mammals, where many joints must be coordinated in a rhythmic fashion to produce motion. Bats have anatomical similarities with other mammals. While the authors are not aware of tests specifically on bats, it makes sense that they would coordinate their joints in a similar fashion to many other mammals: with central pattern generators (CPGs).
Previous robotic flapping flyers and their control design consider one or two degrees of freedom in the wings.\textsuperscript{3,4,7,13} However, even insects like the dragonfly (\textit{Anax parthenope}) are reported to have complex three-dimensional movements by actively controlling flapping and twisting of four independent wings.\textsuperscript{14} Furthermore, prior studies in flapping flight\textsuperscript{1,4,14–18} assumed a very simple sinusoidal function for each joint to generate flapping oscillations, without deliberating on how multiple limbs (or their nervous systems) are connected and actuated to follow such a time-varying reference trajectory.

In order to utilize the knowledge gained from CPGs in biological fliers, we have built a robotic bat, shown on a pendulum testbed in Figure 1 with dimensionality far lower than the animals. These experiments are an early step toward strict modeling of biological fliers, but are more helpful for design of an artificial flapping flier. We exploit the dimensional reduction made possible by simple CPG rules to make control design and aerodynamic testing feasible. In time, we expect to gain insight into the stability and agility of animal flight, though we hope to encounter engineered solutions that are even more efficient than their biological counterparts.

Moderately large birds and bats often spend their time in either a low-frequency flapping mode or a gliding mode. The proposed CPG based controller can switch smoothly to the gliding mode by changing a bifurcation parameter. The gliding mode is not unlike that explored in the traditional flight mechanics literature. However, fully articulated wings inherent in flapping flight create additional control possibilities and concerns. A gliding mode may be used for soaring and to shed energy in preparation for a perching maneuver. Perching can be described as a high angle-of-attack pull-up with high lift and a large drag. The large lift and drag forces cause the MAV to climb and lose speed significantly. A planted landing can be achieved in the process.

Birds successfully perch on a variety of structures such as building ledges, power lines, cliff side, and tree branches. Such perching capability in MAVs can significantly reduce the landing distance. However, perching requires the ability to maintain trajectory very accurately. Furthermore, a typical perching maneuver would not last more than a few seconds. Because of its duration and highly unsteady flight profile, perching is an important agility metric for MAVs. The unsteady flight profile makes control design for perching a challenging problem.

The aerodynamics of perching has been explored for conventional, fixed-wing aircraft by Crowther\textsuperscript{19} who showed that perching could be performed with essentially a simple pitch-up maneuver and used genetic algorithm to optimize the maneuver. Wickenheiser and Garcia demonstrated perching maneuver with controlled wing twist and variable tail incidence.\textsuperscript{20,21} Roberts \textit{et al.}\textsuperscript{22} examined the perching problem from...
controllability aspects. One of the most outstanding experimental demonstrations of a perching maneuver was reported by Cory and Tedrake\textsuperscript{23} where they obtained reliable estimates of the open loop dynamics and used them to perform an maneuver optimized to minimize the error in the final position. In contrast with the aforementioned work, we consider a completely different mechanism (wing dihedral) to control the flight path angle as well as lateral-directional dynamics during perching. The lateral-directional control, in particular, is often neglected in the literature on robotic perching.

The broader objective of the work described in this paper is to aid the design and analysis of a robotic aircraft capable of agile flight in the flapping as well as gliding phases. The immediate objective of this paper is two-fold: (1) to demonstrate the importance of phase differences in stability of low-frequency flapping flight, and (2) demonstrate the essential elements of a perching maneuver in gliding MAVs which use a combination of wing dihedral and elevator for control. A CPG-like control design is used for the flapping phase and for transitioning to the gliding phase for which a more conventional controller is employed. The rest of the paper is organized as follows. In what may be considered part one, we review our experimentation with RoboBat, a flapping testbed. We introduce CPG control in Section II, provide a full dynamic model of the testbed in Section III, and show stability and control of longitudinal modes in Section IV. In what may be considered part two, we review our experiments with an articulated wing gliding testbed. We summarize the mechanics of known articulated wing flight in Section V, develop the control law in Section VI, and present closed loop results in Section VII. Concluding remarks are provided in Section VIII.

II. Biologically Inspired CPG Control Basics

The flapping motion in bats combines many different joints and muscles to create rhythmic motions which produce the aerodynamic forces required to sustain flight. With the current state of the actuator/structure technology, we are unable to mimic the strength and flexibility of the distributed structure of bones, joints and ligaments. More specifically, we are unable to produce muscle-strength actuation of the entire system using mechanical actuators. For this purpose, we have settled on starting our investigation with three main modes of motion: flapping, twisting, and lead-lag (sometimes referred to as plunging, pitching, and sweeping). These three motions (shown in Figure 2) have the strongest connection to traditional flight models and were the first to be discussed in observations of animal flight.\textsuperscript{14} Azuma also notices that the phase differences between the motions is extremely important.\textsuperscript{14} Next we consider how to use CPGs for precise control of phase differences between the three motions.

A. Central Pattern Generators

Many creatures produce their motion by synchronizing periodic motions of limbs, such as running, swimming or flapping. They do this by coupling biological oscillators and synchronizing their outputs. Biological oscillators rely on short timescale (ms) neuron dynamics including spike-bursting, spike frequency adaptation, and post-inhibitory rebound. Herrero-Carrón, et. al.\textsuperscript{24} designed a control law for modular robots by approximating short timescale neuron dynamics. Because there is such a short timescale required for integration, the neuron dynamics were integrated offline. We are unlikely to be able to perform such strict mimicry in an online controller as we add additional neurons for feedback, active control of phase differences, or gait transitions.
In order to make online control more feasible, we can emulate these biological oscillators by using coupled nonlinear limit cycle oscillators. A limit cycle oscillator converges to a stable trajectory which is called the limit cycle. Because of this convergence the oscillator will quickly forget sporadic disturbances and converge back to the stable limit cycle. If the oscillator itself is a smooth vector field, we can smoothly transition between desired trajectories without abrupt changes being required in the motor output.

This type of control design allows the complicated synchronization and trajectory computations to be performed according to simple rules that provide the desired oscillatory behavior. If we can then influence body motion by simply tuning top-level inputs into the CPG network, we can achieve the dimension reduction that allows the control problem to be computationally feasible. Vertebrate brains send top-level chemical signals which modulate, start, and stop CPG behavior, but do not micromanage the joint trajectories.\(^{25}\)

Following our previous work,\(^5\) we use the following limit-cycle model called the Hopf oscillator, named after the supercritical Hopf bifurcation model with \(\sigma = 1:\)

\[
\dot{x} = f(x; \rho; \sigma) + u(t) \quad \text{with} \quad x = (u - a, v)^T \quad \text{and} \quad f(x; \rho; \sigma) = \begin{bmatrix} -\lambda \left( \frac{(u-a)^2 + v^2}{\rho^2} - \sigma \right) -\omega(t) & -\omega(t) \\ -\lambda \left( \frac{(u-a)^2 + v^2}{\rho^2} - \sigma \right) & -\lambda \left( \frac{(u-a)^2 + v^2}{\rho^2} - \sigma \right) \end{bmatrix} x
\]

where the \(\lambda > 0\) denotes the convergence rate to the symmetric limit circle of the radius \(\rho > 0\) and \(u(t)\) is an external or coupling input. The bifurcation parameter \(\sigma\) can change to a negative number (e.g. \(-1\) such that \(\left( \frac{(u-a)^2 + v^2}{\rho^2} + 1 \right)\)). This would change the stable limit cycle dynamics to the dynamics with a globally stable equilibrium point at the bias \(\sigma\).\(^{26}\) Such a change can be used to turn the flapping oscillatory motion to the gliding mode, as was seen in [5]. This could be used to mimic many animal flight strategies. Later sections of this paper discuss terminal perching control, assumed to be in a gliding phase. Often, bats and birds will transition to gliding in order to shed energy well before an actual perching maneuver is performed. In [5], it was shown that coupled networks of Hopf oscillators on balanced graphs exhibit smooth exponentially stable behavior in both oscillatory mode and fixed point mode.

The possibly time-varying parameter \(\omega(t) > 0\) determines the oscillation frequency of the limit cycle. A time-varying \(a(t)\) sets the bias to the limit cycle such that it converges to \(u(t) = \rho \cos(\omega t + \delta) + a\) and \(v(t) = \rho \sin(\omega t + \delta)\) on a circle. The output variable to generate the desired oscillatory motion of each joint is the first state \(u\) from the Hopf oscillator model in Eq. (1).

Phase synchronization means an exact match of the scaled amplitude or the frequency in this paper. Hence, phase synchronization permits different actuators to oscillate at the same frequency but with a prescribed phase lag. In essence, each CPG dynamic model in Eq. (1) is responsible for generating the limiting oscillatory behavior of a corresponding joint, and the diffusive coupling among CPGs reinforces phase synchronization. For example, the flapping angle has roughly a 90-degree phase difference with the limiting oscillatory behavior of a corresponding joint, and the diffusive coupling among CPGs reinforces

Hence, phase synchronization permits different actuators to oscillate at the same frequency but with a bidirectional or a uni-directional coupling between the oscillators. The numbers next to the arrows in Fig. 3 indicate the phase shift \(\Delta_{ij}\); hence \(\Delta_{ij} > 0\) indicates how much phase the \(i\)-th member leads (or lags) from the \(j\)-th member and \(\Delta_{ij} = -\Delta_{ji}\). The positive scalar \(k\) denotes the coupling gain.

Numerous configurations are possible as long as they are on balanced graphs\(^{27}\) and we can choose either a bidirectional or a uni-directional coupling between the oscillators. The numbers next to the arrows in Fig. 3 indicate the phase shift \(\Delta_{ij}\); hence \(\Delta_{ij} > 0\) indicates how much phase the \(i\)-th member leads. Figure 3(a) shows the choice of coupling used in this paper, where \(\phi_w, \theta_w, \) and \(\psi_w\) are flapping angle, twist angle, and lead-lag angle, respectively. Subscripts L and R refer to left and right wings. The graph is balanced. Further, all the phase shifts \(\Delta_{ij}\) along one cycle add up to a modulo of 2\(\pi\). The proof of stability of (2) is given in [5]. As mentioned in [28], this type of oscillator generalizes other types of waveform control such as the split-cycle.\(^8\)
producing control schemes for a tailless vehicle will have even more effectiveness in a free flier. To move

B. Control Design from Physical Intuition and Biological Observation

Our previous work\cite{5} provided a simple intuition for phase difference control of longitudinal motion in flapping
flight. With a zero bias lead-lag and a center of gravity coinciding with the stroke plane, a phase difference of
270 deg between the flapping CPG and the lead-lag CPG gives Azuma’s\cite{29} elliptical model of flapping:
negative lead-lag on downstroke, positive lead-lag on upstroke. The simplest analysis combines a maximum
force with the most-negative lead-lag at the middle of the downstroke to predict a pitch-down moment on
the body. Alternatively, if we see the phase difference to 90 deg, we see the maximum force coinciding
with the maximum positive lead-lag at the middle of the downstroke, predicting a pitch-up moment. This
intuition has been confirmed in numerical simulations and open loop experiments on the RoboBat.\cite{2,29,31} In
section IV, we attempt to exploit this intuition for closed loop, low dimensional control of the RoboBat.

III. Kinematics and Unsteady Aerodynamics

We present the dynamic model of the current RobotBat, which is not intended to be a free flying platform.
The detailed dynamic models of free-flying ornithopters can be found in \cite{2}, \cite{5} and \cite{31} that includes the
wing flexibility. It is intended as a testbed for CPG control designs, experimental confirmation of unsteady
aerodynamics, and experimental determination of optimal wing motions. The weight and power requirements
have not been optimized for free flight. In order to test longitudinal control strategies, it has been attached to
a Quanser pendulum platform, which provides encoder feedback signals that we can use for control. Figure 1
shows the three degrees of freedom: travel, elevation, and pitch (λ,ε,θ). For more information on the testbed, see \cite{30}.

Of note is the fact that the pitch rotation point is not near the center of gravity of the bat. To make
experimentation feasible, we have affixed a counterweight on the pitch arm. By moving this counterweight
or changing its mass, we can alter the natural stability of the pitch motion. One consequence of this scheme
is that the pitch motion has an artificially high moment of inertia. Therefore, we expect that our moment-
producing control schemes for a tailless vehicle will have even more effectiveness in a free flier. To move
toward computations of actual forces and moments generated, we desire dynamic modeling of the pendulum
set-up and the unsteady aerodynamics. If we define our generalized coordinates to be [q1, q2, q3] = [ε, θ, λ],
then using Lagrange’s equations,\[ \frac{d}{dt} \frac{\partial L(q, \dot{q})}{\partial \dot{q}} - \frac{\partial L(q, \dot{q})}{\partial q} = F \] and algebraic manipulations, we can transform the
EOM to standard robot form.\cite{32}
\[ M(q)\ddot{q} + C(q, \dot{q})\dot{q} + g(q) = \tau. \]

The forces and moments on the right hand side can be found as a function of wing kinematics and an
aerodynamic model. The generalized forces remain intact through the transformation to robot form, i.e.,
F = τ, and are computed to be
\[ \tau = \begin{pmatrix} \ell_a (L \cos(q_2) + T \sin(q_2)) \\ M - \ell_b T \\ \ell_a (T \cos(q_2) - L \sin(q_2)) \end{pmatrix}, \]

where L, T, and M are found later in the aerodynamic model. This formulation can then be applied to a
free-flying MAV. Here, we present a refinement on the aerodynamic model of \cite{5}.
The pendulum rig consists of

1. A solid bar with mass $M_1$ hinged at its center of gravity such that it can spin about the vertical axis (angle given by $\lambda$, positive counter-clockwise) and rotate upwards and downwards in the vertical plane (angle denoted by $\epsilon$, positive downwards).

2. A compound pendulum mounted on one end of the bar consisting of two point masses: the robotic bat itself modelled as a point mass $m_b$ and a variable mass $m$. The compound pendulum is free to swing in the plane normal to the bar, with the swing angle given by $\theta$.

3. A counter-weight, $m_w$, located at the opposite end of the bar as the bat.

Three frames of reference can be defined for this system, given an inertial frame of reference $I$ fixed to the Earth:

1. A frame $B$ fixed to the compound pendulum with its origin at the suspension point. The frame $B$ parallel to the aircraft body axis frame centered at the aircraft CG.

2. A frame $P$ with its origin at the bar’s hinge point such that under nominal conditions, the axes of $P$ and $B$ are parallel to each other.

3. A frame $S$ constructed locally at every wing station for calculation the local wind velocity and the aerodynamic forces and moments.

The frame $I$ is first rotated about the $z$-axis by an angle $\lambda$, followed by a rotation about the $x$-axis by $\epsilon$ to coincide with the $P$ frame. Therefore, the following rotation matrix is obtained to transform the components of a vector from $I$ to $P$:

$$ R_{PI} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \epsilon & -\sin \epsilon \\ 0 & \sin \epsilon & \cos \epsilon \end{bmatrix} \begin{bmatrix} \cos \lambda & -\sin \lambda & 0 \\ \sin \lambda & \cos \lambda & 0 \\ 0 & 0 & 1 \end{bmatrix} = \begin{bmatrix} \cos \lambda \cos \epsilon & -\sin \lambda \cos \epsilon & \sin \lambda \\ \cos \lambda \sin \epsilon + \sin \lambda \cos \epsilon \sin \lambda & \cos \lambda \cos \epsilon \cos \lambda - \sin \epsilon \cos \lambda & \cos \epsilon \sin \lambda \\
\sin \lambda \sin \epsilon \cos \lambda - \cos \epsilon \sin \lambda & \sin \lambda \cos \epsilon \cos \lambda \sin \epsilon \cos \lambda & \cos \epsilon \sin \lambda \end{bmatrix} $$

The frame $P$ is rotated about the $y$-axis to obtain frame $B$:

$$ R_{BP} = \begin{bmatrix} \cos \theta & 0 & -\sin \theta \\ 0 & 1 & 0 \\ \sin \theta & 0 & \cos \theta \end{bmatrix} $$

The rotation matrix from $B$ to $S$ is obtained by performing the standard $3 - 2 - 1$ rotations through angles $\psi$, $\theta_w$ and $-\phi$, respectively:

$$ R_{SB} = \begin{bmatrix} \cos \phi \cos \psi & \sin \psi \cos \phi & \sin \phi \\ -\sin \phi \sin \theta_w \cos \psi - \sin \psi \cos \theta_w & -\sin \phi \sin \psi \sin \theta_w + \cos \theta_w \cos \psi & \sin \theta_w \cos \phi \\ -\sin \phi \cos \theta_w \cos \psi - \sin \psi \sin \theta_w & -\sin \phi \sin \psi \cos \theta_w + \sin \theta_w \cos \psi & \cos \theta_w \cos \phi \end{bmatrix} $$

The matrix $R_{SP} = R_{SB}R_{BP}$ transforms the coordinates of a vector from the $P$ frame to $S$ frame, and will be useful while calculating velocities and forces.

Let $\omega_p$ denote the angular velocity of the horizontal rod in the $P$ frame, i.e., $\omega_p = [\dot{\epsilon} \ 0 \ -\dot{\lambda}]^T$. Let $\omega_B = [0 \ \dot{\theta} \ 0]^T$ denote the angular velocity of the compound pendulum about $O_B$. Finally, let $\omega$ denote the angular velocity of a wing with components in the $B$ frame (a similar expression can be derived for the other wing). It follows that

$$ \omega = \begin{bmatrix} -\dot{\phi} - \dot{\psi} \sin \theta_w \\ \dot{\theta}_w \cos \phi - \dot{\psi} \cos \theta_w \sin \phi \\ \dot{\theta}_w \sin \phi + \dot{\psi} \cos \theta_w \cos \phi \end{bmatrix} $$

Let $Y = [0 \ l_z \ 0]^T$ denote the position vector from $O_P$ to $O_B$. Let $z = [0 \ 0 \ -l_z]^T$ denote the position vector from $O_P$ to the bat CG, and $y(y) = [0 \ y \ 0]^T$ denote the vector from bat CG to a wing station with
\[ V = RSP S(\omega_P) \mathbf{Y} + RSB S(\omega_B + RBP \omega_P) \mathbf{z} \]

\[ \text{equivalent to flight speed } V_\infty \]

\[ + S(RSB(\omega + \omega_B) + RSP \omega_P) \mathbf{y} \]

(8)

The local angle of attack is given by

\[ \alpha = \tan^{-1} \left( \frac{V_3}{V_1} \right) \]

(9)

where \( V_i \) is the \( i \)th element of \( V \).

The local acceleration is assumed to arise entirely from flapping. Since the model developed in this paper is intended to be as generic as possible, the model proposed by Goman and Khrabrov\(^{33}\) is presented as a candidate model for computing the lift and the quarter chord moment while drag is estimated assuming the classic drag polar. In the authors’ estimate, Goman and Khrabrov’s model offers at least two advantages over the existing models (e.g., Theodorsen or Peters\(^{34}\)). First, the model is cast in the form of a single ordinary differential equation (ODE) and two algebraic equations, one each for life and the quarter chord pitching moment. The state variable for the ODE corresponds, physically, to the chordwise location of flow separation on the airfoil. Therefore, the model is quite easy to implement as part of a numerical routine. Second, the model is inherently nonlinear and applicable to post-stall conditions.

The following equation describes the movement of the separation point for unsteady flow conditions

\[ \tau_1 \dot{\nu} + \nu = \nu_0 (\alpha - \tau_2 \dot{\alpha}) \]

(10)

where \( \tau_1 \) is the relaxation time constant, \( \tau_2 \) captures the time delay effects due to the flow, while \( \nu_0 \) is an expression for the nominal position of the separation point. These three parameters need to be identified experimentally or using CFD for the particular airfoil under consideration. The coefficients of lift and quarter-chord moment are then given by

\[ C_l^* = \frac{\pi}{2} \sin(\alpha(1 + \nu + 2\sqrt{\nu})) \]

\[ C_{m_{ac}}^* = \frac{\pi}{2} \sin(\alpha(1 + \nu + 2\sqrt{\nu})) \left[ \frac{5 + 5\nu - 6\sqrt{\nu}}{16} \right] \]

(11)

The lift and the quarter chord moment per unit span are then given by

\[ L(y) = 0.5\rho V^2(y) c C_l^* + \frac{\pi}{4} \rho c^2 \left( \dot{\xi} + V_\infty \alpha - (x_\alpha - 0.25)\dot{\alpha} \right) \]

\[ M(y) = 0.5\rho V^2(y) c^2 C_{m_{ac}}^* + \frac{\pi}{4} \rho c^2 \left( V_\infty \dot{\xi} + \frac{(x_\alpha - 0.25)\dot{\xi}}{2} + V_\infty^2 \alpha - c^2 \left( \frac{1}{32} + (x_\alpha - 0.25)^2 \right) \dot{\alpha} \right) \]

(12)

where \( \theta(y) \) is the twist angle, \( \rho \) denotes the density of air and \( \xi \) is the transverse displacement of the wing due to flapping. Furthermore, \( V = ||V|| \) is the local wind speed with \( V \) defined in Eq. (8), and \( V_\infty \) is the freestream speed of the aircraft. The last term of each expression was added to Goman’s original model\(^{33}\) and corresponds to the apparent mass effect.\(^{35}\)

There is, unfortunately, no simple expression for the sectional drag coefficient. Assuming laminar flow on the wing, the sectional drag coefficient can be written as \( C_D = \frac{0.89}{\sqrt{Re}} + \frac{1}{\pi e A_R} C_L^2 \) where \( A_R \) is the aspect ratio of the wing, \( Re = \frac{e V_\infty}{\mu} \) is the chordwise Reynolds number, and \( e \) is Oswald’s efficiency factor. A refined model for calculating drag, incorporating dynamic stall, may be found in DeLaurier.\(^{35}\)

IV. CPG-based Control Results of RoboBat

Previous numerical results have shown that, for longitudinal modes, dimensional reduction via CPGs can be effective.\(^{5}\) It was shown that control could be reduced to just two parameters: frequency (corresponding with velocity) and the phase difference between flapping and lead-lag (\( \Delta_{31} = \Delta_{32} + \Delta_{21} \) corresponding with pitch state). Recently, open loop non-equilibrium steady state experiments have supported this idea further.\(^{30}\) This paper intends to show how the CPG structure allows very simple top-level controllers to provide stability and control in closed loop.
Simple symmetric PID controllers were used for all experiments,

\[
\Delta_{31} = \Delta_{75} = -5(\theta - \theta_d) - 0.5\dot{\theta} - 0.1\int_0^t (\theta - \theta_d) \, dt,
\]

(13)

where \(\theta\) is the pitch angle and \(\dot{\theta}\) is computed using the derivative filter, \(\frac{\omega_{cf}^2}{\omega_{cf}^2 + 2\zeta_f \omega_{cf}}\), with \(\omega_{cf} = 40\pi\) and \(\zeta_f = 0.9\). The saturation values were set so \(\Delta_{31} \in [180^\circ, 270^\circ]\). Even though we are able to use simple PID controllers in the top level, the overall controller is very nonlinear due to the CPGs in Equation (2).

We begin experimentation at an open loop flapping frequency of 2.5 Hz. At this frequency, the open loop appeared stable. Figure 4 shows the response to a change in desired body pitch from \(-10^\circ\) to \(-20^\circ\). Two notes from [30] should be kept in mind. First, the actual value of body pitch is affected by the precise position of the pitch counterweight. Therefore, it is not worrisome that the values are not exactly around zero or some other intuitively desired value. Second, at 2.5 Hz, the apparent maximum change of body pitch due to open loop control of \(\Delta_{31}\) is around \(10^\circ - 12^\circ\). This experiment demands a change of \(10^\circ\) and experiences saturation problems as it nears the final desired state.

Moving the frequency to 3 Hz caused instability in the open loop. Figure 5 shows that by activating the PID control of \(\Delta_{31}\), we can stabilize the unstable system. At this frequency, we also have appreciably more control authority. Figure 6 shows a commanded pitch change of \(15^\circ\), which is easily obtained. We expect that at speeds typical of bat flight (2-3 m/s with frequencies of 7-10 Hz\(^{15}\)) and pitch moment of inertias not inflated by the pendulum setup we will see even more control effectiveness. Numerical results have supported the idea that this control effectiveness will be much higher.\(^5\)

Finally, we would like to feed back velocity into flapping frequency and demonstrate flight mode changes. Velocity control is achieved using a similarly simple PID controller on top of the CPG network. Importantly,
we were able to transition to a glide by simply flipping the sign of the bifurcation parameter $\sigma$ in Eq. 1. After dissipating energy in the glide, the transition back to flapping was again as simple as flipping the sign. Experimental results are found in Fig. 7. During the flapping phase, both pitch and velocity controllers are active (desired velocity: 0.5m/s, desired pitch: 35 degrees). During the glide phase, notice that rapid inhibition causes the CPG outputs to go to zero (the plotted CPG output is the normalized value of the flapping angle). At this time, the frequency and phase difference values are not meaningful, as the CPGs exhibit exponentially stable fixed point dynamics. During such a glide, control laws like those proposed in the second part of this paper (Sec.V-VII) or in [2] could easily be used to control the bias signals of the Hopf oscillator network. A perching maneuver is not currently feasible for the flapping testbed, as the moments of inertia needed to facilitate future wing motion optimization are too large to allow or a noticeable perching type maneuver. In order to demonstrate a perching type maneuver, we move to a vehicle that has the appropriate weight characteristics.

V. Flight Mechanics of MAV with Articulated Wings

The work presented in this paper is based on Ref. [2], where the concept of dihedral-based control for MAVs was described and analysed extensively. A few important observations have been recapitulated in this section. It is important to note that the word “tailless” only implies the absence of a vertical tail. Figure 8 illustrates the physics underlying the use of wing dihedral as a control. Increasing the wing dihedral reduces the force acting in the body $z$-direction, and generates a side force. The reduced $z$-force affects the aircraft flight path angle and angle of attack, and hence the flight speed. On the other hand, the side force can be used for providing the centripetal force for turning, and as a source of the yawing moment. In particular, if the CG is located behind the line of action of the side force, then a positive side force produces a positive yawing moment and vice-versa. It follows that a positive rolling moment (wherein the lift on the left wing is higher than the right wing) is accompanied by a positive yawing moment if the wings have a positive dihedral deflection. Consequently, the adverse yaw produced by rolling is reduced.

The yaw control effectiveness of the wing dihedral (measured in terms of the yawing moment produced per unit anti-symmetric deflection) is tempered by the negative pitching moment produced by wings with a positive camber. Let $\delta_{\text{asym}}$ denote the anti-symmetric deflection, i.e., $\delta_{\text{asym}} = \delta_L - \delta_R$, where $\delta_L$ and $\delta_R$ denote the dihedral angles of the left and the right wing, respectively. The yaw control effectiveness, $N_{\delta_{\text{asym}}}$, is approximated as follows:

$$N_{\delta_{\text{asym}}} \approx \frac{1}{2I_z} \rho V_\infty^2 S_{\text{out}} c \left( \frac{x_a C_{L,n} \alpha}{c} + C_{m,ac} \right)$$

where $x_a/c$ denotes the normalized distance of the aerodynamic center (AC) from the center of gravity; $S_{\text{out}}$ is the combined area of the outboard sections of the two wings and $I_z$ is the aircraft moment of inertia about the $z$ axis. The term $x_a$ is positive if the wing aerodynamic center is ahead of the center of gravity. The effectiveness can be negative at low angles of attack for wings with positive camber ($C_{m,ac} < 0$). Thereafter, for a range of angles of attack, control effectiveness is sensitive to the angular rates before it becomes positive.
uniformly across the routinely flown flight envelope. The angle of attack at which the effectiveness ceases to be negative increases with increasing wing camber. The reader may be tempted to assume that the issue of negative control effectiveness only affects controllability and can be dealt with as such. However, it can have a significant impact on the turning performance of the aircraft. At low angles of attack, for example, an entry into right turns requires that the left wing dihedral be larger than the right wing dihedral to generate the required positive side force. This configuration, however, produces a negative yawing moment which
inhibits the turn. The only way to address this problem effectively is to use wing twist or ailerons. At the same time, it must be noted that controlling the wing dihedral deflections is sufficient to ensure stabilization and yaw rate regulation.

Finally, although this may be obvious to most readers, it is worth recalling that the absence of a vertical tail renders the lateral-directional dynamics unstable. The divergence for the MAV in this paper is rapid, with a time constant of approximately 0.2 s. One of the key differences between aircraft with and without a vertical tail is the build-up of roll rate. The dihedral-based mechanism described here can bring about rapid changes in the yaw rate, but it is significantly less effective at regulating the roll rate.

VI. Control Law Design

A control law design for the MAV is described in this section. The control law has a two-tier hierarchical structure based on time-scale separation which occurs naturally between the fast rotational dynamics and the slow translational dynamics:

- The innermost loop commands the elevator and the asymmetric components of the wing dihedral.
- The outer loop commands the angle of attack and turn rate to be tracked by the inner loop based on flight speed and turn rate. The turn rate and the flight path angle are computed based on position measurements.

A schematic of the controller has been shown in Fig. 9.

A. Simulations

PID controllers can be used for controlling the MAV, with gains tuned using an approach inspired by dynamic inversion (DI). The time histories in Figures 10 (no external disturbances) and 11 (persistent
Figure 10. Simulated time histories of the aircraft in a disturbance-free flight. A 12 deg (0.2 rad jump in the angle of attack, $\alpha$, is commanded. The resulting disturbances are rejected by the control law.)

periodic disturbances) show that the controllers performed satisfactorily when the angle of attack was kept above 11 deg to ensure that the yaw control effectiveness of the dihedral was uniformly positive. A similar performance was seen for angles of attack less than 6 deg, where the yaw control effectiveness was uniformly negative.

The purpose of the simulations was to demonstrate a general control design technique. However, in the course of experiments, we were able to make reasonable estimates of the open loop dynamics. This allowed us to tune controllers without resorting to a DI-inspired scheme.

B. Angle of Attack Control

The stability of the longitudinal dynamics depends on the CG location. Two longitudinal controllers were designed: one for the configuration with the vertical tail where the CG was placed around the quarter-chord point of the wing, and another for the configuration without a vertical tail where the CG was placed between 0.25 $c$ and 0.3 $c$ behind the wing AC. Here, $c$ denotes the wing root chord length.

The longitudinal dynamics of the configuration with a vertical tail were seen during experiments to be stable across the angle of attack envelope, as a consequence of a favorable CG location, while the lateral dynamics showed a divergent unstable yaw mode. The angle of attack is controlled using a simple PID scheme which ensures satisfactory tracking and retains an ease of implementation on the hardware.
Let $e_\alpha(t) = \alpha_c(t) - \alpha(t)$, where $\alpha_c(t)$ is the commanded angle of attack. A gain-scheduled PI controller commands the elevator deflection in the configuration with a vertical tail:

$$\delta_e(t) = k_pe_\alpha + k_i \int_0^t e_\alpha dt,$$

where $k_p = k_i = -0.45 - 0.0061 \cdot (\alpha - 10)^2$  \hfill (15)

The longitudinal dynamics of the tailless Configuration are stable, but poorly damped for $\alpha > 8$ deg. Around $\alpha = 15$ deg, the elevator effectiveness saturates and higher angles of attack are unattainable under routine flight conditions. The open loop response was measured to have a time period of 1 s. The observed reduction in the amplitude of oscillations was used to approximate the damping coefficient to 0.046. The open loop dynamics can be written in the form

$$\ddot{\alpha} + 0.62\dot{\alpha} + 40\alpha = -40\delta_e + 5.6$$ \hfill (16)

Therefore, an essentially derivative-integral controller was designed for the tailless configuration:

$$\delta_e(t) = 0.14 - \alpha_c + k_d\dot{\alpha} + k_i \int_0^t e_\alpha dt,$$

where the offset of 0.14 rad was added based on the measured $\delta_e - \alpha$ trims. The gain $k_i$ was similar to
that for the configuration with a vertical tail, while $k_d = 0.217$ is chosen so that the damping coefficient is approximately equal to 0.7.

C. Yaw Control by Asymmetric Wing Dihedral

Yaw control has been often neglected in the literature on perching, primarily because the aircraft possessed the traditional roll and yaw control surfaces. On the other hand, lateral-directional control is an important concern for aircraft which lack a roll control surface and use a highly unconventional yaw control mechanism. Two different yaw controllers are needed for the configurations with and without a vertical tail because the wing dihedral plays a separate role in each configuration. Moreover, although both configurations are laterally not actuated, the dihedral angles need to be controlled for different maneuvers such as entering or recovering from a turn.

The asymmetric component of the wing dihedral angles, $\delta_{\text{asym}}$, is commanded by a PI controller. Let $e_r(t) = r_c(t) - r(t)$, where $r_c(t)$ is the commanded yaw rate. The anti-symmetric dihedral deflection commanded by the controller is given by

$$\delta_{\text{asym}}(t) = 1 \cdot e_r(t) + 0.5 \int_0^t e_r(t) dt \quad (18)$$

Unlike the configuration with a vertical tail, the tailless aircraft is seen to be highly unstable in the open loop. The lateral-directional dynamics are primarily underdamped, which mandates the use of a derivative controller (unlike the PI which sufficed for the configuration with a vertical tail).

Based on experimental observations, it was estimated that the open loop yaw-rate dynamics are of the form

$$\ddot{r} + 2\zeta \omega \dot{r} + \omega^2 r = N_{\delta_{\text{asym}}} \delta_{\text{asym}}, \quad \zeta \approx -0.1, \quad \omega \approx 2\pi \quad (19)$$

for $\alpha < 8$ deg. Thereafter, the yaw dynamics are unstable and oscillatory in nature. Recall the approximation for $N_{\delta_{\text{asym}}}$:

$$N_{\delta_{\text{asym}}} \approx \frac{1}{2I_z} \rho V^2 S_{\text{out}} c \left( \frac{C_L \alpha}{3} + C_{m,ac} \right)$$

where $S_{\text{out}}$ is the combined area of the outboard sections of the two wings and $I_z$ is the aircraft moment of inertia about the z axis. Substituting the estimates for the geometric and aerodynamic terms, it follows that

$$-2 < N_{\delta_{\text{asym}}} < -1.2, \quad \alpha < 6 \text{ deg} \quad (20)$$

Finally, in order to account for the actuator time delay of 0.2 s, a lead compensator $L(s)$ is designed given by $L(s) = \frac{8(s + 4.5)}{4.5(s + 8)}$. Furthermore, a derivative filter of the form $D(s) = \frac{12(s + 4)}{s + 8}$ is designed. The role of dihedral control is regulation, and it suffices use a derivative controller for damping addition, so that the commanded dihedral deflection is given by

$$\delta_{\text{asym}} = k_d D(s) L(s) e_r(s) \quad (21)$$

D. Perching Guidance Loop

The outer control loop is designed to ensure rapid changes in the flight path over a short duration. For the sake of completeness, it must be noted here that, in general, the guidance loop commands the flight path angle as well as the turn rate. The flight path angle ($\gamma$), the heading angle ($\chi$), and the turn rate ($\omega$) are given by

$$\sin \gamma = \cos \alpha \cos \beta \sin \theta - \sin \beta \sin \phi \cos \theta - \sin \alpha \cos \beta \cos \phi \cos \theta \quad (22)$$

$$\sin \chi \cos \gamma = \cos \alpha \cos \beta \cos \theta \sin \psi + \sin \beta (\sin \phi \sin \theta \sin \psi + \cos \phi \cos \psi)$$

$$+ \sin \alpha \cos \beta (\cos \phi \sin \theta \sin \psi \sin \phi \cos \psi) \quad (23)$$

$$\omega = \dot{\chi} = \text{sign}(\dot{\chi}) \sqrt{p^2 + q^2 + r^2} \quad (24)$$
The flight path angle is controlled in discrete time so that a symmetric dihedral angle is commanded every 0.2 s (which is equal to the dihedral actuator time delay). The commanded dihedral angles are given by

\[ \delta_R = \delta_L = \sqrt{2 + \frac{2}{f(\alpha)\tan\gamma_c}}, \quad f(\alpha) \approx \frac{C_L(\alpha)}{C_D(\alpha)} \]  

(25)

where \( \gamma_c \) is the commanded flight path angle which is, in turn, given by

\[ \gamma_c = \tan^{-1}(h) \approx \frac{h}{1 + 0.28125h^2}, \quad h = \frac{z - z_l}{\sqrt{(x - x_l)^2 + (y - y_l)^2}} \]  

(26)

Here, \( x_l, y_l \) and \( z_l \) are the coordinates of the desired landing point on the ground, or a point in the air where a perching command is to be sent to the aircraft. It has to be noted that the dihedral and flight path angles are computed and commanded every 0.2 s. This is not an optimal gliding strategy because it does not take into account the instantaneous flight path angle and aircraft speed. It was seen to be effective over the short duration of the experiments, although it needs to be improved for experiments which may last over a longer duration. It is interesting to note that changing the wing dihedral brings about a significant effect in the pitching moment and using a continuous-time flight path controller leads to undesirable oscillatory behavior due to coupling with the pitch dynamics. This problem is bypassed by updating the dihedral angle every 0.2 s, an interval which was arrived at after trial and error in the course of experiments.

VII. Experiments on the Elements of Perching

A perching maneuver consists essentially of two phases: (1) a guided approach to a suitable point away from the landing point, and (2) a pitch up, with the aircraft attaining post-stall angles of attack, which ends in a perched landing at the desired point with a very low speed. The low speed allows for a perched landing, or it allows for capture mechanisms like hooks and suction pads to mechanically stop the aircraft at the desired spot. This section brings together the elements required to execute a perching maneuver: (a) calculations for the suitable point where the pitch up may be commenced, (b) demonstration of control laws to hold the angle of attack and flight path during the first phase, and (c) the actual pitch-up.

A. Elements of Perching

A typical perching maneuver has a two-step profile: a low-\( \alpha \) glide followed by a rapid, transient pitch-up to post-stall \( \alpha \) which achieves the desired deceleration and flattening of the flight path. A perching maneuver requires three key ingredients: (a) a guidance law which brings the aircraft to any desired point, (b) a yaw controller which regulates the heading, and (c) identification of a suitable point to commence the pitch-up. Task (a) is a formidable problem in its own right and has not been addressed here because our objective has been to understand the capabilities of the aircraft and the flight mechanics underlying the maneuvers. Task (b) was addressed in the previous section. Task (b) has been largely ignored in the literature because perching has been studied using a stable aircraft. However, tailless aircraft are unstable and the instability is rapid enough to be of relevance even in a rapid maneuver like perching. We have addressed the problem of yaw control in Sec. VII-C.

In this section, we identify a suitable point with reference to the landing point at which the pull up maneuver is executed. Our identification is purely at the level of flight mechanics. The reader is referred to Refs. [20, 21, 23] for optimal guidance laws. We assume that \( C_L \) is essentially constant during the second (constant \( \delta_c \)) phase of perching. This leaves us with three variables to contend with: the initial flight speed, the initial flight path angle, and the distance from the landing point at which the maneuver is commenced. We seek to calculate the final speed and the \( C_L \) required for the maneuver. Note that the value of \( C_L \) required for the maneuver depends on the initial distance from the landing point.

We start with the longitudinal equations of motion of the aircraft. Let \( \eta = \rho S/(2m) \), where \( S \) denotes the area of the wing, and let \( \cos \gamma \approx 1 \). Then, the equations of motion are given by

\[ \dot{V} = -g \sin \gamma - \eta V^2 C_D \]
\[ \dot{\gamma} = \eta V C_L - \frac{g}{V} \]
\[ \dot{z} = -V \sin \gamma \]  

(27)
We wish to use the $x$ coordinate as the independent variable instead of $t$. Let $V' = dV/dx$, and note that $\dot{x} = V \cos \gamma$. We make a small angle approximation, i.e., $\sin \gamma \approx \gamma$. Therefore, we get

$$V' = -\frac{g}{V} \gamma - \eta V C_D$$

$$\gamma' = \eta C_L - \frac{g}{V^2}, \quad z' = -\gamma \tag{28}$$

The equation for $V'$ can be solved analytically. Multiplying both sides by $V$ gives

$$VV' = -g \gamma - \eta V^2 C_D \implies \frac{V^2}{2} = e^{-2\eta C_D x} \frac{V_0^2}{2} - g \int_0^x e^{-2\eta C_D (x-\tilde{x})} \gamma d\tilde{x}$$

$$\implies \frac{V^2}{2} \approx e^{-2\eta C_D x} \frac{V_0^2}{2} - g(z - z_0)$$

If the pitch up is assumed to start at $x = 0$ and $z = z_0$, then the approximate final velocity, $V_f$, at $z = z_0$ and $x = x_f$ is given by

$$V_f = e^{-\eta C_D x_f} V_0 \tag{29}$$

where $V_0$ is the flight speed at the time of commencement of the pitch-up.

It now remains to find an expression for $C_L$, which would yield $C_D$ to compute $V_f$. Consider the last two equations in Eq. (28). It follows that

$$z'' = \frac{g}{V^2} - \eta C_L \tag{30}$$

We wish to command a constant value for $C_L$. To get an estimate, we could assume that $V$ is a constant. This is not very accurate, especially because perching usually involves a considerable deceleration. However, since the purpose is to obtain a simple yet reliable estimate, we could use the average speed $V_c = 0.5(V_f + V_0)$. In any case, the right hand side of (30) is a constant, and it follows that

$$z_f = 0 = \left( \frac{g}{V_c^2} - \eta C_L \right) \frac{x_f^2}{2} - \gamma_0 x_f \tag{31}$$

Furthermore, the value of $C_L$ has to be chosen to satisfy

$$C_L = \frac{g}{\eta V_c^2} - \frac{2\gamma_0}{\eta x_f} \tag{32}$$

The right hand side depends on $V_c = 0.5(V_f + V_0)$, where $V_f$ is obtained from Eq. (29) which depends, in turn, on $C_L$. Therefore, an iterative procedure is required to compute $C_L$. Equation (29) also sets a bound on the smallest attainable $V_f$ without stalling: $V_{f,min} = e^{-\eta C_D x_f} V_0$, where $C_{D,s} = C_{D_0} + k C_L^{2,\text{max}}$.

Figure 12 shows the $C_L$ required as a function of $\gamma_0$ for different $x_f$. The value of $C_L$ is seen to saturate for steeper flight path angles. At the same time, the final flight speed decreases with steeper initial flight path angle and a larger $x_f$. This observation can be explained by the fact that, in both cases, a larger distance is available for deceleration. Note, however, that once the saturation point is reached, a steeper glide renders the desired landing point unattainable. Wing twist can be used to execute a perching maneuver when the option of dropping below the landing point during the course of the pull-up is not available.39

Figure 12 illustrates the importance of perching in the post-stall flight regime, which is marked by high values of both $C_L$ as well as $C_D$ which, in turn, help ensure a reduced $V_f$. The problem of flying in the post-stall regime, though, is the possibility of loss of elevator effectiveness. This can be mitigated by using wing twist which allows the wing angle of attack to be increased to post-stall values without compromising the effectiveness of the horizontal tail.40 Wing twist can scheduled to hold the angle of attack of the wing constant, and the elevator can be commanded to maintain the angle of attack of the aircraft (the fuselage, to be precise). The availability of wing twist, together with high drag, allows for considerably large deceleration in a very short period of time following a steep, rapid glide.

Figure 12 also illustrates the importance of wing articulation. The ability to change the wing dihedral symmetrically can be used to fly steep glides without a steep increase in the flight speed. This ability would translate to a reduction in the terminal speed at the end of a perching maneuver. Note, however, that the amount of space available below the landing point constrains $\gamma_0$, the flight path angle at the end of the descent phase. A perching maneuver which starts with a steep drop is used by birds to perch on cliffside nests, and can be used by robotic aircraft to perch on power lines or ledges.
Figure 12. The $C_L$ required for perching, starting with $V_0 = 5 \, \text{m/s}$ for different values of $\gamma_0$ and $x_f$. The value of $C_L$ has been calculated using Eq. (32). The arrow indicates the direction of increasing $x_f$.

B. Aircraft and Data Acquisition

The experiments described in this paper were performed on a test MAV, shown in Figure 13, which is a modified version of the commercially manufactured ParkZone Ember 2. The geometric properties of the MAV are listed in Table 1. Both wings were free to rotate from a maximum $45$ deg dihedral to minimum $-15$ deg anhedral for a total arc range of $60$ deg. The actuators for wing dihedral, it may be recalled, are controlled independently on both wings for yaw stability and control.

The VICON motion-capture system, consisting of 16 infrared cameras, was used to collect flight data, in particular the aircraft position and spatial orientation, at 100 Hz. The VICON system uses triangulation to locate the markers with an accuracy of 1 mm. The real-time datastream provided by the VICON motion-capture system includes the global reference position and the Euler angles of each object. The availability of tracking data is contingent upon the visibility of the objects. For time-steps with information loss, which were minimal and rarely comprised consecutive frames, a linear fit was used to estimate the missing data. Experiments were performed within the effective volume of capture, containing an area of 6 m. $\times$ 4 m. and a height of 2 m. Since VICON provides only position and attitude information, a second order Lagrangian polynomial was used to compute velocities and angular rates, which were then filtered to eliminate noise.

One of the limitations in the MAV is the time lag in the actuator response. For example, the actual response of the wing dihedral angle and the elevator lags the commanded values by approximately $0.2$ s. Furthermore, the digital filters implemented to compensate for the time delay amplify noise in the output and are designed with a low order Padé approximation. Due to torque limitations of the servos and their limited ability to handle high wing-loading, the dihedral angles are typically $10 - 15$ deg higher than the

<table>
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<tr>
<td>Propeller Thrust</td>
<td>39</td>
<td>g</td>
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commanded values. Finally, we have no way of dealing with the problem of control reversal (i.e., changing sign of $N_{\delta_{\text{asym}}}$ in Eq. (14)) in a way which accommodates the large time delay.

C. Angle of Attack Control for Perching

Figure 14 shows the experimentally-measured longitudinal flight parameters. For these experiments, the wing dihedral was not controlled actively which caused the aircraft heading to deviate steadily from a straight line. An angle of attack of 5 deg was commanded while the flight speed and flight path angle were not controlled. The controller for the tailless aircraft yielded similar characteristics as the one with a vertical tail.

D. Lateral-Directional Control for Perching

In the aircraft with a vertical tail, local lateral stability was achieved using a simple PID controller. However, in several flight tests, the roll rate was seen to build up due to the dihedral effect and, without wing twist or ailerons, could not be compensated. This led to a divergent lateral-directional behavior despite local stability. Figure 15 shows the time histories for the case where the lateral dynamics were seen to be stable. A zero heading angle was commanded. The heading angle as well as sideslip converge to small values. However, the transient response does not vanish within the limited flight duration. Nevertheless, the yaw rate slows significantly by the end of the flight indicating good closed loop stability characteristics. Lateral control of a tailless configuration is under experimental investigation.
Figure 15. Experimental results showing the sideslip, velocity heading and the Euler angles measured during a yaw control test of the aircraft with a vertical tail. Parameters appear to be regulating during the short experiment.

E. Flight Path Control for Perching

An effective flight path controller is necessary for a successful perching maneuver. The aircraft must be able to track the desired flight path in order to arrive at a spatial target with an acceptable flight speed and height. The PID controller gains on the angle of attack controller were tuned to ensure sound tracking characteristics across a range of flight path angles. The flight path angle itself, as explained in Sec. VI-D, is controlled using the wing dihedral angles. Figure 16 shows the longitudinal parameters for two experiments where flight path angle tracking demonstrated. It is important to note that the flight path angle dynamics are considerably slower than the duration of the experiments, which is why a convergence is very difficult to obtain in the course of every flight test.

Figure 16. Angle of attack and flight path angle during flight path guidance control. The flight path angle was controlled using the wing dihedral angles.
F. Experimental Demonstration of a Perching Maneuver

In conjunction with the guidance controller, a perching maneuver is executed as follows. An appropriate altitude is chosen such that a perching command is sent when the aircraft crosses it. This value was chosen to accommodate the actuation time delays for the wing dihedral as well as the elevator. Until this point, the angle of attack and flight path angle controllers described in Sec. VI were used actively. Once the aircraft reaches the prescribed altitude, zero dihedral and maximum pitch-up elevator angles are commanded. These signals are held until touch-down. Figure 17 shows the perching signal sent at the 0.6 s mark. The angle of attack builds up to 30 deg, causing the speed to reduce, and the aircraft climbs momentarily. Flight speed is halved within 1 s to 3 m/s. After a brief ascent, the MAV lands at a low angle of attack. It is interesting to note that the final speed has reduced substantially even without using wing twist. Addition of wing twist would not only enable a further reduction in the final speed, but also provide for better roll and yaw control during the approach.

![Figure 17. Flight parameters during a perching attempt that was triggered at 1.5m above the ground](image)

Experiments are currently under way on an aircraft which uses ailerons for roll control as well as for ensuring that $N_{\delta asym}$ does not change sign in the flight envelope. This will allow us to use the yaw controller during the course of the entire perching maneuver, particularly during the pull-up phase. The ailerons on either wing are controlled independently of each other, which means that they can be used as conventional flaps as well to reduce the terminal speed further. Eventually, a claw or a suction pad may be added to the aircraft to ensure that the aircraft performs a perched landing at the desired spot.
VIII. Conclusion

Bat-like flight is a challenging problem that cannot be solved via averaging or with traditional tail-derived stability. We have demonstrated the ability to stabilize and control longitudinal motions via CPGs with the RoboBat. As expected, the top-level controllers are of low dimension and can be made very simple, because most of the hard work is done by the CPGs. Given the extent of mechanical coupling in the design, it is remarkable that such control was immediately as effective as it was. Further work can still be done to create a pattern generator layer so to optimize the output waveforms. Additionally, we expect to quantify the forces and moments actually produced via the aerodynamic model, so that we can make better predictions for a free-flying robotic bat.

As mechanical design of actuators develops, we expect robotic fliers in free flight to be able to utilize the key feature of phase synchronization and control of phase differences in stability and control of body motions. The major problem of identifying a method of proving such stability analytically is still open. However, this paper has demonstrated the result experimentally. Since this CPG controller design also features rapid inhibition of oscillation, it leads to the important problem of gliding flight and maneuvers while gliding.

For this reason, we described perching experiments using a novel MAV concept featuring independent wing dihedral actuation for longitudinal as well as yaw control. A guidance and control scheme was designed for the MAV and closed loop experiments were performed indoors to demonstrate its perching capability. Preliminary results indicate sound yaw control characteristics. Future work will focus on improving the lateral-directional control capability of the wing dihedral mechanism and adding heading tracking capability.

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