Reply to the review by John Kalogiros on "Measuring the 3-D wind vector with a weight-shift microlight aircraft"

We thank John Kalogiros for his valuable comments on this manuscript and the detailed feedback. In below text we hope to answer your questions and clarify the approach of our study. The comments by the reviewer are indicated with an asterix (*) and are cited in italics, followed by our reply.

General Comments

* *The subject of this manuscript is the calibration of a five-hole turbulence probe on WSMA aircraft, which is a relatively original subject, well within the scope of AMTD, with good research applications. As the authors conclude the main issue is wing upwash correction due to the specific conditions in WSMA: trike rotational freedom (mainly roll angle difference from wing) and aeroelastic wing. The paper is probably too long and difficult to follow due to many redundant technical details, which could probably omitted or simply mentioned in short. For the same reason the Appendices could be omitted because they reproduce other papers (Lenschow, 1986; Williams and Marcotte, 2000).*

The manuscript is intended to make wind measurement from weight-shift microlight aircraft (WSMA) reproducible for potential users. A suitable combination of measurement elements and transformation equations is introduced. The comprehensive presentation is desirable for two reasons. Firstly the computation of the wind vector can differ considerably in detail (e.g., Tjernström & Friehe, 1991, van den Kroonenberg et al., 2008, Williams & Marcotte, 2000). Secondly the presented uncertainty propagation allows a transparent evaluation throughout the various steps of the wind computation. The Appendices were used in order to provide sufficient detail but not overload the main article. Here the reader is provided with details of, and modifications to nine references, aggregated in consistent notation with the main paper. We propose to remove Appendix B2 'Uncertainty measures' from the <u>manuscript</u>.

* *Among the minor points given below there is also a major point in data processing. More specifically Figure 8 shows decrease of upwash angle with measured lift coefficient, which is unrealistic and the possible cause is mentioned. This is a critical point because this negative slope is used to establish a real-time correction for upwash, which however is in error.*

Basic aerodynamic laws predict an increase of the wing induced upwash with the lift coefficient, as stated in Eq. (3). However the quantity treated in our correction is not the isolated upwash induced by the wing, but the net flow distortion by the WSMA at the five-hole pressure probe (5HP) location. This also includes the variation in distance and orientation between the 5HP and the wing, propeller thrust, as well as the flow around the trike. In reply to "Page 1328, Eq. (7) and Fig. 8" (below) we explain in detail how these effects are counteracting the wing induced upwash, and propose to expand on it in the <u>manuscript</u>. The result is a decrease of the net upwash attack angle with measured lift coefficient, which is presented in Fig. 8.

Specific Comments

* **Page 1306, third line from the end.** *The root mean square deviation should probably be renamed to root mean square error in order to discriminate it from the standard deviation which has to do sensor uncertainties.*

The root mean square deviation will be renamed in the manuscript.

* **Page 1310, Eq. (1).** *The accurate definition of lift is the force (i.e. acceleration) perpendicular to the free airstream instead of the vertical one. The difference may be significant when there is significant vertical velocity of the aircraft e.g. fast ascent and descent during forced oscillation manoeuvres (calibration flight patterns).*

While approximately constant during level flight, the free airstream direction varies within ±10° during the forced oscillation manoeuvres VW3. Due to the low sensitivity of the cosine for small angles, the geodetic normal acceleration and the acceleration perpendicular on the airstream differ by ±1 %. Since the VW3 manoeuvre was only used for evaluation, small angle approximation was applied. The VW1 (level acceleration - deceleration) flight pattern used to derive the upwash correction is a level flight, and not subject to similar approximation.

We propose to insert at page 1310 line 5 of the manuscript: "For simplicity the acceleration perpendicular to the airstream was approximated by the vertical acceleration in the GCS. The maximum deviation during severe vertical manoeuvring (only used for validation) does not exceed ±1%.".

* **Page 1310, Eq. (3).** *This equation is valid for solid wing with elliptical loading. The wing of WSMA is quite different not only due to aeroelasticity but also due to its shape (delta like wing with considerable sweep and small aspect ratio, i.e. significant downwash induced by wing tips), which results in different (lower, i.e. less lift) proportionality factor between upwash velocity and the product $V_{tas}CL$.*

This equation is a simplified aerodynamic model. It solely considers the upwash ahead of an elliptical wing, but no other sources of flow distortion. Introducing a lower proportionality factor could adjust the upwash model to the properties of the WSMA wing. The consequence would be a decrease in the magnitude, but also in the amplitude of the modelled upwash. Yet the amplitude of the modelled upwash is (a) already lower and (b) phase reversed compared to the observed upwash (Fig. 7). Therefore a lower proportionality factor alone can not explain the difference between the modelled upwash and the observed net flow distortion.

We propose to insert at page 1327 line 4 of the manuscript: "Introducing a lower proportionality factor to Eq. (3) could account for the particular properties of the WSMA wing. This would reduce the magnitude of the modelled upwash, but could not explain the higher variability as well as the phase inversion of the observed upwash.".

* **Page 1311, top and Page 1321, line 12.** *The "upwash attack angle ξ" is simply the angle of the longitudinal body axis with the line connecting the probe, which is below the wing, with the wing aerodynamic centre (pressure centre) and should not be called "upwash attack" angle in order to avoid confusion with upwash attack angle in Eq. (7) and Fig. 8.*

The direction of the wing induced upwash (ξ) can be more fittingly termed 'wing upwash angle'. This should help to avoid confusion with the 'upwash attack angle' (α_{up}) as used in Eq. (7) and Fig. (8). We propose to rename all instances in the manuscript.

* **Page 1311, second paragraph.** *Due to quite possible deviation of the flow around probe from the theoretical spherical model (which the authors have actually found during calibration) it is useful to measure dynamic pressure also with a Pitot probe which is quite insensitive to flow angles up to 20 degrees. In this way, they will be also able to diagnose the deviation of the flow around the probe from the spherical model. Such a test is described in section 3b of Kalogiros and Wang (2002b).*

The Pitot probe method can be useful to characterize the flow around the radome of large fixed wind aircraft. Unlike Kalogiros & Wang, 2002 the wind measurement on the WSMA was not conducted with a radome, but with a 5HP in front of the aircraft. The flow around the 5HP and aircraft nose cap was evaluated in a wind tunnel. Throughout 570 combinations of flow angles and dynamic pressure the deviation (RMSE) from the spherical model was 0.4° for the flow angles, and 0.04 hPa for the dynamic pressure. This is well within the effects of sensor accuracies, i.e. 0.6° and 0.06 hPa, respectively (Table 5).

We propose to insert at page 1324 line 14 of the manuscript: "The wind tunnel study proofs the applicability of the spherical model, Eqs. (A5) - (A7), to determine flow angles and dynamic pressure from our 5HP: the deviations are well within the effects of sensor accuracies, i.e. 0.6° and 0.06 hPa, respectively (Table 5). Consequently in-flight tests with similar scope were omitted.".

* **Page 1314, Eq. (4).** *There is an error. The β (sideslip angle) and α (attack angle) should replace each other in the equations. Also, the authors with the terms "mechanical" and "measured" flow angles probably mean spherical coordinates (like latitude and longitude) and "projection" angles (like the ones used by Lenschow, 1986 or Williams and Marcotte, 2000). The sideslip angle is the same between these angle systems, while the attack angle differs (in the latter system it is the "latitude" angle of the projection of the point of the sphere on the plane β=0).*

In our manuscript we distinguish between flow angles (α, β) 'measured' by the 5HP, and flight mechanical angles (or spherical coordinates, $\tilde{\alpha}$, $\tilde{\beta}$), used e.g. in wind tunnel experiments. The former are the angles between different projections of the true airspeed vector, i.e. between the aerodynamic- (ACS) and the body coordinate systems (BCS), as used in Lenschow, 1986, Williams & Marcotte, 2000. The latter are the actual rotation angles between these coordinate systems. The exact appearance of transformation Eq. (4) depends on the order of rotations, which can be reversed. Throughout the manuscript we follow Boiffier, 1998: First a rotation of the ACS about the vertical axis by the angle $-\tilde{\beta}$ and secondly about the transverse axis by the angle $\tilde{\alpha}$ describes the airspeed vector $\boldsymbol{v}_{tas,b} = (v_{tas,u}, v_{tas,v}, v_{tas,w})$ in the BCS:

$$v_{tas,u} = -|v_{tas}| \cos(\tilde{\alpha}) \cos(\tilde{\beta})$$
$$v_{tas,v} = -|v_{tas}| \sin(\tilde{\beta})$$
$$v_{tas,w} = -|v_{tas}| \sin(\tilde{\alpha}) \cos(\tilde{\beta})$$

Yet the rotation angles $\tilde{\alpha}$ and $\tilde{\beta}$ are not known from the 5HP measurement. From the geometric definition of the trigonometric function it follows Bange, 2007:

$$\cos(\alpha) = v_{tas,u} / \mathrm{sqrt}(v_{tas,u}^2 + v_{tas,w}^2) = \cos(\tilde{\alpha}), \text{ and}$$
$$\tan(\beta) = v_{tas,v} / v_{tas,u} = \tan(\tilde{\beta}) / \cos(\tilde{\alpha})$$

Using only the measurable angles α and β in the above relations, the resulting airspeed vector equals the definition of Lenschow, 1986, as given in Eq. (A11). In analogy the transformation Eq. (4) has to be applied to compare the flight mechanical angles of the wind tunnel to the flow angles at the 5HP. A numerical example is given in the Appendix of this reply (below).

We propose to insert at <u>page 1314 line 10 of the manuscript</u>: “The exact appearance of transformation Eq. (4) depends on the order of rotations, see Eq. (A11), which can be reversed.”.

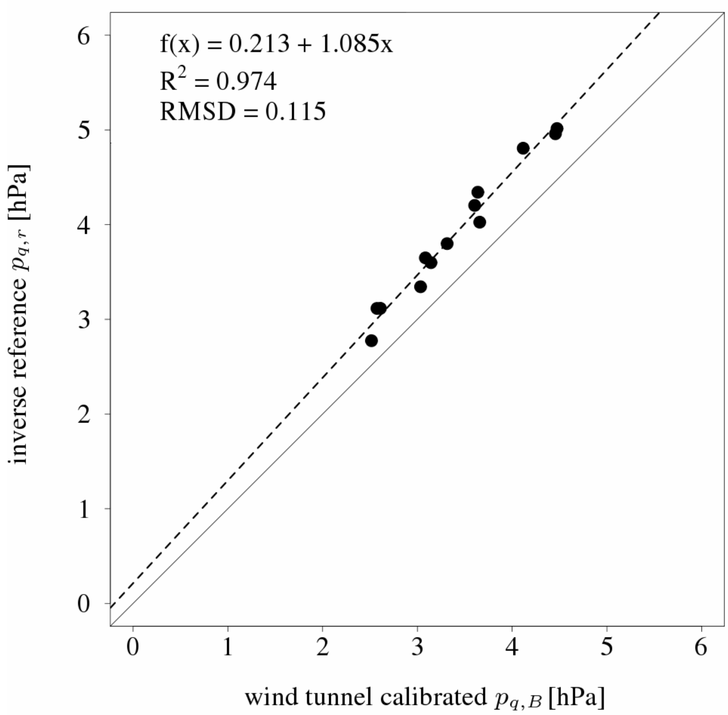
* **Page 1314, Racetrack pattern.** *Mean wind direction is usually not known with sufficient accuracy at flight level to use it for in-flight calibration purposes. A better method for calibration using this flight pattern (also known as reverse heading manoeuvre) is to carry it out in a random direction and require that the estimated components of horizontal winds are the same in both directions, which differ by 180 degrees. This can be done by comparing the average wind components or minimizing the total difference of wind components estimated at the same positions of the flight directions.*

* *Also, with the phrase “...adjusting dynamic pressure in Eq. (A8)” the authors mean estimating a calibration bias (offset) or slope? Their Table 4 implies probably the second. What could be the reason for this slope (higher than unit)? It is the deviation from the spherical model of the flow around the probe or the flow distortion by the aircraft? The first should be taken care by the wind tunnel calibration. The second is usually known for aircraft with fuselage and pressure ports on it as static pressure defect, which also affects (increases) the measured static pressure at the same magnitude but with opposite sign and this is not applied by the authors.*

* *In addition, the turbulence probe is within the propeller flow “tube”, which implies an increase of measured dynamic pressure and a decrease of the same magnitude of the measured static pressure relative to the free atmosphere. The engine is probably weak and the distance of the probe to propeller is probably large enough at 3.5 m, which may result in small effect of the propeller on the probe measurements. The level*

acceleration-deceleration flight pattern (constant altitude speed run maneuver) can show this effect of the propeller on measured static pressure as the difference between acceleration (close to full engine thrust, maximum propeller effect) and deceleration (low engine thrust, small propeller effect) as described in section 2 of Kalogiros and Wang (2002b).

The wind square and reverse heading patterns were carried out in multiple directions. In Eq. (6) the wind square patterns were analyzed in a way corresponding to the reviewer's suggestion for reversed heading manoeuvres. From all reverse heading patterns 14 racetracks pairs were found to be suitably aligned with the mean wind direction. The alignment enables the use of independent data from the inertial navigation system to adjust the true airspeed flow measurement to in-flight conditions. This is a standard procedure (e.g., Leise & Masters, 1993, Williams & Marcotte, 2000), and was successfully realized for the WSMA:



The measured true airspeed is predominantly sensitive to dynamic pressure $p_{q,B}$ (Table 5). This enables to calculate an inverse reference of dynamic pressure ($p_{q,r}$), requiring to minimize Eq. (5) by free iteration. As mentioned in Section 4.1 Step D, the slope correction was used to account for the loss in $p_{q,B}$ (and consequently true airspeed) magnitude due to the net flow distortion: the general direction of the wing upwash as modelled from Eq. (3) is forward, right and upward (Fig. 5), while slipstream from the propeller is directed backward and upward at the 5HP location. The (not significant) regression offset was considered as inversion residue of atmospheric inhomogeneities, and consequently discarded.

To preserve the sum or pressures (see also reply to Page 1324, Step C – Tower fly-bys, below) the measured static pressure should be corrected by the same magnitude, but with opposite sign, as the dynamic pressure. However the maximum correction at

6 hPa dynamic pressure (0.51 hPa) scarcely exceeds the uncertainty in the static pressure offset (0.43 hPa, Section 4.1 Step C). Using the static pressure sensitivity of the true airspeed computation (-0.01 m s^{-1} hPa^{-1}) in Table 5, the neglected systematic error in the wind measurement is ≤|0.0051| m s^{-1}.

In comparison with reference static pressure measurements on the ground (Section 4.1 Step C), no significant relation between true airspeed (as proxy for propeller thrust) and the static pressure offset was found (slope -0.09 v_{tas}, R^2=0.05). Over the full range of true airspeed of the WSMA the corresponding systematic error in the static pressure measurement is in the order of 1 hPa. At a flight altitude of approx. 1000 m a.s.l. of the level acceleration - deceleration pattern VW1 (or constant altitude speed run) proposed for calibration, this systematic error corresponds to 10 m difference in altitude. This is in the order of the aircraft altitude fluctuations, which is one of the reasons it was impossible to isolate the influence of propeller thrust. Moreover during level flight at different true airspeed (or horizontal acceleration), not only propeller thrust, but also the flow around the wing and trike, as well as the orientation and distance between the 5HP and the wing vary. Again, this potential net effect on the static pressure should be of little concern, due to its low sensitivity in the true airspeed computation.

We propose to amend page 1325 line 5 ff. of the manuscript: "We have seen that the wing upwash in front of the wing of the WSMA is effective forward, right and upward (Fig. 6), while the propeller slipstream is directed backward and upward at the 5HP location in body coordinate system. As net effect we find that the magnitude of dynamic pressure ($p_{q;B}$) measured at the 5HP tip, and with it the calculated true airspeed, is reduced. The slope correction from racetracks was used to account for this loss in $p_{q;B}$. The suggested offset was considered as inversion residue of atmospheric inhomogeneities during the racetrack manoeuvres, and consequently discarded. Also an analogous correction for the static pressure measurement has been discarded: at a flight altitude of approx. 1000 m a.s.l. the maximum correction at 6 hPa dynamic pressure (0.51 hPa) corresponds to ≤10m difference in altitude. This is in the order of the aircraft altitude fluctuations. With this the accepted error is in the same order as the uncertainty of the static pressure offset from tower fly-bys.".

* **Page 1317, Eq. (6).** *The uncertainties of airspeed and sideslip angle given are actually uncertainties of wind components. The uncertainty σ_β is not even dimensionally correct (m/s units, not degrees) to claim it a sideslip uncertainty.*

The paragraph preceding Eq. (6) explains: along-track wind components are predominantly sensitive to errors in v_{tas}, while cross-track wind components are predominantly sensitive to errors in β. This property is used to isolate the uncertainty in the wind components originating from v_{tas} and β. To avoid confusion we propose to rename respective uncertainties in the manuscript, Eq. (6) to $\sigma_{uv,tas}$ and $\sigma_{uv,beta}$.

* **Page 1318, VW3 (Forced oscillation).** *The aerodynamic response of wing to forced oscillations due to pilot actions is different from the response to turbulence (travelling air disturbances, wind oscillations). More details on this difference and the real-time estimation of turbulence effect on upwash are given in section 3 of Kalogiros and*

Wang (2002b). Thus, this manoeuvre does not give the correct information for evaluation of the effect of thermal turbulence in ABL on the flow around the aircraft.

The WSMA possesses a relatively high ratio of climb rate to true airspeed (≤0.2), which enables it to follow topographical contours. A potential application area of the WSMA wind measurement is to determine turbulent fluxes at low and constant altitude above complex terrain. Here pilot actions are required in order to follow the terrain, which is approximated in excess by the forced oscillation pattern.

We propose to amend page 1318 line 24 f. of the manuscript: "VW3 was used to assess the integral influence of vertically accelerated flight on the vertical wind measurement, e.g. during terrain following flights in the ABL.".

* **Page 1324, line 11.** *Define "working" angle.*

In the legend of the related Fig. 6 the working angle is defined as acos(cos(α) cos(β)), in accordance with Crawford & Dobosy, 1992. We propose to include the definition also in the manuscript text on page 1324 line 10.

* **Page 1324, Step C – Tower fly-bys.** *As mentioned in a previous comment if an offset adjustment due to position error is applied to static pressure measurement a same magnitude, but opposite sign adjustment should applied to dynamic pressure (i.e. total pressure remains constant). Also, "Table 3" in line 21 should be "Table 4".*

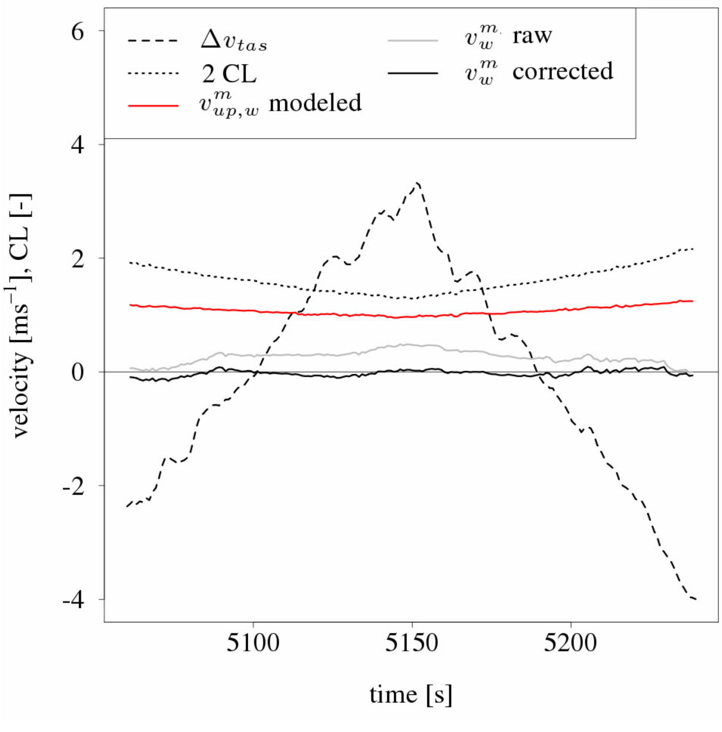
In contrast to e.g. Kalogiros & Wang, 2002, static- and total pressure ports are located only165 mm apart from each on the 5HP (Fig. 2). The dynamic pressure is measured using a differential pressure sensor between these ports. If there is a position error, say due to the high pressure field below a moving wing, it can be expected to have the same effect on both, static- and total pressure ports. Therefore the sum of pressures can be written as $p_d = (p_t - p_0) - (p_s - p_0)$, with measured dynamic pressure p_d, total pressure p_t, measured static pressure p_s, and the position error p_0. It can be seen that (a) static pressure must be corrected for the position error while (b) dynamic pressure must not be corrected.

We propose to insert at page 1324 line 23 of the manuscript: "The dynamic pressure (p_q) is measured using a differential pressure sensor between the static and total pressure ports (Fig 3). These ports are located only 165 mm apart from each other, and are therefore subject to the same position error. Consequently position error cancels out in the p_q measurement.".
Also "Table 3" will be renamed to "Table 4".

* **Page 1326, end of page, Fig. 7, and page 1327, top of page.** *As pointed out in a previous comment Eq. (3) is not expected to be exactly valid for the wing of WSMA. However, the main conclusion from Eq. (3), which is that upwash is proportional to airspeed and lift coefficient, should be valid but with smaller magnitude (i.e. less lift) of the proportionality factor.*

** During the forced oscillation manoeuvre airspeed also varies in addition to lift coefficient and in a different way. Thus, there could be a phase difference between lift coefficient and upwash. Also, there are significant altitude changes (i.e. significant vertical velocity of the aircraft), which give a small error in the estimation of lift by Crawford's model Eq. (1) as mentioned in a previous comment. Furthermore, there should be a propeller effect during accelerations as mentioned in a previous comment. I assume that the measured upwash was estimated as the remaining air vertical velocity assuming zero actual wind velocity above ABL and that the proper rotational transformation has been applied with an angle of -41.9 degrees and roll angles difference between trike and wing, because the probe is below the wing and, thus, the upwash direction at the probe is not vertical. With the above details in mind I don't think that it can be concluded from Fig. 7 that the general upwash model (upwash, i.e. wing circulation, proportional to airspeed and lift coefficient) is not valid in the case of WSMA. This conclusion would be unrealistic and not in agreement with typical aerodynamics.*

As pointed out previously, the quantity assessed here is not the isolated upwash induced by the wing, but the net flow distortion (or net upwash for its vertical component) by the WSMA at the probe location. The general direction of the observed net flow distortion (forward, right and upward) is in agreement with the modelled wing upwash from Eq. (3). Using Eq. (A13) the latter was rotated about the wing upwash angle and roll angle difference into trike body coordinates (Fig. 5). In contrast to the modelled wing upwash, the observed net upwash (the difference between the uncorrected vertical wind and zero) is phase inverted, i.e. negatively correlated with the lift coefficient, and has lower magnitude but greater variability (Fig. 7). These findings are confirmed by the level acceleration - deceleration flight VW1:



This pattern does not include altitude changes, and due to its long period (180 s) the lift coefficient can be expected in phase with the wing induced upwash. As outlined before, the negative correlation between lift coefficient and upwash can not be addressed by introducing a lower proportionality factor to Eq. (3).

We propose to complement Fig. 7 in the manuscript with above figure.
Also page 1326 line 24 ff. should be amended: “Assuming a constant vertical wind, not necessarily but likely approaching zero above the ABL, variations in the vertical wind measurement are referred to as “net observed upwash“. As opposed to the parameterization by Crawford et al. (1996) for fixed-wing aircraft, the net observed upwash at the five hole probe location is smaller by one order of magnitude but more variable, as well as phase inverted with CL. These findings are confirmed with the level acceleration – deceleration flight VW1 with a long period (180 s) and negligible vertical velocity (Fig. 7). With it a potential phase difference between airspeed and wing loading during the VW3 flight can be ruled out as explanation for the antagonistic relationship between CL and the observed upwash.”.

* **Page 1328, Eq. (7) and Fig. 8.** *Continuing the previous discussion for the upwash model, the proportionality of upwash with airspeed and lift coefficient translates to an upwash attack angle proportional to the lift coefficient. It’s typical in aircraft aerodynamics that the lift coefficient of a wing increases with attack angle of free airstream ranging from zero lift angle to stall. For the aeroelastic wing of WSMA the lift coefficient may be simply considered to be dependent on airspeed due to changes in the shape of the wing as the authors point out at the end of page 1308 (section 2). If the negative slope seen in Fig. 8 was real this would imply that when lift increases (i.e. wing circulation increases) then upwash decreases. But, wing circulation is proportional to upwash. Also, a negative slope implies that at zero lift coefficient (i.e no lift, no wing circulation and no upwash) the upwash attack angle is maximum!!! Thus, the negative slope of upwash attack angle versus lift coefficient cannot be realistic.*

For a change of true airspeed during level flight from 30 m s^{-1} (low lift coefficient) to 20 m s^{-1} (high lift coefficient) the following effects contribute to the observed net upwash:

- increase of upwash production from the wing according to Eq. (3);
- decrease of wing circulation effective at the 5HP through 15% larger distance between 5HP and wingtip (pitch and roll effect);
- decrease of propeller induced upwash, which is located 0.8 m above the 5HP measurement.

The latter effects counteract the wing induced upwash. In addition the shape of the aeroelastic wing, as well as the flow around the trike changes. The sum of these effects, i.e. the observed net upwash, is correlated with lift coefficient (-0.53), true airspeed change (0.57), and wing pitch (-0.50). Due to existing formulations (Crawford et al., 1996, Kalogiros & Wang, 2002) we decided to use the lift coefficient as proxy for a correction of the net upwash distribution in the flow angles α and β.

We propose to amend page 1327 line 10 ff. of the manuscript: “Considering a change from high true airspeed (low lift coefficient) to low true airspeed (high lift coefficient) during level flight actually a number of effects contribute to the observed net upwash:

(a) increase of upwash production from the wing according to Eq. (3), (b) decrease of wing circulation effective at the 5HP through larger distance and opening angle between 5HP and wing, and (c) decrease of propeller induced upwash. The latter effects counteract the wing induced upwash. In addition (d) the shape of the aeroelastic wing, as well 25 as (e) the flow around the trike change. Therefore the net upwash of a WSMA can neither be parameterized nor corrected with the Crawford et al. (1996) wing upwash model alone. Garman et al. (2008) on the other hand proposed to correct for upwash by considering the actual wing loading factor (LF), which carries information on the aircraft's vertical acceleration. In contrast to the study of Garman et al. (2008), WSMA weight, fuel level as well as dynamic pressure (p_q) are known. Therefore CL can be directly determined and used instead of LF. This has the advantage that information on the aircraft's trim, i.e. information on above effects (b) - (e), is included: as formulated in Eq. (2), p_q carries information on v_{tas} at given air density. Over eight independent flights of patterns VW1, VW2 and VW3 the observed net upwash is correlated with CL (-0.53±0.16), change in v_{tas} (0.57±0:16), and wing pitch (-0.50±0.20).",
as well as page 1328 line 16: "As outlined above the complex interaction of wing upwash and aeroelasticity, distance and opening angle with the 5HP, propeller slipstream and flow around the trike is collectively correlated in CL. This offers the possibility of a dynamic treatment of the net flow distortion in one single explanatory variable.".

** If the authors used Eqs. (8) and (9) from Garman et al. (2008) to compute the upwash attack angle I note that there are sign errors in tan(β) and especially the sin(θ) term in these equations. I think that this is the reason in that paper the authors have also "observed" a negative slope of upwash attack angle versus lift coefficient similar with the current paper (despite their aircraft had a rigid wing unlike WSMA). If in addition the authors of the current paper used this attack angle to estimate upwash after multiplication with airspeed then the measured upwash in Fig. (7) is also in error.*

The equations of Garman et al., 2008 were not used to determine the upwash attack angle. Instead the attack angle was freely iterated until yielding zero vertical wind for flights above the ABL. Following Eq. (7) the difference of the attack angle observed at the 5HP and this inverse reference is presented as upwash attack angle. For the level acceleration - deceleration (vertically unaccelerated) flight pattern VW1 the above effects result in a decrease of the net upwash attack angle with measured lift coefficient (Fig. 8). The same correction however also holds true for the vertically accelerated flight patterns presented in Figs. 7 and 9 and the tower comparison (Fig. 12), where as a result the RMSE is reduced by 31%.

** However, an equation similar to Eq. (7) (i.e. "including" the measured lift coefficient) using acceleration measurements is valid for the real-time upwash correction of measured attack angle regardless of the wing aeroelasticity (i.e. the possible dependence of lift coefficient on airspeed). A Fourier method to estimate the appropriate parameters (the actual response function of the wing) using real-time data in the ABL and compute correct time series of attack angle was presented in section 3, Eqs. (6) and (7) in Kalogiros and Wang (2002b). The processing in* *frequency space is needed because the response function of the wing is frequency dependent and not a constant over all frequencies. In the case of WSMA a rotational transformation for the angle and the roll angles difference between trike and wing is needed because the upwash direction at the probe is not vertical, in order to separate the upwash in vertical and horizontal components.*

With the Fourier method proposed by Kalogiros & Wang, 2002 the frequency dependence of the wing induced upwash can be modelled. The distinct difference from time domain methods is an amplified (approx. 20 %) upwash correction in the inertial subrange of atmospheric turbulence compared to lower frequencies. Due to minor contributions of the inertial subrange, the effect on the eddy flux measurement at the flight altitude (≤4%) was however found relatively small. At the same time a transformation from the wing to the trike coordinate system would be required for the WSMA, which potentially introduces phase shifts. Also the remaining effects outlined above would not be considered, and could only be isolated with considerably more in-flight data.

We propose to amend page 1334 line 17 ff. of the manuscript: "With the Fourier method proposed by Kalogiros and Wang (2002b) the frequency dependence of the wing induced upwash can be modelled. The distinct difference from time domain methods is an amplified (≈20%) upwash correction in the inertial subrange of atmospheric turbulence compared to lower frequencies. Due to little contributions of the inertial subrange, the effect on the eddy flux measurement at flight altitude (≤4%) is however relatively small. At the same time a transformation from the wing to the trike coordinate system would be required, carrying a potentially variable phase difference. Moreover the interactions with propeller and trike, resulting in the net flow distortion, remain untreated. Isolating these interactions would require considerably more in-flight data and analytical effort. In return such procedure could address forenamed dependence of the wind components on v_{tas} and additionally allow for superior wind measurements during turning manoeuvres.".

*** Page 1334, line 20, "dynamic flight modes… require infinitely more in-flight data".** *As pointed in the previous comment the use of the measured lift coefficient (i.e. acceleration measurements) is the only measurement required for a simple real-time (dynamic) correction, which will also result automatically in correct energy of air vertical velocity in the inertial subrange of its spectrum. I note again that the aerodynamic response of wing (i.e. the changes of upwash) to turbulence (travelling air disturbances) is different than its response to forced pitching oscillation. The first is of interest in the case of aircraft turbulence measurements. For better quality measurements the interference of the pilot with control actions should be minimal (i.e. smooth straight flights are preferable).*

See previous reply, as well as the reply to Page 1318, VW3 (Forced oscillation). We propose to remove all instances referring to improved wind measurement under the influence of thermal turbulence from the manuscript.

*** Page 1337, Appendix A, Eqs. (A5) and (A6).** *These equations are the approximate equations of Williams and Marcotte (2000) for small attack and sideslip angles. Why not use their exact analytical equations which are valid for larger flow angles, too?*

This should be more appropriate for a slow moving aircraft like WSMA. I assume also that in Eq. (A7) $p_{q,B}$ is the dynamic pressure p_q used in the rest of the Appendix and in the main paper ($p_{q,B}$ is also used in the main paper). Probably $p_{q,B}$ should replaced by p_q to avoid confusion.

As outlined on page 1324 lines 3 - 8, we have also tested exact analytical equations. Those of Williams & Marcotte, 2000 were found to work less accurate with our probe design (1.7° and 1.5° RMSE for α and β, respectively), as compared to the approximate equations (both 0.4° RMSE). While β was indeed slightly less scattered as compared to the approximate equations, α and β were both underestimated at elevated angles. We speculate that this behaviour arises from the amplified pressure drops in the attack and sideslip differential pressures at elevated angles. In contrast to their 1.5 mm pressure ports, the dynamic pressure is measured against a direction-independent static pressure port (Fig. 2). While allowing for slightly more scatter due to angular cross-dependency, the approximate equations compensate the difference in pressure drops. In addition a calibration polynomial was tested, but rejected due to insufficient robustness during in-flight use.

As introduced in the preceding paragraph, the subscript B in the dynamic pressure $p_{q,B}$, Eq. (A7), distinguishes the calibration stage B (wind tunnel) from A (laboratory). For the remainder of the Appendix the general variable for dynamic pressure p_q is used to remain valid during all calibration stages.

We propose to insert after page 1324 line 4 of the manuscript: "We speculate that this behaviour arises from the amplified pressure drops in the attack and sideslip differential pressures ($p_{\alpha,\beta}$) at elevated angles. In contrast to their 1.5 mm pressure ports, the dynamic pressure ($p_{q;A}$, subscript upper-case letters A–G indicating calibration stage) is measured against a direction-independent static pressure port (Fig. 3). While allowing for slightly more scatter due to angular cross-dependency, the approximate Eqs. (A5) and (A6) compensate the different pressure drops in the quotient $p_{\alpha,\beta}$ / $p_{q;A}$.".

* **Table and figure legends.** *Many of them are too long and should be shortened. The details can be given in the text during the presentation of the corresponding tables or the figures.*

We tried to provide the information necessary to read a table or figure in its respective caption. The intention is to spare the reader from going back and forth in the main text. If desired this can be changed in the manuscript.

Appendix: an R routine for the conversion between flow angles and flight mechanical angles

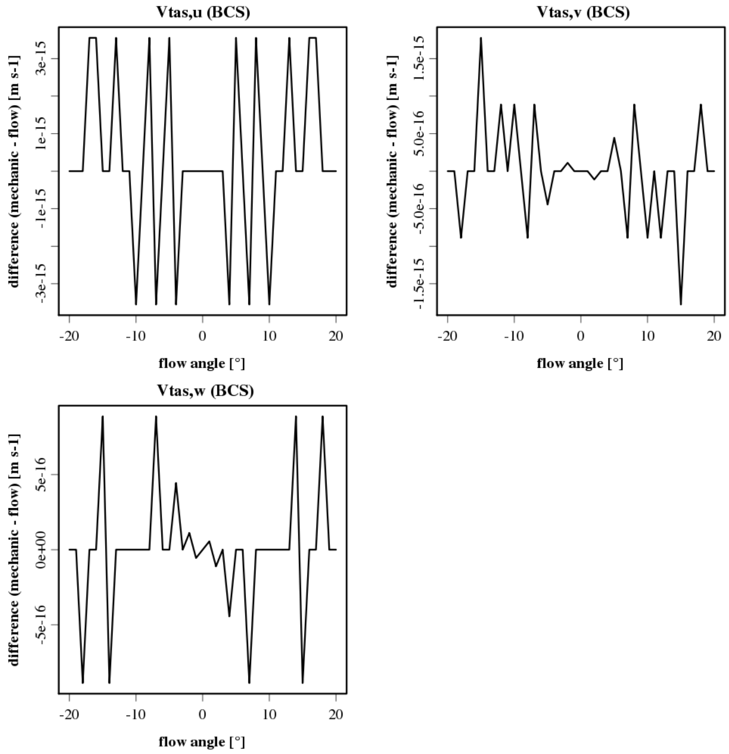
```
#(A) VARIABLES
  #true airspeed [m s-1]
    vtas <- 27
  #flow angles in degree
    dum <- -20:20

#(B) 3D TRUE AIRSPEED WITH FIVE HOLE PRESSURE PROBE (5hp) MEASURED FLOW ANGLES
  #flow angels in radians
    alp5hp <- dum / 180 * pi
    bet5hp <- dum / 180 * pi
  #true airspeed u, v, w in aircraft body coordinates (BCS) according to the definition of Lenschow, 1986, as given in Eq. (A11)
    D5hp <- sqrt(1 + tan(alp5hp)^2 + tan(bet5hp)^2)
      uL5hp <- vtas / D5hp
      vL5hp <- vtas / D5hp * tan(bet5hp)
      wL5hp <- vtas / D5hp * tan(alp5hp)

#(C) 3D TRUE AIRSPEED WITH FLIGHT MECHANICAL ROTATION ANGLES (fmr)
  #conversion according to Eq. (4)
    alpLfmr <- alp5hp
    betLfmr <- atan(tan(bet5hp) * cos(alp5hp))
  # true airspeed u, v, w in aircraft body coordinates (BCS) according to Eq. (C32) in Boiffier, 1998
    uLfmr <- vtas * cos(alpLfmr) * cos(betLfmr)
    vLfmr <- vtas * sin(betLfmr)
    wLfmr <- vtas * cos(betLfmr) * sin(alpLfmr)

#(D) COMPARISON (C) - (B)
  par(mfrow=c(2,2))
    plot(dum, uLfmr - uL5hp, type="l", xlab="flow angle [°]", main="Vtas,u (BCS)", ylab="difference (mechanic - flow) [m s-1]")
    plot(dum, vLfmr - vL5hp, type="l", xlab="flow angle [°]", main=" Vtas,v (BCS)", ylab="difference (mechanic - flow) [m s-1]")
    plot(dum, wLfmr - wL5hp, type="l", xlab="flow angle [°]", main=" Vtas,w (BCS)", ylab="difference (mechanic - flow) [m s-1]")

# -> the true airspeed components are only identical (within numerical accuracy 1e-15 m s-1) when using conversion Eq. (4):
```



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